UNIVERSITY OF SURREY

Investigation into the fire and racking behaviour of structural sandwich panel walls
A methodology to assess loadbearing sandwich panels in fire

A Thesis Submitted for the Degree of Doctor of Philosophy

By

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THESIS CONTAINS

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ABSTRACT

The aim of this investigation is to evaluate the fire and racking behaviour of loadbearing lightweight sandwich wall assemblies. Structural sandwich walls are innovative building units, which due to their composite build-up combine low weight, high loadbearing ability and excellent insulation qualities. The wall panels are composite sections of three-layered construction, in which stiff, thin facings are bonded to both sides of a low-density core substrate.

The work has developed a methodology in which fire testing in three-scales is used to model the fire damage and its effect on both fire and structural performance of the structural wall systems. The fire performance evaluation was undertaken using bench, intermediate and full-scale tests, enabling the parametric study into the influence of material and panel composition to wall performance. The methodology links the results of the different fire tests to determine critical failure and behaviour patterns within the building system, also covering different end-use conditions, to model the progressive fire damage in sandwich walls. Based on the findings an analytical model was developed to predict the fire resistance of structural sandwich walls. The performance of sandwich walls was found to be governed by the degradation behaviour of the exposed panel layers and since the smaller scaled testing exhibited the critical design factor to the fire resistance of structural sandwich walls, the need for full-scale testing was reduced. Minor changes to the sandwich wall composition are suggested to be evaluated using the analytical model and/or minimal bench-scale testing, also allowing the assessment of wall performance in a range of fire scenarios. To further enhance the versatility of the methodology an analytical FDM procedure for predicting the heat build-up in the layered wall unit has been assessed. Whilst the technique was found to provide an easy and reliable tool, the lack of detailed material properties in their various degradation stages made meaningful correlation to test results difficult.

The second area of research concentrated on evaluating the racking behaviour of the composite walls and examined the current Code of Practice (BS EN 594 and BS EN 5268: 6.1) for use with structural sandwich wall assemblies. Whilst the testing method was found to be adequate, the modification factors employed to adopt the test results to different wall configurations were only partially applicable. The work examined the principal differences between timber frame and sandwich wall constructions and found the horizontal rail configuration to be the overriding influencing factor in the racking behaviour of sandwich walls. The vertical load performance of the wall was affected in particular and to account for the differences in behaviour, changes to the design Code for use with structural sandwich walls are proposed; further testing is needed to endorse exact modification factors.
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Ignis Aurum Probat

(Value is proven in the fire)
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Chapter 1

Introduction

1.1 Overview
The constantly growing demand for houses in Europe has created a need for affordable and economic building systems and structural sandwich panels have been used increasingly in the last five to ten years to meet this demand. Structural sandwich wall building systems are used in industrial and residential construction of up to three storeys height; the need to improve and validate the fire and racking performance of these composite wall building systems has led to the research described in this thesis. Fire is always a critical design factor in lightweight building systems. In structural sandwich walls this is of particular concern due to the types of materials used in the panels and the fact that these material layers need to remain structurally bonded to maintain the loadbearing ability of the wall. The investigation into the fire performance of structural sandwich walls has led to the development of a fire testing methodology in different scales, which may also have significant relevance to other forms of construction materials. The fire degradation of structural sandwich wall panels has immediate effect on their structural performance and in the second part of the study their overall structural performance and especially their racking resistance is investigated. The racking evaluation of wall systems currently uses the timber frame assessment method, which in the case of structural sandwich walls is not wholly appropriate. This chapter introduces the development of sandwich panels in prefabricated domestic building systems, discusses the panels' typical composition and overviews their typical fire and structural behaviour. The principal objectives of the work and the layout of the thesis are outlined at the end of the chapter.

1.2 Lightweight building systems
Solid construction methods using stone and concrete have for long been the traditional way of building in most European countries. However, through the 1990's, the use of lightweight, loadbearing timber- and cold-formed steel frame wall assemblies has steadily increased and they are now widely accepted and common in both dwelling and industrial/commercial construction for up to four storeys height. The ability to clad the framed building shell with a conventional brick skin or brick slips enables the preservation of the traditional appearance and has been vital to the rapid development of lightweight construction. Composite panel systems are a further form of lightweight construction. They differ from the framed systems in that they do not require studing elements at regular intervals in the wall to bear the building load. This construction method, known in North America under the generic term of structural insulated panels (SIPs), uses a loadbearing composite section of three-layered construction, in which stiff, thin facings are bonded to both sides of a low-density core. The outer boards are the main-load carrying members in the panel and the core serves as a spacer to distance them apart,
providing the construction depth for enhanced bending and buckling resistance. Faces and core have identical functions to flanges and web in an I beam. As the two structural face veneers “sandwich” the core, this form of construction is often referred to as “sandwich panel”. Due to the internal core these panel systems provide excellent heat insulation.

1.2.1 Sandwich building systems
In sandwich panels the face veneers must be bonded to the core. This bond is provided either by the self-adhesion of the core material to the faces or the application of glue at the interfaces. The sheathing materials used in the panels differ depending on the final application of the units; two distinct systems can be differentiated:

(i) panels used as cladding units and non loadbearing partitions and therefore not designed to transmit or sustain significant stresses,
(ii) primary loadbearing structural panels, which transfer dead and live loads to the foundations.

The cladding type panels, mainly used as external wall and roofing units on steel portal frames, have flat or corrugated steel sheet faces and are common throughout Europe in a range of applications, including cold storage and food processing plants. The panels are commonly designed solely to resist wind loads if used as wall panels, and a combination of wind and snow loads when cladding the roof of the structure. In large factories the panels are also employed as internal walls and non-structural ceilings to divide the large, open factory floor area into separate working units. In recent years these types of panels have generated a considerable debate over their reliability in fires.

The sandwich panels investigated in this study are of the second type panels and are employed as loadbearing building members, specifically as walls. In the US, where the panels were introduced in the housing market in the 1950’s, they are also frequently used as floor and roofing units. The panels are primary structural elements and standard building board products of thickness of 8 to 15mm are used as sheathing materials (see drawing 1-1). The US panel systems are predominantly built with wood-based face veneers, in most cases OSB. For the European market the face veneers are often cement or gypsum-based boards and to a lesser extent wood-based boards.

In both types of sandwich panel systems the core materials range from synthetic polymeric foam materials to mineral wool (Drawing 1-1). The former include closed cell Polyurethane (PUR) and Polyisocyanurate (PIR) foam and expanded/ extruded Polystyrenes (EPS and XPS). Polyurethane and its derivatives (such as PIR) are self-adhesive to the faces and allow in-situ production processes whereas Polystyrene, like Mineral Wool has to be fixed to the veneers by adhesives. In the building industry the use
of rigid plastic foams, such as PUR and PIR, has steadily increased and this is primarily due to their superior insulation qualities and manufacturing advantages. Some loadbearing sandwich systems additionally include internal units, placed in the core to enhance the link between veneers and to resist exceptional conditions, such as fire. The overall thickness of the loadbearing panel units range from 80 to 250mm, depending on insulation requirements and vertical loading (i.e. resistance to buckling). In most systems the wall assemblies are constructed from 1.2 wide by 2.4m high units to enable their manual handling on site. The method of connecting the single panels to a wall assembly but also the horizontal rails linking the walls to the next level of construction varies depending on the system used. Board, core, jointing and other internal members are critical to panel design.

**Drawing 1-1: Sandwich panel key components**

### 1.2.2 Advantages of loadbearing sandwich panel building systems

Although loadbearing composite systems are also used in commercial and industrial structures the largest potential area of current interest is in their use in dwelling construction. Lightweight construction in general and sandwich technology in particular offer several advantages to traditional construction methods:

- (i) quality controlled factory prefabrication of the units,
- (ii) time savings, no delay due to weather and generally reduction in on-site work
- (iii) use of less skilled workforce,
- (iv) rapid erection of outer building shell, providing clean, weather independent working conditions for the finishing trades,
- (v) EGAN compliance, i.e. predictable project completion times, productivity and thereby enhanced cash flow,
- (vi) environmental benefits:
protecting natural resources by saving energy through superior thermal insulation provided by the building envelope and reducing generation of site waste,
- automated production of the panels minimises material waste,
- suitability for large developments.

However, the main benefit of the sandwich type construction is the high thermal insulation achieved by the internal core layer. As environmental and energy saving issues have become more and more important over the last decade, insulation requirements for building envelopes have become increasingly stringent. These energy and environmental issues have necessitated the cavity of lightweight timber and steel framed walls to be filled with low density, insulation materials. Sandwich panels already comprise a highly heat insulating core as structural component and wall U-values of 0.12 to 0.2 W/m²K are easily achievable. This combination of loadbearing ability and superior heat insulation properties is likely to make the use of structural sandwich panels increasingly popular with house builders in the near future.

1.3 Structural sandwich panel design issues
1.3.1 Design of structural sandwich walls

In Europe the design of structural sandwich panels is verified through testing since no specific design codes are in place to guide on the theoretical determination of ultimate loadbearing capacity or serviceability of the panels under vertical, horizontal and in-plane racking loading conditions. Their ultimate loadbearing capacity when loaded vertically and horizontally can be determined by composite design principles, taking into account the additional deformations caused by the comparatively shear-weak core used in structural sandwich panels. Such theoretical ultimate loads are generally confirmed through testing and referred to in the technical literature of the specific panel product. Due to the types of materials used and their structural bond, the loadbearing capacity of the panels is high in relation to their weight, allowing considerable building loads to be transferred comfortably. However, the composite nature of the panels leads to a severe design issue in fire where the panels are prone to early damage, which can lead to their premature collapse and poses a considerable threat to the life safety of building occupants.

1.3.2 Fire design

In the event of a fire, which for fire legislation purposes is assumed to occur at least once in the lifetime of a structure, a building element has to perform so as to protect the inhabitants, limit the damage to the property and allow the fighting of the fire without unduly high risk. The building walls play an important role in ensuring the overall fire safe environment and are therefore required to be fire resistant for an established period
of time, dependent on the type of occupancy and the function of the wall (i.e. loadbearing or non-loadbearing). The fire resistance of a building element describes its ability to continue to perform as a barrier and/or a structural component during the course of a fire. Adequate fire resistance helps in restricting the size of the fire, ensures its confinement to compartments and thereby enables the maintenance of safe areas for escape and prevents the collapse of the building.

1.3.2.1 Fire risk in dwellings
The highest fire risk to humans originates from fires in structures, especially dwelling fires. In 1998 there were 70,000 reported fire incidents in residential structures in the United Kingdom (Watson and Gamble, 1999) and, whilst only accounting for approximately 20% of all fires, dwelling fire incidents account for ¾ of all casualties, amounting to 400-600 lives each year. Most casualties are encountered in the early morning hours between 3 and 4am, although accidental fires mainly occur between 4pm and 8pm, peaking around 5pm to 6pm, most likely related to cooking fires. The lack of awareness of the victims during the night contributes to the elevated death rates and is termed sleeping risk in fire legislation. The statistics imply that people are most vulnerable in their own homes.

A statistical survey conducted in the US established that in about 9% of all home fires structural members/ framing elements were the first materials ignited. As such they were the third most frequent item first ignited, after cooking materials and rubbish and the third most frequent cause of death, after furniture and bedding materials (Rohr, 2001). Short circuits in electrical distribution equipment were the fifth leading cause of home fires and home-fire casualties, particularly when thermal or acoustic insulation was the first material ignited. This is of special interest since services in lightweight construction are installed within the wall cavity along the stud units and in sandwich panel building systems they can commonly be found embedded in the core.

1.3.2.2 Fire issues related to structural sandwich panels
The likelihood of fires in residential construction and the high risk to human life originating from these fires necessitate predictable and reliable fire resistance from dwelling walls to maximize the chances of escape and rescue. All sandwich panel building systems have fire safety implications as the interface between face veneers and low density core, forming the loadbearing sandwich compound, is damaged at temperatures ranging from 100-300°C, depending on the materials used. In an average fire this temperature can be reached in minutes. Some panel systems include internal veneer linking units, which hold the panel parts together as the heat damage progresses and prevent the peeling away of veneers, delaying the breakdown of the wall. At the same

1 Surveyed between 1993 and 1997
time the structural internal panel core is most affected by the heat generated in a fire so that the determination of fire behaviour and failure mechanisms are essential to ensure adequate fire resistance performance. Furthermore, the generation of decomposition gases from the synthetic cores when exposed to elevated temperature levels has a potential to amplify the fire risk to the inhabitants of the building as fire deaths mainly result from being overcome by gas or smoke (Watson and Gamble, 1999). The levels of toxic gas and smoke produced by materials and construction systems are as yet not determined in standard test procedures and are not subject to approval by building authorities. However, the increasing awareness of public opinion towards this issue has led to the approval of new regulations (1) and is likely to lead to more stringent regulatory control in the future.

1.3.3 Assessment of fire performance through tests
The fire tests used to assess products, published by national as well as international organizations, are at least in the hundreds (Babrauskas, 1995a). In the United Kingdom alone 35 to 40 different fire tests are currently in effect. These tests can be broadly divided into two categories:

(i) Reaction-to-fire tests
(ii) Resistance-to-fire or fire endurance tests

Reaction-to-fire tests evaluate how a material responds to heating, which generally includes the determination of ignitability, flame spread, heat release and the generation of toxic, obscuring combustion gases (Babrauskas and Peacock, 1992). As such this form of test is focussed on quantifying the fire hazard constituent from a product. Test regimes like the ISO 9705 room corner test (2) or the Cone calorimeter (3) are reaction-to-fire tests and are commonly employed for assessing the fire performance of cladding, non-structural building elements such wall and ceiling linings. The fire performance of structural building elements is required to be determined through a fire resistance test. By contrast resistance-to-fire tests determine the capability of a product/system in preventing fire spread beyond compartment boundaries while maintaining the ability to bear the imposed load. Full-scale fire resistance tests do not assess the interaction of the specimen with the heat source, but the resilience of the construction to the imposed heat regime. Legislators require resistance-to-fire test results for building approval of loadbearing wall elements. The fire resistance of a construction element is traditionally determined by testing a full-scale sample to failure while subjected to a standard fire generated in a furnace. The standard fire is simulated by varying the furnace temperature with time, which is achieved by controlling the rate of fuel supplied to the burners. First records of fire resistance tests date back to the end of the 19th century and the first fire test standard was published under the auspices of ASTM in 1916 (Babrauskas and Williamson, 1980a, 1980b). Since then the temperature-time curve used in fire resistance testing has only slightly changed (Malhotra, 1982). The test regime is intended to be a comprehensive
evaluation of the fire resistance of the entire unit. This, in the case of a wall, will include vertical and horizontal jointing, internal members and other specific wall features such as sacrificial layers of plasterboard. For loadbearing units, a load appropriate to the end use condition is applied to the test specimen. For wall tests, the furnace is oriented vertically with the test wall closing off one side of the burn chamber. Full-scale fire resistance tests, for wall units such as the BS EN 1365 (7), BS 476 Part 21 (4), ASTM E119 (9) and DIN 4102 (10), are proving tests, assessing the overall stability, integrity and insulation performance of the structure when subjected to furnace condition. The test walls are generally 2.4 to 2.7m high and 3m long. In the BS EN 1365 (7) test five temperature measurement devices are distributed over the 8 square metre of test panel to monitor the surface temperature of the wall and thereby establish the compliance with the insulation criteria. Deflection gauges are positioned on the unexposed face of the wall to monitor the stability of the specimen and the displacements needs to remain below $Wallheight/300$ to pass the test criterion. The output of the test is a time rating, which specifies the time period for which the fire spread criteria, i.e. insulation and integrity, and the loadbearing function of the unit was maintained. Although test performance has been shown to be variable and studies have proven that the heat transfer characteristics of a furnace, i.e. the physical properties of the furnace walls, influence the severity of the exposure (Malhotra, 1982), only one successful test is required by building authorities to prove adequate compliance with building regulations. The poor repeatability of full-scale fire resistance test performance and the comprehensiveness of the wall construction, which includes all wall joints and plasterboard cladding, make this assessment method unsuited for establishing the fire reactions of the layered composite structural sandwich walls. Therefore a testing methodology needed to be developed to enable a parametric evaluation into the panels’ fire behaviour. The methodology uses test data generated from fire testing in three scales, including the reaction-to-fire bench-scale Cone calorimeter test (3) and two scales of fire resistance tests.

1.3.4 Structural Design

Structural sandwich panels can be used in a variety of applications within a building, ranging from external and internal walls to floors and roofs. The scope of the thesis is restricted to their use as wall units. In building walls the panels are vertically loaded by gravitational loads through roof and floors and once the load distribution has been determined sandwich walls, in absence of specific design codes, can be designed in accordance with BS 5268 Part 2 (11) or Eurocode 5 (12). Horizontal loads mainly constitute wind loading in the U.K. but in other countries can also include earthquake loads. In principle wind and seismic loadings are different; whilst wind loading is dependent on the exposed surface area and the wind velocity, seismic loading is related to the acceleration imposed by the shaking motion and the mass of the structure. However, for analysis purposes their effects are similar as both are fast acting, variable in amount.
and evoke similar responses from the wall. Horizontal loads act on building walls in two ways:

(i) directly, where wind pressure/suction acts onto the face of the building shell, which causes the walls to bend between top and bottom supports,
(ii) indirectly, where the walls are being loaded from horizontal diaphragms and transferring the load down the building to the foundation. Here the walls act as shear walls and are deformed in their plane. The walls may also act as vertical cantilevers depending on the orientation of the load transfer.

External walls may need to be designed for both direct and indirect horizontal loading cases in addition to the vertical loading. The shear resistance of the wall to lateral forces, i.e. the indirect loading through the wind or earthquake loads causing in plane deformation, is known as the racking resistance and is designed using BS 5268, Part 6 (13). Whilst the direct horizontal and vertical loads can be easily designed for by sizing of units, the racking resistance of walls is far more difficult to enhance and structural engineers often considers the racking performance as governing design factor.

1.3.4.1 Loadbearing wall panel design issues
Generally the structural performance of the composite panels when subjected to vertical and direct horizontal loads exceeds the imposed stresses expected in dwelling and commercial and industrial type construction. Their loadbearing behaviour is different to the related timber frame, where the internal studs, hence the frame of the walls, are the main loadbearing components accommodating compression and bending. In sandwich walls there are normally no internal studding units, or additional shear bracing, and their loadbearing ability relies on the composite nature of the section provided through the bond between core and board faces. The elastic central core stiffens the outer slender faces through the strong glue bond so that the loadbearing reserves in the compression strong boards can be activated and major loads can be transferred comfortably.

The UK test method and evaluation procedures for wall racking have been developed from work on timber frame walls, where lateral loads are commonly resisted by the structural sheathing fastened to the internal wood frame and in some cases also through additional bracing. Although sandwich walls have a different loadbearing behaviour to timber frame walls in compression and bending, their racking behaviour is similar, since both types of wall rely on the strength provided in the cladding boards and their fixings to the base structure. With respect to racking performance, sandwich walls are a derivative of timber frame construction. Although the use loadbearing sandwich walls steadily increases, this form of construction represents too small a population to justify a customized design method. Their racking design relies on the timber frame design procedures as outlined in BS EN 594 (15) and BS 5268 Section 6.1 (13) and more recently BS 5268 Section 6.2 (14) which widens the use to commercial and industrial
situations. However, the principal differences in wall composition between the studded timber frame walls and sandwich panels are likely to lead to inaccuracies, if not overestimation of sandwich wall racking performance. This is especially likely in the design of ground floor wall panels, where the large percentage of openings in the wall weakens the resistance to in-plane loads.

1.3.4.2 Assessment of racking resistance
The racking performance of wall assemblies is determined from tests on a standard panel format, normally 2.4m long by 2.4m high. The test regime, BS EN 594 (15), evaluates the ability of a wall unit to perform as sidewalls in a building and panel performance is dependent on panel construction. Therefore the composition of the wall panel, i.e. type of sheathing board, its thickness and fixing (type, size and spacing), should include all the important materials' features to allow for a single design value to be deduced from the test. The test procedure enables stiffness tests to be carried out at different vertical load conditions, i.e. at 0 and 5kN/stud. The design figure will guarantee the appropriate factor of safety necessary for the form of construction and will also limit the in-plane deflection to 0.003 of the wall height. The racking performance of walls is increasingly important in earthquake and tornado areas where ultimate wall failure is likely to occur. In the UK wall failures are generally design related due to the conservative design approach adopted. For structural sandwich walls this conservative design approach is possibly even more at error due to the marked differences in wall construction to traditional timber frame.

1.4 Objectives of investigation
Generally the design of structural sandwich wall panels must show the satisfactory structural performance of the units for

(i) accidental fire,
(ii) vertical load,
(iii) bending,
(iv) racking.

The provision of adequate fire resistance is likely to be the governing factor to panel design. Out of the three fire resistance requirements, insulation, integrity and stability, the stability of the sandwich unit throughout the fire exposure is likely to be the critical factor. Fire exposure represents an ultimate limit state and is generally localised in one part of the building. The localised fire damage has greatest impact on the vertical load performance of the wall unit and the prevention of a structural breakdown for the required fire resistance period will be the overriding issue. Generally horizontal loading is less critical due to the light wind loading of the walls in bending and the possibility of redundant horizontal load paths within the building. In that respect the loss of damaged wall section can be compensated by proportionally increasing the contribution of the
remaining intact wall sections. Therefore the superimposition of racking and fire design of the walls is not necessary.

The fire and racking resistance are the two main areas, which rely on empirical rather than theoretical assessment and therefore form the focus for this investigation. The likelihood of accidental fires to cause the catastrophic failure of the sandwich panelled building shell concentrates the objectives of the fire study to the following basic design concerns:

(i) examine and establish the fire behaviour of sandwich walls,
(ii) conduct a parametric study to determine the influence of a wide range of panel parameters on the fire resistance of structural sandwich walls, including the effect of veneer and core materials, the presence of internal studding and the effect of plasterboard cladding,
(iii) develop a test scheme/methodology to enable the parametric study; the final application of the methodology developed through this work could be used for a wider range of building systems and fire exposure conditions, reducing the amount of expensive full-scale testing, establishing the true adequacy of building systems and facilitating the development of novel building products,
(iv) evaluate and quantify the effect of panel degradation on the loadbearing capacity and fire resistance of the composite wall structure, including the assessment of vertical load performance at varying stages of destruction, complemented by bending tests, which also assess the effect of internal studding,
(v) investigate the composite action between panel members and evaluate impact of loss of connectivity on overall loadbearing performance of structural sandwich walls of varying configurations,
(vi) develop and guide on the use of the scaled testing methodology and customize analytical methods for predicting the response of sandwich walls in fire.

Since the fire design of the wall structures was seen to be independent of the racking design, the investigation into the racking performance of the sandwich wall units was conducted separately from the fire programme with its own set of objectives. Although both programmes were autonomous, the fire design of the wall units interlinks with the racking design through the choice of preferred principal wall features. Whilst the outcome of the fire investigation was likely to govern the principal wall features the objectives of the racking programme were to:

(i) examine and appraise the effect of methods of construction and assembly of sandwich walls on racking resistance.
Since structural sandwich walls are a derivative of timber frame wall construction they are designed and assessed based on the same regulatory framework. Therefore the objectives of the racking programme were enlarged to

(ii) assess and discuss the current Code of Practice for racking design for use with structural sandwich wall assemblies,

(iii) identify differences in timber frame and sandwich wall behaviour and guide on the design areas required for adoption.

1.5 Outline of Thesis

Following on from this introduction Chapter 2 presents and discusses the general design and construction issues of loadbearing sandwich wall assemblies. The section overviews the most commonly used connections and installations in sandwich wall construction and concludes with an overview of the characteristic panels features of the three systems investigated in this study. The main test programmes undertaken on each panel system are presented and explained. Chapter 3 then reviews the literature relevant to the investigation. The overview starts with the fire and structural research in the area of sandwich panel construction. Since the project aims to develop a testing methodology to enable the parametric investigation into the fire behaviour of sandwiched loadbearing walls, the review presents work undertaken in the field of scaled testing and extrapolation of fire behaviour. This covers both the behaviour of materials but also the fire resistance behaviour of entire lightweight wall building systems. The fire and structural testing methods selected for this investigation are described in Chapter 4. The methods used for the fire investigation are overviewed and discussed in greater detail to present the philosophy behind the scaled testing regime. Chapter 5 presents the results of the different fire related test programmes undertaken in the scope of the study. The results are reported in chronological order, starting with the intermediate-scale fire test regime, the bench-scale test results and concluding with the full-scale fire work. The full-scale work undertaken in the scope the study has been supplemented by findings of other fire investigations on structural sandwich walls from a range of sources. The outcome of the testing regimes has been put in context to the here presented findings and a discussion highlights the similarities and general learning points to be extracted from this work. The fire test results and the structural testing at ambient temperature supporting the fire investigation are reported independently. The findings examining the fire behaviour of the walls are combined and analysed in conjunction in Chapter 6. Chapter 6 compiles all the findings of the fire investigation to explain the effect of the single panel components on performance and to guide on the effect of failures on the overall loadbearing capacity and fire resistance of structural sandwich walls. A final section of the chapter presents the implications and applicability of the developed testing methodology and guides on areas meriting further research. Since the racking programme was autonomous, Chapter 7 independently compiles and discusses the findings of the racking programme.
chapter reviews the behaviour of timber frame walls to in plane loading to prepare for the detailed analysis of the racking behaviour of structural sandwich walls. Thereby the basis for the Code approach is considered and the changes and alterations required for structural sandwich walls are discussed. Chapter 8 concludes the findings and summarises the implications of the work and the learning points as well as give an outlook on future work.
Chapter 2

Sandwich Panel Construction

2.1 Introduction
This chapter details sandwich wall construction and introduces the factors influencing the design of the composite units, ranging from their ease of manufacture to the robustness of the system. It describes the three main systems covered by the testing work and examines their connectivity in covering all horizontal and vertical joints. The sequence of construction, the internal and external finishes, the provision of services and the surface finish of the wall are detailed to give a general understanding of sandwich construction. The standard solutions for wall-floor connections and wall details such as the construction of corners, the manufacture of openings and fixing methods are presented. Within the industry various board and core thicknesses and a wide range of corner details, vertical and horizontal jointing methods are used; the construction of floor supports are also diversified. The design of wall features is generally dependent on the wall manufacturer and construction solutions are rapidly increasing and vary with core depth and veneer thickness. In this chapter no attempt has been made to cover the entire range of construction details and specific dimensions used in connection with sandwich panel construction. The construction details overviewed here are typical for the panel systems investigated in this study and are in principle similar to the various solutions currently found in the industry. Although the implications of certain construction solutions have been discussed it has not been attempted to give an encompassing appraisal of structural sandwich wall construction.

2.2 Sandwich Panel construction
2.2.1 Panel/ System components
2.2.1.1 Erecting buildings with structural sandwich walls
The structural sandwich panel building systems investigated in this study are predominantly used in domestic construction of up to 3 storeys height, although their structural capacities would enable their use in higher rise buildings. The single panels, normally 1.2m wide by 2.4 to 2.7m high, are manufactured in a factory and delivered to site for assembly. The prefabrication of the units minimises delays on site and rapid erection times are commonplace. Foundations provided, a trained crew can erect the main structural components, i.e. walls, beams and roof, of a 150m² floor area house weather tight in as little as 2-3 days for the internal finishing trades. Due to the straightforwardness of the panel system an untrained workforce would also be able to handle the systems with ease and confidence. The panels' low weight enables the handling and assembly of the system without the need for craning. Individual units can be manhandled and carried by two to three workers. In a minority of projects entire wall
sections of lengths larger than 1.2m are delivered to site ready for assembly with a crane, further accelerating the erection speed. However, at present this is not the common assembly method for these types of panels but might become increasingly popular in future.

2.2.1.2 Assembling walls
Each panel consists of two face veneers either side of a low-density core. Materials used as boards and cores are wide ranging and have been overviewed in Chapter 1. In Europe mineral based structural boards and synthetic, self-adhesive foam cores are most commonly used. The choice of materials affects on various panel design issues and this is discussed in further detail in the following sections. Some panels include additional internal, veneer linking sections, but this is the exception rather than the rule. The internal steel studs are generally used in foamed panels and are perforated to allow the free expansion of the foams during manufacture. The internal units are not designed to contribute to the panels’ loadbearing capacity and only inserted to stabilise the veneer layers in the case of fire.

Figure 2-1 shows a typical sandwich wall assembly. The single panel units are connected along their vertical edges and at the top and bottom. The sequence for construction is first to place the leading panel on the bottom rail, which will previously have been fixed to the floor. The trailing panel is then positioned and fixed to the bottom rail and to the leading panel using the respective vertical jointing mechanism. Finally the top rail is positioned and fixed linking the panels. Screw fixings or other connection devices are inserted as customary for the respective systems. Continuity in the wall assembly is of paramount importance for the structural performance of the system but also with respect to its airtightness and insulation performance. The vertical and horizontal jointing is therefore designed to enable the tight alignment of the units and allows for precise positioning of panels at the intersection with the floors. The various joints are often additionally sealed with expanding sealant to minimise air infiltration. In most wall constructions additional internal layers of sacrificial material, commonly plasterboard, are connected to the panel surface through battens or plaster dabs, as shown in figure 2-2. The thickness of the sacrificial layer depends on the fire resistance/ acoustic requirements. The additional layer of material simplifies the surface finish, the supply of services and the uniformity of the wall. In most cases the cavity formed between the wall panel and the sacrificial lining is used to chase/ install services, pipes and cables. There are uses, especially in dwelling construction, where no internal finishes are required and in this case services are laid within the panel units. Here electrical services are habitually chased through the panels’ core and fed through the veneers at regular intervals and predetermined height, often adapted to customer wishes during the planning stage. Pipes, drains and other supplies requiring wider diameter holes are cut out of the panel when running horizontally or have to be encased in architraves when running along the height of the panels. The surface of the plain sandwich walls, without additional plasterboard cladding, is generally good and provides a smooth working surface for the finishing trades. Normally screw and nail
heads are spotted and joints filled with joint compound, similar to the treatment of plasterboard surfaces in other lightweight wall constructions.

Whilst the internal finishes are limited to plasterboard or similar lining products, the choice of external finishes is extensive and ranges from traditional brick skins, as shown in figure 2-2, fastened to the panel by brick ties, to wooden panelling which can be mounted onto battens. The most widely used external finish is the single skin brick wall, linked to the sandwich panel wall by standard brick ties, which are screwed onto the panel faces at required spacing. Before any finishes or cladding are applied a weatherproof membrane is always installed to protect the external face of the panels. The cavity is maintained between both walls and should be ventilated and BS 5628 Part 1 and 3 should be complied with (Grantham and Enjily, 2003). The differential movement encountered in sandwich panel construction is likely to be less than in timber frame due to the low moisture content of the panel materials and the minimised compression deformation of the face layers. Exterior finishes to the sandwich panel wall are not considered contributory to structural performance.

In domestic construction single plain panels are delivered to site for assembly and openings for windows and doors are normally cut out of the panels where required. For larger openings special lintel, load-transferring sections are provided. The cutting of the panels is accommodated with a standard circular saw, emissions and waste are minimal and the prefabricated lintel systems and window/door inserts also contribute to the remarkable erection speed. There are no restrictions to where an opening can be placed. For larger building projects the panels can be pre-cut and assembled without the need for additional cutting, which further accelerates the construction speed.

2.2.2 Design issues related to requirements for wall units

2.2.2.1 Manufacture

Prefabricated building units, such as structural sandwich walls, are preferably manufactured in a fully automated production process since the speed of panel production and the possibility of stringent quality control are major advantages for the building industry. In structural sandwich wall manufacture a fully automated production is facilitated with self-adhesive foams. With the use of these foams the connection between face layers and core is rapid and reliable and does not require additional gluing. For the foams used in the structural sandwich walls the polymerisation process must be rapid, easily controllable and the curing periods should be as short as possible. The core substrate must be chosen so as to self-adhere to a wide range of non-treated surfaces at practical temperatures since only with reliable core adhesion can a truly composite section be formed. This is especially important if additional internal units are present in the panel. In order to ensure the quick production process the panels should be injected with foam in one course, which also implies that any internal units must allow the free expansion of the core mixture (see internal studding in panel system 1, figure 2-10). If the internal units are solid, non-permeable, the foam will have to be injected in separate sections, which is time consuming. Core substrates, which are not self-adhesive, such as
mineral wool, must be glued to the board faces in a separate production step. In those panels any internal members disrupt the composite section.

2.2.2.2 Structural
As wall units the panels are principal loadbearing members transmitting vertical and horizontal loads to the foundations. In a structural sandwich wall the board faces are the main load carrying members, which are generally strong in compression but, due to their slenderness, weak in buckling. The superior vertical load performance of sandwich walls relies on the centrally placed elastic core, which continuously supports the face veneers and thereby prevents their separate, premature buckling, increasing the overall loadbearing ability of the wall. The adequate connection between the faces and the core is vital and the thicker the sandwich wall unit the better its global buckling and consequently also its bending capacity. To prevent the sliding of the veneers under load the core must have adequate shear stiffness. The compound of high density faces and low density, shear flexible core causes additional shear related deformations, as generally encountered in composite building units. The shear soft sandwich compound also influences the long-term performance of the panels through its creep behaviour. This can cause large deflections, which are also greatly influenced by temperature and moisture levels. Ageing of the core is likely to influence the overall structural performance, especially when the flexibility and ductility of the core substrate and its connectivity with the facing layers diminishes. Although these issues are of importance, their impact on the overall loadbearing ability can be estimated and designed for (ECCS, 1998 (16)).

With respect to racking performance, the composite nature of the panel is not of paramount importance although the core prevents the faces from deforming separately out of the plane of the wall. Rather more important to the racking resistance of a sandwich wall assembly are the horizontal and vertical connections. Since the panels do not normally include internal framing, the lateral resistance of the wall unit relies solely on the connections provided along the bottom rail and to some extent at the vertical joint to minimise uplift movement in the front of the panel. The provision of adequate connectivity between the single panel units in making up wall elements is generally essential to the structural functioning of the system but is also critical in order to prevent disproportional collapse, a failure pattern especially common in prefabricated building units and renowned from the Ronan Point Disaster in 1968.

2.2.2.3 Fire resistance
Structural sandwich walls are vulnerable in fire situations as the exposure to the fire environment degrades the panels by softening the essential glue compound between faces and core. The transition of the core/ glue-line towards a soft and rubbery state occurs at relatively low temperatures and can cause the premature failure of the sandwich wall. In order to provide adequate levels of fire resistance the design and final installation of the unit within the building is of great importance. In most systems the decomposition of the sandwich compound is postponed by reducing the heat build-up within the panels through sacrificial layers of plasterboard, a commonly adopted solution in all lightweight building
systems. Another concern with respect to the fire performance of sandwich walls is the fact that the internal combustible core promotes hidden fire spread compromising the compartmentalisation of the fire. Due to the layered panel composition there is a possibility for the core decomposition process to progress behind the veneers to areas remote from the direct fire exposure, i.e. to other storeys or adjacent rooms. Since this fire damage is contained behind the panel faces it is hidden from direct view and the extent of its spread cannot be assessed. This form of hidden panel destruction can reduce the time available for safe escape of occupants and puts the life of fire fighters at risk. The provision of adequate fire stops between panels/room units can eliminate the hidden spread of fire destruction and ensure the compartmentalisation of the fire.

2.2.2.4 Insulation
One major advantage of sandwich panel building systems is their superior insulation performance. In contrast to timber or cold-formed steel frame walls, where the insulation needs to be fitted separately, sandwich walls already incorporate a low-density, insulating core substrate as part of their build-up. This economic use of the core material as both structural and insulating component within the building unit is often regarded as the biggest advantage of sandwich walls. To ensure this superior insulation ability the joints between single panel units and interconnections of panel with floor need to be carefully designed to avoid cold bridging. However, the light weight of the panel and its good thermal insulation mean that its sound insulation properties will be less good. As in other lightweight wall structures the acoustic performance requirements are provided through additional, heavy cladding. This need for additional cladding reduces the economy of the system but provides superior quality building walls for a wide range of applications.

2.2.2.5 Robustness
Of paramount importance in any prefabricated building systems is its robustness in
(i) transport,
(ii) handling/erection and
(iii) final use.

The structural sandwich wall systems are generally robust, since the exposed facing board materials used in the units are impact and weather resistant and due to the foam backing stiff and strong. The panels are normally transported to site in packs of up to 8 on tightly packed wood palettes, wrapped in polythene. This transport method is commonly used within the industry and the panels are rarely damaged upon delivery. The polythene wrapping has an additional advantage in that it protects the panels from the weather if they are stored remote from shelter. During the erection and positioning of the panels damage can occur, especially when the internal core is recessed to include internal horizontal and vertical jointing. The first cause of damage can be the protruding, unsupported board edges, which are prone to impact and tend to break off, especially when brittle materials are used as face veneers. This type of damage has the potential to reduce the racking performance of the wall assembly. A second cause of damage can be over tight tolerances in the dimensioning of the recesses, causing the cracking, breaking.
and sometimes delamination of boards along its length/height upon insertion of jointing section. This has potential to markedly reduce both the racking and fire resistance of the wall unit as the breakage destroys the continuity within the wall and exposes the vulnerable jointing section between panels enables to direct access of heat. Both types of damage are difficult if not impossible to repair and broken panels should be replaced since the continuity in the facing board is irreversibly damaged its function as load transferring and shielding connection between panel and next level of construction is weakened. Damage occurring to the final wall, after the panels have been installed is equally difficult to repair especially when damage occurs within the central part of the panel. Any damage to the facing veneers should be assessed carefully even if sacrificial lining is hiding the panel surface in the final wall assembly. Sandwich wall manufacturers provide repair kits, allowing for cosmetic remedial action to be undertaken. However, large damage (greater than about 30%) is likely to impact on the panels' loadbearing behaviour and cannot be restored so that the damaged panel should be replaced.

2.2.2.6 Durability
Knowledge of the long-term performance of sandwich wall systems is limited due to their fairly recent introduction into the European market. Apart from private research undertaken by some of the panel manufacturers durability of the panels in the long-term has not been assessed in detail and needs to be studied with ongoing usage of the system. However the weather tight, high insulated building envelope and low tolerance construction technique is bound to positively impact on the overall ageing of the panels. Due to durability concerns the use of glued in connections, common in some of the US systems, is penalized by large safety factors whilst the long-term performance of the glue is assessed. As in any composite building unit the ageing of the vital connection between the various components can lead to enhanced creep deformations, which can impact on the serviceability of the building system and its loadbearing ability.

2.3 Assembly of building
2.3.1 Jointing of Floor/ Foundation and Sandwich Wall Unit base
Structures built with sandwich panels are normally erected in platform construction, where storey height wall segments support floor elements to create a platform on which the subsequent storey is erected. The panels are assembled into the building shell by connecting the room high wall units to the floor system using top and bottom rails, which are also used to link the single panels at their top and bottom horizontal edges. At the base of the panels the rails are connected to the floors/foundation by bolts. Soleplate straps are also used to secure the connection between the bottom rail and the foundation. The foundation or foundation strips have to be accurate, level and square, especially at corners. The nature of the construction means that the top level of the foundation should not vary by more than ±5mm in 3m along the wall and the diagonal measurements should be within ±5 to 10mm. If the foundation is not level the bottom rail connection can either be shimmed or grouted to adjust any irregularities. All sandwich wall systems have relatively similar horizontal connection methods. The horizontal connection mechanisms used in this study are shown in figures 2-3. In systems 3 and 2, figures 2-3
(a) and (b), the horizontal links are hidden inside the panel behind the veneers, with the connector sections recessed into the core. In such systems additional internal lining is only needed for fire resistance or acoustic performance as the smooth external panel face enables finishes to be applied directly. Although the internal link is beneficial as it might eliminate the need for sacrificial lining, the recesses formed at top and bottom of the panels leave the board vulnerable to damage since it is unsupported and prone to breakage, especially in brittle face boards. In that respect the channel section, capping the panel ends (Figure 2-3 (c)) is advantageous in manufacture as it is external and no recesses need to be provided within the panels, improving the robustness of the system. Additional plasterboard lining is essential in this form of visible connection to hide the joint and providing a smooth surface for the finishing trades.

In the systems investigated in this study the internal horizontal rail sections are normally 2.4m long to simplify their insertion into the panel (see also figure 2-1). The warping of jointing sections is a particular problem when wood rails are used and minor inaccuracies in the rail or panel can lead to a breach of tolerance, which can make the installation process extremely difficult. The horizontal rails need to be positioned with respect to the vertical panel joint (as shown in figure 2-1) to maintain continuity in the wall. This is especially important for the racking resistance of the wall assemblies as will be discussed in further detail in Chapter 7.

2.3.2 Connection between panels
Manufacturers have varying methods for connecting the panels together along their vertical edges to form a wall unit. Although it would be beneficial to pre-assemble entire wall units prior to their dispatch and thereby reduce the operations required for erection, a wall of panel units is usually assembled on site of 1.2m wide by 2.4m (sometimes 2.7m) high panels connected along their vertical junction. The vertical joint between panels varies depending on the manufacturer and together with the horizontal rails represents the "fingerprint" of the system. The method of vertical jointing can be subdivided into two main categories:

(i) contiguous fit along the height of the panel,
(ii) intermittent jointing connections placed at set distance along the panel's vertical edge.

The contiguous joint can be made from many materials and shapes, here typical vertical connectors are presented which represent the most common solutions in the marketplace. Figure 2-4 (a) shows one of the most commonly used systems in the US where OSB tongues are placed into recesses behind the veneers. The vertical inserts are either fixed by screws or glued. These thin sections are often preferred to avoid cold bridging at the vulnerable intersections of panels. For improved insulation and airtightness many systems inject sealant along the cored vertical edge in the centre of the panel, which is applied supplementary to the wood inserts behind the veneers. In US systems glue is applied to both the vertical connectors and horizontal rails and screws or nails are sometimes inserted in addition to ensure the tight fit at the joints, but are only secondary fixings. The
different components do not require special treatment prior to the application of the glue. Wood based glues are set off with moisture and therefore the different joints are sprayed with water upon assembly. The glue is deemed to have fully cured and reached its full capacity after a maximum of 24 hours. A similar type of wall panels is also used in the UK but here all vertical and horizontal joints are fixed using screws and glue and sealant is only applied to the jointing areas to ensure airtightness.

Another form of contiguous fit is shown in figure 2-4 (b-1) here a wood stud of identical depth as the core is inserted into a recess along the height of the panel and fixed commonly by nails or glue. The dimensions of the wood insert can also be enlarged, so that only one wood stud is needed for each panel connection (Figure 2-4 (b-2)). In one of the systems investigated in this study, namely System 2, new, more innovative contiguous assembling solutions have been used, shown in figure 2-4 (c) and 2-4 (d). Joint 1 (2-4 (c)) is of GRP material, forming a hollow section, which fits into recesses cast in the panel sides. Similar to the wooden units used in the conventional SIP systems. The second joint is a tongue and groove joint (2-4 (d)), which employs a cold-formed steel section, foamed in during manufacture, mating into a female mould in the adjoining panel. In some specimens, the female section formed a permanent shutter so that upon assembly the joint is two-leafed (Figure 2-4 (d-2)). Screws fix both joints. In case of the GRP joints wide ringed screws need to be used to prevent unnecessary carving out of material which reduces the effectiveness of the connection due to the widening of the screw hole.

The hook system, shown in figure 2-5 adopted in System 1, is a commonly used intermittent vertical joint. In this system a maximum of four jointing units per panel height are positioned along the vertical edges of the panel. The core substrate along the edges is also profiled to enable an airtight fit upon assembly. The hook mechanism draws the panels together and the profile aligns both panel halves tightly and accurately. There are no extra screws or fixings required along the vertical edge.

2.3.3 Corners

As with the horizontal rails and vertical joints the construction of external and internal corners differs between systems. The different corner details are summarised in figure 2-6. The differences in construction are not major and in general external corners are built up either by abutting two panels with wood studs at the ends, fixing them with special long ring shank nails, through the solid wood section, or by removing the depth of the panel from the enclosing panel, but leaving the external veneers capping the internal cores of each panel. The second system requires a corner strap, which firmly tightens the panel along the external and internal edges of the corner.

The connection of internal partition walls to the outer wall shell is mainly dependent on the vertical jointing system. In system where the vertical junction is formed by contiguous edge splines the internal walls are connected to the external wall shell in a T connection, inserting a fixing through the external wall into the wooden edge stud of the
internal wall. In systems where no solid edge stud is available at the end of the internal wall the internal section is tightly abutted to the external wall and connected to the outer walls by corner straps.

2.3.4 Connection of flooring with wall units

Structural sandwich wall systems are used in conjunction with both heavyweight concrete floors and lightweight timber floor joists or other composite floor systems. In sandwich wall buildings the choice of floor system is dependent on the building size and use. Although timber joists are the traditional flooring used with the lightweight structural wall panels, the use of concrete floors is becoming increasingly common in sandwich panel housing.

The floor system determines the design of the support at the head of the panel, which is also dependent on the client’s space requirements and the final use of the building. Two types of construction can be distinguished:

(i) the floor rests on the wall section,
(ii) the floor cantilevers off the wall section (eccentrically loading the wall).

In private residential construction timber floor joists are common and often rested into pockets in the panel, formed by cutting out the internal veneer and core leaving the external veneer as outer demarcation (Figure 2-7 (a)). The joists are rested on scabs and restrained from torsional buckling by scabs at each side. To avoid cold bridging, which would compromise the panel’s superior insulation performance, the joist is slightly staggered from the back of the external veneer and the formed gap is then filled with additional insulation material. The number of joists and respective cut outs in the panels is determined by the structural design, hence the loading of the floor. The load transfer in this type of floor erection is critical since the floor joist partly rests in the internal soft-core substrate and structural testing is advised. Wall systems, which use wood based board materials and wooden horizontal rails and vertical jointing studs are mostly used in combination with wooden floor joists and these can also be hung from the panel by joist hangers (see figure 2-7 (b-1)). Commonly the joist hangers are rested on the internal panel veneer and fixed by screws along the internal veneer. The joists are then rested into the hanger so that no additional beam filling against cold bridging is necessary. However, as the floors are not rested on the panel but hung from them the wall panel is eccentrically loaded and the actual room height is reduced. Floor decking is applied as usual and the next construction level is built starting with the storey high sandwich walls. In addition to the eccentrical loading of the wall a further structural design issue in this type of floor erection relates to the joist hanger, which in most systems only connects to the internal panel face.

Some structural sandwich panel systems are commonly used with heavyweight concrete floors. In those systems joist hangers are much more common and the joist flange embraces the entire panel so that the seam clamps onto the external veneer of the units (Figure 2-7 (b-2)). The floor deck can also be supported onto the panel resting on the
entire depth of the wall panels. In this configuration cold bridging is most severe and this problem is overcome by applying additional insulation to the external face of the slab which is then finished by weather resistant particle board, as shown in figure 2-7 (c). Floor decking is applied as in traditional construction.

2.4 Opening in Wall Units

Every wall system provides customised solutions for the inclusion of openings. An opening can be created in two ways:

(i) Cutting the opening out of the wall, see figure 2-8 (a),
(ii) Placing an opening between two full height/ full length walls and filling the space above the door or window opening and below the window using cut sections, see figure 2-8 (b),
(iii) Cutting out small width panels as shown in figure 2-8 (c).

In the US systems openings are generally cut out of the walls and therefore always require a framing trimmer as shown in figures 2-9 (a). The second solution does not require a trimmer but a specifically designed lintel section is inserted above the opening as seen in figure 2-9 (b). The lintel spans the opening and at the same time closes off the cavity above and below the window out to the external skin, which reduces the amount of work steps required before the window frame is put in. In this form of construction there are no restrictions to where an opening can be located.

The inclusion of openings impact on all other design issues but predominantly on the structural and fire behaviour. Large openings reduce the vertical load and bending capacity of the wall and considerably reduce the racking resistance of the wall section. This is of paramount importance in external ground floor walls where there will be window as well as door openings. The racking performance of the perforated wall unit is also affected by the shape of wall panels around the opening as will be discussed in greater detail in Chapter 4. The inclusion of openings with method (i) gives better performance figures. In addition to the effect on the loadbearing ability of the panel a wall opening also negatively impacts on the fire resistance of a unit. An opening always represents a weakness as it potentially allows the direct access of heat to the internal core material. Since the opening destroys the continuity of the face veneers heat can short circuit into the panel if the opening is not carefully sealed.

2.5 Panel Systems investigated in study

Three panel system, have been investigated in this study. Here they are described with respect to their board and core material, horizontal and vertical jointing and any other characteristic unique to the system.

2.5.1 System 1 (Panablok system from Marshalls Ltd.)
The Panablok panel consist of 8 mm thick cement bonded particle boards (Pyrok) on a 70mm self-adhesive PUR core. The single panels include internal cold-formed steel studs.
at 600mm centres, i.e. 2 internal units per panel. The cold-formed section are folded to I sections as detailed in figure 2-10 (a-2). In 2000 the shape of the internal studs in the panel system was altered to a Σ shape (2-10 (a-1)). The internal stud units were included in the panel to stabilise the panel veneers in the fire case and are not designed to contribute to the panels' structural performance. The horizontal top and bottom rails are formed from cold rolled galvanised steel channels of 1.15mm thickness, as shown in figure 2-10(b), in which the panels are slid upon assembly. The base of the u-channel sections are connected to the substructure through central, pre-cut 7mm diameter holes through the base of the section by self tapping bolts. Additionally the channels have predrilled holes at 200mm length starting 100mm from each end, 64mm from the base, by which the channel is fixed to the panels with EJOT self-drilling tapping screws (see figure 2-10(c)). The vertical joint is intermittent and takes the form of hook closure mechanism described earlier (Figure 2-5). With three units on each vertical edge the panels are drawn together to form a structurally stable, airtight connection and the bevel of the tongue ensures a relatively smooth surface transition at the joint on both faces. The hook is manufactured from 3mm mild steel built into a high impact styrene casing. The single panel units are 1.2m wide and 2.4 or 2.7m high. The system was tested for racking resistance under varying vertical load, see also table 2-1. Test schemes also included vertical and horizontal load tests in full- and reduced scale. Standard full-scale fire tests to BS 476: 21 (4) under varying vertical load and plasterboard protection were undertaken on the system. The study also assessed the fire behaviour of the panels in intermediate- and bench-scale. The panels exhibited good fire resistance with loads of up to 60kN/m. The plasterboard thickness and type was adjusted and 30 and 60 minutes fire resistance requirements were met.

2.5.2 System 2 (Experimental prototype system)
In the experimental system, the panels consist of 10mm thick gypsum bonded particle boards (Sasmox) on a 70mm PUR/PIR core. In the fire test panels different core materials were used and the units were reduced in size; the various test panel configurations are described in Chapter 4: Methods and Materials. The fire test panels include internal cold-formed Σ-shaped steel studs at 1200mm centres, i.e. 1 internal unit per panel (see also figure 2-11 (b)). The panels used in the racking tests did not include any additional internal units apart from the vertical jointing along the panels' vertical edge. The horizontal top and bottom rails are formed from GRP sections in hat form, placed into recesses internal to the wall, i.e. behind the veneers (Figure 2-11 (a)). The panel recesses were cast slightly deeper than required to allow proper sectioning of the rails but they leave a considerable overhang of 90mm Sasmox exposed to damage prior to assembly of the panels. The base of the horizontal sections are connected to the substructure through central, pre-cut 7mm diameter holes through the base of the section by self tapping bolts with the bolt head recessed into the GRP section. The board of the panel was connected to GRP bottom and top section by screws at 200mm starting 100mm from each end, 70mm from the base (base connection as shown for cold-formed u-channel in figure 2-10(b)). The vertical joint for the full-scale fire and racking test is a contiguous cold-formed steel section, described earlier (Figure 2-4 (d)). For the full-scale fire test the female part of the
steel joint remained in the panel, forming a double-leafed joint with increased stiffness (Figure 2-4 (d-2)). In the racking tests the female section was cast too deep as the whole ‘C’ section had been used as the mould. The male section could not be manufactured to give an adequate protrusion and the veneers had to be cut back prior testing. Full details of the various joints are shown in figure 2-11(b). The intermediate-scale fire testing programme also experimented with the system when joined by the contiguous GRP section joint shown in figure 2-4 (c), more details are given in Chapter 4. Table 2-1 summarises the testing programmes undertaken to assess system 2. The single units, 1.2m wide by 2.4m high, were assembled and assessed in racking tests at three different lengths; 1.2m, 2.4m and 3.6m. Three walls with openings were also constructed. Short lengths were added to complete rails and to ensure adequate base fixity of the walls. One full-scale and multiple intermediate-and bench-scale fire tests were undertaken on the system. The main objectives of the scheme were to develop a panel product based on the System 1, presented before. The programme was developed to enable the reduction of additional sacrificial plasterboard layers, which due to their labour intensive installation increased the costs per square meter of wall. It was aimed to omit the plasterboard for the 30 minutes fire resistance requirements, which also meant that the external horizontal steel u-channel needed to be replaced by an internal jointing section so that no additional cladding was necessary.

2.5.3 System 3: Structural Insulated Panel (as commonly used in the US)

The Structural Insulated Panel system investigated in the scope of this programme is common in the US market and consists of 11mm thick oriented strand boards (OSB) and a 100mm EPS core. The 11mm thick OSB veneers are glued to a 100mm thick EPS core with adhesive 1. The single panels do not include any internal veneer linking units. The core is recessed by 40mm to accommodate the top and bottom horizontal timber rails, which both link the panels lengthways and connect them to the next level of construction as shown in figure 2-3 (a) and in more detail in figure 2-12. The vertical joint between panels is shown in figure 2-4 (a). In the recessed grooves within the core adjacent to the face layers, 76mm wide by 12mm thick tongues of OSB are inserted and fixed by glue linking the panels. The top/ bottom rail and vertical inserts are all site fixed using a wood adhesive 1.

This system was assessed in racking under varying vertical loads and in the bench-scale fire testing scheme, see also table 2-1. The third system was a more general system and included in the programme for comparison. In the racking programme the influence of bottom rail detail, glued connections and ductile board materials was examined. The fire performance of the wooden based board was examined in the bench-scale fire tests for comparison to the mineral based board materials used in the previous systems. Full-scale fire test data was made available by Kingspan TEK Ltd. and enabled an enhanced comparison of a derivative of the generic panel system using a PIR core instead of EPS. Whilst the vertical joint between panels remains the biscuit system described above,

1 Glue specification: Apollo Astrolok adhesive (A. 7521)
additional full height wooden studding is placed at regular intervals along the length of the wall (similar to edge inserts shown in figure 2-6 (a)). Furthermore TEK walls always use screws to connect both the horizontal rails and the vertical OSB biscuit sections. Both types of System 3 panels are lighter than panel Systems 1 and 2.

2.6 Summary sandwich panel construction

Structural sandwich wall building systems accelerate the construction process mainly due to their factory prefabrication and modular building technique. Whilst the use of pre-assembled wall units of long lengths would further speed up the building process, as successfully demonstrated in the related timber frame wall construction, this is as yet not common in the sandwich panel industry. In most cases wall units are assembled from single panels, connecting the panels at their vertical interface and linking them horizontally through a continuous rail. In most wall systems the vertical joint takes principally two forms either contiguous or intermittent; both types have been investigated in further detail in the scope of the work. Whilst horizontal joints are always continuously linking the panel sections they can vary in their method of insertion into the panel. In the majority of wall systems an internal horizontal joint is used, which is slotted into a recess behind the cladding veneers. This can be advantageous especially in applications in which no additional sacrificial plasterboard is required. In some wall systems the horizontal rail is external, capping the panel ends and in this configuration an extra finishing layer is necessary to providing a smooth surface finish. Both types of horizontal link have been investigated in further detail. Sandwich walls are in most cases finished with varying layers of sacrificial plasterboard to meet the fire resistance and acoustic requirements. These layers are commonly mounted onto battens and the created void between panel and lining is used for laying services. External finishes are more diversified and range from brick skin to wooden cladding.

The efficiency, quality and applicability of sandwich wall systems in domestic construction is affected by a range of design issues spanning from the manufacture of the panels to their structural, fire and insulation capacities. The durability and robustness of the panels are also factors influencing the design of the walls. In commercially available wall systems all these issues are considered and the choice of principal panel materials, the application of sacrificial board layers and guidelines with respect to handling and storage guide on the successful use of sandwich wall systems for residential and commercial construction. The long-term performance of the wall panels has to be monitored and examined in more detail as this type of construction is introduced more widely into the building industry.

In structural sandwich panel wall construction a range of techniques for mounting floors, providing openings and constructing corners is employed. The solutions reviewed in the chapter are related to the three panel systems investigated in the study. This was felt necessary since construction details are diversified and dependent from the panel manufacturer. However, the construction solutions are presented in generic from and only minor differences would be encountered amongst different systems.
Three different structural sandwich wall panel systems have been investigated in detail. All three panels differ in their veneer and core materials and also with respect to horizontal and vertical jointing and the presence of internal linking units. System 1 (Panablok) and System 2 (Experimental prototype system) were related and both incorporated internal studding. System 3 is the most closely related wall system to the American “SIP” sandwich panels with no internal intermediate studding and wood based veneers. As system 2 was the experimental system developed specifically for this study the widest range of testing was conducted on these panels.
Vertical joints linking the single panels see figures 2-4 to 2-5

Horizontal rails (Figure 2-3)

2.4 to 2.7 m

1.2 m

2.4 m

Connection to floor

Figure 2-1: Typical assembled structural sandwich wall unit with horizontal and vertical connections

Timber battens or plasterboard dabs

Services duct in cavity or inside the panel-chases at set height and centres

Internal layer of plasterboard, can be of varying thickness depending on requirements

Walltie, if required

Waterproof breather

Figure 2-2: Section through sandwich wall with internal and external cladding
Glue sealant is applied to ensure airtightness (as common in wall System 3).

(a) Bottom rail connections with wooden internal rails, fixed by nails or glue. Sealant is applied to ensure airtightness (as common in wall System 3).

(b) Bottom rail connection used in prototype building system 2 innovative GRP (glass reinforced phenolic) top hat section is placed behind veneers, into recesses cast into the panel ends and fixed. The holding down bolt is installed through the top of the joint.

(c) Bottom joint detail showing bottom u-channel of cold-formed steel capping the panel ends. The core is not recessed to take the joint. Panel and rail are connected through self-tapping screws (as used in System 1).

Figure 2-3: Bottom rail configurations (used inverted for the top rail)
Timber splines fixed by nails or glue

Glue sealant injected along panel joint

Glue-fixed OSB splines

OSB tongues inserted behind the veneers. Sealant is applied to ensure airtightness of joint.

(a)

Contiguous vertical wood stud. Either inserted in (1) two single stud units, (2) one large section. Both fixed by nails or glue.

(b) b-1

(c) Align and fix

GRP vertical section used as joint in experimental panel system.

(d) Align and fix

Contiguous steel joint used in experimental panel system (1) one-leafed joint, (2) two-leafed joint.

One-leafed steel joint

Two-leafed steel joint

In end configuration 1-2 mm gap between board edges

Figure 2-4: Contiguous vertical jointing options
Figure 2-5: (Courtesy of Marshalls Panablok Ltd.) Intermittent vertical joint. Three hook sections are placed along the height of the panel.

Figure 2-6: Corner and junction details
Floor-wall connection: Scabs are placed at both sides of the joist to prevent torsional buckling. Insulation is installed between floor joist and outer face of panel to ensure adequate performance.

Floor joist hanger used for hanging light- and heavyweight floors from the wall sections. In this solution cold-bridging is avoided.

Floor rests on wall panel. Cold bridging is avoided by installing an additional insulation board.

Figure 2-7: Most common floor-wall constructions
Figure 2-8: Options for forming openings in structural sandwich walls

(a) Openings cut out of large panels

(b) Opening build up of parts (common)

(c) Opening cut out of small panels

Figure 2-9: Framing of openings

(a) Framing of opening
(Courtesy of Kingspan)

(b) Special lintel detail
(Courtesy of Marshalls)
Figure 2-10: System 1 panel details

Figure 2-11: System 2 details (Internal studding as in System 1: Σ-stud)

Figure 2-12: Bottom rail construction System 3 (no additional studding)
Table 2-1: Testing programmes undertaken for each panel system

<table>
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<th>Testing Programmes</th>
<th>Racking</th>
<th>Bending</th>
<th>Compression Vertical</th>
<th>Full-scale fire</th>
<th>Intermediate-scale fire</th>
<th>Bench-scale fire</th>
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3.1 Introduction to literature review

To prepare for the research work on the fire and racking resistance of sandwich panels it has been necessary to survey literature in a number of areas. This review starts by giving a general introduction to the history, use and application of both cladding and loadbearing sandwich panels in the construction industry. Although extensive information is available on the load and fire behaviour of cladding sandwich panels, the work on structural sandwich panels in similar areas is minimal. With regard to the structural, in plane racking behaviour the review will therefore report the little work undertaken on loadbearing sandwich walls and supplement this with appropriate information from the much greater volume of work on timber frame construction. The most comprehensive evaluation on the fire behaviour of structural sandwich walls has been undertaken by the Forest Products Laboratory in the United States.

The main aim of the research has been to establish the factors influencing the fire behaviour of structural sandwich walls through a parametric study. This has necessitated a much wider review of literature to cover

(i) research undertaken into the fire behaviour of materials associated with structural sandwich walls, i.e. core and board materials,
(ii) use of indicative testing in small scale to predict the fire performance of materials and building systems in larger scale
   • non-loadbearing, cladding materials and building units
   • loadbearing materials and building systems,
(iii) analytical models developed to predict the fire resistance of lightweight wall systems.

Although the specific materials used in the structural sandwich walls investigated in this study have not been subject of detailed examination, the research into their generic material groups, i.e. synthetic cores and mineral based boards, has been pursued more extensively. The review presents the more applicable studies and comments on the findings and their relevance to this investigation.

In both, material and system testing, the use of smaller scale indicative tests is preferred to full-scale testing. The modelling and prediction of the fire behaviour of materials and building systems in larger scale tests through indicative small-scale testing, is a growing field in fire engineering. At the start of the project the range of fire test methods suitable for the parametric evaluation of the fire resistance behaviour of structural sandwich walls were surveyed. The test options were narrowed and the following selected for the investigation

(i) a bench-scale reaction-to-fire Cone calorimeter test,
(ii) a reduced-scale furnace fire resistance test, and
The use of these methods for research and the correlation work for materials and systems most closely related to sandwich wall panels is presented. The presentation of this work is further subdivided since the three chosen test methods are in their traditional use not entirely compatible in their purpose. Whilst the two furnace tests establish the fire resistance of building units, the Cone calorimeter is a bench-scale reaction-to-fire test, which is commonly used to model the specimen's reaction to full-scale fire exposure. Although reaction-to-fire tests establish the characteristic fire response of buildings systems in a fire compartment, they do not assess the effect of material degradation and other decomposition mechanisms on their loadbearing ability and fire resistance. In the scope of this study both issues, the degradation characteristics of the sandwich wall building units and their reduction in loadbearing ability, need to be evaluated. Therefore a subsequent section of the survey presents work in which reduced-scale testing has been employed to predict and assess the fire degradation of building systems in correlation with their reduction in loadbearing ability.

The final section reviews several analytical structural fire resistance models for lightweight building systems and the focus is on lightweight timber frame and cold formed steel walls. For these analytical models, results from full-scale fire resistance tests have been collected to correlate the temperature-time history and stiffness loss of the lightweight walls. In fire resistance testing small-scale testing is not common and only a few researchers have investigated the correlation of specimen performance in small- and large scale furnace tests: this work is also reviewed.

3.2 Fire and Structural behaviour of sandwich panel building products

3.2.1 Development of sandwich panels

Before the development of polyurethane in the 1960's, sandwich panel technology was mainly used in the aeronautical and ship industries. The lightweight, highly insulating, water resistant and loadbearing panels were used as outer skins for ships and aeroplanes (Allen, 1969). Structural insulated panels for use as loadbearing units in domestic constructions were first introduced in the US housing market in the 1950's (Chaleff, 2001). The core materials were then not self-adhesive and the composite section was formed by gluing the outer faces to the internal core substrate (Eickner, Heebink et al., 1973). Since the developments in the synthetic polymer technology, sandwich panels have become increasingly common in the building industry. The demand for low cost factory premises and superstores across Europe and the rest of the world has fuelled the demand for cladding sandwich panel construction, which are used in conjunction with steel portal frames (Murrell and Kotthoff, 1999). More than 20 million m² of polyurethane filled insulation panels are produced in Europe each year (Walter and Wittbecker, 1993). The number easily doubles if other core materials, such as mineral wool or expanded polystyrene, are included. Their high thermal insulation makes them especially suited for extreme climate environments (Karst, 1995), (Battrick, 2001), such as cold storage or the...
food processing industry. In large factories cladding panels are not only used as an outer envelope but also for internal walls and non-structural ceilings dividing the large factory floor area into separate working units (IACSC, 2000).

Since their first introduction in the 1950's, loadbearing sandwich building systems have adopted modern production techniques, using the same core materials as cladding panels and established a market share in dwelling construction in the US of 3 to 5 %, depending on the State (Andrews, LeRoy et al., 2002). Their excellent insulation performance makes them increasingly popular with house builders although the wider spread of the system, especially in the US, suffers from the non-unified and widely scattered number of manufacturers across the country. Attempts to organize the various manufactures have been only partly successful and a trade organisation (SIPA) has been initiated (Wachtler and Cushman, 2002).

3.2.2 Cladding sandwich panels

3.2.2.1 Structural behaviour cladding sandwich panels

Research work into the structural behaviour of the cladding panel units was pioneered by Allen (1969), Berner (1978), Berner (1998), Davies (1987a,b). In a range of studies the authors have researched and commented on the performance of sandwich slabs in compression, bending, shear and creep and also covered the behaviour of isotropic/ anisotropic sandwich plates (Allen, 1969). Davies (1987) developed an analytical model for compression behaviour of sandwich struts and Berner (1978, 1998) investigated and reported the bending and creep performance of multi-span sandwich slabs. Based on Berners' (1978) work span tables for cladding roof and wall sandwich panels have been developed, also incorporating the distribution of internal bending forces in statically undetermined sandwich slabs when exposed to large natural temperature variations (winter/summer). This is especially important in the design of roofing panels where the surface colour of the steel sheathing has major influence on the temperature differential through the panel depth. Their combined work formed the basis for regulations like the ECCS (European Convention for Constructional Steelwork) (16) and EN 14509 (17).

3.2.2.2 Fire behaviour of cladding sandwich panels

In the last decade fires in premises constructed with metal-faced sandwich panels have been frequent. With direct losses of more than £10m per fire, the food processing industry was particularly affected (Cooke, 1997). In 1993 a fire in the Sun Valley poultry factory in Hereford cost the lives of two firemen. The rapid, hidden fire spread within the highly combustible polystyrene cores, causing the metal faces of the panels to delaminate and bend over, trapping both firemen as they tried to fight the fire inside the premise. In addition dense, toxic smoke built-up and delayed their rescue. Following on from these events coverage in the printed media was frequent and research interest was fuelled. The concerns were grave and fire brigades embarked on surveying single-storey structures to determine the extent of the use of insulating sandwich panels (Murrel and Kotthoff, 1999). In addition the Home Office's Fire and Emergency Planning directorate published a report (Morgan and Shipp, 1999) offering guidance for fighting fires in building...
containing sandwich panels. It concluded that, despite the reluctance of British fire services to abandon burning buildings and leave them to complete destruction, the dangers from burning sandwich panels would demand defensive tactics as the usual method of response. Work by Cooke (Cooke, 1987), (Cooke, 1997), (Cooke, 1999), (Cooke, 2001) analysed the main failure mechanisms of sandwich panels exposed to fire of varying severity; the most dangerous effect was established to be the delamination of the face veneers. He suggested special connection methods for the panel ends, which rigidly joined the steel faces to the structure.

Fires in sandwich panel structures also cause indirect damage such as the loss of records, interruption of business and widespread smoke damage. Together these amount to many times the direct fire losses. Requirements of insurance companies are therefore more stringent than those of statutory authorities (Davies, 1996). The limitation of damage to the structure of the building, its contents and the viability of the business are aspects of fire design which are being enforced by insurance organisations such as the Loss Prevention Council (LPC) in the UK or Factory Mutual (FM) in the USA and worldwide. The LPC is a product approval organisation acting on behalf of the major insurance companies in the UK (Davies, 1996). It consolidated its position on the fire performance of insulating sandwich panels by publishing recommendations specifying the performance requirements for panelled structures (18). At the same time the test evidence collected from the standard fire resistance tests to BS 476: Parts 21 (4) and 22 (5) were not accurately revealing the fire hazard constituent from cladding sandwich panels. Towler and Jackman (1995) warned that results from standard fire tests needed to be analysed and implemented with great care to ensure the life safety of occupants in buildings clad with insulating sandwich panels. The ideal testing regime for such panels was regarded to be a combination of reaction-to-fire and resistance-to-fire tests. In such a regime the panelled structure could be assessed in the larger-scale to obtain information about critical panel characteristics such as the joints between single units and the connectivity of panels and structure. A range of large-scale tests was suggested, including LPS 1181 (18), ISO 9705 (2) and ISO CD 13784 (19), (Knight, 1998) and several arrangements trialled (Rogowski, 1987), (Walter and Wittbecker, 1993), (Hildebrand, Levio et al., 1993), (Parlor, 2000). Currently the ISO 9705 room corner test (19) is one of the most commonly used and recognized tests for cladding sandwich panels within the European Union. It is a full-scale reaction-to-fire test where the sandwich wall panels are installed on a non-combustible frame creating a room of 3.6x2.4x2.4m (length/width/height). The measurements include the time to ignition of the specimen; rate of flame spread, radiation intensity, i.e. overall heat released in the room, rate of burning, temperature levels, chemical composition of combustion gases and smoke emission rates. The first ten minutes generally allow the classification of the majority of combustible materials/products, but products of superior fire performance can also be discriminated in later stages of the test (Babrauskas, 1997). In addition to the large-scale assessment indicative smaller-scale tests have been put forward to enable the ranking of various lining and cladding products used within the building industry, including cladding sandwich panels. Several small-scale regimes have been trialled and the Single Burning
Item test (SBI test) (20) has been adopted by the European Union. Apart from the SBI based performance correlation, the ranking of products and prediction of ISO 9705 (2) room corner performance through a reduced test-scale could not be achieved (Babrauskas, 1997).

In order to address the emerging safety issue with regard to the fire behaviour of cladding sandwich panels several testing schemes have been conducted on such panels since the 1980's, including the real scale fire test of a three- storey steel frame structure clad with non-structural sandwich panels in Germany (Studiengesellschaft für Anwendungstechnik von Eisen und Stahl e.V., 1986), (Karst, 1995). Since 1993 the number of test programmes assessing cladding sandwich panels has increased (Colwell and Smith, 2000), (Parlor, 2000), (Babrauskas, 1997). From these tests it can be concluded that the method of construction, e.g. jointing and mounting of panels, is critical to determining their fire performance. Panels with PUR, PIR and EPS cores perform distinctively and different from panels filled with non-combustible materials, such as the most commonly used mineral wool. All researchers seem to agree that synthetic polymeric foams represent a greater fire threat.

3.2.3 Loadbearing sandwich panels
3.2.3.1 Structural behaviour
Recent research about structural sandwich panels is less numerous. A textbook by Allen (1969) on sandwich construction summarizes research into the structural behaviour of sandwich structures undertaken in the 1940's to 1960's mainly by the US Department of Defence and the Forest Products Laboratory. It is a comprehensive textbook providing the analytical tools for designing sandwich panels in bending and vertical load. Sandwich theory is derived from the analogy that the stiff facings of a sandwich unit are the main-load carrying parts and the core serves as a spacer to distance them apart. Faces and core have identical functions to flanges and web in an I beam (Birman and Bert, 2002). Consequently the core needs to be stiff enough, to ensure that the faces remain spaced apart, and shear strong to prevent the sliding of the veneers under load (Allen, 1969). In addition the core provides a constant elastic support to the faces (Berner, 1998), which stabilises them and thereby increases their loadbearing capacity by preventing local buckling or wrinkling failure modes often observed in metal faced sandwich panels. However, despite the adequate shear performance of the core, the connection of high-density faces and low-density core causes additional deformations under loading due to shear deformations within the core which need to be taken into account (Blass, Aune et al., 1995).

The US Department of Housing and Urban development (HUD) in cooperation with the Forest Products Laboratory (FPL) completed the most extensive work on loadbearing sandwich panels. Their state of the art report established performance criteria for the loadbearing units and was published in 1973 (Eickner, Heebink et al., 1973). The report, authored by the Forest Products Laboratory in Madison/ Wisconsin, covered topics such as the structural design, fire safety, acoustic performance, dimensional stability and
durability. The report compiled design issues as raised by the American building code requirements and surveyed potentially new areas of building legislation, such as smoke development and toxicity levels produced by the panels in a fire case. The report covered a range of structural design issues such as bending, vertical load and racking resistance and also provided design tables for panels with isotropic faces or orthotropic cores. Bending and vertical load capacities of the composite panels with thin faces and antiplane core were recommended to be verified by design formulas given in Kuenzi (1959). The analysis through the design formulas was recognized to yield good prediction of the panels’ structural performance.

3.2.3.2 Racking

A) Sandwich walls

With respect to racking performance the FPL report (Eickner, Heebink et al., 1973) only gives limited information. The ultimate loadbearing abilities of the panels when exposed to lateral forces in the plane of the units were established through racking tests. The loadbearing sandwich systems were found to have high resistance to the shearing distortion and usually could withstand high loads. Although the panels’ racking resistance was confirmed to be above the requirements for building walls, a proving test was recommended as a standard panel assessment to ensure adequate wall performance, especially with respect to the nailed fixings of the wall at vertical and horizontal joints.

Anderson (1993) examined the racking performance of wood frame cavity walls, which were sprayed with polyurethane foam to enhance the wall’s insulation performance. Although the panels are not sandwich panels, the aim of the programme to assess the use of a sprayed foam layer on wall performance aligns the investigation with the work presented in this thesis. The programme assessed thirty panels, eighteen of which were sprayed with polyurethane and twelve control panels were tested without additional spraying for comparison. The study established an improvement in racking performance between the standard, non-sprayed wall and the insulated wall. The sprayed wall performed superior to the conventional wall. The improvement allowed the spacing of studs to be enlarged. When the panels were tested in compression under axial loads no difference in performance between sprayed and plain panels could be found. The vertical load capacity was found to be dependent on stud size and spacing and also sheathing material.

B) Lightweight frame walls

Although sandwich and timber frame walls transfer vertical and bending loads differently, their mechanisms of transferring lateral loads in the plane of the wall are similar. In both systems the sheathing boards and their fixings to the horizontal and vertical jointing and framing members, if present, provide the lateral racking resistance. Furthermore both systems are most commonly erected in platform frame construction, which is the most widely used lightweight system construction method in the UK and

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1 Shear stress is assumed constant over the depth of the core
North America. In platform frame construction each floor is built as a platform on which the subsequent storey is placed (Burchell and Sunter, 1987). The clear similarities between both lightweight wall systems and the lack of detailed information on sandwich wall racking behaviour justify the survey of timber frame literature in the scope of this work. The following section will concentrate on reviewing the UK timber frame design approach although a considerable amount of racking work using the American racking assessment method for walls is also available. The UK and US racking design approaches are different in principle. In the US codes the racking resistance of a wall unit is generally based on the results of the standard ASTM E 72 test (22), which applies horizontal load, identical to the UK standard, to the top of a 2.4 by 2.4m wall assembly. However there are major differences with respect to the vertical loading, idealized in the UK test by single load application points aligned with the stud positions at the top of the assembly. The ASTM test is a zero vertical load test but provides over-turning and uplift restraint by a holding down strap positioned at the front of the wall, where the racking load is applied (Skaggs and Rose, 1996). The vertical load applied to the wall in the UK method to achieve a similar uplift and racking resistance to the ASTM test is substantial and not thought representative of dwelling construction (Griffiths and Wickens, 1996a), (Griffiths, Mettem et al., 1996).

The UK timber frame design was developed independent of the ASTM method based on their own test method. They argued that the ASTM tests was initially designed as a pass/fail categorisation test and was unsuited to giving design information. This argument was supported by the introduction of the E564 test in the US in which both vertical restraint and top load were removed. In order to provide an accreditation system the UK investigated the practical performance of timber frame walls in great detail; the UK approach is deemed more comprehensive than in any other country (Griffiths and Wickens, 1996). A comprehensive summary of timber frame behaviour and the derivation of design figures included in early versions of the Code of Practice can be found in Griffiths, 1987. As the current UK Code does not cover the performance of panels in excess of 2.7m in height, the increasing use of timber frame walls for higher rise commercial and industrial buildings (Mettem, Pitts et al., 1996) has led to further research. Adjustments for panel heights exceeding 2.7 m and up to 4.8 m were established in a joint research programme of the Building Research Establishment and the University of Surrey and are reported in Enjily and Griffiths, 1996. The investigation found the racking resistance at increased panel heights (>2.4m) not to be proportional to the reaction moment resulting from the horizontal force and the height and adjustment factors have been suggested. At the same time the UK investigated higher rise construction and the “Timber Frame 2000” project (Grantham, Enjily et al., 2003) covered both structural and fire tests on a five storeys high, full-scale timber frame building.

The influence of panel length has been investigated by various researchers (Patton-Mallory, Wolfe et al., 1985), (Griffiths, 1987) and predictive models have been developed (Johnson and Dolan, 1996). Generally racking resistance (in unit kN/mm) will tend to a maximum value the longer the panel. Although strength performance is not critical to the
design for shorter wall lengths, it becomes increasingly significant for longer panels at higher vertical load. The influence of vertical load on panel performance is dependent on the length and improvement in performance with additional vertical load decreases with length. Therefore the UK design code considers the vertical load factor in conjunction with the length factor (Griffiths, 1987), (Griffiths and Wickens, 1996), (Griffiths and Wickens, 1996a), (TRADA, 1989).

Openings within a wall reduce the racking capacity of the unit. The effect of openings on the racking performance of walls has been researched extensively in the UK and the US (Johnson and Dolan, 1996), (Line and Douglas, 1996), (Patton-Mallory, Wolfe et al., 1985), (Griffiths, 1987). The research found that the size and form of the individual openings, their layout and the direction of load impacted on the magnitude of performance loss. Openings formed by narrow widths but full height panels were seen to weaken the wall in addition to the natural performance reduction effect of openings. It was shown that this effect could be avoided if wider board pieces were used (Griffiths, 1987). Johnson and Dolan (1996) assessed a simple method proposed by Sugiyama for predicting the loss in racking strength in walls with openings. They concluded that Sugiyama’s analytical method underestimated the capacity of walls with openings but could be used for a safe lower bound prediction. Line and Douglas (1996) investigated the need for additional tension anchorage within the walls in front of openings. They proposed a design method by which fewer uplift restraints were needed.

For the UK Code of Practice the influence of various sheathing materials on the racking performance of walls has been investigated in an extensive test programme (Griffiths, 1987). The effect of other board materials such as low-density bitumen impregnated boards, have been assessed more recently (Griffiths and Bregulla, 2002b), (Griffiths and Bregulla, 2002c). The effect of sheathing on both sides of the frame has been investigated by Patton-Mallory and McCutcheon (1987). The effect was modelled by four types of fastener load-slip data, which predicted the wall displacement under load. The asymptotic fastener curves were compared with test results from small wall tests and were found to yield good predictions of shear wall performance. The analytical modelling and prediction of shear wall behaviour has been pursued (Griffiths, 1987), (Dolan and Foschi, 1989). For a successful prediction of racking stiffness and strength it is essential to model the nail performance both parallel and perpendicular to the grain of the frame timber and the respective failure modes of nails and board. Recent models have included non-linear behaviour of the connectors between sheathing and framing and also incorporated bearing effect of the sheathing elements. Dolan and Foschi (1989) validated their model with static shear wall tests and found that the capacity of the wall could be predicted accurately. Whether the stiffness performance of the wall was successfully calculated is not explicitly stated in the reference. Griffiths (1987) had noted the difficulties of modelling both the high early stiffness of walls and their failure performance.

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2 In the standard test only one side of the frame is sheathed with the board material.
The fact that most of the catastrophic wall failures occur under dynamic loading associated with natural disasters rather than due to standard wind loading has increased the research interest in dynamic and reversed cyclic loading in recent years. The influencing factors to cyclic, seismic racking resistance of timber frame shear walls has been investigated amongst others by White and Dolan (1996) and Skaggs and Rose (1996). Their research established the dynamic racking resistance of walls to be governed by the same influencing factors as the static racking resistance discussed earlier. Whilst wall length and vertical load improve performance, openings reduce the performance level of the wall.

The shear wall behaviour of lightweight wall systems other than timber frame has also been evaluated; the systems most often researched are lightweight cold-formed steel walls. Gad, Chandler et al. (1999), Gad, Duffield et al. (1999), Serette, Encalada et al., (1997), Salenikovich, Dolan et al. (2000), Griffiths and Bregulla (2000a) amongst others have undertaken racking test programmes to different Codes of Practice. The test programme undertaken to the UK standard (Griffiths and Bregulla, 2002a) has shown the influence of the superior connection of rails and studs, which reduced the uplift in front of the panel and the effect of openings. The bracing and detailing of the wall panels, especially in the front of the wall and around openings, were established vital to performance.

3.2.3.3 Fire behaviour of loadbearing sandwich panels
As part of the research programme into structural sandwich walls the FPL conducted several series of fire tests to establish the fire performance of the structural sandwich walls. The first series of tests were undertaken in 1975 and designed to compare the fire resistance of sandwich panel assemblies with standard timber frame wall construction, widely used in the US. These tests also enabled acceptance criteria for sandwich panel construction for application in dwellings to be determined. Later, in 1978, a second series of tests was completed which attempted to evaluate the fire risk represented by the use of the new sandwich type construction when exposed to realistic fire scenarios. A final set of loaded full-scale room fire tests was conducted in 1980 assessing the same type of wall panels as in the previous 1975/1978 fire test series. In the final series the comparison of panel performance when subjected to real fire exposure conditions was examined. The findings of three test series are presented subsequently. Further details of the test set-up and findings of the test series are discussed in Chapter 5.

A) Forest Products Laboratory, 1975: Fire resistance tests on sandwich walls
(Eickner, 1975)
Research work into the fire resistance of sandwich panels was pioneered at the FPL in the United States. The first test series was designed to compare the fire resistance of structural sandwich walls with the traditional timber frame walls commonly used in US building construction. Wood frame walls in dwelling construction had generally indicated adequate fire endurance performance and with the introduction of the new type of loadbearing walls, advocated for their material efficiency, new acceptance criteria needed
to be established. This fire resistance test series is of great value to the investigation since it describes the characteristic performance pattern of loaded sandwich walls in fire. The test set-up and observations have been discussed in greater detail in Chapter 5 and only a brief review of the main findings is presented here.

The outcome of the test regime suggested that the internal core material of the sandwich unit was influential to the fire resistance and structural performance of the wall units. The Phenolic treated paper honeycomb core performed superior to the PUR and PIR cored wall panels. However, all sandwich walls performed inferior to standard timber frame construction, exhibiting the critical failure mode of structural collapse earlier in the fire tests. The research established a distinct difference between the sandwich panel wall and timber frame wall fire behaviour. Whilst the sandwich walls failed structurally through buckling the timber frame walls predominantly exhibited burn through, hence insulation/integrity failure and no structural collapse. The fire resistance of the sandwich panel walls could be enhanced by cladding the exposed face of the sandwich assemblies with sacrificial layers of fire rated plasterboard. Thereby their fire resistance rating could be augmented from 3-6 minutes in the unprotected wall assemblies, to 29 minutes in the plasterboard clad assemblies. Alternative protection measures were assessed but were only found effective at increased thickness. Smoke development was considerable in all sandwich wall tests and was noted as potential threat.

**B) Forest Products Laboratory, 1978: Room corner wall fire tests (Holmes, 1978)**

In the second set of fire tests the sandwich walls and their single components were tested in a room-corner wall set-up. As the fire resistance tests presented before this programme was designed to establish fire performance criteria for sandwich panels in comparison with traditional building materials. The room-corner test scenario was chosen due to the realistic fire exposure and auxiliary measurements enabling flammability, flame spread and smoke and combustion gas generation to be determined. The main purpose of the work was to establish the contribution of the sandwich units' internal and highly combustible core material to the fire hazard in a room compartment. It also assessed whether the cladding board layers in the sandwiched unit could reduce or even eliminate the anticipated fire hazard of the highly flammable cores.

The results showed that the sandwich walls contributed considerably to the severity of the room fire scenario. The overall temperature levels reached in the test room constructed with sandwich panels were markedly higher than in that clad with traditional materials. When the sandwich panel core materials were tested without cladding boards the temperature and smoke build-up in the room was even more affected, especially with PUR and PIR cores. In these test rooms the measured temperature levels and smoke build-up were up to twice as severe as in any other scenario. A fire retarded paper honeycomb core exhibited improved performance levels when compared to the synthetic PUR and PIR core substrates. When the entire sandwich panel unit was tested flame spread and smoke development could be reduced. Gypsum plasterboard further reduced both hazards. The results established the relative performance of "traditional" materials.
with respect to the new synthetic materials used in sandwich panels. Overall the results were not highly conclusive, especially with respect to the analysis of combustion gases

C) Forest Products Laboratory, 1980: Fire endurance of one-storey three-room structures of sandwich and wood-frame construction under load (Eickner, Holmes et al., 1981)

The final test programme in the series was a combination of both fire resistance and the reaction-to-fire test. Several one-storey flat roofed room structures of, 4.8x7.3x2.4m height, were constructed in the open with sandwich and wood-frame wall elements. The structure was subjected to a realistic fire scenario modelled by wood cribs. The principal objective of the study was to obtain information about the actual fire endurance time of structural sandwich walls when exposed to realistic interior fires. The structure was to be representative of a two storey dwelling construction and the vertical wall loads were applied accordingly. In addition to the evaluation of the plain dwelling walls, the programme assessed the influence of sacrificial plasterboard protection on the heat build and fire severity within the fire compartments. The performance of the sandwich structures was compared to conventional wood-frame construction and both performances were compared to the findings obtained from laboratory-scale fire resistance tests in 1975 (Eickner, 1975).

The large-scale tests confirmed the poor fire resistance performance of the unprotected sandwich walls previously indicated by the laboratory tests. Interior protection, in the form of additional sacrificial plasterboard improved the performance of the sandwich wall structures. Although the relative performance levels were in general agreement between both test regimes, the resistance times in the furnace test could not be predicted. The fire resistance of the walls in the structure tests were generally prolonged when compared to the full-scale furnace tests described in Eickner (1975). The authors concluded that life safety in dwellings could only be ensured if the load-bearing wall elements supported the loads beyond the time of flashover and loadbearing sandwich panels were only able to meet this criterion if the exposed faces of the panels were clad with protective layers, such as plasterboard. The panels did however provide adequate burn-through protection throughout the test time. This was regarded as sufficient to meet the insulation and integrity resistance for non-loadbearing wall assemblies. Although the report clearly shows the different performance levels of sandwich and timber frame walls it does not provide detailed data regarding the deflection behaviour of the units. The results do not focus on the correlation of temperature build-up and stiffness loss of the units. The smoke and toxicity measurements are ambiguous although the heat flux measurements indicated the severest compartment fire conditions in the structures constructed with sandwich walls. The potential threat of sandwich walls with respect to the sudden and premature failure underlined the need for further research.
D) Full-scale fire resistance tests on commercial sandwich wall products (Courtesy of Marshalls Panablok and Kingspan TEK.)

Extensive full-scale fire resistance test experience on System 1 and System 3 has been made available to the study by Marshalls Ltd. and Kingspan Insulation Ltd. The work by Marshalls has been of particular value to the study, since it assessed a wide range of wall compositions and loading regimes. General understanding of the fire behaviour of loadbearing sandwich panels has been extracted from the work and the test results have been commented and combined with the findings of this investigation in Chapter 5.

3.3 Fire reaction of materials related to structural sandwich panel walls

3.3.1 Influence of material performance

In the following section the literature relating to the fire reaction and performance of the materials used in the primary panel components of loadbearing sandwich panels is reviewed. The review concentrates on the main material groups employed in these components:

(i) foamed synthetic internal core substrates,
(ii) mineral-based outer board layers.

The assessment and testing of the components used in structural sandwich panels has been pursued separately from sandwich panel evaluation to a much wider scope. The fire performance of insulating foams has been researched to increasing extent in the last 20 years.

The fire reaction and degradation of single panel components and materials is of importance as the heat response of any building structure is related to the changes in the properties of the materials with which they are constructed. Classical structural design is based on the material properties at ambient temperature, i.e. around 20°C. When materials are exposed to severe heating conditions they degrade and the induced damage can take many forms. These heat induced changes to the materials affect on the structural properties and also influence on the temperature rise within the structure. The temperature dependent properties of material can be subdivided into four groups (Malhotra, 1982):

(i) Chemical, e.g. decomposition and charring
(ii) Physical, e.g. density, melting, spalling, softening
(iii) Mechanical, e.g. strength, elasticity, strain, creep
(iv) Thermal, e.g. conductivity, specific heat, enthalpy

The characteristic degradation parameters of various traditional building materials, such as concrete, wood and steel are inter alia described in Lie (1972), Malhotra (1982) and Shields and Silcock (1987). For the materials used in or related to structural sandwich walls, the research has focussed on the effect of heat on the chemical and thermal material properties. This is partly due to the fact that the single panel components are
generally employed in non-structural applications so that the changes in their physical and mechanical properties are of lesser importance.

3.3.2 Core materials
Sandwich panels are generally filled with auto-adhering synthetic foams, such as PUR or PIR and also in recent years, Phenolic. Although mineral wool cored cladding panels have been aggressively promoted due to their perceived superior fire performance (Parlor, 2000), loadbearing sandwich panels are predominantly injected with self-adhesive synthetic foams due to the more efficient manufacturing process. The following section reviews the literature on synthetic foams in general, their manufacture and other characteristics. The subsequent section then presents the work on the fire behaviour of these materials and reviews decomposition characteristics and factors influencing the foam design.

3.3.2.1 Synthetic foams
Synthetic foams are manufactured polymers. They are produced by mixing liquid polyisocyanates with polyols in the presence of a catalyst. These components bond in a process called polymerisation, which is an exothermic reaction. The foam may be obtained in a wide range of capacities from a few grams per minute to over 500kg/minute. The basic requirements for polymer production are a temperature controlled and conditioned environment, accurate metering and weighing facilities, uniform and reproducible mixing of the components, and a definite curing period (Oertel, 1993). Polyurethanes can be manufactured in a wide range of grades in densities from 6kg/m³ to 1220kg/m³, from flexible elastomers (rubbers) and foams to hard plastics. Sandwich panels are filled with rigid foams of closed cell structure with densities of about 40kg/m³ and above, which are extremely adhesive in a short time (app. 1 minute) (Woods, 1990). This allows the foam to be combined with most facing materials used in the building industry (Oertel, 1993), (Woods, 1990). General information about polyurethanes and other synthetic foams, their derivatives, manufacture and physical properties, with special reference to foams used in building products are given by Woods (1990) and Oertel (1993). Background information on synthetic polymers and their uses is also given by Schey (1987) and Demharter (1998) covering the general background to polymeric materials and their manufacture. The latter does not specifically focus on building products.

3.3.2.2 Fire behaviour of synthetic core materials
The general chemical decomposition of a wide range of synthetic foams, including polyurethanes, Phenolics and expanded/ extruded Polystyrene, are given in Beyler and Hirschler (1995). The most commonly used inorganic core material is mineral wool and although intrinsically inert, the material degrades by sintering when exposed to high temperatures (Klingsch and Wittbecker, 2001).

Various researchers have been pursuing the investigations into the fire behaviour of synthetic foams (Ohlemiller and Rogers, 1978), (Ohlemiller and Cleary, 1994),
(Checchin, Cecchini et al., 1999), (Torero and Fernandez-Pello, 1995), (Anderson, Randall et al., 2000), (Modesti, Lorenzetti et al., 2001) (Modesti and Lorenzetti, 2002), (Rossi, Camino et al., 2001), (Koo, Venumbiaka et al., 2000). Their findings cover the determination of the burning characteristics such as combustion modes and heat release rates, smoke production and toxicity levels of various foams. Some of the investigations concentrate on assessing specific foam formulas to determine the influence of fillers and other additional foam components on various fire characteristics (Modesti, Lorenzetti et al., 2001; Modesti and Lorenzetti, 2002), (Rossi, Camino et al., 2001), (Koo, Venumbiaka et al., 2000). The most commonly used tool for establishing the fire performance of polymers is the bench-scale Cone calorimeter (3), which was found to have good sensitivity for discriminating the fire characteristics of closely related foam types and also enabled post-ignition characteristics such as smoke production and weight loss to be determined (Checchin, Cecchini et al., 1999). The Cone calorimeter test, which employs a specimen of 100x100x50mm (breadth x width x depth) is used increasingly to reduce poor material choice, commonly made on the basis of arbitrary standards based fire tests (Irvine, McCluskey et al., 2000). In the majority of investigations this bench-scale test is used to determine the effectiveness of different flame retardant systems (Modesti, Lorenzetti, et al., 2001), charring agents (Modesti and Lorenzetti, 2002) or smoke suppressants (Rossi, Camino et al., 2001) on the fire performance of foams. The additives were proven effective in enhancing the fire performance of the materials. However, the improvement in the fire characteristics could lessen the mechanical performance levels of the polymers, as found by Modesti, Lorenzetti, et al. (2001) and Modesti and Lorenzetti (2002) for different commercially available PUR foam systems. Whilst polyurethane- and polystyrene- based foams have been assessed by a number of investigations the fire performance of the self-adhesive Phenolic foam, similar to the one employed in this work, has not been evaluated specifically. A related investigation by Mouritz and Gardiner (2002) tested PVC and Phenolic cored composites at varying irradiance levels. In comparison with the PVC cored sandwich, the Phenolic cored sandwich section was more flame and heat resistant. The core charred at temperatures of about 550°C. The generated char was solid and heat resistant and the uncharred portions of the foam, ahead of the direct combustion zone were also partly degraded, although the extent of damage was not quantified. The use of Phenolic resins is also common in the ship and aerospace industries and comparative studies with other resins have been undertaken. Hshieh and Besson (1997) compared the flammability of epoxy and Phenolic composites using three different test methods3. In all three test regimes the Phenolic based products exhibited the lowest ignitability, peak heat release rate, propensity to flashover and smoke production.

Despite the fact that synthetic foams and resins can be designed to meet specific fire requirements all researchers agree that the final selection of materials must be based on the combination of a range of properties, including mechanical strength, process ability, weight and cost. The work presented above concentrated on the quantification and assessment of the fire performance of specific synthetic polymer foam/ composite types,

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3 The Cone calorimeter, the NASA upward flame propagation test and the LOX (Liquid OXYgen) mechanical impact test.
which were governed by their chemical composition and additives. The findings exhibited the wide ranging performance levels of foams and underlined the necessity for detailed performance assessment. Although the findings prove the possibility for modification and enhancement of fire performance, the research into the fundamental principles of synthetic core combustion is more valid within the scope of this work.

Synthetic foams exhibit various decomposition modes ranging from smouldering and flaming combustion to punking (Drysdale, 1998). Fundamental research into the fire behaviour of foams has been undertaken for flexible and rigid polyurethane based foams. Considerable work has been conducted and reviews of the subject can be found for instance in Drysdale (1998). In the scope of this work only the available literature with special interest in rigid PUR foams is reviewed. Rigid PUR based foams are susceptible to smouldering combustion due to their large surface to volume ratio and low permeability to gas flow. Smouldering combustion is a special form of combustion in synthetic foams in which the surface oxidization of the generated char provides the heat necessary to propagate further thermal degradation of deeper material layers. Despite the low heat released in the smouldering process, the incomplete oxidization accompanying this form of decomposition, results in the over proportional generation of toxic combustion products. Furthermore the combustion mode is coupled with a potential transition to the rapid and dangerous flaming combustion, both of which make this form of combustion a severe fire safety issue. Ohlemiller (1978) was amongst the pioneers in investigating the combustion behaviour of flexible and rigid PUR foams and found two competing degradation reactions. When exposed to a heat flux the foam generated either

(i) smoulder suppressing tar, or
(ii) smoulder enhancing char.

Both reactions are competitive and the degradation of the foams, especially in rigid foams, is influenced by the formulation variables, such as water or retardants, and the physical factors, such as heating rate and air supply. Smouldering combustion was found to be the predominant degradation mode and the findings of Anderson, Randall et al. (2000) and Torero and Fernandez-Pello (1995) confirm this early research and identify the minimum temperature to sustain smouldering combustion to be about 300°C. The assessment of the burning characteristics of foams is an established field chemical reaction engineering. Branca, DiBlasi, et al. (2003) analyzed scanning electron microscope (SEM) photography of intact and decomposed rigid polyurethane foam structures. The structure of the intact rigid polyurethane foam consisted of polygonally shaped void cells of varying size ranging from 200μm to 800μm. After the exposure to heating at 25kW/m² the charred residue was of homogenous, slightly corrugated surface and consisted of an open network of particles (about 100μm), aligned in chains. When tested at 50kW/m² the decomposed foam residue was more fragile and particle size was further reduced. In both cases the cellular structure of the foam was destroyed and an open network of micron-sized particles was left. The strong reduction in pore size and the

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4 "Chemical reaction engineering" is the research into the equations for the combustions kinetics of decomposing materials, including the thermo gravimetric analysis of decomposing foams.
5 Commercial name polyurethane VO-RACOR, Dow chemical company, applications include sandwich panels.
change in morphology were established to be affecting the physical and thermal properties of the material.

3.3.2.3 Potential threats from burning core materials
Plastic building materials, such as synthetic foams, have the potential to shorten the time to flashover (Irvine, McCluskey et al., 2000). Occurrence of flashover, within a room is regarded as the limit to occupant survival and the onset of untenable conditions and greatly increased risk in other rooms within the building (Peacock, Reneke et al., 1999). Fire atmospheres expose people to life-threatening conditions, such as heat, smoke and toxic gases and escape criteria have been established through animal testing (Purser, 1995). The time until untenable conditions are reached within a fire compartment directly effects the escape chances of the building occupants and the basic principles underlying the safe and timely escape of building occupants from burning enclosures are reviewed in Marchant (1972, 1976). Synthetic foams, in particular when used in insulation, are known to emit a large amount of toxic fumes and also contribute to the severity of the fire within an enclosure. The majority of fire casualties each year are due to gas or smoke inhalation, which is recorded by the UK fire statistics (Watson and Gamble, 1999), (Goddard, 1995), and other statistical surveys and sources (Richardson, 2001), (Richardson, 1999), (Gann, Babrauskas et al., 1994), (Williams, 1984).

Generally there are no regulations limiting the use of synthetic materials in the UK. However, regulatory bodies are increasingly aware that the restriction of materials generating large amounts of toxic decomposition products is necessary (Gann, Babrauskas et al. 1994). In the US building codes, requirements for plastics and wall linings are implemented. In France, materials in public establishments are selected to limit the production of HCL and HCN (Sumi and Tsuchiya, 1978). The regulation addresses decorative materials, such as curtains and false ceilings and also interior finishes for walls and furniture but does not apply to insulation materials. The enforcement of these regulations is handicapped by the lack of a standard test methods able to check the compliance with the specific requirement levels (Babrauskas, 1995a).

3.3.3 Board materials
3.3.3.1 Board materials used in the sandwich wall panels investigated in this study
Although the cement-and gypsum based board materials used as face veneers in the structural sandwich panels in this study are not focus of research, the fire behaviour of the wood based Oriented strand board (OSB) has been pursued with respect to performance enhancing measures. The fire behaviour of these wood-based boards is marked by flaming and charring but can be improved using special treatment for the wood flakes (Yang, Chen et al., 2001).

3.3.3.2 Research on related board products: Plasterboard
Plasterboard is closely related to the mineral based board products used as structural boards in the sandwich walls and has been investigated by a number of studies. Plasterboard is frequently employed in lightweight building systems as cladding
sheathing, delaying the heat build-up in loadbearing units, and thereby providing so-called passive fire protection (Gerlich, Jones et al., 2001). The degradation behaviour of plasterboard has been investigated by a range of researchers (Mehaffey, Cuerrier et al., 1994), (Sultan, 1996), (Sultan, Alfawakhiri et al., 2001). A large number of plasterboard types from various manufacturers have been evaluated by researchers and the reported findings differ markedly (Jones, 2001). In the scope of this work the most frequently used plasterboard properties are presented.

Plasterboard acts as an effective shield against elevated temperatures because the gypsum material absorbs the heat energy from the fire. As a consequence the material changes its chemical structure and water is dissociated from the plasterboard's chemical structure. This causes the shrinkage of the gypsum core. The process, called dehydration is described further in Chapter 6. During the heat exposure the changes in material composition affect the thermal and physical properties of the plasterboard lining. Researchers have examined the effect of elevated temperatures on the following plasterboard properties

(i) density,
(ii) specific heat capacity,
(iii) thermal conductivity,
(iv) specific volumetric enthalpy.

They have found greatly varying values for all the above properties (Jones, 2001). The changes in density are particularly affected by the plasterboard type and the variations in product composition also affect the degradation of the thermal properties. The specific heat capacity of plasterboard ranges from 600 to 1500J/kgK at ambient temperature. All researchers found a peak in specific heat capacity, ranging from 900 to 50,000J/kgK, at about 100°C. Some researchers established a second increase in heat capacity at about 600°C. Similar to the specific heat capacity, the thermal conductivity also varied considerably, especially above 400°C. All researchers found an increase in enthalpy at 100°C to about 500MJ/kg. The temperature dependent plasterboard properties established by Mehaffey, Cuerrier et al. (1994) have been widely used in modelling work (Jones, 2001), (McGraw and Mowrer, 1999), (Gerlich, 1995), (Gerlich, Collier et al., 1996), (Gerlich, Jones et al., 2001).

The shrinkage of plasterboard and its change in thermal properties at high temperatures have a significant influence on its heat transfer characteristics. Axenenko and Thorpe (1996), Sultan (1996), Lie (1972) have investigated the linear expansion of plasterboard at various temperatures and provide dehydration analysis for standard and glass-fibre reinforced plasterboards. Axenenko and Thorpe (1996) introduced the concept of a dehydration front moving through the plasterboard layers and developed a reliable numerical algorithm for the analysis of heat transfer processes in plasterboard layers. The stresses developed within the plasterboard upon dehydration were found to exceed the tensile strength of the plaster by about two orders of magnitude, resulting in cracks. Shrinkage, crazing and cracking within the board depth affect the protection capacity of the plasterboard in the post-dehydration state. These failure mechanisms in the
plasterboard correlate to the temperature levels behind the protective layer and also affect the temperature exposure of the protected structural members. Shrinkage becomes significant at high temperatures and is responsible for fissures and the opening of joints, both of which can have marked influence on the heat build through an assembly (Mehaffey, Cuerrier et al., 1994). In glass-reinforced plasterboard this effect becomes predominant at board temperature above 700°C (Jones, 2001).

3.4 The effect of fire on materials and structures:
Multi-scale testing to predict the reaction/ performance of materials and systems when exposed to fire
3.4.1 The use of bench-scale testing to establish material performance in larger scale
The fire reaction of a material/ system can currently be evaluated in two ways. One is to undertake an experimental evaluation at full-scale, covering all possible end-use conditions and a range of fire scenarios. The other option is to use smaller-scale tests in conjunction with a calculation procedure to extrapolate the full-scale performance. The second approach is evidently more versatile and less time and cost consuming (Janssens, 1997). Generally the prediction of the fire behaviour of several products by a single encompassing predictive method is as yet not possible (Babrauskas, 1995a). Each type of product needs to be assessed separately with a custom-tailored predictive method. Full-scale testing remains essential to establish empirical information such as geometry and other product specific characteristics (Babrauskas, 1995b). Several predictive methods have been developed for special material/ product groups. The first successful modelling attempt was undertaken in the 1980’s when the full-scale heat release rate curves from burning upholstered furniture in the furniture calorimeter was successfully predicted using heat release rates measured in the bench-scale Cone calorimeter (Hirschler, 1999).

Researchers, such as Wickström and Göransson (1992), Östman and Nussbaum (1992), Östman and Tsantaridis (1994), Hekestad and Hovde (1999), Janssens, Dietenberger et al. (1995), Grexa, Janssens et al. (1995), Quintiere (1993), Babrauskas (1997), Ohlemiller and Clearly (1994), Brehob, Kim et al. (2001) amongst others have attempted to correlate material performance in a bench-scale test to fire reaction in the Room/ corner ISO 9705 test (2) set-up or to upward flame spread. Common to all developed models is the use of bench-scale test data, predominantly determined through the Cone calorimeter (3) and to a lesser extent, the LIFT apparatus (23). The bench-scale test regime provides material properties, such as heat release rate or time to ignition, which are employed in a calculation procedure. The bench-scale input data and the extrapolation convert the characteristic material reactions to the large-scale test performance. To date the best results from modelling have been achieved with relatively simple correlations (Babrauskas and Grayson, 1992). Although the methods are simple in application, their development is time-consuming.
3.4.2 Models for wall and ceiling linings

One of the most successful models developed for a wide spectrum of wall and ceiling linings showed excellent agreement with the predictions based only on the use of a single bench-scale test irradiance of 50kW/m² (Babrauskas, 1995) was developed by Wickström and Göransson (1992). The prediction was successful for materials ranging from expanded polystyrene to steel faced mineral wool sheets. Wickström and Göransson (1992) consider the modelling of the room/ corner test (ISO 9705 (2)) through a versatile, cheap bench-scale test as essential in an efficient fire classification system of products. However, their work showed that the translation between large and full-scale performance was not possible for all products. They advocate that certain products should always be assessed at full-scale, as their fire behaviour is impossible to model through a small-scale test. As a consequence they excluded products from the model, which

(i) contained protective surface layers,
(ii) contained joints,
(iii) suddenly cracked,
(iv) disintegrated and,
(v) generally any product which exposed highly flammable materials to the heat environment upon degradation.

The range of reaction-to-fire test models such as (Wickström and Göransson, 1992) (Östman and Nussbaum, 1992), (Östman and Tsantaridis, 1994) for wall ceiling linings show the elemental ability to predict the behaviour of materials in a full-scale room test by a smaller scale test regime. The main difficulty is in matching a bench-scale test to a larger scale tests with dissimilar boundary conditions, where the assessed materials exhibit very different failure modes. Some models provide a versatile correlation, by extrapolating the bench-scale performance in combination with the inherent burning behaviour of the materials in the larger scale tests (e.g. Wickström and Göransson, 1992). In these models the limitations of the predictions have often been associated with materials exhibiting uncontrolled burning behaviour upon degradation. However, other models do not incorporate any fire behavioural characteristics of the materials, such as the flame spread characteristics, which are bound to be influenced by the material and the flashover potential and only establish limited correlations in relation to the density of the material (such as in Östman and Tsantaridis, 1994).

3.4.2.1 Non-loadbearing sandwich systems

The reaction of sandwich structures, i.e. units built from layers of different materials, has also been assessed through reduced scale reaction-to-fire testing. Although the materials employed were not similar to the materials investigated in this study the investigations give valuable insight into the behaviour and testing of layered sandwich structures in bench-scale testing regimes.
A) Babrauskas' model of toxic gas emissions in cladding sandwich panels' tests (1997b)

Babrauskas (1997b) compared the generation of toxic combustion products emitted from cladding sandwich panels of various make up in the full-scale room/ corner test (19) with two different bench-scale apparatus. The bench-scale tube furnace test, DIN 53436 (24) and the Cone calorimeter (3) were used to predict the toxicity levels in the full-scale. In the programme three steel faced cladding sandwich panels were tested. The panels were filled with foam cores of

(i) polyurethane,
(ii) mineral wool,
(iii) expanded polystyrene (EPS).

The Cone calorimeter bench-scale test was conducted at fluxes of 35 to 50kW/m² in a horizontal position using a protective frame covering the edges of the specimen. The sandwich sections were 50mm deep and tested both with and without the covering steel facing. It is not mentioned in further detail how the toxic gases could be collected from the edge protected bench-scale sample when the steel face covered the core. Furthermore other work on layered sandwich units with synthetic cores (Grenier, Dembsey et al., 1998) indicated the distorting effect of edge burning and illustrated the major impact on the measured panel performance characteristics. These disturbing edge effects were not reported in the tests undertaken by Babrauskas (1997b). This seems to be at odds as the steel clad sandwich panels are likely to be prone to vigorous edge burning due to the highly conducting steel faces, which are expected to heat the edges of the sample rapidly.

The influence of edge effects is discussed further in section B).

Babrauskas (1997b) reported that the room structures built with the synthetic foam panels (i) and (iii) above, showed widespread collapse and a peak heat release of 6000kW. The panels were observed to warp, buckle and collapse in all parts of the test room. The mineral wool cored panel produced a much reduced peak heat release rate of 200kW and only localised damage with no collapse was observed. Total smoke production was highest in the room clad by the steel/ polystyrene panel and the author noted the major danger to fire fighters in premises built with synthetic cored sandwich panels. The correlation of toxicity levels in bench-and full-scale room tests was poor. The toxicity levels established from bench-scale tests differed significantly from the full-scale results. This outcome for smoke and toxicity gas correlation was in accordance with findings from Heskestad and Hovde (1999), who also analysed Cone calorimeter data with respect to smoke generation and compared the results to the findings for the materials in the full-scale room/ corner scenario. For cladding sandwich panels Babrauskas estimated that the utility of bench-scale fire test methods would remain limited and that it was also unlikely that a bench-scale data based fire model would emerge in near future. The authors reasoned that the mechanical aspects of sandwich panel failure were too complex to derive a successful, meaningful bench-scale testing approach.

B) Sandwich panels used in the ship and aeronautical industry

The modelling of the fire behaviour of composite materials, mainly used in the ship and aeronautical industry, has been pursued more widely. Grenier, Dembsey at al. (1997)
established the fire characteristics of cored composite materials for use in commercial vessels and passenger boats. The study used the bench-scale Cone calorimeter (3) test to classify the sandwich panel performance and establish input parameters for the fire growth model by Quintiere (1993). The specimens were composite sandwich units, where the outer skin consisted of glass-fibre reinforced plastics (GRP). The materials used within the walls were of superior quality and as such not comparable to the markedly cheaper, less sophisticated products used in building panels. The overall thickness of the sandwich units was about 30mm. All samples were tested in the Cone calorimeter in a horizontal position under 20 to 90kW/m² irradiance level. A protective edge frame was used in all tests. The authors noted that the prevention of edge burning and premature involvement of the core was of paramount importance since a real fire scenario would not expose the edges of the sample. The use of an edge frame was deemed satisfactory to prevent the edge burning effects. The time to ignition and heat release rates were analysed (Grenier and Janssens, 1997) and were seen to be affected by the non-homogenous nature of the cored composite materials. The degradation of the layered unit created irregular effects such as the delamination of the GRP skin, melting of the foam core and edge effects, which would not be encountered with solid materials. Building on these findings Dembsey and Jacoby (2000) evaluated common ignition models, such as Mikkola Wichmann (1989) and Delichatsios (1997), for use with cored composites. They found that the ignition models could not accurately predict time to ignition and should only be used with caution when used as input to fire growth simulations. The fire growth model was sensitive to input parameters such as the surface temperature at ignition and especially the heat of gasification ($L$), which needed to be carefully analysed from the bench-scale test results.

3.4.3 Correlation of reaction-to-fire behaviour and structural performance

Work in modelling materials behaviour in large-scale or even natural fires has been focussed on specialized areas, such as wall and ceiling linings, composite panels used in ships and furniture (Babrauskas and Grayson, 1992). With the exception of the composite panel units used in ship hulls, none of these material models require the incorporation of loadbearing behaviour for the correlation with larger scale performance. Composite sandwich units used in passenger vessels are often employed as loadbearing units and need to remain stable during the course of the fire. Dao and Asaro (1999) have investigated the structural degradation of the outer exposed composite skin of the sandwich units (similar to the materials used in Grenier, Dembsey at al., 1997) and found the temperatures in the centre and at the back of the skin layer to be the most influential parameters to its structural performance. Mouritz and Gardiner (2002) used the Cone calorimeter to correlate the degradation of the facings materials with the reduction in ultimate load carrying capacity of the entire unit.
A) Modelling of the compression properties of fire-damaged polymer sandwich composites- Mouritz and Gardiner (2002)

Mouritz and Gardiner (2002) examined the effect of fire-induced damage on the compression properties of polymer sandwich composites used in small ships, such as yachts and powerboats. In the test programme one side of the composite panels was subjected to 10 to 100kW/m² radiant heat flux. Subsequent to the exposure the damaged units were subjected to a vertical load test, which determined the ultimate vertical load capacity of the panels in damaged state. In this combined fire and ambient testing programme the fire induced damage to sandwich units was correlated with the reduction in vertical load ability. As such the work represented a succession to the work by Grenier, Dembsey et al. (1997).

Although sandwich panels exhibit various failure modes under compressive loading, including core shear failure, the investigation concentrated on the occurrence of global, or Euler buckling. Since the determination of the ultimate buckling load through the traditional Euler formula does not account for the additional shear deformation developed in shear weak structures, such as sandwich panels, Mouritz and Gardiner employed the critical buckling load formula as given by Allen (1969), see also Chapter 5, Section 5.6. To account for the fire damage in the sandwich unit the shear stiffness of the sandwich unit for varying exposed face and core thicknesses was computed using the theoretical relationship (Allen, 1969). Three levels of fire damage were considered

(i) fire damage to one face skin,
(ii) fire damage to one face skin and core,
(iii) fire damage occurs through exposed skin and core and partway through unexposed face skin.

The theoretical compressive loads derived using the model were in good agreement with the measured loads. The analysis showed that substantial thermal decomposition had occurred prior to the ignition of the samples. The derived vertical load capacity for the Phenolic cored sandwich sections was conservative when compared with the measured capacities. This was thought to be due to the assumption that the charred region of the Phenolic core had no residual stiffness. Considering the rigidity and coherence of the char this assumption was likely to be conservative. Although the length of the tested sandwich unit was only 150mm and the length-to-width ratio of the samples was approximately 2, the work does not discuss or give detailed information about the buckling length assumed and the influence of sample geometry. Furthermore the model assumes that the core layer still undergoes shear deformation after one face skin has been removed. This assumption is discussed further and compared to findings of this investigation in Chapter 6. The work does not explain in detail how the 150mm long and 74mm wide specimens were tested in the standard Cone calorimeter set-up, which in the standard set-up tests samples of 50mm². The positioning of specimens underneath the heater and the spread of damage from the area under irradiation to the outer edges of the samples is not discussed in further detail. The influence of intact panel areas at the edges of sample on the ultimate failure loads is not considered.
3.4.4 Fire resistance use of scaling

The prediction of the fire resistance performance of building units in full-scale tests through smaller-scale furnace tests is not as common in fire research as the reaction-to-fire test correlations presented earlier. Researchers have used small-scale furnace tests to validate heat transfer models such as Mehaffey, Cuerrier et al. (1994). White (1982) conducted a small-scale test programme to assess the heat transmission performance of calcium silicate board. White (1982) concluded that the small-scale investigation provided valid information for comparing the performance of different board material options but was unlikely to yield the final rating for panels on a full-scale assembly. Work by Sultan (1996) challenged these findings when he modelled the fire resistance of loadbearing and non-loadbearing lightweight gypsum clad timber and cold-formed steel wall assemblies through a small-scale furnace fire resistance test. Whilst the reaction-to-fire test presented earlier successfully modelled the full-scale performance of the systems by accounting for characteristic failure mechanisms encountered during the test, the fire resistance model of Sultan did not incorporate these correlations but was based on a correlation of test times.

A) Correlation of small-scale and full-scale fire resistance tests for lightweight steel wall assemblies (Sultan, 1996)

Sultan (1996) correlated small-scale and full-scale fire resistance furnace tests on lightweight frame gypsum board wall assemblies. The work compared test failure times of 14 full-scale and 14 small-scale assemblies. The lightweight frames consisted of steel or wood studs. In each scale four tests were conducted with 90mm insulation in the wall cavity, namely glass fibre, cellulosic fibre, and two tests with mineral wool fibre insulation. Five full-scale tests were vertically loaded the remaining nine tests were tested without load. The failure criteria given in CAN/ULC-S101-M89 were adopted. Of the five loaded wall assemblies four failed through structural failure. One loaded and the remaining nine unloaded assemblies exhibited insulation failure. The full-scale wall assemblies were about 3.0m high by 3.7m wide, whereas the small-scale wall assemblies were 914mm by 914mm and un-loaded. The results were correlated by comparing the fire resistance ratings of identical wall sections in small- and full-scale. Using linear regression Sultan determined a correlation function to predict the full-scale fire resistance rating (FRR) of an unloaded wall from the identical small-scale test assembly; the results for loaded and un-loaded panels are shown drawing 3-1 and 3-2.

The correlations presented by Sultan (1996) are of limited value to this investigation, as the interrelation of the various tests' failure and boundary conditions is not incorporated. The link between the wall performances in the various scales is only valid for these specific test assemblies when subjected to identical test conditions. The lack of in-depth investigation of the reasons for the performance differences in intermediate- and full-scale, reduces the work to merely providing a statistical link between the tests scales rather than giving an understanding to the underlying wall failure phenomena in the various tests.
3.5 Analytical models for lightweight building systems

3.5.1 Introduction: Modelling the performance of structures in fire

The empirical fire resistance requirements for a building units are normally satisfied by undertaking a full-scale fire resistance tests or by the use of 'deemed to satisfy' data based on such tests. Developments in the understanding of the response of structures to fire have not progressed uniformly for all forms of construction. The design of building elements in fire and the fire reaction of traditional, common building products are comprehensively covered in literature (Lie, 1972), (Marchant, 1972), (Malhotra, 1982), (Shields and Silcock, 1987). Lie (1972), provides a conclusive summary of thermal strength and properties of building materials at elevated temperatures covering common building materials such as concrete and its aggregates, steel and wood. Although he also
discusses the behaviour of structures exposed to fire, the most practical review of building structures in fire and their design is given in Malhotra (1982).

The prediction of the loadbearing behaviour of structures exposed to fire is nowadays often calculated using commercially available computer packages. Common to all models is the use of Finite Element Method (FEM) combined with heat transfer algorithms, which compute the temperature profile in the exposed structure (Holman, 1990). The established temperature profile within a structure linked to a material degradation model determines the reduction in loadbearing capacities and ultimately the expected failure time. These FE models are useful and proven for traditional building materials such as steel and concrete in standard structural boundary conditions. However for novel, layered building structures, such as timber frame walls, the failure mechanisms of the various constituent materials and their interrelations are not yet fully established.

3.5.2 Models for lightweight loadbearing and non-loadbearing cold-formed steel walls

The structural fire behaviour of cold-formed steel walls was investigated by various researchers (Gerlich, 1995), (Gerlich, Collier et al., 1996), (Gerlich, Jones et al., 2001), (Alfawakhri, Sultan et al., 1999), (Alfawakhri and Sultan, 2000a, b), (Klippstein, 1978; Klippstein, 1980). The test programmes investigated the fire performance of traditional interior wall construction, in which the cold-formed steel studs were clad with sheet material linings, most commonly paper-faced gypsum plasterboard but also fire resistant gypsum cores reinforced with glass fibres. The stud shape investigated was predominantly the “C” section usually with stiffening lips (Alfawakhri, Sultan et al. 1999). Research for the American Iron and Steel Institute (AISI) published by Klippstein (1978 and 1980) is the most extensive work on loadbearing cold-formed steel studding. Based on this extensive test programme the inherent degradation properties of cold-formed steel exposed to high temperatures were investigated and have been used in modelling work since.

Gerlich, Collier et al. (1996) reported the results of three fire resistance tests on loadbearing cold-formed steel stud walls. A more detailed description of the test and data analysis can be found in (Gerlich, 1995). One test was undertaken to a modified time-temperature regime to determine the effect of severity of fire exposure on the obtained fire resistance rating. The tests were well instrumented and provided detailed information about temperature development and deflection rates of the walls during the tests. The temperature gradient within the studding units caused thermal deformations, which could be predicted with good accuracy. A FE heat transfer model was used to predict the temperature-time histories within the studs, which was reasonably accurate. Refinement of the model was needed to simulate significantly hotter conditions than simulated in furnace fire tests. The failure mode of the steel studs was governed by buckling of the compression flange on the ambient side of the assembly. Perforations within the web of the section reduce the elastic buckling resistance (Kesti and Davies, 1999). Walls with low axial loads were predicted to perform better in the fire tests than in actual fires.
because of frictional restraints and the redistribution of load in the furnace set-up. A simple graphical design method was proposed which gave conservative predictions, considered satisfactory for design purposes.

Kodur, Sultan et al. (1999) reported the results of loaded fire resistance tests on insulated lightweight steel framed assemblies, conducted at the National Research Council Canada in collaboration with industrial partners. Temperature development at different depths within the assembly and deflections were recorded. The temperature and stiffness performance of the steel studded assemblies were believed to be sensitive to the test arrangements, which necessitated an accurate and comprehensive account of test parameter and observations. The temperature rise in the studs was most severe for the glass fibre insulation and slowest for cellulose fibre insulation. Sultan (1995) documented the effect of insulation in the wall cavity on fire resistance of non-loadbearing light frame steel assemblies in further detail. The glass fibre insulation did not affect the fire resistance rating of the walls, whereas cellulose slightly reduced and rock fibre insulation markedly increased the fire resistance rating. Generally benefit of insulation seems to be maximized if the insulation was installed tightly between the stud sections. Alfawakhir and Sultan (2000a) and Alfawakhir, Sultan et.al., (2000b) investigated the effect of insulation, resilient channels and stud spacing on the fire resistance of light-frame steel assemblies. Their report covered detailed description of the tested wall assembly, instrumentation and loading conditions. Their analytical model predicted with reasonable accuracy the temperatures within the assemblies as long as the gypsum layer remained in place. Gerlich, Jones et al. (2001) modelled the heat transfer into the wall assembly with the commercially available computer programme TASEF, whereas Sultan, 1996 and Sultan, Alfawakhir et al. (2001) developed a one-dimensional numerical procedure to predict the temperature distribution across the steel stud in insulated and non-insulated wall cavities. The models by Gerlich, Jones et al., 2001 and Alfawakhir, Sultan et al., (2000) in particular show promising correlation with full-scale fire test results. The models will be described in further detail later. Local buckling failure in ambient LSF walls is predominant. The use of plasterboard lining increases the failure loads of cold formed steel walls and close fixing enhances the failure load (Telue and Mahendran, 1999).

3.5.3 Timber frame walls

Clancy and Young (1995) modelled the structural behaviour of lightweight timber frame walls exposed to a range of fire exposures. The heat transfer through the assembly was modelled by a two-dimensional computer programme, WALL2D (Takeda, 1999), (Mehaffey, Cuerrier et al., 1994). In their model the sacrificial gypsum layer was assumed to fall off when the maximum temperature of 700°C was reached on the unexposed face of the plasterboard. The temperature was related to the melting temperature of the glass fibres in the board. The theoretical fire resistance model for the timber frame wall, (FIREFRAME) approximated the composite action between sheathing board and frame based on linear slip stiffness between the two members and accounted for the changes in compression, tension and elastic modulus of the wood. The fasteners, connecting gypsum
board and studs, were assumed to behave as shear springs. The model was validated through testing. The deflections predicted were within 20% of the test result, the predicted test time of 38 minutes correlated reasonably well with the test result of 42 minutes. The factors influencing the fire resistance of lightweight timber frame walls were established using the model above (Clancy, 2002). The main parameters were established to be

(i) the loading of the wall: with increasing load the time to failure reduced,
(ii) wall height: time to failure decreased with wall height,
(iii) stud spacing: the wider the studs were spaced the shorter the time to collapse, due to heat transfer through the cavity,
(iv) initial crookedness: fire resistance was generally inversely related to initial crookedness,
(v) Young’s modulus and strength of wood: the higher the E-modulus/ strength at ambient temperature the higher the fire resistance of the assembly,
(vi) thickness of gypsum board: time to failure increased approximately proportionally with the total thickness of plasterboard. If one thick board was used instead of two thin boards the time to failure increased due to the non-linear response in the heat transfer,
(vii) change in enthalpy: the lower the enthalpy the larger the the temperature gradients within the assembly and the faster heat transfer.

Richardson (2001a) summarises some general comments and visual observations on fire resistance tests on lightweight timber frame walls. In accordance with findings in cold formed steel framed walls (Gerlich, 1996), (Klippstein, 1980) the use of load spreading beams in fire resistance testing causes large proportions of the test load to be transferred to edge studs, which potentially delays structural failure of the walls. As observed by Alfawakhiri, Sultan et al. (2000a) for cold formed steel wall assemblies, the sacrificial plasterboard considerably impacts on the fire resistance of the assembly. The ability of the board to stay in place is a major factor influencing the protection. The loss of the paper face of the plasterboard, due to burning, and the dehydration of the plaster are the main reason for the weakening of the lining and its falling away from the studs. The effect of plasterboard falling off is not as pronounced in walls as in ceilings, as the board leans against the studs. Joint compound and filler were seen to fall away from the wall, prior to major failures in the board. This caused premature exposure of board edges.

3.6 Summary of reviewed literature

Although the structural sandwich wall building method was first introduced in the 1950’s information about the structural and fire behaviour of these composite walls is scarce. Whilst the related cladding sandwich panel construction has been researched in detail, structural sandwich walls for use in buildings have only received minor attention. The Forest Products Laboratory undertook the most comprehensive work on structural sandwich walls for use in housing. Their detailed fire investigation of structural sandwich walls is extremely valuable to the work as it exhibits the failure behaviour and poor fire performance of the walls, which structurally collapsed as early as 3 minutes into the standard fire resistance test. In comparison to the timber frame these results are especially
worrying as in the studded system the fire resistance ranges from 20 to 35 minutes and their failure is related to insulation and integrity breach and not structural collapse. This type of failure makes the fire design for timber frame wall sections, using BS 5268 Section 4.2, unsuitable for structural insulated wall systems and underlines the need for a detailed investigation into factors influencing sandwich wall design in fire. Similar to the fire resistance performance, the generation of smoke and toxic gases is more severe in rooms built with sandwich wall panels and the wall reactions to elevated temperatures can only be dampened by sacrificial plasterboard cladding. The FPL report on the building wall panels gives a good general background to sandwich wall behaviour and therefore the fire investigation and some of the structural test results are used for further comparison in Chapter 5. With respect to the structural racking performance of structural sandwich wall assemblies, the lack of detailed information has been overcome by surveying the performance of timber frame wall panels under in-plane loading. The comprehensive research on the structural performance of timber frame walls gives an excellent understanding of their behaviour and indicates the suitability of the work for the development to structural sandwich walls. Although timber frame and sandwich walls transfer horizontal and vertical loads differently, their racking behaviour is similar, relying on the common veneer materials. The effect of vertical load, wall length, openings and other factors have been presented. The racking performance of light gauge steel walls has also highlighted the effect of increased uplift restraint. This may become critical to structural sandwich wall performance, where the loss of the internal frame is likely to increase the importance of the holding down of the panel. This is addressed in further detail in Chapter 7.

To further explore the factors influencing the fire performance of structural sandwich walls, information on the performance of the various materials forming the sandwich panel has been reviewed. The review of literature into the fire performance of the single panel materials, i.e. boards and core, has established that the board materials used in the sandwich panel construction have not been researched in detail whilst information on the fire behaviour of synthetic foams is more detailed. The foamed core materials can be fire enhanced using specifically designed additives, but this was reported to alter their structural performance. Synthetic foams, which are the most commonly used core materials in structural sandwich walls, exhibit a special type of combustion, termed smouldering combustion. The characteristic fire reaction of the foam layer in the layered sandwich wall configuration is compared to these findings and in Chapter 5 and 6 similarities and differences are explored. Despite the fact the board materials used in sandwich walls have not been assessed in detail, plasterboard is closely related to the mineral based board products and has been investigated by a number of studies. The overriding temperature reaction in the gypsum based board layer is related to the shrinkage of the gypsum core, which induces cracking and alters its heat transfer characteristics- a failure mode likely to be governing performance of the mineral based board layers used in structural sandwich walls. This is assessed in detail through the multi-scale testing regime, especially the modified bench-scale set-up, see Chapter 5 and 6.
Whilst the modelling of fire performance (reaction-to-fire) of materials through a small scale test regime is the most versatile, time and cost saving approach, the success of such methods is limited and concentrated on non-structural wall and ceiling linings. The surveyed literature on the use of indicative small-scale testing for use with structural sandwich walls, suggests that the use of bench-scale test method for predicting the fire performance of sandwich walls is deemed to remain limited due to the unpredictable, geometry influenced burning behaviour of such elements. Edge effects encountered when testing sandwich walls in bench-scale set-ups further impair meaningful correlation to larger scale performance. Although the use of bench-scale reaction-to-fire test have been used successfully to predict the full-scale performance of non-structural wall and ceiling linings, the fire resistance performance of structural and non-structural building products remains to be assessed through full-scale furnace testing. The little information available on the correlation of small- and full-scale fire resistance ratings was judged to not provide a valid basis for application in the scope of this research since boundary conditions and loading arrangements were not considered in detail. However, the analytical work on the fire resistance behaviour of lightweight steel and timber frame walls does give a valuable insight into the performance of lightweight wall systems. Extensive full-scale testing of the systems and the development of dedicated analytical fire resistance models establish the influencing factors to the fire resistance performance of lightweight wall systems. These are of value to this investigation and the findings from these studies will be re-used in Chapter 6 where the similarities of timber and steel lightweight wall systems and structural sandwich wall assemblies are further explored.

The literature survey has clearly shown that the relative newness of the sandwich panel building construction particularly out of the US has meant little direct information is available. Fortunately, much information is available for parallel systems and here learning may be applicable to structural sandwich walls. The fire work indicates there could be enormous benefit from using small-scale testing and there is a need for a methodology to link such a range of tests.
Chapter 4 Methods and Materials

4.1 Outline and Introduction to the Methods and Materials used in the study

This chapter presents in detail the choice of methods, the materials and panel configurations employed in this study. The experimental methods describe fire tests at varying scales and structural testing at ambient temperature. The introduction overviews the framework of the different testing methods adopted, reasoning the use of the multi-scale fire testing methodology and illustrating the need for supplementary structural testing at ambient temperature. Whilst the fire test procedures needed to be adopted to enable the evaluation of the fire performance of the multi-layered wall units, the structural tests including bending, vertical loading and racking, were undertaken using standard procedures. Overall the racking programme was independent of the fire assessment and was conducted with its own set of objectives. However, the ultimate aim to optimise sandwich wall panel design at ambient and fire temperatures reunites both programmes and closely interlinks the assessment of the factors influencing structural and fire related panel performance. The sandwich wall components in need of detailed assessment in the fire and ambient test programmes are listed at the end of the introduction section. The choice of materials for the various wall components is discussed in the subsequent section, giving a detailed account of the relevant material properties and introducing their generic background and traditional use. Secondary panel components such as jointing and internal studding are also detailed for future reference.

The methods used in the fire programme are described prior to the structural and racking test methods and presented in increasing specimen size. To enable the collection of research relevant wall performance data, the fire test methods were adopted from standard procedures and the changes are given in detail. The structural test methods are based on standard procedures and detailed separately for horizontal, vertical and racking loading tests. At the end of each test section the panel systems and units assessed in the respective test programme are detailed.

4.1.1 Background to developed fire testing methodology

When structural sandwich assemblies are employed as loadbearing external and internal walls they are required to compartmentalize an accidental room fire by maintaining their loadbearing ability and containing smoke and toxic gases. At the same time the wall structure heats up, degrades and deforms and all of these reactions weaken the resilience of the unit. The detailed knowledge of the various degradation processes ultimately determines whether a structure will satisfy the fire resistance requirement. Drawing 4-1 details the main steps required to establishing the fire behaviour of a structure (adopted from Malhotra, 1982). The flow chart extracts the relevant parts of the generic analytical routine required for computing the fire resistance of loadbearing assemblies. The generic
model constitutes of various subroutines, all of which are as yet unsuitable for the type of layered structure assessed within the study.

Heat exposure of building element

Temperature build-up in structure

Physical damage in structure

- Reduction in strength of materials
- Reduction in section size
- Thermally induced stresses

Transient loss in loadbearing ability/ stiffness and ultimately failure

Drawing 4-1: Knowledge establishing the fire resistance of a loadbearing wall unit

The heat exposure of a structure in a room fire can be of varying magnitude. In laboratory conditions this exposure is simulated in a furnace by a standard temperature/time heating curve, though this regime is regarded as unrealistic. It is standard procedure to assume this generalized heat exposure for construction classification purposes. The heat exposure affects a structure and two reactions need to be considered

(i) the rate of heat transferred from the surface into the interior of the construction,

(ii) the physical damage inflicted by the heat.

The rate of heat infiltration into the panel determines temperature development through the depth of the unit and initiates its gradual degradation. Similar to the temperature development, the physical damage sustained at certain temperature levels is material dependent. Whilst these characteristic fire reactions are well researched for traditional building materials, such as concrete, steel or wood, the response of novel building elements and especially layered construction units is largely unknown. Both, the changes in material strength properties, due to the heating, and the extent of physical damage, which causes the reduction in section size, reduce the load carrying capacity of the structure. The collapse criterion is determined by comparing the residual loadbearing ability and the applied design or service load.

4.1.2 Establishing the fire behaviour of structural sandwich walls

In a structural sandwich wall the generic material groups employed to forming the layered unit are particularly diverse since high density, fire resilient boards are used in conjunction with a low density, generally combustible core. Furthermore the boards and core need to be rigidly bonded to establish the composite loadbearing capacity of the wall, which makes the glue bond between the various panel layers structural.
The fire resistance of structural elements is traditionally determined through a full-scale fire test as presented in Chapter 1. However, the comprehensiveness of the wall construction required for this test regime impedes on the evaluation of failure influencing factors. Furthermore the furnace arrangement also hinders the close observation of the units so that the effect of the fire exposure and associated failure patterns cannot be analysed in detail. This makes this test regime especially unsuited for multi-layered building units. To overcome the shortcomings of the full-scale fire test a “custom-tailored” fire testing methodology was developed from fire testing in three scales, combining traditional reaction- and resistance-to fire testing:

(i) material and reaction-to-fire bench-scale testing,
(ii) resistance-to-fire intermediate-scale testing,
(iii) a proving full-scale fire resistance test.

In addition structural tests at ambient temperatures are undertaken to complement the fire test findings and modelling the loadbearing ability of panels at varying failure stages throughout the transient heating regime.

4.1.3 Adopted fire testing framework

Drawing 4-2 illustrates the framework of tests adopted for this investigation. By using a range of sample sizes and heating arrangements a test scheme could be developed which allowed the successive evaluation and in-depth investigation of the variety of factors influencing the wall design of structural sandwich panels. A special sample design was developed which assessed the different panel design issues, allowing for simple panel set-ups, i.e. plain panels, but also more complex panel construction such as jointed and internally linked test specimens.

Test regimes

| Bench- and intermediate-scale findings |
| Structural testing at ambient temperature |
| Full-scale fire resistance test |

Output

Material related wall reactions
- Influence of degradation of panel layers (transient stiffness loss)

System related wall reactions
- Steady state tests assessing effect of damage in panel layers

Time and type of failure
- Insulation, Integrity, Loadbearing

Fire resistance of full-scale wall

Drawing 4-2: Framework of multi-scale testing to establish the fire resistance behaviour of structural sandwich wall assemblies
Drawings 4-3 and 4-4 detail the performance information gathered in each test regime. In the intermediate-scale test set-up the test specimen was subjected to the identical furnace heating regime as in a full-scale fire resistance test. Although the test conditions are less severe than in the full-scale test regime, since the specimens remain unloaded, the temperature build-up in the multi-layered panel is modelled in a similar mode to the full-scale wall test.

**Principal failure mechanisms in the wall**

Related to degradation of
the different *Panel Components*

![Diagram](image)

**Establishes the effect of material degradation on the fire behaviour of full-scale wall**

Drawing 4-3: Intermediate- and bench-scale fire testing

The vertical loading was purposely omitted since the application of vertical loads onto a 1m² panel would by no means cause identical stress levels in the specimen. The use of vertical loading in the reduced-scale test can only be indicative of large-scale performance if the panel dimensions, i.e. overall thickness of the unit’s layers, and material specifications are accordingly reduced. Scaling applications in fire research have become increasingly popular but are still out of the ordinary assessment methods, and subject to basic research (Quintiere, 1989). However, the determination of the load/deflection behaviour is crucial to correlate internal temperature levels to the change in the unit’s stiffness, i.e. its change in loadbearing ability. The intermediate-scale test was therefore designed to incorporate a load application mechanism, described in further detail later. By monitoring the temperature development through the depth of the sandwich unit with numerous thermocouples at various locations, the temperature reaction within the layered unit could be correlated to the characteristic transient reduction in loadbearing ability. But similar to the full-scale test the intermediate test set-up hinders the close observations of the exposed face of the test object. Once the sample is clamped to the furnace there is no close observation ward to monitor the failure modes taking place during the test and smoke, falling debris and local flaming emitted from the wall can obstruct view. This hampers the correlation of failure modes and temperature levels within the sample, especially in the later stages of the test. By the time the sample is removed from the furnace the destruction is advanced and especially with the materials used in sandwich panels, practically no combustible core residuals are left after the
exposure conditions of a 1 hour test. This is a major downside of fire resistance test methods as the determination of failure modes and behaviour at varying stages of exposure is essential to good fire design.

Therefore the bench-scale Cone calorimeter was introduced in the testing scheme, facilitating the observation of failure behaviour and the correlation of temperature and degradation within the layered sandwich unit. The set-up of the test specimen, further reduced to one tenth of the size of intermediate-scale sample, uses a localised irradiance created by an electrical heater. This test set-up allows for the variation in test times and thus enables a step-by-step evaluation of temperature-time-failure behaviour. It was also realised that the evaluation of materials could be undertaken in greater detail, as replicates could be tested at low cost, thereby reducing the number of intermediate scale tests required. The bench-scale tool adopted from a standard procedure, traditionally employed to determine the reaction-to-fire behaviour of cladding materials. The tool has as such never been used in conjunction with fire resistance testing and the classical Cone calorimeter and its application areas are described in further detail later in this Chapter.

An altered, as such new method, has been developed from the standard Cone by this work, which moves away from the classical use and therefore also dispenses some of its characteristic features such as the heat release and smoke measurements in favour of measurements beneficial to the investigation. The classical test set-up was also used in the programme but to reduced extent, for reasons further explained in the section.

The structural testing at ambient temperature (Drawing 4-4) complements the findings of the fire investigation. Through the steady state structural tests the transient stiffness loss is further analysed. The effect of component loss on the performance of the wall was assessed for different wall systems, i.e. sandwich walls with and without internal studding. This testing provided vital information on the system performance and test samples were designed according to the findings from the fire testing.

The structural testing at ambient temperature (Drawing 4-4) complements the findings of the fire investigation. Through the steady state structural tests the transient stiffness loss is further analysed. The effect of component loss on the performance of the wall was assessed for different wall systems, i.e. sandwich walls with and without internal studding. This testing provided vital information on the system performance and test samples were designed according to the findings from the fire testing.

Input from intermediate- and bench-scale test:
- Temperature related component failure and transient stiffness loss

Structural tests at ambient temperature:
- Steady state tests establishing the effect of component degradation on loadbearing performance
- Load effect of loss in composite action
- Influence of internal studding

Assessment of the fire performance of various sandwich wall systems/ configurations

Drawing 4-4: Structural testing at ambient temperature
4.1.4 Parametric assessment of panel components

The combination of the findings from the reduced scale fire test programmes and the structural tests at ambient temperatures (Drawing 4-2) models the failure mechanisms of a full-scale wall structure exposed to elevated temperatures. The scheme enables to isolate the various panel components and successively evaluates their contribution to the overall performance of the wall. The main panel components influencing the fire resistance of sandwich walls were regarded to be the materials used as veneer and core, together with connecting elements assembling the single panels into a wall unit. Therefore principal components for investigation were:

(i) the veneer,
(ii) the core,
(iii) the internal studs linking the veneers,
(iv) the connectors forming,
   (a) the horizontal joint
   (b) the vertical joint.

A range of veneer and core materials were assessed in the intermediate-scale test and full advantage was taken from the diversified testing regime. In the bench- and full-scale testing the material choices were narrowed and only a fraction of the initial materials were examined, due to constraints in time and funding.

One of the final applications of this research is its use for product development, where the varying material configurations and replicates can be tested in increased numbers with decreasing scale. The initial indicative and developmental testing can be undertaken in bench-scale, covering a great variety and range of materials and material combinations, evaluating the differences in performances and facilitating the selection of suitable components. The number of material alternatives would be condensed in the intermediate-scale testing regime and further specimen characteristic and features of the full-scale system (such as jointing) could be included in the assessment. A further selection process would then detect the material combinations and system features with good potential and confirm the most suited system for the given application. In a final and minimised full-scale testing scheme the materials and components of the system are confirmed and the full-scale test would then be a merely proving test with high chance of passing the set criteria for the chosen application of the system. In time the creation of a database allows the wider collection of behavioural pattern, refining and increasing the success of the approach described above. In particular the bench-scale test could be used to model fire exposures of varying severity and thereby enable the assessment of building elements in a range of fire scenarios.

4.2 Materials

The materials chosen for the programme as sandwich wall components were the most commonly used materials in this type of construction and representative of sandwich wall systems employed in Europe. It was tried to assess each material group over a range. Whilst this was achievable for the core materials, which were chosen from a lightweight, lower density polyurethane core to a high density Phenolic core and an inert mineral wool.
core, the choice of suitable board materials was less varied. This is due to the fact that structural board materials used in the building industry are of principally two types

(i) mineral based boards, such cement bonded particle boards,
(ii) wood based boards, such as OSB (Oriented Strand Board).

The choice of board materials as structural outer wall layers was therefore concentrated to these board types ranging from 8mm thickness cement based boards to 10 and 12mm gypsum and calcium silicate based boards and the wood based OSB at 11mm thickness. The use of jointing materials was also closely aligned with the most commonly used systems throughout the industry. Amongst the standard wooden rails and cold-formed steel channels, a more innovative glass-reinforced Phenolic protrusion was tested. These were prototype units and formed from single components rather than being a single protrusion of constant thickness. Similar to the jointing alternatives, the material and installation for the internal veneer linking sections were chosen from the existing wall System 1 (described in Chapter 2), where the internal board linking sections are used by default.

4.2.1 Board materials

The board materials were chosen on the basis of fire and structural performance. Mineral based boards made up the largest proportion of the board choices since wood based board, although common in North America due to their versatility and robust structural performance, have the propensity to promote ignition and flame spread. This necessitates the use of additional plasterboard in dwelling applications, which is often regarded as disadvantage. For economical reasons the additional protection of the panel with plasterboard lining is to be minimised as it is time and labour intensive to install. The ideal situation would be the omission of the sacrificial lining in dwelling applications so that the most suitable wall face layer would need to be incombustible and non-flame spreading. Within the building industry only mineral based, i.e. cement or gypsum bonded, boards combine both fire performance and structural ability.

A further vital issue in the design of the prefabricated wall is the weight of the units. Since the core is extremely lightweight, the faces must be chosen so as to not excessively increase the overall weight of the panel. In choosing a board material a sensible balance between fire and structural performance and overall weight has to be met. In that respect panels clad with wood based building boards and plasterboard lining are profitable since they combine low density and excellent structural performance and rely for the fire performance on the plasterboard. All the board options have been explored and in the programme five board materials were used as facing layers in the sandwich walls. All five materials were standard board building products used in the construction industry. The application of the boards ranged from cladding boards used in timber frame, such as Pyrok and OSB, to soffit boards such as Sasmox and Cape.
4.2.1.1 Pyrok

Pyrok is a structural board material commonly used in the timber frame modular build industry. It is a cement-bonded wood particleboard available in thicknesses from 6 to 18mm and manufactured by Cape Calsil. Throughout the test programme the board was used at 8mm thickness. Since the board is extremely durable and impact resistant it is also typically used in flooring, shaft wall systems and internal and external cladding. Some structural and thermal material properties are given in Table 4-1 below. The board can be fixed to the underlying construction by both screws and nails. Whilst the latter connection method is more common, due to its extensive use in timber frame applications, screw fixings have become increasingly used especially in conjunction with lightweight cold-formed steel frame walls. Throughout the test programme the board was connected to horizontal and vertical jointing members by screws.

Table 4-1: Material properties Pyrok (Manufacturer’s values)

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density (kg/m³)</td>
<td>1250</td>
</tr>
<tr>
<td>MoE (N/mm²)</td>
<td>5000</td>
</tr>
<tr>
<td>Compressive strength (N/mm²)</td>
<td>15</td>
</tr>
<tr>
<td>Tensile (</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity k (W/mK)</td>
<td>0.23</td>
</tr>
<tr>
<td>Surface spread of flame (fire propagation)</td>
<td>Class 0</td>
</tr>
</tbody>
</table>

4.2.1.2 Sasmox

Sasmox is a board manufactured in Finland and due to its superior fire performance and durability used in places of public assembly such as schools, sport and youth facilities. The manufacturing process is conducted in half- dry process, which uses only minimal amount of water, increasing the strength and fire properties. Sasmox is a gypsum-bonded wood fibreboard available in thicknesses from 8 to 22mm and traded in the UK by McLoughlin Wood. In the programme a 10mm board was used. Its typical applications are as internal and external cladding and also as soffit board, but also for fire protection purposes. Further material properties are given in Table 4-2 below. Recommended to be used in combination with screws.

Table 4-2: Material properties Sasmox (Manufacturer’s values)

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density (kg/m³)</td>
<td>1200</td>
</tr>
<tr>
<td>MoE (N/mm²)</td>
<td>4000</td>
</tr>
<tr>
<td>Compressive strength (N/mm²)</td>
<td>9.5</td>
</tr>
<tr>
<td>Tensile (</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity k (W/mK)</td>
<td>0.24</td>
</tr>
<tr>
<td>Surface spread of flame (fire propagation)</td>
<td>Class 0</td>
</tr>
</tbody>
</table>
4.2.1.3 Fels

Fels is another gypsum-bonded paper fibreboard available in thicknesses from 10 to 18mm and manufactured by Fermacell. In the programme a 10 and 12mm board was used. Its typical applications are as internal cladding and sheathing board on timber frame walls. Its surface spread of flame classification is Class I(Class 0: Building Regulations, Approved Document B) and further material properties are given in Table 4-3 below. In timber frame walls the board is generally fixed using nails. In the programme the board was only employed in the plain sandwich wall samples and has not been carried forward into the structural testing so that alternative fixings were not decided. The board was excluded from further testing based on findings related to the boards’ shear capacity at fixings (see below).

Table 4-3: Material properties Fels (Manufacturer’s values)

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density (kg/m³)</td>
<td>1000-1250</td>
</tr>
<tr>
<td>MoE (N/mm²)</td>
<td>3500</td>
</tr>
<tr>
<td>Compressive strength (N/mm²)</td>
<td>10</td>
</tr>
<tr>
<td>Tensile (</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity k (W/mK)</td>
<td>0.36</td>
</tr>
<tr>
<td>Surface spread of flame (fire propagation)</td>
<td>Class 0</td>
</tr>
</tbody>
</table>

All above structural boards are manufactured by mixing and pressing the cement/ gypsum and wood/ paper fibres on large steel plates and subsequently curing the mixture until a durable bond has been established between both components. Since all boards are used in semi-structural applications their structural, fire and thermal capacities are practically identical. But since the programme established the design criteria for both the fire and the racking performance of the walls, the board materials needed to be performing well in both areas. To establish the performance of the boards when cladding a racking wall a small-scale comparative board edge tear out performance test was conducted. The test regime was undertaken to assess the tear out capacity of the board along the fixings. The tests were small-scale indicative testing for racking walls (Griffiths, 1987) and the set-up sketched in drawing 4-5.

Board material fixed to wood rail

Drawing 4-5: Test set-up to assess tear out capacity of structural board materials used as sandwich wall facings
Whilst the boards were similar in their strength and elastic properties, their resistance to nail movement was different. The Pyrok and Sasmox performed similar whereas the softer Fels was less stiff and less strong as seen in Drawing 4-6.

![Drawing 4-6: Tear out capacity of three mineral based structural board materials](image)

4.2.1.4 Cape
The final mineral based board used in the programme is commonly employed in the building industry as purely cladding, hence non-structural board. Although the appearance, density and toughness of the board is comparable to the structural boards presented before and was anticipated to perform similar structurally, it was primarily chosen due to its highly rated fire resistance. Cape is calcium-silicate polypropylene fibreboard available in thicknesses of 10mm manufactured by Eternit. Its typical applications are in wall cladding/ rendering and infill panels; when used with polymeric renders the board is also suitable for external walls and rainscreen cladding. One layer of the board is quoted to provide a 60 minutes fire resistance when applied to timber or steel frames, a rating well above the shielding performance of the board materials presented before. This superior protection performance is thought to be related to the internal polypropylene fibres, which strengthen the board in fire conditions. The fibres are included to reduce and relieve the expansion stresses within the materials and thereby improve the shielding capacity and coherence of the board when exposed to heat. Further material properties are given in Table 4-4 below. As the board is regarded as non-structural the manufacturer does not provide material strength properties for the board. The board is generally attached to the sub-structure using screws.
Table 4-4: Material properties Cape Blueclad (Manufacturer's values)

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density (kg/m²)</td>
<td>1200</td>
</tr>
<tr>
<td>MoE (N/mm²)</td>
<td>-</td>
</tr>
<tr>
<td>Compressive strength (N/mm²)</td>
<td>-</td>
</tr>
<tr>
<td>Tensile (</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity k (W/mK)</td>
<td>0.19</td>
</tr>
<tr>
<td>Surface spread of flame (fire propagation)</td>
<td>Class 0</td>
</tr>
</tbody>
</table>

4.2.1.5 OSB

Oriented Strand Board (OSB) is an engineered, mat-formed panel product made of strands, flakes or wafers sliced from small diameter round wood logs and bonded with an exterior-type binder under heat and pressure. The wood based OSB is available in thicknesses of 6 to 25mm. In this investigation an OSB class 3 (for use as loadbearing board in humid conditions) of 11mm thickness was used. Its typical applications are in sheathing of timber frame or other lightweight wall constructions but also as roof decking. Further material properties are given in Table 4-5 below. The low weight and superior strength and stiffness performance, especially in tension, provides enhanced ductility to the board, which makes it popular for timber frame applications, where the board can be fixed to underlying frame at short edge distances without an impairment in loadbearing performance. This is especially beneficial on the central stud fixings in timber frame construction, where two board layers need to be fixed to one stud. Since OSB is commonly used in timber frame construction it is generally fixed using nails. In the programme the conventional nail fixings and glued connections were employed.

Table 4-5: Material properties OSB1

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density (kg/m³)</td>
<td>640-660</td>
</tr>
<tr>
<td>MoE (N/mm³)</td>
<td>3500</td>
</tr>
<tr>
<td>Compressive strength (N/mm³)</td>
<td>15</td>
</tr>
<tr>
<td>Tensile (</td>
<td></td>
</tr>
<tr>
<td>Thermal conductivity k (W/mK)</td>
<td>-</td>
</tr>
<tr>
<td>Surface spread of flame (fire propagation)</td>
<td>Class 3</td>
</tr>
</tbody>
</table>

4.2.2 Core materials

Unlike the board materials, the core substrate can be chosen from variety of types, all of which provide adequate structural performance. The components used to produce the foam layer can be selected to meet specific structural and fire performance levels. Improving the fire performance of foam can have adverse effects on its structural performance since the additives and fillers can cause the foam structure to become brittle. Although the foam properties can be manipulated and designed to meet the specific needs

1 mean values taken from EN 12369-1:2001
of the end application, the changes and improvements are linked to the costs. In a commercial wall system the cost for panel component must be balanced and specifically designed, high-density foams tend to be more expensive and less viable in application. For comparison one high-density Phenolic foam has been included in the study to assess the improvement in panel performance achievable by foam design. All synthetic foams used in the sandwich wall panels were self-adhesive, with exception of the EPS and mineral wool core, which needed to be attached to the outer faces by glue. Self-adhesion of the foam is preferred in continuously manufactured wall panels and an adequately strong glue line is of major importance to maximize the structural performance of the sandwich wall. Similarly important is the shear capacity of the core, which will be discussed in further detail in later Chapters. Despite the fact that the shear capacity of the core is influential to the loadbearing performance of wall panels, this material property is rarely quoted in product literature. In any way the provision of such material properties are only of limited value since Davies, 1987a points out that the shear capacity can vary through the depth of the panel and in its area. For all synthetic cores curing periods were observed. The toxic emissions of foams in fire conditions are a major threat to the life safety of building occupants but are currently not determined in fire resistance testing. The methods and findings presented in this work will enable a detailed study into these important life safety issues of product performance and should be addressed in future work.

The fire performance of a synthetic core can be influenced by its composition. Polyurethane based foams contain the repeating unit -NH-COO in their chemical composition. These foams are manufactured from the condensation of polyisocyanates (e.g. diisocyanate) and polyols (see drawing 4-7). The most frequently occurring chemical structure in these foams forms straight chains (aliphatic structures).

\[
\text{Polyol} + \text{Diisocyanate} \rightarrow \text{Polyurethane}
\]

\[
\begin{array}{c}
\text{OH-R-OH} + \text{OCN-R'-NCO} \\
\hspace{1cm} \downarrow \\
\hspace{1cm} \text{Polyurethane} \\
\hspace{1cm} \text{C-N-R-N-C-O-R-O} \\
\hspace{1cm} \text{O H H O}
\end{array}
\]

Drawing 4-7: Polyurethane addition reaction

To enhance the fire resistance of a polymeric synthetic core the chemical structure is altered from straight chains to thermally more stable ring structures or benzene rings (aromatic structures) in a process called trimerisation. Examples for fire-optimised foams are polyisocyanurate (PIR) (see drawing 4-8) or Phenolic foams. The percentage of isocyanurate rings in the foam is indicative of its resistance to high temperatures and is given by the isocyanate index.
Isocyanate — Trimerisation — Isocyanurate

Drawing 4-8: Isocyanurate formation

Mineral wool is the most commonly used inorganic core material and manufactured from molten stone fibres layered to a low-density monolithic material in a sophisticated manufacturing process. The wool is not self-adhesive and needs to be connected to the facings by glue.

EPS is common in the American structural insulated panel systems, where it is attached to the face layers by wood glue (generally polyurethane-based glue). EPS foams are generally less dense, which has a major impact on the weight of the panels. Table 4-6 summarises the densities and glue line properties of the core materials investigated in this study.

Table 4-6: Core materials investigated

<table>
<thead>
<tr>
<th>Material</th>
<th>Density $kg/m^3$</th>
<th>Glue-line</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>Polyurethane (PUR)</td>
<td>~ 45</td>
<td>Self-adhesive</td>
<td></td>
</tr>
<tr>
<td>Polyisocyanurate (PIR)</td>
<td>~ 45</td>
<td>Self-adhesive</td>
<td>Isocyanate index 200-300</td>
</tr>
<tr>
<td>Phenolic</td>
<td>~ 90</td>
<td>Self-adhesive</td>
<td>Specifically designed for fire resistance</td>
</tr>
<tr>
<td>Mineral wool</td>
<td>~ 45</td>
<td>Applied PUR glue</td>
<td>Slabstock</td>
</tr>
<tr>
<td>Expanded Polystyrene (EPS)</td>
<td>25-30</td>
<td>Wood glue (PUR based)</td>
<td>Slabstock</td>
</tr>
</tbody>
</table>

4.2.3 Secondary panel components

4.2.3.1 Internal sections

Some structural sandwich panels include internal sections, linking the veneers in order to prevent the delamination of the exposed face in case of fire. In the panels used for this study the internal units were made from folded zinc coated cold-formed steel sheets of 0.7 to 1.0mm thickness and spaced normally at 600mm but up to 1200mm centres, between the panels' veneers. The internal connections act as full height studs and were folded to
two shapes of I (I) and sigma (Σ) cross section, the latter being included in panels manufactured from 2001 onwards. They are fixed to the veneers by screws at 200mm centres prior to the foaming process. Oval holes stamped into the web of the studs allow the foam to expand freely between the boards and encase the stud units in the finished panel. More details about the characteristic material properties of cold-formed steel sections are given in BS EN 10147: 2000 (25).

Shape and sheet thickness of the studs are of minor importance to the overall design, since a wide range of shapes and materials can be used to efficiently tie the outer veneers. The internal studs are independent from the panel design, as they are not required to react compositely with the panel assembly. They are not connected in any way with the horizontal links, are non-loadbearing and do not interact with any other panel component at ambient temperature so that their design is reduced to two factors

(i) if the studs are placed inside the panel, they need to be permeable to the expanding foam mixture, i.e. contain holes
(ii) the internal units should be as light as possible to not add to the overall weight of the final panel.

Hence the most efficient solution meets the above criteria, is manufactured cheaply and inserted into the panel quickly.

4.2.3.2 Jointing

The intermittent vertical joint used in System 1 (Figure 2-5) consists of a hook closure mechanism. The hook is manufactured from 3mm mild steel built into a high impact styrene casing and connects into a end profile with bar to take the hook. The horizontal joints in this system use 80mm deep cold-formed steel channels “U” sections, capping the panel ends, with predrilled holes at the sides at 200mm centres and 7mm diameter holes in the base, also at 200mm centres (shown in figure 2-10). The “U” section is manufactured from cold-formed steel sheets of 0.9mm thickness bent into shape through an automated production process. The horizontal joints are fixed to the panel by self-tapping screws at 150mm centres.

In System 2 the horizontal rail units are constructed from a fire resistant, heat insulating glass-reinforced Phenolic (GRP) resin renown for its fire resilience and low smoke/ toxic gas emissions. The horizontal joint, manufactured by Fiberline (©), is internal and recessed behind the veneers. Phenolic resins are widely used in the aviation and automotive industry but also in offshore construction, and can be protruded in various shapes. The “top hat” section shown in figure 4-1(a) and the vertical joint shown in figure 4-1(b) used in the intermediate-scale panels were manufactured from standard Fiberline(©) components whereas in full production it would be a simple extrusion of constant wall thickness.

In half of the intermediate-scale panels and in the full-scale fire test panel the GRP vertical joint was replaced by a contiguous vertical steel joint (as shown in figure 2-4(d)). The joint was one-leafed in the intermediate scale panels and two-leafed for increased...
stability in the full-scale wall panels, shown in figure 2-4(d-1) and 2-4(d-2) respectively. The jointing sections formed permanent shutters hence did not contain openings and were manufactured from 1mm thick cold-rolled steel. The joint parts were fixed to the panel edges prior to the foaming process by screws at 150mm centres. The profiles were designed to enable the mating of male and female mould upon assembly. In the one-leaved joint the female section was removed from the panel edge after foaming thus only one profile type was required.

4.2.3.3 Sacrificial lining
In the full-scale fire test and in some of the intermediate- and bench-scale tests plasterboard was used as sacrificial lining, protecting the panel's exposed face. The employed board was standard plasterboard, also termed wallboard, of 15mm thickness and was obtained from the plasterboard range of Knauf. Plasterboard is generally used as cladding board in timber frame and lightweight steel wall assemblies as it combines excellent fire protection with efficient surface finishing. Its paper surface and tapered edges provide a good surface base for the finishing trades. The protection time it provides is dependent from the composition of the board and ranges from 20 to 40 minutes. The latter can only be achieved with glass-reinforced boards. The wallboard, not containing any glass fibres in the gypsum core, is rated a class 0 board.

Table 4-7: Plasterboard standard (manufactured by Knauf), not reinforced at 15mm

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Density (kg/m³)</td>
<td>700-720</td>
</tr>
<tr>
<td>Thermal conductivity k (W/mK)</td>
<td>0.16</td>
</tr>
<tr>
<td>Surface spread of flame</td>
<td>Class 0</td>
</tr>
</tbody>
</table>

4.3 Fire Testing Methods
4.3.1 Bench-scale testing: The Cone calorimeter in the Standard Configuration
4.3.1.1 Introduction
When during the late 1970's and early 1980's it was realised that knowledge about the burning behaviour and toxicity of materials was crucial to understanding fire hazard, the Cone calorimeter was developed to address these issues as then unaccounted for in other fire tests. The cone calorimeter is a bench-scale reaction-to-fire test and was first introduced in 1982, with the corresponding ISO 5660 standard (3) published in final form in 1993. The success of the Cone calorimeter since it's first introduction is related to the comprehensiveness of the test regime, which uses a small test specimen to evaluate material reactions contributing to the fire development in a larger scale fire compartment. Typical building fires are mostly initiated by careless handling of hot substances, such as cigarettes or matches (Goddard, 1995) and if enough oxygen is provided the fire will spread from the ignition source to nearby combustible surfaces and will then steadily grow until the whole compartment is involved. The performance and reactions of the
involved materials are of major influence and fire growth and therefore fire hazard is influenced by their heat release (Babrauskas and Peacock, 1992). The bench-scale Cone calorimeter enables to determine the heat release rate from burning materials and also establishes the ignitability of materials, their effective heat of combustion, mass loss rate, smoke, soot and toxic gases generation.

4.3.1.2 Cone calorimeter operating principle
The principle of measurement in the Cone calorimeter test is based on oxygen consumption calorimetry, which is recognized as the most accurate and practical technique for determining heat release rates from experimental fires. Oxygen consumption calorimetry is based on the fact that a large number of liquids, gases and fuels release a roughly constant net amount of heat per unit mass of oxygen consumed. A schematic view of a standard cone calorimeter is shown in figure 4-2. The test specimen (100 x 100 x 50mm), placed in a specimen holder, is rested on a load cell and positioned underneath the heater. The combustion gases produced by the sample are collected in an exhaust duct above the conical shaped heater. The heater coil contains a hole\(^2\) in its centre through which all generated combustion products are sucked into the duct. The ignition source is located about 5mm above the sample surface. A spark plug arrangement mounted on a moveable carrier arm is used as external ignition and does not impose any additional, localised heat flux.

A) Radiant heater
The Cone calorimeter derives its name from the conical shape of the heater. The radiant heater is powered electrically and generates a uniform irradiance over the entire exposed face of the specimen. The heater, consisting of coiled wire elements, is controlled by temperature rather than by power intake to maintain a constant irradiance level. The heater is designed to achieve irradiance levels of up to 100kW/m\(^2\) but test heat fluxes are generally below that level.

B) Choice of heat flux and ignition
The selection of irradiance level is dependent on the application of the test results. Theoretical bounds for possible heat flux levels can be derived from real fires. A room fire burning near its maximum can generate gas temperatures over 1000°C and produces an irradiance level to walls and contents of about 150kW/m\(^2\), according to the Stefan-Boltzmann equation

\[ \dot{q}^* = \sigma \varepsilon (T_f^4 - T_0^4) \]

where

- \( \dot{q}^* \) Intensity of radiant energy per unit area (W or kW)
- \( \sigma \) Stefan-Boltzmann constant = 5.667x10\(^{-8}\) W/m\(^2\)K\(^4\)
- \( \varepsilon \) Emissivity, i.e. the efficiency of the surface as a radiator \( \varepsilon \rightarrow 1 \) for larger flames
- \( T_f \) Temperature of the gas (°C)

\(^2\) About 80mm in diameter
$T_0 \quad \text{Ambient temperature, contribution insignificant since } T_0 << T_f$ 

The theoretical value for possible highest heat flux however overestimates the maximum irradiance level found in building fires relevant to this study. The peak heat fluxes for common ignition sources, such as cigarettes, matches, paper but also small burners and wood cribs range from 20 to 50kW/m$^2$. 

The majority of tests in the bench-scale test programme were undertaken at a heat flux of 50kW/m$^2$, which is recognized as being the most suited irradiance level for larger scale correlations. Furthermore this irradiance level was found to be corresponding with the ISO 834 (27) and BS EN 1363-1 (8) standard temperature/ time furnace temperature curve used in fire resistance testing during the first 30-40 minutes (Tsantaridis and Östman, 1998), (Tsantaridis, Östman et al., 1999), (Silcock and Shields 1995). However, for some products such as light polymeric foams, the heat flux should be chosen so as to prolong the time to ignition. The heat flux should be chosen in order to promote ignition after about 1 minute and a heat flux of about 25kW/m$^2$ was found adequate for the foams assessed in this study. An external ignition source was used in all tests, as auto-ignition of the materials did not occur. In all tests in the testing scheme an airflow rate of 241/s was used.

C) Test Specimens 

The size of test specimen is restricted to 100mm$^2$ and an overall depth of 50mm. Generally the specimen thickness should be as close as possible to actual thickness of the commercial product. In order to ensure purely radiative heating of the sample the recommended specimen orientation is horizontal with the specimen face exposed to the heat flux. In this orientation the convective contribution to the overall heat transfer was found to be immeasurably small. The vertical orientation is not recommended for use in standard testing, even for products that would, in full-scale, be installed vertically, such as wall lining materials, as a boundary layer of hot gases develop, which will add varying, uncontrollable convective flux over the height of the specimen. The specimen is wrapped into a single sheet of aluminium foil and backed by a low-density$^3$ ceramic fibre blanket before being placed into a steel panel of 111mm$^2$. The single sheet aluminium foil covers the sides and the bottom of the specimen to avoid flowing out of liquidised decomposition products during the test. In the bench-scale test environment heat will also be transferred at the edges of the samples. Especially problematic are specimens, whom incline to ignite along the outside edges and subsequently burn vigorously. This form of behaviour can be found with certain wood materials and composites, for which a protective steel edge frame should be used. The edge frame eliminates edge burning as it closes off the sample perimeter with a 3mm lip around the edge of the specimen face. It also prevents samples from edge warping and curling which can also cause disproportional burning (Babrauskas, Twilley et al., 1992). An edge frame was used in all tests undertaken for this study.

$^3$ Nominal ~65kg/m$^3$
D) Measurement, Data logging
The raw data from the test are processed and converted with a special computer package called Fire Data Management System. The software package was specifically developed to simplify exchange of fire test data amongst testing laboratories and professional users for fire prediction models or product development.

4.3.1.3 Materials/ System tested in the standard Cone calorimeter set-up
Table 4-8 overviews the standard Cone calorimeter test programme. The tests for this programme were undertaken with the Cone calorimeter of the University of Ulster (Jordanstown) at FireSert in Carrickfergus (now Newtonabbey). In an initial programme four board materials, namely, Pyrok, Sasmox, Fels and OSB, two core materials, PUR and EPS and two Systems, Pyrok-PUR and OSB-EPS were tested. The fissuring of the fire face veneer is one of the predominant failure reactions and was modelled in the standard set-up samples by including a manufactured crack along the centreline of the surface. The test programme concentrated on the determination of heat and mass loss rates of structural wall panels. In order to develop the test for the research, one test series incorporated temperature measurements from within the samples, by feeding thermocouples through the predrilled holes at the back of the closed specimen pan. Since the standard set-up raises the specimen holder on a socket, which interlocks with the load cell, recording the mass loss of the sample, the thermocouples interfered with load cell measurements. The recorded mass loss, which computes the heat release rate, was faulty and not regarded as representative when thermocouples were present in the sample.

4.3.2 Bench-scale testing: Modified Cone test set-up
4.3.2.1 Introduction
Initial tests with the Cone calorimeter established the principal combustion behaviour of sandwich units. At the same time the unambiguous classification of failure mechanisms was impeded by the layered structure of the sandwich units and the dissimilarity between the materials forming the layers. In order to reliably link failure mechanisms to material performance, temperature measurements from within the composite unit were required.

A variety of material options had been tested in the first stage of the intermediate-scale furnace-testing programme prior to the Cone calorimeter scheme. Here the monitoring of temperature-time histories had established performance levels amongst the different material combinations. But since only one replicate was tested for each material grouping, the confidence margins were accordingly small. The possibility of monitoring the temperature-time reaction for a range of material options with a bench-scale apparatus was noted as a potentially cheaper and time efficient alternative to the material assessment in a small-scale furnace. If successful the bench-scale approach would have several advantages:

i) influencing factors to the panel's temperature reaction could be established (by isolating, altering panel components)
ii) several replicates of each material combination could be tested, allowing the reliability of material temperature responses to be evaluated

iii) due to the cost effectiveness of the approach a greater variety of materials and ranges (i.e. density, thickness and so forth) could be assessed

iv) bench-scale size specimens could be produced without the need for large-scale manufacturing equipment, which would simplify the assessment of products still in development.

But more importantly the bench-scale set-up would allow the temperature reaction of the samples to be monitored in conjunction with the predominant failure pattern, which could not be achieved in the furnace based test regimes.

4.3.2.2 Set-up

The apparatus chosen for the adopted bench-scale testing was a reduced Cone calorimeter set-up. From the standard apparatus only the cone heater, heat flux meter, the spark ignitor, exhaust duct and the temperature controlling equipment were retained, see figure 4-3. In reaction-to-fire testing a similarly reduced cone set-up is sometimes employed to monitor the mass loss of a sample when exposed to constant heat flux. In this apparatus, commonly known as mass loss cone, the test sample, 100 x 100 x 50mm (length x width x height) is placed on the load cell in the standard specimen holder. The facility modified for this testing was kindly provided by Dr Grayson of Fire Testing Technology.

A) Specimen size

Although composite panels had been tested successfully in the Cone before (Grenier, 1997) the fire behaviour of the sandwich units investigated in this study caused substantial difficulties when tested in the standard Cone set-up. On heating, the combustion gases produced by the decomposing core material escaped around the edges of the sample and poured out of the edge frame, leading to ignition and flaming along the side of the specimen (Drawing 4-9). As a consequence the core material was consumed before failure through penetration of the veneer material could occur. However, in the larger scale tests where the edges can be assumed to be sealed off from direct heat, the failure mechanism is cracking of the fire-faced veneer followed by consumption of the core.

![Drawing 4-9: Ignition around the edges of the specimen and not through penetration of board](image-url)
A solution to the problem was found through enlarging the edge perimeter of the specimen to 200mm², while leaving the area under irradiation at 100mm² (Drawing, 4-10). With this change the unheated area of the specimen provided insulation to the core edges of the sample and the heat exposure of the core material occurred through the cracked board material as observed in the larger scale tests. The unheated area of the sample was additionally covered with aluminium foil, which further reduced the heating of the sample edges. As the sample thickness was no longer restricted to 50mm by the specimen holder the whole sandwich unit (80-110mm thick) was subjected to the heat flux. In order to be able to observe the changes to the specimen surface during the test a 40mm distance between bottom of cone heater and specimen surface was provided. The thermocouples, which needed to be connected to the data logging equipment and the change in specimen size to 200mm², necessitated an altered specimen holding device. To enable the temperature measurements in the altered set-up on the enlarged specimen a tripod with circular top plate and adjustable leg height was constructed to rest the specimen underneath the cone heater. The tripod position was aligned with the centre of the heater and positioning points were marked. This allowed for the tripod to be removed from underneath the heater for installation of test specimen/ thermocouples and simple repositioning shortly before the start of the test. To guarantee uniform heating conditions prior to each test the legs of the tripod were levelled to ensure the specimen was horizontal.

B) Heat flux and temperature measurement

Five Type K thermocouples were positioned at three different depths within the specimen: behind the exposed veneer, at mid-depth of core and at the interface of core and unexposed veneer. The majority of the samples were tested at a heat flux level of 50kW/m². Several samples were tested at 35 and 70kW/m². The temperature readings were recorded and processed by an ORION data logger and PC computer system.

![Diagram](image)

**Drawing 4-10: Enlarging the edge perimeter of sample with unheated area providing insulation to the core edges**

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4 Nickel/ chromium TC, measurable temperature range -200 to 1100°C, accuracy ± 2.5°C
4.3.2.3 Materials/ System tested with modified Cone Set-up

In the modified cone entire sandwich systems, but also single components, were tested. The testing concentrated on Sasmox-PIR/ PUR, Pyrok-PIR/ PUR and Fels/ PIR systems; some OSB-EPS units were also tested as shown in tables 4-9 to 4-11. Two foams were assessed in greater detail, namely PUR and PIR.

The various jointing materials used in the experimental panel system, hence the glass-reinforced Phenolic used as top and bottom joint and as vertical link between panels and the vertical steel joint were tested in reduced size clad by varying board types and thicknesses. The effect of 15mm plasterboard cladding protecting the exposed face of the panels was also evaluated in the bench-scale set-up.

4.3.3 Furnace fire tests

4.3.3.1 Introduction

In contrast to the bench-scale testing, where the samples are exposed to radiation emitted from an electrical heater, the intermediate- and full-scale fire resistance tests expose the specimen to heat generated by burners in a furnace. In both scales the specimen is clamped to a furnace opening and the exposure of the specimen to a standard heating curve provides a means of quantifying the ability of the element to withstand exposure to high temperatures. The procedure enables criteria to be set by which the fire containment (integrity) and the thermal transmittance functions (insulation) and loadbearing ability of the units can be evaluated. In the intermediate-scale the test the specimen is un-loaded so that the full-scale fire test is the more onerous test in that it allows vertical loading of the wall. The exposure of the specimen in the furnace is standardized and characterized by:

(i) the heat build-up in and the transfer from the furnace to the specimen,
(ii) the pressure in the burning space,
(iii) the loading of the element (for loadbearing full-scale tests),
(iv) the restraint and set-up of the panel in the test frame.

The two main factors influencing on the test environment are the temperature development in the furnace and the pressure conditions along the height of the specimen. The loading of the wall is decided based on the final application of the wall and the restraint conditions are imposed by the furnace arrangements. The temperature in the furnace is controlled by the supply of fuel to the burners. During the test the heat generated within the furnace is transferred to the test object by convection and radiation, although the convective part of the transfer is shown to be relatively small (Malhotra, 1982). The higher the insulation capacity of the furnace walls the higher the temperature within the furnace and the greater the temperature exposure of the specimen. It is the aim to make testing conditions amongst furnaces as similar as possible by ensuring that heating conditions are calibrated to transfer a minimum amount of heat to the specimen. In accredited furnaces the heat exposure of the specimen is so similar, that temperature course of the furnace is sufficient to prescribe the heating conditions of the specimen. The
standard test procedure to BS EN 1363 (8) requires the furnace temperature to be recorded and monitored by a plate thermometer.

In addition to the temperature exposure, the specimen needs to be subjected to representative pressure conditions in the furnace. The pressure conditions adopted in the furnace tests are derived from observations of gas flow at openings of a fire compartment, where there is an outflow of hot gases above a certain level, whereas below this neutral plane cold air is drawn into the enclosure from the outside. This compensation of pressures occurs, as the pressure inside the fire compartment is higher than the outside level. In a BS EN 1363 (8) based test the neutral axis is placed at 500mm from the bottom of the specimen. Above the neutral axis gases flow out and below the neutral axis air is drawn into the furnace. In a furnace tests the pressure conditions cause cold air to be drawn through cracks and openings in the negative pressure zone at the bottom of the panel and smoke and toxic gases to be transported out at the top of the specimen. This is thought representative of a real fire situation and allows integrity and insulation criteria to be adequately monitored. Loading and boundary restraint to the panel are both influential to the loadbearing/stiffness performance of the specimen and are subsequently discussed in detail for each test scheme.

4.3.4 Intermediate-scale furnace testing (BS EN 1364-1: 1125 x1125mm samples)

4.3.4.1 Introduction

The small-scale tests were conducted in accordance with EN 1364-1 (6). In addition to the evaluation of heat transmission the test was enlarged to monitor the reduction in stiffness through an enhanced measurement scheme, which will be explained in further detail. The intermediate-scale tests were carried out at the CERAM research laboratory in Stoke-on-Trent (see also Appendix II).

4.3.4.2 Set-up

A) Method/Furnace

The intermediate-scale specimens were tested in a natural gas fired furnace lined with low-density ceramic fibre and active furnace volume of about 2m³. During the test one side of the panels was subjected to increasing heat flux. The temperature in the furnace was increased using EN 1363-1 (8) temperature-time relationship:

\[ T = 345 \log_{10}(8t + 1) + 20 \]

where

- \( T \) is the required average furnace temperature in °C
- \( t \) is the time from the start of the test in minutes

The furnace temperature is increased by a single burner of about 290 kWh capacity. The flame is baffled to ensure an even distribution of heat. The neutral pressure plane is kept at 500mm from the bottom threshold of the unit and regulated through dampers in the ventilation of the furnace. To monitor the insulation criteria, BS EN 1364 stipulates that
thermocouples are placed on the unexposed face of the panel. From these readings average and maximum temperature reached during the test on the ambient side of the wall are determined. The EN 1364-1 (6) test is normally used to test non-loadbearing full-scale walls. It has been used in the past to test smaller scale units incorporating some of the full-scale wall features. However, due to the reduced scale and limited number of wall components the results can then only be indicative of full-scale performance. The specimen size of 1.125m x 1.125m was chosen to suit the size of furnace available for testing.

B) Installation of specimen

Figure 4-4 shows the furnace and panel arrangement. The test panel is installed into the steel frame screened with one brick block work. The block work adjusts the height of the sample within the steel frame. Around the perimeter of the specimen mineral wool gaskets are inserted which are additionally protected by specialised fire shield board. This is to protect edges of specimen from heat penetration and prevents the bridging of heat. This is especially important in sandwich walls where the premature involvement of the vulnerable internal core from the open sides can markedly distort the performance of the wall section in the test.

C) Enhanced measurements

The EN 1364 (6) procedure was extended to allow more detailed research information to be evaluated. In each test the panels were monitored visually, temperatures were recorded at numerous points within the panel and face deflection measured centrally on the unexposed face. The exposed face of the test specimen was observed through a viewport situated at the back of the furnace. As the test progresses, the smoke being generated from the decomposing wall panel obscures the view so that observation of the exposed face has to be stopped and only the unexposed face remains monitored.

On average fourteen type K thermocouples (TC) were placed at four different depths within the test samples: at the interface of the exposed veneer and core (A), at mid-depth of core (B), at the interface of ambient veneer and the core (C) and at the surface of the unexposed veneer (D). The thermocouple locations as related to panel area were altered depending on the panel configuration tested (see also Appendix 1). This set-up allowed the temperature response of the panels at key locations within the panel to be monitored, and also allowed comparison between material options.

A deflection measurement linked to a horizontal point load applied centrally to the panel on its unexposed (cold) face was introduced as an additional monitoring system to assess the damage to the panel during the heat exposure (Figure 4-5). The load was applied through a lever arm system fixed to the laboratory floor. The load chosen was sufficient to give a measurable deflection without causing excessive deflection or secondary damage to the face veneer when both the fire exposed veneer and the core had been consumed by the fire. The load was applied 15 minutes before commencing the fire test and care had to be taken to adjust the rig to allow the full movement of the force.

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application point. In practice the applied weight was 100kg, which transmitted a 1kN force to the panel. The horizontal load applied was small in relation to the bearing capacity of the panel. Analysis suggested that this force would result in an initial deflection of 1mm increasing to 20mm in the damaged condition. Furthermore, due to the support conditions of the panel within the steel frame the load had to chosen to not push the panel beyond the supports.

The increase in deflection during the fire test indicates a reduction in panel stiffness. Previously, determination of failure of the units was related to the first signs of visible cracking in the exposed veneer as viewed through the furnace window, a condition difficult to determine accurately and objectively. The measurement of the horizontal deflection has been found to give a much more accurate indication of the performance of the unit with time and can also be related to the visible damage and temperature readings recorded. Deflection measurements in the tests were carried out with a LVDT (Linear Variable Displacement Transducer) which had an accuracy of 0.2mm and the load had been previously determined to an accuracy of ±1%.

4.3.4.3 Materials/Systems assessed in intermediate-scale test regime

The programme provides detailed information about the performance levels of four board materials, four core materials and five jointing units. Details of the intermediate-scale test panels are given in Tables 4-1 to 4-7 and 4-12 and also in Griffiths and Bregulla (2002d). In a first set of ten tests the principal influencing parameters of board and core material, but also the effects of internal studding, were evaluated. Two core/board material combinations were carried forward for further testing. The next set of tests then evaluated the jointing options. The panels were manufactured with either Sasmox or Pyrok boards on a PIR core. In all panels a glass reinforced Phenolic (GRP) top hat section was used as the horizontal joint. A steel ‘U’section and a GRP rectangular vertical insert were tested clad by each board material.

Construction tolerances were built into these panels to simulate acceptable worst-case scenarios. Two problems were addressed; the overall tolerance in a wall causing gaps at panel joints and tolerances in recesses to ensure the correct alignment of the inserts can be achieved, be they horizontal or vertical rails. This led to the following problems

(i) there could be a gap between the veneers allowing heat to attack the joint directly

(ii) there could be a vertical gap between the core and the vertical joint dependent on the gap between the panels and vertical recess tolerance. For the steel this would be concentrated in the junction between the steel tongue and the recess in the second panel. For the GRP joint the gap could be shared between the recesses in the two panels or could concentrated in one recess since the GRP section is independent of the panels

(iii) there could be a horizontal gap between the core and the horizontal rail
since the length of the vertical joint should not exceed the height of the core there will be a gap between the underside of the horizontal rail and the top of the vertical connector.

Taken together that meant that there would be a gap in the veneers towards the top of the panel not protected by either the horizontal or the vertical joint. This air gap would go right through the panel and then link directly with horizontal and vertical gaps along the joint boundaries and, importantly, with the void in the centre of the vertical GRP connector. Each of the joints was designed to have an approximate 3mm gap to represent these tolerances. No attempt was made to protect these weaknesses, as could be easily achieved in practice, so as to test a worst-case scenario. The nature of the small-scale test specimens meant that the required “inaccuracies” could be reasonably accurately constructed. In later tests where full-scale panels were used very much greater inaccuracies had to be accommodated due to manufacturing problems. However, in steady production it could be assumed that the accuracies applied in these tests could be achieved. None of the panels tested with joints incorporated the cold formed I sections included in the full-size panels to space the veneers and prevent them delaminating. Normally these sections would be 600mm away from the vertical joint, i.e. outside the small-scale tests panels.

Another jointing option was tested, which represented a reduced scale replicate of System 1 (in chapter 2), tests 15 and 16 in table 4-12. One small-scale test specimen was cut from two standard size panels with veneer linking studding units at 600mm centres. To allow direct comparison to be made with the tests on the GRP/contiguous vertical GRP and steel jointing options, a further small-scale System I (Panablok) panel was manufactured without internal studding units. Both panels were positioned top and bottom in cold-formed steel channels and the two panel halves connected by the airtight Camlock joint, as typical for the system’s configuration. As the specimens were taken from panels manufactured on the standard production line no tolerances or air gaps were present in the
panels. Standard fixings were used to locate the panels in the channels. The top channel was then protected by 15mm standard plasterboard to ensure that the test focussed on the vertical joint. The effect of plasterboard cladding was evaluated in a separate set of four tests. In tests 17 to 20 (Table 4-12) the fire-exposed side of the panel was lined with standard plasterboard; two panels were clad with one layer of 15mm plasterboard and the others with two layers of 15mm board. The results of the small-scale test regime formed the basis for material chosen for the full-scale test panel.

4.3.5 Full-scale fire test to BS EN 1365 on 2.95m long by 2.4m high wall unit

4.3.5.1 Introduction

The full-scale fire test was carried out in accordance with BS EN 1365-1 (7), which differs from EN 1364-1 (6) in that it allows vertical loading of the wall and is consequently a more onerous test. Being a development panel the wall tested was a prototype wall, which incorporated flaws, due to the adopted manufacturing process. The section will describe the test panel and all its weaknesses. The standard method was enhanced by a laser measurement, which was included into the test method to monitor the deflection at various points and numerous temperature recordings taken from various depths and locations within the panel.

4.3.5.2 Set-up

A) Method/ Furnace

The full-scale test was undertaken at the Building Test Centre at East Leake/Loughborough (see also Appendix II). The Building test centre operates a LPG\(^5\) fired furnace of an active volume of about 17m\(^3\), lined with refractory concrete. The furnace temperature closely followed the prescribed EN 1363-1 (8) curve (shown before) using 24 single burners. The furnace pressure is adjusted so that the neutral pressure plane is 500mm above floor level, as prescribed in the Code. Dampers in the exhaust fans control the pressure and temperature levels within the furnace.

B) Installation of specimen

The test specimen was installed in a 3m wide by 4m high steel frame. A plinth of one brick block work was built under the specimen to adjust the frame to the 2.4m panel height. The tested composite wall was 2.4m high x 2.95m wide, constructed from two 1.2m x 2.4m panels and one 0.6m x 2.4m part panel. Mineral wool gaskets were installed at the panel’s sides to allow free movement of the edges. A uniformly distributed load of 25kN/m was applied by five hydraulic jacks through a steel spreader beam positioned on top of the test wall. Furnace and test specimen are shown in figure 4-6. The specimen was loaded 15 minutes prior to the tests and the load was kept constant for the duration of the test. The load applied through the spreader beam enables the even distribution of the load put on by the concentrated single jack loads. This is regarded as representative of the floor loading of a wall in a building. However, this type of load application allows for the shedding of load once one area of the wall is damaged. With the loading arrangement the

\(^5\) Liquefied Petroleum Gas: Mixture of propane and butane (a by-product of the oil refinement industry)
unit can compensate for damage. In wall systems used in conjunction with timber floors, the adoption of a spreader beam, allowing the shedding of load, is not representative and has potential in overestimating the loadbearing capacity of the wall unit.

C) Measurements
A total of 70 thermocouples were distributed at different depths within the wall and at different locations related to the face to monitor behaviour in the panels and at the joints, see Appendix I. Three LVDTs were positioned to measure principal movements in the panel. The first monitored vertical displacement in centre at the top of the panel and the remaining two measured buckling of the wall at mid height; one gauge being positioned in centre and the other at the edge of the wall. Additionally a low accuracy laser distance measurement device was set-up to monitor buckling behaviour at more than thirty points at intervals of two minutes. Of principal interest was the movement of vertical panel joints where readings were taken at the ¼ points of the joints.

4.3.5.3 Test panel
The major panel features of the full-scale wall assembly were chosen as a result of the small-scale testing regime. Limitations imposed by the manufacturing process affected the choice of materials and also the quality of the wall assembly. The test wall is shown in figure 4-7. The veneers were 10mm Sasmox, the core was 70mm of PIR to produce an overall panel thickness of 90mm. The horizontal joint was a built-up Phenolic reinforced top hat section and fitted into recesses cast into the panel during manufacture. The vertical joints consisted of a profiled male section and a female section, which acted as a permanent former when foaming the core of the panel. The custom made male and female profiles enabled an accurate fit and the double thickness of steel gave increased rigidity to the joint. The partition was built with prototype units and the adopted manufacturing process was beset with difficulties. The spacer placed at the panel ends to form the recesses for the horizontal rails was too deep and therefore in the assembled panels an air gap of about 20mm was formed between the rails and the core/steel inserts, which were cut to match the height of the core. Thus neither the studs nor the vertical steel joints abutted with the top or bottom rail. The joints were site fixed with screws at 150mm centres. The veneers were tied internally by S-shaped cold-formed steel studs, which were foamed in and placed centrally in each panel.

The panels were assembled into a 2.95m long wall; two full-length panels were used together with a third section cut from a standard panel, which included the central stud. As a consequence the steel sections stiffened both ends of the wall. The panels were tightly abutted to avoid internal air gaps between the veneers. The fire-exposed face was clad with 15mm standard plasterboard (wallboard) fixed through 15mm x 45mm timber battens, laid vertically on the panel at 600mm centres. All joints were taped and filled and screw heads spotted with joint filler as appropriate.
4.4 Structural tests at ambient temperature

4.4.1 Introduction
The structural tests at ambient temperature cover a range of testing programmes from horizontal bending and vertical compression tests to racking tests. The horizontal and vertical loading tests were contributory tests to the fire investigation whereas the racking programme represented a self-contained testing scheme with its own set of objectives. The horizontal bending tests were undertaken at various spans and specimen widths and also included tests of full size single wall units. The vertical tests were only undertaken on single full-scale units of varying build-up, here no reduced scale investigation was undertaken. Both full-scale tests in bending and compression on the entire wall units stemmed from a programme undertaken at an earlier date. Within the scope of the programme the opportunity was taken to re-evaluate these results and analyse them with respect to the findings of the fire investigation. The overview in this section starts with the horizontal bending investigations, followed by the vertical and finally the racking tests. Each test section will be followed by a summary of panel configurations tested in the respective programme.

4.4.2 Horizontal bending tests

A) Set-up and loading
The specimens were tested in horizontal position in a five range SATEC universal, screw driven testing machine. The panels were loaded at the intersection of the specimen’s centre line and \( \frac{1}{2} \) length. The test speed was deflection controlled at 4 to 5mm per minute. The screw thread load piston was applied through the load cell to a ball seated distribution beam. The deflection was measured with an LVDT to ± 0.05mm accuracy along the centreline of the specimen aligned with the loading point at \( \frac{1}{2} \) length at the back face of the panel. The specimens bore onto hinged supports.

B) Systems chosen for testing
Horizontal bending tests were undertaken on reduced width System 1 panels with 8mm Pyrok facings on a 70mm PUR core. The objective of the work was to evaluate the influence of internal studding on the sandwich panel bending capacity. Since System 1 is the only system, which includes internal units by default and was chosen for testing. This however was not a great limitation since the strength and stiffness characteristics of the board and core materials used throughout the study were similar, so that the findings were thought to be indicative for all panel compositions. In order to assess the influence of internal studding the panels were tested with and without internal cold-formed I studs. Two types of composite cross section were investigated:

i) two identical face veneers of 8mm Pyrok, spaced apart by a 70mm PUR foam
ii) as in i) but with the two face veneers linked by a cold-formed steel I section in addition to the foam bond. The section is foamed in with the PUR core and fixed by self-tapping screws along the outer surfaces of the veneers at nominally 150mm centres along the length of the section.

---

6 20kN, 50kN, 100kN, 200kN and 500kN
Type (i) panels were tested at 100, 200 and 400mm width. Type (ii) panels, with veneer linking studs, were also tested at three widths: 200, 400 and 600mm. Several replicates of each width were tested 1.1m span. Both panel types were also tested at the full-scale length of 2.4m at each width. The screw spacing for the specimens including internal studs was altered and the effect of screws spacing of 100mm, 166mm and 500mm centres on the bending capacity of the unit was compared. This was done for each width and span. Table 4-13 overviews the bending test programme.

Following from findings in the fire tests it was regarded important to evaluate the horizontal bending performance of damaged panel sections. To allow a comparison to and estimation of performance of panel systems without internal units the tests were carried out on sections with and without stud units. So in a separate set of test 200mm wide specimen, with and without internal unit, were tested with one face (the board in tension) removed from the panel. In a second set the delaminated board was reattached to the test specimen with screws along the internal stud unit at 500 and 100mm centres, in order to assess the effectiveness of the section once the glue bond between exposed face and core had been destroyed but the screw fixings still remained in place. Table 4-13 summarises all the horizontal bending tests at reduced scale.

4.4.3 Vertical load tests full-scale tests

4.4.3.1 Introduction

The vertical loading tests on the full-scale single wall panels were undertaken as part of an earlier test programme on System I panels as part of their British Board of Agrément certification. In the scope of this work these results were re-evaluated as the investigation into the fire resistance of the panels raised renewed interest in the structural test data collected at the earlier date. The findings of this work complemented the earlier results, and in combination enabled to enhance the knowledge about panel performance.

A) Set-up and loading regime

A self-reacting test rig was constructed on the strong floor of the laboratory to withstand loads up to 300kN. The rig consisted of two heavy column sections connected to four pre-stressing rods that provided the load transference between the two sections. The panels were mounted horizontal in the rig and about 300mm off the ground to allow a buckle to develop. The capping channels, cut to length were fixed to the panels and at the base bore onto the column section which had been carefully adjusted to have its face normal to the plane of the panel. The top of the panel was seated into a channel section allowing the two concentrated loads to be distributed into the wall unit. The 300kN rams and their load cells were carefully aligned to act in plane of the panels and acted on the channels through ball seatings. Five LVDT measured buckling displacement along the top centre line of the panels and four LVDT indirectly measured shortening of the panels by monitoring movement in the distribution and reaction beams. Each panel was initially loaded in a stiffness cycle, bedding the panel into the rig and monitoring the panel set. In the stiffness cycle the specimens were loaded to 40% of the maximum estimated load.
The estimation of the failure load introduced inaccuracies but since this value was only regarded as approximate this was not of major importance. Following a recovery period of about 10 minutes the panels were loaded to failure. The test speed was deflection controlled at 1mm per minute in the stiffness test and 4 to 5mm per minute when the specimen was loaded to failure.

B) System assessed

As the face loading tests described before the vertical load testing was undertaken on the 1.2m x 2.4m Panablok unit, with 8mm Pyrok facings cladding a 70mm PUR core. A total of 12 panels were tested. All units included I-shaped internal studs at 600mm centres in addition to the bond established by the self-adhesive core. For testing, the single panels were rested in the cold-formed channel section used in the wall system to horizontally link the panels to wall units. The panels were tested in varying composition as seen in table 4-14. Three replicates assessed the vertical load capacity of the units in their original composition when centrally loaded. The second test set evaluated the vertical load capacity of the panels when one face was delaminated from the panel but remained tested with the rest of the panel. In set three, the delaminated board was entirely removed from the wall for the test. In a separate regime three replicated assessed the influence of eccentric loading on the panels' ultimate loadbearing capacity.

Table 4-14: Vertical loading tests

<table>
<thead>
<tr>
<th>Set-up</th>
<th>Test No.</th>
<th>Loading</th>
<th>Replicates</th>
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<td>3</td>
</tr>
<tr>
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<td>3</td>
</tr>
<tr>
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<td>central</td>
<td>3</td>
</tr>
<tr>
<td><img src="loading_diagram.png" alt="Diagram" /></td>
<td>4</td>
<td>eccentric</td>
<td>3</td>
</tr>
</tbody>
</table>

4.4.4 Racking

4.4.4.1 Introduction

The University of Surrey has great expertise in the racking assessment of lightweight building structures, specifically timber frame walls, and from this wide-ranging work has taken a leading role in the development of the UK timber frame design code. The wide-ranging knowledge, although not documented in much detail, due to previous publication, has been implemented and interpreted for sandwich wall behaviour. This expertise has
benefited the investigation into loadbearing sandwich panel walls since the inception of the test work and the interpretation of sandwich wall behaviour could be accomplished in accordance with the original design and Code requirements. This specialty of Surrey allowed to isolate vital design factors in sandwich wall performance, which facilitated the in-depth investigation into the in-plane performance of the walls. The background knowledge also enabled to compare timber frame and sandwich wall racking performance, which was of major importance with respect to the applicability of the Code of Practice to sandwich wall structures. The lack of replicate tests in some areas of the racking work has been balanced against the previous experience to enable authoritative comment to be made. However, in order to allow finalised design figures to be suggested these configurations should be methodically retested for statistical consistency.

A) Set-up, test rig and procedure

The panels were tested in purpose made rig, which enabled the panels to be loaded vertically and horizontally within their planes whilst allowing horizontal racking and upward movement, see figure 4-8. Restraint was provided against lateral deflection. The standard test procedure outlined in BS EN 594: 1996 (15) has been used throughout the programme. A vertical pre-load cycle was performed to ensure the proper seating of the wall unit in the test rig. The racking test procedure consisted of the stabilising cycle, the stiffness cycle and a strength test. Details of each cycle are described in the Code (BS EN 594). Before a full test can be performed an estimation of the panel's maximum racking load is required in order to determine the stabilising load (0.1F_{max \ ext}) and the stiffness cycle test load (0.4F_{max \ ext}). The Code allows an estimated maximum load to be within 20% of the test maximum before results must be checked. The estimated maximum load is allowed to be adjusted as further tests proceed to attain a value close to the test failure load. Thereby a more accurate stiffness result for the panel can be achieved. During the tests the panels were carefully monitored for damage. Racking was continued after failure as indicated by the maximum load to check for recovery of the panel. Damage at this stage is secondary but is used to indicate the weaker areas within the overall construction. Each panel was tested under one vertical load only. Applied loads were measured by pressure gauges for the vertical loads, but supported by one load cell under the leading jack, and by a load cell for the horizontal racking load. Deflection measurements were taken using LVDT at the top front and top rear of the panel for horizontal movement, at the bottom rear for sliding and the bottom front for uplift. Figure 4-8 shows the loading points and measurement locations for the deflection readings for each panel. Since the sandwich walls do not contain solid studs, as encountered in timber frame walls, the horizontal racking loading mechanism had to be adopted to enable the direct loading onto the sheathings and avoiding the compression damage of the internal soft core. A special steel angle was used, which spanned the core and enabled to horizontal loading sledge to apply the load to the outer facings. Another difference between timber frame wall and sandwich wall panels is the application of vertical load at the top of the wall unit. In timber frame wall tests the vertical load is applied at 600mm centres, coinciding with the positions of internal studding of the frame. In sandwich walls, where there are no internal studding units, the vertical load application needs to be defined. In practice the load was
applied evenly at points equally spaced along the top rail. In the experimental panel system (System 2), which was tested at varying lengths, three points were used on the 1.2m panel at 0.6m nominal spacing, five points on the 2.4m panel again at 0.6m nominal spacing and five points on the 3.6m panel with a 0.9m nominal spacing. The total loads for the three panels lengths were 15kN, 25kN and 35kN respectively. The 2.4m long wall units tested for system 1 and 3 were also loaded at five points along the length of the wall.

The bottom rail of the walls was fixed through a sole plate to the rig by using M12 holding down bolts and 50mm x 50mm x 10mm washer. The test walls were bolted to a 90mm wide sole plate. The base rail was ensured to be rigidly fixed to the test rig, such that the measured racking resistance was mainly due to the resistance of the wall unit, i.e. fixity of the boards to the rails, in preventing the differential movement of rails and panel. In sandwich walls the installation of the holding down mechanism is dependent on the bottom rail of the system. Closed bottom rails, such as the GRP joint used in System 2, require to be partially opened to enable the bolts to be fed through to the bottom plate of the joint, as shown in table 4-17. This is much simplified in System 1 or 3 bottom where the bottom rail is open and easily accessible for the bolting down procedure. Furthermore the bottom rail design is vital in that it allows or prevents the rotation of the veneers, especially at the trailing end of the panel, where down throw of the wall unit can be expected. In timber frame the veneers are free to rotate in the plane of the panel, hence no support is provided to the panel sheathings once the fixings start to break and veneers start to move independently from the rail. This however is not necessarily the case in sandwich walls, as can be best appreciated in System 1, see table 4-15. Here the bottom rail caps the panel ends and thereby encases both veneers, so that they are restricted in rotational movement at any stage during the test. This was thought to affect the racking performance of the entire wall unit and the influence of bottom rail design has been assessed in the test programme on System 3 (see also table 4-16). Here the wall unit was tested with two bottom rail widths. One wide base preventing the rotation the veneers and once reduced width where the veneers were free to rotate once the fixings started to fail.

For the testing programme on System 2 (see Chapter 2) varying length and opening configurations were tested, see table 4-17. For the 1.2m and 2.4m long plain walls, separate tests were carried out at zero vertical load and an equivalent of the 5kN/stud load commonly used in timber frame wall tests. The separate tests for the 1.2m and 2.4m plain panels allowed both stiffness and strength to be measured for each vertical load condition. The remaining walls could not be tested fully with and without vertical load due to a restriction of panels available for testing. A full test was carried out at the zero vertical load and an additional two stiffness cycles performed under the vertical load condition. Since these tests diverged from the standard test method the adopted procedure will be described in more detail:

(i) vertical pre-load cycle with 1kN applied at each load point to settle panel in the rig
one stabilising cycle and two cycles to $0.4F_{max \text{ est}}$ at zero vertical load following standard procedures to establish elasticity in panel and define panel set

(iii) gauges re-zeroed followed by one stabilising cycle and two cycles to $0.4F_{max \text{ est}}$ for the vertical load with vertical load applied. As in the standard test the second cycle allows the repeatability of the wall reaction to be assessed and the mean performance to be established.

(iv) gauges re-zeroed with the vertical load removed and the panel racked to failure

By this means, extra information is made available without compromising other parts of the test. However the inability to test to failure under vertical load limits the use that can be made of the results, as strength is likely to govern all design conditions. Experience shows that the zero vertical load condition is more valuable to design hence the full test was carried out for this case.

B) Systems/ panel configurations assessed

The racking work presented in the thesis reviews work on three structural sandwich wall systems as shown in Table 4-15 to 4-17. Standard racking tests on 2.4m x 2.4m plain wall units were undertaken on System 1, described in Chapter 2. Another set of six standard racking tests was undertaken on OSB-EPS, System 3, wall assemblies. Racking performance of panels with varying lengths, 1.2, 2.4 and 3.6m, and openings was assessed on the prototype panel system, System 2. Details of the materials and jointing methods employed for each assembly are described in Chapter 2. Tables 4-15 to 4-17 overview the different test programmes undertaken within the scope of the racking study. The test programme was intended to evaluate sandwich wall system specific racking characteristics with respect to timber frame behaviour. As such it assessed the influence of bottom rail design, veneer materials and their fixings and vertical loading. A further three factors are bound to influence on the racking performance of the three-layered sandwich units namely openings within the wall, wall length and the vertical jointing mechanism.

B) Panel configurations

Openings and Length influence

In timber frame walls openings in a wall unit have a major influence on the wall racking performance. Openings within the wall reduce the stiffness and strength of the shear wall. The effect of openings is aggravated if the opening is long and the connection of the lintol above the opening and the wall is weak. The extensive research work on timber frame walls also showed that the wall area under a window was contributing over proportionally to the racking resistance. Based on the wide ranging experience on timber frame walls the test programme on System 2, assessed in particular the effect of various shaped openings and the influence of length. Although the size of the opening was restricted to two, namely a window and a door opening, the location and layout of the opening within the wall unit was altered, see table 4-17. The wall systems were tested at three lengths: 1.2, 2.4 and 3.6m.
Vertical connection between panels

Another factor investigated in the racking investigation was the type and effectiveness of the vertical joint between single panel units. It is known from vertically loaded timber frame walls that the boards act to greater extent independently as the internal studs are restricted in their uplift. Therefore adequate vertical joints between the sheathing have potential to strengthen the wall unit. In timber frame panels the vertical joint is located where the two 1.2m wide sheathing boards meet on the central stud of the frame and adequate edge distances of the fixings provided this central stud provides additional rigidity to the panel especially when vertical load is applied. The effectiveness of the vertical joint is increased when panels’ top and bottom horizontal connections are overlapping the vertical joint (by at least 600mm). In sandwich panels this strengthening vertical connection cannot be found in every system and the investigation covered the most commonly found range of vertical jointing alternatives. Ranging from the weakest type of vertical joint in system 1 to contiguous joints in System 2 and 3. Despite the fact that the intermittent hook in System 1 is adequately performing perpendicular to its plane and in tension, it is thought to be ineffective in racking walls, since it does not restrict the shear motion within the wall allowing the deformation of the wall comparable to a bead of pearls. These differences amongst vertical jointing systems were assessed within the programme.

4.5 Summary

A wide range of tests have been performed in the scope of this study. The chapter gives a detailed description of the interaction of the tests, the learning and problems encountered with each test scale and presents the final test set-up chosen for the study. This summary is felt to be important especially as non-standard test methods have been employed.

The materials used in the testing panels have been presented and their relative performance level discussed. Whilst the mineral based boards (i.e. Sasmox, Pyrok, Fels, Cape) are very similar in their material composition and strength characteristics, their suitability for use in racking walls varies considerably. This is related to the different material compositions, which can affect on the resistance of the board to in-plane shear resistance. Experience has shown that the ductile wood based OSB board is best suited for cladding racking walls, but its fire characteristics are inferior to the non combustible mineral based boards. In contrast to the non-combustible mineral board surfaces, the wooden surface promotes flame spread and the final wall can only be employed in dwellings in conjunction with plasterboard. This comparison of board characteristics exhibited the importance of knowledge about fire and structural material properties of panel components for optimising the performance of the wall elements. The study intends to supplement further material information on board, core and other panel components in order to optimise the design of structural sandwich walls. The core materials chosen for use in the panels investigated in the study are in principal similar. The majority of cores were self-adhesive synthetic polymers of similar density. The dense Phenolic core used in the intermediate-scale test panels was purposely chosen to enable a range of different
types of core substrates to be assessed. For comparison the inorganic mineral wool core was also included, although the emphasis of the material choice was clearly on synthetic alternatives, due to their advantages in manufacture.

The fire testing regime is subdivided into bench-, intermediate and full-scale testing. Whilst intermediate and bench-scale testing has been specifically adapted to structural sandwich wall testing, the full-scale test remained in much of its original form. As a consequence bench- and intermediate-scale testing was most extensive, whilst the full-scale test regime was minimised. This was in parts due to limited funds but more importantly because of the fact that the test regime was thought to be of limited value to the investigation. The comprehensiveness of the wall assembly, incorporating all wall components, such as jointing and plasterboard, in the full-scale fire test impairs the detailed analysis of the performance influencing panel components. In contrast the smaller scale tests enable the consecutive evaluation of panel components and also allow the testing of replicates of one material combination, assessing the variability of responses in structural sandwich walls. The bench-scale test was developed from the reaction-to-fire bench-scale test tool the Cone calorimeter. The set-up and specimen dimensions needed to be altered to eliminate edge burning effects, which compromised the collection of relevant test information. The introduction of a deflection device into the intermediate scale regime enabled to monitor the stiffness loss so that each test scale provided a range of different characteristic panel performances. To gather detailed research information the number of thermocouples and the monitored panel interfaces have been markedly increased in all three test regimes. The measurement scheme in the full-scale test was decided based on the findings in the smaller scale tests, monitoring all relevant panel areas. Together with a range of structural tests at ambient temperature in which the panels were tested in different destruction stages as established through the fire testing the test, vital information on the fire performance of structural sandwich walls was collected. Whilst the fire behaviour of the walls needed to be analysed by a specifically developed testing method the racking test method, currently used for timber frame wall testing, was seen to be well suited for sandwich panel wall structures. The wide ranging expertise at the University of Surrey with the testing and design of timber frame walls has facilitated the minor changes required to the test for application with structural sandwich walls and valuable research data could be collected and analysed. A range of wall configurations were tested to assess the various influencing factors to the composite walls racking performance; namely length of the wall, openings within the wall, rigidity and continuity of horizontal and vertical joints.

In each test regime a range of material combinations and panel configurations were assessed and the chapter gives detailed information on the various wall tests conducted in this study.
Single shapes connected by phenolic glue

Figure 4-1: GRP jointing sections- assembled from standard rectangular shapes (Dimensions in mm)

Figure 4-2: Standard cone calorimeter set-up (Babrauskas, 1997a)
Table 4-8: Panel configurations tested with standard Cone calorimeter test

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Type</th>
<th>Test regimes</th>
<th>Board</th>
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<td>11</td>
<td>PUR EPS</td>
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<td>8</td>
<td>11</td>
<td>PUR EPS</td>
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<tr>
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<td>Pyrok Sasmox, Fels OSB</td>
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<td>10, 10</td>
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<tr>
<td>Just foam</td>
<td>HRR, MRR</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>PUR EPS</td>
</tr>
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</table>
Setup of altered Cone with controlling equipment and logging system

Close-up on Cone heater and specimen holder

Specimen after ignition
Full-depth specimen under heater during test
Thermocouple are inserted from the back of the sample
Apart from heated perimeter, sample surface is protected by Alufoil

Figure 4-3: Modified bench-scale fire test set-up
Table 4-9: Modified bench-scale Cone testing "Cone Surrey" I

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Type</th>
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<th>Core</th>
<th>C (mm)</th>
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<td>PIR</td>
<td>70</td>
</tr>
<tr>
<td>Specimen</td>
<td>Plain</td>
<td>Pyrok</td>
<td>8</td>
<td>PIR</td>
<td>42</td>
</tr>
<tr>
<td>(50mm)</td>
<td></td>
<td>Sasmox</td>
<td>10</td>
<td>PIR</td>
<td>40</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Fels</td>
<td>12</td>
<td>PIR</td>
<td>38</td>
</tr>
<tr>
<td></td>
<td>Plain +</td>
<td>Pyrok</td>
<td>8</td>
<td>PUR</td>
<td>70</td>
</tr>
<tr>
<td></td>
<td>Plasterboard</td>
<td>Sasmox</td>
<td>10</td>
<td>PUR</td>
<td>70</td>
</tr>
<tr>
<td></td>
<td>(15mm standard)</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Just foam</td>
<td>-</td>
<td>-</td>
<td>PUR</td>
<td>50</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>PIR</td>
<td>50</td>
</tr>
</tbody>
</table>
Table 4-10: Modified bench-scale Cone testing "Cone Surrey" II

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Type</th>
<th>Board</th>
<th>d (mm)</th>
<th>Core</th>
<th>c (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Specimen size 200x200xfull depth (mm)</td>
<td>Plain</td>
<td>Pyrok</td>
<td>8</td>
<td>PUR</td>
<td>70</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Pyrok</td>
<td>8</td>
<td>PIR</td>
<td>70</td>
</tr>
<tr>
<td></td>
<td></td>
<td>OSB</td>
<td>11</td>
<td>EPS</td>
<td>100</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Sasmox</td>
<td>10</td>
<td>PUR</td>
<td>70</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Sasmox</td>
<td>10</td>
<td>PIR</td>
<td>70</td>
</tr>
<tr>
<td></td>
<td>Plain (50mm)</td>
<td>Pyrok</td>
<td>8</td>
<td>PIR</td>
<td>42</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Sasmox</td>
<td>10</td>
<td>PIR</td>
<td>40</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Fels</td>
<td>12</td>
<td>PIR</td>
<td>38</td>
</tr>
<tr>
<td></td>
<td>Plain + Plasterboard (15mm standard)</td>
<td>Pyrok</td>
<td>8</td>
<td>PUR</td>
<td>70</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Sasmox</td>
<td>10</td>
<td>PUR</td>
<td>70</td>
</tr>
</tbody>
</table>
Table 4-11: Modified bench-scale Cone testing "Cone Surrey" III

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Type</th>
<th>Board</th>
<th>d (mm)</th>
<th>Core</th>
<th>C (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Specimen size 200x200xfull depth (mm)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>c-d</td>
<td>Plain 150 mm²</td>
<td>Pyrok Sasmox</td>
<td>8 10</td>
<td>PUR PUR</td>
<td>70 70</td>
</tr>
<tr>
<td>c-d</td>
<td>Plain Break board</td>
<td>Pyrok Sasmox</td>
<td>8 10</td>
<td>PUR PUR</td>
<td>70 70</td>
</tr>
</tbody>
</table>
Figure 4-4: Intermediate-scale fire test arrangement
Small-scale fire test furnace with test rig inside protected area

Supporting steel framework

LVDT- Deflection measurement

Applied load

Load application point

Base of the rig fixed to laboratory floor by holding down bolt

Thermocouples

Applied weights

View on the load installation with the thermocouples in place before start of test

Figure 4-5: Load rig used in intermediate-scale fire test to monitor change in panel stiffness
Table 4-12: Intermediate-scale fire test programme

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Type</th>
<th>Boards</th>
<th>Core</th>
<th>Joints</th>
<th>Plasterboard(^1)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>d (mm)</td>
<td>c (mm)</td>
</tr>
<tr>
<td>Test 1(^2)</td>
<td>A</td>
<td>Cape</td>
<td>Phenolic</td>
<td>10</td>
<td>80</td>
</tr>
<tr>
<td>Test 2</td>
<td>A</td>
<td>Cape</td>
<td>Phenolic</td>
<td>10</td>
<td>80</td>
</tr>
<tr>
<td>Test 3</td>
<td>A</td>
<td>Cape</td>
<td>Min. Wool</td>
<td>10</td>
<td>80</td>
</tr>
<tr>
<td>Test 4</td>
<td>A</td>
<td>Cape</td>
<td>PIR</td>
<td>10</td>
<td>70</td>
</tr>
<tr>
<td>Test 5</td>
<td>B</td>
<td>Sasmox</td>
<td>Phenolic</td>
<td>10</td>
<td>70</td>
</tr>
<tr>
<td>Test 6</td>
<td>B</td>
<td>Pyrok</td>
<td>PUR</td>
<td>8</td>
<td>70</td>
</tr>
<tr>
<td>Test 7</td>
<td>B</td>
<td>Fels</td>
<td>Phenolic</td>
<td>10</td>
<td>70</td>
</tr>
<tr>
<td>Test 8</td>
<td>B</td>
<td>Pyrok</td>
<td>Phenolic</td>
<td>8</td>
<td>70</td>
</tr>
<tr>
<td>Test 9</td>
<td>B</td>
<td>Sasmox</td>
<td>PIR</td>
<td>10</td>
<td>70</td>
</tr>
<tr>
<td>Test 10</td>
<td>B</td>
<td>Pyrok</td>
<td>PIR</td>
<td>8</td>
<td>70</td>
</tr>
<tr>
<td>Test 11</td>
<td>C</td>
<td>Sasmox</td>
<td>PIR</td>
<td>10</td>
<td>70</td>
</tr>
<tr>
<td>Test 12</td>
<td>C</td>
<td>Sasmox</td>
<td>PIR</td>
<td>10</td>
<td>70</td>
</tr>
<tr>
<td>Test 13</td>
<td>C</td>
<td>Pyrok</td>
<td>PIR</td>
<td>8</td>
<td>70</td>
</tr>
<tr>
<td>Test 14</td>
<td>C</td>
<td>Pyrok</td>
<td>PIR</td>
<td>8</td>
<td>70</td>
</tr>
<tr>
<td>Test 15</td>
<td>D</td>
<td>Pyrok</td>
<td>PUR</td>
<td>8</td>
<td>70</td>
</tr>
<tr>
<td>Test 16</td>
<td>D(_s)</td>
<td>Pyrok</td>
<td>PUR</td>
<td>8</td>
<td>70</td>
</tr>
<tr>
<td>Test 17</td>
<td>C+</td>
<td>Pyrok</td>
<td>PIR</td>
<td>8</td>
<td>70</td>
</tr>
<tr>
<td>Test 18</td>
<td>C+</td>
<td>Pyrok</td>
<td>PIR</td>
<td>8</td>
<td>70</td>
</tr>
<tr>
<td>Test 19</td>
<td>C+</td>
<td>Sasmox</td>
<td>PIR</td>
<td>10</td>
<td>70</td>
</tr>
<tr>
<td>Test 20</td>
<td>C+</td>
<td>Sasmox</td>
<td>PIR</td>
<td>10</td>
<td>70</td>
</tr>
</tbody>
</table>

1) Standard wallboard—no glass-fibre reinforcement
2) Tested to BS 476 standard
3) Horizontal rail unit at top and bottom of panel
4) The top u-channel was protected by a 15mm thick strip of standard plasterboard, the rest of the panel was unprotected

**Panel Type**

<table>
<thead>
<tr>
<th>Panel Type</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>Plain panel—no veneer linking studs</td>
</tr>
<tr>
<td>B</td>
<td>Plain panel—veneer linking studs at 600 mm centres</td>
</tr>
<tr>
<td>C</td>
<td>Panel (System 2) incorporating horizontal and vertical joint. Also tested with varying layers of plasterboard (C+)</td>
</tr>
<tr>
<td>C+</td>
<td>Panel (System 1) tested with horizontal and intermittent vertical hook joint. One specimen tested with internal veneer linking studs at 600 mm centres (D(_s)).</td>
</tr>
</tbody>
</table>

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Figure 4-6: Full-scale fire test arrangement, showing furnace, load application and load spreader beam.
2.4 full-scale wall

Section A-A

PIR foam core
Sismon faces
Fixings at 150 mm c/s

Steel section

25 kN/m applied through spreader beam

Section B-B

Manufacturing problem: Air gap between foam/steel inserts and horizontal rails app. 20mm. All steel inserts not abutting!

Figure 4-7: Sandwich wall assembly in full-scale fire test
Table 4-13: Panel configurations tested in the structural bending tests at ambient temperature

<table>
<thead>
<tr>
<th>Panel set-up</th>
<th>Stud</th>
<th>Length (m)</th>
<th>Screw spacings (mm centres)</th>
<th>Width (mm)</th>
<th>Replicates</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>100</td>
<td>166</td>
<td>500</td>
</tr>
<tr>
<td>X X X X X</td>
<td></td>
<td>1.1</td>
<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td>X X X X X</td>
<td></td>
<td>1.1</td>
<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td>X X X X X</td>
<td></td>
<td>1.1</td>
<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td>X X X X X</td>
<td></td>
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<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
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<td></td>
<td>1.1</td>
<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td>X X X X X</td>
<td></td>
<td>2.4</td>
<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td>X X X X X</td>
<td></td>
<td>2.4</td>
<td>X</td>
<td>X</td>
<td></td>
</tr>
<tr>
<td>X X X X X</td>
<td></td>
<td>1.1</td>
<td>X</td>
<td></td>
<td></td>
</tr>
<tr>
<td>X X X X X</td>
<td></td>
<td>1.1</td>
<td>X</td>
<td></td>
<td></td>
</tr>
<tr>
<td>X X X X X</td>
<td></td>
<td>2.4</td>
<td>X</td>
<td></td>
<td></td>
</tr>
<tr>
<td>X X X X X</td>
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<td>2.4</td>
<td>X</td>
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<tr>
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<td></td>
<td>1.1</td>
<td>X</td>
<td></td>
<td></td>
</tr>
<tr>
<td>X X X X X</td>
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<td>X</td>
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</tr>
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<td>X X X X X</td>
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<td>X</td>
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<td></td>
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<td></td>
<td>1.1</td>
<td>X</td>
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<td></td>
</tr>
<tr>
<td>X X X X X</td>
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<td>1.1</td>
<td>X</td>
<td></td>
<td></td>
</tr>
<tr>
<td>X X X X X</td>
<td></td>
<td>1.1</td>
<td>X</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

1) Board delaminated and re-attached
2) Stud tested on its own, unstiffened
3) Stud tested on its own, stiffened to avoid premature deformations
Figure 4-8: Racking rig used for the structural in-plane load tests at ambient temperature
Table 4-15: System 1 panel configurations assessed in racking tests

<table>
<thead>
<tr>
<th>Panel dimension (m) (width x height)</th>
<th>Configuration tested</th>
<th>Joints</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Top</td>
</tr>
<tr>
<td>1</td>
<td>2.4 x 2.4 plain</td>
<td>U-chan</td>
</tr>
<tr>
<td>2</td>
<td>2.4 x 2.7 plain</td>
<td>U-chan</td>
</tr>
</tbody>
</table>

Table 4-16: System 3 panel configurations assessed in racking tests

<table>
<thead>
<tr>
<th>Panel dimension (m) (width x height)</th>
<th>Configuration tested</th>
<th>Joints</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Top</td>
</tr>
<tr>
<td>1</td>
<td>2.4 x 2.4 plain</td>
<td>Glued Connections</td>
</tr>
<tr>
<td>2</td>
<td>2.4 x 2.4 plain</td>
<td>Glued Connections</td>
</tr>
<tr>
<td>3</td>
<td>2.4 x 2.4 plain</td>
<td>Nailed Connections</td>
</tr>
</tbody>
</table>

Chapter 4- Methods and Materials
Table 4-17: System 2 panel configurations assessed in racking tests

<table>
<thead>
<tr>
<th>Panel No.</th>
<th>Panel dimension (m) (width x height)</th>
<th>Configuration tested</th>
<th>Joints</th>
</tr>
</thead>
<tbody>
<tr>
<td>1-0/1-5</td>
<td>1.2 x 2.4 plain</td>
<td>GRP</td>
<td>Steel</td>
</tr>
<tr>
<td>2-0/2-5</td>
<td>2.4 x 2.4 plain</td>
<td>GRP</td>
<td>Steel</td>
</tr>
<tr>
<td>3</td>
<td>2.4 x 2.4 window</td>
<td>GRP</td>
<td>Steel</td>
</tr>
<tr>
<td>4</td>
<td>3.6 x 2.4 plain</td>
<td>GRP</td>
<td>Steel</td>
</tr>
<tr>
<td>5</td>
<td>3.6 x 2.4 window</td>
<td>GRP</td>
<td>Steel</td>
</tr>
<tr>
<td>6</td>
<td>3.6 x 2.4 door</td>
<td>GRP</td>
<td>Steel</td>
</tr>
</tbody>
</table>
Chapter 5_results

5.1 Introduction

The experimental investigation covered fire and structural tests at ambient temperature. The fire testing programme was developed to establish and assess the influencing factors to loadbearing sandwich panel wall design. It consisted of three types of fire tests: The bench-scale Cone calorimeter and two furnace tests in intermediate and full-scale, based on standard procedures of EN 1364 (6) and EN 1365 (7) respectively. The structural testing programme was subdivided into two parts. Part I was concerned with providing supplementary information to the loadbearing characteristics of the sandwich walls exposed to fire conditions and consisted of bending and vertical load tests. The second part of the structural testing assessed the racking performance of the wall units and as the test programme was self-contained, the results and findings are presented and discussed independently in Chapter 7. The results of the different fire related test schemes are presented in chronological order. The section starts with the results of the intermediate-scale fire tests, which are followed by the bench- and finally the full-scale test findings. Drawing 5-1 sketches the framework of testing presented in this section. The intermediate-scale investigation was undertaken first within the programme as it represented a test with which a wide range of material and system options could be tested in a furnace environment, similar to the test regime of a full-scale assembly. Since the funds of the project did not allow for multiple full-scale fire tests, the intermediate-scale was regarded as the best approach for initiating the parametric investigation into the influencing factors to sandwich wall design. The enhanced intermediate-scale test regime enabled the parametric investigation of isolated panel design factors and the monitoring of stiffness reduction during the test. However the lack of adequate means of observation of the specimen during the test and poor repeatability of the results made the test too variable for a scientific investigation. In order to eliminate the weaknesses of the furnace based tests the bench-scale test was developed. It was adapted from the Cone calorimeter test, which in its standard form proved to be inadequate for the type of system investigated. The modified bench-scale test was employed to establish material and failure characteristics of the layered sandwich wall systems. The result chapter will present the findings of the standard and modified Cone calorimeter test regimes, although the results obtained from the modified test are more comprehensive. In each test-scale a separate set of tests has been conducted to establish the usefulness and limitations of the fire test methods. The fire results section concludes with the presentation of the findings from the full-scale fire test. The test evidence is compared with full-scale fire experience from various other sources and these additional tests are re-evaluated and analysed to assess a wider range of sandwich wall behaviour characteristics.

The findings of the structural test programme at ambient temperature are presented for the horizontal bending test programmes, followed by findings of the vertical load programme. All results are reported independently and are recombined and discussed in
conjunction in Chapter 6. The horizontal and vertical loading tests at ambient temperature were designed to establish characteristic loadbearing behaviour of the sandwich walls, evaluating specific areas of wall design such as the effect of internal studding and the ultimate loadbearing ability of damaged sandwich wall structures.

The overriding objective of the combined fire and structural test programme was to establish the response of loadbearing sandwich wall units to high temperatures. The proposed scaled methodology was employed to collect specific information on

1. the temperature profile within the unit - the heat transfer into the structure,
2. the effect of heat on the structure - physical damage and failure mechanisms,
3. the reduction in loadbearing capacity,
4. the ultimate failure limit.

All four issues are influenced by the panel design and material choices together with the structural boundary conditions of the wall/ floor system. All objectives enable the collection of information for the standard analytical approach to computation of fire resistance as given in Malhotra (1982) as yet unknown for sandwich panel wall systems. Traditionally the fire resistance of the closely related lightweight building systems, such as timber frame or lightweight steel frame walls, has mainly been provided through the additional sacrificial plasterboard layers applied to the exposed face of the assemblies rather than on the basis of time/ temperature dependent physical damage and the correlated reduction in loadbearing capacity.

The intermediate-scale fire test findings are presented in section 5.2, the bench-scale findings in section 5.3, the full-scale fire behaviour of walls is analysed in section 5.4 and the outcome of the structural tests at ambient temperature in section 5.5 and 5.6, for horizontal bending and vertical loading respectively.
5.2 Intermediate-scale Fire Testing

5.2.1 Introduction to intermediate-scale investigation
The section presents the results of the intermediate-scale fire testing programme. For each panel configuration the following panel reactions are discussed

(i) the temperature reaction of the panels as monitored by thermocouples (TCs) placed at different depths,
(ii) the stiffness reactions of the panels as monitored by the horizontal deformation in the centre of the reduced size wall,
(iii) the visual observations of the exposed and unexposed panel face recorded during the test.

The intermediate-scale fire test is characterised by the size of the test specimen, which is reduced from the full-scale wall unit to one approximately $1\text{m}^2$ wall section. The panel composition in the reduced scale is identical to the full-scale unit in that boards and core form a sandwich wall section of identical thickness and appearance. Joints and other internal sections are incorporated through a special sample design also accounting for potential weaknesses such as tolerance air gaps.

The main objective of the intermediate-scale regime was to establish the temperature and stiffness reactions of the structural sandwich wall when exposed to the heating regime. The factors influencing the characteristic temperature and stiffness response of the wall units were assessed by varying:

(i) board materials,
(ii) core materials,
(iii) internal studding,
(iv) incorporation of joints,
(v) plasterboard cladding.

Table 4-12 overviews all panel configurations investigated in the intermediate-scale programme. This section presents the results without commenting in detail on the characteristic panel reactions causing the temperature and stiffness response encountered. This approach is adopted since only the combination and correlation of the findings throughout the different scales enables the establishment of the underlying panel failure characteristics (see Chapter 6). In this section a final review of the results evaluates the influence of the intermediate-scale test boundary conditions on the temperature reaction of the sandwich wall unit.

5.2.2 Temperature response of structural sandwich walls

5.2.2.1 General temperature reaction
Drawing 5-2 summarises the general behavioural temperature pattern of sandwich wall constructions exposed to high temperatures; this temperature reaction is typical for the entire range of sandwich wall tests and figure 5-1 shows an example of the measured temperature development in a plain sandwich unit (Test 1: Cape boards on Phenolic core).
In all sandwich wall tests the temperature development through the depth of the wall was found to exhibit three regions of behaviour:

(i) Stage I shows a gradual temperature rise (generally less than 10°C/min),

(ii) Stage II follows with a sudden accelerated temperature increase (up to 50°C/min)

(iii) Stage III the temperature levels out at furnace temperature before the end of the test.

Each stage is characterised by its onset time, slope (rate of temperature increase) and the final temperature reached before the onset of the subsequent stage. Through the depth of the layered sandwich unit the temperature reaction develops as sketched in drawing 5-3.

The rate of temperature increase in the first stage of the temperature reaction ($\alpha_A$, $\alpha_B$, $\alpha_C$, $\alpha_D$) reduces the further the measurement location is away from the exposed face. Whilst the temperature response converts to stage II at about 100°C regardless of measurement depth, the onset of stage II is dependent from panel depth. The rate of temperature rise in stage II also reduces with increasing distance from the exposed face of the wall. However, the extent of the slow down is for the most parts dependent on the internal core material. Behind the exposed veneer temperatures reach about 90% of the furnace temperature before levelling off in stage III. This overall temperature level reduces with increasing distance from the exposed panel face.
The later stages of the temperature curve are not always reached for positions C or D. In such cases panel failure occurs before the temperature responses can fully develop. In the subsequent sections the influencing factors to this general temperature pattern are discussed. In the plain panel tests the influence of board, core material and internal studding was further assessed. The jointed panel tests evaluated the influence of jointing and the temperature performance of the joints and a separate set of plasterboard tests examined the effect of additional protective layers.

5.2.2.2 Plain panel tests (Tests 1-10, Table 4-12)
The plain panel test series was subdivided into two sets. The first set of tests (Tests 1 to 4) assessed the general fire performance of reduced-scale sandwich walls. In this first test programme all panels were clad with 10mm calcium-silicate board but backed by varying core materials, ranging from the synthetic PIR and Phenolic cores to an inert Mineral wool core, see table 5-1. Apart from the Mineral wool core all internal substrates were self-adhesive. Whilst the EN heating regime was employed throughout the test work, one of the panels in this first set of tests was tested to the BS heating regime. The EN regulation adopts a marginally different heating curve and more severe pressure conditions along the specimen height in comparison to the BS standard. This comparative testing of one material combination to two standards allowed the evaluation of the effect of the test regime on sandwich wall performance. The effect of the heating regime on panel performance is discussed at the end of this section (5.2.4) together with the overview of other common test boundary effects. The panels in the second test series and in all subsequent tests were subjected to the EN heating regime.
Table 5-1: Panel details for programme 1, tests 1 to 4 with no internal studding units

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Type</th>
<th>Studs</th>
<th>Boards</th>
<th>Core</th>
<th>Glue line</th>
<th>Joints</th>
<th>Plaster-board</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>(mm)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Test 1 (BS)</td>
<td>A*</td>
<td>No</td>
<td>Cape 10</td>
<td>Phenolic 80</td>
<td>Self-adhesive</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>Test 2 (EN)</td>
<td>A*</td>
<td>No</td>
<td>Cape 10</td>
<td>Phenolic 80</td>
<td>Self-adhesive</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>Test 3</td>
<td>A*</td>
<td>No</td>
<td>Cape 10</td>
<td>Min. Wool 80</td>
<td>Slab stock</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>Test 4</td>
<td>A*</td>
<td>No</td>
<td>Cape 10</td>
<td>PIR 70</td>
<td>Self-adhesive</td>
<td>N/A</td>
<td>N/A</td>
</tr>
</tbody>
</table>

In the second stage of the plain panel tests (Tests 5 to 10) the choice of core and especially board materials was enlarged and internal, veneer linking units were placed in the panel cores as shown in table 5-2. The internal, veneer linking, cold-formed studs were placed at 600mm centres, so that each panel incorporated two studding units. Internal veneer linking studs are not common in all sandwich wall systems and their inclusion into the panel is thought to provide enhanced fire resistance of the panel by preventing the delamination of the face veneers in the fire case. The intermediate-scale regime was regarded suited to investigate the function of studs within the panels.

The materials were chosen so that tests 9 and 10 used the same core as tests 1 to 4 but extended the range of board materials to Pyrok and Sasmox, which were thought to be more suitable board materials for unprotected panels. Tests 5 to 8 were designed to assess the change from PIR (PUR) to Phenolic for both the Sasmox and Pyrok board. To complete the assessment of board effect on panel performance test 7 evaluated a Fels board on a Phenolic core in addition to the three board materials (Pyrok, Sasmox and Cape).

Table 5-2: Panel details for programme 2, tests 5 to 10 with internal studding units

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Type</th>
<th>Studs*</th>
<th>Boards</th>
<th>Core</th>
<th>Glue line</th>
<th>Joints</th>
<th>Plaster-board</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>(mm)</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Test 5</td>
<td>B*</td>
<td>Yes</td>
<td>Sasmox 10</td>
<td>Phenolic 70</td>
<td>Self-adhesive</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>Test 6</td>
<td>B*</td>
<td>Yes</td>
<td>Pyrok 8</td>
<td>PUR 70</td>
<td>Self-adhesive</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>Test 7</td>
<td>B*</td>
<td>Yes</td>
<td>Fels 10</td>
<td>Phenolic 70</td>
<td>Self-adhesive</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>Test 8</td>
<td>B*</td>
<td>Yes</td>
<td>Pyrok 8</td>
<td>Phenolic 70</td>
<td>Self-adhesive</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>Test 9</td>
<td>B*</td>
<td>Yes</td>
<td>Sasmox 10</td>
<td>PIR 70</td>
<td>Self-adhesive</td>
<td>N/A</td>
<td>N/A</td>
</tr>
<tr>
<td>Test10</td>
<td>B*</td>
<td>Yes</td>
<td>Pyrok 8</td>
<td>PIR 70</td>
<td>Self-adhesive</td>
<td>N/A</td>
<td>N/A</td>
</tr>
</tbody>
</table>

*Internal veneer linking studs at 600mm centres

In the second test programme, besides the new materials used in the panel, the inclusion of studs represented a major change in the panel composition. Thus the temperature measurement locations in tests 5 to 8 were altered to monitor the response of the stud
units (see Appendix I). This relocation of thermocouples reduced the measurements in the interfaces of the panels so that in these tests only the core temperature in the centre of the panel was monitored. In tests 9 and 10, the final tests of the plain panel test series, the measurement locations were chosen so as to cover all potentially influencing panel parts, which allowed a direct comparison to tests 1 to 4 in the first stage of the tests.

The objectives of the first set plain panel test series (Tests 1-4) were

(i) to determine the characteristic temperature development and reaction of the sandwich wall panels when exposed to the furnace temperature regime,
(ii) to assess the influence of the internal core material on the temperature behaviour of the units,
(iii) establish the difference between the BS and EN heating regime on the overall performance level of the units (discussed at the end of the section).

In the second plain panel test series the load application device was employed so that the stiffness reduction of the units could be monitored in addition to the temperature reaction. This allowed for an extended parametric investigation into the effect of the principal panel materials on the stiffness in addition to the temperature behaviour. Furthermore the effect of internal veneer linking studding units was investigated. Thus the objectives of the second stage plain panel tests were to;

(i) establish the influence of the board material,
(ii) extend the assessment on the influence of the core material,
(iii) investigate the effect of internal veneer linking studding with regard to;
   a) the temperature reaction within the sandwich wall,
   b) the heat reaction of the studs

The temperature reactions obtained from the variety of material combinations in the plain panel tests are shown for measuring positions A to D in figures 5-1 to 5-6. The comparison of panel reactions in all plain panel tests allows assessing the effect of the board and core material and also internal studding. The subsequent section discusses the effect of the various panel components on the temperature reaction of structural sandwich walls.

(A) Influence of board material (Figure 5-2)
The influence of the board material was best monitored by the temperature measurement in location A. The rate of temperature increase in the first stage of the response was varied and ranged from a steep and sudden increase to a gradual temperature rise (see figure 5-2) both of which could not be linked to a specific board material. Although the performance in stage I seemed to be occurring randomly, the onset time of stage II was clearly influenced by the exposed board layer (Figure 5-2).

The time delay in the temperature reaction was indicative of the shielding ability of the exposed board and whilst the Pyrok and Cape clad PIR panels converted to stage II at similar times, the Sasmox clad PIR core exhibited a 5 minutes delay. Since the board materials were backed by identical cores, were of similar density and also possessed
about the same thermal material properties (as shown in Chapter 4) the difference in performance can only be related to differences in material composition and thickness. The findings suggest that the gypsum-based boards perform superior to the cement-based board layers. In addition to the effect of board material composition and thickness the shielding performance of the board layer was also seen to be dependent on the backing core. Whilst the boards backed by the PIR substrate exhibited shielding times in the range of 8 to 15 minutes, the temperature behind the Phenolic backed board layer converted to stage II delayed, following from an extended temperature plateau. The findings suggested that a dense core with improved fire resistance enhanced the shielding ability of the board. Unfortunately the measuring depth behind the exposed veneer was not monitored in all plain panel tests so that not all Phenolic cored panels could be examined to back-up the assumption. The fact that the inert Mineral wool panel exhibited the same early onset of stage II as the remaining PIR cored panels (Figure 5-2) might demonstrate the importance of the glue line, connecting face veneer and internal core. In the Mineral wool cored panel a PUR based glue was used to connect the not self-adhesive Mineral wool to the faces. In general the results suggest that the decomposition of the glue line formed between boards and core affects the degradation behaviour of the exposed board. Once the temperature had converted to stage II, the rate of temperature increase in stage II was steep, regardless of board material. A slightly more moderate temperature gain was found for the more fire resistant cores, i.e. the Mineral wool and the Phenolic cores. The final temperatures reached at the onset of stage III were similar for all board and core materials.

(B) Core material (Figure 5-3 and 5-4)

Whilst the exposed board layer influenced the onset of increased heat exposure of the panel interior, the onset and course of the temperature reaction in the centre of the panel was dependent from the internal core material. Figure 5-3 compares the core temperature developments in the panels' centre for all plain panels, with and without internal studs. In figure 5-4 the different core materials are colour coded, which enables a clearer distinction of the different temperature reactions. In comparison to the temperature reaction behind the exposed veneer the temperature gain in the centre of core in the first stage of the reaction and the onset and rate of temperature increase in stage II were delayed and less steep.

The rate of temperature increase in the centre of the core in stage I is marginal. Only the Mineral wool cored panel showed a sudden rise in temperature shortly after the start of the test. This temperature effect was observed through the depth of the panel with only marginal time delay and was thought to be due to a manufactured defect in the core. Since the Mineral wool is manufactured in slab stock technique local air gaps can be created, which can be the cause of sudden temperature rise. The temperature development within the panel was seen to level soon after the initial rise and suggested that the effect was contained by the surrounding intact core material.

Whilst the temperature in the PUR and PIR cored panel exhibited a marked and consistently early rise in stage II, the temperature rise in the Phenolic and Mineral wool
cored panels was delayed and substantially less steep. In the Phenolic cored panels the earliest conversion to stage II was measured in the Pyrok clad Phenolic core, where the core temperature increased at identical times to the PIR cored panels showing no plateau stage and no delay in the inception of stage II. In one of the Phenolic cored panels, the Cape-Phenolic specimen tested to EN standard, the temperature reaction did not convert to stage II at all. The analysis of temperature readings in positions C and D for these test panels will show that both core temperature reactions were atypical and misrepresented the performance level of the Phenolic core. The early conversion to stage II was thought to be related to local destruction within the core material. Local destruction can distort the temperature readings and are not deemed representative of the overall panel performance. The second atypical reaction was due to a faulty temperature measurement and for comparison the reading has been replaced by the temperature measurements taken from the identical panel composition tested to BS 476: 22 (5).

The final temperatures reached in the centre of the panel were consistently highest in the PUR/PIR cored panels and, with the exception of the already mentioned Pyrok-Phenolic panel, markedly less severe in the Phenolic and Mineral wool cored panels. Generally the temperature response of the Phenolic cored panels was more varied than in the PUR/PIR panels.

The onset of sharp temperature increase, i.e. stage II, in the centre of the panel was related to the temperature response behind the exposed veneer. In Pyrok and Cape clad PIR cores the onset of stage II was observed 5 minutes earlier than the temperature reaction in the Sasmox clad PIR core. This board dependent time delay was also observed for the temperature response behind the exposed veneer. The findings suggest that the temperature increase behind the exposed veneer triggers the temperature reaction throughout the panel depth. Similarly to the delay in temperature reaction between measuring depth A and B, a further delay is observed in measuring depth C. The temperature development in this panel depth was not assessed for all material combinations. The time delay between the onset of stage II behind the exposed veneer and in the centre of the core is replicated in the onset of stage II on measuring location C, as seen in drawing 5-2 and figures 5-3 and 5-5 and is also governed by the internal core material. As observed in measurement location B, the temperature build-up in this measurement depth was most severe in the PIR/PUR cored panel and least severe in the Mineral wool and Phenolic cored panels. The PIR panels were the only panels progressing to stage II of the temperature reaction and the rate of increase in stage II was markedly consistent. The final temperature reached in the Mineral wool cored panel interface was similar to the temperature in the Phenolic panel.

Although the temperature development of the ambient side of the wall should be equally influenced by board and core material the effect of the board material was negligible. The surface temperature of the unexposed face, as shown in figure 5-6, was monitored in all plain panel tests. The highest surface temperature build-up was observed in the PIR/PUR cored panels, regardless of board material. The delay in temperature build-up in the PIR
cored panel exhibited a difference in temperature performance between both core materials, which was not observed in locations A to C. This indicates the marginally superior performance levels of the PIR. The unexposed board surface temperature remained at lowest levels in the Phenolic cored panels. In contrast to the irregularities in temperature reaction in the centre of the panel, all the Phenolic cores had a consistently moderate temperature reaction in this measuring position. This was also observed for the Mineral wool cored panel, where the temperature stabilised after an early rise. This indicated that the Mineral wool core was an effective heat shield and enabled the localised damage to be compensated. The high temperatures encountered in the PIR filled panels indicate flaming and local destruction of the core layer.

(C) Internal studding- Studs

Influence on temperature reaction of panel

The inclusion of studs did not influence on the characteristic temperature response of the sandwich wall. In position A the initial temperature rise, onset of stage II and overall temperature levels were in the same range for all plain panels regardless of internal studs. As observed for the temperature reaction behind the exposed veneer the presence of internal veneer linking units did not seem to influence on the temperature responses of respective core material, which was dominated by the effect of the core material.

Temperature reaction of stud units

The standard temperature response of the ambient flange of the internal stud was dependent on the filling core material as shown in generalised form in drawing 5-4. The temperature response of the studs was monitored through four thermocouples located on the unexposed flanges. Each stud was monitored by two thermocouples located on the upper and lower half of the unit. In figure 5-9 the mean stud temperature development in all panels are compared. The single temperature readings have been compiled in the Appendix IV.

The characteristic temperature development of the studs could be subdivided into two groups. The stud units in the PIR/PUR panels showed similar temperature development, remaining in stage I at ambient temperature for about 20 minutes and then significantly heating up to temperatures up to 300°C at the end of the test. There was no difference in stud temperature reaction between the PIR and the PUR cored panel. The temperature reaction of the studs in the Phenolic filled panel was different in that stage I was prolonged and onset of stage II was not attained before the test was stopped. In these panels the ultimate temperatures reached were well below the 300°C observed in the PUR/PIR filled panels. Overall temperatures at the end of the test are highest in the Pyrok-PUR/PIR sample. The stud temperatures in the Fels-Phenolic panel showed the earliest increase. Despite this early and marked temperature rise the final temperatures reached within the studs were similar to the ones observed in the remaining Phenolic cored panels. The torsional movement of the stud unit, caused by the uneven heating, can cause thermocouples to be moved from the measuring surface and temperature readings must be analysed with care.
In some panels (most pronounced in Pyrok-PUR panel, see Appendix IV) the temperature development of the ambient flange of the units was found to rise differently depending on the location of the measurement. The TCs located at the upper half of each of the studs monitored a more rapid heat pick-up than the TCs measuring further down.

![Diagram](image)

**Drawing 5-4: Generalised temperature development in internal studding units**

*Performance of studded and non-studded panel section*

Figures 5-7 and 5-8 compare the temperature development at various locations in the Sasmox/Pyrok-PIR panels. In these tests it was tried to measure the relative heat transfer performance of a studded as opposed to a non-studded panel section. Therefore on each stud one TC was placed 30mm beside the stud and one TC was placed on the unexposed flange as before. It was intended to compare those readings with the temperature development in the centre of the panel at the interface of core and unexposed board.

Drawing 5-5 summarises the general findings from the readings. It was found that the heat levels in the studded cross sections of the panel were less severe than the heat levels measured in the plain sections of the panel. In some panels the differences were only marginal. Generally the central and upper half of the panel was affected by the heat environment to greater degree, which could result in temperature variations of up to 300°C. The Sasmox board cladding offered better protection to the stud units so that the studs converted to stage II of the temperature build-up delayed when compared to the Pyrok clad PIR panel.
D) Observations

The visual monitoring of the panels during the test was valuable in order to relate the measured panel responses, temperature build-up and stiffness loss, to the failure patterns occurring in the panel. Due to the highly combustible synthetic cores, smoke development could be severe, often hindering observations of the exposed face later in the test. The fire damage to the panel was sketched and could be related to the fire duration. Subsequently the general observations made during and at the end of the tests will be summarised.

All boards showed signs of cracking within the first 3-4 minutes. After about 15 minutes, cracks had developed over the entire exposed surface and opened up quickly, exposing the core material to the furnace environment. For the PUR/PIR panels the observation of the exposed surface was then no longer possible as opaque smoke generated from the decomposing core quickly filled the furnace chamber. However, for the Phenolic and Mineral wool cores this was not the case; here the exposed face remained visible until the end of the test as smoke production was reduced. Once the Phenolic core was exposed to the direct heat it charred, which looked similar to charred wood. In some parts of the panel board fragments remained attached to the charred, honeycomb-shaped core pieces. In all tests the studs remained partly covered by the board materials until the end of the test. After 45-55 minutes, dark brown/black smoke emitted from the perimeter of the sample on the unexposed face into the smoke chamber surrounding the furnace. Once the heat has penetrated into the panel interior the smoke generated from the core are relieved through the spot of least hindrance, hence at the top of the panel and not through the centre of the ambient board, where no failure will have occurred at that time. This was observed earlier at 20-25 minutes for the PUR/PIR cores. The constant flow of smoke from the edges of the sample discoloured the unexposed board. The tests were then
terminated as the panel had failed the integrity criteria. This occurred, on average, after 66 minutes for the Phenolic-cored specimens and after 20 minutes for the PUR/PIR panels. Burn through of the board was observed either in the centre of the panels near the load application point or along the upper edge of the specimen.

On termination of the test local flaming was extinguished and the panels were left in the furnace to cool down. After 20 to 30 minutes the specimens could be removed from the furnace for post-experimental observation. In the Phenolic cored panels a clear layer of unaltered, unburnt material was still attached to the unexposed board. In the PUR/PIR filled panels no virgin core material was left. Some heavily charred, lightweight core pieces were observed in the PIR panels, especially along the studs and the 5 cm outer perimeter of the panel. The perforated web of stud units retained the charred core well and in the Phenolic cored panel the entire stud unit, apart from the exposed flange was still encased. In the centre of the panel no core material was left.

![Appearance of Phenolic Core after 110 minutes](image1)

![Appearance of Polyurethane Core after 50 minutes](image2)

Cold-formed steel studs tying veneers

Drawing 5-6: Appearance of core substrates after termination of test

### 5.2.2.3 Jointed panels

After the evaluation of the effect of board, core and internal units the second stage of the programme was designed to establish the influence of jointing units on the wall performance. In a full-scale wall jointing members disturb the continuity of the wall and due to the layered composition of sandwich walls the joints can allow the premature access of heat to the panels' more vulnerable interior core. Thus joints have a potentially weakening influence on the overall fire performance of walls, which makes the investigation into the effect of jointing on panel performance of paramount importance.

The alternatives for horizontal and vertical jointing of sandwich wall panels are numerous. In the programme three vertical and two horizontal jointing alternatives were
assessed. Each jointed panel used PIR/ PUR cores and was clad by either Pyrok or Sasmox boards, as seen in table 5-3. Two of the vertical joints were contiguous and one joint was intermittent, placed in the centre of the specimen. The horizontal joints were an internal top hat section or a capping cold-formed steel u-channel. Details of the jointed panel set-up can be found in Chapter 4: Methods and Materials. Tests 11 to 14 also incorporated air gaps to simulate tolerances within the panels. The manufacture of panels 15 and 16 was not likely to effect internal tolerances at the joints since no recesses were formed to accommodate the jointing mechanisms. Except for panel 16 the panels did not include any additional internal linking.

Table 5-3: Tests on panels with vertical and horizontal joints

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Type</th>
<th>Studs</th>
<th>Boards</th>
<th>Core</th>
<th>Joints</th>
<th>Plaster board</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>d (mm)</td>
<td>c (mm)</td>
<td>vertical joint</td>
</tr>
<tr>
<td>Test 11</td>
<td>C</td>
<td>No</td>
<td>Sasmox</td>
<td>10</td>
<td>PIR 70</td>
<td>Steel</td>
</tr>
<tr>
<td>Test 12</td>
<td>C</td>
<td>No</td>
<td>Sasmox</td>
<td>10</td>
<td>PIR 70</td>
<td>GRP</td>
</tr>
<tr>
<td>Test 13</td>
<td>C</td>
<td>No</td>
<td>Pyrok</td>
<td>8</td>
<td>PIR 70</td>
<td>Steel</td>
</tr>
<tr>
<td>Test 14</td>
<td>C</td>
<td>No</td>
<td>Pyrok</td>
<td>8</td>
<td>PIR 70</td>
<td>GRP</td>
</tr>
<tr>
<td>Test 15</td>
<td>D</td>
<td>No</td>
<td>Pyrok</td>
<td>8</td>
<td>PUR 70</td>
<td>Camlock u-channel</td>
</tr>
<tr>
<td>Test 16</td>
<td>Ds</td>
<td>Yes</td>
<td>Pyrok</td>
<td>8</td>
<td>PUR 70</td>
<td>Camlock u-channel</td>
</tr>
</tbody>
</table>

*Top u-channel clad with 15mm standard plasterboard

The objectives of the jointed panel programme were to

(i) establish the influence of jointing-
(ii) investigate the influence of the form of the joint, i.e. intermittent or contiguous-
(iii) assess the effect of tolerance air gaps -
(iv) observe the impact of any additional veneer linking sections on the temperature and stiffness behaviour of sandwich wall specimens.

In addition to the above stated objectives the programme was employed to

(v) compare the failure behaviour of the different jointing materials.

A) Influence of jointing on temperature reaction of panel

Figures 5-10 to 5-13 compare the temperature response of the jointed and plain panels behind the exposed veneer and in the centre of the core. Several measuring locations are compared. Whilst figures 5-10 and 5-11 only compare the plain and continguously jointed panels, figures 5-12 and 5-13 enlarge the comparison to include all jointed panels assessed in the programme. As the plain panels, the jointed units exhibited the three-stage temperature reaction. Onset and the speed of temperature response were similar for plain and jointed panels, depending on the board material. The Sasmox clad panels exhibited the typical delay in temperature build-up. In the jointed panels the temperature responses,
i.e. onset of stage II but especially the temperature at the onset of stage II, varied depending on the measurement location within the panel. This was most apparent in the Sasmox clad jointed panels where the GRP jointed specimen showed the greatest performance variations. Generally the delayed temperature measurements were located close the joint or in the bottom half of the panel.

The type of joint i.e. intermittent or contiguous did not influence on the temperature reactions within the panel units as shown in figures 5-12 and 5-13, which was to greatest extent affected by the board material. In both measuring depths, locations A and B, the intermittently joined Pyrok clad panels performed similar to the other Pyrok clad- plain and contiguously jointed panels. The temperature development in location A of the studded Camlock jointed panel was delayed when compared to the non-studded Camlock and the plain panel.

B) Performance difference between contiguous vertical GRP/ steel and intermittent joints
The heat build-up pattern for both contiguous joint materials was similar as seen in the temperature response at the top of both contiguous joints (Figure 5-14). Drawing 5-7 summarises the general temperature reaction of both joints. The temperature rise and the overall temperatures reached within the material were generally dependent on the cladding board material. The hollow GRP section exhibits higher temperature at the start of the test, which was related to the tolerance air gap at the top of the joint allowing hot air to infiltrate the panel and joint early in the test. As the temperature in the furnace increases the steel section rapidly heats up and performance levels out. The temperature reaction of the steel joint embedded in the core was further investigated in the modified bench-scale programme, where the degradation of the core was seen to be accelerated by the steel joint. In all jointed intermediate-scale tests the gypsum based Sasmox veneer offered the better protection to the jointing section.

Drawing 5-7: Comparison temperature development in contiguous jointing sections
However, when the temperature response of jointed and un-jointed cross section within a jointed panel for both contiguous jointing options is compared (Figure 5-15 and 5-16) a performance difference between joint materials can be observed. The temperature rise in the plain cross section of the steel jointed panels was much more marked than in the GRP jointed panel. At 40 minutes the temperature in the Sasmox clad steel jointed panel was 700°C whereas the temperature level reached at the same time in the GRP joined unit was only 150°C. An identical trend could be observed in the Pyrok clad jointed units at 30 minutes. This is thought to be related to the premature degradation of the core material along the steel jointing section and has been assessed further in the bench-scale regime.

Whilst the temperature reaction within the panel area was seen to be similar for all Pyrok clad panel regardless of jointing type, i.e. contiguous (steel or GRP) and intermittent, the temperature build-up at the top of the vertical panel intersection was distinctively dissimilar (Figure 5-17). The intermittently linked panel halves exhibited a temperature build-up similar to the better performing Sasmox jointed panels. This higher temperature in the contiguously jointed panels was thought to be linked to the detrimental effect of tolerance air gaps. The commercially manufactured intermittently linked panel was free of internal tolerances, which positively impacted on the temperature increase at the interface of core and unexposed board (location C). In the centre of the panel the effect was less marked (Figure 5-18). In this measuring location the inclusion of studs in the intermittently joined panel were seen to reduce the temperature build-up in the panel.

C) Temperature reaction top joint
Figure 5-19 compares the temperature development of the exposed face of the different top jointing systems. The external cold-formed u-channel capping the specimen ends was protected by a strip of 15 mm plasterboard to prevent premature heating of the panel top. The external u-channel with extra protection performed superior to the GRP joint, which was certainly due to the shielding effect of the extra plasterboard. The GRP jointing section performed well, considering that part of the jointing section was exposed to the direct heat of the furnace throughout the entire test and apart from difference in board performance the GRP exhibited a consistent response to the heat exposure.

D) Visual Observations
Due to the fact that the panels had tolerance air gaps, they included a direct heat path to the vulnerable panel core at the intersection of top rail and vertical joint. Upon heat exposure the core was accessed rapidly opaque smoke evolved, which quickly filled furnace. Despite the heavy smoke generation in the furnace, the unexposed surface remained observable until termination of the test, which proved the adequacy of the protective beading around the panel. Soon after the start of the test white smoke emitted from the top of the vertical joint. Smoke was emitted constantly from then on, changing to a black colour towards the end of the tests. Later on the smoke also emitted around the perimeter of the panel. The locations of smoke emission sometimes changed during the test. Smoke was observed to emit from the nearest opening to equalise the pressure.
difference between the furnace and its surrounding. Once the core had been consumed in one panel area, other panel locations reached combustion levels and the emissions were observed to move towards these locations. The severe decomposition processes inherent in these types of panels attacked the sealant mastic, provided to bed and protect the panel in the steel frame. Although not penetrable at the beginning of the test, the progressive decomposition of the panel weakened the beading around the perimeter of the panel and allowed for enhanced smoke emissions.

The GRP jointing materials exhibited different failure behaviours dependent on the protection they received from the panel. When used as a vertical joint the material degraded and weakened early in the test. This was most notable in panel 10, PK-PIR-FL-FL (Drawing 5-8). However, when the GRP joint was used as a horizontal joint the material performed well and was not seen to degrade. It is believed that this different failure behaviour is due to the fact that the vertical joint is not only heated on its fire exposed side through the gaps between the board edges but also along its inner perimeter, as heat penetrates through the 2-3mm gap at the intersection with the horizontal joint and then into the open end of the section. The extra heat causes the GRP and the glued joints between individual sections to break down prematurely. At the horizontal joint there are no gaps for fire or heat to penetrate and although parts of the outer face of the section were exposed this did not cause any problems in performance. Many of the vertical GRP sections decomposed severely so that the matrix completely melted leaving only brittle glass fibres with no remaining intrinsic strength.

The steel sections did not delaminate nor appeared to fail. They had lost strength but this was less important as they continued to behave as an effective joint although local buckling behaviour was noted when the core around the steel was burnt away.

Drawing 5-8: Vertical joint section at post-experimental observations
The jointed panels failed later than the standard panels. The difference was less significant for the Pyrok clad panels but marked for the Sasmox panels where panels with joints generally failed about ten minutes later than an unjointed panel. The type of vertical jointing material had negligible effect on the time to failure. The visual monitoring of the Camlock jointed panels showed that the weak Camlock joint distorted and opened up early in the test. The principal area of weakness was in the centre of the panel where the two panels started to open up. Both panels showed similar performance and the internal studs had little effect on the overall stiffness of the panel.

5.2.2.4 Jointed panels and plasterboard

In practice the full-scale sandwich wall is clad with varying layers of sacrificial plasterboard. This is common practice to provide adequate fire and acoustic insulation. In most cases the plasterboard is fixed onto battens, which are attached to the panel. The batten fixing mechanism is often preferred as it provides a gap between panel surface and plasterboard, which can be used to lay services and run pipes. In the final stage of the intermediate-scale testing regime the two most common plasterboard layer thicknesses, 15 and 30mm, were chosen to protect the panel. Generally the overall thickness of protective layers is dependent on the type of plasterboard used. When glass-fibre reinforced plasterboard is chosen the overall thickness can be reduced and layers of 12.5mm and 2 x 12.5mm are common for 30 and 60 minutes fire protection respectively. However due to the reduced scale of the test it was regarded more appropriate to use the less sophisticated wallboard without glass fibres. This was also seen in prospect to the subsequent full-scale fire test where the cheapest wall protection was regarded as the preferred alternative. So in a final programme of four tests the fire exposed side of the contiguously jointed panel was lined with standard plasterboard; two panels were clad with one layer of 15mm plasterboard and the others with two layers of 15mm board.

Table 5-4: Jointed panels with plasterboard protection

<table>
<thead>
<tr>
<th>Test No.</th>
<th>Type</th>
<th>Studs</th>
<th>Boards</th>
<th>Core</th>
<th>Joints</th>
<th>Plasterboard</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>vertical</td>
<td>horizontal</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>joint</td>
<td>joint</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>15 mm</td>
<td>2 x 15 mm</td>
</tr>
<tr>
<td>Test 17</td>
<td>C+</td>
<td>No</td>
<td>Pyrok 8</td>
<td>PIR 70</td>
<td>Steel</td>
<td>GRP</td>
</tr>
<tr>
<td>Test 18</td>
<td>C+</td>
<td>No</td>
<td>Pyrok 8</td>
<td>PIR 70</td>
<td>GRP</td>
<td>GRP</td>
</tr>
<tr>
<td>Test 19</td>
<td>C+</td>
<td>No</td>
<td>Sasmox</td>
<td>PIR 70</td>
<td>GRP</td>
<td>GRP</td>
</tr>
<tr>
<td>Test 20</td>
<td>C+</td>
<td>No</td>
<td>Sasmox</td>
<td>PIR 70</td>
<td>Steel</td>
<td>GRP</td>
</tr>
</tbody>
</table>

The plasterboard was screwed to 15mm x 45mm timber battens, which were fixed vertically to the test panels. The Sasmox clad panel with steel vertical joint was tested with 15mm plasterboard. The effect of 30mm plasterboard was then checked on the same board using the GRP vertical joint combination. For the two tests on Pyrok clad panels the combinations were reversed.
The objectives of the tests were to assess the influence of additional plasterboard cladding on the

(i) temperature reaction of the unit
(ii) stiffness performance of the units.

A) Temperature reaction

The temperature reactions of all the plasterboard protected jointed panels were similar and are shown in generalised form in drawing 5-9. In figure 5-20 the temperature course behind the exposed Sasmox veneer of the jointed samples is compared to the temperature development of the jointed samples with varying plasterboard protection. With 15mm plasterboard protection the temperature behind the veneer of the Sasmox clad steel jointed panels rose at about 60 minutes, 40 minutes delayed to the temperature rise in the non-protected panel with steel joint. As in the plain and jointed panel tests discussed before the board material was influential to the onset of the temperature reaction behind the exposed veneer. The Sasmox board delayed the onset of the temperature reaction by a further 5 minutes when compared to the Pyrok clad samples (Figure 5-21). However, for both board material combinations and plasterboard thicknesses a temperature of about 100°C was reached at the onset of stage II. When the panel was protected by two layers of standard plasterboard the temperature rose after about 110 minutes. This meant that the differing thicknesses of plasterboard layers offered 40 to 90 minutes protection for 15 and 30mm respective plasterboard thickness, regardless of exposed board materials. The overall temperatures at the end of the test were similar to the final temperature levels in the unprotected specimens, respectively delayed depending on the number of sacrificial layers. In none of the panels stage III of the temperature response was reached since the test needed to be stopped due to local burn through failure of the unexposed face.

![Temperature Reaction Graph](image-url)
B) Visual Observations

For the jointed panels clad with plasterboard the fire exposed face remained observable as the panel weaknesses had been protected and no smoke emitted from the sample. However, on the cold side, smoke was emitted from the tenth minute onwards and emitted from the top of the vertical joint with varying intensity as the plasterboard protection reduced. For 30mm of plasterboard the first sign of cracking on the fire exposed side of the plasterboard was observed at about 50 minutes. Once the plasterboard had cracked the cracks opened up quickly, and the board delaminated and fell from the panel. Then the furnace quickly filled with smoke and observation of the exposed face had to be stopped. At no time could the fire-exposed face of the sandwich panel be visually observed.

5.2.3 Stiffness response

5.2.3.1 Stiffness reaction-plain panels

The deflection behaviour of the plain panels subjected to the 1kN central point load (see Chapter 4) are shown in figure 5-22. The general pattern for the deflection against time curves was similar to the temperature against time curves, i.e. a period of little change in deflection, followed by a period when the deflection increased quickly, and finally a reduced deflection rate period as shown schematically in drawing 5-10.

![Drawing 5-10: Typical deflection response](image)

There was a strong correlation between the starting point for the stage 2 of deflection phase and the onset of sharp temperature increase behind the exposed veneer (Stage II). This will be discussed in further detail in the Chapter 6. The first stage of the deflection course showed little uniformity. Initially there was a small increase in deflection probably due to thermal bowing as the fire facing board surface expanded. This was followed by a
period of little change except in two (Pyrok-PIR and Pyrok-Phenolic) panels where a reduction in deflection and a movement out of the furnace was recorded. These negative deformations could have possibly been the result of the panel seating into the rig. The pressure build-up in the furnace could also have affected the deflection pattern in these first minutes of the tests. Both effects have little impact on the overall deformation signature of the units and can be considered as bedding of the panel.

In the first stage of the deflection response the gradual softening of the fire face board resulted in only minor damage to the wall panel. In the second stage of the deflection course significant damage to the panel is likely since the stiffness loss is rapid and severe. Despite the large increase in deflection the ultimate deflection reached at the onset of stage III was too lower deflection for the complete destruction of the panel, indicating the composite action to be retained in parts of the panel and damage to be localised in the centre of the panel and not through the entire area of the wall. The Phenolic cores deflected less than those cladding PUR/PIR cores although there was a similarity in deflection rates between the Pyrok clad panels regardless of the core material, as observed before in the temperature reaction.

5.2.3.2 Stiffness reaction joints

A) Comparison to plain panels

The reduction in stiffness of the jointed panels are shown in generalised form in drawing 5-11 and in figure 5-23 relative to the plain panel performances. Figure 5-24 compares the results for the four jointed panels (Tests 11 to 14) with two plain panels using identical materials (Tests 9 and 10) and the two Pyrok-Camlock panels with and without additional studding. The deflection pattern of the jointed panels was more variable than for the plain panel tests and generally less marked. The improved stiffness performance of the contiguously jointed panels was linked to the joint section, which stiffened and reinforced the central part of the wall panel, where the load was applied. The contiguous joints performed over a wider range. The weaker performing joints exhibited a four-stage deflection pattern characterised by sharp, short stage II and an intermediate stage where the deflection increased further before the panel deflection levelled off in stage III. The panels with lower overall deflection were observed to display a short, less steep stage II followed by a marginal deflection increase in stage III and a final sharp increase in deflection at the end of the test, marking a thermal bowing of delamination effect. Whilst the contiguously jointed panels exhibited lower deflection levels, the panels incorporating the intermittently jointed panels exhibited a three-stage deflection course similar to the plain panels. The results of the stiffness tests are not highly conclusive mainly because the small-scale panel is supported on all four edges when resisting the horizontal load and the span length is small. As a consequence the vertical joint is less of a weakness to the panel.

B) Comparison intermittent and contiguous jointing

The intermittently linked panel units exhibited the most marked stiffness loss. This was thought to be linked to the fact the panel had no continuity through the joint except at the
centre of the wall. There was little difference between the panels with and without studs; clearly the studs did not markedly improve the hinging mechanism. In contrast to the other contiguously joined panels the Pyrok panel with GRP vertical joint deformed at similar rate as the weaker intermittently joined panels. The rapid stiffness loss and high final deflection suggested that the vertical section was badly damaged by the fire. This was confirmed in the visual monitoring. In all these tests the Pyrok clad panels behaved worse than Sasmox clad panels.

C) Jointing materials
The performance of the contiguous joints was dependent on the cladding board material, both jointing materials performed superior when clad by the Sasmox board. Generally the GRP joint performed weaker than the steel joint as it was prone to disintegrate during the test and thereby weakening the central part of the panel. The build-up of the GRP joint, which was glued together from separate standard sections, had a great impact on this failure signature.

![Diagram showing stiffness reduction in jointed panels](attachment:image.png)

**Drawing 5-11: Generalised stiffness reduction in jointed panels**

**5.2.3.3 Stiffness reaction in plasterboard protected panels**
The deflection rates of the steel and GRP jointed panels are compared in figures 5-24 and 5-25 and related to the plain panel deformations of the Sasmox/ Pyrok-PIR panels tested in the plain panel programme. Drawing 5-12 generalises the findings of the stiffness measurements.
The plasterboard cladding prolonged the gradual increase in deflection in the first part of the test, before gradually deflecting at a higher rate towards the end of the test. The jointed panels deflected at a more constant rate from the start of the test without exhibiting the previously established, clearly distinguishable four-stage behaviour. In the plasterboard protected sample the onset of stage II was less distinct and the deflection rates converted smoothly into the following stages. In all panels a final sharp increase in deformation occurred towards the end of the test.

The GRP jointed panels deflected to greater extent than the steel joined units, see figure 5-25. However when the GRP joint was protected by the superior Sasmox board and enhanced plasterboard protection the panel deflected at a reduced rate. The steel joint profited more efficiently from the plasterboard protection and in both cases the deflection rates were reduced and the marked increase in deflection delayed.

5.2.4 Method
5.2.4.1 Influence of testing regime: Comparison BS-EN test regime
The effect of test method on panel performance is shown for the BS and EN test on similar Cape clad Phenolic cored panels in figures 5-26 to 5-28. It demonstrates a slower response to the BS test with both a delay in reaching stage II and a less steep temperature gradient. This shows the BS method to be less severe than the EN method.
5.2.4.2 Influence of location of measurement

Drawing 5-14 summarises the influence of temperature response of structural sandwich walls with respect to panel area.

Two influencing factors area effect have been identified

(i) proximity of restraint
(ii) height

The temperature build-up measured in edge areas, around the perimeter of the panel, is generally lower than in central parts of the wall as shown in figure 5-29. The location of the measuring thermocouple influences both the onset and the rate of temperature rise. In
figure 5-29 the TCs located close to the edge perimeter exhibited lower temperature readings throughout the tests (TC 1 in both panels). In the Sasmox clad panel these temperature effects are also exhibited in later stages of the test, which can be appreciated by comparing TC2 and TC3. Whilst TC2 exhibits a second sharp temperature increase towards the end of the test, the temperature measured in location TC3 located closer to the edge of the panel levels off. The reason for this performance differences is thought to be related to the enhanced restraint provided to the exposed board pieces in edge areas, which seems to shield the inside of the panel and thereby delays heat infiltration and dampens overall heat build-up. Generally, temperature levels can differ to a maximum of 500°C between protected, well-restrained board areas and central board areas.

A) Effect of internal links
Since the dampened heat build-up in edge areas was correlated to the enhanced restraint provided to the exposed veneer, the effect of internal veneer linking studs on the overall temperature performance, especially in stages II and III of the temperature reaction was further assessed. Figure 5-30 and 5-31 concentrate on the effect of additional internal links within the Carnlock jointed panels. In accordance with the findings before, the specimen without additional internal studs exhibited a more severe temperature rise, especially towards the end of the tests. In figures 5-30 and 5-31 the temperature reaction in different measuring locations in each measuring depth, A, B and C have been analysed. The measuring locations differed with respect to their proximity to restraint. Especially the central areas of the panel profited from the intermediate restraint provided by the studding, since the TCs located in the central area of the non-studded panel were measuring the sharp temperature rise in locations A and C was earlier than in the panel incorporating studs. In measuring depth B (centre of the core) the effect levelled as the temperature rise and the overall temperatures reached differed only marginally between the two panel types. Close to the edge perimeter of the panel the effect of additional studding was negligible as the temperatures in location A and B occurred at similar times in both panels. Solely TCs in position C showed the earlier rise in the non-studded panel. This suggests that the edge restraint is the overriding influencing boundary condition and that additional internal board restraint can only achieve marginal further improvement. In any case this effect is also related to the overall panel area so that the internal studding within a panel is likely to benefit the temperature build-up with increasing panel size.

B) Height
The location of the thermocouple position with respect to height was seen to be significant. This is typified in drawing 5-14 and figure 5-32, showing the temperature response to be delayed for the lower locations where typically the temperatures will be slightly less hot due to the rise of gases. Towards the end of the test, in stage III, the temperature stabilises, rising at a much reduced rate in both measuring positions and levelling at approximately the same overall temperatures of about 700°C.
5.2.5 Summary of findings in intermediate-scale fire testing regime

A) Temperature reaction

The temperature reaction of a sandwich wall is characteristically subdivided into three stages. Stage I shows a gradual temperature rise (rising to approximately 100°C in about 15 min) which is followed by a sudden accelerated temperature increase (of up to 100°C in a minute) in stage II. Before panel failure the temperature levels out in stage III. The further the measuring location is away from the exposed face of the panel the later the temperature responses take place. The later stages of the temperature curve are not always reached for positions at the interface of core and ambient face or on the surface of unexposed face. Panel failure will have terminated the test before the temperature reaction occurred. The onset and rate of temperature increase in each stage is dependent on board and core material. The onset of temperature rise behind the exposed veneer triggers the temperature build-up within the subsequent layers of the entire unit. Therefore the fire performance of the exposed board layer was seen to have major impact on the panel reactions and in the testing regime the gypsum based board was seen to perform superior to the cement based board materials tested, offering enhanced protection times. The performance of the exposed board layer was also influenced by the backing core material and shielding performance of identical boards could in some cases double depending on the core substrate. Internal stud units did not affect the temperature build-up behind the exposed veneer and therefore did not delay the degradation process of the panel.

The fire behaviour of the internal core material affected on the speed of temperature increase throughout the panel and the PIR/ PUR cores exhibited the earliest and most severe temperature increase. The PUR was marginally less fire resistant than the PIR. In contrast the Phenolic core exhibited the most moderate but also least consistent and reproducible reaction to the furnace environment. The Mineral wool core resembled the Phenolic with respect to the overall temperatures reached but its temperature reaction was marked by an early temperature rise similar to the PIR/ PUR core. This was thought to be affected by the PUR based glue used to fix the non-adhesive Mineral wool to the sandwich faces.

The fire reactions of internal units, such as stud and joints, were also affected by the shielding ability of the board and insulation capacity of the core. Internal units profited from enhanced board shielding times and fire resistant cores, both of which slowed down the degradation of the internal units.

The analysis of the results showed that local effects, related to the destruction of material in one area of the panel, could distort the overall temperature characteristics of a panel. This could be avoided by examining the temperature measurement through the depth of the panel but also at different locations as related to the panel area, which underlined the need for multiple temperature recordings in fire resistance testing.
B) Stiffness loss in plain panels

Stiffness loss was established to be similar to the rise of temperature increase within the panel unit, also subdivided into three stages. The deflection of the panels was marked by a period of little, followed by a rapid increase in rate of deflection. As in the temperature reactions the Phenolic cored panels showed the latest onset of increase in deformation as well as the lowest overall deflection at the end of the test. In the PIR/ PUR cored panels the most severe stiffness loss was encountered. The temperature increase behind the exposed veneer and the onset of sharp deflection rise were correlated.

C) Visual monitoring

The visual monitoring of the panel was affected by the generation of smoke, which in case of the PIR/ PUR foams was so dense and rapidly evolving from the panel that monitoring had to be discontinued. The Phenolic cored panels showed considerably less smoke production and the exposed face of the panels remained observable until the end of the test. After the test the PUR/ PIR core was to great degree consumed and hardly no remains were left, whereas the Phenolic was left at much of the original depth adhered to the back of the unexposed face. The Phenolic was seen to be heavily charred.

D) Jointed panels

In the jointed panels the temperature reaction of the panels was more varied than in the plain panels, exhibiting large temperature variations depending on measuring locations. This was related to the additional stiffening of the vulnerable central part of the panel by the vertical jointing section. The panels clad with the stronger performing Sasmox board exhibited larger variations in temperature reaction throughout the panel than the specimen clad by the thinner Pyrok veneer. The temperature reaction for GRP and steel contiguous joint materials were similar. The tolerances and air gaps had considerable effect on the integrity of and the rapidity of temperature build-up within the panel. This was especially apparent in the comparison of contiguously joined panels and the intermittently joined System 1 panels. Although the contiguous joint offers greater restraint to the shielding board and furthermore protects the combustible internal core at the vulnerable intersection of panels, the intermittently jointed panel performed superior to the contiguously jointed panels with respect to temperature build-up. The superior temperature performance was related to the fact that the system did not have any tolerance air gaps. Tolerance air gaps created substantial weak spots as they enable heat to penetrate into the panel.

E) Stiffness jointed panels

Although all jointing options were shown to render similar heat build-up characteristics in the panels' interior, their effect on the stiffness loss in the panel was markedly different. The generalised stiffness behaviour of the contiguously joined test panels differed from the deflection signature of the plain panel. The stiffness loss was less consistent and exhibited more stages. The intermittent and weak contiguous joints behaved similar to the plain panels. The intermittent Camlock joint and the Pyrok clad GRP joint exhibited the greatest stiffness loss. The high final deflections indicated the complete destruction of the
jointing mechanisms. The steel joint deformed to lesser extent and profited more effectively from the protection it received from the exposed board material. Post experimental examination of the joint materials underlined the deflection findings. The GRP joints were in most panels severely damaged; delaminated pieces were common so that the integrity of the joint was impaired. When the GRP material was used as top joint these effects were minimised and the joint could maintain its original appearance and strength. The steel joints bowed when exposed to the heat but did not delaminate, showed any other signs of disintegrating. The joint pieces of the Camlock joint were completely consumed and no residuals were found.

**F) Plasterboard protection**
The differing thicknesses of plasterboard layers offered 40 to 90 minutes protection for 15 and 30mm respective plasterboard thickness, in temperature build-up as well as stiffness loss. All decomposition reactions, such as smoke emissions, were respectively delayed.

**G) Method**
The EN test method was shown to result in more severe exposure conditions, effecting early temperature rise within the panel. In the BS method the temperature reaction of the panel was delayed and the rate of temperature increase in each measuring interface was less marked. In addition to the influence of heating regime, the boundary restraint provided to the exposed board was also seen to be influential to the temperature readings. In areas of high restraint temperature development was less severe, whereas close to the centre of the panel the temperature build-up was most marked. Therefore any additional restraint to the board in the centre of the panel was beneficial to the overall temperature performance of the wall unit. Furthermore panel exposure was more severe at the top of the panel than in the bottom half. This is possibly due to extra heating generated by the rising decomposition gases.
Figure 5-1: An example heat transfer into sandwich panel wall section (Cape-Phenolic)

Figure 5-2: Temperature reaction behind exposed board of sandwich wall

* NO studs in Cape clad panels
Figure 5-3: Temperature reaction in centre of panel core
(All plain panel tests)

Figure 5-4: Schematic view of core temperature reaction

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Figure 5-5: Temperature reaction in wall at interface of core and unexposed panel face

Figure 5-6: Temperature reaction at ambient surface of plain panels

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Figure 5-7: Stud/plain interface temperatures Sasmox-PIR (Test 9)

Figure 5-8: Stud/plain interface temperatures Pyrok-PIR (Test 10)
Figure 5-9: Mean stud temperatures of all panels with studs
(Test 5 to 10)
Figure 5-10: Comparison plain and contiguously jointed panel temperature reaction behind exposed face in location I

Figure 5-11: Comparison temperature reaction between plain and contiguously jointed panels in centre of core, location I
Figure 5-12: Comparison plain and all jointed panel temperature reaction behind exposed face in location II

Figure 5-13: Comparison temperature reaction between plain and all jointed panels at centre of core, location II

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Figure 5-14: Temperature reaction of contiguous joint at top of panel

Figure 5-15: Comparison of temperature reaction between steel jointed section and plain panel section at interface of ambient board and core
Figure 5-16: Comparison of temperature reaction of GRP joint and plain panel section at interface of ambient board and core

Figure 5-17: Comparison of temperature reaction of all jointed panels at top of panel

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Figure 5-18: Comparison of temperature reaction of all jointed panels in lower part of specimen

Figure 5-19: Comparison of temperature reaction for different top joints
Figure 5-20: Temperature reaction of Sasmox clad jointed panel protected by 15 and 30mm plasterboard behind exposed face

Figure 5-21: Temperature reaction of Pyrok clad jointed panel protected by 15 and 30mm plasterboard behind exposed face
Figure 5-22: Deflection measurements plain panels with studs (Tests 5 to 10)

Figure 5-23: Deflection/ stiffness performance of jointed units in comparison with plain panel units

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Figure 5-24: Stiffness reduction in protected steel jointed panels

Figure 5-25: Stiffness reduction in protected GRP jointed panels
Figure 5-26: Comparison of panel reaction to BS and EN temperature exposure (Location A)

Figure 5-27: Comparison of panel reaction to BS and EN temperature exposure (Location C)
Figure 5-28: Comparison of panel reaction to BS and EN temperature exposure (Location D)

Figure 5-29: Influence of edge restraint
Figure 5-30: Comparison temperature reaction through depth of wall panel with and without additional internal studding (I)

Figure 5-31: Comparison temperature reaction through depth of sandwich wall panel with and without additional internal studding (II)
Figure 5-32: Influence of height (plain panels)
5.3 Bench-scale Fire Testing

5.3.1 Introduction to bench-scale investigation

This chapter presents the finding of the bench-scale study in which the detailed investigation into the factors influencing panel design, initiated in the intermediate-scale tests, was progressed. The bench-scale test was developed so that the failures of the sandwich unit were representative of larger scale. The objectives of the testing programme were to:

(i) investigate the modified Cone test method, i.e. influence of irradiance level, specimen dimensions and location of measurement,
(ii) confirm and establish in enhanced detail the effect of the heat on sandwich wall structures,
(iii) test replicate panels to assess the repeatability and variability of the characteristic temperature responses,
(iv) examine the effect of additional plasterboard cladding on the decomposition characteristics of structural sandwich walls,
(v) assess the influence of internal members, such as joints and internal veneer linking studs, on panel performance,
(vi) establish the heat reaction of the internally placed jointing members.

In the bench-scale test regime the panel test area is further reduced to 100mm², which is about one tenth the area of the intermediate-scale test sample. When compared to the intermediate- and later full-scale tests the bench-scale regime has two further significantly different features:

(i) the sample orientation in the test is horizontal,
(ii) the heat exposure is provided by an electrical heater.

The horizontal orientation of the sample in the bench-scale is adopted to ensure a purely radiation controlled heat exposure. In a vertically oriented bench-scale test the hot gases generated upon decomposition of the specimen have the potential to additionally heat the sample through convective heat transfer. In the horizontal orientation a heat environment can be created, which closely resembles the furnace exposure of the sample in which the main heat transfer mechanism is also radiative and the convective component is negligibly small. Whilst the furnace conditions are controlled by the supply of fuel to the burners, the heat exposure in the bench-scale test is generated by an electrical conically shaped heater, as discussed in Chapter 4.

Due to the heating arrangements and the size of the sample, the panel units remain observable during the test. This allows for a detailed examination into the effect of heat on the units and their specific failure mechanisms, both of which are of paramount importance to establishing the fire resistance of the units. The ability to terminate the bench-scale test at different test durations enables to examine the time related damage patterns to the sample before complete destruction has occurred. The observation of successive failure is not possible in a furnace-based tests since the installation of the
samples into the furnace opening limits the ability to monitor the failures during and after the test has stopped.

Although the testing with the modified Cone calorimeter set-up was undertaken after the trials with the standard set-up, the results of the modified testing are discussed first. Even though the standard Cone calorimeter testing regime was essential in determining the changes incorporated in the modified test regime, the use of the results is limited due to the edge burning failure mechanisms explained in detail in Chapter 4.

Within the modified Cone testing programme a set of experiments was undertaken to establish the optimised and most suitable bench-scale test condition for sandwich wall panels with respect to:

(i) irradiance level,
(ii) specimen perimeter and depth,
(iii) location of temperature measurements with respect to panel area.

After the optimum test conditions had been evaluated the sandwich panel testing was progressed. In the subsequent section various sandwich wall material combinations and their temperature build-up and failure pattern is evaluated. This investigation into the primary panel materials concludes with the temperature reaction and failure characteristics of the sandwich wall bench-scale units protected by sacrificial plasterboard. The previous test schemes, intermediate- as well as bench-scale, had only evaluated the temperature reaction of the internal core material when covered by the dense board layer. In a separate test scheme the temperature reaction of the two main core materials, PUR and PIR, without the shielding layer was assessed as this was felt to yield important information about the decomposition signature of synthetic foams, influential to the overall failure pattern of the layered panel. The separate testing also established whether the modular testing of single materials enabled to piece together the three-layered panel performance, as suggested by Grenier et al. (1997).

The bench-scale investigation also determined the characteristic temperature and failure responses of sandwich wall units including internal members, i.e. jointing and internal veneer linking studding. The results examine the temperature reaction of GRP and cold-formed steel joint and also establish the characteristic failure and temperature reaction pattern for internally linked panel units. The findings of the bench-scale evaluation are compared to the characteristic temperature findings in the intermediate-scale tests at the end of the section.

In a final sub-chapter the findings of the standard cone set-up programme are presented. Despite the fact that the results are only of limited meaningfulness due to the misrepresented failure signatures of the specimens, the comparative tests establishing the heat release and mass loss rate of the different sandwich systems is of value to the investigation. Reaction-to-fire performance characteristics are not evaluated in fire resistance tests and are crucial to examining the contribution of sandwich wall panels to the fire hazard in a room fire. The results presented here are only indicative. After the
initial tests in the standard set-up had exhibited the problematic premature involvement of
the core emphasis of the tests shifted to observing failure mechanisms of the samples
when exposed to the heat flux. The measurements at that stage were secondary and the
tests were stopped at varying time steps to observe and analyse the damage. The failure
characteristics observed in the standard set-up are supplemented to the modified Cone
findings where appropriate.

5.3.2 Investigation into the boundary effects of the modified Cone test
Since the cone set-up had not been used in this application before a test programme was
undertaken to investigate the test’s boundary conditions on the temperature reaction of
the layered sandwich wall units.

5.3.2.1 Irradiance level
The effect of irradiance level on the temperature distribution within the panels is shown
in summary in figures 5-33 and 5-36 and in greater details in Appendix V. The Sasmox/
Pyrok-PUR samples were subjected to three irradiance levels:
(i) 35kW/m²
(ii) 50kW/m²
(iii) 70kW/m²

The sandwich units’ temperature reaction was monitored at the usual three interfaces
(i) behind the exposed veneer (A),
(ii) in the centre, of the core (B),
(iii) at the interface of core and ambient face (C).

Three replicates of Pyrok and Sasmox-PUR samples were tested at each irradiance level.
The meaned temperature response for each material combination at varying depth and
irradiance has been plotted in Figures 5-33 and 5-34 respectively. Figures 5-35 and 5-36
summarise the temperature reactions through the depth of the unit by plotting the onset of
stage II, sharp temperature rise, for each irradiance level.

In both panels the increasing irradiance level shortened the reaction times within the
sample, i.e. the onset of stage II of the temperature curves. The delay in temperature
response behind the exposed veneer with descending irradiance level ranged between 2
and 3 minutes. Once the sharp temperature rise was initiated in one panel depth the rate of
temperature increase in this position was similar regardless of irradiance level. The
temperature reaction through the depth of the panel was linear at 50kW/m² for both
material combinations and non-linear at 35 and 70kW/m². This was thought to be linked
to decomposition dynamics, which were greatly affected by the extent of core damage
behind the exposed veneer. These effects were particularly severe at 70kW/m² where
local burn through at the sides of the sample were observed. Here the failure of the
protective insulating edges around the 100mm² test surface terminated the tests. The high
heat level fuelled the consumption of material outside the directly heated areas so that the
decomposition of the panel was more extensive and progressed quicker throughout the
depth of the unit. This was mirrored in the temperature reaction by an over-proportional
temperature rise throughout the panel depth at 70kW/m², as seen in figure 5-35 (for Pyrok-PUR material combination). Although no burn through failure was apparent at 35kW/m², due to the much reduced heat input, similar uneven burning reactions behind the exposed veneer are likely. At 50kW/m² the temperature distribution through the depth was regular, which made this irradiance level best suited for the bench-scale test programme. Overall the Sasmox faced sandwich unit exhibited delayed and less severe temperature reactions in all three measuring positions and at all three test irradiances, which confirmed the findings of the intermediate-scale test work.

5.3.2.2 Specimen dimensions
The optimum specimen dimensions were analysed with respect to

(i) specimen width
(ii) specimen depth

A) Specimen width
Preliminary tests with the standard IOOMM2 sample had exhibited the premature involvement of the core at the outer edges of the sample. In the standard test regime the core material was heated behind the board and emitted combustible gases, which escaped around the sides of the sample and ignited in the test environment. It is thought that the heat transferred from the steel pan and edge frame was also influential to this premature failure. In figure 5-37 the extent and dynamic of core failure just before the unwanted ignition around the edges of the sample is shown. The heat transferred through the board caused the foam to decompose from the centre towards the, by the edge frame protected, edges. This type of failure misrepresented the failure in larger scale where the core was consumed through cracks in the exposed face board and underlined the importance of specimen size to represent adequate edge conditions. Figure 5-37 compares the temperature response of Pyrok-PUR sample as measured in a standard Cone (100mm² and 50mm deep) sample with the temperature reaction of an enlarged 200mm² and full depth sample. The temperature reaction shows the over proportional heating, premature and more severe temperature build-up in the smaller, traditional sample. In figure 5-38 the effect of the protective non-heated perimeter around the testing area is examined in further detail. For the two board materials, Sasmox and Pyrok, two replicates were tested with reduced edge length of 150mm instead of 200mm, here only the Pyrok clad specimen reactions are shown. The change in sample perimeter reduced the unheated, sealing edges around the 100mm² test area to 50mm (rather than a 100mm as in the 200mm² sample). Whilst the temperature reaction behind the exposed veneer was practically identical for both sample sizes, the core temperature was found to be more marked in the reduced width specimen. This more severe temperature build-up was in principle caused by the same failure principle encountered in the standard specimen, which was tested without any insulating strip around the edges. In the reduced sample the insulation strip around the tested volume, as seen in figure 5-38, was seen to burn-through. This enabled the access of a proportionally higher amount of oxygen to the burning core substrate, which is likely to have affected the severity of consumption and thereby the temperature level within the core. In summary the results show that an
insulation strip is needed when testing structural sandwich panels in a bench-scale set-up, due to the likelihood of edge burn through failure of the foam. Although the reduced edge protection strip does not affect the temperature response of the units at the start of the test it increases the likelihood of insulation failure at the perimeter of the test volume and has the potential to cause over proportional burning towards the end of the test. This edge condition misrepresents the failure signature of the layered units in larger scale, where the edges are sealed from direct air access and should be avoided.

B) Sample depth
The effect of sample depth is shown in figure 5-39. The temperature reactions of a full depth Sasmax sample, incorporating both boards either side of the core are compared to a reduced sized sample of 50mm overall thickness, incorporating one full-depth board (the exposed board) and 40mm of core. The mean temperature reactions recorded behind the exposed veneer were similar in both samples, suggesting that the thickness of core does not affect the temperature response behind the board. However, the core temperature measurement in the reduced thickness sample was marginally higher which is to be expected since the core depth in front of the measuring thermocouple was reduced to 20mm rather than 35mm as encountered in the full-depth sample. Accounting for this reduced core the differences between both measurements were marginal.

5.3.2.3 Measurement locations
The variation in temperature development throughout the 100mm² irradiated surface area is examined in figures 5-40 to 5-42. In each test five thermocouples were placed at one specific depth, location A, B or C, and moved in 10mm steps from the centre of the sample to the outer edge of the irradiated 100mm² surface.

Figure 5-40 plots the temperature response at measurement depth A, behind the exposed veneer. The first three temperature measurements, from the centre of the sample towards the outer perimeter of the test area, showed similar responses. Here both the onset and rate of temperature rise were consistent. The temperature measurement placed 30mm away from the centre of the irradiated area, measured distinctively different temperature course, with delayed onset of stage II, reduced rate of temperature increase and lower final temperature. This temperature response was further distorted on the outer perimeter of the 100mm² surface, where the measurement did not record any sharp temperature rise and an overall temperature of 100°C at the end of the test. In comparison the temperature in the centre of the sample had reached temperatures of 400°C at the end of the test. This variable temperature reaction was related to the extent of damage encountered within the panel, similar to the effects observed under high irradiance levels. Behind the exposed veneer the damage was restricted to an area of about 50mm² diameter. In deeper panel layers (measuring depths B and C) the variation in the temperature reactions was not as marked and with increasing distance from the exposed face the temperature response was more unified, regardless of the location of the measurement within the area. This was true to the extent that in depth C lower overall temperature levels could be encountered close to the centre of the irradiated area rather than at the outer sides of the test area.
Temperature reaction and degradation signature are correlated and figure 5-43 summarises the temperature distribution within the panel. The findings also guide on the location of temperature measurements in order to avoid location influenced boundary effects. In the modified set-up should be positioned within a 50mm radius from the centre of the test area.

5.3.3 Plain panel tests in modified Cone set-up
Six material combinations were tested under 50kW/m² with the modified test set-up (also overviewed in Chapter 4: Table 4-9);

(i) full-depth sandwich wall panels, where two faces clad the core
   1- 10mm Sasmox veneers on a 70mm PUR core
   2- 8mm Pyrok veneers on a 70mm PUR core
   3- 11mm OSB veneers on a 100mm EPS core

(ii) 50mm overall depth panels with only one veneer and part of core
   1- 10mm Sasmox veneer on 40mm PIR core
   2- 8mm Pyrok veneer on 42mm PIR core
   3- 12mm Fels veneer on 38mm PIR core

The units were instrumented with five thermocouples (TC), varying in depth and location. Identical to the intermediate-scale test thermocouples were located

(i) behind the exposed veneer- location A (2 TC per test),
(ii) in the centre of the core- location B (1 TC per test) and,
(iii) at the interface of core and unexposed face - location C (2 TC per test).

To avoid temperature edge effects the measurements were located in a 50mm radius from the centre of the sample. A maximum of six and a minimum of three replicates were tested for each material combination. This enabled to assess the variability in the temperature and failure reaction of the units; a great advantage when compared to the intermediate- or full-scale evaluation, where only one replicate of each material combination could be tested.

The modified bench-scale test method enabled to make the important correlation between the failure progression and the temperature reactions within the layered structure. The combination of both wall reactions allowed gaining an understanding of the various temperature dependent decomposition stages of sandwich walls in fire situations. The subchapter on the plain sample tests presents the temperature reaction and degradation characteristics of the various material combinations assessed in the scope of the programme. Detailed observations of the temperature dependent failure characteristics in each material combination are reported. The temperature reactions of the samples in bench- and intermediate scale are correlated; the specimen performance in both scales is discussed schematically.
5.3.3.1 Temperature reaction and variability of response

The temperature responses of the various material combinations tested in the modified cone bench-apparatus are shown in figures 5-44 to 5-45 and in more detail in Appendix V. Similar to the findings in the intermediate-scale the specimens' bench-scale test temperature response also exhibited a three-stage response to the heat exposure. The onset times and overall temperatures reached in the different panels are shown in table 5-5. The Sasmox clad panels exhibited the most varied temperature response behind veneer, whereas the Pyrok clad units reacted more consistently to heat environment in this measuring position. In the core temperature development this was reversed; here the Sasmox clad cores reacted more predictably. This was thought to be linked to the cracking pattern of the exposed veneer (see sketch in table 5-5) and is described in further detail later. The PIR core temperature reactions cannot be compared since the reduced sample depth shifted the measurements, which were recorded at different distances from the exposed face. Furthermore the reduced number of replicate samples makes the commentary on marginal differences less meaningful. In all panels the temperature behind the exposed veneer at the onset of stage III was similar at about 350 to 400°C, which was related to two test conditions

(i) the irradiance level,
(ii) the heat permeability of the exposed board material.

The heat permeability of the exposed board layer was the governing factor since the change in irradiance could be established to be of minor influence to the overall temperature level reached in stage III, as seen in figures 5-33 to 5-34. Since the exposed board layer shields the internal of the sandwich wall, the effect of its removal has a major impact on the temperature development, as seen in figure 5-46, where the temperature development in a Pyrok clad PUR core with two board conditions is compared. In the first panel the board remains undisturbed during the test period and apart from the damage induced by the imposed heat flux, the board remains intact. In the second panel the exposed board is destroyed in a perimeter of 50mm² in the centre of the test area, just subsequent to the ignition of the samples. As the board was removed the temperature measured behind the exposed veneer and in the centre of the core picked-up, increasing at a considerably increased rate when compared to the shielded case. Upon destruction of the board the temperature at exposed board and core interface increased nearly simultaneously to the core temperature. Especially in the core the rate of temperature increase tripled and exhibited temperature differences of up to 200°C between the broken and unbroken panel. This rise in temperature was related to the mode of core consumption, which altered from a restricted slow burning process through the cracks of the exposed board to a severe flaming combustion once the board was removed. The access of abundant oxygen fuelled this severe combustion.

Compared to the mineral board cladded PUR/PIR panels the samples consisting of wood-based OSB veneers on EPS cores exhibited a distinctively different reaction to the heat environment and could only be tested for a maximum time of 10 minutes. The ignition and subsequent violent burning of both board and core material consumed the samples rapidly so that they needed to be removed from underneath the heat source. The
temperature response in location A, as shown in figure 5-44, could be subdivided into mainly 2 stages and with only a slight delay of 1 minute the onset of stage II in the centre of the core (location B) was observed. The temperature response was consistent in all three tests. The OSB-EPS results underline the impact of the internal core material on the severity of the temperature build-up within the wall layer. The EPS, although tested at increased thickness degraded instantly once the heat had penetrated through the exposed board layer, whilst the PUR (PIR) filled panels exhibited a time delay, which was consistent and reproducible regardless of board material.

The specimens tested with PIR core were not full depth samples but reduced to 50mm overall depth. Comparing all three board materials with reduced depth PIR core in figure 5-45, the Fels clad samples exhibited the latest and most moderate rise in temperature. The time span between the onset of sharp temperature increase behind the exposed face and in the core (Location B) was generally consistent in all three material combinations, which is related to the decomposition speed of the core material.

The specimens tested were not full depth samples but reduced to 50mm overall depth. Comparing all three board materials with reduced depth PIR core in figure 5-45, the Fels clad samples exhibited the latest and most moderate rise in temperature. The time span between the onset of sharp temperature increase behind the exposed face and in the core (Location B) was generally consistent in all three material combinations, which is related to the decomposition speed of the core material.

The intermediate-scale tests have shown that the backing core material influences on the temperature reaction behind the exposed veneer, and thereby the core is seen to affect the degradation behaviour of the exposed board. The bench-scale test was regarded well suited to investigate this matter further and in a set of plain tests the temperature build-up behind a Sasmox and Pyrok board when (Figure 5-47) backed by a highly heat conducting 50mm mild steel plate was established. When the non-combustible steel backed the boards the temperature response was distinctively different compared to PUR and PIR backed board materials. When the board was backed by a PIR core the temperature response was most severe for both boards. The PUR backed boards showed a similar response pattern to the PIR cored samples but the onset of stage II was slightly delayed, hence less critical. In the steel backed samples the temperature behind the veneer rose gradually at constant rate, without an instantaneous sharp temperature increase at the 100°C threshold level. These comparative tests reinforced the intermediate-scale findings where the temperature response of the exposed board layer was also seen to be affected by the backing core material. Two core characteristics are deemed to be influential to the temperature response of the board and hence to the degradation of the entire wall unit;

(i) the conductivity of the core,
(ii) the decomposition characteristics of the core.

The temperature build-up behind the exposed veneer is reduced when a highly conductive material is backing the board. Here the incoming heat is transported from the board layer, whereas when a highly insulating core is employed the heat is congested at the interface of board and core. This not only affects on the degradation of the board but also initiates the premature degradation of the first core layers. After the initial heating of the wall the threshold degradation temperature of the core degradation is of major importance. The findings suggest that the more fire resistant PIR accelerated the temperature build-up in the wall.
5.3.3.2 Comparison to intermediate-scale temperature reaction

Drawing 5-15 summarises the respective characteristic temperature development in both testing scales in location A for both test scales, shown in further detail in figures 5-48 and 5-49. The three main characteristics of the sandwich wall temperature response are:

(i) the onset of stage II
(ii) the rate of temperature increase in stage II
(iii) the overall temperature level reached at the onset of stage III

The comparison shows that for both board materials the onset of stage II could be well predicted by the bench-scale test. The test results for both backing core materials, i.e. PUR and PIR, are included in the comparison as their behaviour in the intermediate-scale test was indistinguishable similar and therefore suggested the levelling out of differences between both materials the larger the scale of test. Although the Sasmox board exhibited slightly earlier temperature response in the bench-scale tests when backed by a PIR core, its temperature response in both combinations wraps the range of reaction times encountered in intermediate-scale response at varying locations on the panel area. This was also found in the comparison for the Pyrok board. However in all cases the bench-scale test predicted the onset times of the temperature reaction within 2 to 3 minutes of the measurement in the intermediate-scale.

Although the onset of the temperature response was well predicted, the rate of temperature rise and overall temperature level reached in stage II was only in parts correctly modelled by the Cone set-up. The temperature rise encountered in the bench-scale samples shortly after the onset of stage II matched the rate of temperature increase encountered in the intermediate-scale sample, but the temperature rise soon levelled off. With a measured maximum temperature level of about 400°C, the bench-scale results
were about 200 to 300°C below the maximum temperatures encountered in the intermediate-scale tests. However, the temperature reaction within the intermediate-scale test specimen was varied depending on the measurement location, as discussed earlier. The temperature levels measured in the lower parts of the intermediate-scale samples were seen to be marginally below the temperature reaction measured in the Cone so that the prediction was much more accurate for the well restrained areas in the intermediate-scale sample.

The characteristic temperature responses in the centre of the core as measured in the bench- and intermediate-scale samples are shown in generalised form in drawing 5-16. The bench-scale temperature reactions are shown for two irradiance levels, 50 and 70kW/m². Figures 5-50 and 5-51 show the detailed comparison.

Drawing 5-16: Characteristic temperature response in centre of core in bench-scale under varying irradiance levels and in intermediate-scale

The irradiance level, regardless of material combination, affected the temperature response in the centre of the core. The higher the irradiance the earlier and marked the sharp rise in temperature. Despite the differences in temperature build-up behind the exposed veneer depending on the backing core material (PUR/PIR), the onset of stage II in the core temperature development was uniform. This could only be assessed for the Sasmox clad specimens since the Pyrok/PIR sample was only tested at reduced depth. As observed behind the exposed veneer the overall temperatures reached in the bench-scale samples did not match the intermediate-scale temperature levels. Here the overall temperature level in the Sasmox clad PIR core was marginally below the temperature levels encountered in the PUR cored sample. However, the onset of temperature response was well modelled under the 50kW/m² irradiance level, whereas the 70kW/m² irradiance
level evoked a premature temperature response. The prediction of core temperatures was very accurate through the bench-scale test. Although the reaction times were brought forward under 70kW/m² irradiance, due to the more severe exposure, the rate of temperature rise in stage II did not adjust to the rate of temperature increase encountered in the larger scale test. This was thought to be due the horizontal orientation of the test sample in the bench-scale set up, which did not cause breaking away of board pieces and other secondary failure effects, present in a vertically oriented sample. The effect of suddenly falling board pieces on the temperature reaction within the panel this breaking away of board was manually simulated was shown in figure 5-46. This tests with the manually destroyed samples it was shown that the rate of increase in stage II was affected by the secondary failures to the board, whereas the onset of stage II was dependent on the material performance of the exposed board layer. The 50kW/m² exposure modelled this onset time very accurately, backing work by Silcock and Shields (1992), which evaluated the 50kW/m² irradiance level to be modelling the furnace exposure in the first 30 to 40 minutes of standard fire resistance test.

5.3.3.3 Visual Observations
Figures 5-52 to 5-57 combine the temperature reaction of the various material combinations with the failure observations recorded during the tests. The open configuration of the test set-up and the confined heat application allowed for the monitoring of the sample during the tests, which were related to the recorded temperature levels through the test time.

All mineral based boards cracked upon heat exposure, whilst the wood based OSB ignited. The Sasmox board surface changed in colour after 1 minute exposure from its original white to pencil grey/black colouration (Figure 5-52). At about 5 minutes some evenly distributed surface cracks appeared. At 7-8 minutes the cracks opened up and became more apparent. In the Pyrok clad panels the surface of the board had cracked shortly after the start of the test. The cracks were evenly distributed in the first minute of their appearance but were seen to develop in localised deeper cracks with ongoing irradiation (Figure 5-53). This was different to the Sasmox board, where cracks developed later and were evenly distributed over the surface and no localised deeper cracks were observed. In the Fels board the cracking was delayed further and was not as pronounced as in the other two mineral-based boards, Sasmox and Pyrok. Furthermore the cracks did not seem to open up to the same extent. Whilst the initial surface cracking of the exposed mineral based boards was not affected by the backing core material, the speed of crack progression and the opening of the cracks was accelerated for the PIR cored specimen. The effect of backing core material on the cracking of board layers can also be appreciated in figure 5-47, where the steel backed Sasmox board shows less extensive and deep cracking when compared with the PUR filled sample. The through depth cracking of the board aired gases, which were generated by the degrading core below the board. These gases were of distinct smell and preceded the ignition of the samples with mineral based board layers. This stage of failure coincided with the onset of stage II behind the exposed veneer.
The exposed wood board produced smoky gases as soon as the sample was placed underneath the heater. Within the first 50 seconds the wood surface ignited. After 1 minute the entire OSB surface was covered in flames, but the flame height soon decreased and the wood surface was left charred, expanding towards the heater but at the same time shrinking in its plane (see figure 5-54).

In all mineral based board the cracking was accompanied by sizzling noises, which were probably generated by decomposition gases escaping from the Aluminium foil protected edges of the sample. In some of the Sasmox clad panels the board was observed to bow towards the heater just prior to the sizzling noises. At the same time as onset of the sizzling noise a strong antiseptic smell could be sensed. Both, the sizzling noise and the distinctive smell were observed just prior to the onset of sharp temperature rise behind the exposed veneer at about 100°C. Soon after the onset of the sharp temperature rise white smoke emitted from the cracks, which promoted the ignition of the sample. In the PIR cored specimen ignition was delayed and was observed after the onset of stage II temperature response behind the exposed veneer (Figure 5-55). Figure 5-55 shows a typical Sasmox-PIR unit after ignition had occurred. The exposed board is removed from the sample so that the extent of core damage at this stage of the test can be appreciated. Once the panel had ignited the gas/ smoke emission turned to black. After ignition the smell emitted from the specimen changed from antiseptic to burnt notion. The Fels-PIR samples did not ignite (Figure 5-57). In these samples the cracks were observed to glow, intermittently pulsating in red/ yellow colour. All tests needed to be stopped after about 20 minutes, as gases were escaping through holes formed upon insertion of the TCs through the unexposed side of the sample. In one test the TC was pushed out of the sample under great pressure.

In the OSB-EPS panel a considerable amount of smoke emitted from the sides of the sample, where the core had receded from the face veneer. The core melted and formed flaming droplets, which puffed out of the gaps, formed between the exposed veneer and core glue-line (figure 5-54). In some cases the exposed boards just fell down, as the core below had been completely consumed. This could be as soon as 10 minutes after the start of the test and with at that time the board had charred and deep fissures had formed.

A) The effect of heat on core material, core decomposition at cracks
Figure 5-58 examines the effect of the heat on a PUR core layer behind the exposed veneer in two stages of degradation: veneer is heated but not cracked and veneer manually cracked. In the un-cracked sample, the foam layer expands upon heating, especially in the layers closest to the heat exposure, hence at the interface of exposed face and board. At the interface the glue line is transformed to tarry, liquid material of about 1-2mm thickness, as seen in figure 5-58. With increasing distance from the interface the foam layers change in colour and consistency. The originally grey, stiff PUR foam alters to brown and the rigid foam structure changes to soft and flexible. The samples with the manufactured crack were thought to render especially useful observations with respect to
the foam performance. The crack was manually inserted into the 100mm edge perimeter sample and reached from the outer edges of the sample. In the area of the crack the foam changes its consistency and softens upon direct exposure. The consumption of the core along the crack causes ignition and behind the intact board halves the heating softens the core. Both effects, the consumption of core material at the crack and softening of the core in protected areas cause the board halves to sink in. As observed before, the foam changed in colour ahead of the direct combustion zone, which was linked to the softening. Once the core was removed from underneath the heat source the softened core layers regained in parts their rigidity, although the foam structure appeared to be more brittle, especially where the colour change had occurred.

5.3.4 Modified bench-scale tests on plain samples with plasterboard

After having successfully experimented with the plain sandwich units it was important to use the bench-scale tool to gain an understanding on how sacrificial layers of material affected the temperature response within the units. The Sasmox and Pyrok-PUR units were therefore covered with 15min standard plasterboard on their exposed face. The monitoring thermocouples were placed, as before, behind the veneer, in the centre of the core and at the interface of unexposed veneer and core. The 15mm plasterboard layer placed over the sandwich samples was found to delay the temperature response of the specimen. To assess the effect of falling off plasterboard, as commonly encountered in vertically oriented wall tests, the plasterboard protection was removed from the samples’ surface at set times.

5.3.4.1 Temperature response

Drawings 5-17 and 5-18 compile the general temperature effects encountered in the plasterboard protected bench-scale samples, in further detail in figures 5-59 and 5-60. The removal of the plasterboard, as encountered in larger vertically oriented tests, had two main effects

(i) reduce the time to sharp temperature increase behind exposed veneer,
(ii) accelerate the temperature build-up in deeper panel layers.

The time to onset of stage II behind the exposed veneer (location A) was about 5 minutes shortened when compared to the onset of temperature increase in the plain sample. The amount of delay encountered in the onset of sharp temperature gain is dependent from the time until the protecting plasterboard layer remains cladding the panel, as seen in drawing 5-17. The longer the plasterboard remains cladding the sample the shorter the time to onset of stage II in the sandwich panel. Without the sudden removal of the plasterboard the temperature was seen to increase steadily and no sudden conversion into stage II could be observed in the panel.
Similarly the time gap between the temperature reaction behind the exposed veneer and the centre of the core was shortened in the samples once the plasterboard had been removed (Drawing 5-18). Although the rate of temperature increase behind the exposed veneer was similar to the one measured in the non-protected sample the rate of temperature rise in the centre of the core was markedly increased. This suggests that the panel had been preheated behind the plasterboard.
At the interface of core and unexposed face no marked temperature changes were observed. Once the plasterboard had been removed from the sandwich wall surface white smoke was emitted from the sample, which ignited in seconds. In the Sasmox clad sample smoke also emitted from the isolative, unheated protective edges around the perimeter of the sample, which indicated the foam had been consumed beneath major parts of the 200mm² surface of the sample. The test then needed to be stopped. This extended damage was due to the long and even heating, which was facilitated behind the plasterboard.

5.3.4.2 Visual observations
The temperature behind the exposed Pyrok veneer was reaching the threshold level of 100°C, 25 minutes earlier than in the Sasmox sample (see figure 5-59 and 5-60). When the plasterboard was removed from the Pyrok samples it could be observed that the Pyroks surface had crazed, with some deeper cracks. The cracks extended and deepened further within seconds white smoke was emitted, which ignited the sample. In both material combinations a light antiseptic smell was perceived at about 20 minutes. In both tests the plasterboard layer showed similar reactions to the imposed heat flux. Within the first seconds of the test the board heavily emitted white smoke, possibly vapour, which ceased with the ignition of the paper surface of the board. The paper ignited at about 60 to 70 seconds, within 10 seconds the flaming stopped and the paper was charred and torn on the first white layer of plasterboard. The plasterboard showed first signs of cracking after
about 15 minutes. Towards the end of the tests the cracks in the plasterboard deepened and opened up to about 1-2mm.

5.3.5 Modified bench-scale tests on core materials

5.3.5.1 Temperature
Following the investigation on the entire sandwich units the programme focussed on assessing the foam on its own, detached from the board material. This was regarded important not only to observe characteristic core failure behaviour but also with respect to the similarities, differences in decomposition behaviour of the foam material with and without shielding layers, in the real panel provided through the board material. The outcome of this regime also enabled to give advice on whether the testing of isolated panel components could be assembled from independent tests to determine the overall performance of the layered sandwich panel unit as suggested by Grenier (1997).

Three replicates of PUR and PIR foam were tested under an irradiance level of 20kW/m². The irradiance level was reduced so as to prevent the sudden, accelerated burning of the foam material and allow the test to continue so that temperature measurements and observations could be recorded. The samples were overall 50mm thick and instrumented with 5 thermocouples, distributed in the depth of the sample (see also sketch in figure 5-61). The TCs were placed at 10mm intervals, starting from 10mm beneath the exposed surface. Figure 5-61 compares the temperature measurement at the varying depth for both core substrates. In all these measuring positions there was a cross over point, where the initially slower rising PUR temperature, rose above the temperatures encountered in the PIR material. As the TCs were located further from the exposed face the time until the temperature cross over was delayed and the rate of temperature rise was less, which was related to the char formed in the exposed layers. Figure 5-62 shows the EPS core material after several seconds of exposure. The failure performance of the thermoplastic EPS was seen to be very different from the charring PUR/ PIR performance. In the standard Cone set-up the EPS was tested without the cladding board and once the EPS got involved it practically evaporated and only liquid drops of residuals remained.

5.3.5.2 Failure observations
Figures 5-63 and 5-64 also record the observations made on the decomposition behaviour of the core. Both foams heavily smoked once placed underneath the heater but no ignition was observed. The PUR performed different to the PIR. Upon heating the PIR foam a sizzling noise was generated and the surface seemed to boil, as the minute air bubbles enclosed in the foam’s structure expanded ruptured the cell walls of the foam. The area affected by this form of decomposition was restricted to about 25 to 50mm². After 1-2 minutes test the exposed foam surface charred and the smoke emissions were reduced. In some samples a skin formed which enclosed a gas/ air bubble of about 10mm in diameter. However this was not observed in all samples. Figure 5-63 shows a typical PIR sample after termination of the test. Despite commonalities in behaviour between the two cores, the degradation reactions of the PUR were observed earlier and more pronounced. The visual boiling effect was over the entire 100mm² surface and within 60
seconds a bubble of charred foam skin, had formed spanning the entire test surface. At around 8 minutes the bubble deflated and the black charred skin lowered into a crater. In some tests the core had expanded as it became more flexible and had deformed at the back of the sample. Overall the PUR emitted less smoke than the PIR.

The sample surfaces for both materials can be compared in figures 5-63 and 5-64. Upon decomposition the PIR produced a rough surface as the single burst air pores charred, whereas the PUR surface formed of a smooth skin. Beneath the initial layer of burnt material the core exhibited differing stages of decomposition. The foam changed from coherent lightweight char with black appearance on the exposed surface to a dark-yellow/orange colour (also shown in figure 5-61). In the foam layers just below the exposed charred surface the structure of the foam was still intact although the heating had changed the rigidity of the foam. With increased depth the foam colour and structure gradually converted to the original virgin foam. The heavily charred and decomposed region of the core was a maximum of 3mm apart from the virgin material layers. When the foam was tested behind a board layer the decomposition characteristics of the respective foam layer were different as shown before in figures 5-58. The damage was less extensive and a tarry glutinous material was generated in the first core layers, which promoted the ignition of the sample. Once the foam had ignited behind the veneer and a solid char layer was formed the principally different appearance of the char between the PUR and PIR remained. The differences in decomposition between both foams are thought to have an effect on the board layer when tested in the sandwich configuration.

5.3.6 Modified bench-scale tests on the effect of internal panel members
The bench-scale test programme also assessed the two contiguous jointing options of System 2, the cold-formed steel and GRP section. The tests were undertaken to gain an understanding on the joint materials' fire behaviour but also to assess the interaction of sandwich panel and joint insert under heat. In a second test series the temperature and failure behaviour of the internal steel studs was examined. The stud units were placed in a Pyrok-PUR sample.

5.3.6.1 Temperature development
A) Joints
To examine the failure behaviour of the GRP section a plain Sasmoxy-PUR specimen was prepared so that the GRP joint would partially replace the foam-board interface in the test area (Figure 5-65). The foam and unexposed veneer were removed from the exposed face in the area of the joint and GRP section inserted. The section was 200mm long and therefore reached to the outer edges of the sandwich test sample. The 76mm wide GRP section left two strips of 12mm foam at each side of the joint in the 100mm² test area. In one test the effect of board falling off the joint was assessed and an area of 50mm² of the exposed board was removed after 40 minutes into the test. Three replicates of the joint section were tested inserted into a Sasmoxy-PUR panel and one GRP section was tested when inserted into a Pyrok-PUR panel. Drawing 5-19 shows the general temperature response of a GRP jointed sample to the heating regime.
The temperature build-up behind the exposed veneer was measured at two locations (see also figure 5-65): At the interface of board and cored part and at the interface of board and the surface of the GRP joint. In both locations the increase in temperature was similar although marginally less severe in the area of the joint, which was thought to be related to the different conductivities of the GRP and core material. Due to the additional insulation provided through the thickness of the fire resistant GRP joint the heat build-up in the exposed flange of the joint was much reduced. The temperature measured at the back of the exposed flange was about half of the temperature influx measured at the interface of exposed board and joint. The temperature measured in the centre of the joint was further reduced. This was expected since the hollow joint was open to ambient air at its sides. This makes the measurement less representative for larger scale performance, where the top and bottom of joint would be enclosed within the wall panel and closed off from ambient air. Figure 5-66 shows the influence of shielding exposed board on the temperature development. As observed throughout the investigation the joint temperature development, too was influenced by the board material. The thicker gypsum-based Sasmox provided enhanced protection, which resulted in delayed temperature increase and overall lower temperature levels throughout the section. When the GRP section was incorporated into a Pyrok-PUR panel the temperature in all measuring positions rose at an increased rate.

The Sasmox-PUR unit was also tested as a steel-jointed section (Figure 5-67). Here the panel parts were connected by a protruding cold-formed steel section, which mated into a female recess (sketch in Figure 5-67). The female part of the joint was cast too deep, which resulted in an air gap being formed between the steel section and the recessed foam upon assembly. This was not influencing the temperature measurements, which were taken from within the sample in the cored male joint. When the sample was aligned in its
The final configuration the board edges on the exposed face were about 2mm apart. The steel section was 200mm long and therefore reached to the outer edges of the specimen. Thermocouples were installed to monitor the temperature of the hot and ambient steel flange and the core temperature within the jointed area. In one test about 50mm² of the exposed board faces were removed to assess the influence of falling board pieces on the temperature reaction of the units. Figure 5-67 plots the temperature reaction within the steel jointed bench-scale sample. The exposed flange temperatures are marginally above the core temperatures and generally exhibit a two-stage behaviour. When the board faces are removed a sudden temperature increase in the exposed flange and in the centre of the core is noted.

The temperature development in the steel joint and the GRP joint were similar, although the steel temperature reactions were marginally more severe as can be seen in figure 5-68. It has to be acknowledged that the steel joint was more severely exposed as the board shielding the joint was not overlaying the exposed flange of the joint in one piece, as constructed in the GRP section. The gap formed by the aligned panel halves is thought to have increased the heat exposure of the joint. Another vital difference, which is an inherent rather than a construction issue, is the fact that the GRP section is hollow, whilst the steel section is foamed into the panel. Therefore the core temperature development in the steel-jointed sample is thought to model the larger-scale test condition. The heat build-up in the core was influenced by the amount of board gap opening during the test.

B) Studding units

A second test series evaluated the temperature reaction of sandwich units incorporating internal steel stud units. Six replicates of I-shaped studs embedded in a Pyrok-PUR section were subjected to a heat flux of 50kW/m². The internal steel studs were centred behind the exposed board and 200mm long so that they reached through to the sides of the specimen. The temperature measurements were located to

(i) monitor the temperature rise in the exposed flange (2 TCs)
(ii) monitor the temperature rise in the core of the sample (1 TC)
(iii) monitor the further temperature development of the ambient flange (2 TCs).

Figure 5-69 compiles the temperature readings for all six tests. In all tests the exposed flange heated up gradually. The cracking of the board was observed at identical times, although it extended beyond the directly heated area to the outer edges, following the internal studding unit. The rate of core temperature increase in both stages was only marginally different and the conversion into the second stage of the response occurred at varying times. The core temperature levels observed in the studded sections were similar to the ones observed in the plain panel sections and within the observed variability of the Pyrok-PUR material combinations. The ambient flanges gradually heated up to 60°C when the test was stopped.

In one test the exposed board was manually destroyed in a perimeter of about 50mm² after 10 minutes test (Figure 5-70). As observed before in the plain panel test the
temperature within the units increased immediately. Figure 5-70 compares the temperature development in both samples. In general the temperature reaction of the ambient flange was not as severely affected by the destruction of the board. However, enough time provided the area and extent of board removal would have an impact on the temperature development of the ambient stud flange, similar to the increased temperature gain in the exposed flange. This would become especially likely when the decomposition of the core material would convert to severe flaming combustion so that the insulation of stud would gradually reduce and exposing a constantly increasing area of stud.

5.3.6.2 Comparison to intermediate-scale test results

A) Joints
Figures 5-71 to 5-72 compare the temperature development of the joints in the intermediate-scale and the bench-scale tests. Although the steel joint tested in the bench-scale exhibited a similarly slow gradual rise in the first minutes of the test, it did not convert into the sudden, rapid temperature increase encountered in the intermediate-scale sample. Figure 5-72 compares the temperature development in the GRP joint. Although the bench-scale test could model the difference heating rates of the exposed flange of the joint as dependent from the shielding board it did not correctly model the rapid temperature increase of the exposed GRP joint encountered after about 10 minutes in the intermediate-scale sample. The temperature response of the intermediate-scale ambient flange of the GRP joint was scattered and did not exhibit a clear trend.

B) Internal studding
Figures 5-73 and 5-74 compare the temperature development of the internal studding units in the bench- intermediate- and full-scale set-up. Although the bench-scale test correctly predicts the temperature response of the ambient flange of the stud in the intermediate-scale test, the rapid and marked increase of ambient flange temperature towards the end of the test could not be replicated. Although the ambient flange heated up at increased rate in the bench-scale test when the exposed board was broken away from the sample the rate of increase in the larger sample could not be matched. This was thought to be due to the reduced scale, which did not model the rapid and severe consumption of the core material, likely to have caused the sudden temperature build-up in the internal members. This can also be commented for the stud temperature development in the full-scale. The burning conditions on larger scale are bound to be more severe, which can be appreciated in the rate of temperature increase in stage II of the stud temperature/ time history in the full-scale sample. Although the broken board sample could manipulate the rate of temperature increase in the stud, the overall rate was not matching the full-scale test result. The ambient flange temperature build-up provided good indication of the temperature development in the full-scale sample.

5.3.6.3 Observations

A) Joints
The observations of the witness during the test are related to the temperature development within the jointed samples in figures 5-75 and 5-76. The board cladding the GRP section
cracked as observed in the plain, standard sandwich panel units. The cracks formed after the white Sasmox board had turned to black. After a couple of minutes the dark colour observed in the cracks faded and the board returned to its original white colour. From then on the cracks gradually widened without completely opening, leaving the joint partly covered. The cracking pattern of the board was similar and only marginally delayed when compared with the plain, cored sandwich sections. The cracking was less severe as no substantial degradation was taking place in the joint behind the board. At 25 to 30 minutes smoke emitted from the unheated edges of the section. The smoke was generated from the remaining core material underneath the heated board area and since no substantial cracking was observed, the smoke emitted from the unprotected edges of the sample. At 35 to 40 minutes cracking noises occurred, which were thought to be generated by the expanding jointing material. When exposed board was removed from the section during the test, leaving the section directly exposed to the irradiation from the heater, the Phenolic matrix melted out and evaporated. Only brittle glass fibres were left in the exposed layers of the jointing material.

In the steel jointed section the board edges curled up under the heat, cracking of the exposed board was also observed, which did not differ from the cracking observed in the standard sandwich unit, see figure 5-76. At about 12 minutes the antiseptic smell was sensed. At about 18-20 minutes the sizzling noises started and in some tests the board edges moved further away from the joint, creating a gap of 4-5mm between the exposed face of the joint and the back of the exposed board. At 23-25 minutes smoke emitted from the unheated edge of the steel joined unit, where the steel joint was embedded in the foam (see sketch in figure 5-76) The heating up of the foam seemed to have decomposed core pieces, which created air gaps extending to the outer edges of the sample. The continuous air gaps channelled the smoke and decomposition products. At 30 minutes this became more apparent as the visible insulating edges of the specimen showed the foam to alter from the original yellow coloured rigid foam to a brown glutinous liquid, delaminating from the board and steel.

B) Internal studding members

Figure 5-77 and 5-78 list and exhibit the failures within the studded panels during and after the termination of the test. The board cracked at about 2 to 4 minutes, as observed for the plain panel tests. At about 8 minutes the foam was observed to convert to a glutinous brown liquid and smoke was emitted between the upper flange and exposed veneer at the outer edges of the 200mm sample, which was followed by a 12 sizzling noises and a distinct smell, both remained persistent from then on. In some panels ignition would occur at 11 to 13 minutes, the flames were of blue colour and much smaller than observed in the plain panel tests. However not all panels ignited. With the smoke continuously emitting from the sides of the sample the panels were prone to ignition along the insulating perimeter. The smoke seemed to be channelled by the contiguous steel surface, which reached through to both ends of the 200mm long sandwich sample. The extent of damage behind the heated board area can be best
appreciated in figure 5-78, where the core is melted around the steel. The core damage was extended well beyond the directly heated area.

5.3.7 Standard Cone Calorimeter tests

5.3.7.1 Introduction

The tests in the standard Cone set-up were the initial bench-scale tests and have been used to evaluate the wall with respect to heat release rate (HRR), mass loss rate (MRR) and overall mass loss. The enhanced auxiliary measurements, such as smoke obscuration and toxic gas generation have not been employed and would be subject of later testing schemes. In these first set of tests two types of sandwich units of an overall depth of 50mm (100mm² test sample) were tested at 50kW/m²

(i) 8mm Pyrok on a PUR core (42mm),
(ii) 11mm OSB on EPS core (39mm).

In addition to the layered sandwich samples four board materials were tested

(i) 10mm Sasmox,
(ii) 8mm Pyrok,
(iii) 12mm Fels,
(iv) 11mm OSB.

In the intermediate-scale tests the boards showed extensive crazing and cracking, which was felt to be influential to panel performance. In the bench-scale test it was therefore tried to include a crack in the bench-scale samples to assess its affect on the measured parameters.

5.3.7.2 Heat and mass loss rate measurements

Figures 5-79 and 5-80 compare the OSB-EPS and the Pyrok-PUR sample's heat release and mass loss rates when subjected to a 50kW/m² heat flux. Both panels contained a manufactured crack though the board thickness in the centre of the sample. The OSB-EPS sample reacted instantly to the heat environment and the flaming combustion of the wood face resulted in a high heat and mass loss rate. In contrast the Pyrok-PUR panel was not responding to the heat environment until 5 minutes into the test. Then reactions increased but were still well below the OSB clad sample.

For each material combination a cracked sample was also tested at an irradiance of 25kW/m² (Figures 5-79 and 5-80). At the lower irradiance the reactions were delayed and less severe, similar to the temperature reactions observed in the modified Cone set-up. This is to be expected since decomposition levels of the panels are reached later and the reduced heat input dampens the failure degradation.

Figures 5-81 and 5-82 compare the heat release rates of the intact sample and the manually altered, cracked sample at 50kW/m². In the OSB-EPS sample there was no difference between the manually cracked and intact sample in both the heat release and mass loss rate, which indicated the cracking type failure to be a less predominant failure signature in wood based boards. In the Pyrok-PUR sample there was a slight delay (about 2 minutes) in the onset of increase in heat release rate for the non-cracked sample. Both
samples showed similar peak mass loss rates (Figure 5-82). The marginal delay in the heat reaction of the intact sample suggests that extensive cracking has the potential to accelerate the decomposition of the sandwich unit. However, the small population of replicate tests and the reduced sample dimensions make authoritative comment difficult. In both material combinations the test on the non-cracked sample was terminated prematurely due to the severe edge burning.

Whilst the characteristic decomposition signature (HRR and MRR) of the Pyrok-PUR sandwich unit was governed by the heat reactions of the PUR foam (Figures 5-83 and 5-84), the OSB-EPS response was most impacted by the decomposition characteristics of the OSB veneer (Figure 5-84), which exhibited similar HRR, peaking at 1 and 6 minutes. In both cases the first peak coincided with the ignition time of the sample. The mineral based board, Pyrok, Sasmox and Fels, behaved similar and all distinctively different from the wood based OSB. All mineral based boards had negligible heat release rates for the first 11 minutes. Comparing the absolute mass loss in figure 5-85 it could be shown that the mineral based boards exhibited similar mass loss gradients. Whereas the OSB showed a distinctively different mass loss pattern, exhibiting the steepest rate of mass loss out of all boards tested. This was thought to be more representative.

Overall the heat and mass loss rate were not highly conclusive which was to great extent affected by the misrepresented burning behaviour of the sample in the standard cone configuration. The consumption of the internal foam at the edges of the samples distorted the results.

5.3.8 Summary
Since the bench-scale test was modified to enable the detailed assessment of the heat reactions of sandwich wall structures, its boundary conditions and sample edge conditions were evaluated in detail. The three main test parameters were investigated, i.e. the effect of irradiance level, the effect of specimen dimensions, and the effect measurement location. With increasing irradiance level the temperature reactions shortened. The characteristic degradation of exposed veneer and core was best modelled by the bench-scale test at an irradiance level of 50kW/m². A protective edge area of 100mm around the irradiated 100mm² was found to be necessary to ensure representative consumption of the panel in the bench-scale. The omission of an insulation strip caused the premature consumption of the core at the sides of the sample, which clearly misrepresented the larger scale behaviour of the vertically oriented units. Reducing the insulation edge could lead to burn-through at the sides of the sample, increasing oxygen supply to core and thereby augmenting the temperatures within the core. Overall sample thickness did not affect the temperature reaction behind the board or in the core but the reduction in sample depth curtailed the measurement interfaces. The central part of the 100mm² sample was the most evenly heated and also the most representative area of the test sample. An area of 50mm² was found to exhibit identical and reproducible temperature reactions. The temperature development in measuring location outside this perimeter was less severe and not reliable.
The bench-scale samples exhibited the identical three stage temperature response as observed before in the sandwich wall tests in the intermediate-scale regime. The temperature reaction behind the exposed veneer was shown to be influenced by the board material and its thickness but also by the backing core material, supporting the findings in the intermediate-scale regime.

Whilst the onset of marked temperature rise in the core was influenced by board and core type, the rate of increase in stage II was shown to be related to secondary failure modes of the board. These secondary failure modes could be modelled by the breaking of exposed board from the remainder of the panel. The removal of board caused extensive flaming of the partially degraded core and a substantial temperature increase in deeper panel layers was recorded instantaneously. Since temperature measurements were up to 12 times repeated the reliability and reproducibility of the temperature responses could be examined. The consistency of temperature responses was remarkable and differences in temperature reactions in the various panel layers could be related to the extent and type of cracking of the board layer. The effect of plasterboard protection was to delay the temperature reaction, although a pre-heating effect was observed. Destruction and failure of the samples could be observed and linked to the temperature levels within the units.

In the modified bench-scale set-up the failure characteristics of the various board materials on different backings could be examined. The mineral based Pyrok and Sasmox boards exhibited similar behaviour, although the Sasmox performed consistently superior. Both boards cracked when exposed to heat, although the gypsum based Sasmox appeared to crack in area, whilst the cement based Pyrok developed cracks through the depth of the board layer. The wood based OSB board performed distinctively different to the mineral based board, flaming shortly after exposure and charring. Large fissures in the board exposed the backing core early in the test. The core fire behaviour was also examined and here the findings of both the standard and modified bench-scale regime were combined.

PUR/PIR exhibited principally different fire reactions: Whilst PUR/ PIR cores were seen to alter to a glutinous liquid and subsequently charred, the EPS evaporated and melted only leaving small residuals of melted material. Although the PUR and PIR performed principally similar, their failure features and break down temperature were marginally different. The temperature reaction was less severe in the PIR and degradation of the exposed PIR foam surface was more crystalline than that of the PUR. In the sandwich wall configuration, i.e. when the exposed board layer clad the core substrate, the fire reaction of the PUR/ PIR cores was subdivided into two stages, which were related to the degradation of the expose board. When the PUR/ PIR cores were heated behind the intact board a glutinous liquid and degradation gases were formed in the upper core layers, both of which ignited as soon as cracks had developed through the depth of the board. Once the cracks in the board opened up or the exposed board was removed from the panel the flaming combustion increased and char formed in deeper layers. The latter combustion mode accelerated the core consumption and was therefore more severe.
The bench set-up could also model the failure characteristics of the jointing materials. Especially in the steel jointed/studded samples the interaction of the board/steel and core could be established. In the steel jointed panel, the core damage was more extensive than in the plain, non-jointed section, since the steel heated up rapidly and transported the heat to less involved panel areas, remote from the directly heated section. The temperature reaction of GRP and steel was similar as observed in the intermediate scale programme. However the destruction of the GRP could not be modelled as severely as encountered in the intermediate-scale tests, which was related to the enhanced protection it received from the board in the horizontally orientated bench-scale test.

Heat and mass loss
The misrepresented burning behaviour of samples in the standard set-up makes the findings in the heat release and mass loss rate measurement only indicative of real behaviour. Generally the OSB-EPS sample reacted very differently to the Pyrok-PUR samples, which was mainly related to the ignition of the wood surface in the OSB clad samples. In the OSB samples the heat release and mass loss reaction was marked by an early sharp rise, which coincided with the ignition of the board surface, whereas the Pyrok surface showed low mass loss and heat release rates until the end of the test. Whilst the peak rates for the Pyrok-PUR sample were seen to be governed by the decomposing core material, the heat and mass loss rate of the OSB-EPS samples were governed by the cladding board. At lower irradiance the panel reactions were delayed but similarly severe. When an artificial, manufactured crack was inserted into the sample the OSB-EPS sample showed no difference in response, which indicated and underlined the findings that cracking of the wood based board layer was a less predominant failure characteristic. The Pyrok-PUR showed the cracked sample to give slightly premature reaction times with respect to heat release and mass loss rate.
Figure 5-33: Temperature reaction of Pyrok-PUR sample at varying irradiance levels

Figure 5-34: Temperature reaction of Sasmox-PUR sample at varying irradiance levels

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Figure 5-35: Temperature development through depth of Pyrok-PUR panel depending on irradiance level

Figure 5-36: Temperature development through depth of Sasmox-PUR panel depending on irradiance level

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Chapter 5- Results: Bench-scale fire testing

Figure 5-37: Temperature reaction of Pyrok-PUR sample at 50 kW/m² irradiance level in standard and modified test set-up

Figure 5-38: Effect of sample area on failure performance at 50 kW/m² (example of Pyrok-PUR sample)
Figure 5-39: Comparison of temperature development in different depth samples (example of Sasmox-PIR at 50 kW/m²)

Figure 5-40: Influence of measurement location (Depth A) on measured temperature reaction

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Figure 5-41: Influence of measurement location (Depth B) measured temperature reaction

Figure 5-42: Influence of measurement location (Depth C) on measured temperature reaction

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Figure 5-43: Schematic view of temperature distribution through depth of panel
Figure 5-44: Comparison of temperature reaction of PUR cored specimen and OSB-EPS sample

Figure 5-45: Comparison of temperature reaction of all PIR cored specimen

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Table 5-5: Comparison of temperature reactions for the various material combinations

<table>
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<tr>
<th>Specimen</th>
<th>Time (min)</th>
<th>Temperature (°C)</th>
<th>Stage III</th>
<th>Time (min)</th>
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<td>MIN</td>
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<td>18</td>
<td>14.5</td>
<td>22</td>
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<td>12</td>
<td>11.5</td>
<td>-</td>
</tr>
<tr>
<td>Sasmox-Steel</td>
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<td>52</td>
<td>52</td>
<td>-</td>
</tr>
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<td>7.5</td>
<td>18</td>
</tr>
<tr>
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<td>6</td>
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<td>-</td>
</tr>
<tr>
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<td>30</td>
<td>-</td>
</tr>
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<td>14.25</td>
<td>-</td>
</tr>
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<td>6.5</td>
<td>6.0</td>
<td>7</td>
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</table>
Figure 5-46: Comparison of temperature reaction in Pyrok-PUR depending on integrity of exposed board layer (mean of 2 tests)

Figure 5-47: Comparison temperature response in depth A for Pyrok and Sasmox on varying core substrates
Figure 5-48: Comparison of temperature reaction in measurement depth A for Sasmol (10mm)-PUR/PIR sample in modified bench- and intermediate-scale

Figure 5-49: Comparison of temperature reaction in measurement depth A for Pyrok (8mm)-PUR/PIR sample in modified bench- and intermediate-scale

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Figure 5-50: Comparison of temperature reaction in centre of core (Location B) for Sasmox (10mm)-PUR/PIR in modified bench-scale (at varying irradiance level) and intermediate-scale test

Figure 5-51: Comparison of temperature reaction in centre of core (Location B) for Pyrok (8mm)-PUR/PIR in modified bench-scale (at varying irradiance level) and intermediate-scale test

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In the first minutes of the test the exposed board discolours to pencil grey-black.
At 4-6 min minute cracks form over the entire surface, cracks are black in colour, then the cracks dry out and the surface returns to its original white colour, cracks not pronounced.
At ~7 min cracks open up further.

2 Sizzling noise and antiseptic smell, sometimes visible bow towards heater.

3 White smoke emitting from cracks.

4 Ignition, immediately afterwards smoke/gases turn to brown-black colour.

Ignition of gases through cracks.
Gases generated by core degradation.

Figure 5-52: Characteristic failure observations related to the temperature development in Sasmox-PUR samples.
In the first minutes of the test discolouration of board to darker grey
At 1-2 min minute cracks form over the entire surface, cracks
are black in colour, but then the cracks dry out and turn back to original greyish
colour
At ~7 min cracks open up further

2 Sizzling noise and antiseptic smell

3 White smoke emitting from cracks

4 Ignition, immediately afterwards smoke/gases turn to brown-black colour

Figure 5-53: Characteristic failure observations related to the temperature
development in Pyrok-PUR samples
1. In the first 50 sec wood surface ignites with high flames. After 1-2 min flame height decreases. Surface charred.

2. Wood expands towards heater but shrinking in its plane.

3. Heavy smoking from sides. Sudden puff and exposed face falls.

Figure 5-54: Characteristic failure observations related to the temperature development in OSB-EPS samples.
1. In the first minutes of the test the exposed board colour changes to black.
   At 1-2 min minute cracks have formed over the entire surface, cracks are black in colour and dry out and turn back to original white colour.
   At ~6 min cracks open up further.

2. Antiseptic smell and sizzling noise.

3. White smoke and smell emitting from cracks.

4. Ignition, smoke/gases turn to brown-black colour, back of the sample bows out and foam is liquidised.

Figure 5-55: Characteristic failure observations related to the temperature development in Sasmox-PIR samples.

Core damage confined to exposure area

(After test exposed board was removed from sample)
1. In the first minutes of the test the exposed board changes in colour to dark grey. At 1-2 min, minute cracks have formed over the entire surface, cracks are black in colour and dry out returning to the original lighter greyish colour. At ~4 min, cracks open up further.

2. White smoke emitting from cracks.

3. Ignition, immediately afterwards smoke/gases turn to brown-black colour.

Figure 5-56: Characteristic failure observations related to the temperature development in Pyrok-PIR samples.
1. In the first minutes of the test the exposed board surface turns to black. At 1-2 minutes small cracks form over the entire surface, cracks are black in colour then the cracks dry out and turn back to original white colour, cracks are not as pronounced and distributed over the entire surface. At ~5 min cracks open up further, but not as pronounced as in other boards.

2. Sizzling noise

3. Distinct smell emitted

4. No ignition, gases escape from the back of the sample and back is bowed out, cracks in board glim redly and pulsate.

Figure 5-57: Characteristic failure observations related to the temperature development in Fels-PIR samples.
Figure 5-58: Changes to core material when (a1) to (a3) heated behind intact exposed board (b) heated behind partially cracked board layer
Figure 5-59: Temperature reaction of Sasmox-PUR with and without plasterboard (15mm)

Figure 5-60: Temperature reaction of Pyrok-PUR with and without plasterboard (15mm)
Figure 5-61: Comparison temperature development through the depth of PUR and PIR foams

Figure 5-62: Appearance of EPS core before and after ~2 minutes of test under 25kW/m²

Chapter 5- Results: Bench-scale fire testing
In the first seconds, large amount of smoke/vapour emits
No ignition
At ~3 min smoking and charring reduces

Surface of crystalline appearance sinks in craters of 10mm diameter
In some tests small bubbles of max. 5mm diameter on exposed surface

Burnt smell

Exposed PIR foam surface after test:
Single air pores within foam burst then foam chars

Figure 5-63: Characteristic failure observations related to the temperature development in PIR cores
In the first seconds sizzling noise, surface appears like the surface of a boiling liquid, some smoke/vapour emits. No ignition. At ~1-2 min small bubbles form to large bubble over entire 100mm² surface, skin of bubble consists of thin charred layer of foam.

Bubble deflates and grey smoke is emitted, smoking is heavy.

Unexposed face of foam sample bulged out.

Exposed PUR foam surface after test: Single air pores burst and form a smooth char skin.

Figure 5-64: Characteristic failure observations related to the temperature development in PUR cores.
Figure 5-65: Temperature reaction of Sasmox clad GRP joint

Figure 5-66: Temperature development in GRP joint depending on cladding board material
Figure 5-67: Temperature reaction of Sasmox clad steel joint

Figure 5-68: Comparison of temperature response of GRP and steel joint

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Figure 5-69: Temperature reaction of Pyrok clad studding units

Figure 5-70: Comparison of temperature development in hot and ambient stud flange depending on board integrity

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Figure 5-71: Comparison of temperature development in steel joint in intermediate- and bench-scale regime

Figure 5-72: Comparison of temperature development in GRP joint in intermediate- and bench-scale (Loc. 1)
Figure 5-73: Comparison of stud temperature development in bench- and intermediate-scale regime

Figure 5-74: Comparison of temperature development in steel studs in bench- and full-scale

Chapter 5- Results: Bench-scale fire testing
1. Cracking of exposed board as in plain board panel where the board is backed exclusively by foam.

2. Smoke emits at side of joint.

3. Cracking noises.

4. When board is broken and GRP exposed the phenolic matrix melts out leaving only the glass fibres in the uppermost layer.

Degradation of GRP unit when exposed behind board

Figure 5-75: Characteristic failure observations related to the temperature development in GRP jointed sandwich samples.
1. Board edges curl up towards heater
   Cracking as in plain board panel

2. Antiseptic smell of varying intensity

3. Sizzling noises. In test 3 board moves further towards heater away from joint

4. Smoke emits at side of joint

5. At joint foam now brown liquid delaminated from steel

Extent of core damage at steel joint
(one half of exposed board removed for observation)

Depth of core damage at steel joint

Figure 5-76: Characteristic failure observations related to the temperature development in steel (cold-formed) jointed sandwich samples
1. In the first minutes of the test discolouration of board to darker grey. At 1-2 min minute cracks form over the entire surface cracks are black in colour, but then the cracks dry out and turn back to original greyish colour.

2. Antiseptic smell, foam transforms to brown liquid at exposed flange.

3. Smoke emits from stud at side sizzling noise.

4. Ignition, but not in all tests. With increasing smoke from the unheated perimeter of the sample flaming jumps from surface to the edges.

Figure 5-77: Characteristic failure observations related to the temperature development in sandwich samples with internal links.
Figure 5-78: Failure progression in studded panel

- Start of core decomposition at hot flange of stud
- Core failure around stud progresses
- Extent of core damage in stud region after 20 minutes exposure
Figure 5-79: Comparison HRR for samples with crack under varying irradiance levels

Figure 5-80: Comparison MRR for samples with crack under varying irradiance levels
Figure 5-81: Comparison of HRR for cracked non-cracked samples at 50kW/m²

Figure 5-82: Comparison of MRR for cracked non-cracked samples at 50kW/m²
Figure 5-83: Comparison of HRR for cracked, non-cracked and foam sample at 50kW/m²

Figure 5-84: Comparison of MRR for cracked, non-cracked and foam sample at 50kW/m²
Figure 5-85: Mass loss (g) for different board materials
5.4 Full-scale Fire Performance

5.4.1 Introduction to full-scale test findings

Within the test programme one full-scale fire test on a vertically loaded 2.4m high by 3m long sandwich wall assembly was conducted. Similar to the specimens in the previous smaller scale fire test regimes the temperature build-up in the wall was closely monitored such that the instrumentation of the units was well above the requirements set out in the Code of Practice. A full-scale fire test is an important proving test, since both the size and the assembly method of the wall units are as similar as possible to the end use of the sandwich assembly. With the enhanced instrumentation of the wall, this test set-up enabled assessing the effect of the furnace heat on structural sandwich walls, correlating the internal temperature build-up and altering loadbearing ability. Despite the similarities between laboratory and end use wall assembly, the installation of the wall within the furnace frame, the loading and heating rate are not entirely representative of the real exposure conditions in a dwelling fire. These test boundary conditions have considerable impact on the performance of a wall assembly so that the furnace tests can only be indicative of real fire performance. This is discussed in further detail in section 5.4.4.

To compare and further analyse the full-scale fire performance of different types of sandwich walls, fire test data from various other sources have been examined. The test experience presented stems from two types of fire tests:

(i) fire resistance furnace tests, in principle identical to the test undertaken within the programme,

(ii) reaction-to-fire tests in which the sandwich wall specimens are assembled and exposed to heat generated by burning wooden cribs so as to create a more realistic fire exposure.

The fire requirements for wall systems in building applications range from ½ to 1 hour and fire resistance tests (to either BS 476: 21 (4) or BS EN 1365 (7)) are employed to prove the compliance of the structure to building authorities. Commercial sandwich wall systems, such as System 1 (Marshalls Panablok®) wall panels and System 3 (Kingspan TEKHaus®) wall panels, have successfully undertaken such tests. Both companies kindly contributed their fire test data to the study. In addition the Forest Products Research Laboratory (FPL) published the test results of an extensive fire resistance study on structural sandwich wall panels of varying material composition. The panel systems investigated by FPL was similar to System 3 described in Chapter 2 and was tested with varying levels and types of protection. The data from all three sources is compared in this section.

The second type of testing analysed for comparison, the reaction-to-fire testing, was also undertaken by the FPL in the US. In their work the failure pattern of various types of sandwich wall configurations when exposed to natural, realistic fire scenarios were examined. Although the wall behaviour established through these tests cannot be directly
correlated to the fire resistance of sandwich walls the failure signatures give valuable insight to the general fire behaviour of structural sandwich walls.

The chapter is organised in four sections. In the first section the panel features, test arrangements and instrumentation for the single test undertaken within the programme are detailed, the findings are discussed in three parts:

(i) the visual observations,
(ii) the temperature reaction of the wall,
(iii) the stiffness reaction of the wall.

The second section presents and compares the findings of the various full-scale fire resistance investigations on structural sandwich panel walls. The comparison will discuss the characteristic fire behaviour of sandwich walls, with respect to temperature build-up and stiffness performance. Unfortunately the test evidence from these other sources is not comprehensively covering both, the temperature and stiffness reaction of the wall. The discussion tries to overcome these gaps and attempts to draw together the commonalities to establish principles of sandwich wall behaviour. In a third section the inherent boundary conditions of furnace based fire resistance tests are reviewed and a discussion dwells in particular on the adopted loading arrangement. The final section supplements the findings of two reaction-to-fire test programmes on sandwich wall systems, analysing their burning and smoke characteristics. This is useful additional information and complements the fire resistance investigations, which are not designed to evaluate the contribution of building units to the intrinsic fire hazard in a compartment.

5.4.2 Fire resistance test on System 2
The fire test undertaken in the programme was conducted on a System 2 wall (see Chapter 2). On the exposed face the wall was protected by 15mm standard wallboard and the unit was vertically loaded by a uniformly distributed load of 25kN/m. The type of plasterboard protection and the magnitude of loading were chosen based on the intended final application of the wall in dwelling construction. The additional costs of glass-fibre reinforced plasterboard, with improved shielding ability, were avoided to enhance the economic efficiency of the wall system. The use of sacrificial plasterboard also linked in with the intermediate- and bench-scale test work. Although standard construction was attempted, the prototype wall unit included some non-standard features due to difficulties in construction. The single panel units were linked horizontally at top and bottom by glass reinforced Phenolic (GRP) top hat sections attached to the wall panel by screws. The panels were vertically linked through contiguous connectors formed from cold-formed steel sheets. Along the panel height the female and male shutter were permanently foamed into the core and were fitted by screws upon assembly. The objective of the test was to establish the temperature and vertical load reaction of the unit when exposed to fire conditions. To enable the use of the wall construction in dwelling applications a 1-hour fire resistance was attempted. The test wall was heavily instrumented to record all wall reactions.
5.4.2.1 Panel details and construction problems
The wall assembly was constructed with prototype panels. The manufacturing process for the test panels was adopted from the continuous line manufacture of System 1. This caused several problems in the prototype panels. The internal core material was chosen to be PIR foam but since the manufacturing rig was constructed for PUR foam injection the facility did not provide heating coils for warming up the different panel components prior to injection. As a consequence the adhesion between core and boards was weak and local delamination of the components was encountered. Manual preheating of the components could restrict the damage to the perimeter of the single wall units and the overall delamination problem was regarded as minor. The manufacturing line was restricted to 1.2m depth so that the contiguous steel joints needed to be aligned with the edge of the board, rather than protruding from the edge for later insertion. This was a minor effect, since the board edges could be cut back and a tight fit could be created upon assembly.

A second wall weakness was caused by the method adopted for forming the recesses in the core for the internal top and bottom rail. The recesses were moulded into the core with spacers placed at the panel ends. The spacers for the horizontal rails were too deep and upon assembly of the wall an air gap of approximately 20mm was created between the rails and the core. Furthermore the vertical units were cut to match the height of the core and thus neither the studs nor the vertical steel joints abutted with the top or bottom rail (Figure 5-86). This construction flaw destroyed the continuity in the wall and had an impact on the heat and fire resistance of the wall especially at the top of the wall where exposure is more severe. In practice this could be avoided by adopting standard templates for the length and depth of the vertical inserts and spacer. The commonly adopted method of carving out the excess core at the panel ends with specialised tools becomes non-practical when vertical solid inserts are present in the panel. The panel arrangement in System 1 is advantageous as the height of the vertical inserts and core match the height of board.

5.4.2.2 Pre-test preparation
A specialized building crew trained to install structural sandwich wall panel systems erected the test wall into the rig. The test house provided the infill plinth on which the wall was positioned in the steel frame. Once the test wall was built into the test frame the TC measurement locations and laser measurement scanning points were marked. Holes at different depths were pre-drilled allowing for the TCs to be inserted at the correct position and depth. All temperature and deflection measurement points were subsequently numbered.

5.4.2.3 Instrumentation
The test wall was instrumented with 70, type K, thermocouples. The thermocouples were placed at various locations and at five different depths within the test specimen;

(i) behind the sacrificial plasterboard layer (PB),
The exact measurement locations are sketched in appendix 1. Figure 5-87 shows the test wall just prior to the start of the test. The various TCs are seen to be connected to the electronic data logger (on the right of the picture), which recorded the data and was linked to control room.

Three LVDTs measured the deflection of the wall panel. The first monitored vertical displacement at the top of the panel and the remaining two devices measured the out-of-plane lateral deflection of the wall. The vertical displacement was measured at the spreader beam in the centre, at the top of the wall. The out-of-plane deformations were measured from the unexposed face at mid height of the wall; one gauge being positioned in the centre of the wall assembly and the other along its edge. Additionally a low accuracy laser distance measurement device was set-up to monitor the lateral deflection behaviour of the wall. With the equipment it was possible to monitor more than thirty points at intervals of two minutes. Of principal interest was the movement of vertical panel joints where readings were taken at the ¼ points of the joints.

The tests witnesses were equipped with breathing apparatus and could communicate with the control room through radio equipment.

5.4.2.4 Visual monitoring

Visual monitoring of the panel was carried out from the rear of the furnace. The observation post gave a clear view of the fire-exposed face of the wall unit. During the test the generation of smoke obscured this view so that detailed observations of the various destruction stages of the wall assembly were no longer possible. The changes to the ambient face of the wall were monitored from the laboratory floor and also recorded by cameras. The observations recorded for the System 2 wall tests are summarised below.

At the start of the test the interior of the furnace was hazy but after several minutes the exposed wall became clearly visible. After 2 minutes the paper face of the plasterboard was burnt and flaked off the gypsum core of the board. The appearance of the skimmed plasterboard joints also changed, developing cracks and where the filler material was most thinly applied, parts fell off. At 13 minutes white, quickly smoke was emitted from the top left corner of the unexposed face, which quickly dissolved. This emission was most probably steam, generated from the plasterboard and timber battens once a temperature of 100°C had been reached. At 22 minutes the plasterboard joints started to open up and the plasterboard began to crack. Three minutes later the plasterboard edges of the centre board, where joint material had fallen out, bent towards the furnace, and exposed the timber battens, which started to char. With the plasterboard sheet slightly bending into the furnace, the plasterboard around the screw fixings at the joints became torn. At that stage white smoke was emitted from the test specimen and the colour of the
exposed Sasmox board started to change from white to grey-black. At about 30 minutes half of the centre sheet of plasterboard had broken from the wall and fallen into the furnace. Three minutes later more pieces fell off exposing about 50% of the test wall. Where the wall was built into the test frame the plasterboard was well restrained and remained in place, shielding the test panel from direct heat. On the unexposed face red smoke started to emit from the bottom edge. At 37 minutes the furnace chamber had filled with smoke that was being generated from the test specimen; visibility reduced and observation of the exposed face was stopped. The first significant changes to the unexposed face were observed after 40 minutes when heavy smoke was emitted from the vertical joints on the unexposed face; here the unexposed board face discoloured. A deflection towards the furnace was clearly visible at 42 minutes. At 45 minutes the unexposed veneer broke along the length of the partition, where the air gap between vertical inserts/ core and joint was present and caused by the top joint rotating under the applied load. Although this type of failure is generally regarded as structural failure of the panel the test was continued to enable further observations on the structural behaviour of the wall. At 47 minutes the panel buckled into the furnace approximately 600mm from the head of the panel and the test was terminated.

5.4.2.5 Temperature data
The sandwich wall exhibited two characteristic temperature reactions, which could be related to the wall area;

(i) in the central part of the wall the temperature levels were high and marked by an early and instant rise through the depth of the wall,

(ii) in the edge areas of the wall temperature build-up was slowed down with delays through the depth of the wall panel and overall less severe.

The area of high temperature levels within the sandwich wall unit was concurrent with the area of early plasterboard loss (Figures 5-88, 5-89 and 5-90). In the centre of the wall assembly the temperature response behind the plasterboard layer indicated that the board integrity was impaired at about 20 minutes, which was probably related to the opening of the plasterboard joint. However, the majority of temperature measurements in the centre of the wall recorded increased temperatures at about 30 to 35 minutes. For measurements close to the edges of the wall, especially in the corners of the test frame, the temperature reaction did not convert to stage II within the test time. In these edge/ corner areas the plasterboard was observed to remain cladding the sandwich wall panel. Once the plasterboard layer had fallen off the wall unit into the furnace, the temperature reaction within the sandwich wall was rapid through its depth (Figure 5-91). This severe and locally confined temperature response suggests that overall failure of the wall was governed by the damage sustained after the plasterboard loss in the centre of the wall. Towards the edges of the wall the temperature build-up through the depth of the wall was delayed and less severe, as shown in drawing 5-20.
The temperature reaction of the internal units was also governed by the location of the stud with respect to panel area. The temperature build-up in the internal stud and jointing units was recorded on both the exposed and ambient flange of the units and in accordance with the temperature measurements on the plain sandwich wall sections, the temperature reaction of the internal units was more severe in the areas of early plasterboard loss, as seen in figure 5-92. Whilst the ambient flange temperatures were found to be less affected, the exposed flanges showed temperature variations of up to 650°C between partly covered and directly exposed stud regions. The temperature build-up in the unit was governed by the amount of board retained on the stud and also by the extent of core damage around the studding. Close to the perimeter of the panel the board was supported by enhanced edge restraint, which protected both the stud and the core from direct furnace exposure. The characteristic core degradation is also thought to be influential to the temperature development in the stud. The vertical edges of the wall were comparatively unaffected at wall failure and had benefited from continued protection from the plasterboard with respect to board, core decomposition and consequently stud heating.

5.4.2.6 Stiffness response to vertical load

Figure 5-93 plots the out-of-plane deformation of the wall assembly, as measured in the centre of the wall and at the left side of the assembly. While the plasterboard was still cladding the sandwich wall the deflection rates were moderate and just prior to the falling off of plasterboard in the centre of the wall the wall deformation gently recovered to the extent that the wall was pushed back into its original position. Once plasterboard had fallen off, the wall deflected at a high rate towards the furnace, reaching 25mm deflection at 45 minutes. Visual monitoring as well as the temperature recordings from the panel
suggested that by the time of rapid stiffness loss approximately 50% of the test wall had been attacked by heat, mainly in the centre part of the wall, following delamination of the plasterboard. The damage along the top joint was noted as the unexposed veneer broke along the entire length of the wall and subsequently the top rail rotated under the load. Before this failure, the exposed and unexposed veneer and its fixings along the top joint had bridged the manufactured gap between core and top joint and the load was transferred into the panel through this load path. However, once the exposed veneer had broken and the joint rotated and rested on the internal studding the deflection rate reduced and the buckling failure of the entire wall unit terminated the test at 47 minutes. The deflection of the wall unit along the left free edge of the specimen was negligible when compared to the central deflection. As observed in the central wall deflection the edge of the wall also gradually deformed towards the furnace at the start of the test, although this deflection was very small only reaching a maximum of 0.5mm in 10 minutes. Subsequent to the visible failure at the top of the joint (at 45 minutes) the measuring device was removed, as failure was imminent. By then the wall had deflected to 7mm away from the furnace. The much reduced deflection in this part of the wall is related to the installation of the test specimen into the steel frame, restricting the movement of the wall. In addition the wall panel is not heated as severely along the edges as plasterboard and exposed board are likely to be retained and shielding the internal parts of the wall. The drawing in of ambient air along the sides of the wall due to the differing pressure conditions in furnace and surrounding test laboratory has also been identified influential to these lower temperature and deflection signatures, which were also found by Klippstein (1980).

The experiment to measure the movement of the studs by a low quality laser was disappointing as very little deformation was measured even in places where high movements were recorded by the LVDTs. The results can clearly not be relied on. These misleading measurements were thought to be linked to the laser principle, which is affected by the visual quality of the measured surface. In fire tests smoke can impair the light path of the pointer leading to inaccuracies. However, the trial measurements with the device prior to the fire test were promising and it is felt that further development of the technique has the potential to enable valuable data to be recorded.

5.4.2.7 Post-experimental observations

Figures 5-94 to 5-95 show the panel at the moment of failure and figures 5-96 and 5-97 after the panel had been hosed and removed from the furnace. Visual inspection of the panel confirmed that the wall had sustained the most severe damage in the central parts of the wall. Along the sides of the part of the plasterboard and the exposed were still retained and behind large pieces of charred core were found. In the central part of the panel no foam residuals were left. The foam was in some parts still glowing and exhibited signs of flowing in some edge areas, figure 5-97. The post-experimental observations can only give an approximate indication of the state of the panel at failure. The hosing of the wall unit and the removal from the furnace cause loosely attached, degraded board pieces to fall from the panel. Despite the fact that these board pieces were degraded they would
have still provided some shielding to the interior of the panel. In furnace tests it is generally very difficult to obtain an accurate state of damage at the end of the test.

5.4.3 Full-scale test results from other sources

5.4.3.1 Review of test evidence

This section will review and compare the fire resistance test evidence of various sandwich panel wall assemblies from four sources:

(i) System 1 (2 tests),
(ii) System 2 (1 test, described before),
(iii) System 3 (2 tests with wooden edge infills),
(iv) Forest Products Laboratory tests (FPL, 1975 fire resistance tests on 9 sandwich wall assemblies of varying composition).

The tests of (i) and (iii) were undertaken to BS 476: 21 (4), (ii) was undertaken to EN 1365 (7) and the FPL regime (iv) was conducted to ASTM E-119 (9). Despite the different testing regimes the furnace exposure of the walls were similar as shown in figure 5-98. Nevertheless, the detailed comparison of all test findings is difficult since the main objective of standard fire resistance tests, which are part of the product approval testing scheme, is to establish the time until one of either the insulation, integrity or loadbearing criteria of the wall unit has failed. The compliance with the ½ or 1 hour fire resistance requirement can be established by limiting the measurements to the monitoring of the surface temperature of the ambient side of the wall and the deflection of the unit. The integrity criterion is checked with a cotton pad monitoring the passage of hot gases along the wall as appropriate. The largely reduced number of temperature readings used in test regimes (i) and (iii) does not enable to compare heat build-up characteristics amongst the different wall assemblies. However, for the sandwich walls tested to BS/EN standard (i.e. test regimes (i) to (iii)) the deflection signature of the wall assemblies can be analysed.

Whilst the FPL tests report does not detail the wall deflection patterns, the research oriented study monitored the temperature reaction of the sandwich wall sections, with measurements similar to the ones taken in the System 2 wall (Source (ii)). The FPL conducted the tests as part of a large research programme to establish acceptance criteria for sandwich panel construction for application in dwellings. The test programme was undertaken in 1974 (published in 1975) and designed to compare the fire resistance of the sandwich wall assemblies of various configurations with the fire resistance performance of timber frame wall construction, which was then widely used in the US. Nowadays lightweight timber frame wall construction is very common in Europe and structural sandwich wall panels are sold into the same market. This makes the comparison of performance between both wall systems additionally valuable to this investigation.

5.4.3.2 Comparison of sandwich wall performance

Table 5-6 overviews the various sandwich wall systems and summarises their main features. Whilst Systems 1 and 2 include internal cold-formed steel veneer linking studs
(of $\Sigma$ and I shape) the remaining wall systems do not include any internal veneer links apart from the jointing. In system 3 the assembly is routed at top, bottom and sides for 40x110mm wooden studding. The wooden inserts are employed to horizontally link the single panels. The vertical studs are edge inserts commonly used at regular intervals in this form of panel system, generally to stabilise the longer wall sections. Sometimes vertical wooden edge posts are recommended when testing sandwich walls to prevent the premature involvement of the core, which would distort the outcome of the test. The record of the FPL test does not give any detailed information about the horizontal jointing mechanism. The similarity in vertical jointing method between the FPL tests and the System 3 assembly, makes the use of wooden horizontal rails in these tests likely. These panels did not incorporate timber edge fillings. The loading is similar in all tests, except one of the System 3 tests where the vertical load is reduced to 13kN/m. In all tests load spreader beams have been used to uniformly transfer the jack loads to the panel top. All commercial wall tests (System 1 to 3) use plasterboard, with exception of System 2 and one test in System 3, the plasterboard is fire rated. Some of the FPL wall assemblies were also protected by glass-reinforced plasterboard, evaluating the effect of the cladding on the fire resistance on the sandwich wall unit. The fire rated plasterboard used in the States in 1974 is not necessarily comparable to the glass fibre plasterboard used nowadays. The test series also assessed the shielding capacity of the intumescent matrix of varying thickness. In addition to sandwich wall tests the FPL tested five wood frame panels. They were constructed from 50/ 100mm studs spaced 406mm centres. Glass fibre insulation, 90mm deep, was stapled to the stud edges facing the back of the exposed veneer. In four walls the exposed face consisted of gypsum plasterboard and one wall used plywood sheathing.

In table 5-6 the fire resistance rating and failure criteria of the different sandwich wall assemblies are listed. Except for the System 3 panels all sandwich walls failed the loadbearing criteria and their buckling towards the furnace terminated the test. The sandwich walls of System 3, together with the timber frame walls failed the insulation/integrity criterion, exhibiting burn through failure generally in the centre of the assemblies. The summed test evidence enables the analysis of three main characteristics of sandwich wall behaviour;

(i) Visual observations recorded during the test,
(ii) Temperature performance,
(iii) Deflection and failure signature.

(i) Visual observations
The visual observations have been recorded in all test regimes and common to all sandwich wall tests are the characteristic stages of the plasterboard failure. In the first minutes of all test the paper surface of the plasterboard burns and generates flames. Subsequently, the jointing compound skimming the gap between the single plasterboard sheets falls off, which can cause localised early temperature increase behind the plasterboard. The time until visible cracks start to form in the plasterboard layer depends on the plasterboard type used. In standard wallboard this is observed as early as 20
minutes after the start of the test, in glass-reinforced plasterboard this is generally delayed and the temperature build-up is accordingly reduced, as shown in figure 5-99. Once the joint compound has fallen out along the height of the wall and board integrity gradually reduces, the firm shielding layer loosens and heat penetrates at increasing rate into the sandwich wall panel. In wooden faced panels this increased heat influx causes the ignition of the exposed wood boards and flames are emitted from behind the remaining plasterboard. With ongoing exposure the board degradation progresses and cracking in the plasterboard is extensive. In this stage large plasterboard pieces break and bow away from the wall, eventually falling into the furnace. This effect is observed earliest in the centre of the wall. In the tests where the deflection of the wall was monitored the loss of plasterboard protection in the centre of the wall triggered the onset of sharp deflection increase. In the FPL sandwich walls test structural failure occurred concurrent with the loss of plasterboard. The plasterboard protection hinders the detailed observations of the failure progression of the exposed sandwich wall layers. By the time the large plasterboard pieces has fallen off the wall dense smoke is emitted from the walls and partly from the wooden battens and the view is obscured.

Similar to the paper facing of plasterboard the protective mastic coating ignites after 1 minute into the test and the initial flaming dies down quickly. After the initial flaming has stopped the mastic surface is heavily cracked and after 5 minutes large blisters (50mm deep and 300mm in diameter) form. With ongoing exposure the cracks in the protective layer deepen. Once the coating has formed large bulges and in most areas disintegrates from the wall, structural failure of the wall is observed. Doubling the thickness of the protective coating layer approximately doubles the time to failure of the wall and the attained fire resistance of the wall assembly was equal to that obtained for 12.7mm plasterboard.

In the FPL programme the sandwich walls were tested without plasterboard cladding, which enabled the examination of the failure signatures of the wooden exposed veneer. The exposed plywood was observed to ignite after about 1 minute. Within 20 seconds flaming was extensive and extended over the entire surface. At about 2 minutes fissures were formed within the board, which was accompanied by heavy smoking but reduced flaming and instantaneously the walls buckled into the furnace. The panels generally fail within 3 to 4 minutes and explosive sounds accompany the decomposition of the core. Once the damage to the exposed veneer has progressed to expose parts of the core, the flaming picks up again and heavy, dense smoke fills the furnace and yellow-grey smoke is emitted along the top of the unexposed face of the wall. Large pieces of the exposed plywood face fall into the furnace and burn-through along the joint of the unexposed face is observed.

(ii) Temperature (Figures 5-100 and 5-101)

The temperature reaction of the wall was monitored for the System 2 wall assemblies and the sandwich walls tested in the FPL programme. Figures 5-100 and 5-101 compare the temperature development
behind the exposed veneer of the sandwich wall,

in the centre of the core.

The temperature reaction of the plain, unprotected sandwich wall assemblies is instant and within 3 minutes the furnace temperature has progressed through the depth of the wall. The temperature rise behind the exposed veneer is rapid and structural buckling failure of the walls is imminent. Following structural failure of the FPL panels the tests were continued to assess the burn-through behaviour of the walls. Within 5 to 12 minutes the fire had consumed the walls and flaming penetrated through the unexposed face. The paper honeycomb (noted as “Paper” in the graph) core performs best and also profited most from the additional plasterboard. In all sandwich wall tests heavy smoking and increased flaming marked the post-structural failure phase.

The plasterboard cladding delays the temperature reaction within the walls. Although the System 2 assembly is clad with the inferior wallboard the temperature build-up is later than in the FPL assemblies protected by glass-fibre reinforced plasterboard. This could be due to the fact that the ignition and flaming of the wooden veneers releases additional heat, which contributes to the degradation of the plasterboard and therefore indirectly accelerates the heat exposure of the walls. The principally different failure signature of the exposed veneer Pyrok and plywood veneer layer is seen influential to the temperature build-up in the wall and the bench-scale testing exhibited the advantages of mineral based boards with respect to shielding ability.

Whilst the onset of sharp temperature increase within the unprotected FPL walls coincided with the loadbearing failure of the walls, the onset of sharp temperature increase within the System 2 wall panel did not immediately result in the failure of the wall, see figure 5-100. Although the internal panel temperatures rise once the plasterboard has fallen off the wall and the deflection rate of the walls sharply increased, structural failure only occurred after further 5 to 10 minutes. This was thought to be linked to

(i) the additional internal links present within the System 2 wall,
(ii) the fact that the plasterboard failure was restricted to the central part of the assembly.

Since the internal links are the main difference in the panel composition the delay is likely to be related to their inclusion and their function within the sandwich panel is further assessed in the structural testing programme at ambient temperature, which is discussed after this section. The concentration of the main wall failure in the centre of the wall together with the use of a load spreader beam is thought to delay the failure in general. Since the wall load is spread to the sides of the wall, which are still protected by the plasterboard and to lesser extent attacked by failure the structural failure can be postponed. The slight delay in the overall buckling failure in the FPL tests clad with plasterboard might reinforce this assumption.
The deflection signature of the walls can only be compared for Systems 1 to 3, figure 5-102. In the FPL tests the LVDTs were removed before wall failure occurred so that the holding arrangement of the LVDT would not influence on the buckling deflection. The deflection data was not supplied in the report.

Systems 1 and 2 showed a similar deflection pattern whereas the System 3 wall assemblies behaved markedly different. This can be in parts due to the lower test loads but together with the fact that the system only uses thin OSB tongues of negligible stiffness to link the panel units, the superior performance is thought to be related to wooden studding at the sides of the wall assemblies in System 3. The loading arrangement adopted in full-scale testing enables the shifting of the load to the edges of the wall once the central part of the wall assembly is damaged. This makes the sides of the wall the main load carrying members and although the use of these solid edge posts is reduced to a minimum the test load can span between the two posts once the exposed veneer is consumed and the composite action is lost. Then the charring rate and reduction in effective cross section of the edge posts becomes the predominant load carrying criteria with respect to the loadbearing performance. As the edge posts are located at the outer perimeter of the assembly and enclosed into the foam, where the well restrained board pieces are likely to reduce the exposure of the post, their rate of charring is minimal. An estimative calculation to BS 5268: 4 (30) can prove that the applied load is easily carried through both edge posts and that after 30 minutes direct exposure the ultimate stress levels in the post are still 50% below the ultimate destructive stress. The bum-through dominated failure advocates this load shift scenario and aligns the System 3 wall behaviour closely with the timber frame fire performance in the FPL tests described further below.

Comparing the deflection signature and failure times of Systems 1 and 2 the benefit of glass-reinforced plasterboard can be appreciated. The fact the shielding ability of the fire rated boards is enhanced delays the failure of the System 1 walls. The glass-fibres in the fire rated plasterboard improve the coherence of the plasterboard after its degradation, since the glass fibres in the gypsum core hold the single board pieces together. This reduces the falling off phenomenon and thereby dampens the heating up of the loadbearing wall. Although the System 2 wall fails earlier than the System 1 walls the magnitude of deflection increase in the first minutes of the test, while the plasterboard is still intact, is overall lower in the System 2 wall, which employed solid contiguous vertical joints.

In the FPL fire tests the unprotected sandwich walls usually showed structural failure by buckling within 3-6 minutes. The best performance was obtained from the paper honeycomb core panel. The protection of the exposed face of the sandwich walls by a sacrificial layer of fire rated plasterboard delayed structural failure by about 20 minutes. All sandwich walls failed by buckling and at the time of failure no burn through (insulation criteria) was observed.
When the panels are clad with plasterboard the time to structural failure is delayed to 23-26 minutes. There are about 30-40 seconds between the failure of the plasterboard and the overall failure of the wall. Once the plasterboard has failed and fallen from the exposed face of the wall the smoke development is as heavy as observed for the unprotected wall. Therefore all tests are terminated about 5 minutes after structural failure had occurred.

In comparison the wood frame wall assemblies failed after 16 to 35 minutes dependent on the exposed facing material. The hardwood plywood faced timber frame panel failed earliest. The gypsum faced wood panels shows much improved fire resistance. The majority of tests were terminated due to burn through as large areas of the walls had failed the insulation criteria. At that time no structural failure has occurred. Structural failure preceded insulation failure in two panels. However, in general the dominant failure mode was established to be burn-through rather than structural failure.

5.4.4 General comments to full-scale fire resistance testing methodology
The testing methodology is highly influential to the outcome of the test. In addition to the heating regime and the pressure conditions the application of the load is a vital part of the test assessment of loadbearing building walls. The current full-scale fire test codes give different advice with regard to load application.

5.4.4.1 Regulatory advice on testing to BS and EN
In EN 1365-1: 1999 (7) the requirements for the loadbearing equipment in fire resistance tests on loadbearing wall elements is detailed under subsection 4.3. It is stated that the load may be applied by means of loading jacks at top or bottom of the frame. When a rigid frame is used to apply the load it must be stiff enough to ensure uniform vertical deflection along the test specimen. If the load is applied separate loading points the system must ensure that the load remains constant at each point throughout the test. The recommendations does not state clearly which loading arrangement should be chosen for testing. In the British Standard BS 476: 21: 1987- A.6.3.2, clause 8 (4) the loading of vertical elements is discussed in more detail. Here the loading frame must enable the positioning of loading at specific points but a uniformly distributed load is also permitted, similar to the EN standard. However, with respect to the uniformly distributed load the BS Code of practice states that the application via load spreading beam must not permit any bridging of load, especially if the specimen deforms vertically in irregular manner. This is a recommendation especially important in the context of the research on loadbearing sandwich walls.

5.4.4.2 Standard constructions in test labs in UK
A range of furnace load arrangements has been reviewed and it appears that the distribution beam is used in 90% of test houses, although some laboratories also use the single loading arrangement on request. Some laboratories advise on special protection of edges especially when testing sandwich walls, where the internal core is deemed structural. To protect the vulnerable core and avoid premature failure which would not be
representative of real scale behaviour the test labs advice on installing wooden edge studs at the panels' side so that edge burning can be avoided.

Figure 5-103 illustrates that a load spreader beam spanning the length of the wall can distort the loadbearing performance of the wall. When parts of the wall unit recede from the load the beam allows the bridging of the load to intact wall parts. The investigation has shown that sandwich walls are less damaged along the sides of the test wall. If, as in System 3, these wall sides are additionally stiffened by wooden edge posts the loadbearing capacity of the wall is even further enhanced. This form of load application has the potential to misrepresent and overrate the fire resistance of structural sandwich wall units.

5.4.5 Other full-scale evidence of the fire performance of sandwich wall behaviour
In the tests assessing the fire resistance of the sandwich wall structures, among other panel reactions the generation of large amounts of smoke was noted. Whilst fire resistance tests solely evaluate the resilience of the structure to the heat exposure, the heat induced reactions of the structure are not assessed. The emphasis of the testing is misleading with regard to the intrinsic life safety issues in fire compartments, which are to a great extent related to the reaction of the materials to the fire exposure. Escape criteria, described inter alia in Irvine (2000) and Purser (1995) have been established through animal testing and dangerous, life threatening local conditions are related to the generation of

(i) Heat (causing burns to skin and respiratory tract),
(ii) Smoke (visual impairment),
(iii) Toxic gases (causing hypoxia).

The FPL has attempted to evaluate the fire risk represented by the use of the new sandwich type construction. This information has been compiled here so as to give a guide on future investigations into the real-scale performance of structural sandwich walls, which remains still outstanding. To enable a realistic evaluation, the heat environment was created by wooden cribs. Two test series were conducted

(i) un-loaded room-corner set-up (in 1978)
(ii) loaded three storey structure (in 1980)

Whilst the test programme conducted in 1978 was a material assessment study, the study in 1980 was a combination of fire resistance and reaction-to fire test arrangement, evaluating the loadbearing reaction of the sandwich walls in addition to the contribution of structural sandwich wall systems to a room fire development. Both programmes tested the same type of wall panels as in the previous 1975 fire resistance test programme. In particular the comparison of the test programmes in 1975 and 1980 enabled to evaluate the panel performance depending on the fire exposure.
The heat exposure of the walls in these later tests is more representative of a room fire where the temperatures are undergoing the three-stage development shown in drawing 5-21. Whilst fire resistance tests model the exposure of the wall after flashover has occurred, the realistic fire scenarios cover the growth period after ignition when temperature slowly rise, the fully developed fire, where maximum temperatures are reached and the decay period where temperature slow down. Since the sandwich wall assemblies in the early tests had failed after only 3 to 6 minutes the evaluation of their fire resilience in realistic scenarios was of paramount importance with respect to life safety issues. To ensure the life safety occupants and fire fighters structural elements have to remain stable passed the occurrence of flashover within the compartment.

Drawing 5-21: The course of a well-ventilated compartment fire expressed in the temperature increase as a function of time

5.4.5.1 FPL (1978): Room-corner reaction-to-fire tests in sandwich walls

Holmes (1978) compared the fire performance of different sandwich panels and components in a reaction-to-fire room-corner wall setting. The corner wall test was chosen as it allowed to monitor

(i) vertical and horizontal flame spread,
(ii) temperature development,
(iii) combustion products and smoke development.

In the 1970’s the corner- wall tests were increasingly used in fire research after it had been employed by the University of California, Berkeley to assess the fire risk/ hazard presented by some polymeric building materials (Williamson and Baron, 1973).

Fifteen materials and combination of materials were tested in the programme. The sandwich panel walls consisted of 6.4mm Douglas fire grade A-C plywood cladding, urethane, isocyanurate and paper honeycomb cores. The core materials were also tested separately to evaluate their contribution to the severity of the room corner fire scenario. This is regarded as especially useful since the failure progression of the panels clearly indicate that the foam layer will be exposed to direct heat exposure after the cladding
board layers have fallen off the panel. Red oak, asbestos millboard and ceramic fibreboard were chosen as reference materials. The tests were conducted in a 2.4x3.6x2.4m (width x length x height) room structure. A 2.3kg wood crib was used as ignition source. The programme measured

(i) temperatures reached during the tests in the room,
(ii) smoke density,
(iii) combustion gases.

Table 5-7 compiles ignition times, maximum temperatures reached and flame spread indices for the fifteen tested specimen. The results are listed by increasing order of maximum temperature reached at 1.5m above floor level in the centre of the test room. This reference point was chosen, as it was believed most indicative to life safety. The test witnesses recorded the time until ignition of the wall and subsequent ignition of the ceiling occurred. A flame-spread index was derived based on the time required for the flames to travel up the walls and across the ceiling. The indices were computed relative to the red oak specimen, which was assigned a maximum value of 100 and asbestos millboard, which was assigned the minimum value of 0.

### Table 5-7: (Holmes, 1978)

<table>
<thead>
<tr>
<th>Material</th>
<th>Max. Temperature deg C</th>
<th>Heat flux W/cm²</th>
<th>Ignition (min)</th>
<th>Flame spread</th>
</tr>
</thead>
<tbody>
<tr>
<td>Asbestos millboard</td>
<td>55</td>
<td>0.03</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Paper Honey comb core (fire retarded)</td>
<td>79</td>
<td>0.06</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Ceramic Fibreboard</td>
<td>92</td>
<td>0.03</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>Sandwich panel: Plywood faces, PUR core (76.2mm) and 12.7mm fire-rated plasterboard</td>
<td>110</td>
<td>0.02</td>
<td>5.2</td>
<td>-</td>
</tr>
<tr>
<td>Isocyanurate (50mm), Alufoil both sides</td>
<td>127</td>
<td>0.05</td>
<td>4.9</td>
<td>-</td>
</tr>
<tr>
<td>Red Oak</td>
<td>127</td>
<td>0.09</td>
<td>4.5</td>
<td>5.7</td>
</tr>
<tr>
<td>Isocyanurate, sawn surface</td>
<td>132</td>
<td>0.05</td>
<td>3.4</td>
<td>-</td>
</tr>
<tr>
<td>Isocyanurate, alufoil removed</td>
<td>132</td>
<td>0.05</td>
<td>4.9</td>
<td>-</td>
</tr>
<tr>
<td>Sandwich panel: Plywood, PUR (76.2mm) and 9.5mm gypsum wallboard</td>
<td>132</td>
<td>0.05</td>
<td>5.3</td>
<td>-</td>
</tr>
<tr>
<td>Douglas Fir Plywood</td>
<td>138</td>
<td>0.19</td>
<td>3.8</td>
<td>5.2</td>
</tr>
<tr>
<td>Sandwich panel: Plywood, PUR (76.2mm)</td>
<td>235</td>
<td>0.28</td>
<td>4.8</td>
<td>5.5</td>
</tr>
<tr>
<td>Sandwich panel: Plywood, PIR (50mm)- alu foil removed</td>
<td>312</td>
<td>-</td>
<td>4.2</td>
<td>5.2</td>
</tr>
<tr>
<td>Sandwich panel: Plywood, Paper</td>
<td>335</td>
<td>0.18</td>
<td>4.6</td>
<td>5.5</td>
</tr>
<tr>
<td>Urethane, sawn surface</td>
<td>338</td>
<td>0.56</td>
<td>3</td>
<td>3.2</td>
</tr>
<tr>
<td>Sandwich panel: Plywood faces, PIR (76.2mm)</td>
<td>349</td>
<td>4.2</td>
<td>5</td>
<td>113</td>
</tr>
</tbody>
</table>

*Note 1* Relative to performance of red oak in room wall corner test- assigned FS value of 100, Asbestos Board FS= 0
In all tests the temperature level was elevated at the 1.5m reference point in the centre of the room. The room temperature generated by the burning wood crib on its own was determined to 55°C with a heat flux level of 0.03W/m². When the cores were assessed without the exposed wall veneer the fire-retarded paper honeycomb core performed best, showing no ignition nor flame spreading (flame spread index of 0) and consequently the room temperatures only increased about 30°C above those recorded in the wood crib test. However the heat flux doubled to 0.06W/m². Maximum heat flux levels of 0.56W/m² were reached when the PUR core was tested without plywood cladding. It ignited after 3 minutes and flame had spread to the ceiling in seconds. The flame-spread index of the urethane core is 76% above the one for red oak. The PIR core performed markedly better than the urethane core with maximum temperatures of 132°C and heat flux levels of 0.05W/cm² and moderate flame spread index.

The sandwich panels caused temperature levels above 200°C, with the maximum temperature level of 350°C, observed in the plywood/ PIR panel test. Heat flux levels were respectively increased in the sandwich panel tests when compared to the reference walls- in average 0.2W/m². This was ten times higher than the heat flux levels reached in the tests with asbestos millboard cladding and doubled when compared to the red oak reference test. Douglas fir plywood, when tested on its own, attained a flame spread index of 116. This index remained approximately constant when the plywood was used as exposed sandwich veneer. The sandwich panels generally ignited at about 4-5 minutes and only seconds later the ceiling became involved in the flames, similar to when the plywood was tested on its own. In the sandwich walls the plywood face delayed ignition times but increased the flame-spread rate. Overall the heat/ temperature produced in the room was most affected by the core material used in the sandwich. When clad with plywood the urethane core performed superior to the PIR, as illustrated by 100°C temperature difference measured between both tests. As in the core investigation the plywood faced paper honeycomb core produced the lowest overall heat flux and temperature levels. The heat flux measurement for the PIR cored panel was flawed and is not given in the reference. In general the radiation intensities recorded at the measuring point, 3.35m away from the burning specimen, would have been insufficient to cause auto- or pilot ignition of any usual room combustible located in similar distance.

Plasterboard cladding improved the fire behaviour of the sandwich walls since temperatures reached within the room were well below 200°C and heat flux levels compared to the asbestos millboard test result. In addition flame spread indices are 0 when plasterboard was used to clad the exposed face of the wall. Nevertheless the protected wall ignited after 5 minutes, which is to certain extent contradictory information to the before mentioned flame spread results. Unfortunately it is not stated in the text how and where ignition occurred and which part of the wall was burning. It can only be assumed that the plywood faces ignited after parts of the plasterboard had been damaged and thereby exposed parts of the sandwich wall, which allowed heat to reach the panels. However the involvement of the wall was not severe enough for flames to ignite the ceiling panels.
The smoke development during the tests was analysed to determine two measures

(i) The total smoke build-up in the test room
(ii) The rate of smoke increase

Especially the second measure (ii) is closely linked to life hazard as it causes decreasing visibility and thereby reduces escape activity. The results collected are not conclusive. The PUR foam core, when tested without cladding board generated the greatest amount of smoke. The 76.2 and 50mm thick PIR foams caused equally high smoke levels, both about ten times greater than red oak and more than three times greater than the asbestos lining. The application of sacrificial, protective plasterboard layers reduced the total amount of smoke and the rate of smoke build-up by almost half. Similar to the smoke measurements before the sampling of combustion gases was afflicted with difficulties and thought to be affected by the measurement technique. The author considered the results of the gas analysis to not be truly representative of material behaviour and suggested further analysis procedures to verify the measured combustion gas levels.

Overall these results give a good indication on the performance of the more modern sandwich walls investigated in this study. Generally the use of mineral based veneers is advantageous as it eliminates the spread of flame within the compartment. In OSB clad sandwich walls the flame spread must be restricted and plasterboard claddings are well suited to achieve this. Once the exposed veneer has failed the core decomposition contributes significantly to the heat and smoke/toxic gas build-up in the fire compartment. Although modern fire retarded foams are reducing the contribution of the foam to the fire severity, the materials currently used in sandwich wall assemblies are likely to aggravate the fire development. With the suggested changes to Cone calorimeter the performance of foams used in sandwich walls should be assessed in more detail.

5.4.5.2 FPL (1980): Structure fire test

The 1980 test house, founded on concrete slabs were flat-roofed and subdivided into three rooms (Drawing 5-22). The rooms were interconnected by door openings. Windows were located in each room and left open through the course of the test as were the doorways.

![Diagram of the test house](image)

**Drawing 5-22: Plan view of sandwich and wood-frame structures, showing locations of wood cribs, (Eickner, Holmes et al. 1981)**

Chapter 5- Results: Full-scale fire testing 5-123
The wall constructions were identical to the ones assessed in the furnace wall resistance tests, reported by Eickner in 1975, see table 5-6. The fire load was simulated by wood cribs. The average fire load density was 22kg/m², which corresponded closely with the fire load density in residences established in a survey of American homes by the National Bureau of Standards in 1942. The interior plywood linings contributed to the overall combustible content in the structures. A vertical load of 18.6kN/m was applied to both of the longer north and south walls of each structure, see drawings 5-23.

![Drawing 5-23: Loading condition of test walls, (Eickner, Holmes et al. 1981)](image)

The load was applied through four pulley and cable systems attached to slabs spanning across the roof, fastened to a dead load. Due to the three-dimensional room set-up the test walls were partially restrained at their vertical edges by the east and west walls as well as by the internal wall in the centre of the structure. There also was some restraint provided through the roof members. Structural failure was considered to have occurred when the deflection of the walls was so large that the dead weights were lowered to the ground. Although the deflection of the walls during the tests was monitored they were not included in the final report of the tests. The fire was well ventilated since the unglazed windows were open to inflowing air from the start of the test. The total window area was chosen so as to enable the critical air flow required for ventilation controlled fires, as described in Lie (1972).

The test structures were instrumented to record the main data recorded was

(i) temperature development within the room structure,
(ii) wall deflection and failure,
(iii) toxic gas levels (carbon dioxide, carbon monoxide, hydrogen),
(iv) light obscuration by smoke development,
(v) visual observation.

Although the heat build-up in the rooms were closely monitored the temperature reaction of the wall structures were not analysed in detail. The report mentions the positioning of thermocouples in the layered construction but exact locations are not detailed and remain
uncommented throughout the work. Similar to the temperature reaction of the walls their
deflection patterns were not reported in the publication of the work.

In the structures built with wall elements with non-combustible linings the average
flashover time was approximately 25 minutes. When combustible linings were exposed to
the wood crib fire the time to flashover was reduced by about 20 minutes. Just prior to
flashover there was considerable emission of non-flaming hot gases from all windows. In
the four structures with plywood linings the average temperature associated with
flashover, at 2.1 m room height, was 338°C. In the structures with non-combustible wall
linings this temperature was slightly higher (376°C). In the fully developed fire stage
peak temperatures of 980°C to 1150°C were reached. The highest temperatures were
observed in the protected fibrewood sheathed timber frame construction. In general
higher overall temperature levels were reached in the structures clad with gypsum board
lining, as these tests were continued longer than the tests on unprotected assemblies.
Incident heat flux measurements were only of limited meaningfulness, as the instruments
were not recording due to a malfunction or were removed shortly before flashover. One
of the maximum radiation values recorded just before flashover was 7kW/m². Although
the materials used in the walls in the current study were not identical to the ones in the
FPL tests described before, the general trend of these results is deemed to represent the
conditions in a fire compartment built with the more modern wall panels investigated
before.

The unprotected sandwich panel walls performed worst out of all walls and buckled under
the load as soon as 5 minutes prior, and only 4 minutes after flashover. At failure the
temperatures near the walls had reached 200- 430°C. The early failure of the walls at the
respectively low temperature levels reinforced the need of plasterboard protection for
these types of walls. In the protected sandwich walls failure was delayed to 7 to 17
minutes after flashover had occurred in the large room. After the structural failure the
interior, exposed plywood facing partially had delaminated from the core and had bowed
towards the fire while the remaining panel had buckled outward. The burning plywood
facing and backing core material produced a large amount of smoke and occasionally
explosive noises were recorded prior to structural failure, especially in the unprotected
PIR cored walls. In contrast to the sandwich walls the plywood sheathed timber frame
walls failed at 8 to 20 minutes after flashover. When the frame was additionally protected
by plasterboard the failure was further delayed and failure only occurred 30 minutes after
flashover.

The results of the light transmission measurement showed ambiguous results and the
smoke build-up in the room could not be determined accurately. The data was widely
scattered but generally indicated highest smoke obscuration shortly before flashover.
Comparisons amongst the different wall assemblies were not undertaken and are difficult
to evaluate in retrospect. Chemical analysis of the atmosphere in the room showed very
little evidence of HCN, although this compound is likely to be formed upon degradation
of urethane based foams. As noted before with the evaluation of smoke data, the CO and
CO$_2$ gas concentrations were analysed with respect to their occurrence within the structure rather than in dependency of the different wall assemblies.

The fire resistance established for the identical wall assemblies under laboratory conditions and furnace exposure (Eickner, 1975) could not be correlated to the fire performance in the room set-up. Times to failure were much reduced when the panels were exposed to the standard heating curve and the less severe temperature build-up in the room fire prolonged the fire resilience of the sandwich wall assemblies. In table 5-8 the average ranking for the four walls in the room test are compared to the ranking established by the fire resistance test. The ranking of the wall performance in the respective test was for most wall assemblies similar, suggesting that the overall performance might well be predictable through the fire resistance test but the established failure might be enhanced in a real room fire scenario. In any case (wood clad) sandwich walls must be protected by additional plasterboard to ensure the life safety of occupants and fire fighters and allow the wall load to be supported beyond the time to flashover.

**Table 5-8: Comparison of ranking of wall structures between structure fire and ASTM standard fire resistance test, (Eickner, Holmes et al. 1981)**

<table>
<thead>
<tr>
<th>Structure</th>
<th>Wall construction</th>
<th>Structure fire test</th>
<th>Fire resistance test</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Time to failure</td>
<td>Ranking</td>
<td>Time to failure</td>
</tr>
<tr>
<td></td>
<td>(mm:ss)</td>
<td></td>
<td>(mm:ss)</td>
</tr>
<tr>
<td>1</td>
<td>Plywood-PUR</td>
<td>14:30</td>
<td>03:00</td>
</tr>
<tr>
<td>2</td>
<td>Plywood-PUR+ 12.7 fire rated PB</td>
<td>42:15</td>
<td>23:00</td>
</tr>
<tr>
<td>3</td>
<td>Plywood-PIR</td>
<td>19:15</td>
<td>03:10</td>
</tr>
<tr>
<td>4</td>
<td>Plywood-Paper</td>
<td>24:00</td>
<td>05:55</td>
</tr>
<tr>
<td>5</td>
<td>Wood frame-plywood+ 12.7 fire rated PB</td>
<td>46:00</td>
<td>29:30</td>
</tr>
<tr>
<td>6</td>
<td>Wood frame- wood fibre 12.7 Fire rated PB</td>
<td>39:45</td>
<td>34:00</td>
</tr>
<tr>
<td>7</td>
<td>Wood frame- plywood</td>
<td>32:15</td>
<td>21:08</td>
</tr>
</tbody>
</table>

The comparison of wall performance in standard fire resistance testing and realistic fire scenarios has great value to the application of the fire resistance test results gathered in this study. It is acknowledged that the fire resistance regime does not provide meaningful performance data for assessing the fire performance of building systems in real fire scenarios. With the testing methodology developed in this study, mainly with the modified bench-scale test, the evaluation of wall systems in different fire scenarios can be facilitated. In combination with the findings from Eickner, Holmes et al. (1981) this allows to further explore the effect of severity of fire exposure on the fire resistance performance of walls and is strongly recommended for further study.

Chapter 5- Results: Full-scale fire testing 5-126
5.4.6 Summary
The highly instrumented full-scale wall tested in the scope of the project allowed the
detailed examination of the temperature reactions within the wall and their relation to the
stiffness reaction of the wall under vertical loading. The loss of plasterboard was the main
factor influencing the temperature reaction of the wall. Areas of early plasterboard loss
were mirrored by high, early temperature readings within the loadbearing wall unit. The
area of plasterboard loss was located in the central part of the wall, where edge restraint
was least influential and here the rise of temperature was sudden and immediate. In areas
where the plasterboard loss was delayed the temperature development through the depth
of the section was more gradual and less instant. Internal studding and jointing units were
subject to the same temperature variations depending on their location; the outer studs/jointing sections were least affected by the heat environment in the furnace and remained
at nearly ambient temperature level throughout the test, whilst central studding heated up
considerably. Throughout the test a temperature differential between exposed and
ambient flange of the studding units was measured. The sandwich wall deflected into the
furnace from the start of the test, levelling before rapidly deflecting to failure. At failure
the top joint had rotated bridging the air gap, which was caused by manufacturing
inaccuracies and the studs in the centre of the wall buckled. In the edge areas of the wall
charred core pieces were found covering the internal studs. In these edge areas of the wall
brittle board layers were still attached to the cold-formed steel units. Despite the
disappointing outcome of the laser deflection measurement the principle is thought to
offer great advantages in the detailed monitoring of walls in fire resistance tests, allowing
the scanning of more than 20 locations in less than 2 minutes.

The data of additional full-scale fire resistance tests has been analysed and compared to
the findings of the full-scale wall tested in the scope of the study. Whilst System I and
System 2 exhibited very similar deflection patterns, although the initial deflections
recorded in the System 1 panels were larger, System 3 performed different. The System 3
panel system remained stiffer throughout the test, which was in parts due to the lower
vertical load applied to the wall but mainly caused by the inclusion of full height wooden
edge posts, included to prevent the premature involvement of the core at the sides of the
wall. Whilst failure of the sandwich wall is generally related to the structural failure, the
inclusion of wooden edge posts does prevent the critical loadbearing failure and burn
through insulation failure becomes the predominant failure criterion. This type of reaction
aligns the behaviour of the structural sandwich walls closely with timber frame wall
performance where insulation and integrity performances are governing to design. This
characteristic performance is mainly influenced by the full-scale test set-up, which
normally employs a load spreader beam for applying the load. This spreading of load
allows the panel to remain stable for longer periods of time, as less damaged wall areas
take over major parts of the load. When no internal units are present in the panel
composition sandwich walls fail structurally within 5 minutes; sacrificial plasterboard
lining delays failure by up to 25 minutes. The use of glass-reinforced plasterboard further
delays the temperature build-up and also reduces the stiffness loss in the sandwich wall.
Wall performance is generally influenced by the boundary conditions of the test regime; the use of a load spreader beam to apply the vertical load has been shown increase the loadbearing ability of the wall section by allowing the spreading of load to strong wall areas. This shift is not always representative of the in-situ application of the wall where timber joists are rested individually on the wall section, hence applying localised point loads, which could fail the wall as soon as one area has been damaged.

The contribution of sandwich wall assemblies to the pre-flashover fire environment in a compartment has been re-evaluated by presenting test information from reaction-to-fire tests undertaken by the Forest Products Laboratory. These tests have shown that the facing board material of structural sandwich walls governs the rate of flame spread and therefore fire growth in the pre-flashover compartments, whilst the internal core material impacts on the temperature levels in the fire compartment. The synthetic core materials caused high temperature within the room structures and increased flashover potential in the compartment. Generally the use of sacrificial plasterboard, shielding the panel was seen to enhance performance and could delay the involvement of the panels. In comparison to traditional timber frame walls the smoke build-up was more severe in test arrangements with sandwich walls, although the results were not always conclusive. For modern sandwich walls these results remain valid. In applications where plasterboard is omitted, panels with wood based veneers cannot be used since they promote flame spread, mineral based boards should be safe in use. Generally sandwich walls represent a fire hazard when the internal core material is exposed. New fire rated foam materials would need to be further examined to assess their smoke and toxic gas and heat release potential in a room fire scenario. The severity of exposure was seen to affect the performance of the wall structure. Both, the fire hazard and influence of fire severity on wall performance need to be examined in further detail.
Table 5-6: Results of various full-scale fire resistance tests on structural sandwich walls

<table>
<thead>
<tr>
<th>System</th>
<th>Test</th>
<th>Boards</th>
<th>Cores</th>
<th>Adhesion</th>
<th>Jointing</th>
<th>Internal links</th>
<th>Protection (mm) (Plasterboard)</th>
<th>Load (kN/m)</th>
<th>Fire resistance (min)</th>
<th>Type of failure</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1</td>
<td>Pyrok</td>
<td>PUR</td>
<td>SA&quot;</td>
<td>U-channel</td>
<td>Yes (Σ)</td>
<td>15*</td>
<td>25</td>
<td>56</td>
<td>Loadbearing</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>Pyrok</td>
<td>PUR</td>
<td>SA&quot;</td>
<td>U-channel</td>
<td>Yes (I)</td>
<td>15</td>
<td>25</td>
<td>67</td>
<td>Loadbearing</td>
</tr>
<tr>
<td>2</td>
<td>1</td>
<td>Sasmo</td>
<td>PIR</td>
<td>SA&quot;</td>
<td>Hat (GRP)</td>
<td>Contiguous Steel</td>
<td>Yes (Σ)</td>
<td>15</td>
<td>48</td>
<td>Loadbearing</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>OSB (3)</td>
<td>PIR</td>
<td>SA&quot;</td>
<td>Wood rail</td>
<td>OSB tongues</td>
<td>No</td>
<td>12.5</td>
<td>54</td>
<td>Integ./Insul.</td>
</tr>
<tr>
<td>3</td>
<td>1</td>
<td>Douglas fir</td>
<td>PUR</td>
<td>Adhesive</td>
<td>-</td>
<td>Plywood tongues</td>
<td>No</td>
<td>19</td>
<td>3</td>
<td>Loadbearing</td>
</tr>
<tr>
<td></td>
<td>2</td>
<td>Douglas fir</td>
<td>PUR</td>
<td>Adhesive</td>
<td>-</td>
<td>Plywood tongues</td>
<td>No</td>
<td>19</td>
<td>23</td>
<td>Loadbearing</td>
</tr>
<tr>
<td></td>
<td>3</td>
<td>Douglas fir</td>
<td>PUR</td>
<td>Adhesive</td>
<td>-</td>
<td>Plywood tongues</td>
<td>No</td>
<td>19</td>
<td>8</td>
<td>Loadbearing</td>
</tr>
<tr>
<td></td>
<td>4</td>
<td>Douglas fir</td>
<td>PUR</td>
<td>Adhesive</td>
<td>-</td>
<td>Plywood tongues</td>
<td>No</td>
<td>19</td>
<td>3</td>
<td>Loadbearing</td>
</tr>
<tr>
<td></td>
<td>5</td>
<td>Douglas fir</td>
<td>PUR</td>
<td>Adhesive</td>
<td>-</td>
<td>Plywood tongues</td>
<td>No</td>
<td>19</td>
<td>24</td>
<td>Loadbearing</td>
</tr>
<tr>
<td></td>
<td>6</td>
<td>Douglas fir</td>
<td>PUR</td>
<td>Adhesive</td>
<td>-</td>
<td>Plywood tongues</td>
<td>No</td>
<td>19</td>
<td>25</td>
<td>Loadbearing</td>
</tr>
<tr>
<td></td>
<td>7</td>
<td>Douglas fir</td>
<td>Paper</td>
<td>Adhesive</td>
<td>-</td>
<td>Plywood tongues</td>
<td>No</td>
<td>19</td>
<td>6</td>
<td>Loadbearing</td>
</tr>
<tr>
<td></td>
<td>8</td>
<td>Hard board</td>
<td>Paper</td>
<td>Adhesive</td>
<td>-</td>
<td>Plywood tongues</td>
<td>No</td>
<td>19</td>
<td>4</td>
<td>Loadbearing</td>
</tr>
</tbody>
</table>

1) self-adhesive foam
2) Thickness of fire retardant mastic coating applied to exposed face of panel
3) Paper (16.3kg) honeycomb core, 12.7mm cell size, treated with Phenolic

*Plasterboard reinforced with glass fibres
Internal units, i.e. studs and vertical joints, are not abutting at top and bottom (shown in picture)

Figure 5-86: Construction flaw in full-scale test panel
Vertical inserts and core recessed too much

Figure 5-87: Full-scale sandwich wall unit before test
Figure 5-88: Temperature development in sandwich wall assembly behind plasterboard

Figure 5-89: Temperature development in full-scale sandwich wall at interface of exposed face and core

Chapter 5 - Results: Full-scale fire testing
Figure 5-90: Combined temperature readings behind plasterboard and at interface of exposed board and core of full-scale sandwich wall

Figure 5-91: Temperature development in centre of full-scale panel and at interface of core and ambient veneer

Chapter 5- Results: Full-scale fire testing
Figure 5-92: Temperature differential measured in internal studding units

Figure 5-93: Deflection signature in centre and edge of structural sandwich wall

Chapter 5- Results: Full-scale fire testing
Figure 5-94: Full-scale wall unit at failure

Figure 5-95: Full-scale wall after failure
View of ambient face
Figure 5-96: Exposed face after removal from furnace

Figure 5-97: Close-up on exposed face of internal unit, enclosed in decomposed foam
Figure 5-98: Comparison of ASTM E-119-95 (9) and BS 476: 20 (4) and BS EN 1363-1(8) heating regime

Figure 5-99: Temperature development behind exposed plasterboard layer- Comparison standard and fire rated plasterboard
Figure 5-100: Temperature development in depth A of various sandwich wall systems. Comparison System 2 and FPL tests (Eickner, 1975)

Figure 5-101: Temperature development in centre of core of sandwich walls Comparison System 2 and FPL tests (Eickner, 1975)
Figure 5-102: Comparison of central deflection rates in different sandwich walls clad with 15mm standard and glass-reinforced plasterboard (PB) (All panels include internal veneer links)

Figure 5-103: Bridging of load when central part of the wall is damaged
5.5 Structural bending tests at ambient temperature

5.5.1 Introduction to structural testing

The structural testing of the units at ambient temperature, in bending and vertical loading presented later in section 5.6, was designed to support and expand the knowledge on the fire behaviour of structural sandwich walls. Although the fire test results had established the damage sustained by the walls during the fire exposure, the stiffness reduction measured during the fire tests was linked to ongoing, transient damage being inflicted to the wall. The structural testing of the walls at ambient temperature enabled to establish datum wall performances at varying extent of damage. The extent of the damage was modelled based on the findings of the fire investigation and the specimen panels were manufactured to model the most prominent fire failure characteristic: the loss of the exposed board. The basis for the assessment of the reduction in loadbearing ability is the knowledge about the loadbearing performance of the walls at ambient temperatures and intact composition. Whilst sandwich theory is shown to be well suited for classical sandwich walls, consisting of two boards and core, the inclusion of an internal stud, as present in some of the sandwich wall systems needs to be examined for bending and vertical load performance.

Table 5-9: Overview panel composition and test regimes for structural tests at ambient temperature

<table>
<thead>
<tr>
<th>Panel composition</th>
<th>Intact</th>
<th>Damaged</th>
<th>Intact</th>
<th>Damaged</th>
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<tbody>
<tr>
<td>Test regime</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Bending</td>
<td>X</td>
<td>X</td>
<td>X</td>
<td>X</td>
</tr>
<tr>
<td>Vertical load</td>
<td>X</td>
<td></td>
<td>X</td>
<td>X</td>
</tr>
</tbody>
</table>

5.5.2 Objectives and framework of structural bending tests

The objectives of the bending test programme were (see also Drawing 5-24)

(i) to determine the ultimate bending capacity of the sandwich walls in plain and studded configuration,

(ii) assess the influence of internal studs including the effect of panel width and connection between sandwich section and internal unit,

(iii) evaluate the influence of panel width (for both plain and studded panel section),

(iv) study the extent of performance loss in different panel compositions.
The test scheme employed a range of panel configurations as seen in drawing 5-24 and the specimens were all exclusively taken from panels of System 1, described in Chapter 2 since these panels included internal veneer linking units by default. The performance levels of the panels clad by the range of board material assessed in the fire testing programme, apart from the wood based OSB, are thought to perform similar to the Pyrok board used in System 1. The small-scale shear test programme (see drawing 4-6) confirmed this assumption. Wood based boards are generally less brittle and more ductile in their load performance, an asset which is vital in the racking performance (Chapter 7), where the board material has been included in the testing programme. The samples were cut out from full size panels and in some tests the tension board was manually delaminated from the remainder of the panel. In three tests the delaminated face was re-attached to the panel by screws at varying width. All specimens were subjected to three point bending and were tested for stiffness and strength. The exact testing procedure and sample replicates have been presented in Chapter 4.

5.5.3 Influence of internal stud units, width, length and fixing of stud
The first tests were designed to establish the loadbearing capacity of the sandwich wall sections with and without internal veneer links. The variation in panel width, composition and screw fixings was to establish the composite capacity of the 1.1m length sections. The theoretical determination of the loadbearing ability of structural sandwich walls as presented in Allen (1969) has been examined. In System 1, where studding is used by
default in order to enhance the panels' fire resistance, the studs have never been accounted for in the structural design. Their contribution with respect to stiffness relative to the stiffness of the entire sandwich unit was thought to be minimal and therefore neglected in design calculations.

5.5.3.1 Performance of plain panels without studs
The typical load/deflection plot for the sandwich units without stud was characterized by a linear, gradual increase in deflection with increasing load. The load/deflection plots for the plain sandwich wall panels are shown in figure 5-104 and stiffness and strength performances are listed in table 5-10. With increasing width, strength and stiffness of the panel increase. In figures 5-105 the panel width and ultimate failure load/stiffness is correlated. The best-fit correlation of the width influence is of linear form and quite accurate and is similarly applicable to the load/deflection gradient (stiffness) as seen in figure 5-106. With increasing width the results are more variable, both with regard to the rate deflection and the overall ultimate load. The failure mode of the panel showed a 45° tension failure in the lower board progressing through 2/3 of the core. The failure was brittle and sudden.

A) The prediction of the panels' bending deflections
The deflection rates of the sandwich units without studding could be well predicted through the deflection formula given by Allen (1969), see figure 5-107.

\[ w_{\text{Total}} = w_{\text{Bending}} + w_{\text{Shear}} \]

and as in the test case of a single load in the centre of the span

\[ w_{\text{Total}} = \frac{Fl^3}{48D} + \frac{Fl}{4AG} \]

where

\[ F \] Applied load
\[ l \] Span
\[ D \] Bending stiffness of sandwich unit \( \rightarrow D \approx E_f \frac{bd^2}{2} \)
\[ b \] Width of the sandwich unit
\[ t \] Thickness of face layer
\[ d \] Distance between centre lines of opposite face layers

\[ A = \frac{bd^2}{c} \] derived from \( \frac{dw}{dx} = \gamma \frac{c}{d} = \frac{Q}{Gbd} \frac{c}{d} = \frac{Q}{AG} \)

\[ \gamma \] Shear strain
\[ c \] Core depth
\[ Q \] Shear force
\[ G \] Shear modulus of core

Sandwich structures are shear weak, since the different material layers move relative to each other under load. The overall deflection of the unit is therefore calculated by adding
the bending and shear deflection, as shown above. The overall performance of the units is to great extent influenced by the shear modulus of the core. The shear modulus used in the prediction was determined from test evidence on small-scale shear samples of the system to quality assurance testing to ECCS (Griffiths and Bregulla, 2002d). The shear values given in product literature for cores are generally lower than encountered in the panel, which can affect greatly the accuracy of the prediction. This is discussed further in section 5.5.3.2. The accuracy of the prediction decreases at increasing panel width as the model overrates the stiffness of the unit. This could be related to additional creep effects encountered in the test units but also to a varying shear modulus within the core at increasing panel width, as suggested by Davies (1987). The ultimate load of the units could be well approximated through the tensile strength of the lower board, which was seen to initiate sudden failure. Here the mean board tension strength was derived, since the manufacturers’ board data was overly conservative. The comparison of panel performance at different spans exhibited that performance was independent of the panel’s stiffness $EI$ and shear stiffness $AG$ such that the load/deflection behaviour at the different spans could be simply correlated through a length conversion factor (see also figure H-1 in Appendix Results Horizontal tests).

5.5.3.2 Performance of panel with studding

When an internal stud unit is foamed into the core the load/deflection relation changes, as shown in figure 5-108. The load/deflection response obtained from testing a 1.1m long beam to failure was found to exhibit three regions of behaviour. At low applied loads, to about 4kN, the stiffness was highest. As the load increased there was a measurable reduction in stiffness, shown by a change in gradient. The initial stiffness was dependent on beam width, as was the ultimate load reached. Failure was more ductile than in the non-studded units. In panels where the peak load was significantly above the 5kN mark the load dropped upon failure from the respective higher load to the 5kN level. At failure the tension board was torn and the internal studs had yielded, at large deflections the upper board was spalling and crushing with the increasing load. Figures 5-109 and 5-110 and table 5-10 plot the strength and stiffness performance of the panels at the varying widths. Whilst the strength results are given for each test, the stiffness results, which were very uniform, are the mean of the respective replicate tests. Although in both cases the correlation of panel width and ultimate load and stiffness was linear, the strength results were scattered especially at larger widths. This was observed before in the plain panels without internal studding and the influence of span was also similarly a pure length conversion, independent of the panel section properties.

Comparing section of identical widths with and without studs, as in figure 5-111, the gradient of the non-studded section was less steep and the failure was premature and more brittle. With increasing width the stiffness gradients approached but only to a maximum of 2/3 to the studded load/deflection rate. The ultimate tension board deflection was limiting the ultimate load for both panel types and in both configurations
the governing factor in initiating the failure of the panel (here shown for 400mm wide specimens).

A) Prediction of bending capacity of panels with internal studding
As before in the plain sandwich wall sections, the deflection model by Allen (1969) has been used to predict the performance of the walls by adding the stud stiffness\( (EI_{Stud}) \) to the plain panel stiffness. The enhanced stiffness and strength encountered in the sandwich units with internal steel stud could not be modelled through this approach and the characteristic deflection could not be matched, see figure 5-112, as it merely proportionally increased the load deflection rate of the unit but to a lesser extent than tested. Since the stiffness of the internal stud unit only marginally affects on the bending deformation of the panel, the enhanced bending capacity of the sandwich walls with stud must be related to the reduced shear deformation of the units. To enable the low deflections encountered in the studded panel tests the shear displacement must be reduced to great extent. The governing factor to the shear performance of the wall is the shear modulus \( (G) \) of the core material. The influence of varying shear modulus on the load performance of sandwich walls is shown in figure 5-113.

B) Influence of internal studding on the shear displacements of sandwich units
The loadbearing enhancement achieved by the augmented shear capacity of the core can be appreciated in the theoretical design formulas and also by test work by the Forest Products Laboratory (Eickner, 1975) or Davies (1987). By linking the veneers the internally placed steel stud enhances the shear resistance of the core, i.e. the relative movement of the veneers under loading, decreasing the overall deflection of the unit under load. The stiff internal steel unit reduces the relative shear displacement of the upper and lower board layer and thereby minimises the additional shear deformations encountered in the shear weak composite structures. To model the load performance of studded sandwich panels the core shear modulus needs to be adopted (Figure 5-113). In order to fully profit from the internal unit as shear reinforcement all panel components must be rigidly connected and the glued compound established by the self-adhesive core, aided by the holed webs of the studs, is of paramount importance in preventing the independent movement of the components. If this bond is adequate the increase of screw fixings along the length of the stud does not impact on the overall loadbearing capacity of the panels as seen for varying panel widths in figures 5-114 to 5-115. In the panels with closer screw spacings the load recovers more efficiently after the tension failure of the board. This can be attributed to the fact that the remaining panel section, remote from the tensile failure can contribute to greater extent to the increase in load, due to the closer attachment of board and stud section.

In the bending test programme on the delaminated panels the characteristics of the composite compound effect was further investigated by re-attaching the delaminated board to the remainder of the panel by screws at various distances, figure 5-116. Although re-attaching the board marginally improved the loadbearing ability of the unit, it did not recover to the capacity measured in the intact panel. There was a marginal
improvement in loadbearing capacity the closer the screws were inserted into the restored panel. The differential movement between board, stud and core, exhibited through movements of the studs at the specimen ends, clearly showed the slip between the members, indicating the members to react only as part-composite. The design model for this type of wall section is discussed in further detail, establishing the loadbearing ability of partly decomposed sandwich walls.

C) Effectiveness of shear links within panel core
In the intact, adequately adhered panel the effectiveness of internal linking members within the intact sandwich unit with respect to reduction of shear deflection is dependent on the width of the sandwich unit. Figure 5-117 plots an “effective G modulus” of the board, stud and core composite, as established through tests, dependent from panel width. It can be seen that the enhancement in shear capacity decreases the wider the sandwich unit. A best-fit analysis has been conducted and the resultant curve shows good accuracy ($R^2=0.969$). Based on these results the influence of internal studding on the shear displacement of a sandwich unit can be extrapolated for a wider range of panel widths. The internal stud always enhances the shear resistance of the core when compared to a non-studded section, even at maximum panel widths, as seen in figure 5-118.

5.5.4 Performance of damaged panel
In the second part of the bending test programme the loadbearing ability of partly damaged sandwich panels has been examined with the aim to correlate the findings to the fire performance of the wall. In the fire investigation the loss of the exposed veneer was identified as one of the predominant failure signatures.

In the fire tests the degradation of the exposed veneer was found to be influential to the stiffness performance of the wall, but since the failure progression in fire tests is transient, the structural tests were designed to conduct a steady state assessment of the effect of such a panel component failure. By inducing the predominant type of damage, its effect on panel performance could be evaluated. As before the influence of internal studding was examined and the failure of the exposed panel veneer on the panel’s loadbearing capacities of the respective panel configuration is shown in figure 5-119, where intact and damaged panels are compared. In all panels the tension face was removed and all test specimens were 200mm wide. The programme was only seen as indicative and therefore only one replicate was tested in each configuration.

5.5.4.1 Effect of veneer loss on loadbearing ability on sandwich walls
The removal of the tension face in a non-studded panel section reduced the overall loadbearing capacity by 77%. Deflection was increased and the panel exhibited a sudden, brittle failure. Failure was initiated by the tension rupture of the core, which proceeded through the depth of the core and was followed by the sudden bending failure of the board. Deflection increased up to about 40mm upon failure.
The loss of one veneer considerably reduces the loadbearing capacity of the non-studded panel but when an internal link is included in the damaged panel the reduction in loadbearing capacity can be markedly dampened. The performance of the damaged section with stud was superior to the damaged non-studded panel but also to the intact non-studded panel. In the studded section failure was more ductile and 90% of the failure load was supported over a deflection of 40 to 45mm. However, the response shown by the delaminated panel section with internal studding remains below the performance level of the intact sandwich unit, with respect to both stiffness and strength (by about 20%). This is due to the fact that the residual unit, consisting of a cold-formed I section and the upper compression board, no longer represents a sandwich unit. Its build-up and loadbearing behaviour resembles more closely a composite structure in the classical sense, as encountered in timber-concrete or concrete-steel composite girders, where the relative moment of resistance of stud, core and board is influential to the overall performance.

The load/deflection behaviour of the delaminated sandwich section with and without internal studding can be well predicted with the relative E-modulus theory, commonly used for beams combining two or more materials (Timoshenko and Young, 1968), see figure 5-120. The prediction of the load-deflection relation for the steel studded unit was accurate up to the ultimate yield stress for the steel. The load-deflection behaviour of the plain section without stud could be well predicted with the board and core material properties noted in the graph. Three section properties affecting on the accuracy of the prediction:

(i) Young’s modulus of the core material
(ii) Section properties of the folded cold formed steel unit
(iii) Slip between stud, foam and board

To the disadvantage of the precise prediction of the load/deflection signature of non-studded delaminated sandwich units the E-modulus of the foam core is difficult to establish from product literature. Since the core’s contribution to the overall bending stiffness of the sandwich unit is normally ignored (Allen, 1969), this material characteristic is generally not quoted. However, the value has been approximated through the density of the core with the help of diagrams given in Davies (1987b). Similarly influential to the prediction of the load/deflection plot are the section properties of the steel stud, which proved to be difficult to be computed accurately by classical methods. The difficulty in establishing the actual stiffness of the unit is probably due to the effect of the folded flanges of the I shaped units, which move relative to each other under load, especially in later stages of the test at higher deflections. The folded flanges do not act as one single unit, as assumed in the calculation for the second moment of inertia (see I calculations in Appendix). In order to ascertain a representative value for the stud stiffness, the moment of inertia was derived from a test on a steel unit (see Chapter 4). Slip between board, stud and core was observed in the tests, which is thought to have additionally affected the accuracy of the prediction.
A) Discussion of models available to account for partial veneer loss and comparison to findings

The gradual and ultimately complete veneer loss in fire damaged sandwich panels without stud sections was theoretically modelled by Mouritz and Gardiner (2002) and is described in further detail in Chapter 3. Their theory is based on the fundamental geometrical principles of shear weak structures as documented in Petersen, 1992 for the case of sandwich composites by Allen (1969). In the Mouritz and Gardiner model it is assumed that the core substrate, without second veneer is still undergoing shear deformation. The deflection analogy for shear weak structures is therefore applied for the changed geometry of the damaged panel, i.e. the decreasing overall thickness of the sandwich section with progressing fire damage.

This model is not thought to be representative of the sandwich panels investigated in this study for two reasons

(i) no gradual veneer loss encountered but sudden through depth failure
(ii) with the material types and quality of cores used in sandwich panel for the building industry it is believed that the core is not strained in shear once one face is removed.

The fire investigation showed the failure pattern of the mineral based board to be sudden rather than gradual as encountered in the facing materials used in the Mouritz and Gardiner study. Similarly the cores used within the study are thought to bend with the stiff facing as it deforms under the given loads rather than deform in shear. This approach was successfully modelled, as seen in figure 5-120. This implies that once one face is ineffective or removed from the panel the remaining panel face governs the loadbearing ability with only marginal contribution from the core, which is solely providing a shift in neutral axis. It was also shown that the ultimate bending capacity was substantially reduced when only one intact face remained cladding the core. In modified sandwich panels where internal veneer linking units are present this internal link provides additional loadbearing ability.

In a sandwich wall the internal stud unit takes over two major functions, depending on the state of damage:

(i) Once the sandwich unit is damaged: the stud takes over major parts of the applied load,
(ii) In the undamaged unit the stud enhances the loadbearing ability of the undamaged unit by reducing the shear deformation of the core.

5.5.5 Summary

In the reduced scale bending programme the stiffness and ultimate bending capacities of the sandwich sections were shown to be dependent on the width of the unit and the presence of any internal members. With internal cold-formed veneer linking section the ultimate load and stiffness of the entire unit was increased by about 50%. The failure mode of the studded cross-sections was less brittle and abrupt. When internal members were present the ultimate bending load was however independent of the number of
fixings connecting the board and the internal cold-formed member. The theoretical prediction of the load deflection performance of the non-studded units was successful and based on work by Allen (1969). The presence of internal member were seen to have negligible influence on the stiffness of the unit, but were established to reduce the shear deformation of the core, which enhanced the loadbearing ability of the sandwich structure. The examination of the composite bond between internal stud, external panel faces and core exhibited the importance of adequate glued bondage between all members, likely to be only achievable by self-adhesive cores and permeable studing inserts.

In a separate programme the effect of board delamination on studded and non-studded sections was assessed. The results showed that the removal of the tension face in a non-studded panel section reduced the overall loadbearing capacity by 77%. The ultimate load of a studded section was reduced by 20% when compared with the performance of the full panel. In the damaged panel the function of the stud within the sandwich wall altered from acting as shear reinforcement to the core in the intact panel, to becoming the main load carrying section in the damaged panel. When the tension face of the wall was debonded from the panel and reattached with screw fixings at varying distance, panel performance could be improved, although the initial performance of the intact panel could not be regained. However, the shorter the distance between fixings the better the specimen recovered after the tension failure of the board. In both panel set-ups, with and without studding, the removal of one board face was the main influencing factor to load carrying ability.
Table 5-10: Horizontal bending results

<table>
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<tr>
<th>Panel type</th>
<th>Length (m)</th>
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<th>Stiffness (kN/mm)</th>
<th>Load (kN)</th>
<th>Deflection (mm)</th>
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<td>X</td>
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</tbody>
</table>
Figure 5-104: Bending performance of different width sandwich panels without studs

Figure 5-105: Width effect for panels without internal studs- Strength performance
Figure 5-106: Width effect for panels without internal studs
Stiffness performance

Figure 5-107: Prediction of bending capacities for sandwich units of different widths
Figure 5-108: Bending performance of different width sandwich panels with studs

Figure 5-109: Width effect for panels with internal studs

Strength performance
Figure 5-110: Width effect for panels with internal studs
Stiffness performance

Figure 5-111: Comparison of panel performance with and without studs
at 400 mm width (500 mm c/s screws)
Figure 5-112: Influence of change of overall stiffness of unit and G-modulus on accuracy of prediction

Figure 5-113: Influence of studs and G-modulus on prediction of bending capacity (200 mm width samples 500 mm c/s screws)
Figure 5-114: Effect of screw spacings (400 mm panel width)

Figure 5-115: Effect of screw spacings (600 mm panel width)
Figure 5-116: Effectiveness of sandwich compound

Figure 5-117: Influence of stud on G-modulus of sandwich section with increasing width

Chapter 5- Results: Structural bending tests at ambient temperature
Figure 5-118: Theoretically extrapolated influence of one stud on the G-modulus of sandwich section at varying widths

\[ G = 0.1849b^{-0.4779} \]

\[ R^2 = 0.969 \]

No internal stud

Figure 5-119: Bending capacities for sandwich walls in varying failure configurations

Chapter 5- Results: Structural bending tests at ambient temperature
Figure 5-120: Predictions of bending capacities for the different panel configurations
5.6 Vertical load Tests at Ambient Temperature

5.6.1 Introduction
As part of the ambient test programme vertical load tests were undertaken on a range of panel configurations. The test results stem from an earlier test programme and have been re-evaluated in the scope of this study. The findings presented here have been gained from tests on System 1 panels (see also Chapter 2 for further system description) including two internal cold-formed steel units at 600mm centres. The tests were conducted on full size (i.e. 1.2m width and 2.7m height) panels. As before in the structural bending tests at ambient temperature the effect of veneer loss on the panel overall loadbearing capacity was examined in further detail. As part of the full-scale fire resistance programme on various types of intact sandwich walls the FPL (Eickner, 1975) conducted vertical load capacity tests at ambient temperature, which are compared to the findings in this study.

5.6.2 Vertical load performance of sandwich walls in varying configurations
The results of the vertical load tests on three single panels with varying panel composition have been compiled in table 5-11.

5.6.2.1 Vertical load tests- intact panel (V)
All three System I panels reached ultimate loads above 200kN with the lowest value being within 12% of the mean and failed in compression shear 300mm or respectively 600mm from the reaction end. At failure both boards were broken and the internal steel channels were locally buckled. The failure plane was at 45° through the thickness of the panel.

The vertical load tests undertaken by the FPL showed a wider spread in ultimate failure load and failure behaviour. The Plywood PIR panels were performing well below the System 1 wall panels, whilst the Paper honeycomb core exhibited markedly higher failure loads. The PUR core performed weakest of the three sandwich panel types, with a failure load of 134kN and the paper honeycomb cored panel failed at the overall maximum compression load of 357kN. The panels with plastic foam cores, i.e. PUR and PIR cores, are reported to have failed in buckling whereas the paper honeycomb cored panel failed due to compression failure in the facings, similar to the System 1 wall failure.

5.6.2.2 Vertical load tests- eccentric (E) loading of intact panels
The effect of the eccentric loading was to reduce the panels' ultimate vertical load by 1/3. The additional moment applied to the panel altered the failure modes of the eccentrically loaded panels. In these tests the predominant failure was crushing failure of the boards compounded by bending. Two panels failed at the loaded end within the channel section. One panel failed at the centre; it is probable that first the steel channels buckled followed immediately by double compression failure in the top board and tensile failure in the lower board, which could be considered a true buckling failure.
5.6.2.3 Vertical load tests- Delaminated (D) panel

Further three tests were undertaken on panel System 1 to evaluate the vertical load capacity of the sandwich wall panel when one face was debonded from the core but remained in place for the test alongside the remainder of the sandwich panel, namely one face glued to the core, one single face and two studs. These tests linked in with the bending investigation and modelled the effect of veneer loss on the loadbearing ability of the sandwich walls. The vertical loading represented the exposure conditions in the full-scale fire tests. The ultimate failure loads ranged from 66kN to 81kN with the lowest load deviating from the mean by 9%. In some cases the panel and delaminated board bulged into the opposite direction during the tests.

5.6.2.4 Vertical load test- single sided (SS) panels

When the de-bonded face was completely removed from the test panel the ultimate failure load was further reduced to a mean value of about 40kN, as seen in table 5-11. The large reduction in ultimate loadbearing capacity in comparison to the delaminated test panels was thought to be related to the fact that the removal of the veneer imposed an additional eccentricity.

The performance of the wall panels was varied and dependent from panel composition as summarised in figure 5-121. The complete loss of one face board causes the loadbearing capacity to be reduced by about 80%. Due to the veneer loss the panels loadbearing ability had dropped from 208kN/m loadbearing capacity to about 33kN/m. The removal of the veneer also affected on the failure mode, which converted from a compression plane failure in 45° at the upper end of the wall panel to a pure buckling failure with maximum deflections, recorded at the centre of the wall.

5.6.3 Prediction of vertical load capacity

The bending test programme has established the effect of internal links on the overall loadbearing ability of sandwich wall panels. The increase in shear resistance was seen to improve the bending capacity of the panel and figures 5-122 and 5-123 show the effect of shear modulus on the critical vertical load. The vertical load capacity for sandwich columns has been determined using Allen (1969):

\[ P_{\text{crit}} = \frac{P_E}{1 + \frac{P_E}{AG}}, \text{ where } P_E = \frac{\pi^2 EI}{l^2} \]  

the theoretical Euler buckling load.

The accuracy of the prediction of ultimate vertical load capacity for a sandwich wall section with internal links is dependent from three factors

(i) the adjustment of the core shear modulus, which should incorporate the account for the enhanced shear capacity of the core with internal studding member,

(ii) the stiffness of the internal studding unit, resulting in an overall stiffness of the composite unit of \(\text{EI}_{\text{Sandwich/Studs}}\).
As observed before in the bending results, the influence of the shear modulus of the core is substantial, figure 5-122. With a shear modulus of $G_{\text{Core}} = 0.0045\text{kN/mm}^2$, as established for plain, non-studded panel section tested in bending, the results of the vertical load tests could be approximated. However, when the established $G_{\text{Core+Stud}} = 0.009\text{kN/mm}^2$ was used as shear modulus, accounting for the increased shear capacity through the internal stiffening linking stud units the results could be entirely matched. As can be expected the G-modulus adopted model proved to be sensitive to the assumed buckling length, see figure 5-123.

The FPL vertical load test also exhibit the influence of core shear capacity. Since the shear stiff paper honeycomb core exhibited superior vertical loadbearing capacities when compared to the comparatively shear weak PUR cores also assessed at ambient conditions ($G_{\text{Honeycomb}} > G_{\text{PUR}}$).

The ultimate vertical loads encountered in the delaminated wall panels prove that a sandwich panel, with only one board remaining cannot bear the applied load in its own, necessitating the shift of the load onto internal studs. This was shown by calculation and theoretical predictions in the ambient vertical load and bending tests. The ultimate buckling resistance of the internal stud units about their strong axis, since the remaining sandwich panel face holds the weak axis, is about 30kN/m and was determined by BS 5950-5 (29), see also Appendix III.

5.6.4 Summary

As before in the horizontal test programme the vertical load test programme assessed different panel configurations. The panel configuration ranged from the intact, standard panel to a panel in which one face was completely removed. The removal of one panel face resulted in a major performance loss (from about 200kN in the intact panel to 40kN in the damaged panel). Some improvement in performance was observed when the face remained partially effective. Together with the analysis of vertical load tests undertaken by the Forest Products Laboratory (FPL) the influence of core shear modulus on overall performance could be observed. As in the bending programme before, the inclusion of studding and the increase of shear modulus of the core in general resulted in improved vertical load capacities. As before once the panel was damaged the function of the internal stud shifted from shear reinforcement to main load carrying member. This effect is discussed further in Chapter 6.3 “Reduction in loadbearing capacity”.
Table 5-11: Vertical load test results

<table>
<thead>
<tr>
<th>Type</th>
<th>Board</th>
<th>d (mm)</th>
<th>Core</th>
<th>d (mm)</th>
<th>Adhesion</th>
<th>Stubs</th>
<th>Panel size (m) x Height (m)</th>
<th>Loading</th>
<th>Test results (kN)</th>
<th>Mean (kN)</th>
<th>UDL (kN/m)</th>
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</thead>
<tbody>
<tr>
<td>Intact panel</td>
<td>V</td>
<td>8</td>
<td>PUR</td>
<td>70</td>
<td>SA&lt;sup&gt;1&lt;/sup&gt;</td>
<td>Y</td>
<td>1.2 x 2.4</td>
<td>centric</td>
<td>265.6</td>
<td>244.6</td>
<td>203.8</td>
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<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>250.4</td>
<td>217.7</td>
<td></td>
</tr>
<tr>
<td>Intact panel (FPL, 1975)</td>
<td>V</td>
<td>Plywood</td>
<td>6.4</td>
<td>PUR</td>
<td>76</td>
<td>Glue</td>
<td>N</td>
<td>1.2 x 2.4</td>
<td>centric</td>
<td>134</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Plywood</td>
<td>6.4</td>
<td>PIR</td>
<td>76</td>
<td>Glue</td>
<td>N</td>
<td>1.2 x 2.4</td>
<td>centric</td>
<td>208</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td></td>
<td>Plywood</td>
<td>6.4</td>
<td>Paper&lt;sup&gt;2&lt;/sup&gt;</td>
<td>76</td>
<td>Glue</td>
<td>N</td>
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<td>centric</td>
<td>357</td>
<td>-</td>
</tr>
<tr>
<td>Intact panel</td>
<td>E</td>
<td>Pyrok</td>
<td>8</td>
<td>PUR</td>
<td>70</td>
<td>SA</td>
<td>Y</td>
<td>1.2 x 2.4</td>
<td>eccentric</td>
<td>178.8</td>
<td>161.9</td>
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<td>156.6</td>
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</tr>
<tr>
<td>Delaminated panel</td>
<td>D</td>
<td>Pyrok</td>
<td>8</td>
<td>PUR</td>
<td>70</td>
<td>SA</td>
<td>Y</td>
<td>1.2 x 2.4</td>
<td>centric</td>
<td>81.1</td>
<td>73.1</td>
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<td></td>
<td></td>
<td></td>
<td>71.8</td>
<td>66.4</td>
<td></td>
</tr>
<tr>
<td>Single sided panel</td>
<td>SS</td>
<td>Pyrok</td>
<td>8</td>
<td>PUR</td>
<td>70</td>
<td>SA</td>
<td>Y</td>
<td>1.2 x 2.4</td>
<td>centric</td>
<td>40.1</td>
<td>40.3</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>40.5</td>
<td>40.2</td>
<td></td>
</tr>
</tbody>
</table>

1) self-adhesive foam
2) Paper (36.3 kg) honeycomb core, 12.7 mm cell size, treated with Phenolic
Figure 5-121: Vertical load capacities for sandwich walls in varying configurations

\[ P_{crw} = \frac{P_K}{1 + \frac{P_K}{AG_c}} \]

Figure 5-122: Vertical load capacity of full-scale sandwich walls depending on G-modulus of core
Figure 5-123: Vertical load capacity of full-scale sandwich walls depending on G-modulus of core and critical buckling length
5.7 Summary of fire reactions in structural sandwich walls

The combined testing programmes have established the main influencing factors to the fire performance of structural sandwich wall performance. In the full-scale wall the failure of the exposed board layers was seen to be the main influencing factor to its fire resistance. In plasterboard lined assemblies, the plasterboard cladding delays the temperature build-up and the loss of the plasterboard induces the failure of the sandwich wall assembly. This is reinforced by the fact the wall layers heat up behind the plasterboard, accelerating the reaction of the wall once the plasterboard falls off. The shielding ability of the plasterboard relates to the type and thickness of plasterboard; with glass-reinforced plasterboard the thickness of protection can be reduced. The protection provided by the exposed wall layer, the exposed veneer, is also material dependent and the gypsum-based boards were seen to perform superior to cement based boards. Both the plasterboard layer and exposed board surface fall off the wall when they degrade and the amount of restraint provided to the decomposed wall layer is influential to the temperature build-up in the wall. In areas of high restraint, around the perimeter of the wall, the temperatures are less severe than in central panel areas, where the board is least restrained. Once the exposed wall layers have been removed the fire behaviour of the internal core material is the main influencing factor to the speed of temperature increase through the depth of the wall, hence the integrity and insulation performance.

The loss of the exposed board layers results in a marked reduction in loadbearing ability. In classical sandwich walls loadbearing failure occurs, once the exposed board layer has failed. In panels with internal units structural failure is delayed. The function of internal units within sandwich wall structures has been investigated. In the intact sandwich wall, the internal linking of the veneers enhances the shear resistance of the core and thereby reduces the shear related deformations, commonly encountered in composite structures. This core reinforcement can only be achieved if all panel components are rigidly connected and in panels built with self-adhesive foams and perforated stud units this bond is adequate and does not require additional screw fixings. In the damaged panel the internal units become loadbearing members due to a shift in load once the sandwich compound is destroyed. The temperature build-up in the internal units is mainly influenced by the fire performance of the core and shielding ability and restraint of the exposed board. Fire resistant cores have the ability to protect deeper panel layers, either by char or inherent non-combustibility, which protects the internal units by sheltering them from direct heat exposure.
6.1 Introduction

The discussion examines the reaction of structural sandwich walls to severe heating conditions based on the findings presented in Chapter 5. The outcome of the parametric study in each test scale is analysed to establish the main failure criteria as related to

(i) pattern of temperature reaction, linked to the failure signature in the layered wall unit,

(ii) effect of heating and degradation of the wall components on the loadbearing ability and fire resistance of the full-scale wall structure.

The fire resistance performance of layered structural sandwich walls is linked to the degradation characteristics of the individual panel components. Therefore the discussion firstly evaluates the heat reactions and degradation characteristics of the individual panel components, i.e. boards, sacrificial plasterboard, core, intermediate studs and joints. The findings in the testing regimes, e.g, fire testing in three scales and structural testing at ambient temperature, are combined to examine the effect of the panel components’ heat reactions on the overall wall performance. The bench-scale regime has been of major importance for this part of the assessment as it enables, in its modified form, the determination and correlation of failure mechanisms throughout the wall depth. The findings in the bench-scale test regime have been used extensively throughout the section.

In the subsequent section the largest set of the results is combined to establish the reduction in loadbearing ability of the composite wall units with exposure time to ultimately determine the factors influencing fire resistance. The temperature reactions and failure mechanisms leading to the reduction in loadbearing capacity are assembled from the findings on the single panel components, discussed in the previous subsections. The structural testing at ambient temperature contributes crucial additional information to examine the varying stages of panel degradation within the sandwich wall in the transient heating regime. The combined fire reactions of the panel components translate into the fire resistance of structural sandwich walls when subjected to a loaded full-scale fire resistance test. Based on the findings three generic types of sandwich walls can be determined and the chapter suggests the analytical modelling of their fire resistance.

Finally in section 6.4 the link between the different testing programmes and the predictability of the wall fire response through the multi-scale test approach is analysed. Whilst the test scheme was developed to examine the fire performance of sandwich walls, this chapter explores the possibilities of the testing in scales in establishing the true adequacy of building units. The physical link between the scales is presented and the limitation of the reduced scale approach discussed if used alone. The findings in each
scale piece together the performance characteristics of structural wall units and a scaled test scheme can be suggested, which, depending on the type of change in wall composition, reduces the need for full-scale testing. The information gathered through the testing in the various scales also allowed to develop a fire resistance model for structural sandwich walls, which has the potential to replace testing altogether in certain cases. To further reduce testing a simple finite difference analytical heat transfer model is suggested to aid the prediction of the early stages of wall temperature reaction. With this model the implications of the experimental findings are further explored and the limitations of the use of analytical prediction for sandwich walls are presented. To successfully model the temperature reaction of sandwich wall units, the thermal material properties of the material layers in their various stages of degradation have been found to be of paramount importance, thus highlighting areas for further research.
6.2 Fire behaviour of structural sandwich walls

6.2.1 Effect of board

6.2.1.1 The influence of exposed board degradation on panel performance

This section of the discussion examines the influence of the outer exposed board layer on the fire behaviour of structural sandwich walls. The factors influencing the heat reaction of the exposed board are analysed in detail, drawing together the findings of the three scale fire testing regime presented in the previous chapter. For the analysis and comprehensive discussion of failure signature and correlated temperature reaction through the depth of the sandwich wall, the work in the bench-scale has been vital. The interpretation and determination of the degradation stages of the wall units in the controlled test environment of the Cone facilitated detailed observations and enabled controlling exposure time. Several board materials were investigated but two mineral based boards, the 8mm Pyrok and the 10mm Sasmox were covered in more detail. The wood based OSB has also been evaluated, though to a lesser extent. The effect of the board reaction on the full-scale sandwich wall performance is analysed in together with the influence of core, internal studding described in the following sections in Chapter 6.3 (Reduction in loadbearing capacity). Since the heat performance of mineral based board wall layers and sacrificial plasterboard cladding protecting the wall are in principle related, this section also discusses the factors influencing the effect of plasterboard on the wall performance. Additional factors affecting the shielding ability of plasterboard are also considered.

The facing veneer is the first panel component exposed to the increasing temperatures in a room fire, and its degradation characteristics and failure behaviour affect the rate and extent of heat build-up in the subsequent panel layers and components. The exposed board layer screens the remainder of the panel and its performance directly impacts on the stiffness, insulation and integrity performance of the entire wall unit. The ambient board layer is of less importance to the fire performance of structural sandwich walls, since this part of the panel is generally well protected by the internal insulating core. Only in the later stages of the fire is this board layer attacked and is generally preceded by the structural failure of the wall.

The exposed board layer undergoes three distinct reaction stages when subjected to elevated temperatures:

(i) warming up stage i.e. initiation of degradation,
(ii) damage to board layer, i.e. cracking in mineral based boards and ignition/ charring in wood based boards,
(iii) degraded board pieces fall off the panel until the exposed board layer is completely delaminated.

Whilst the temperature reactions of the board layer at the beginning of the exposure, (i) and (ii), are related to the material and thickness of the board layer, the breaking away of board pieces from the panel in stage (iii) is mostly influenced by the restraint provided to the exposed board by the panel and the test frame.
6.2.1.2 Warming up stage and damage to board layer (Stage I and onset of stage II)

The study of the temperature build-up behind the exposed veneer is most suited to analysing the break down mechanisms of the different board materials. The first stage of temperature response, i.e. stage I and onset of stage II as shown in figure 6-1, is linked to the warming up and the initial degradation of the exposed board layer. The warming up stage I is generally variable, as sketched in drawing 6-1, ranging from a gradual hyperbolic temperature rise to a sudden early increase.

![Diagram showing temperature response stages](image)

Drawing 6-1: Warming up stage I, measured behind the exposed board layer of the wall

However, in all wall combinations the onset of stage II occurred at a temperature level of about 100°C at the interface of the board and backing, sandwiched core. Whilst the warming up stage I cannot be linked to specific board related influencing factors, the time and mechanisms leading to the onset of stage II can be correlated to several board material characteristics. These are:

(i) material,
(ii) thickness,
(iii) surface finish,
(iv) density, as well as the 
(v) core material behind the veneer.

The influence of the exposed board on the fire resistance of sandwich wall units is substantial as the delay in temperature build-up behind the exposed board also postpones the loss in loadbearing ability of the wall unit. Figure 6-2 compares the typical temperature development through two identical specimens clad with two different board materials. Depending on the type of board cladding the sandwich wall the temperature rise within the panel, shown here for the first 20 minutes, is affected. In the wall clad with board B the temperature rise throughout the depth of the panel is premature and more severe.
A) Board material type
The parametric study established a clear distinction between the temperature reaction of the different board material types. Two kinds of boards were investigated in detail: two types of mineral based and one wood based board. The adapted bench-scale Cone set-up investigated the failure and temperature reaction of these different materials. The majority of test replicates were condensed to the two mineral based board materials, the cement-based Pyrok and the gypsum-based Sasmox, also compared in the intermediate scale test. In a separate test programme the wood based OSB was investigated. The wood based board showed principally different decomposition behaviour, which is discussed further down.

Amongst the two mineral based boards the cement bonded Pyrok board exhibited reliable temperature reaction behind the exposed veneer, and was observed to form localised deep cracks, whereas the gypsum bonded Sasmox board showed a greater variability in temperature reactions with a cracking pattern oriented in area rather than in depth. In the latter the board layers were seen to consecutively delaminate, as described in detail in Chapter 5. As any material, board materials will undergo chemical change when exposed to heat. In mineral based products, such as the board products investigated in the study but also concrete or plasterboard, the heat alters the chemical structure of the material by dissociating free and chemically combined water from its crystal lattice. The process, which absorbs the energy input from the heat, occurs in several temperature dependent stages and can be subdivided in two principal forms:

(i) desorption: the evaporation of free water from the pores of the material,
(ii) dehydration or calcination: the removal of chemically combined water.

Significant removal of free water from the material's pores starts at 100°C about simultaneously with the first dehydration reaction. In gypsum based materials CaSO₄ · 2 H₂O dehydrates to CaSO₄ · ½ H₂O + 1.5 H₂O. In cement paste, which consists of two main components CaO · SiO₂ · 1.5 H₂ and CaO · H₂O, the first component, commonly referred to as tobermorite gel, dehydrates at 100°C.

In both gypsum and cement paste a second dehydration process takes place at 300 to 400°C. Here the gypsum-based materials alter to CaSO₄ · ½ H₂O and in materials containing cement pastes the water from the CaO · H₂O component is removed from the material's chemical structure. In cement-based board materials a third dehydration process is observed at about 700 to 800°C, where the tobermorite gel is further condensed (Lie, 1972). These various dehydration stages cause the board layers to shrink, which induces cracking. The shrinkage cracks progress though the depth of the veneer as shown in drawing 6-2, causing temperature level in the shielding layer to rise as shown in figure 6-3.
Cement-based materials, undergoing three dehydration stages, shrink to an overall amount of 4% of their original length, whilst gypsum based materials only reduce by 2.5% of their original length (Lie, 1972). The effect of board material type on the temperature distribution within the board layer and at the interface between board and core are shown in figure 6-4.

As the board is heated a "dehydration front" moves through the layers of the material from the heated side, respectively shrinking the layer once the dehydration temperatures are reached. Axenenko and Thorpe (1996) introduced the term of "dehydration front" to describe the successive chemical changes in plasterboard at different temperature levels when heated from one side; it is similarly applicable in the context of this research. Constant energy input assumed, two or three dehydration fronts, depending on the material and temperature levels, move consecutively through the depth of the board layer, leaving behind dehydrated and cracked material. Once full-depths cracks have formed, a direct heat path through the veneer is created so that the temperature levels behind the exposed veneer rapidly rise, indicated by the onset of stage II in the temperature reaction behind the exposed veneer as sketched in generalised form in figure 6-5.

A further indication of the time to through-depth failure of the exposed veneer is the emission of combustion gases through the cracks. In the bench-scale test on PUR/ PIR cored samples through-depth cracking promoted the ignition of the combustible decomposition gases generated by the degrading of the core. Although the time to the ignition of the core's combustion gases is linked to the amount of cracking, it does not provide the precise time to formation of full depth cracks in the respective veneer material. In contrast to the temperature reaction, the ignition time is a function of the magnitude of cracking, allowing the leakage of a sufficient amount of combustible gases to enable ignition. A definite indication of the presence of full-depth cracks was the occurrence of antiseptic smell at the onset of stage II, at about 100°C behind the exposed layer.
veneer. This link was established for all material combinations with synthetic cores. With ongoing irradiation the cracks open up as more material (in the area of the exposed board) reaches the critical dehydration temperatures and increasing amount of combustible degradation products are emitted. By then the temperature reaction behind the exposed veneer has converted into the second stage. The delay in board failure postpones the heat build-up in subsequent panel layers, which has a positive impact on overall panel performance.

B) Board thickness
Since the time to through-depth cracking is the major influencing factor to the boards’ shielding ability the thickness of the board is vital. In table 6-1 the time to onset of stage II for four board materials, two of each material group, at two thicknesses are compared. Since not all boards have been tested in the intermediate-scale the bench-scale results for the 10mm gypsum Sasmox board have been supplemented to allow the direct comparison to the 12mm Fels board, only tested in the modified bench-scale set-up. The bench-scale results show to render shorter dehydration times than encountered in the intermediate-scale result so that direct material comparison, although approximated well by the bench-scale set-up, should be made in one scale for higher accuracy. From this comparison it can be found that 1mm board material provides up to 1 to 1.5 minutes protection to subsequent material layer when exposed to a heat flux of 50kW/m² or standard furnace conditions. Gypsum-based material render marginally better protection times with increasing thickness, which is thought to be due to the less violent shrinkage process.

C) Effect of surface finish
Normally structural mineral based building board, as used in loadbearing sandwich walls, are manufactured from a pulp of wood and gypsum or cement. Whilst the surfaces of most structural building boards are generally only polished to provide the smooth base for the decorative finishing, some boards, such as the Sasmox board, are finished with a layer of pure gypsum (of about 2mm thickness). This extra gypsum layer is likely to improve the heat resistance and shielding ability of the board material as it is not cross linked with the combustible wood fibres and therefore protects the deeper board layers more efficiently. The test evidence summarised in table 6-1 exhibits that the effect of surface finish in the boards tested for this study was only marginal and within the range of variation of the results. However, it is felt that increasing the depth of the finishing layer would positively impact on the shielding ability of the board. Further testing would be needed to confirm this effect and the modified bench-scale test would be well suited for such a testing programme.

D) Board density
In addition to board material, thickness and surface finishing, the density of the board material is likely to be influential to the shielding performance of the exposed board layer. The influence of increased board density on the shielding performance of sacrificial plasterboard layers has been noted by Jones (2001). This was not investigated in further detail in the programme as board densities were chosen representative of the boards used
in the building industry and applicable to sandwich construction. It can be safely assumed that an increase in board density would have improved the shielding ability of the board and thereby further delaying the temperature build-up in the panel.

E) Effect of backing material

Since sandwich panels are built from layers with substantially different thermal properties, the heating process is not strictly one-directional, which complicates the prediction of the dehydration process in the exposed board material. As the core is highly insulating, hence transmitting heat at a slower rate than the board material, the heat is congested at the interface of exposed face and foam, causing the board to be heated at increased rate. Furthermore the heat transferred into the core causes its degradation and the generation of combustion gases as discussed earlier. With the degradation of the core, which will be discussed in greater detail in the subsequent section, altered liquidised foam is formed beneath the board (see figure 6-6) which also affects on the heat build-up in the board, as the thermal conductivity of the boundary foam layer (initially $k \equiv 0.034\text{W/mK}$) changes. In order to assess the effect of the core material on the board decomposition behaviour table 6-1 compares the Cape board material (at one thickness) backed by three different core materials. When backed by a Phenolic core the onset of stage II temperature response behind the veneer is markedly delayed. However, since the temperature performance of the Phenolic core was very variable, its effect on the dehydration behaviour of the board could not be established unambiguously. This was especially evident in the Phenolic backed Pyrok board, which exhibited an atypical early onset of stage II of the temperature response behind the veneer. Since the through depth temperature reaction of this panel was similar to the remaining Phenolic cored specimen, this board effect was probably caused by some localised failure and distorting the general trend. It illustrates the wide range of temperature reactions encountered in the Phenolic cored panels and underlines the remarkably consistent response of the PIR backed boards.

In the enhanced bench-scale test a variety of core materials, ranging from PUR, PIR to mild steel, was combined with the Sasmox and Pyrok boards to explore the effect of core backing on the temperature behaviour of the exposed board layer (Table 6-1). The boards backed by the non-combustible steel plate showed a distinctly different temperature response, which was attributed to two main factors:

(i) the high thermal conductivity of the steel,

(ii) the non-combustibility of the steel.

The steel backing delayed the temperature rise in the board material, as its high thermal conductivity guaranteed the strictly one-directional heating of the board. This is a reversed condition to the synthetic, highly insulating PUR/PIR cores, where heat transfer is slowed down at the interface of core and board. More importantly the non-combustibility of the steel affects the principal shape of the temperature reaction, which increased linearly with time and not in three stages as observed with the synthetic cores. The steel backed samples did not ignite and the cracking in the board was reduced. The temperature response of the steel backed specimens was regarded as providing a lower bound of core performance, aligning with the Phenolic core performance in the
intermediate scale test. In these Phenolic cored panels, similarly to the steel cored samples, the onset of stage II behind the exposed veneer was delayed and the temperature build-up was reduced. However, as both the Phenolic and the PUR/PIR are synthetic, organic cores, the three-stage temperature response can be observed on both samples. The mineral wool core, although incombustible, affected the board decomposition similar to the PIR core. This is thought to be related to the fact that the mineral wool core is not self-adhesive and the faces need to be adhered using synthetic glues. In the mineral wool panel tested in this programme, a PUR based glue was employed as bonding agent. This glue line formed between core and exposed face was seen to be influential on the dehydration behaviour of the board. The response of the mineral wool backed board was more closely aligned with the board reactions in the synthetic cored panels rather than with the more fire resistant alternatives.

In addition to the core effects observed before, the chemical degradation of the organic cores generates toxic gases and smoke, which accumulate behind the veneer early after exposure. The extent of gas generation is dependent on the thermal stability of the core material. In case of gypsum-based boards, where shrinkage is reduced and consequently through depth cracking is delayed, the gases can cause the board to bow towards the heat source. This puts additional strain on the board material and is likely to accelerate through depth cracking. This build-up of pressure behind the veneer is seen as potentially hazardous especially in non-standard fire conditions where rapid temperature increase in the enclosure can promote wide spreading core degradation behind the exposed veneer before the entire board depth is decomposed. This panel failure signature has potential to shorten the shielding ability of the exposed veneer and thereby induce premature widespread damage.

6.2.1.3 Falling off board pieces (Stage II and stage III of temperature reaction)
The characteristic temperature response in stages II and III was seen to be variable. The rate of increase could be marked in some areas of the panel whereas in other areas the temperature rise was moderate, generally following from a delayed onset of stage II. Similar to the variations in onset and rate of temperature rise the overall temperature levels reached within the panels could vary and exhibit discrepancies of up to 400°C between two locations. The speed of temperature build-up and the overall temperature levels reached were dependent from secondary failure modes of the boards after dehydration had been completed. The main influencing factors to the post-shrinkage board response were determined to be

(i) the cracking depth and pattern
(ii) backing restraint to board

A) Influence of cracking depth and pattern
The cracking of the veneer together with the changes in physical properties of the dehydrated board layers impair the loadbearing and shielding capacity of the exposed veneer. Dehydrated board can acts as a shield against direct flames and radiation, as found by Alfawakhiri and Sultan (1999) and confirmed through this testing work but
therefore it is essential for the veneer to remain cladding the panel throughout the exposure time. The effects of cracking and other secondary failure modes on the temperature reaction of the panels in stage II and III are illustrated in principle in figure 6-6. The magnitude and location of board cracking affects on the temperature build-up within the panel. In case of minor board damage the onset of stage II can be delayed. Generally the extent of cracking within the exposed board area ranges from minor, small dehydration cracks to several millimetre wide cracks. The latter are often accompanied by progressive core damage. There is an intermediate form of damage where the crack is through the depth of the board but not opened to same extent so that the subsequent core layers are not involved to the same extent. All cracks appear in random order although generally the upper, central part of the panel is most affected. With increasing height the effect of the heat is more pronounced due to the pressure conditions and the hot gases rising up during the test (see also drawing 6-3).

In areas where large shrinkage cracks occur the temperature response can be more severe, which is shown by an increased temperature rise in stage II of the response. This is not always representative of the overall temperature performance of a specimen. However, the low, delayed temperature readings at minor cracks are more likely to be misrepresenting the panel's temperature performance. The extent of damage to the panel affects on the overall temperature level reached at the end of the test period. It is governed by the amount of dehydrated, cracked board pieces falling off the unit, which is dependent on the restraints provided to the exposed board.

B) Influence of backing restraint

In the vertically oriented larger scale tests, i.e. the intermediate and full-scale testing, the extent of board cracking and the simultaneous degradation of the glue line, causes pieces of dehydrated board to fall away from the wall unit. This exposes large areas of the panel interior to the direct heat from the furnace or fire. It was found that the overall temperature levels in the panel reached in stage III were lower close to the perimeter of the panel or along internal stud units. Here the exposed board was still well attached to the remainder of the panel and additionally restrained by its fixings and embedment in the test rig. Although the board was decomposed it remained shielding the subsequent panel layers as sketched in figure 6-6.

The decomposition signature of the core and the exposed glue line not only affects on the temperature response behind the exposed veneer, but also impacts on the amount of board being removed from the panel in the post -dehydration stage. This is especially important in the centre area of the panel away from stud or internal jointing units and enhanced edge restraint. The preservation of the physical connection between board and core is important and was seen to be considerably different depending on the core material. The Phenolic core, known for its enhanced fire performance, was observed to form a rigid char to which board pieces were still attached at the end of the test. The inert mineral wool core did not show any benefit in that respect as the PUR glue used decomposed
comparable to a fully PUR cored panel. Board pieces were delaminating from the panel as observed in the remaining combustible cores tested.

In addition to the restraint provided by the core material itself any internal units and the boundary restraint provided to the board by the surrounding test frame enhance the shielding capacity of the board. In these areas the board remains attached although the core behind the board might have been consumed. Particularly along internal units the post-shrinkage strength of the decomposed board is influential to the effectiveness of the fixing.

The fact that the board remains cladding the core in the bench-scale test for the entire test period, due to the horizontal orientation of the sample, allows to approximate the time at which the exposed board layer becomes ineffective in shielding the remainder of the panel. Figure 6-7 illustrates that the bench-scale test can predict the onset of stage II in the intermediate scale test quite accurately. However the temperature rise in the bench-scale sample levels off at about 400 to 500°C, whereas the intermediate scale temperature response rises to above 600°C in some locations even adjusting to the furnace temperature level. These high temperature levels are caused by falling off board pieces and the effect was modelled in the bench-scale test by removing part of the exposed board material at a set time in the test. The ultimate temperatures reached in this manually destroyed bench-scale sample correlate well with the temperature levels observed in the intermediate scale temperature readings. The intermediate-scale does not encounter a sudden rise in temperature, which suggests that the removal of the exposed veneer is a gradual and constant process rather than a sudden, localised failure induced in one part of the wall. This could be confirmed through the observations recorded during the intermediate-scale tests (shortly before the view was hindered by smoke). These stages of destruction encountered in both the broken bench-scale and the intermediate-scale samples were seen to vary depending on the extent of damage and the consumption of core material. The damage caused to the panel is not uniform in these final stages of the test and can vary to great degree between central parts of the panel and close to the edge perimeter. In larger scale tests, intermediate and full-scale, the variation of board temperature over the area of the exposed face is more marked. In a full-scale wall the pattern of secondary board failure is sketched in drawing 6-3. The restraint provided by the installation of the wall into the test frame at the perimeter of the sample reduces the secondary damage to the exposed wall layer. Since pressure conditions are more severe at the top of the wall, panel damage is found to be more extensive in the upper areas.
6.2.1.4 Effect of board on the stiffness response of structural sandwich walls

The typical effects, which lead to the sharp temperature rise behind the veneer, can be correlated to the stiffness reaction of the panels. Figures 6-8, 6-9 and 6-10 compare the temperature reaction behind various exposed veneer materials, with the deflection course recorded in the samples in the intermediate- and full-scale test. The responses were of similar form and showed close to identical onset of increased deflection and temperature rise. Thereby the deflection measured in the centre of the panel is influenced by the dehydration behaviour of the exposed board material. The onset of stage II of the temperature curve behind the exposed veneer was determined to be the time to generation of through depth cracks in the veneer, so that the occurrence of these calcinations cracks were seen to drastically reduce the stiffness of the sandwich unit in a fire environment. The full-depth cracks in the exposed board impair the composite action between the stressed board skins and the core. The through depth failure of the board marks a significant stage in panel degradation, which is further examined in section 6.3 “Reduction in loadbearing ability”

6.2.1.5 Other board materials

The bench-scale test also investigated the wood based oriented strand board, though to reduced extent. The decomposition behaviour of wood based board products was observed to be very different from the mineral based products and characterised by surface flaming, charring and tearing, see table 6-1. The board was observed to shrink and fissure, creating large direct heat paths to the core within minutes. The temperature response measured behind the 11mm OSB layer is compared to the remaining boards in table 6-1. Due to the charring of the upper layers in the wood board, seconds after exposure the decomposition of deeper layers within the board is delayed. Temperature rise is therefore slightly less severe than observed in the cement-based board in the first
minute of the response. However, with ongoing exposure the low-density wood board is rapidly consumed and the heat dries the board surface, which creates up to 10mm wide fissures in the area of the board. With these large openings in the exposed the temperature behind the veneer increases to greater extent than observed in the mineral based materials.

6.2.1.6 Sacrificial plasterboard
Although not contributing to the structural ability of the sandwich wall the sacrificial plasterboard layers delay the heat build-up in the panel, similar to the function of the exposed veneer when no additional layers are present. This commonality together with the fact that plasterboard is a gypsum-based board links the above discussion of the exposed veneer failure signature with the behaviour of additional cladding layers to sandwich wall performance.

The protection provided by the plasterboard lining increases with its thickness; the thicker the sacrificial lining the longer the delay in temperature build-up. Although the plasterboard layers are non-structural the delay in temperature degradation provided through this sacrificial lining postpones the stiffness loss of the sandwich wall unit. Plasterboard lining is commonly used in timber frame and other lightweight building systems to give the systems adequate fire resistance times. The ability of the material to absorb the heat from a fire is due to the fact that the energy provided through the heating drives the dehydration process, as discussed before.

A) Effect of plasterboard
In the full-scale test the occurrence of high temperature levels within the sandwich wall assembly could be unambiguously linked to the areas of early plasterboard loss. The test showed the plasterboard joints to be especially vulnerable to early failure as the thin layer of plaster skim applied at the joints dried out quickly and crumbled from the wall.

Central parts of the wall assembly were most severely affected by the plasterboard failure whereas the edge areas of the wall remained well covered, see figure 6-10. Whilst the central part of the sandwich wall assembly was observed to heat up to furnace temperature, the edge areas of the sandwich wall maintained near to ambient temperature levels throughout the test. These location dependent temperature effects were observed throughout the programme and could be linked to the amount of restraint provided to the exposed board along its height. Although both vertically oriented furnace test regimes, the intermediate- and full-scale test exhibited the location dependent temperature effect, the onset of the temperature reaction and the overall temperature levels reached within the units were not identical. This is due to the fact that the plasterboard layer in intermediate-scale test receives the proportionally higher restraint due to the reduced panel height. This effect is further aggravated in the bench-scale regime, due the horizontal orientation of the specimen. This is further discussed in Section 6.4.
The bench-scale regime accounted for the loss of plasterboard by manually removing the sacrificial layer from the sample surface. Despite the fact that the additional layer of material protected the unit the temperature levels within the unit increased although at very much reduced rate. This slow preheating mechanism affected the temperature response of the unit once the plasterboard was removed from the surface. The temperature reaction times were reduced, suggesting that the plasterboard layers transferred heat in sufficient amount to shorten attainment of the decomposition threshold temperatures, see drawing 6-4.

![Temperature reaction of panel without plasterboard](image)

**Drawing 6-4:** Sandwich wall reaction behind protective layer of plasterboard

Generally the protection ability of plasterboard seems to be subdivided in two stages. During dehydration, at the start of the exposure, protection is best, later when the shrinkage cracks have established and gradually open up protections is reduced but still partly effective if restraint and fixity is adequate. The fire performance of poorly fixed plasterboard is inferior and marked by the premature failing off of large board pieces from the loadbearing wall unit. This has a substantial effect on the loadbearing ability and fire resistance of sandwich wall units and poor, substandard fixing must be avoided.

**B) Stiffness loss**

In the protected intermediate-scale sandwich wall samples the stiffness loss of the panels was delayed, similar to the observed temperature effects. The plasterboard loss reduces the stiffness in the central part of the assembly where the deflection of the wall increases; but in most of the full-scale set-ups the load is bridged to the outer, well-protected and less decomposed panel areas. In these edge areas stiffness loss is minimised and therefore failure is not imminent. Although the intermediate-scale test loading arrangements allows to record the stiffness loss in the vital central part of the assembly the extrapolation to the
larger scale test performance needs to additionally account for the boundary conditions of the board. This will be discussed in further detail in subsection 6.4: Link of scales.

C) Correlation of protection times
Due to the effects discussed before the protection provided to the panel by a plasterboard thickness in the intermediate scale is not reproducible in larger scale. For a 15mm plasterboard layer the protection time, hence the onset of temperature increase behind the exposed panel veneer or subsequent panel layers, is reduced by 40% (from 55 minutes in the intermediate scale to 35 minutes in a full-scale sample). Whilst the test programme exclusively used the standard wallboard plasterboard, the use of glass fibre reinforced plasterboard is very common in the industry and is proven to provide improved protection times. Glass fibres reduce the magnitude of shrinkage cracks and allow the board to remain stronger and more coherent after dehydration is completed. The fire behaviour of plasterboard has been investigated by various researchers. Research about the thermal properties of both plasterboard types is as numerous and reported in Jones (2001). However none of the models account for the falling-off phenomenon. The fact that plasterboard is also a mineral based material and undergoes the same dehydration and shrinkage processes together with the fact that the backing and the restraint of the board affects on the overall fire resistance of the sandwich unit protected by it aligns its effects with the other mineral based boards investigated as facing materials.

6.2.1.7 Summary
The exposed board and its decomposition characteristics affect the temperature build-up in the panel. Mineral based boards undergo chemical changes when exposed to heat, which causes them to shrink and crack. These shrinkage cracks together with changes in thermal material properties reduce the board material's shielding ability and accelerate the destruction of the panel. These processes are similar for both the mineral based veneers of the sandwich wall unit and the sacrificial plasterboard layer, cladding the unit to enhance its fire resistance. In wood based boards the decomposition characteristics are distinctly different and are marked by flaming and charring, creating deep fissures through the depth of the board. Although the charred board is a potentially better insulant to the incoming heat flux the deep fissures undermine this effect and aggravate the decomposition of the core. The insulation and decomposition of the backing core also influences to the board's decomposition time, since a highly insulating core congests heat at its interface with the board, which can accelerate the degradation of the board. Additionally decomposition gases are formed, which pressurize the back of the exposed board and accelerates its cracking. Full-depth cracking in the board impairs the loadbearing ability of the stressed skin wall, which results in sudden loss in stiffness, shown by the onset of stage II in the deflection/time plot. Once through depth cracks have formed in the board layer, the temperature development behind the exposed veneer is mainly influenced by the restraint provided to exposed board. In areas where the board is well restrained, such as close to internal units (studs or joints) or close to the edge perimeter of the wall unit where the wall is embedded into the test frame, the board will
remain shielding the panel and the temperature build-up in the subsequent panel layers is reduced.

The failure mechanisms of the sacrificial plasterboard layer are closely related to the decomposition characteristics of the mineral based exposed panel veneers. The dehydration shrinkage cracks cause the board to decompose and fall/ delaminate from the panel, which affects the temperature build-up in the subsequent layers of construction, hence the sandwich wall. The restraint provided through the fixing of the plasterboard is vital to the protection and the better the restraint can be ensured in the post-shrinkage stage the better the shielding capacity of the plasterboard layer.
Figure 6-1: Three stage temperature response behind exposed veneer

Figure 6-2: Temperature development through panel depending on exposed board material
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Figure 6-3: Temperature distribution in mineral based exposed board layer at different stages of dehydration.
Figure 6-4: Comparison of shielding ability between two types of mineral based boards investigated in the study.
Figure 6-5: Shielding ability of mineral-based exposed board layer depending on degradation stage
Table 6-1: Effect of board Temperature

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<th>Specimen</th>
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<th>Core</th>
<th>Scale</th>
<th>Replicates</th>
<th>Failure pattern</th>
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Figure 6-6: Second stage of temperature response behind exposed veneer
Figure 6-7: Impact of secondary failures in exposed board material

Figure 6-8: Correlation of stiffness reduction and exposed board failure for Sasmox-PIR panel in intermediate-scale test

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Figure 6-9: Correlation of stiffness reduction and exposed board failure for Pyrok-PIR panel in intermediate-scale test

Figure 6-10: Correlation of stiffness reduction and board failure in full-scale test
6.2.2 Effect of core material

6.2.2.1 Contribution of internal core material to sandwich wall performance

In the following section the influence of the core material on the temperature response of the composite sandwich wall structures is discussed. The test evidence presented in Chapter 5 has evaluated that the most important function of the internal core layer is to delay and dampen the heat build-up through the depth of the wall once the shielding capacity of the board material is damaged. The effectiveness of the core in reducing the heat temperature rise within the wall is related to the degradation reaction of the core material. In the programme a range of core substrates were evaluated, although two core types, namely the synthetic PUR and PIR, were assessed in greater detail. The findings show that the heat build-up and stiffness loss are reduced in panel filled with high density synthetic or inert foams. The governing factor for enhanced core performance in synthetic foam products is their ability to form high density, coherent char. Whilst the lighter foam materials, which are representative of the foams used in commercial wall products, also generate char upon heating, the char is lightweight and not as heat resistant. The effect of the internal core on the temperature build-up in the wall, mirrors its influence on the stiffness performance on the panel; the stiffness reduction in the panels filled with heat resistant (Phenolic) and incombustible (Mineral wool) cores is reduced. Although the high density, synthetic Phenolic foam was seen to provide enhanced fire features and was in addition suitable for continuous manufacture, in contrast to the Mineral wool core, its use is currently not economical in commercial wall products due to its high costs and difficult handling. Although the study included the range of foams to advise on core design features suitable for use in structural sandwich walls, the emphasis of the work has been on the foam products currently most used within the industry.

For the core assessment the testing in intermediate- and bench-scale was especially valuable since it enabled the determination of the different stages of core destruction and thereby examined the influence of the cores’ decomposition characteristics on the overall panel performance. The programme assessed the foam performance of PUR and PIR cores in different configurations, i.e. when clad by board and when tested without cladding layer. This enables to comment on the specific core degradation situation in sandwich walls, where the internal core substrate is encased behind the high density facings. Furthermore the study can advise on whether the testing on single materials can be used to optimise design of sandwich walls. The findings suggest that the combination of test information obtained from small-scale testing on single panel components is not representative in predicting the fire performance of the materials when used in a composite section. Therefore the collected test evidence from individual material testing has only limited use in helping the material choice for the most suited panel composition.

6.2.2.2 Core performance in sandwich walls

A) Sandwich wall ambient temperatures

At standard temperatures sandwich walls have superior insulation characteristics, reducing the consumption of heating/cooling energy to a minimum even in the severest conditions. The high insulation performance of the sandwich wall structures is provided
by the internal core whose closed cell structure minimises the permeability to air. The closed cells within the foam hinder the transportation of heat and thereby reduce the loss/gain of heat through convective heat transfer. Although with time cell walls can degrade and allow the transportation of heat, these effects are localised and generally do not impact on the overall insulation performance of the wall layer.

B) Sandwich wall exposed to fire conditions

When sandwich walls are exposed to fire conditions, the internal core material is exposed and the closed cell structure degrades at increased rate. In a sandwich wall build-up the core is exposed to the fire environment in three characteristic configurations:

(i) behind the exposed board layer, prior to board damage,
(ii) behind the exposed board, after board degradation has been completed,
(iii) exposed directly to the heat environment when large board pieces are delaminating from the panel.

The temperature reactions of the core materials used in this study range from charring and sintering to melting. Whilst the synthetic thermosetting PUR/PIR and Phenolic foams have the propensity to char, the inert mineral wool is known to exhibit localised sintering effects. In contrast to the above foams the EPS core substrate is renown for its rapid and severe thermal decomposition, forming flaming droplets and causing widespread damage.

Since the degradation reaction of PUR and PIR foams have been the focus of the investigation, their standard, established temperature reactions are discussed in further detail. Although PUR/PIR and Phenolic foams differ in their chemical structure (see Chapter 4), their temperature reactions are to some extent related since all three foams start to break down at about 250°C and their closed cell, balloon-like, structure promotes the tendency to char. The amount and kind of char generated by each type of foam differs; the PUR foam generally forms less char than the PIR and the Phenolic. Abundant air supply can enhance the char formation in polyurethane based foams (i.e. PUR and PIR), which can slow down their degradation. Phenolic foams have superior fire resistance as they generate 50 to 60% rigid char upon heating, which minimises the weight loss between virgin and decomposed Phenolic foam to only 10%.

In a combustible foam material the ability to char is a governing factor to its fire resistance. The generation of char is generally a surface phenomenon and restricted to the first layers of material and in combination with the low air permeability of the closed cell foams, the air access to deeper materials layers is hindered. In addition char acts as an intumescent layer, effectively slowing down the combustion of deeper layers. The degradation of deeper core layers can only progress if chemical and mechanical breakdown of the closed cell structure permits the air access to the material. This special degradation condition promotes a special type of combustion, termed smouldering combustion. The propagation of smouldering combustion in rigid synthetic polyurethane foams has been investigated amongst others by Ohlemiller and Rogers (1978), Torero and Fernandez-Pello (1995) and Anderson et.al. (2000) see also Chapter 3. Although all
synthetic polymeric foams tend to char, the extent of their char forming ability was found to be depending to varying degrees on

(i) the chemical composition of the foam,
(ii) the combustion environment of the foam (inert or oxidative),
(iii) the heating regime (Ohlemiller and Rogers, 1978).

In some combustion configurations, Ohlemiller and Rogers (1978) found that the degradation mode of polyurethane based foams could alter; then the foam produced a tar-like substance upon heating instead of char. Although tar versus char formation are competitive degradation reactions, the generation of char is the dominant degradation reaction in most fire environments. Therefore polyurethane based foams generally exhibit the slow smouldering combustion mode upon heating.

(i) Heating of core behind higher density board layer
At the start of the heat exposure the core decomposition dynamics in a sandwich unit are linked to the degradation behaviour of the shielding board material. The core undergoes the first stages of decomposition prior to the through depth failure of the board. Whilst the exposed board shields the interior of the wall for the first minutes of the exposure, the temperature transferred through the board layer starts to heat up the core. This is promoted by the low conductivity of the foams used in sandwich walls, congesting the heat flow at the interface of board and core. The thermal properties of the board are also of influence and wood based boards are thought to reduce the heat infiltration in the initial stages of exposure due to their char formation. The effect of the heat on the core material was examined in detail for the PUR/PIR foams in the modified bench-scale set-up. The similarity of temperature responses in the PUR and PIR cored panels in the intermediate scale suggests that both core materials have close commonality in their reaction to the heat environment, when tested in the full-scale vertical wall configuration. Phenolic cores were seen to have a wider range of temperature reactions and their degradation behaviour in these initial stages of the exposure could not be verified in detail since the core was only tested in intermediate-scale. This was also the case for the mineral wool core. The test arrangement in the furnace based tests does not enable detailed analysis of the early degradation reactions and detailed back-up testing programme would be beneficial to investigate these effects further. The modified Cone set-up is thought to be well suited for such a test programme.

In the initial stages of fire exposure the heat transferred through the exposed board layer causes the PUR/PIR foam structure in the upper core layers to change. The polyurethane based foams, investigated in detail, did not generate the generally encountered oxidizable char but decomposed upon the dampened heat exposure with the less likely tar degradation reaction, shown in drawing 6-5. Although the PUR/PIR cores had produced the commonly encountered char when tested without the shielding board layer (as seen in drawing 6-5), the suppressed heating the foams induced the formation of a glutinous tar like material of distinct smell. In the directly affected zone the glue line and adjacent core layers loose their original rigidity, as seen in figure 6-11. The gradual degradation of the
upper core layers generates combustion gases, which accumulate behind the intact board layer. The gas pressure building up behind the layer can be so high so as to bow the exposed board towards the heat source. Despite the fact decomposition has started within the panel, temperatures are low at this stage of degradation.

Although detailed observation of the temperature reaction of the Phenolic core behind the exposed board layer could not be undertaken, the temperature reactions seem to indicate that the slow, smouldering combustion mode was predominant in these types of cores. The core degradation observed in the intermediate-scale was marked by extensive char formation and together with the higher breakdown temperature of the Phenolic glue line, the more heat resistant core is thought to prevent the tar formation observed in the PUR/PIR foams. In addition the Phenolic core retained large board pieces, which remained adhered to the core throughout the test. This observation underlines the assumption that the Phenolic did not alter to the liquidised, tarry substance at the interface of exposed board and core, which would have removed the board restraint. Further testing is needed to establish the reactions in this core configuration in more detail.

In contrast to the above foam materials, mineral wool is an inorganic material, which does not decompose, hence ignite or burn. Upon heat exposure it does not alter and only exhibits local sintering effects in areas of high temperatures (Klingsch and Wittbecker, 2001). In the initial stages of the shielded fire exposure high temperatures are unlikely as the board is still intact. The fact that the Mineral wool is not self-adhesive necessitates
that board faces and core are connected by glue to form the sandwich compound. The glue used in combination with inert mineral wool cores is commonly PUR based and exhibits similar breakdown temperatures as the PUR/PIR core products discussed before. Consequently increasing temperature levels behind the intact board cause similar damage to the glue line as observed in the PUR/PIR glued panels. From the findings in the programme it appears that in case of the mineral cored panel the reactions of the PUR/PIR and Phenolic cores are combined. Whilst the glue line at the interface of exposed board and core degrades early, the temperature reaction of the core layers in increasing panel depth is less severe, which is beneficial in the later stages of the exposure.

The EPS core substrate is a thermoplastic material, which does not form char. The material melts and volatilises at relatively low temperatures and its rapid chemical decomposition results in low thermal stability. In the initial stages of exposure the board protection curtails the oxygen supply, so that the melting process is slowed down. However, since the threshold temperature of EPS degradation is low, ongoing heating, even through an intact board layer can cause the extensive melting of the core (Figure 6-12). The melt can ignite and form flaming droplets, enough oxygen provided. The onset of decomposition and the progression of damage is very rapid and the core recedes from the heat source in seconds and the foam cannot be ignited. Overall the process is so rapid that detailed observations are impossible.

The degradation reaction of the upper core layers when heated behind the exposed board layer, affects on the boards’ dehydration dynamics as discussed in the previous section 6.2.1 and shown in table 6-2. The marginally different combustion performance of the PIR observed in the modified Cone set-up is thought to have an impact on the dehydration of the board (see also table 6-2), however the difference in behaviour is to small to show up in the larger scale test.

(ii) Core partly exposed through failure in board (Drawing 6-5 and Figure 6-11)

With ongoing heating the board materials further degrade, developing fissures and cracks, creating direct heat paths to the interior core, shown in drawing 6-5 and figure 6-11. At this stage of board degradation the temperature exposure of the panel interior increases and the fire resistance of the core becomes increasingly influential to overall panel performance. Once the core is directly exposed, the decomposition converts from the restricted, suppressed degradation mode to an accelerated and sometimes flaming combustion. The behaviour of the foam after the through depth board failure is dependent from the extent of “pre-decomposition” and the degree of board failure, hence the size of cracks. Through depth cracks in mineral based boards are generally smaller than the larger fissures encountered in charred wood based boards.

At the through depth failure of the mineral based board layer the PUR/ PIR cores had degraded to the stage where the decomposition gases of the core escape through the cracks in the shielding layer and ignited the panel, shown in figure 6-11. The liquidised
The fire performance of the core material in this stage is related to the threshold temperatures at which the core degradation converts to burning. The comparison of the core reaction with and without board (Figure 6-11) shows that the intact veneer dampens the temperature build-up in the first stages of the exposure but once the shielding layer is impaired, rapid degradation of the foam follows. In the case of the Phenolic foam the degradation threshold temperatures are increased and together with the extensive charring the temperature reaction is much less severe and delayed. The enhanced fire resistance of the core does not promote the conversion to flaming combustions and remains in the slow smouldering combustion mode throughout the test.

The temperature response of each core material once the board layer is impaired can be very different. And the time gap between sharp temperature rise behind the veneer and the onset of sharp temperature rise in the centre of the core is an indicator of the fire resistance of the core as shown in figure 6-12. Table 6-2 lists the performance levels of the varying foams and figure 6-12 compares the temperature build-up through the depth of the internal core for three core substrate: Phenolic, PIR and EPS. The speed of temperature reaction through the PIR cored panels was much increased when compared to the Phenolic core, where the temperature build-up was much delayed. Worst performing core out of all three is the EPS, where the temperature rises within minutes throughout the depth of the wall. The reaction times of the mineral wool core, listed in table 6-2, shows the core to be performing similar to the Phenolic core. Drawing 6-6 sketches in general form the performance ranges of core substrates possible in sandwich walls. A core beneficial to the fire performance of a structural sandwich walls delays the dehydration of the board and dampens the heat build-up through the depth of wall layer. A core with inferior fire qualities accelerates the degradation of the exposed board layer and is consumed by the fire rapidly, resulting in high temperature levels throughout the wall in short amount of time.
(iii) Core exposed directly as board pieces fall off panel

The performance of the core materials after the board has failed is also linked to the amount of board falling from the panel. Ignition of the foam layer and substantial softening in the core substrate does promote the removal of large board areas since the restraint to board is reduced, especially if the panel is tested in vertical position. The loss of board makes the temperature build-up more severe, see figure 6-11, as this sudden exposure to the direct heat of the furnace in combination with abundant air supply, attacks the internal core material. This form of core decomposition is the most severe and rapid.

In the PUR/PIR panels the removal of large board pieces converted the speed of decomposition from reduced rate oxygen deprived burning mode to rapid heat intensive flaming combustion, causing a steep rate of temperature increase in stage II of the PUR/PIR core temperature reactions (Table 6-2). This heavy burning mode could be linked unambiguously to the removal of board, which caused the severe flaming and smoking in the bench-scale scenario, as seen in figures 6-13 and 6-14. When the board, although cracked and calcinated, remains covering the recessing core the rate of temperature reaction is dampened. This acceleration of panel destruction is best appreciated in figure 6-12 where no PIR foam is left in the panel after 40 minutes exposure.

The Phenolic cores were designed for enhanced fire resistance and due to their high charring ability exhibit smaller weight loss than the polyurethane foams. The increased content of thermally stable aromatic chemical building blocks favours the slow smouldering combustion of the foam. Consequently the severity of temperature increase is dampened and as smouldering tends to be much less intense than the rapid flaming combustion observed the PUR/PIR foams the temperature reaction in the panel centre.
was both delayed and reduced. In contrast to the PUR/PIR foams, the Phenolic foamed panels remain observable for the entire test period, as only small amount of smoke is emitted and the foam is left charred at much of its original depth. After 110 minutes exposure a distinct layer of virgin core material adhered to the unexposed face. Due to the effective delay provided by the decomposition characteristic of the material an increase in core thickness is an effective means of slowing down the overall temperature build-up in the panel.

Since mineral wool is inert the failure and falling of the board does not initiate flaming combustion. The increase temperature levels can cause the sintering of the core, which leaves the mineral fibres brittle and with no strength. Since the combustion mode is less severe the temperature reaction is moderate and followed by gradual increase in temperature build-up in subsequent panel layers. Similarly to the Phenolic foam the mineral wool core substrate can profit from enhanced core thickness.

The EPS core substrate is likely to have decomposed entirely as soon as the board layer starts to crack. These severe decomposition characteristics of polystyrene based foams, such as EPS, have been in the centre of attention in recent years. In EPS cored panels the decomposition is generally accompanied by large heat release much in contrary to sandwich systems built with synthetic foams. An increase in core thickness is of no effect since the combustion is severe and sudden through the depth of the layer so that increased thickness would not provide any considerable delay in temperature reaction.

The effect of board falling off the full-scale wall and the related core decomposition is sketched in figure 6-15. Whilst the fall out of smaller board pieces only exposes a restricted area of foam the damage cone is reduced and adjacent, undamaged panel parts are protected from the heat exposure. In panels filled with fire weak cores, such as the EPS these effects are likely to already cause widespread damage. The larger the board damage the larger the affected core area and the wider spread of core damage and increased temperature levels. The provision of adequate fire stops is of paramount importance in order to dampen the involvement of less attacked areas.

6.2.2.3 Effect of core on panel stiffness and panel internals

The core decomposition characteristics also affect on the reduction in stiffness of the units. It is highly influential on the heat build-up within the panel but also to the temperature exposure of any internal units. The inertia of a partly damaged sandwich section, hence the resistance to imposed load, is governed by the amount and rigidity of char and virgin foam retained on the ambient veneer. The better the thermal stability of a core material, the larger the amount of foam retained attached to the ambient board and therefore the stiffer the remaining panel after the loss of the fire face veneer. The greater the amount of char retained in the panel the lower the temperature exposure to internal units, such as joints or studding. A temperature differential can be developed within the section, which are heated on their exposed face but protected from high temperatures due
to enclosure in the partly charred, partly virgin foam. This will be discussed in further
detail in subsequent chapters.

6.2.2.4 Secondary failure effects
All core materials no matter how heat resistant shrink or sinter and thereby reduce in size.
Especially materials charring to great extent and marked by moderate temperature
development are likely to produce fissures and shrinkage gaps. These gaps are created
within the core and with ongoing irradiation and involvement of deeper core layers the
gaps open up and steadily progress through the core depth. Temperature readings can be
affected by this localised form of failure especially if they are located in an air gap rather
than in a coherent piece of char. This can cause atypically high readings as the insulation
capacity of the core is breached (observed in the Pyrok-Phenolic intermediate-scale
panel) and such results need to be carefully analysed and ideally compared to back-up
readings at identical depth remote from the failure area. Such a result can however not be
seen as representative for the overall performance and temperature reaction through the
depth of the panel needs to be investigated to establish the occurrence of local effects.

6.2.2.5 Toxic gas emissions and other safety concerns associated with burning foams
When exposed to heat the bond between the different material groups forming the foam
during polymerisation breaks down, a process termed scission, and parts of the materials
vaporize and are re-condensed as smoke. The higher the exposure temperature the larger
the generation of toxic gases, such as hydrogen cyanide, carbon monoxides to name but
the predominant ones. Foams with higher fire resistance are generally regarded as
yielding higher amounts of carbon monoxide. Work by FPL (Eickner, Heebink et al.,
1973) has shown that the exposed sandwich panel board layer reduces the toxic emissions
of sandwich panels. To confirm these results for the various degradation stages of the
panel and core identified through this research, the standard Cone calorimeter test in
conjunction with the modified sample size should be undertaken. Furthermore the effect
of core decomposition behind the exposed layers and spreading to less involved panel
areas can be of concern. Especially thermoplastic type of foams, such as EPS, adequate
fire stops are of paramount importance. The rapid, hidden fire spread in EPS cores behind
the panels' steel faces and the subsequent extensive delamination of the faces was
regarded responsible for wide spread damage and the death of fire fighters in fire
incidents involving cladding sandwich panels.

6.2.2.6 Summary
In summary the contribution of the core to the performance of the sandwich wall is
dependent on
(i) the dehydration behaviour of the cladding board
(ii) the decomposition behaviour of the core
(iii) the charring ability of the core, since the formation of low-density-high
porosity carbonaceous char will act as an intumescent layer, slowing down
the thermal decomposition of deeper layers.
The foams have been shown to produce different levels of protection depending on their chemical composition and decomposition behaviour. This was investigated in greater detail for PUR/PIR foams and from these findings characteristic Phenolic and mineral wool reactions were discussed. The core degradation also affects the stiffness response of the unit since the thickness of core layer and the char formed upon decomposition affects the overall stiffness of the unit, stiffening the ambient board. Internal units are similarly protected the more fire resistant the core.

The core heat reactions also affect the degradation speed of the board. Furthermore the rapidity of core consumption influences the amount of falling off board pieces in second stage of board degradation (hence after full depth failure of board layer). In this second stage of panel degradation less fire resistant core will spread the fire damage to less involved areas and fire stops become increasingly important. With PUR/ PIR and Phenolics this can be well managed since these cores char upon direct heat exposure. In cores such as EPS or XPS the containment of damage can become more difficult, since the reactivity of the foam at low threshold temperatures effects rapid and severe spread of damage beyond the zones of direct fire attack. All synthetic foams can be enhanced and a considerable amount of literature covers the optimisation of foams with respect to thermal resistance, flammability, smoke production and flame suppression, some of which have been discussed in Chapter 3. The issues of toxic gas production and smoke development are of major importance in this context as the char formation in foams, which enhances smouldering combustion, increases toxic gas emission and thereby gives rise to incomplete combustion products. With respect to fire involvement the inorganic Mineral wool core performs best and is in many respect safer than the synthetic cores discussed before. However, the disadvantages in manufacture and the lack of self-adhesion have to be carefully considered.
### Table 6-2: Effect of core

<table>
<thead>
<tr>
<th>Core materials</th>
<th>Insulation (min)</th>
<th>Rate of increase (°C/min)</th>
<th>Influence of core on board (min)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Bench</td>
<td>Intermediate</td>
<td>Board</td>
</tr>
<tr>
<td>EPS</td>
<td>2 (15)&lt;sup&gt;1)&lt;/sup&gt;</td>
<td>-</td>
<td>47 (3)</td>
</tr>
<tr>
<td>PUR</td>
<td>8 (30)</td>
<td>8(3)</td>
<td>30(6)</td>
</tr>
<tr>
<td>PIR</td>
<td>~10 (15)&lt;sup&gt;2)&lt;/sup&gt;</td>
<td>8(3)</td>
<td>2&lt;sup&gt;1)&lt;/sup&gt;, 60 (2)</td>
</tr>
<tr>
<td>Phenolic</td>
<td>-</td>
<td>20(3)</td>
<td>-</td>
</tr>
<tr>
<td>Mineral wool</td>
<td>-</td>
<td>16(1)</td>
<td>-</td>
</tr>
<tr>
<td>Steel</td>
<td>-</td>
<td>13 (1)</td>
<td>50 (3)</td>
</tr>
</tbody>
</table>

1) N= Number of replicate readings  
2) Onset in position B and C not marked
Chapter 6: Fire resistance of structural sandwich walls: Effect of core

Temperature

Severe flaming combustion, enhances charring

Restricted, slow burning to measuring location

Tar

Foam tested without board layer

Char

Changed foam

Glutinos, viscous tar instead of char

Core degradation products burn through board cracks; Ahead of direct combustion zone temperatures increase and core softens

Increased temperature levels change consistency of foam from rigid to flexible

Time

Tar

Heated foam

Figure 6-11: Temperature reaction of PUR/PIR foams when exposed to heat flux with and without shielding board layer
Figure 6-12: Speed of temperature build-up in sandwich walls depending on core materials

Chapter 6 - Fire resistance of structural sandwich walls: Effect of core
Figure 6-13: Effect of secondary failure of exposed board on core reaction: Comparison of intermediate- and bench-scale core reaction (Pyrok-PUR)

Figure 6-14: Effect of secondary failure of exposed board core reaction: Comparison of intermediate- and bench-scale core reaction (Sasmox-PIR)
Figure 6-15: Impact of board damage on heat build-up and damage in internal core material
6.2.3 Effect of internal links

6.2.3.1 The contribution of internal links to the fire performance of sandwich walls

This section examines the effect of internal links on the fire performance of structural sandwich wall units. The inclusion of internal studding in sandwich walls is anticipated to enhance the fire resistance of the units since the internal linking of the face boards is thought to prevent the delamination of the fire exposed panel veneer and thereby delaying the structural collapse of the unit. This type of delamination failure is generally encountered in the closely related cladding type sandwich panels. The insertion of internal units, which are foamed into the internal core substrate, is not common in structural sandwich walls due to the fact that their placing complicates the manufacture of the walls and requires additional time consuming working steps. Of the sandwich wall systems surveyed for the study only one used the internal studding by default (System 1). The study examined the influence of internal studding on the fire performance of sandwich walls with respect to temperature and stiffness performance. In addition the influence of studding in the panels in non-fire, normal state was evaluated through a multi-scale structural testing regime, which is further discussed in Section 6.3. In this section the effect of internal studding in sandwich wall structures is discussed by examining two main points:

(i) the influence of internal studs on the sandwich walls’ fire and structural performance,
(ii) the temperature response of the internal units in sandwich wall units.

6.2.3.2 Effect of studs on panel performance

The impact of internal studding on sandwich panel fire performance is linked to the decomposition stage of the exposed board and therefore related to

(i) heating up stage (time until through depth cracking of the board),
(ii) post-dehydration stage (linked to the delamination of large board sections).

The temperature reaction patterns of all sandwich units tested in the study, regardless of internal studding, were governed by the characteristic fire performances of the board and core materials. The occurrence of through-depth shrinkage cracks, which are linked to the onset of sharp rise behind the exposed veneer, is independent from internal veneer links as shown in figure 6-16. Along the length of the stud, the shrinkage of the board might be marginally delayed, due to the higher conductivity of the stud in comparison to the foam. But since the stud is enclosed in foam this effect is marginal and not influencing on the overall performance of the exposed board and sandwich wall. Generally the dehydration time of the board and the glue line degradation are neither prevented nor delayed by the inclusion of studs. In both, the studded and non-studded panel configurations, the time to sharp temperature increase behind the veneer is only influenced by the thickness and type of board material and the backing core substrate as listed in table 6-3. Although wood based veneers exhibit different decomposition behaviour when compared to mineral based boards, the inclusion of studs is considered similarly ineffective in the initial reaction stages of the wall.
The post-dehydration stage (ii) is marked by violent core decomposition and together with the ongoing heat exposure large board pieces tend to fall away from the panel, aggravating the heat exposure of the panel interior. The pattern of board failure in the full-scale wall has been presented in Section 6.2.1. In the post-shrinkage stage of the board degradation, internal studs are beneficial as they support and restrain the dehydrated and cracked board pieces from falling off the panel, as sketched in figures 6-17 and 6-18. The studding units and their connection to the board through screw fixings effectively reduce the length of unrestrained board pieces, retaining large board pieces on the panel and thereby dampening the heat infiltration and reducing the overall temperature levels measured within the panels (Figure 6-18). This also implies that internal veneer links, when placed at adequate distance within the panel can improve the insulation and integrity performance of sandwich walls, reducing the consumption of deeper core layers and therefore slowing down the heat increase on the ambient face of the specimen (Figure 6-19). Structural wall systems using wood based boards would encounter similar benefits from internal links, once the exposed board has fissured. In central parts of walls the enhanced restraint provided through the internal link gradually reduces and in between studs the benefit diminishes. The improving effect of additional restraint along internal studs reduces for board pieces about 200mm away from the internal link. The effectiveness of the internal studding in restraining the board pieces is therefore related to the

(i) number of internal studs per panel,
(ii) number of fixings connecting the internal unit and the exposed board.

Figure 6-17 to 6-19 illustrate the effect of internal studding on the restrained board area and its influence on the temperature reaction throughout the wall. The effectiveness of the internal studding with respect to restraint it can provide to the board, is also depending on the post-degradation strength of the exposed board material. Strong boards remain coherent and impact resistant so that the grip of the fixings is more efficient and in these materials an increase in fixings can enhance the shielding capacity of the board.

A) Impact of internal links on stiffness performance of structural sandwich wall in fire

The stiffness reaction of fire exposed studded and non-studded panels cannot be compared easily since the deflection device in the intermediate-scale was developed and incorporated into the testing regime after and as a consequence of the first set of tests on panels without internal veneer links. Hence temperature related panel stiffness data is only available for the studded sections. However, the link of sharp temperature rise behind the exposed veneer and the onset of considerable loss in stiffness has been noted before and suggests that the stiffness loss would have not been delayed, since the temperature reaction behind the veneer also showed no influence of the stud units (Figure 6-16, Table 6-3). The reduced temperature increase through the depth of the panel with studs also has a positive impact on the stiffness performance of the internal units. The reduced heat build-up in the studded panel is related to the fact that core consumption is reduced due to the enhanced protection from the retained dehydrated board pieces and therefore residual stiffness of the panel is improved. The importance of intact core material to the loadbearing performance of structural sandwich walls has also been
illustrated in the structural bending tests at ambient temperature. In the intermediate-scale test set-up board damage is concentrated in the centre of the wall, hence in areas furthest away from the studs. Since the deflections in the intermediate-scale test are measured in the centre of the wall section, the stiffness performance will be governed by the damaged sustained in this part of the test specimen. In the typical intermediate-scale wall failure the studs will merely hinge as the panel deforms into the furnace, only reducing the span of the unexposed board.

In the full-scale wall the effect of internal studding is more marked, since the studs are in the direct path of the vertical load. In Section 5.4 (Results of full-scale investigation) the comparison amongst full-scale sandwich walls tested under identical loading condition the internal studding delayed the loadbearing failure of the wall. With wooden edge studs the loadbearing failure was seen to be avoided completely and insulation and integrity failure became predominant. This was mainly influenced by the test set-up rather than inherent wall resilience, which is discussed in further detail in Section 6.3. Figure 6-20 compares the effect of intermediate cold formed studding in structural sandwich panel wall assemblies. In sandwich walls with the same thickness and type of board layers, core and sacrificial plasterboard protection the fire resistance rating was affected by the stiffness ($I_{\text{studding}}$) of the internal (intermediate) sections. The higher the initial stiffness of the stud the longer the wall maintained its loadbearing ability after the cladding board layers had failed. However, the effect reduced the higher the loading of the wall, shown in principle in drawing 6-7. The effect of internal studding to the overall ambient and fire performance of sandwich walls is further discussed in the “Reduction in loadbearing capacity” chapter concluding the fire section of the discussion.

Drawing 6-7: Effect of magnitude of load and stiffness of internal, intermediate studding units on fire resistance (loadbearing criterion) of structural sandwich walls (not applicable for walls with wooden edge studding)
B) Influence of studding in structural sandwich wall at ambient temperatures

In the ambient, undamaged sandwich wall the studs reduce the shear deflection encountered in the panels and thereby enhance the loadbearing ability of the walls. However, once one veneer is impaired, as encountered in a wall exposed to fire, the function of the stud unit shifts from shear reinforcement to main load carrying member. This increases the residual loadbearing ability of the section compared to a damaged "classical" (non-studded) sandwich wall. In the intact panel the composite compound between stud unit, core and boards is of paramount importance in order to efficiently exploit the shear stiffening effect of the studding unit. In the damaged panel the connection between the different panel components is of less importance since the unit then performs as a classical composite girder with the remaining unexposed board acting as stiffener against the torsional buckling of the stud. In this damaged configuration the core only contributes to the residual loadbearing ability to minor extent. The effect of stud stiffness on the loadbearing performance of the wall section is further discussed in section 6.5.

6.2.3.3 Reaction stud units

A) Temperature reaction

The temperature reactions of the stud units were closely monitored in the intermediate- and bench-scale scheme and the results have been presented in Chapter 5. Although both test scales provided information about the temperature build-up in the internal units the bench-scale regime additionally allowed for the detailed observation of failure modes. Here the failure of studding units themselves, but more importantly the failure within the composite section (i.e. board, stud and core) was recorded. These visual observations were of great importance to gain an understanding of the overall failure patterns, affecting the decomposition of the composite section, in which the foam encased studding units are bonded to the outer boards together with the internal self-adhesive core.

The heating rate of the internal studding was dependent from

(i) the degradation mode of the core material,
(ii) the location of the stud within the wall.

The heating of internal studding is accelerated in panels in which the core material is prone to flaming combustion once the exposed board is damaged. In contrast foams, which generate rigid and dense char upon decomposition reduce the heating of the internal stud. In these panels the encasement of the internal unit in the charred core reduces its exposed perimeter, dampening exposure so that studs remain at comparatively low temperature levels, especially along the unexposed flange. Furthermore comparative temperature measurement of studded and non-studded panel cross sections have shown that a considerable lower temperature level can be measured in the studded cross section. This is related to the increased board protection within the studded section of the panel, where the dehydrated board is fixed to the stud and acts as an effective shield against direct furnace heat. This also implies that the connectivity of board and stud also potentially improves the temperature shield of the core material encasing the stud unit further protecting the ambient flange of the stud and reducing the exposed perimeter of
the internal units. Edge studs are generally at lower temperature levels than central studs, as seen in figure 6-21.

Whilst the internal stud is potentially slowing down the decomposition of the core within the panel by providing an effective barrier to damage and heat build-up, the compartmentalization of damage is compromised in the panel systems with intermediate studding examined in the study. The perforated web of these stud units, which is preferred to simplify and accelerate manufacture, prevents the internal studding from being an efficient fire stop within the wall.

B) Failure characteristics
The foam enclosing the studs is heated and degrades over the entire length of the stud units. The foam damage in studded panel regions can extend beyond the actual heated area of the wall as the highly conductive steel sections heat up behind the board and transfer the heat efficiently, involving remote panel areas prematurely. Despite the fact that the cores were observed to decompose prematurely in stud regions the extent of damage was limited to the surface, aided by the typical smouldering decomposition mode of closed cell polymeric foams, especially when protected and remote from direct flaming. In the stud region the dehydration of the board (cracking) also extends beyond the heated area along the height of stud, also related to the rapid, efficient conduction the heat along the entire steel section. Both increased board and core temperature levels in studded panel sections increases the heat build-up in the panel, remote from direct damage. This however was not found to affect considerably on the temperature build-up within the entire wall and did not impact on the fire resistance ratings. Although this temperature reaction was not problematic with the materials used in the specimen panels, the use of thermoplastic cores and different board materials will demand re-evaluation and assessment.

C) Correlation of temperature reaction of studs in different scales
The temperature reactions of the studding units in the various fire testing scales were different. The unexposed flange temperatures measured in the bench-scale sample did not match the suddenly rising flange temperatures recorded in the intermediate scale tests. This is to be expected since the bench-scale sample set-up protects the internal section to increased degree as the board material remains in place and the surrounding core decomposes at slower rate. The effect of exposure conditions on the temperature build-up within the studding can be appreciated in figure 6-21, where the temperature history of directly exposed and partly protected studs is compared. In intermediate and full-scale tests the studs are likely to be heated not only from the front but also at the sides as central parts of the panel are damaged early. The involvement of the core in these areas emits flames and emits increased heat levels throughout the wall section. This cannot be fully replicated in bench-scale testing, even if the board material is manually broken from the exposed surface and the core consumed to greater extent. The complete exposure in one part of the stud is likely to lead to increasing temperature levels throughout the height of the entire stud unit. The bench-scale testing depicts a general behavioural response in
the degradation behaviour of the cold-formed stud units as encountered in the first minutes of vertically oriented fire test regimes. The bench-scale response models the temperature reaction of the studded panel units in edge areas of the wall assemblies where the studs are protected for the entire test period. Here the surface oriented damage of the core affects on the temperature build-up through the depth of the stud, which is governed by the heated perimeter of the stud. The ambient flange of the stud and parts of the web are embedded and protected through the intact insulating foam. In addition the charred, tarred surface damage slows down the heat penetration which causes a heat differential to build up between the exposed flange and the rest of the stud unit. With ongoing test time this effect reduces as the core is consumed and breaks down. A further implication of the unequal heating of exposed and ambient flange is the thermal bowing effect, where the hot flange of the units expand but since the deformation is restricted by the connection to the veneer and the enclosing test frame the movement is converted into a bowing deflection towards the furnace.

D) Failure and thermal deformations
In the intermediate-scale furnace exposure the studs are found heavily distorted and warped with some board pieces still attached and a small amount of light low-density char retained in the holed web of the stud. The loosely attached board was generally found in the lower part of the stud rather than in the upper part, which influences some of the temperature readings showing a clear distinction in temperature development depending on height. In the full-scale walls the majority of internal studs buckle towards the furnace, generally in the upper half of the wall. The edge studs are still covered in charred foam over the entire length of the units. Board pieces are retained at the sides of the wall and the studs are found to be protected by the loose board pieces.

6.2.3.4 Summary of internal stud effect
It is generally believed that internal veneer linking studs avoid the delamination, peeling off of the exposed board when exposed to a room fire. The fire resistance of sandwich walls increased once the internal units were inserted, although additional changes to wall composition were also undertaken (such as the additional lining with plasterboard) the outcome of the tests seemed to proof the validity of this approach. However, the present investigation shows that internal units do not delay the occurrence of through-depth shrinkage cracks. This also disproves the common belief that the delamination of the exposed veneer from the core, as encountered in cladding sandwich panels, is the predominant failure mechanism in loadbearing sandwich walls. In loadbearing sandwich walls the internal units are merely providing enhanced restraint to the board in the post-shrinkage stage, which reduces the temperature build-up within the panel interior and the internal studding units. The highly heat conducting steel causes the degradation of the core/ board remote from the area of direct exposure since it efficiently transports the heat to cooler panel parts which would in normal circumstances not have been degrading as early in the test. This effect does not markedly affect the panels' overall performance with the materials used in the specimen panels.
The embedment of the stud units into the core builds up a heat differential between exposed and unexposed flange in the fire case. The heating of the stud units causes their elongation but as their expansion is restricted due to the attachment to the board and the height restriction of the furnace (or a room in the real fire case) the stud will bow out, generally towards the furnace, during the test. A comparison of various full-scale results showed that the stiffness of the stud affected the rate of deflection in the test and the overall fire resistance rating. In the ambient wall the internal stud acts as a shear reinforcement.
Table 6-3: Effect on internal veneer links

<table>
<thead>
<tr>
<th>Panel</th>
<th>Links</th>
<th>Time (min)</th>
<th>Time (min)</th>
<th>Temperature (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cape-PIR No</td>
<td></td>
<td>11</td>
<td>16</td>
<td>760</td>
</tr>
<tr>
<td>Pyrok-PIR Yes</td>
<td></td>
<td>9</td>
<td>16</td>
<td>624</td>
</tr>
</tbody>
</table>

Figure 6-16: Comparison temperature build-up in panel with and without studs
Figure 6-17: Influence of number of studs and spacing of fixings on restrained board area
Figure 6-18: Influence of number of studs and spacing of fixings on temperature build-up within sandwich wall
Figure 6-19: Extent of core damage depending on stud spacing and board fixing
140
120
25kN/m- walls protected by 15mm fire-rated PB
50kN/m- walls protected by 15mm fire-rated and
12.5mm standard PB

Onset of sharp deflection increase indicates through-depth failure of
exposed board- stud becomes
main load carrying member

Figure 6-20: Effect of stud properties on fire resistance of sandwich wall at
two loading conditions (25kN/m and 50kN/m)

800
700
600
500
400
300
200
100
0
Hot flange Cold flange

Figure 6-21: The influence of stud location on temperature development in
studding unit

Chapter 6- Fire resistance of structural sandwich walls: Effect of internal links
6.2.4 Effect of joints

6.2.4.1 Jointing in structural sandwich walls

The subsection on the effect of joint addresses two main points:

(i) the interaction of sandwich panel and joint
(ii) the response of the different jointing mechanisms.

The fire testing programme assessed three jointing options. Two contiguous joints, in cold-formed steel and glass reinforced plastic (GRP) and the most common version of intermittent joints the hook closure mechanism. All panels also incorporated jointing sections at the top of the specimen. In the contiguously joined panel a GRP top hat section was inserted, whereas the intermittent vertically joined panels were aligned with the help of cold-formed steel u-channels capping the panel ends. Details of each test arrangement and joint configuration are presented in Chapter 2 and Chapter 4 and the results of the jointed test panel series are shown in Chapter 5 (Section 5.2).

Within a panel the jointing represents a potential weak spot, especially in recessed systems where inaccuracies can load to air voids. In most cases the jointing section is in direct contact with the heat environment due to its insertion into the panel. Its location and insertion into the panel bridges the outer environment to the core material and thereby also provides a direct heat path into the combustible panel interior, potentially short circuiting the board material and other protection measures. In intermittent joints this is not of paramount concern since their installation places them in the centre of the core material and therefore remote from the direct heat impingement. However, the intersection of panels is always of major concern as the boards of the single panels meet and, even with tight alignment of both panels, air voids cannot always be avoided.

Both horizontal and vertical jointing sections need to fulfil additional requirements with respect to internal flame spread potential and ensuring connectivity between panels. Both issues are of secondary nature, since part of the panel need to have collapsed for them to become of paramount importance. Nevertheless the occurrence of internal flame spread behind intact panel veneers can be of catastrophic consequence as it spreads the fire hidden from direct view and thereby compromises fire resistance and escape times for building occupants. Equally dangerous is the loss of connectivity between single panels especially where the panels are used as primary loadbearing elements (related to the danger of disproportionate collapse).

6.2.4.2 Interaction joint and panel

The temperature reactions of the jointed panel areas are more varied within panel area and temperature build-up within the panel can be delayed. This form of delayed and reduced temperature reaction has been associated before with the amount and rigidity of boundary or other restraint to the board. The variability of temperature response in the jointed panels therefore suggests that the inclusion of a horizontal jointing member at the top and the separation of the panel length in two halves, centrally jointed, provide a potentially higher restrained panel unit, which enables the shielded parts of the panel to
remain at lower temperature levels. This is likely since the centre of the panel was established in the plain panel tests to be the most vulnerable area with regards to damage and falling off board pieces. Here the vertical jointing provides enhanced restraint to the board due to its central location. It thereby helps delaying core involvement and dampening the temperature build-up in more remote areas, as sketched in figure 6-22. The benefit of contiguous jointing is therefore identical to the internal studding discussed before, but furthermore, since the jointed section is not perforated the fire stop at the intersection of panels is more effective. In the design of walls this should be taken into consideration, since the use of the jointing section is shown to be multi-purposed and therefore can be optimised in its design.

A) Temperature reaction
The board stiffening effect of the (central) jointing in the centre of the board must be balanced against the potentially weakening tolerance gaps, air voids and other direct heat paths to the combustible core at the vulnerable panel breaks. Air voids and inaccuracies in manufacture were seen to have a major influence on the insulation and integrity performance of the systems. This could be shown by comparing the contiguously jointed panels including tolerances and the panel units manufactured from the System 1, which did not include any tolerance gaps. The minimisation of tolerance air voids positively impacts on the temperature reaction of the wall panel, especially in measuring positions located close to panel joints where the temperature build-up is delayed and overall at reduced level. Although System 1 (tolerance free arrangement) was clad with the weaker performing board the exclusion of air gaps restricted the heat infiltration through the per se weakest panel areas at the joints and the temperature performance of the wall section was as good as if clad with the stronger board.

In the programme the jointed panels performed, if not superior then at least, as well as the plain panels, which suggests that the area most prone to failure are the central areas remote from any support. This has been mentioned before but becomes increasingly important the larger the scale of the test (wall assembly) when the distances between central, unrestrained board areas and jointed board lengths increase. Here the intermittent vertical joint system is bound to perform inferior to the contiguous systems since it does fix the board along the length of joint and therefore effectively increases the area of unrestrained board. Additionally the heat can reach the core at the vertical intersection of panels easily, and with only minor deformations within the wall, since the panel ends practically abut and opening up of the joint due to thermal movements can allow this to happen within minutes of exposure.

B) Effect of joint on stiffness performance of panel
The stiffness degradation, as measured through the deflection device incorporated into the intermediate scale regime, exhibited a principally different form of response than observed in the plain panel degradation patterns. The sandwich wall panels tend to perform stiffer when joints are included. In all panel types, i.e. jointed and un-jointed panel sections, the stiffness loss is influenced by the cladding board material. The fact
that the stiffness response in the intermediate scale test is mainly influenced by the
degradation signature of the central part of the panel suggests that generally the inclusion
of a joint section, especially when contiguous from top to the bottom of the panel,
increases the stability of the central part and hence the stiffness of the entire wall unit, see
drawing 6-8. The deflection measurement, recorded in the centre of the specimen hence
resting on the vertical joint, monitors in this set-up the degradation of the joint rather than
the degradation of the entire panel as before in the plain panel test.

![Diagram](image)

Drawing 6-8: Stiffening effect of internal contiguous vertical joint in intermediate-scale
test

This can be best appreciated for the intermittent joint, which exhibits comparatively high
deflections from the start of the test (see table 6-4). This was also observed for the GRP
joint especially when clad with the weaker Pyrok board. Together with the post-
experimental observations this can be explained as GRP jointing mechanisms was
consumed completely by the furnace environment, similarly to the intermittent hook
closure; both joint systems were destroyed by the end of the test. The OSB strips, biscuits
commonly found in US the SIP system are also bound to perform inferior to the
contiguous full depth joint, due to the slenderness of the strips employed.
Table 6-4: Effect of joint type

<table>
<thead>
<tr>
<th>Joint type</th>
<th>Deflection (mm) of joints at 10 min end of test</th>
</tr>
</thead>
<tbody>
<tr>
<td>Contiguous</td>
<td></td>
</tr>
<tr>
<td>Steel</td>
<td>8 13</td>
</tr>
<tr>
<td>GRP</td>
<td>9 25</td>
</tr>
<tr>
<td>Intermittent</td>
<td></td>
</tr>
<tr>
<td>Hook</td>
<td>10 25</td>
</tr>
</tbody>
</table>

C) Influence of board material

The board material is observed to have greater influence on the stiffness degradation of the joint than directly appreciable when comparing the temperature response behind the exposed veneer. The protection provided by the gypsum-based Sasmox when used as cladding board in the jointed panel is superior to the plain panel performance. In both configurations the Sasmox provides superior protection times, yet in the jointed panels the temperature response indicates that the board profits better from the enhanced restraint, which affects the degradation signature of the joint and therefore the stiffness of the entire panel, see table 6-5. The enhanced restraint could be due to the fact that the thicker gypsum board is tougher after dehydration is completed and therefore offers greater grip to the fixings, which help the board pieces to remain cladding the panel. The dehydration of the board is not affected by this effect but the second stage of the temperature failure of the board, involving the falling off the board pieces is in parts prevented and definitely delayed.

Table 6-5: Effect of board material on joint performance

<table>
<thead>
<tr>
<th>Board</th>
<th>Joint (Contiguous)</th>
<th>Deflection (mm) of joints at 10 min end of test*</th>
</tr>
</thead>
<tbody>
<tr>
<td>Sasmox (10mm)</td>
<td>GRP</td>
<td>2 9</td>
</tr>
<tr>
<td></td>
<td>Steel</td>
<td>4 9</td>
</tr>
<tr>
<td>Pyrok (8mm)</td>
<td>GRP</td>
<td>9 25</td>
</tr>
<tr>
<td></td>
<td>Steel</td>
<td>8 13</td>
</tr>
</tbody>
</table>

*Overall deflection compared at same test time (governed by failure time of Pyrok clad walls)

D) Full-scale

In the full-scale panel the hot and cold flange temperature development of the two-faced steel joint was recorded. The results indicate that the joint units exhibit a temperature differential, which is also observed in the bench-scale test. The heating up of the units is most severe in the areas of plasterboard loss. The fit between the two vertical board edges was improved in the full-scale test where the two leafed flanges of the joint were well
protected. At the end of the test the jointing units buckle in central areas and charred core pieces are retained in some parts of the wall, especially along the wall edges. The OSB tongues inserted in the systems commonly used in the US are consumed by the fire as soon as the exposed board layer is attacked. This form of jointing does not inhibit core damage.

6.2.4.3 Joint system

The degradation of the joint is influenced by several factors. The direct exposure to heat at board intersections but also the direct heat penetration through air gaps formed by tolerances or other openings. The former would be formed along the vertical board edges at the central vertical joint. Since the joint is formed by linking the two panel halves the board edges are about 1 to 2mm apart on the joint, creating a direct heat path to the joint material. In the panels joined by intermittently placed hook mechanisms the effect is more marked as the panel edges and the possible gap between them allows the heat to penetrate directly to the core. This is aggravated by the fact that the board edges at the joint are observed to open up under heat, also observed in the bench-scale tests, increasing the likelihood of core or joint in the test to be exposed to direct heat. The gypsum-based board material is determined to offer superior protection to the joint and the stiffness loss is considerably reduced when compared to the cement clad joints but also with respect to the plain panel. Heat penetration into the joint is likely in the contiguously linked sections where, in the tested panels, a direct heat path was created at the top of the panel by the tolerance between top of the vertical joint and horizontal joint. This was especially critical for the hollow GRP section.

The temperature performance level of both contiguous jointing materials, i.e. steel and GRP, is similar. The principally different materials used to manufacture the joint mainly influenced on their failure and stiffness behaviour within the panel. The typical effects on the temperature build-up in the panel are so similar that it can be suggested that any joint material could effectively be used to obtain similar results, assuming similar tolerance gaps and accuracy.

Vertical Jointing

A) Contiguous Steel

The steel joints provide a robust jointing alternative, which does not delaminate or disintegrate. The high thermal conductivity of the material and the fact that the adaptable shapes are generally foamed into the core promotes a potential weakness in the system. The direct heat exposure of the joint can cause the premature breakdown of the core. This can lead to extensive damage along the height of the panel and was observed to spread beyond the direct joint/foam contact points into adjacent core layers underneath the board areas in the bench-scale tests. Whilst the core gets involved earlier in the steel joined panels, the effect on the temperature response was shown to be only marginally more severe than for the GRP or intermittent joint since panel performance was governed by the combustion mode of the core materials. Meanwhile the steel joint offers the advantage of remaining stable and comparatively stiff, apart from some local bowing and
distortions, which can affect the deflection readings especially later in the test (Figure 6-23)

B) Contiguous GRP
The GRP joint is prone to brittle failure as the protruded, in Phenolic embedded glass fibres are exposed once the matrix is subjected to higher temperatures and evaporates. Failure is especially severe when the joint section is formed from separate units and assembled with glue, see figure 6-23. Then the stability of the unit is dependent from the integrity of the glue line rather than the GRP itself.

C) Intermittent hook
At the end of the test there were no residuals left of the hook closure mechanism. The styrene casing and the hook melt to such extent that they cannot be distinguished from other debris in the furnace. Integrity and insulation failure is imminent at the joint especially with increasing deflection.

D) OSB tongues
The connection system, commonly employed in the US, is thought to be performing similar to the intermittent joint described before. Whilst this jointing systems has the advantage of restraining the board pieces along the height of the wall due to the additionally inserted mechanical fixings, the stiffness of the two inserts is negligible and therefore similar to C). The thin sections are quickly consumed and do not provide an effective shield against spread of damage.

Horizontal Jointing
In the horizontal joint the stiffness of the joint and its degradation characteristics can affect of the loadbearing ability of the wall. A stiff load spreading top joint can potentially enhance the loadbearing capacity of the wall as it allows the spread of the load from attacked wall areas in the centre of the wall to edge areas where damage was found to be less severe. Recessed joints are potentially damaging especially in combination with brittle boards. The unprotected board edges are likely to break and this can form a weakness in the assembled panel.

6.2.4.4 Summary
The horizontal and vertical joints, linking a single panel units to wall elements are potential weak spots, since these locations are likely to create air voids allowing direct heat access to the vulnerable combustible core. The occurrence of tolerance air gaps and voids or other inaccuracies in construction provide the possibility of heat generated in a fire to bridge the board material and thereby short circuiting the temperature infiltration into the panel’s interior. In addition the panel components and especially the boards tend to decompose, deform and open up with ongoing high temperature exposure, which can create additional air gaps for heat to enter the panel. However, a contiguous vertical joint also provides additional restraint to the wall, since it breaks up the wall surface and provides additional fixity to the decomposing board. In particular panels clad by a board
material with good post-shrinkage strength can profit from the fixing to the joint, especially in the vulnerable top areas of the wall, where top horizontal and vertical joint provide enhanced support. The use of intermittent joints, which normally do not rely on fixings to provide the link between single panels, is disadvantageous in this respect since it does not actually provide any support to the board. The opening up of board edges at panel edges is additionally destructive, exposing the combustible core directly after relatively short exposure times.

The use of contiguous joints was also shown to provide additional strength as the joint stiffened the vulnerable central area of the panel in the intermediate scale test. The material choice for contiguous joints is crucial. The material should be chosen so as to not disintegrate with increasing heat exposure. Here the GRP used in this investigation showed weaknesses, especially as the single joint components were glued together to make the final joint profile. The cold-formed steel joint was more suited, although it experienced thermal bowing, its principal integrity remained and avoided air gaps through which flames could penetrate through to the unexposed, ambient side of the wall. The horizontal joint should be chosen as stiff and fire resistant as possible to enable the bridging of load once the central part of the wall is damaged.
Figure 6-22: Exposure conditions of joint in full-scale wall
Chapter 6- Fire resistance of structural sandwich walls: Effect of joints
6.3 The effect of fire damage to sandwich panel components on the loadbearing ability of structural sandwich walls

6.3.1 Introduction

In this subchapter the heat reactions of the single panel components, presented in the previous chapters are combined and assembled to evaluate the failure progression in structural sandwich walls exposed to fire conditions. The fire resistance of structural sandwich walls is generally governed by structural failure, related to the degradation of strength and stiffness of the various wall components. In order to quantify and ultimately design the fire resistance of sandwich wall building systems the temperature reaction and degradation characteristics of the single panel components need to be linked to a decline in load resisting capacity related to fire exposure time. This section establishes the reduction in loadbearing ability by combining the fire test results with the findings of the structural testing regime at ambient temperature. The outcome of the structural testing at ambient temperature links the transient loss in loadbearing ability of the wall sections, as measured in the fire tests, to steady state failure stages in the single panel components. This combination of information approximates the residual loadbearing capacity of the wall at distinct stages of panel deterioration. Through this approach the consequence of fire damage to the components on the loadbearing ability of the entire wall assembly can be quantified. As the contribution of each panel component to the overall fire behaviour of the wall is examined, the design of structural sandwich walls for fire resistance can be improved and optimised.

In this section the influencing factors to the fire resistance performance of three general types of structural sandwich walls are assessed

(i) "Classical" sandwich wall (no solid inserts)
(ii) "Classical" sandwich wall with solid edge/ corner infill sections
(iii) Modified sandwich wall, incorporating intermediate cold-formed internal studs

For each wall type the characteristic failure progression in fire exposure is presented. Based on the findings the analytical approximation of fire resistance for each panel type is suggested and implications of changes in panel composition are discussed.

6.3.2 Characteristic failure progression in loadbearing sandwich walls in fire resistance tests

The investigation into the fire performance of loadbearing sandwich wall established the vertical loadbearing capacity of the wall assembly exposed to fire conditions to be subdivided into three stages:

(i) intact wall at the start of the exposure,
(ii) warming up (intermediate stage),
(iii) loss of exposed board layer.

At ambient temperatures, before the start of the fire exposure, structural sandwich walls rely for their loadbearing ability on the firm connection of the two high density facings to
the internal core. In modified sandwich walls incorporating internal veneer linking sections the loadbearing performance is enhanced. Whilst the benefit from the internal stud with respect to stiffness is negligible, the internal connections of the faces act as reinforcement to the shear weak core. For the effective use of the internal studding in the intact wall section, the three panel members, i.e. stud, core and board need to be tightly connected. The unified response of all three panel components is best achieved with a self-adhesive core substrate and perforated internal studding units. The holes in the studding section are of major importance since they enable the injected self-adhesive foam mixture to expand throughout the panel and efficiently bond the single panel components.

As heat levels increase the exposed veneer is the first panel component subjected to the altered temperature regime and starts to degrade. In mineral based boards the gradual temperature rise through the depth of the board layer induces the dehydration/ shrinkage of the veneer, which results in shrinkage cracks. In wood based boards the heat exposure causes the ignition of the board and subsequent charring. In both types of structural boards the ongoing heating produces cracks through the entire depth of the exposed veneer. Whilst the cracks in mineral based boards are related to the shrinkage of the board, the fissures in wood based boards are much larger and caused by the contraction of the charring board layer. In this intermediate heating stage of the board it is still effective as panel component and contributes to the overall loadbearing ability of the wall. However, in both board types the through-depth failure impairs the shielding capacity of the first panel layer and exposes the internal of the wall to the fire environment. The speed of dehydration in the mineral based boards can be delayed depending on the board material, board thickness and type of backing core, as explained in detail in section 6.2.1 “Effect of board”. Once the through-depth failure of the board is completed, the board is no longer functioning as a structural member and as a consequence a substantial decrease in stiffness is encountered (stage (iii)). This could be shown through structural testing of the damaged panel. The failure of the exposed veneer represents the conversion point in the fire behaviour of the walls marking the time at which the strong loadbearing sandwich compound is irreversibly destroyed. This board failure effect is sudden and changes to the mechanical properties before the through-depth failure contribute to the stiffness response of the entire unit. This is discussed in detail in section 6.3.2.1. The correlation of temperature reaction, failure observations and stiffness performance in the bench-scale and intermediate-scale testing regime established this critical failure mode in structural sandwich walls.

6.3.2.1 The stages of decline in loadbearing ability encountered in structural sandwich walls

In figure 6-24 the reduction in wall stiffness, as monitored through the increase in deflection in the intermediate scale test, is linked to the evaluated successive physical damage encountered in plain sandwich walls. Drawing 6-9 translates the deflection performance of the sandwich wall unit in the intermediate-scale test as related to the principal failure mechanisms occurring during the exposure and correlates these to the
reduction in loadbearing ability of the wall section. In the intermediate-scale test the
effect of secondary failure reactions can be documented, which become of major
importance in the full-scale behaviour of the sandwich wall components. As the vertically
loaded full-scale wall loses the majority of loadbearing ability at the moment of veneer
loss, these secondary failure modes cannot be examined in detail in the larger scale test
due to the imminent collapse of the wall. The intermediate-scale regime enables to
continue the test beyond the board failure stage (transient stages (i) to (iii) above) and
thereby provides vital information on the failure dynamics of the panel internals after the
board layer has been destroyed. The different loading regime in intermediate-and full-
scale test the failures in panel internals have different impact on the overall loadbearing
capacity of the wall, which can be accounted for using structural analysis theory and
based on the findings of the structural testing regime at ambient temperature.

This analysis of the intermediate-scale test performance concentrates on mineral based
board materials and synthetic foam cores, which represented the largest material group
investigated within the study. The reduction in wall stiffness and hence loadbearing
ability in the intermediate-scale test sample is generally subdivided in three stages as
shown in figure 6-24 and drawing 6-9.

![Diagram of loadbearing ability in intermediate-scale test](image)

**Loadbearing ability of sample in intermediate-scale test (with deflection device)**

**Transitional stages in A**
- (i) intact wall at the start of the exposure,
- (ii) warming up (intermediate stage),
- (iii) loss of exposed board layer.

**Decisive for ultimate loadbearing ability of wall under vertical loading**

**Time (min)**

**Drawing 6-9: Reduction of loadbearing ability in intermediate-scale sandwich wall sample**

**A) Stage A (transitional stages (i) to (iii))**
The initial deflection of the unit, similar for vertical as well as horizontal loading, at the
start of the test is affected by two main mechanisms:

(i) the thermal expansion of the first wall layers,
(ii) the warming up of the board material.
The heating of the first material layers causes a temperature differential through the depth of the wall. The elongation of the heated part of the wall assembly is restricted by the test frame but also through the bond between the different layers, which are expanding at different rate due to the constituent temperature differential throughout the wall. Therefore the wall section deflects towards the heat source, similar to the reaction of a restrained steel column. At the same time the heating is degrading the exposed board of the sandwich wall, changing its chemical structure. In mineral based boards the heating initiates dehydration, a form of moisture migration, which temporarily increases the density of the board material layers improving its strength and stiffness properties. The changes in the micro-mechanical properties of the board therefore cause levelling of the deflection increase prior to the onset of stage B. This effect is also commonly encountered in concrete structures (Lie, 1972). Stage A converts into stage B when the board temperature has increased to cause cracking/ fissuring through the depth of the board layer. These characteristic failure stages of the exposed veneer layer and their effect on the loadbearing performance of the sandwich wall, are identical to the full-scale performance of the wall under vertical load.

B) Stage B
Once through depth cracks have formed in the exposed board layer the deflection rate markedly increases and the rate of stiffness loss is related to

(i) decomposition behaviour of the core, influenced by the amount of falling off board pieces,

(ii) the consistency, structural merits of the degraded core.

This stage of wall degradation is not always reached in the full-scale wall, which will be discussed in the subsequent section. However, in this second stage of the deflection reaction, the loss in loadbearing ability is mainly influenced by the core material performance because the core and its connection with the ambient board become the loadbearing components in the assembly. In this stage of panel destruction the charring ability and rigidity of the degraded foam is the governing factor to the residual loadbearing ability in the panel since,

(i) rigid char is an effective heat shield to deeper panel layers, slowing down decomposition,

(ii) rigid char contributes to the loadbearing ability of the unexposed board layer.

As the temperature levels through the depth of the core layer increase, the core material is softened ahead of the zone of immediate degradation, further reducing the amount of rigid char. Section 6.2.2 “Effect of core” has discussed the influence of core material on the extent and type of degradation reaction encountered in sandwich walls. The analytical model presented for the ultimate bending capacity of manually damaged panels (Chapter 5- Section Horizontal tests) also illustrates this effect.

The less severe stiffness loss in Phenolic and Mineral wool cored panels (in comparison to the PUR/ PIR cored panels) exhibits the beneficial effect of reduced core degradation. Although an EPS foamed panel has not been tested in the intermediate- or full-scale vertical regime, its performance in the bench-scale test suggests that the EPS degradation speed and reactions would be the most damaging to panel performance. The type of core
degradation is also a major factor in the loadbearing ability on the vertically loaded wall. In the full-scale wall the intact core layer has no impact on the loadbearing ability of the wall, due to the different loading regime. But the core layer and its degradation speed has a major impact on the heating up of the ambient panel face and any internal members and thereby indirectly influences the residual loadbearing ability of the full-scale wall.

Although the intermediate-scale units also incorporated internal stud units, their temperature dependent degradation is not as influential to panel performance as in the vertically loaded tests. This is related to the destruction pattern and loading arrangement of the intermediate-scale wall, where the damage is localised in the central part of the wall and the loading is perpendicular to the panel face. Since the studs are not part of the direct load path, they only reduce the span of the unexposed board layer, now the sole loadbearing component, but do not restrict the hinging mechanism in the centre of the wall.

6.3.2.3 Full-scale wall performance

The stability of the decomposed core, especially its coherence and rigidity is also the governing factor on the final overall deflection and residual loadbearing ability; this effect was also noted by Mouritz and Gardiner (2002). Although the centre of the panel, between the studding units is prone to greatest damage and likely to have greatest effect on the overall deflection rate, the ambient tests indicate that the complete veneer loss would result in ultimate deflections higher than those encountered in the intermediate-scale fire test. This suggests that a load distribution effect is influencing the panel performance from the onset of stage B. This is related to the failure dynamics in sandwich wall in both loading conditions, hence in intermediate- and full-scale. Once the exposed face veneer is removed in the central parts of the assembly, where cracking is most extensive, the outer panel parts, which are less damaged due to the enhanced edge restraint of the exposed board, must be retaining higher load resisting capacities. Thereby the overall deflection levels of the wall section are reduced and the residual loadbearing ability enhanced. The failure deflections encountered in the structural testing on the manually damaged panels can only be replicated in the fire test if the cracking of the exposed board extends over the full height and width of the test panel. This form of elongated consistent shrinkage crack has not been observed in any of the fire tests. These observations in the intermediate-scale test are of great interest to the reactions in the vertically loaded full-scale walls. As in the intermediate-scale samples where the ambient board face spans the most affected central part of the wall and enables load to be carried through to the less damaged outer perimeter of the sample, the full-scale loading arrangement using a spreader beam achieves a similar load bridging effect. This is discussed in the next section.
first stage of the stiffness response as it delays the temperature exposure and thereby delays shrinkage of the exposed veneer. Although the through-depth failure of the board marks the onset of stage B in both scales, the subsequent decrease in stiffness in stage B is no longer governed solely by the decomposition behaviour of the core material.

The fire resistance performance of full-scale sandwich walls after the failure of the exposed board is governed by the residual vertical load capacity of the wall. Whilst intact sandwich walls can bear vertical loads of up to 300kN/m, structural and full-scale fire test evidence has shown that their vertical load capacity drastically reduces once one veneer is removed. This is in accordance with the fire resistance rating of classical sandwich walls without continuous vertical inserts, as tested by the Forest Products Laboratory full-scale fire tests (Eickner, 1975), where structural collapse occurred within 5 to 8 minutes after the start of the test (for further detail see Chapter 5), which was the approximated failure time of the types of board materials used in these wall tests. This was also confirmed by a full-scale fire test on a wall representative of the systems investigated in this study (System 1 without internal studding), which collapsed after 7 minutes exposure (figure 6-25). The time to failure is marginally increased when compared with dehydration times established for this type of board (Pyrok) in intermediate- and bench-scale (which was about 5 minutes) because the board thickness was increased (from 8mm to 10mm) and the load spreader beam employed to apply the vertical load allowed for redistribution of the load to the less damaged edge areas of the wall. When full-height continuous vertical inserts, either placed at regular intervals in the panel interior or along the sides of the wall (Figures 6-26 and 6-27), are present in the panel the fire resistance of the sandwich wall unit is enhanced. Then the time to failure is delayed due to the fact that the stiffness reduction is governed by the degradation of the additional vertical inserts once the sandwich compound is damaged. The structural tests at ambient temperature on the modified sandwich wall system with internal cold-formed studding proved the importance of internal units to the overall loadbearing ability of the impaired damaged sandwich panel (as shown in Chapter 5- Section vertical load performance). In the vertical load tests at ambient temperature the load was shown to shift onto the internal stud units at the moment of veneer loss and the vertical loadbearing capacity was reduced to about 30kN/m. The shift of load was also shown by calculation and theoretical predictions in the structural vertical load and bending tests.

To further illustrate this point figure 6-28 compares the deflection behaviour of sandwich systems with internal cold-formed studding (System 1) with the characteristic stiffness reduction of lightweight cold-formed steel walls clad with plasterboard (as investigated by Alfawakhiri et al., 1999, 2000). The stud stiffness and loading is similar in both lightweight wall systems, although the spacing of the studding is reduced in the cold-formed steel lightweight wall. This comparison is not intended to compare overall deflection rates but rather the similarities in the characteristic stiffness responses between both systems. Both wall systems exhibit the characteristic reduction in loadbearing ability with the slow levelling deflection rate at the start of the exposure and rapid accelerated deflection increase, ultimately resulting in the buckling failure of the
studding. This similarity in loadbearing ability reduction is further underlined when both wall systems are compared with a sandwich wall assembly with wood based veneers, shown in figure 6-29. System 3 exhibits a principally different stiffness response, here again only the principal shape of deflection response should be considered, since the initial stiffness of the wall and the loading is not identical. The principal difference in the decomposition dynamics of wood based veneers affects the initial deflection rate. In the bench-scale test regime wood based boards were found to combust progressively rather than in a three-stage regime with a plateau temperature reaction as found in mineral based boards. The transition between loadbearing stages, i.e. load carried by sandwich as opposed to internal studding, is therefore not as marked as in mineral based board but smoothened and more gradual. In the case of the System 3 panel panels where the inserts are placed at the sides of the wall the charring of the studding is much reduced and in the current loading full-scale loading arrangement the tests are likely to be terminated on grounds of insulation failure, hence burn through phenomena, rather than loadbearing collapse.

6.3.3 Types of sandwich walls and the effect of damage on their loadbearing ability

Figures 6-25 to 6-27 summarise the findings of the three scale fire testing regime and additional structural testing. The compilation of all results establishes the characteristic, physical damage and its effect on loadbearing ability of the sandwich wall section. From this merge of results three generic types of full-scale sandwich walls exposed to fire can be established:

(i) sandwich walls with mineral based face veneers on a thermosetting synthetic foam core containing no contiguous vertical inserts and no plasterboard,
(ii) sandwich walls with mineral based face veneers thermosetting core and including cold-formed steel studs and contiguous vertical joints ,
(iii) sandwich walls with wood based veneers (for comparison) on thermosetting core and wooden edge (often also used as corner) studs.

As customary the latter two systems are clad by plasterboard. The classical sandwich wall without plasterboard fails as soon as the exposed veneer is degraded (see drawing 6-10). The ultimate loadbearing failure of the wall is marginally delayed due to the shift in load to the less damaged edge areas of the wall assembly, which is facilitated by the load spreader beam. This is a test condition rather than inherent wall strength.

Generally plasterboard cladding delays the loss in loadbearing ability as within the first 20 to 30 minutes of the test, the exact time span depends on the type of plasterboard and the amount of layers, the plasterboard heats up and gradually decomposes. At that stage the sandwich wall itself will also be gradually heated, although the heat input is considerably dampened by the plasterboard. With time the dehydration of the plasterboard progresses, which causes the cracking within the layer. Although the entire wall area is heated, the central part of the assembly is likely to be the most heavily affected, due to the reduced restraint to the degraded board pieces. This is evident from the analysis of the full-scale work and also in the intermediate-scale work in Chapter 5.
and also Chapter 6.2.1 "Effect of board". The bench-scale work showed that the pre-heating of the sandwich wall through the plasterboard layers shortens the temperature reaction of the exposed board layer once the protective layer is impaired. In wood based boards first surface charing is possible.

Drawing 6-10: Vertical loadbearing capacity of type (i) structural sandwich wall assemblies

Once the plasterboard layer has fallen from the wall in one part, the shrinkage of the exposed mineral based board wall layer is accelerated and the temperature reaction within the panel is observed the effect on the reduction in loadbearing ability is shown in drawing 6-11 and 6-12. Wood based boards generally ignite shortly after the plasterboard has fallen from the wall. Flaming is extensive but dies down once the wood chars. In mineral based boards the unshielded heat flux and the gradual destruction also affect the first core layers, which start to decompose and convert to a tar like glutinous brown liquid. In panels using wood based board, this effect is likely to be less predominant, since the board produces large fissures early after exposure. The different degradation mode of the wood based board and the fact the oxygen is supplied to the core immediately favours the smouldering combustion mode of synthetic cores. In more fire resistant cores the marginal difference in board degradation is not of major influence since the core threshold temperatures are higher and degradation is not induced as early upon exposure. Foams with lower threshold decomposition temperature and behind mineral based boards are likely to be affected more severely by the temperature environment and ahead of the direct decomposition zone of the core, the temperature increase causes the foam consistency to alter from rigid to soft, shown by the dotted line in figure 6-26. The extent of this form of core damage was approximated from the results.
of the bench-scale tests. The increasing temperature levels within the unit also affect the internal studding and jointing members. In figure 6-26 major parts of the exposed board in the centre of the wall assembly have been removed and the sudden falling of the board causes abundant access of air, which is likely to cause heavy, flaming combustion of the core, which will be consumed at increased rate. In panels using wood based boards the cores will exhibit enhanced charring performance and consumption of the board might be marginally less rapid. With the destruction of the board the load proportionally shifts onto the internal members of the wall assembly, which are also heated up. The magnitude of drop in load is dependent on the initial stiffness of the studding and the extent of their heating at the time of board failure, as shown in drawing 6-11. With ongoing exposure and extending panel damage the internal links degradation is accelerated and as they are exposed to the increasing heat levels their load carrying capacity reduces.

As the fire environment attacks the central part of the panel most significantly the edge areas with the well restrained board pieces will dampen the heat exposure and core consumption and consequently panel destruction will be reduced. It is thought that the load is spread onto the outer studs at that stage. Here again the test set-up, which employs the load spreader beam allows for this compensation, which is not inherent wall strength and is not representative of the as-built situations in dwellings. However, in the final stages of the test, as the decomposition progresses, the core will also be consumed from the sides rather than only from the front, aggravating the exposure of the internal units leading to the collapse of the wall.

In figures 6-30 and 6-31 the characteristic failures described above are related to the measured stiffness loss in the wall assemblies. In drawing 6-11 and 6-12 the changing stiffness is translated to the reduction in loadbearing ability for the modified types of sandwich walls, types (ii) and (iii). Figure 6-30 illustrates the effect of sacrificial plasterboard layers and the gradual heating up and degradation of the internal studding units, shown by the bench-scale findings. Research into the reduction in loadbearing ability in lightweight cold-formed steel walls (Alfawakhiri, 2000) showed that the insulated wall systems had reduced fire resistance. This was found to be related to the steeper temperature gradient between hot and cold flange of the internal studding unit which reduced the exposed perimeter of the unit and results in higher stress levels in the studding due to increased deflections, leading to accelerated failure.
Loadbearing capacity of sandwich wall unit (kN/m)

-200+kN/m

(Failure time of exposed board layer shortened due to preheating of board behind plasterboard)

Load level dependent on initial stiffness of internal studding

Reduction in loadbearing ability dependent on heating of internal studding and rate of stud's stiffness degradation

Applied load> residual stiffness of internal studding

Buckling failure of wall

Time (min)

0 1 2 3

Depending on plasterboard type

Drawing 6-11: Reduction in loadbearing capacity of structural sandwich walls of type (ii)

Figure 6-31 and drawing 6-12 summarise the effect of the various failure stages on the stiffness response of the sandwich wall panel systems with wooden edge infills and the reduction in load carrying capacity respectively. In these types of wall no three-stage response was observed. Since the onset of stage II in stiffness response could be linked to the dehydration, shrinkage behaviour of exposed mineral based boards, the different stiffness transition was linked to the wooden board material. Failure observations and background knowledge on the decomposition behaviour of wood based materials had shown the decomposition behaviour of wood based boards to be de-coupled from dehydration processes so that the transition between stage 1 and 2 in wood faced sandwich walls was seen to be smoothened. However, further research in the initial stages of wooden board decomposition would be beneficial for sandwich wall design in the future. The effect of the wooden edge studs is apparent, reducing the overall deflection rate and hence reducing the stiffness loss of the unit. Since the wood studs are placed at the sides of the assembly the foam and the ambient steel test frame further reduce the charring rate. This is beneficial to the overall performance level of these types of units as they are additionally protected. In the standard full-scale test set-up the failure of such wall assemblies will be most likely related to burn-through insulation and integrity criteria than loadbearing failure. This is due to the load shift onto the edge studding, facilitated by the load spreader beam employed in fire resistance testing. This is a flawed representation of the as-built situation, where the load is likely to be applied to the wall.
section through joists, hence without the possibility for the load to spread to the outer studs.

![Diagram of Loadbearing capacity of sandwich wall unit](image)

**Loadbearing capacity of sandwich wall unit (kN/m)**

-200 kN/m

- Failure time of exposed board layer shortened due to preheating of board behind plasterboard
- Rate of loss in loadbearing ability dependent on charring rate of timber edge studding
- Reduction in loadbearing ability minimal as load spreader allows load to shift onto timber edge internals at sides of wall
- Depending on board, type and thickness of core and its burning conditions and behaviour
- Insulation or integrity failure

**Time (min)**

0 1 2 3

Depending on plasterboard type

Drawing 6-12: Reduction in loadbearing capacity in type (iii) sandwich walls

6.3.4 Modelling the reduction in loadbearing capacity of structural sandwich walls

From the above correlation of physical damage and reduction in stiffness/load carrying capacity the various modelling steps, required in determining the fire resistance of loadbearing sandwich panel walls, can be identified.

6.3.4.1 Classical, non-studded sandwich walls

The modelling of loadbearing behaviour and fire resistance of classical, non studded sandwich panels is reduced to establishing the failure times of the exposed board since the failure of the board results in the collapse of the entire wall, see also FPL fire resistance tests and figure 6-25 and drawing 6-10. However, even if the studs are not bearing but solely stabilising the ambient board, as tested (see figure 6-32), a marked improvement in deflection rate to the completely stud free assembly can be observed. In this case the failure time of the stiffened ambient board is also dependent on the temperature degradation of the studding since board and stud units form a composite structure and the prediction for this type of wall is presented in section 6.3.4.3. Even in the damaged state where major board pieces are falling from the panel's exposed area, the increased fixing and restraint along the top/bottom joint and the internal units guarantees continuity for the load to be transferred into the internal units as seen in figure 6-33. This will be shown in greater detail later.
6.3.4.2 Sandwich walls with wooden edge infills (as common in US)
The previous discussion on the loadbearing behaviour of these types of walls has shown
that the prediction of fire resistance of these types of walls in the standard test set-up is
reduced to an estimate of panel (ambient board) burn-through time, hence insulation and
integrity criteria. The full-scale test set-up, which currently employs a rigid load spreader
beam for the application of the vertical loads, allows the load to shift to the edge stud,
making this wall set-up a test on a beam-post frame structure rather than the sandwich
wall assembly. This makes this test set-up in effect a non-loadbearing test for the
sandwich wall assembly and makes the loadbearing criterion non-applicable. In the case
where studs are placed more central to the wall assembly the predominant failure might
shift, even with the load spreading beam set-up, and verifying calculations should be
undertaken. Models by Clancy (1998) could be adopted for this case. However
estimations and charring rates as quoted in BS EN 5268: 4 (30) are thought to be
applicable. Since the stud units are regarded as small wood sections adjustments to the
charring rates will have to be considered. The recommendations for timber frame walls
are to be consulted. If the standard test as currently undertaken, remains valid for this type
of tests the fire resistance rating for a sandwich wall with 10mm Sasmox veneers, 70mm
PIR core and 15mm standard plasterboard can be approximated from the bench-and
intermediate-scale work to:

Table 6-6: Estimation of fire resistance of structural sandwich walls with wooden edge
routing, mineral based boards (10mm) and PIR core (70mm)

<table>
<thead>
<tr>
<th>Plasterboard (min)</th>
<th>Exposed board layer (min)</th>
<th>Core (min)</th>
<th>Unexposed board layer (min)</th>
<th>Fire resistance (min)</th>
</tr>
</thead>
<tbody>
<tr>
<td>30</td>
<td>7</td>
<td>16</td>
<td>14</td>
<td>67</td>
</tr>
</tbody>
</table>

The shielding time of the exposed board layer is reduced due to the preheating effect
occurring behind the veneer, as discussed in Section 6.2.1. The area of failure is likely to
be in the centre of the wall. As the edge areas of the wall are likely to fail later and in
these areas through depth consumption will not be the governing factor. The model
suggested by Takeda (2003) could be adopted for this wall type in the standard test set-
up. However, if a vertical point loading is adopted, which would be representative of the
real application of the walls, the fire resistance rating could be predicted through the
failure time of the exposed board as before for the classical sandwich wall assemblies.

6.3.4.3 Sandwich wall panels with internal veneer linking cold-formed stud units
With respect to sandwich panels incorporating steel studding the modelling of fire
resistance becomes more complicated since the degradation and temperature reaction of
the steel studs, after the failure of the exposed veneer, is principally different to timber
and affects the stiffness loss. Work by Gerlich (1995) and Alfawakhiri and Sultan (2000a,
b) has been analysed and put in context to work in structural sandwich walls investigated in this thesis.

Chapter 3 reviewed several models for the prediction of fire resistance of lightweight wall assemblies. Although sandwich walls are principally different in loadbearing technique from cold-formed steel and timber frame walls there are certain similarities with respect to their fire resistance characteristics. All three wall systems rely on the plasterboard layers in order to delay the heat influx and enhance the fire resistance performance. All three wall systems are used in housing as well as in low-rise industrial structures so that their loading regime is similar and their approval by building authorities is always dependent on a successful full-scale fire test. All systems profit from boundary restraints and depend on the connectivity between wall elements.

For the sandwich walls the ambient test programme focussed on determining the inherent loadbearing ability of the panels, in the standard and modified form. In case of the internally linked panel an altered loadbearing performance was established and the vertical load test programme focussed on the modified panels, since they represented the unusual and less understood form of panel. The fact that the internal studding was shown to be influential to the panel's overall loadbearing capacity once the exposed veneer was removed makes the internal units the predominant structural components after through depth board failure has occurred. This closely aligns the fire resistance behaviour of cold-formed steel walls with the fire behaviour of vertically loaded modified sandwich walls in the fire case, as was shown in figure 6-28 and 6-29.

Based on these similarities the fire resistance of loadbearing sandwich walls with internal veneer linking units was analysed through a model by Gerlich (1996) and Alfawakhiri, Sultan (2000). Both were developed to predict the fire resistance of cold-formed steel wall assemblies and were based on work by Klippstein (1978 and 1980), which established the degradation characteristics of cold-formed steel members. Both models have been based on individual extensive test programmes on cold-formed steel walls. The model presented here is a combination of both approaches and has been modified for sandwich panel applications to predict the failure and fire resistance rating for such walls.

A) Adapted Model
The models developed by Alfawakhiri/ Sultan and Gerlich both assume that the gradual heat build-up in the framed wall section causes its initial, thermal deformation, towards the furnace. The thermal bowing curvature of a simply supported vertical element, due to a temperature gradient across the section is described (Schneider, 1996)

\[ \varphi = \frac{\alpha_T \delta T}{d} \]

and the shape of the stress-free thermal deformation

\[ \Delta_T(z) = 0.5 \varphi z(L - z) \]

from which the following deflection at mid height \( z = \frac{L}{2} \) is derived.
\[ \Delta_T = \frac{\alpha_T L^2 \delta T}{8d} \]

In which

- \( \varphi \) thermal bowing curvature (1/mm)
- \( \alpha_T \) thermal expansion coefficient (\(^{\circ}\mathrm{C}^{-1}\))
- \( \alpha_T = (12 + 0.004 T_d) \times 10^{-6} \), Gerlich, 1996 with \( T_d = 0.5 \times (T_H + T_C) \)
- \( \delta T \) temperature difference across the member (\( T_{\text{Hot flange}} - T_{\text{Cold flange}} \)) (\(^{\circ}\mathrm{C}\))
- \( d \) depth of member (here depth of internal steel stud)
- \( \Delta_T \) mid-span deflection due to thermal bowing of member (mm)
- \( L \) member length, equalling to wall height (mm)
- \( z \) height (mm)

Due to this initial stress-free thermal deformation the loadbearing element is pre-deformed, which can be treated as an initial eccentricity \( e = \Delta_T \), resulting in additional bending moments with the application of vertical load, causing theory second order deformations \( \Delta_{II} \), as encountered in imperfect columns. The theoretical derivation of the additive moment within the wall unit can be undertaken with classical theory, see figure 6-34.

The overall bending moment and deflections caused by the initial thermal deformation is then

\[ M(z) = P (\Delta_T + \Delta_{II}) \]

\( \Delta_{II} \) Theory second order deformations

\[ P \] applied vertical load (kN)

with

\[ e = L \sqrt{\frac{P}{EI}} \]

the differential equation and its solution are:

\[ \Delta_{II}'' + \left( \frac{e}{L} \right)^2 \Delta_{II} = - \left( \frac{e}{L} \right)^2 \Delta_T \]

\[ \Delta_{II} = C_1 \sin \frac{z}{L} + C_2 \cos \frac{z}{L} - \Delta_T \]

With the boundary conditions

\[ z = 0: \Delta_{II} = 0 \text{ and } z = L: \Delta_{II} = 0 \]

the maximum deformation at \( z = \frac{L}{2} \) can be derived to
\[
\max \Delta_{II} = \Delta_r \left[ \frac{1}{\cos \frac{\varepsilon}{2}} - 1 \right]
\]

The gradual heating of the stud causes a temperature variation from hot to cold flange and consequently a variation of modulus of elasticity throughout the section (Feng, Wang et al., 2003). The variation in material property of the stud is accounted for by weighting the moment of inertia, \( I^* \), and the area of the section through the E-modulus at the distinct temperature level (Alfawakhiri, 2000b). Therefore the entire section is divided into a finite and sufficiently large number of two-dimensional elements \( q \), so that

\[
I^* = \sum_{i=1}^{q} n_i [I_i + A_i (x_i - c)^2]
\]

where

\[
n_i = \frac{E_i}{E} = 1.0 - 3.0 \cdot 10^4 T + 3.7 \cdot 10^7 T^2 - 6.1 \cdot 10^9 T^3 + 5.4 \cdot 10^{12} T^4
\]

\( I_i \)  Moment of inertia of element \( i \)

\( A_i \)  Area of element \( i \)

\( x_i \)  Distance of element \( i \) from the extreme fibre of the cold flange

The temperature of each element is calculated from

\[
T_i = T_C + \left( \frac{\delta T x_i}{d} \right)
\]

\( d \)  the distance from the centroidal axis of the modulus weighed section to the extreme fibre of the cold flange

\[
c = \frac{\sum_{i=1}^{q} n_i A_i x_i}{\sum_{i=1}^{q} n_i A_i}
\]

This is done in similar manner for the cross section of the stud so that the stress resisting capacity of the degrading stud unit can be computed to

\[
\sigma_F = \frac{P}{A^*} - \frac{P(\Delta_{II} + \Delta_r)}{I^*} c
\]

for the fire exposed side of the stud unit

\[
\sigma_A = \frac{P}{A^*} + \frac{P(\Delta_{II} + \Delta_r)}{I^*} c
\]

for the ambient side of the stud

\( A^* \)  effective stud section area (E-module weighted)

\( I^* \)  effective stiffness of stud section (E-module weighted)
The yield strength, modulus of elasticity and coefficient of thermal expansion alter depending the temperature of the stud. Figure 6-34 shows, in summarised form, the alteration of these values dependent on the temperature of cold-formed steel section. Thermal deflections and additional Theory second order deflections are summed to obtain the overall deflection of the wall. It has to be ensured that with decreasing temperature gradients between the ambient and the hot flange, as will be encountered towards the end of the test where the ambient flange will be heated more severely, the deflection remains at least as high as in the previous time step, since these deflections are plastic, hence non reversible. The critical temperature of the stud can be found when the predicted strength of the stud is equal to applied axial load.

B) Application to sandwich walls with internal cold-formed studding
The model described above computes the temperature degradation and thermal deformations of one stud within an assembly based on the temperature history of its exposed and unexposed flange. It also assumes that this one stud is representative of all the studs within the assembly. In structural sandwich walls the contiguous joints will be loaded as much as the studding units provided they have a comparable stiffness and undergo the same thermal degradation. The temperature variation of all internal units, (especially after through-depth cracking of the exposed board had been completed) located in the central part of the assembly as opposed to studs closer to the edges of the assembly was considerable. This was especially marked in the final temperatures of the internal units, as shown by the comparison of temperature gradients in figure 6-35 and 6-36 and in figure 6-37, also plotting the temperature gradients in the internal jointing units. The temperature variation along the length of the single unit is neglected, although it can at occasions be marked, especially for studs located in the central area of the assembly. The unpredictable falling off pattern of the plasterboard and exposed veneer can cause these temperature differences, which cannot be accounted for theoretically. In figure 6-38, deflection as measured in the full-scale test and the prediction from the model with varying stud temperature histories are shown.

In the tested sandwich wall assembly the internal vertical jointing was of about the same stiffness as the internal stud units and since the temperature gradient within the units was also similar, it can be assumed that all vertical inserts were carrying the wall load proportionally. However, the location of the internal joint at the intersection of the single panels at the board edges, makes the fixing in the units less effective and prone to edge break out as the wall assembly deflects. This has the potential to impair the load transfer into the sections in later stages of the test. Therefore the temperature input data for the model was only taken from the different internal stud locations (and their mean).

The deflection reaction is subdivided into two parts;

(i) the deflection reaction prior to the failure of the exposed board,
(ii) the deflection signature after board failure.
The rate of deflection established for the steel units, based on their temperature gradient within the first minutes of the test, is not representative of the overall deflection rate of the wall assembly. At this stage the intact sandwich unit, rather than just the stud, is reacting to the heat environment. It can be seen that the deflection of the steel studs on their own, due to gradual heating, is not matching the overall deflection rate of the wall and does not account for the recovery (plateau) stage just before the onset of stage II of the deflection response. This first part of the curve is very much influenced by the protective plasterboard layer and the respective heating of the exposed veneer, which can undergo micro-mechanical expansion, temporarily enhancing the density of the board material and thereby increasing its strength characteristics. This is thought to balance the thermal deformation of the unit towards the furnace and also any Theory second order deformations. The deflection of the wall unit at the start of the test were influenced by

(i) the thermal expansion of the wall unit,
(ii) the characteristic temperature properties of the board material compositely reacting with the internal stud units and the degrading core (not quantified in further detail in the test programme),
(iii) the overall temperature gradient within the sandwich unit.

These factors have not been incorporated since a detailed material investigation needs to be undertaken to confirm the magnitude of improvement achieved through the density increase in the board. Furthermore the shift in neutral axis within the sandwich panel due to the unequal heating is thought to affect these first minutes of the tests, similar to findings by Clancy and Young (1995). Therefore figure 6-39 concentrates on validating the adapted Gerlich/Alfawakhiri model for the deflection rate prediction subsequent to the exposed board failure.

Although the studs in this assembly were not of correct height, hence did not contiguously link top and bottom rail the well connected board, linking stud and top/bottom rail was seen to bridge the air gap and thereby transferring the test load reliably for most parts of the test. Despite the gap in continuity the internal units were the main load carrying units once the exposed board of the sandwich wall was impaired. The load transfer was still ensured through the connected board pieces along the horizontal rails and the vertical inserts, as can be appreciated in figure 6-33. This is not the ideal load transfer scenario and should be avoided, as it makes the performance of the bridging board section the decisive factor to the overall wall performance. As before three temperature histories from different locations within the wall assembly have been compared as indicated on the location sketch in figure 6-39. The deflection model could be further detailed by incorporating the internal jointing units as well as the internal studding. This is thought to be of greater value for the accuracy of the prediction if direct load transfer through the height of the wall can be ensured. With respect to load transfer the position of the jointing is disadvantageous since it relies on the screws inserted close to the edges of the board to transfer the vertical load. Whilst the load is certainly bridged over the air gaps at the vertical interconnection of panels, secondary failure, such as edge break out of the board make reliable load transfer unlikely and less suited for prediction.
The deflection rate predicted from the overall mean temperature histories of all internal steel units, hence including the temperature development measured at the cold-formed steel contiguous joints does not match the actual deflection measured in the test at the centre of the wall. Figure 6-39 plots the deflection of the wall as predicted with the above model and also indicates at what time during the test the stud load, carried by the degraded internal stud, exceeded the degraded ultimate loadbearing capacity of the heated stud. The deflection rate as predicted by the mean stud temperatures throughout the assembly does not match the measured deflection curve and also does not correctly predict the overall failure time. Although the deflection prediction from the central stud 2 is also higher than the actually encountered deflections, the failure deflection is accurately modelled. This prediction is thought to be accurate since the observations, as recorded by the test witnesses during the fire test, also stated that buckling failure of the entire assembly was initiated by a failure in this part of the wall. However the deflection rate was best modelled by the temperature history of stud 4 (measured from three measuring locations along the height of the stud). The fact the overall deflection signature of the wall assembly was best predicted through an edge stud is underlining and reinforcing the criticism made regarding the use of spreader beam when applying the test load. Once the wall is damaged in the central part of the assembly the load is shifted to colder, less damaged parts of the wall. However, in this special case of this test, where the top joint rotates as the board gapping the air void breaks, the load is redistributed and puts the hot central studs again under load, which was seen to trigger failure. However, in the current fire resistance test set-up the overall deflection and the failure time of the entire unit cannot be modelled by one stud only as the load spreading beam allows for the shift in load. This necessitates the modelling of all studs within the assembly in order to determine deflection rate and fire resistance rating. The failure in the studs is most likely to be the buckling failure of the compression flange, however the general failure modes of cold-formed steel studs have been investigated in further detail, e.g. Kesti and Davies (1999). However, in the case of any inaccuracies regarding the continuity of the units within the assembly, as encountered in the discussed test result, the weakest link within the load path is decisive for the fire resistance rating. In the present case this was encountered at the top of the panel where the top rail rotated after the board had been weakened. Further test evidence will have to be analysed and assessed to confirm the above suggestions.

6.3.5 Influencing factors to fire resistance rating of loadbearing sandwich walls

6.3.5.1 Wall factors

The extensive fire test scheme undertaken on System 1, allows for a wider range of other issues to be compared. The following section will review the factors influencing the fire resistance of loadbearing sandwich walls

(i) Loading
(ii) Height
(iii) Service holes

The effect of loading is marked. With decreasing load the fire resistance increases, which can also be confirmed through the theory presented; the lower the load, the lower the
stress level in the internal units after board failure. Hence the longer the time until the ultimate load is reached. Higher load levels can be balanced in two ways 
(i) increase the plasterboard protection 
(ii) increase the initial stiffness of the internal studding members.
Small alterations in height did affect on the overall fire resistance of the sandwich wall unit. However, larger height variations will affect the fire resistance as the slenderness ratio of the wall increases and consequently load-resisting capacity reduces. As observed by Clancy (2002) for timber frame wall assemblies: the fire resistance will decrease with increasing wall height. The inclusion of service holes, hence openings allowing the direct access of heat to the vulnerable internal of the panel can be balanced by adequate protection. In some of the test set-ups the increased protection around the openings increased the overall fire resistance of the unit, as shown in figure 6-40.

6.3.5.2 Problems with full-scale testing
Building elements can exhibit varying fire resistance and failure modes depending on the structural boundary conditions and the heat environment. Especially influential to the rate of loss in loadbearing capacity and the residual stiffness of a building element, are the boundary restraints provided by the surrounding building members. They offer rotational restraint or other stiffening effects, which balance the diminishing material/system strength for certain time periods and enhance the robustness of the construction. The fire resistance of an external load-bearing wall for example is likely to be enhanced by abutting internal partition walls or restraining floor slabs, which provide additional stiffening to the gradually decomposing unit. It can be shown that, although the full-scale testing method assesses elements individually, without accounting for the real life boundary restraints, its set-up provides similar beneficial, loadbearing capacity enhancing effects. In a full-scale fire test the boundary restraint provided to the wall through the furnace steel frame is unavoidable and also to some extent representing the embedment of the wall to adjoining wall segment in a house. Besides the boundary restraint provided by the frame, the method of application of the vertical load to the wall unit is also of major importance. The use of a load spreader beam, evenly distributes the load over the entire wall length, which is not always representative of the construction used in combination with walls. Structural sandwich walls for example are most frequently used with
(i) timber joists, supported from the wall, through joist hangers or rested into pockets or,
(ii) concrete floor slab rested onto the wall panels or hung from joist hangers.
Both alternatives represent a point loading scenario, which is very different from the one adopted in the full-scale test. The spreader beam, used in the majority of fire test houses, allows the load to be shifted from the damaged and more severely attacked central parts (or studding) to the shielded areas (studs) along the edges of wall, which delays failure. As this loading scenario is not representative of the as built situation it delays ultimate failure time of the wall assembly and thereby enhancing the performance level of the tested wall. It would be more appropriate and safe to use a point loading scheme, as found when applying floor loads through joists and hangers onto walls, so as to prevent the performance enhancing load shift in the crucial stages of the test.
6.3.6 Summary of factors influencing the reduction in loadbearing ability of structural sandwich walls

This section linked the temperature reaction of the sandwich units and the failure progression within the various components to the stiffness loss in the assembly, which ultimately leads to the overall failure of the wall. The combination of structural tests at ambient temperature and findings of the intermediate-and bench-scale fire tests has established the main failure mechanism in structural sandwich walls. Whilst all sandwich wall types perform similar up to the failure of the exposed board, three types of structural sandwich wall can be distinguished once the exposed board has failed. This study established these three types to:

(i) classical sandwich wall, two faces glued to core,
(ii) classical sandwich wall with wooden edge infills,
(iii) modified sandwich wall with intermediate cold formed studding units.

Whilst the type (i) panel fails once the exposed veneer has exhibited through depth failure, in walls of type (ii) and (iii) failure is prolonged. This is due to the fact that in a real fire scenario, as modelled through the full-scale fire test, the vertical load proportionally shifts onto internal studding units, which become the main loadbearing member. Type (ii) walls are the most fire resistant due to the fact that the wooden edge studding supports the entire test load early in the test and the failure becomes dominated by the insulation or integrity criteria of the unexposed board layer and any remaining core insulation of the sandwich wall. This conveys misleading fire resistance times, since the standard test set-up allows for the spreading of load and in effect tests the sandwich wall without vertical loading. If a point loading scheme is to be adopted in standard fire resistance testing, the fire resistance time of the type (ii) walls could be predicted as the type (i) panels, by establishing the time to through depth failure of the board. In the type (iii) panels the degradation of the studding units due to the temperature increase is decisive for the fire resistance rating once the load has shifted. For this type of sandwich wall a model was developed based on work by Alfawalkhi (2000) and Gerlich (1996). The adopted model showed promising results using the changes in stud temperatures, which were measured throughout the test. Whilst an edge stud governed the deflection rate of the wall, overall failure was initiated by a stud in the central part of the assembly. As before the shift in loading to edge areas is unrealistic and only achievable in a spreading loading system. However, the flawed construction of the prototype panels, which incorporated an air gap, adjusted this compensation and caused the rotation of the top rail in later stages of the test so that the load was re-distributed onto the least protected and most degraded studs in the centre of the wall. In order to take full advantage of internal units, structural sandwich wall must have a continuous load transfer so that the stud units adequately abut and form a contiguous load path from the load application point to the base. If this is not the case, as in the full-scale fire test, the weakest part, in this case the exposed board spanning the gap and crushing with ongoing degradation will fail first, not realising the full loadbearing potential of the stud units. More full-scale fire resistance test data will need to be examined to further validate the model presented.
Deflection (mm)

Maximum deflection dependent on stiffness of decomposed foam residuals attached to ambient board or if no core the bending resistance of ambient board spanning between stud units.

Minor fluctuation due to further minimal changes in core and board stiffness, any thermal bowing encountered in stud units.

Soft, non-structural core.

Dehydrated board pieces fall from panel, mainly in centre, core burns, rate of core decomposition affects on slope of deflection increase. Core ahead of decomposition zone softens, charring minimal.

Through depth dehydration cracks in board Core started decomposition

Bedding in rig and thermal bowing. Heat starts board dehydration and warms core. With heating of board temporary increase in board strength. Duration dependent from board material and decomposition signature of core

Time (min)

Figure 6-24: Failure progression in intermediate-scale wall panel with mineral-based boards and PUR/PIR core
"Classical" sandwich wall- no internal links, intermittent jointing, no plasterboard (PB) protection

$P_{\text{ultimate}}$ up to 300 kN/m

Through depth failure of board causes structural failure

Loadbearing failure dependent on time to through depth failure of board.
With PB loadbearing failure can be delayed
Test result for Pyrok (8 mm)-PIR (70mm) without PB
-7 minutes-

Intact panel parts at the sides of the wall, where board is better restrained can marginally delay failure, since load can bridge to these stronger parts. The effect is minimal, since board restraint is marginal without internal veneer links.

Figure 6-25: Failure progression in structural sandwich walls with no internal vertical studding
Sandwich wall with mineral veneer, synthetic (thermoset) core and intermediate cold-formed steel studs, non-loadbearing plasterboard (PB) protects panel on the exposed face.

0 Connection of panels through contiguous joints

Furnace heat causes PB to shrink and crack: Damage distributed evenly Exposed pre-heats behind PB layer

1 Batten chars

Central sheets of PB fall out first, panel veneer

2 Heat degradation of internal studding leads to structural failure

See figure 6-30

Figure 6-26: Failure progression in full-scale loadbearing sandwich wall panel with internal studding and contiguous jointing veneers clad by mineral based veneers
Sandwich wall with wooden veneer, synthetic (thermoset) core and wooden edge studs. Non-loadbearing plasterboard (PB) protects panel on the exposed face.

0
Connection of panels through splines

Furnace heat causes PB to shrink and crack: Damage distributed evenly

1
Exposed board veneer heats up

Central sheets of PB fall out first, panel veneer ignites and chars then flaming dies down

Batten chars
Large fissures in charred board pieces, internal core chars (smouldering combustion mode)

2
Load spans between wooden studs, which remain well protected by foam and PB

3
Fire resistance > 60 min: Burn through- Insulation and integrity failure

Figure 6-27: Failure progression in full-scale loadbearing sandwich wall panel with wood based board and wooden edge infill
Figure 6-28: Comparison of deflection signature during full-scale fire test for sandwich walls (System 1) and cold-formed steel walls (Alfawakhiri, 2000a)

Figure 6-29: Comparison of deflection signature of two types of sandwich walls with lightframe steel walls (Alfawakhiri, 2000)
Figure 6-30: Reduction in stiffness in sandwich walls with internal cold-formed studding and mineral based veneers
Gradual degradation of wood based veneer and involvement of core. Once through depth failure of veneer is completed, load bridges onto edge studs, which are well protected through restrained board pieces at the sides of the wall.

In wood units progressive charring and reduction of loadbearing section, once foam and board protection is impaired in that wall location. Due to enhanced restrained of board and reduced core degradation, failure is likely to be through insulation or integrity criteria rather than loadbearing failure. This is mainly influenced by the load application mechanisms chosen for standard fire resisting testing.

Figure 6-31: Reduction in stiffness in structural sandwich walls with woodbased boards and wooden edge infill
Failure deflection limit (EN 1365-1) for 2.4 m vertically loaded element

Board stabilised but no studs bearing at 25 kN/m

Only sandwich panel no internal studding at 20 kN/m

Figure 6-32: Comparison of deflection signature in sandwich walls without and partly bearing studs
Figure 6-33: Characteristic failure of sandwich wall with partly bearing internal studning
Figure 6-34: Analytical model for establishing the fire resistance of structural sandwich walls with internal cold-formed studding.

\[ \alpha_T = (0.004T + 12) \times 10^{-6}, \text{ for } T < 1000\degree C \]

\[ \frac{E_T}{E_0} = 1 - 3.0T/10^4 + 3.7T^2/10^7 - 6.1T^3/10^9 + 5.4T^4/10^{12} \]

\[ \frac{F_{yt}}{F_{y0}} = 1 - 5.3T/10^4 + 4.0T^2/10^6 - 1.9T^3/10^8 + 1.7T^4/10^{11} \]
Figure 6-35: Temperature differential in cold-formed steel studding inserted in structural sandwich wall panels.

Figure 6-36: Temperature build-up in internal stud units in central part of wall.
Figure 6-37: Temperature build-up in internal jointing and stud units

Figure 6-38: Correlation of modelled and actually measured deflection
Deflection/ failure prediction based on mean temperatures of all internal units, (see also figure 6-37)

Predicted failure time and deflection for central stud

Model

Measured deflection of wall

Failure of wall, initiated by the compression failure of the exposed board at the top of the wall.

Figure 6-39: Measured and modelled deflection of sandwich wall assembly after failure of plasterboard and exposed wall veneer

Unperforated wall under 50 kN/m vertical load protected by 15 + 12.5 mm PB

Service opening (approx. 400mm²) between internal studs under 50 kN/m vertical protected by 15mm+ 15 mm PB (fire rated)

Figure 6-40: Balancing the effect of weakening service holes within panels
6.4 The link of test scales - Novel testing methodology

6.4.1 The purpose of the developed testing methodology

The scaled testing methodology trialled in this programme was developed to enable the evaluation of the factors influencing sandwich wall performance. The layered composition of the sandwich walls, combining significantly different types of materials (i.e. high density, fire resistant boards with a low density combustible core) and using a range of additional components for jointing and internal links require a step-by-step testing programme. In a full-scale test this form of assessment is difficult to implement especially for modular building systems, which entail the jointing of the single units in the assembly of the wall unit. Therefore the successive assessment of the various panel components through a full-scale test approach is not only very costly but also impractical. In its concept the standard full-scale test is intended to provide merely a pass/ fail information and assesses the wall in only one set of circumstances. The comprehensiveness of the wall assembly in the standard full-scale test impedes on the evaluation of the true performance level of the product. Whilst the fire resistance of most traditional building products and systems can be assessed adequately through a full-scale testing regime due to their inherent fire resilience, the fire performance of novel building units such as structural sandwich walls, can be distorted as a result of the standard test set-up. Therefore the adequacy of the novel building products for certain applications can be misrepresented. Both, the test regime and the comprehensive build-up of the test sample inhibit the discrimination between material/ system performance levels and secondary failure reactions, such as falling off board layers. This does not significantly affect the assessment of traditional, well-understood building systems but the overlay of the various critical material and boundary influences can hamper the research into less understood forms of construction.

The three-scale fire testing methodology was developed to overcome the shortcomings of the traditional fire resistance testing regime and is deemed to provide a promising new approach to examining the true adequacy of building products in fire. Although the current full-scale test cannot be replaced with one single, altered test it can be complemented by smaller scale testing to identify and better assess the performance and contribution of the individual wall components such that

(i) a much fuller understanding of the factors influencing the behaviour of the structural systems can be identified so that small changes, such as the change of veneer thickness, can be related to overall fire performance,

(ii) the number of costly full-scale fire tests can be reduced and cheap smaller scale tests can be undertaken to establish the most advantageous material combinations, so that only one final, merely proving full-scale test is required to comply with building regulations and/or building authority requirements,

(iii) the influence of the severity of fire exposure on the performance of the wall can be examined and the design requirements for walls can be adjusted accordingly.
6.4.2 Conditions for a successful testing methodology for structural sandwich walls
For the successful implementation of a reduced scale testing approach for loadbearing sandwich structures the testing methodology needs to provide information about

(i) the temperature gradient within the unit,
(ii) the failure modes at the relevant temperature levels,
(iii) the correlation of physical damage (failure modes) and reduction in stiffness (as related to the temperature progression),
(iv) the fire resistance (ultimately the time to failure) of the unit when tested in the classical/ traditional full-scale test.

The advantage of the reduced scale fire testing, especially in the bench-scale, is the possibility to assess a wide range of material combinations, which is considerably cheaper than the respective evaluation in full-scale. Furthermore reduced scale assessment allows the testing of replicates, which improves the knowledge and certainty about the measured specimen performance and enables statistical analysis of the results so that mean material performance parameters can be deduced. Whilst the expensive full-scale test is still required as a proving test, the small-scale tests facilitate the parametric investigation into single materials, especially important in multi-layered building units, such as loadbearing sandwich panels. A further implication of reduced scale testing is the possibility to extend the number of tested panel components to gain information on horizontal and vertical jointing systems, veneer links and the use of built up layers such as sacrificial plasterboard to enhance fire resistance. One further advocate for enhanced reduced scale testing is the poor repeatability of full-scale fire test results, which can result in unsafe product performance classification. This should be considered as major issue within the building industry, as only one successful fire test result is sufficient to gain building approval, whereas in the other areas of building product testing, the mean of at least three test results is required to establish performance figures for a given building system or unit.

6.4.3 Summary of the scaled testing used in the methodology
The methodology developed for this work is designed to examine the fire behaviour and ultimately fire resistance of a wall unit. Three test regimes have been chosen in the work, all of which have been seen to provide relevant and complementary information for the different design requirements of wall elements, here for structural sandwich wall structures:

(i) modified bench-scale cone heater set-up
(ii) modified intermediate-scale furnace test (incorporating a loading device to monitor the stiffness loss of the units)
(iii) standard full-scale fire resistance test with enhanced temperature measurements.

Whilst the intermediate- and full-scale tests were vertical furnace-based test regimes, the bench-scale test was adopted from a reaction-to-fire test regime, as yet not commonly used in conjunction with fire resistance testing. The vigorous edge effects encountered
when testing the sandwiched units necessitated the alteration of the specimen size and introduced a new monitoring scheme. Providing that these changes to the specimen perimeter are implemented in the standard bench-scale regime (the Cone calorimeter described in Chapter 4), the test as customary is thought to be granting essential information with respect to smoke and toxic gas generation of these building systems in fire. In product approval and fire resistance design of structures these issues are as yet unaddressed and unaccounted. However, the current emphasis of regulations on fire resistance compliance is not conclusively addressing the fire threat to building occupants, which are in a majority of cases at risk due to enhanced smoke and toxic gas levels before the collapse of the structure becomes the predominant life threat. It is anticipated that the current approach will in time be replaced by new more stringent and encompassing regulations. The work in the research allows the adoption of such new requirements easily and based on the findings in this work the complementary bench-scale test will be able to demonstrate the performance levels of structural sandwich walls. Drawing 6-13 summarises the various testing regimes employed in the methodology and portrays the relevant test information gained in each scale. It also shows how the information is compiled to predict and ultimately design the fire performance of the wall units exposed to fire.

![Fire exposure of wall unit](image)

Drawing 6-13: Testing methodology developed for designing structural sandwich walls

### 6.4.3.1 Bench-scale

The bench-scale test used throughout the study was developed from a standard reaction-to-fire test regime. With an irradiance level of 50kW/m² and the enlargement of the
specimen perimeter a fire exposure similar to the furnace conditions encountered in the furnace tests is generated. Therefore the set-up and design of the test enables the detailed observations of characteristic failure performance of the units in representative conditions, correlating the effect of the heat exposure on the temperature reaction and physical damage to the units. This is especially important in the multi-layered panels, where the single materials interact in their heat reactions and only their combined responses determine the overall performance level of the wall. The bench-scale set-up can accommodate the principal panel components, i.e. board and core, and can also be enhanced to include additional protection, internal units or jointing. Whilst the fire performance of the principal panel components (board and core combination) are indicative of larger scale reactions, the relative sizes of the internal units in the overall bench-sized sample makes correlation to vertical larger scale tests less accurate. However, the observation of the physical damage encountered in the protected, jointed or linked panel is valuable for modelling work and helps in approximating the failure progression in the practical wall. The bench-scale cannot simulate the delamination of pieces of board materials, which is clearly evident in the full-scale. This is a disadvantage of smaller-scale test regime as this secondary failure mechanism can lead to premature failure of the construction. In sandwich walls the falling off, delamination of board pieces is one of the predominant secondary failure responses and clearly evident from the findings in the larger scale vertical test regimes. Due to size of specimen in the bench-scale test and the horizontal orientation of the sample, this board delamination failure cannot be replicated in the bench-scale test set-up. However, this board delamination can be simulated and the work with the modified bench-scale method has shown that these characteristic failure effects can be induced, by e.g. manually destroying the exposed veneer of the sample. Thereby the effect of the damage in both board conditions, i.e. covering the sample and delaminated from the remainder of the panel, on the temperature reaction of the wall can be assessed (see also figure 6-7). This in itself is again of value, as the secondary failure dynamics in the panel are induced in a controlled and monitored manner, which helps correlating the temperature reactions observed in the larger scale walls, see figure 6-7 (Chapter 6- Effect of board). Whilst these effects are constituent in the full-scale wall assembly their impact cannot be distinguished due to the overlay of the range of fire reactions encountered during this type of fire exposure.

6.4.3.2 Intermediate-scale furnace test
The traditional approach for evaluating the fire resistance of a building unit is a furnace-based fire test, which evaluates the three main factors

(i) insulation,
(ii) integrity,
(iii) loadbearing ability.

Whilst full-scale fire tests examine all three fire resistance criteria, small-scale furnace tests, such as the intermediate-scale regime employed in the programme, only examine the insulation and integrity performance of the wall. As such they are just indicative of the fire performance of a full-scale wall since the vertical loading of the samples is...
omitted. The loading of the reduced size test sample would by no means cause stress levels comparable to the full-scale wall.

In the developed testing regime the intermediate-scale furnace test routine is extended to incorporate an additional deflection measurement monitoring the change in stiffness of the unit with time. The application of load bends the panels perpendicular to their plane and induces a small non-damaging stress into the panel so that the decline in loadbearing ability in correlation with the temperature development throughout the panel can be monitored. The loading device and the enhanced temperature measurements are proposed as a major improvement to the test routine, increasing the meaningfulness and application of the test result. Furthermore the temperature profiles measured within the wall link in with the failures in the panel components observed in the bench-scale regime, connecting these to the reduction in stiffness. This combination of findings overcomes a general disadvantage of furnace-based tests, which are always limited in their possibilities to monitor the progressive failure of the wall section. As before in the bench-scale test the boundary restraint to the exposed board layer in the intermediate-scale regime is enhanced when compared with the full-scale arrangement. Since the test is conducted vertically the effect of the boundary restraint is reduced when compared to the horizontal bench-scale regime, but still significant, as will be shown in section 6.4.5.2. As in the bench-scale tests previously discussed, the panel design can be changed so that the effect of jointing, additional plasterboard protection and other related panel features such as tolerance gaps can be examined.

6.4.3.3 Full-scale furnace test
A full-scale fire test evaluates the wall performance as close to its end use condition as possible (Drawing 6-13); this cannot be achieved by any other test arrangement. The assessment of the wall with vertical loading, all wall components and the representative plasterboard cladding links the three main fire resistance performance parameters of a construction: temperature reaction of the system, physical damage in the construction and the correlated reduction in loadbearing ability. This proof of fire resistance performance is required by building authorities to ensure the safety of buildings in the fire case. As in the intermediate-scale test, the furnace arrangement in the full-scale test hinders the detailed observation of physical damage to the wall structure, so that reduction in loadbearing ability cannot be correlated to the temperature and failure reactions of the wall assembly. This reduces the outcome of the standard full-scale test to merely providing a pass/ fail criterion without generating general learning and understanding about the failure dynamics within the wall. Furthermore the test arrangement, including load application and heating curve, are not representative of end use conditions and thereby misrepresent exposure conditions, potentially compromising the safe design of wall structures. Some of the test's limitations were overcome by linking the test information in the various test scales as shown in drawing 6-13. Through the findings in the bench- and intermediate-scale testing the critical performance parameters in structural sandwich walls, such as the time to through-depth board failure, plasterboard fall-off, the function of internal studding could be determined. Based on these findings an enhanced
monitoring scheme (about 70 thermocouples, 2 mechanical deflection devices and one laser deflection measurement regime) was introduced to examine the range of related wall reactions more efficiently. Together with the information of the bench- and intermediate-scale testing the full-scale wall reactions, monitored through enhanced deflection and temperature measurement, were combined and provided vital information for developing a predictive design model for structural sandwich walls, presented in section 6.3.

6.4.3.4 Additional test information
Additional information on the behaviour of structural sandwich walls was generated through structural tests at ambient temperature. In the structural tests the critical governing fire damage in the walls, as determined through the bench- and intermediate-scale testing, is modelled at room temperatures to approximate the residual loadbearing capacity of the structure at the moment of damage. Since loss in loadbearing capacity during fire exposure is a transient phenomenon, the structural testing at ambient temperatures is used to benchmark the effect of the failures found in the scaled fire testing. The correlation of definite physical damage to overall loadbearing performance can give indication and help modelling fire resistance.

6.4.4 Performance information collected in the different scale tests and correlation of results to establish the fire resistance behaviour of structural sandwich walls
Figure 6-41 shows the sample design in the different scales as related to the full-scale wall. The use of the scaled methodology grants three types of information:

(i) the comparison and assessment of materials and material combinations (replicate testing in modified bench-scale test),

(ii) the establishment of characteristic failures within the material layers, their temperature reaction and interrelation to stiffness loss within the unit. Here bench- and intermediate scale tests are linked and with additional structural testing at ambient temperature the transient stiffness loss can be established in steady state tests,

(iii) the fire resistance of the layered wall unit.

6.4.4.1 Assessment of materials and the interaction between material layers
The bench-scale test is well suited to compare the performance levels of materials and material combinations. This is especially important in the sandwich wall units, which combine largely differing material layers and rely on the glue bond between the material layers for their structural loadbearing capacity. As insurance requirements for synthetic polymeric cores are becoming increasingly stringent the construction industry and structural panel manufacturer in particular, need to invest in the development of novel, improved core products. Here the bench-scale regime offers a versatile and comparatively cheap assessment method and can facilitate the assessment of

(i) veneer material: material type, density, thickness, finishing layers, additives

(ii) core material: type of core, density, fillers, retardants, thickness of core layer

(iii) performance of glue bond between board and core substrate

(iv) protective cladding: type of cladding, thickness, layering and
The merit of a material combination, i.e. board and core as individual material layers and their combination, can be established by monitoring the temperature development behind the exposed face of the panel. The relative performance level of varying material combinations can be distinguished as shown in drawing 6-14. This is easily achieved in that the time to onset of stage in the measuring position behind the exposed veneer is assigned the value of 1 in the datum configuration (as shown in drawing 6-14). The range of new board and/or core combinations are then related to this performance, which directly translates into the prediction of shielding ability in the larger scale vertical furnace tests, as will be discussed later. This approach is also suited to adopt the approach for design of experiments (Montgomery, 2001) in which the different influencing factors for e.g. the board characteristics can be altered to examine the design governing board characteristic.

\[ y = f(\text{Board, Core}) \]

**Drawing 6-14: Comparison of board performance.** Relative performance levels are established which help the design of changes and facilitates innovation of wall products

Similarly the different core performances can be compared, as shown in drawing 6-15. For the core assessment two degradation stages can be monitored

(i) the reaction of the core material behind the intact/partially decomposed veneer,
(ii) the temperature/insulation performance of the core when exposed directly to heat environment, modelling the effect of delaminating board pieces in the larger scale tests.
Here the time gap between the temperature increase behind the exposed veneer and in the centre of the core monitors the insulation property of the core, which as previously discussed can be related to a datum performance, i.e. a given core or a randomly chosen core temperature reaction.

### Drawing 6-15: Assessment of core performance in bench-scale test. Two modes of combustion can be monitored

#### 6.4.4.2 Correlation of temperature reaction, physical damage and stiffness loss through bench and intermediate-scale

In order to determine and predict the fire behaviour of structural sandwich walls the findings in bench-and intermediate-scale test are linked through the temperature profiles that develop upon exposure through depth of the wall units, see drawing 6-13. The temperature development throughout the sandwich wall section as tested in intermediate-scale for different exposure times is shown in figure 6-42. The temperature development obtained within the sample tested in adapted bench-scale regime is shown for the first 30 minutes and compared to the temperature development in the intermediate scale in figures 6-43 to 6-45. The bench-scale temperature response of the sample (measured from 12, replicate readings) closely resembles the heat build-up in the intermediate-scale panels for the first 15 minutes (Figure 6-43). After 20 minutes the temperature increase is marginally underestimated by the bench-scale test (Figures 6-44). As the temperature reactions of the sandwich wall samples in both scales are very similar for the first 20 minutes no conversion factor is required to translate the failure signatures observed in the bench-scale with the stiffness loss encountered in the intermediate-scale, see also drawing 6-16.
The underestimation of the severity of temperature build-up from 20 minutes onwards (see figure 6-45) is linked to the fact that the bench-scale test assesses the wall samples in reduced size and horizontal orientation, which does inhibit the falling away of board pieces. It could be shown that this would result in an underestimation of temperature build-up in subsequent panel layers (see Chapter 6- Section 6.2.2: Effect of core), which explains the divergence in temperature development. In structural sandwich walls the first 20 minutes of exposure are the most important with respect to onset of sharp stiffness loss. Comparison of temperature and stiffness performance had shown that the failure mechanisms linked to the sharp increase in temperature behind the exposed veneer was marking the onset of sharp stiffness reduction in the unit. The observations in the bench-scale test linked the occurrence of through depth cracking with the onset of sharp temperature increase and therefore linked the sharp stiffness reduction to through depth cracking of the board. The influence of board loss was also modelled in structural tests at ambient temperature to assess the effect of the failure characteristic on the composite loadbearing bond of the various panel components.

6.4.4.3 Correlation of temperature profile, physical damage and stiffness loss through intermediate-scale in full-scale

In the full-scale assessment the effect of temperature profile, physical damage and stiffness loss are combined and result in the failure of the wall unit (see also drawing 6-13). The structural failure of the wall determines the fire resistance rating, the safe exposure time for the construction. Since sandwich walls generally fail the loadbearing criterion, whilst insulation and integrity are secondary failure modes, the prediction of

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1 Chapter 6.3 has established that the type II classical structural sandwich walls with wooden edge infills predominantly fail the insulation and integrity criteria rather than the loadbearing criterion. This atypical
the time to structural failure is of paramount importance to design. The combination of intermediate- and bench-scale findings correlate the sharp loss in stiffness with the loss of the exposed board layer, which is concurrent with a sharp increase in temperature behind the exposed veneer. This correlation of sharp temperature increase behind the exposed veneer and onset of stiffness loss also predicts the characteristics of stiffness loss in the full-scale wall, see figure 6-46 and drawing 6-17.

Therefore once this link between failure, temperature increase and stiffness loss is established for a material combination, bench-scale testing of materials can establish the board failure time, which is governing the onset of sharp loss in stiffness. Although the mechanism causing the sharp reduction in loadbearing ability can be identified through the scaled testing, the time to onset of the sharp stiffness decline can be delayed by additional layers of plasterboard. The plasterboard layer slows down the heat build-up in the wall and thereby alters the characteristic degradation times of the board materials and the board/ core interface. As previously discussed (Section 6.2.1: Effect of board) the plasterboard protection times in the intermediate-scale are enhanced when compared with the full-scale performance. Therefore the intermediate-scale temperature response of the wall is delayed and cannot directly predict the performance of the wall in full-scale. Here the correlation between intermediate- and full-scale test needs to be adjusted to account for the differing boundary restraints in each scale, such that the temperature and stiffness reaction of the wall assembly is staggered from the intermediate- and bench-scale findings. This is discussed in further detail in section 6.4.5.2.

![Diagram of temperature (Time) reaction of wall in Intermediate-scale and full-scale test](image)

**Drawing 6-17: Correlation of temperature (failure) and stiffness performance in the three scales**

Behaviour is as a result of the load application used in full-scale tests rather than inherent strength within the wall assembly.
6.4.5 Modelling of the fire resistance performance of the full-scale wall

The ultimate aim of any testing regime is to provide information with which an analytical calculation procedure for the wall's fire resistance can be developed. The development of a dedicated calculation procedure is linked to broad, wide-ranging knowledge about the temperature reaction of materials and their specific failure temperatures and correlated degradation profiles. The computation of fire resistance of building units is increasingly common especially for traditional wood, concrete and steel structures, for which the interrelation of temperature, degradation and strength/stiffness loss has been extensively studied. In these calculation procedures the temperature levels within the unit, as caused by the temperature environment, are determined through heat transfer models and the temperature gradients within the structures are then linked to the customary property degradation profile of the material. Temperature levels and degradation characteristics are correlated to the reduction in loadbearing capacity. Although the fire resistance of a unit is traditionally established through fire resistance tests, building systems built with traditional, well researched materials can be proven by calculation or “deemed-to-satisfy” data. The analytical determination of fire resistance is increasingly accepted by building authorities, as is the “deemed-to-satisfy” approach in which a building section is deemed to satisfy fire resistance requirements if its dimensions and construction comply with minimum section dimensions and protection measures included in the fire design guidance documents. This route to building approval is only possible for materials and systems, whose fire resistance performance is backed by extensive test evidence.

6.4.5.1 Fire resistance models for structural sandwich walls

The findings of the developed test regime, in combination with an extensive comparison of the fire behaviour of other sandwich and lightweight wall systems, allowed for an analytical procedure to be suggested for predicting the fire resistance of structural sandwich walls. The scaled testing programme provided the information required the differentiation of the various stages of the walls' fire reaction and a calculation procedure for approximating the fire resistance for the three main types of sandwich walls has been presented. Ultimately this approach would need to be verified by replicate full-scale wall testing and in time a “deemed-to-satisfy” approach could be developed for structural sandwich walls.

Whilst the mechanisms leading to the structural collapse of the unit can be well predicted, the differing boundary conditions in the furnace tests (panel oriented vertically) require a conversion factor to approximate the onset of structural failure as related to the temperature behind the exposed veneer, as shown in figure 6-47, especially when additional plasterboard is used to clad the wall. The reduced scale of the testing regime misrepresents the boundary conditions of the cladding protective plasterboard layers, which also affects the dehydration time prediction of the exposed panel veneer. The fall off times of plasterboard linings and subsequent through depth failure of the exposed wall layer are governing factors for translating the wall performances in the smaller scale to the practical wall set-up.
6.4.5.2 Test scale dependent boundary restraint to exposed board layers

A conversion model is proposed to account for the different boundary restraint conditions of the plasterboard and exposed wall layer, see figures 6-48 and 6-49, in the vertical intermediate- and full-scale test regimes. Whilst it is theoretically possible to convert the bench-scale performance of the boards, it is thought impractical and unsafe to assume that a horizontal scheme could predict secondary failures in a vertically oriented full-scale wall structure. Such a performance translation would be solely based on empiric test data and would not be underpinned by physical test evidence. The proposed conversion factor therefore accounts for the amount of restraint provided in each vertical furnace test scale and is evaluated to:

\[ R = \frac{L_R}{A_R} \]

Restraint factor (1/m).

Two approaches have been adopted:

(i) a sectional approach for the plasterboard conversion,
(ii) a restrained area factor for the exposed board layer.

The empirically established factor for (i) depends on the number of timber battens used to fix the plasterboard to the wall. The embedment of the plasterboard into the test rig around the edges of the wall provides enhanced restraint (restraint length x 1.5). The plasterboard area is divided into sections, which relate to the location of timber battens and edge restraint. Since the plasterboard is only connected to the walls through the battens the restraint length for each area is evaluated separately and the restraint factors for each wall sections added (see also figure 6-48). This is different for the exposed board (ii), as the board here is connected to the core and the internal units, which forms a continuous composite restraint to the exposed board area. Therefore the length of the internal restraints provided by jointing or internal studding is divided by entire board area of the test wall (see also figure 6-49). The restraint length along intermediate vertical joints is doubled, as two rows of fixings are inserted each side of the joint. Since the exposed board is connected to the horizontal rails in addition to the embedment in the test rig, the edge conditions are accounted by factor 3. So in order to predict the time to sharp temperature increase and onset of stiffness loss in the full-scale wall, the time to onset of sharp temperature increase in the intermediate-scale is divided by the conversion factor for the plasterboard and the exposed board layer, as shown in figure 6-50. In both cases the number of fixings connecting the board to the battens or internal units is influential and it should be emphasised that intermediate- and full-scale specimens should be constructed with identical fixing patterns for the correlation to be valid. Future developments should be concentrating on evaluating the influence of fixing pattern on board restraint conditions. Board fixings at close centres should increase the shielding capacity of the board layer. However, the effectiveness of the fixings is also board material dependent and a systematic parametric study into these factors would be of great benefit to a range of wall systems, including timber frame and lightweight cold-formed steel walls.
For the materials and systems tested within the programme the above restraint conversion has worked well and the onset of temperature response could be predicted from the intermediate-scale within 3 minutes of the actual temperature response in full-scale. Since the temperature reaction in the full-scale wall is rapid and instantaneous through the depth of the wall, as seen in figure 6-51, the onset of sharp temperature increase through the temperature reading behind the exposed veneer in the intermediate-scale is likely to be the most accurate prediction for temperature damage in the full-scale wall. Whilst the knowledge of the speed of progression through the depth of the wall is of interest for the insulation and integrity criteria, it is not decisive for the correlation of stiffness loss and is therefore of secondary importance.

6.4.6 Testing regimes required for assessing the fire resistance performance of structural sandwich walls in altered configurations

The testing in scales developed through this work can be used to advise on the design of structural sandwich walls. A database has been initiated, which categorizes and assesses materials based on their contribution to the fire performance of the walls. The fact that the material performances could be linked to degradation profiles and ultimately a reduction in loadbearing capacity makes the use of the three scaled regime increasingly efficient as more test data becomes available, simplifying the assessment of changes in wall composition. The influence of a change on the fire resistance of a structural sandwich wall assembly can be determined through the testing methodology. The type of change determines the number of scales required for its assessment as shown in drawing 6-18.

For all types of changes a final, proving test in full-scale is required to demonstrate the compliance with building regulations. If this testing approach is accepted by building authorities then small changes to wall composition (type I and type II) could be proven through the “deemed-to-satisfy” route, described earlier. In the future and with further experience this would be a great benefit to structural sandwich wall design and reliably indicate the true adequacy of a building product in general.

There are four types of changes to the wall unit:

**Type I** Changes which can be accounted for by the design models developed through this work, i.e.
- Number and size of internal units
- Restraint to standard plasterboard
- Top/ bottom rail jointing
- Size of vertical jointing
- Change from contiguous to intermittent vertical jointing

**Type II** Changes, which affect the onset of sharp stiffness loss in the panel, but remain close to the principal system and material combinations evaluated through the programme, i.e.
- Thickness of exposed mineral based board layer
- Different mineral based board material
- Different core material
- Different board/core combinations

**Type III** Changes in wall composition incorporating materials which have not been examined in detail but are of similar characteristics as the materials used in the current programme, i.e.
  - Vertical jointing material
  - Horizontal jointing
  - Plasterboard type and number of fixings

**Type IV** Changes, which use materials far different from the ones used in the current system or changes which create a new panel layout and assembly, i.e.
  - Board materials, other than mineral (wood) based boards (e.g. faces made of glass reinforced plastics)
  - New core materials, higher spec from the ones in the programme
  - Orientation of board layers, panel assembly (e.g. horizontal panel orientation)

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**Final Proving full-scale fire test - for authority**

**Prediction & Design of fire behaviour and resistance of practical wall**

- **Type I changes**
- **Type II changes**
- **Type III changes**

**Develop New Model**

- **Bench-scale** establishes **Physical damage** in unit related to **Temperature profile**
- **Intermediate-scale** establishes **Stiffness loss** in unit related to **Temperature profile**

**Full-scale** links **Temperature + Physical damage + Stiffness loss** to **Failure of practical construction**

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Drawing 6-18: Testing and modelling required to predict and design the adequate fire resistance of structural sandwich walls

Changes of type I could be assessed from this work, such that the analytical models suggested could be used to predict the influence of the change on the fire resistance of the wall assembly. Changes of type II only require minimal testing in bench-scale and concentrate on the determination of the altered dehydration, hence through depth failure times of the board/core combination. As long as the type of board material considered for
the change is similar in behaviour to the ones used throughout the study, hence exhibits a dehydration type failure, the bench-scale provides the relevant data required by the predictive model. This also applies to a wide range of foam materials since the interaction of the foam with the board layer can be assessed reliably in the bench-scale regime. This simplifies the assessment of a wide range of board materials available in the construction industry.

Changes to the vertical jointing material or indeed a new, innovative jointing method can be assessed in the intermediate-scale. Here the stiffness performance of the jointed section at increased temperatures can give a good indication of its response on larger scale. Some bench-scale testing might be advisable to firm up the understanding on the effect of the jointing on the physical damage in the panel internal. The performance of the horizontal joint can also be examined in intermediate-scale.

An intermediate-scale programme assessing in further detail the stiffness reaction of the structural sandwich walls clad with wood based board would be needed. Although the work by FPL does support the predictive model suggested by the research, a detailed assessment of the interrelation of board failure and stiffness loss is still outstanding. This one off testing programme would enable the assessment of changes to the wood based board layers, as for the mineral based board layers described earlier. A change of plasterboard type and fixings could be undertaken through the intermediate-scale test and its performance in the full-scale extrapolated through the conversion model presented before. It is likely though that at some stage the increase in fixings does not provide much benefit as the governing factor to the shielding ability is related to the post-degradation strength of the board.

The last and most major change in panel composition, requiring the largest number of tests in different scales, would be changes of type IV. Such changes to panel composition would for example alter the exposed board layer from a low spec mineral based board to high spec GRP board material or the use of a principally different core materials. These materials are likely to have different failure behaviour and the prescribed route of the different scaled tests would be used to re-evaluate the links between temperature performance, physical damage and stiffness loss. Building on the findings of the test regime a new behavioural model would need to be established. Furthermore new higher quality materials might exhibit better post-degradation strength and therefore will benefit from internal studding and edge restraint to greater extent. In this case full-scale testing would be required to guide on a new conversion model accounting for the enhanced board boundary restraint in the temperature reaction of the wall unit. Principal changes to the panel assembly, i.e. panel assembled horizontally rather than vertically (e.g. to facilitate openings) or ring beam situations, also require the full three-scale testing assessment to benchmark the performance influencing factors.
6.4.7 Future developments: An outlook
6.4.7.1 Prediction of heat build-up in sandwich wall section

The theoretical determination of the heat build-up in a member exposed to transient heating conditions requires numerical, iterative calculations. In the case of structural sandwich walls the provision of a relatively simple dehydration analysis of the exposed board layer would reduce the required testing even further. Many textbooks, such as Drysdale (1998) and Holman (1990), consider the heat build-up in elements when exposed to defined heat environments with iterative calculation procedures. In Appendix IX the theoretical background for these iterative calculation procedures is briefly discussed.

A finite difference method (FDM) has been adopted from Sultan et al. (2001) for sandwich walls in the intermediate-scale test to predict the temperature history within the unit. FDM is relatively simple in conception and therefore suited for approximate determination of heat build-up in sections subjected to a prescribed, defined rate of heat impact. It approximates equation 6-1 in the form of finite-sized differences between values at particular locations. A physical energy balance technique is used to derive the heat transfer into the section.

The temperature increase at each point, or control cell, is predicted by determining the energy balance, i.e. the heat flow into and out of the control cell, for each material layer. The overriding principle for temperature distribution calculations is Fourier’s partial differential equation for heat flow by conduction:

\[
\rho c \frac{\partial T}{\partial t} = \frac{\partial}{\partial x}\left(k_x \frac{\partial T}{\partial x}\right) + \frac{\partial}{\partial y}\left(k_y \frac{\partial T}{\partial y}\right) + \frac{\partial}{\partial z}\left(k_z \frac{\partial T}{\partial z}\right) + \dot{Q}^w
\]  

(6-1)

where

- \( \rho \) Density of material (kg/m³)
- \( c \) Thermal capacity or specific heat (J/kg K)
- \( k \) Thermal conductivity (W/mK)
- \( T(x, y, z, t) \) Temperature (°C or K)
- \( t \) Time (seconds)
- \( x, y, z \) Cartesian coordinates
- \( \dot{Q}^w \) Rate of heat production per unit volume (W/m³), which in most cases equals zero

The FDM approach to this heat transfer problem adopted the explicit method, by which the temperature at a given level, for a given time is directly computed from previously obtained values. For the present problem the calculation procedure may be reduced to a single dimension assuming that the element is of infinite length and heated uniformly. This can be assumed when the thickness of the panel is small in comparison with its
width and length, since the main heat flow direction is then assumed to be perpendicular to the face. This solving method, although effective, restricts the length of time-step to be applied in order to maintain numerical stability. In the current application this is not restricting since the rapid temperature increase requires short time steps to accurately model the rapid temperature increase on the exposed face. It is considered to be powerful tool to quickly and simply assess the through depth cracking times of board materials.

6.4.7.2 Heat transfer in sandwich walls

In the case of sandwich panels in a furnace test the exposed board surface gains heat from the furnace through radiation (convection has been neglected) and looses heat through conduction into the adjacent board layer. Any internal layers are similarly heated through conduction from the preceding material layer and lose part of the gained temperature through conduction into the subsequent layer, as schematically shown in figure 6-52. The board-insulation boundary layers are a special case since the glue line combines two different materials at that point. The model incorporates this sectional point by assigning it two sets of material properties, one for the board and one for the core. The unexposed board surface looses heat to the surrounding environment through radiation and convection, while gaining heat from the preceding layer through conduction. This represents the reversed situation to the heat transfer of the furnace-exposed face.

The heat build-up on the exposed face was modelled by equation 6-2 (not incorporating convective heat transfer)

\[ T_{j+1} = T_j + \frac{2\Delta t}{\rho_c \Delta y^2} \left[ (\varepsilon r \Delta y ((T_j')^4 - (T_j)^4)) - \left( \frac{k_j^l - k_j^f}{2} \right)(T_j' - T_j) \right] \]  \hspace{1cm} (6-2)

Heat build-up on the exposed face including convective heat transfer alters equation 6-2 to:

\[ T_{j+1} = T_j + \frac{2\Delta t}{\rho_c \Delta y^2} \left[ (\varepsilon r \Delta y ((T_j')^4 - (T_j)^4)) + \Delta y h (T_j' - T_j) - \left( \frac{k_j^l - k_j^f}{2} \right)(T_j' - T_j) \right] \]  \hspace{1cm} (6-3)

where

- \( \Delta t \) Time step, here 1 second
- \( \Delta y \) Material layer, control cell thickness, here 5mm
- \( \varepsilon \) Stefan-Boltzmann constant (5.67x10^{-8} W/m^2K^4)
- \( r \) Resultant emissivity, here 0.8 (combined from gypsum board and furnace walls, lined with ceramic fibre insulation blanket)
- \( h \) Convective heat transfer coefficient (W/m^2K)

Heat conduction inside the face material layer:

\[ T_{j+1} = T_j + \frac{\Delta t}{\rho_c \Delta y^2} \left[ \left( \frac{k_j^l + k_j^f}{2} \right)(T_j' - T_j) - \left( \frac{k_j^l + k_j^f}{2} \right)(T_j' - T_j) \right] \]  \hspace{1cm} (6-4)
Heat build-up in board/core boundary layer

\[ T_{3,j+1} = T_{3,j} + \frac{\Delta t}{(\rho_{3,j}c_{3,j}\Delta y^2) + (\rho_{3,j}c_{3,j}\Delta y^2)} \left[ \left( \frac{k_{3,j} + k_{3,j}'}{2} \right)(T_{4,j} - T_{3,j}) - \left( \frac{k_{3,j} + k_{3,j}'}{2} \right)(T_{4,j} - T_{4,j}) \right] \]  (6-5)

Temperature inside core layer (not modelled here): Equation 6-5

\[ T_{4,j+1} = T_{4,j} + \frac{\Delta t}{\rho_{4,j}c_{4,j}\Delta y^2} \left[ \left( \frac{k_4'}{2} \right)(T_{4,j} - T_{4,j}) - \left( \frac{k_4'}{2} \right)(T_{4,j} - T_{4,j}) \right] \]  (6-6)

A) Factors influencing the heat transfer prediction

The successful modelling of heat transfer for a given cross section is dependent on the characteristic thermal material properties. These however are altering with increasing temperature due to changes within the materials. The temperature dependent material properties of the materials used within the panels investigated in this study are not known and would need careful determination, which was outside the scope of the current work. However, it is believed that with enhanced knowledge of the specific material properties the model presented can be used as a comparatively simple, efficient method for comparing and assessing the heat build-up within specific material combinations (see also Appendix IX). Here the knowledge of break down mechanisms of the materials is of paramount importance. The presented finite difference method will be used to model the temperature build-up at the interface of exposed board and core (location A). The temperature increase in subsequent layers within the unit should be completed when detailed material parameters are available. The temperature dependent material properties of the exposed Sasmox board have been approximated to those of glass reinforced gypsum board as given in Sultan et al. (2001) It is appreciated that this approximation can only be a rough indication of the actual material properties. A later more adapted approach will need to incorporate further material and system characteristics, such as

(i) the breakdown temperature of the foam
(ii) the thermal material properties of the different degradation phases of the foam
(iii) the burning of the foam, likely to contribute to the overall burning and heat build-up dynamics
(iv) an approximation of board pieces falling off from the panel.

The modelling attempt is a crude start and regarded as merely a parametric investigation into the influencing factors to the temperature build-up in that panel position and a verification of the assumption established through the test scheme.

It can be appreciated, from equations 6-2 to 6-6, that the board and core material properties are the most influencing parameters to the correct prediction of heat transfer within the panel (see also Appendix IX). These influencing material parameters also vary with the temperature and can be listed to

(i) \( k \) (W/mK) the thermal conductivity,
(ii) $c \ (J/kgK)$ the specific heat capacity and
(iii) $\rho \ (kg/m^3)$ the density of the material.

B) Parametric study of material properties influencing the temperature model

Figure 6-53 shows the measured and modelled temperature development behind the exposed veneer of a Sasmox-PIR intermediate-scale sample. Only the first 15 minutes of the temperature reaction are predicted as these were established to be the most influential with respect to dehydration and shrinkage damage within the board layer. Furthermore these first minutes are the most important with respect to the modelling purpose since the onset of sharp temperature increase behind the veneer was regarded as the onset of the panel's temperature reaction throughout the depth of the panel and also most influencing to the stiffness response of the units. In the prediction the convective heat transfer from the furnace to the exposed surface has been neglected since it was regarded as negligible in the intermediate-scale furnace test regime (Silcock and Shields, 2001). The temperature dependent material properties have been assumed identical to plasterboard and are shown in figures 6-51. Whilst it is certain that the density of the board material will change with increasing temperature, the density variation has not been included due to the lack of information. Although the transiently increasing density due to water dehydration has been noted as significant with respect to the stiffness performance of the panel its effect on the heat transfer is assumed negligible for this approximate, rough prediction. Figure 6-52 plots the temperature increase behind the exposed veneer as measured and the modelled temperature build-up assuming the board material characteristics change as described by Sultan (2001) and the foam properties remain at the values at the beginning of the test. The model has been based on time steps of 1 second and material layers of 5mm thickness. The temperature development behind the gypsum-based veneer is accurately predicted for approximately 5 minutes. From then on the predicted temperature is above the actually measured temperature levels encountered in the test and the onset of sharp temperature rise is premature. When examining the employed heat transfer model it can be derived that only an overestimation of material properties at the interface of board and core can cause the elevated temperature levels established by the model, since it is assumed that the core retains its original heat transfer characteristics throughout the heating period. This however has been proven unrealistic. The bench-scale test clearly indicated that the glue-line at the interface of exposed board and core underwent transformation upon heating and it was also observed that the alteration of foam properties extended beyond the zone of direct contact with the board. The foam alters to a tar-like, glutinous liquid and most certainly also changes it heat transfer properties listed above. The bench-scale tests also demonstrated that the transformation of the core could be reliably linked to certain test observations, such as sizzling noises and the appearance of a strong antiseptic smell before the onset of sharp temperature rise. Whilst the observations were reliable and repeatable there was no means in establishing the changed material properties of the foam for input to the theoretical model.
6.4.8 Summary of link of scales
A standard full-scale fire test does not establish the true adequacy of a building product as its measurements are restricted to providing a purely pass/fail criterion. The multi-scale testing approach was developed to overcome the shortcomings of the full-scale regime and is well suited to assisting the design of structural sandwich walls. With the developed three scale testing regime the principal design parameters affecting the fire resistance behaviour of structural sandwich walls could be established and based on the findings a predictive model was developed. The varying test scales were correlated and with respect to temperature performance the bench- and intermediate-scale tests accurately predicted the first stage and onset of second stage of the temperature reaction in the first layers of the wall. In the type of sandwich walls investigated, this time span was the most important, since it linked in with the onset of sharp stiffness loss. The influence of boundary restraint becomes increasingly influential with increasing test scale and conversion factors have been suggested. The values of these conversion factors will require further research and back-up testing. With the testing regime, changes to panel composition can be evaluated and depending on the type of change, testing can be superfluous. Four types of changes have been identified, each of which require a different set of testing or modelling. Some changes do not require any testing but can be assessed using the analytical model developed and presented in this work. Changes to board or core materials of sandwich units would only require minimal bench-scale testing, whilst changes to the jointing or principal changes in material types would need further testing in the intermediate-scale and a proving test in full-scale. Major changes in the principal materials require a new investigation and the proposed route, trialled in the thesis, is felt to provide the information required for establishing the fire reactions and ultimately performance of the units. The final aim would be the recognition of the methodology to establish “deemed to satisfy” wall criteria for sandwich structures. The presented analytical heat transfer model could further reduce the need for testing, but for the model to render representative heat transfer predictions, the various interacting decomposition stages of exposed board and core will need to be researched in detail (see also Appendix IX). The modified bench-scale test is deemed to be well suited for such an investigation and this has been discussed further in the recommendations for future work in Chapter 8.
Figure 6-41: Design of test samples in the smaller scales as related to the full-scale wall features
Figure 6-42: Temperature profile in structural sandwich panel in intermediate-scale test

Figure 6-43: Comparison of intermediate-scale and modified bench-scale temperature development at 5 to 15 minutes
Figure 6-44: Comparison of intermediate-scale and modified bench-scale test temperature development at 20 minutes

Figure 6-45: Prediction of intermediate-scale temperature development through modified bench-scale test at 25 to 30 minutes
Chapter 6 - Fire resistance of structural sandwich walls: Link of scales
Figure 6-47: Effect of boundary restraint on temperature development
Chapter 6: Fire resistance of structural sandwich walls: Link of scales

Figure 6-48: Conversion model accounting for the reduction in plasterboard boundary restraint between intermediate- and full-scale regime
Exposed board layer restraint conversion

\[ R = \frac{L_R}{A_R} \]

\[ L_R = (1125 \times 3) + (1125 \times 1.5 \times 3) + (1125 \times 2) \times 10^{-3} = 10.69\text{m} \]
\[ A_R = 1125 \times 1125 \times 10^{-6} = 1.26\text{m}^2 \]
\[ R = \frac{L_R}{A_R} = 8.48\text{ m}^{-1} \]

Since board and jointing are connected compositely in the panel through the core the restrained length is now taken over the entire area of the exposed board

\[ L_R = (3000 \times 3 \times 2) + (2400 \times 1.5 \times 2) + (2400 \times 6) \times 10^{-3} = 39.6\text{m} \]
\[ A_R = 2400 \times 3000 \times 10^{-6} = 7.2\text{m}^2 \]
\[ R = \frac{L_R}{A_R} = 5.5\text{ m}^{-1} \]

\[ R_{\text{mmInterm}} = \frac{R_{\text{mmFull-scale}}}{R_{\text{mmFull-scale}}} = 8.48\text{ m}^{-1} \div 5.5\text{ m}^{-1} = 1.54 \]

Figure 6-49: Conversion model accounting for the reduction in board boundary restraint between intermediate- and full-scale regime
Figure 6-50: Conversion factor accounting for different boundary restraint conditions

Figure 6-51: Speed of temperature reaction through the depth of the panel
Figure 6-52: Generalised heat transfer in layered sandwich walls
**Figure 6-52**: Model of temperature response behind exposed veneer with standard material properties

<table>
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<tr>
<th>Temperature</th>
<th>$k$ (W/mK)</th>
<th>$\rho$ (kg/m$^3$)</th>
<th>$c$ (J/kgK)</th>
<th>Board</th>
<th>Foam</th>
<th>Board</th>
<th>Foam</th>
<th>Board</th>
<th>Foam</th>
</tr>
</thead>
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<tr>
<td>0-70°C</td>
<td>0.25</td>
<td>0.03</td>
<td>1200</td>
<td>45</td>
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<td></td>
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</tr>
<tr>
<td>70-100°C</td>
<td>0.25</td>
<td>0.03</td>
<td>1200</td>
<td>45</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>&gt;100°C</td>
<td>Figure 6-52</td>
<td>0.03</td>
<td>1200</td>
<td>45</td>
<td></td>
<td></td>
<td></td>
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<td></td>
</tr>
</tbody>
</table>

* Taken from Drysdale, D., (1998)
Chapter 7  
Racking resistance

7.1 Introduction

This section analyses the racking behaviour of structural sandwich walls by relating racking test data from three different structural sandwich wall systems to an extensive background knowledge and data bank of results for timber frame walls. With respect to racking performance, sandwich walls are a derivative of timber frame construction and the general background to timber frame racking behaviour is reviewed to explore the similarities and differences in behaviour between both wall types. Furthermore the racking design of structural sandwich walls currently relies on the timber frame design procedures as outlined in BS EN 594 (15) and BS 5268 Section 6.1 (13) and more recently BS 5268 Section 6.2 (14) which widens the use to commercial and industrial situations. This is due to the similarities between both wall construction types and because loadbearing sandwich walls construction method with too small a current use to justify a dedicated design method. Whilst both wall systems have commonalities in their reaction to in plane loading, the distinctively different panel composition of structural sandwich walls does impact on the applicability of the timber frame design guidance. The use of BS 5268: 6.1 (13) for the interpretation of the test results for sandwich panels is likely to lead to inefficiencies in use and possibly some cases where performance will be over estimated. As the use of sandwich panels increases, dedicated test and design methods need to be prepared. The provision of a dedicated design method for sandwich panels will not be a simple procedure due to the variability in methods of interconnecting such panels and in connecting them to load application and load relieving components. This means that any variation in method is likely to be unique to the construction under consideration. Therefore this study assesses some of the most common panel systems to cover a range of design influencing features allowing the compilation of design guidance with respect to the use of BS 5268:6.1 (13) for structural sandwich wall systems.

The findings of the racking investigation assess the range of factors influencing the wall racking resistance of structural sandwich walls, as shown in drawing 7-1. The section discusses the influence of the various panel components, i.e. veneers, extra plasterboard may need to be considered separately, core, vertical and horizontal joints, internal units and fixings as well as the effect of the wall assembly, i.e. vertical loading, length and openings on the racking performance of structural sandwich walls.

The critical design factor in most structural sandwich walls is the provision of adequate fire resistance and this will govern the choice of the panel components. Whilst the racking design is secondary in importance, some of the panel components chosen to enhance the fire resistance of the walls can also be beneficial to the racking performance. This link between both disciplines is further explored and, based on the findings, sandwich wall
design can be optimised. The knowledge about the fact that some factors are of little importance to fire but rather critical to racking such as the base fixings can help the complementary design of walls, which can be adjusted to suit both loading/exposure scenarios.

Drawing 7-1: Material and wall factors affecting on the racking performance of structural sandwich walls

The objectives of the racking investigation were:

(i) to determine the similarities and differences in performance between sandwich panels and timber frame walls
(ii) to evaluate the current procedures whereby sandwich are tested and designed using BS EN 594 (15) and BS 5268 (13, 14)
(iii) to consider the importance of base and between panel connections
(iv) to outline a possible new design approaches suitable for sandwich panels
(v) to consider the added value of plasterboard structurally if included for fire resistance
(vi) to consider the effect of veneer linking studs to evaluate any benefit to racking resistance if needed for fire performance
(vii) to consider links between racking and fire resistance and areas of mutual influence
(viii) give background to racking resistance of timber frame walls
7.2 Timber frame racking behaviour and design

7.2.1 Material/system factors

Under racking loads the different wall components, i.e. frame and sheathing boards, of a timber frame wall have different primary movements. Whilst the frame tries to lozenge, the sheathing boards rotate. Since both wall components are linked through fixings, the interaction of the fixings with board and frame is the governing factor to the racking resistance of the panel. In summary the movement of the wall panel subjected to in-plane loads is determined by the

(i) frame (minimal effect of timber),
(ii) connection of frame to base,
(iii) sheathing and its fixing to frame.

The zone of weakness in the wall assembly is in the front (windward edge), where the rotational action of the racking force lifts the sheathing relative to the bottom rail, as shown in drawing 7-2. The tensile strength in the front area of the panel determines the uplift that can be sustained by the panel, which directly translates into racking deflection and racking resistance of the wall. Vertical loading enhances the racking resistance of the wall as the uplift in the front of the wall is reduced and all components move more independently. As a consequence the risk of premature failure is reduced as all parts are more highly stressed. The relative movement between the panels puts more strain on the fixings along the vertical joint, which allows the build up of higher stress levels throughout the panel.

Drawing 7-2: Racking behaviour of timber frame walls
7.2.1.1 Frame
As the front stud and the bottom rail of the frame are only connected through nails into the end grain of the stud, the frame joint is weak in tension and does not offer any restraint to the rotating, uplifting sheathing. The connection of rear stud and bottom rail is strong in compression, although there can be some crushing of the bottom rail due to high concentrated loads in the studs. Even though the crushing causes extra movement in the panel it increases the local density of the stud, which offers additional strength to the wall assembly. Whilst vertical loading will increase the compression stress in the rear of the wall, it reduces the uplift in the front and thereby the influence of the weak tension connections reduces in this zone. It generally increases the racking stiffness and allows higher loads to be reached before failure.

7.2.1.2 Connection of frame to base
Base fixings are normally required to transfer shear loads from the wall into the foundation. Especially when the wall is not vertically loaded the base fixings need to provide resistance to uplift as well as minimise the sliding of the wall so that the full racking potential of the assembly can be established. Standard bolted connections are used in testing, which strongly links the rail to the foundation and imitates the behaviour of the various proprietary methods of base fixings. Although the first bolt is close to the leading edge of the wall it is not right at the front of the panel, allowing for a small uplift movement in the rail as loads increase. Since the holding down bolts are inserted at regular intervals along the length of the wall significant distortion of the bottom rail is prevented and sliding minimised.

7.2.1.3 Sheathing
The racking performance of the wall assembly is governed by the shear resistance of the joint at the front of the wall. The critical zone is where the fixings attach the sheathing to the leading end of the bottom rail. As there is no external restriction to the rotation of the veneer, the movement of the wall is only prevented by

(i) the fixings to the frame,
(ii) direct contact between boards, which is avoided wherever possible.

A maximum racking load produces a differential movement of the board relative to the restrained bottom rail of at least 10-12mm, depending on the overall racking deflection. At this displacement, due to the edge distance of the nails, the board will have torn around the nail removing its contribution to panel resistance and leading to overall failure. Under vertical load the maximum vertical movement of the leading stud is reduced, although nail failure will have started at a deflection of 10mm as before. However, the final critical failure under vertical loading moves to the vertical joint of the two boards, in timber frame walls on the centre stud. Brittle boards will cause panel failure at very low racking deflection, whereas a ductile connection will allow racking deflection in excess of 50mm at failure with a maximum load plateau being maintained throughout the test. In testing the direct contact between boards, point (ii) only, concerns
the contact of two single sheets at the top of vertical joint but in practice this form of board contact is also present along horizontal joints. The code takes the lower bound approach and discards this form of load transfer.

The overriding influencing factor to racking performance of the wall is the general the failure mode of the fixings at the front of the panel, which is very much dependent on the strength of the sheathing relative to the fixing and frame. Brittle boards tolerate only little nail movement within the plane of the board and the nailing close to the edge of the board causes "v"-shaped edge break out as shown in drawing 7-3. With increasing deflection the board and each nail is progressively lifted from the bottom rail inducing board failure. The edge distance of the fixing governs the strength of the front joint. In brittle boards the edge distance should be as large as possible to increase the shear failure plane in the board and reduce the effect of board damage when driving the nail. Brittle boards are also susceptible to premature failure particularly when the fixing is overdriven, as this causes the board to locally shatter and overall reduces the uplift resistance of the board.

(a) Tensile failure, "v"-shaped edge break out common in brittle board, little damage to fixing
(b) Fixing pull out, common in ductile boards
(c) Splitting of bottom rail, common in strong, dense or thick boards

Drawing 7-3: Tensile failure modes at leading edge of panel

Ductility in boards prevents edge break out and the joint tends to fail by tearing action of the nail through its thickness (Drawing 7-3 (b)). Therefore ductile boards, which are strong in tension in the plane of the board such as OSB or plywood, can be fixed to the underlying construction close to their edges without reducing the contribution of the board to the racking performance of the wall. Depending on the bearing capacity of the board, the nail will either bend and pull out of the frame or the nail head will turn into the sheathing, pulling it away from the frame. Very strong and dense or thick boards prevent nail movement and failure is likely to be in the shear of the fixing or more likely in the splitting of the frame itself (Figure 7-3(c)).
The interaction between the sheathing and fixing at the front of the panel is an important factor in the racking design of the wall. The diameter of the fixing, its spacing and the thickness of the board all influence the racking deflection and each factor is covered in the design of the walls and modification factors are given in BS 5268: 6.1 (13). The design approach proposes that the racking resistance is proportional to the diameter of the fixing and thickness of the board, since an increase in both positively impacts on the nail bearing and reduces the edge failure effect. Although this in principal applicable to all board materials, the Code advises to use the conversion factor for the fixing diameter only for ductile, hence wood based boards (Category 1). Brittle boards such as plasterboard, often used in lightweight timber frame construction, should always be fixed using a minimum of 2.65mm nail and no enhancement is allowed for larger diameters. For all board types the Code limits changes to thickness of the board to ± 25%. Against common belief the number of fasteners is not directly proportional to racking performance of wall section. The governing influencing factor to the racking resistance is the end distance of the nailing at the joint between base rail and cladding in the front of the wall. The edge distance of the fixing should be as large as possible. Especially in brittle boards a minimum nail spacing should also be maintained in order to prevent a premature failure of the front board edge along a tensile crack, linking the fixings.

Timber frame walls are generally tested with sheathing on only one side of the frame. Test results have shown that a board on the second face of the timber frame does not add its full performance as if tested alone. If both board materials are of the same material additional sheathing on the second face of the timber frame adds only about 50% of the single sheathed wall.

7.2.2 Wall performance
7.2.2.1 Vertical load
Since vertical loading reduces the uplift at the windward end of the wall and causes the individual movement of the panel parts, the performance of the wall is enhanced. The more independent movement of the boards involves more of the fixings and thereby increases the resistance of the timber frame wall. The vertical load spreads the damage from the single zone of weakness in the bottom rail connection under zero vertical loading to include the vertical joints between panels. This re-distribution of loading enables much of the extra racking loading to be carried into the second of the 1.2m sheets. The higher the vertical load the larger the improvement in performance and at 5kN/stud the racking performance is improved by a factor of 1.77 when compared with the performance at 0kN/stud vertical load (Drawing 7-4). The improvement in performance with vertical load decreases with length. BS 5268: 6.1 (13) therefore provides a vertical load factor which is dependent on the length of the wall unit:

\[ K_{107} = 1 + \left[ (0.09F - 0.0015F^2) \times \left( \frac{2.4}{L} \right)^{0.4} \right] \]

where
$F$ is the uniformly distributed load in kN/m, limited to a maximum of 10.5kN/m, $L$ is the length of the wall in m.

**Figure:**

RR* (kN/m) vs. Vertical load (kN/stud)

- 2.4 x 2.4m panel
- 1.77 x Performance

**Performance**

Depending on category of sheathing and fixing

Vertical load (kN/stud)

RR= Racking resistance

**Drawing 7-4:** Improvement of racking performance in vertically loaded timber frame walls

### 7.2.2.2 Length

It is clear that the racking resistance will tend to a maximum value for longer panels, assuming the load to be carried uniformly into the wall panel. The length and vertical load performance of timber frame walls is linked since the improvement in performance with vertical load decreases with length. However, the influence of length on panel performance was initially assessed independent of vertical loading and the relationship between wall length and racking load was found to be non-linear. Therefore the Code adopted a length factor ($K_{105}$) subdivided in three parts:

1. For panel lengths $L \leq 2.4m \rightarrow K_{105} = \frac{L}{2.4}$
2. For panel lengths $L \geq 4.8m \rightarrow K_{105} = 1.32$
3. And for intermediate panel lengths $2.4 < L < 4.8 \rightarrow K_{105} = \frac{L}{2.4^{0.4}}$

The strength performance of the wall becomes increasingly critical the longer the panel and the higher the vertical loading of the wall. The make up of the wall has little effect on performance. Generally the use of small sheathing widths (~0.6m) should be avoided,
although the drop in racking performance is not critical to design. Wall assemblies should be constructed with as much wide board sheets as possible. The taping of board joints is seen to effectively enhance the width of the sheathing layers, especially in plasterboard clad walls, which benefits the racking performance of the wall.

7.2.2.3 Openings
Openings reduce the stiffness and strength of a timber frame shear wall. Window openings break the wall into narrow full height panels and smaller wall units above and below the opening increasing the ductility of the wall. A door opening is more severe as it effectively breaks up the wall into separate units of narrow widths. The effect is further aggravated if the opening is long and the connection of the lintol above the opening and the wall is weak. In timber frame walls openings can affect the diaphragm behaviour to such extent that bending in the frame can be observed. This is especially pronounced at low vertical loads.

An opening in the front of the panel, close to the load application causes a greater loss in performance than an opening situated in the trailing part of the wall. The Code does account for openings larger than 250mm in diameter in extremely simplistic format through a reduction factor based on the relation of aggregate wall to total wall area:

\[ K_{106} = (1 - 1.3p)^2 \quad p = \frac{A_o}{A_t} \]

where

- \( A_o \) is the total aggregate area of opening in the wall,
- \( A_t \) is the total area of the wall including openings.

Thereby the Code does not differentiate between location, size or form of the individual openings, nor their layout and the direction of load. It only limits the distance between openings and guides on the size of small openings, which are not considered to affect the racking resistance. The Code approach takes the onerous safer lower bound value by determining the ratio of the perforate wall performance against that of the same plain, non-perforated wall. In some cases a wall may be found to have a greater racking performance if it is designed in parts, especially when walls contain a considerable length of plain panel or a considerably large opening. In long panels it is generally more accurate to deduct the loss in performance due to the openings from the full length wall assembly, rather than adding up its short lengths parts. Since the Code factor accounts for openings in an extremely simplistic format both approaches, i.e. using the reduction factor and the summation of parts, are valid. In any case the openings should be properly framed and the walls should capable of transferring horizontal loads above and below the opening. The fact that the movements of the sheathing above and below the opening are markedly smaller than the frame deformations can be exploited for strengthening the wall, as long as sheathing is continued around the opening. A wall with an opening cut out to produce a "C" board shape are stronger than an opening formed by single, independently rotating board widths.
7.2.2.4 Height
The factor for the wall height adjustment suggested in the Code is based on simple analysis and is relatively untested. The application of the height factor is restricted to wall panels of 2.1m to 2.7m height and takes the form

\[ K_{104} = \frac{2.4}{H_{wp}} \]

where

\( H_{wp} \) is the wall panel height in m.

The approach is based on the assumption that the racking resistance of the wall panel is proportional to the couple resulting from the height of the wall and the horizontal force; an analogy to cantilevered beam design.

7.3 Performance of structural sandwich walls in racking tests
7.3.1 Overview all results
Table 7-1 overviews all the sandwich wall results and in Appendix VIII the detailed load deflection graphs for all panel tests are plotted. In the result table the wall configuration, horizontal bottom/ top rail and vertical connection methods are sketched. Whilst System 1 and 2 use screw fixings, System 3 uses glued as well as nailed connections, which is also indicated in the table. Systems 1 and 3 (see also Chapter 2) have been tested in standard size, i.e. 2.4 x 2.4m wall units, whereas System 2 has also been tested at varying lengths and opening configurations. All panels were tested at two loading conditions: 0 and the equivalent of 5kN/stud loads. Since System 2 (Chapter 2: Experimental system) was an experimental panel system and manufactured from prototype units the number of wall panels available for testing was restricted and no replicates could be tested. The panels tested at different lengths and opening configurations could only be failed in one vertical load condition, which was chosen to be the zero vertical load case. This was seen as the best compromise since all plain panel tests had shown the design of the panels to be governed by their ultimate strength performance rather than their stiffness. The fact that only a limited number of replicates were tested in certain configuration makes authorative comment on definite design figures difficult; nevertheless principal design issues are explored and are anticipated to be confirmed by confirmative, back-up testing.

7.3.2 Sandwich wall systems
The effect of board and core material, their glued bond, the inclusion of internal stud units as well as the bottom and top rail configuration and the effect of fixing method potentially have influence on the wall racking performance of sandwich wall construction. Whilst timber frame walls are built to standard dimensions with regards to stud/ rail dimensions, spacing of studs, the construction of vertical board joints, the use of fixing patterns and edge nailing distances, these factors may be non existent in sandwich wall construction or can vary considerably depending on the specific wall system. The three systems chosen
for this investigation are representative of systems available to house builders in the UK and vary in board and core materials, but also with respect to top and bottom rail connections, vertical jointing and fixing methods. Whilst System 1 and 2 are considered to be in frequent use in the European market, System 3 is the most common structural sandwich wall system in the US. In the timber oriented American house building market, wood based veneers are preferred whilst the European market favours cement and gypsum based building products. The investigation into the fire behaviour of the different wall systems has illustrated the benefit of using mineral based veneer layers in structural sandwich walls. Whilst the mineral based boards can be used without additional sacrificial lining, the propensity of wood based boards to ignite, spread flame and release heat always necessitates the use of a sacrificial plasterboard lining in dwelling construction.

7.3.2.1 Influence of horizontal rail
The importance of the bottom rail and its joint with the sheathing has been discussed for timber frame wall panels. In sandwich walls the bottom and top rail construction is also a major influencing factor to the racking resistance. The construction and type of the horizontal connection influences on the racking resistance of the sandwich wall in two respects

(i) rigidity of the bottom rail in preventing uplift,
(ii) the shape of rail (bottom and top connection) in restraining or allowing the rotation of the veneers of the wall.

(i) Bottom rail uplift
The rigidity of the bottom rail affects the uplift of the panel at the windward front end of the wall. The stiffer the bottom rail the smaller the uplift of the panel ahead of the first rail fixing reducing the overall deflection of the wall. The softer, more bendable the bottom rail the larger the uplift measured in the front of the panel and consequently the down throw at the rear end of the wall, as shown in figure 7-1. The veneer capping thin cold-formed steel u-channel employed as bottom rail construction in System I is particularly weak and bends markedly (with the propensity to buckle) ahead of the first holding down bolt. The marginally thicker GRP rail offers enhanced stiffness against uplift. The wooden bottom rail in the System 3 panels performs stiffest due to the increased rail thickness and the short span ahead of the first holding down bolt. The rigidity and the type of materials used as horizontal rails influence on the failure performance of the entire panel. In System I the in-plane deformation of the wall pulled the panels away from the channel and the screws either sheared the board or were twisted out of the board. Although a brittle board was used in these walls edge break out was not the predominant failure mechanism, which was related to the ductile bottom rail and its large deformations reducing the strain at the board edges. As the racking load rose an increasing number of fixings were affected and the bottom channel distorted. Later, similar damage was also noted at the top channels, especially in the area of the vertical joint between the panels. At higher loads, when many fixings had been damaged, the...
panels started to crush at the bottom channel in the trailing corner of the assembly and at the top channel in the leading corner of the trailing panel (at the vertical joint).

In System 2 the tension caused a failure either in the board due to its brittle nature and the short end distance of the fixings or through the screw heads to pulling through the board. Both methods showed the principal weakness to be in the board. The top rail was influential in the failure pattern of the System 2 walls, since the direct bearing of the veneers further resisted relative movement between the boards. At higher loads the compression stress on the board edges at the trailing end of the wall, caused the board to crush and expand, partly sliding over the lips of the bottom rail. Whilst the stiff bottom rail in System 3 lifted only marginally under the loading, the main area of weakness in the System 3 panels was in all cases in the glue line between the veneers at the bottom rail. This was apparent from cracking sounds and was caused by the relative rotation of the boards about the timber rails. In the case of narrow soleplated panels this was the only type of failure and occurred in both the tensile (uplift) and compressive (down throw) zones. Where the wide sole plate was used damage was noted at the trailing end of the units where the veneers crushed in their contact zone with the sole plate. At failure, damage was evident in both bottom rail and the glue line itself. Glue line failure normally occurred at the rear end of the panel in the compression zone. The bottom rail failure occurred at the leading end of the panel as result of tensile uplift. The failure in the nailed panels exhibited similarities with the glued wall tests. Initial damage was noted in the horizontal joint induced by rotational movement due to the racking load. Nails were pulled out mainly near the front and the rear end of the panel, allowing the panels to move up independently of the rail in the front, and down at the back of the panel. Core crushing was observed in the compressive zone at the trailing end.

(ii) Influence of the shape of the rail
The findings of the programme have shown that the design of the bottom and top rail impacts on the racking performance and more importantly on the vertical load reaction of structural sandwich walls. Drawing 7-5 sketches the bottom rail configuration tested in the programme. The three options were different in that rail design (a) and (b) allowed the direct bearing of the veneers, whereas design (c) offered no restraint to sheathing other than the fixings along the rail. In System 3, where the horizontal fixing member is internal, the sheathing members are free of any direct bearing top and bottom of the wall, which enhances their ability to rotate; this is a set-up similar to the traditional timber frame.

The free rotation of the veneers especially at the rear end of the wall, induces early and abrupt failure, which is aggravated under vertical loading. Figures 7-2 to 7-5 compare the racking performance of the different sandwich wall systems at the two vertical loading conditions. In the System 1 wall assemblies where the panel units lie within the veneer enclosing channel section, the failure is ductile and failure loads are held over large deflections, as seen in figure 7-2. System 2 becomes an intermediate case since the veneers are partially rested onto lips formed by the joint. The stiffness performance
(Figure 7-4) of the wall assemblies is not affected by the bottom rail design, since the maximum loading in the stiffness cycle does not cause the failure of the connection between rail and veneer.

In the stiffness cycle the racking deflection is dependent from the rotational restraint offered by the board and its fixing to the rail. The glued connections in System 3 in combination with the ductile OSB facing veneers performed superior to the systems clad with brittle boards and connected through mechanical fixing methods in System 1 and 2. The stiffness enhancing effect of glued connections can be appreciated in figure 7-3, where a System 3 assembly has been tested with mechanical nailed connections instead of glue. The improvement in performance between System 2 and System 1 is thought to be related to the rigidity of the bottom rail connection in System 2.

The strength performance (Figure 7-5) is governing the design of the walls. The systems clad with brittle boards (System 1 and 2) exhibit similar ultimate failure loads in both vertical loading conditions. Both systems profit from the vertical load and higher strength performances are encountered under 25kN top loading. System 3 performs superior to both systems when tested without vertical loading. This has been attributed to the use of glue in the joint between rail and panel. This is discussed in further detail under the influence of fixings section 7.3.2.5. Under vertical loading the trend observed in the stiffness and strength performance under zero vertical load is reversed. When vertical load is applied the panel system fails marginally earlier than in the zero vertical loading condition. This type of behaviour is related to the bottom rail detail and an extended test programme on System 3 allowed for the direct assessment of the influence of bottom rail design on performance. Figures 7-6 and 7-7 compare the performance of System 3 wall assemblies at both vertical load conditions depending on the bottom/top rail design. In the wide soleplate configuration the performance of the walls is enhanced. Here the rotation of the veneer and the crushing of core in the trailing end of the panel are reduced, which increases the racking performance. This effect can be further enhanced, when the core is
stiffened at the down throw zone of the panel at the rear, as in System 2 tests, where the male protruding foamed in steel joint resists the localized compression stress at the intersection of bottom rail and foam.

From the above comparison it is clear that the racking design of structural sandwich walls depends on the configuration of the walls with respect to the horizontal joint design. Two types of wall behaviour can be distinguished:

(i) Panel systems using horizontal connections which allow the free rotation of veneers (similar to timber frame). Whilst this wall type performance similar to timber frame at zero vertical load, in contrast to timber frame vertical loading is detrimental to performance.

(ii) Panel systems using wider rail connections, restricting the free rotation of veneers. Here the racking performance at zero vertical load is enhanced when compared to timber frame as the compression strong trailing comer and the restricted movement at the vertical joint reduce the uplift in the front of the wall. Vertical loading improves panel performance.

The main difference between the wall systems presented lies in the effect of vertical loading. Timber frame and type (ii) sandwich wall panels profit from vertical loading, as the vertical load reduces the tensile problem at the front of the wall without unduly adding to the compression stress in the down-throw zone of the wall at the rear. Structural sandwich walls of type (i) using narrow horizontal rails perform weaker when vertical loading is applied as the vertical load adds to the compression weakness of the panel.

7.3.2.2 Influence of board materials
As in timber frame walls the veneers of a sandwich wall are the main load resisting components in a shear wall and their fixings to the horizontal and vertical connectors are influential to the overall wall performance. In sandwich walls too, the board materials affect the performance with respect to their failure behaviour at the front and rear end of the wall. The board materials assessed within the programme could broadly be subdivided into two groups in accordance with the performance of boards in timber frame walls:

(i) brittle behaviour boards such as the cement based Pyrok used in System 1 and the gypsum-based Sasmox used in System 2,

(ii) ductile behaviour boards such as the OSB used in System 3.

The characteristic board failures for both types of board were identical to those witnessed in timber frame tests. However, the sandwich wall results also showed that seemingly advantageous board characteristics can be overridden by the bottom rail design, discussed earlier in section 7.3.2.1 and the influence of the connection type between rail and panel, section 7.3.2.5.

In sandwich walls clad with brittle boards the influence of tolerances in the joints can become a major issue especially in systems where an internal, recessed horizontal rail or vertical connection mechanism is used (see drawing 7-6(b)). If the construction is too
tight the likelihood of the board breaking along its length/ height is probable. This destroys the continuity in the wall and can have major impact on the walls’ racking performance. Furthermore the brittle boards are prone to edge failure (see drawing 7-6(a)), which is equally influential to the racking performance especially in the front and the back of the wall at zero vertical load, where the zone of weakness is concentrated.

![Diagram of jointing section inserted into recessed panel](image)

**Drawing 7-6:** Panel areas prone to damage when recessed jointing is used in conjunction with brittle boards

7.3.2.3 Influence of core material internal veneer links

For the in-plane loading case, the core is of less significance to the structural behaviour of the wall. The core material has almost no influence on the racking resistance, provided that it is stiff enough to prevent the faces from moving independently; a condition usually satisfied in structural sandwich panels. Internal veneer links do not impact the racking performance as shown in figure 7-2 to 7-5 where all wall systems with and without internal links are compared.

In panels assembled with narrow bottom rails, where the veneer are free to rotate and move, an compression strong end stud at the rear end of the wall enhances performance since it restricts the down throw of the panel and thereby reduces the uplift in the front of the wall and provides additional strength throughout the wall. Further improvement can be expected when internal units and bottom rail are connected directly so that the rotational movement is resisted not only by the board and its in plane shear strength but also by the internal units acting as a stiffening uplift reinforcement. The improvement would be especially felt in combination with brittle boards. In systems where core and therefore internal units are recessed to accommodate the bottom and top horizontal rails, the internal units would have to be rigidly fixed to the rails to enable an effective load path. Only then a positive impact on the racking performance can be expected.
7.3.2.4 Influence of vertical joint
The vertical joint between two panels is of less importance to panel performance when compared to the bottom joint influence, especially at zero vertical load. However the stiffer the joint between panels the better the racking resistance of the wall assembly. The differential movement between two single units should be as small as possible as this prevents the panels from rotating independently as separate units. This can be appreciated in the comparison of System 1 and 2, figures 7-2 and 7-3. Although System 1 uses the superior wide rail connection, the flexibility of the rail together with the weaker intermittent jointing at the vertical intersection contributes to the inferior stiffness and strength performance. Despite the fact that the hook system performs reasonably well in tension and compression, its performance when subjected to in-plane shear deformations of the wall is negligible. In System 1 the aggregately weak top/ bottom and vertical joints result in large deflections and consequently reduced stiffness and marginally lower strength performance. However, the flexibility of rail and vertical connection showed the System 1 panels to be the most ductile, holding 90% of the failure load over 30mm deflection. Vertical joint made of glued tongues, as in System 3, are advantageous as they connect the single panels rigidly, reducing differential movement between the single panels to a minimum. This rigid connection connects the two panels to one single large panel, which increases the racking performance of the wall. This trend in performance enhancement was also found in timber frame wall panels with tapered board joints. In glued sandwich panels this effect is exacerbated. This is also especially apparent in wall assemblies containing openings, since strong residual panel areas are created which form a long continuously connected wall panel.

7.3.2.5 Fixing method
The fixings connecting panel veneers and bottom rail have major influence on the panels’ strength and stiffness performance. This was apparent in the test series in System 3 (SIP), in figure 7-3, which is commonly built by gluing the panels at the intersections, along the bottom and top rail and the vertical joint. The glued connections perform well providing constant fixity to the ductile board material. As a consequence the System 3 panels exhibit high stiffness and strength values in the tests, however, due to the failure mechanisms with glues the overall failure of the units is abrupt and extensive damage in the glue line along the bottom rail is apparent. Glued connections are generally the most powerful fixing method, as shown before for the glued bottom rail and vertical joint connections. However as the long-term performance of such connections is still unproven the use of glue in structural building elements is penalized by the application of large safety factors. Although the use of glue in the connections enhanced the racking stiffness and strength of the sandwich wall units the failure of the walls was abrupt and sudden and aggravated by vertical load. This was especially evident when using a narrow soleplate since the additional compression stresses in the glue line due to the vertical loading reduced the racking capacity of wall.

Nail and screw fixings are more common in the building industry. A panel’s failure behaviour is more ductile when it is connected using mechanical fasteners and the effect
of vertical load is more closely related to timber frame construction. When the glued connections in System 3 were replaced by nails the strength and stiffness of the wall unit was reduced by 25%. Failure was more ductile and 90% of the maximum load was maintained over 30mm deflection. The narrow soleplate used in the nailed panel tests will have exacerbated the reduction in performance. In panel systems using brittle boards the influence of edge distance is marked. The closer the fixing to the edge of the board the less its resistance to in-plane shear and break out and strength performance is likely to be reduced, however stiffness should remain unaffected.

7.3.3 Length performance
The length performance of the sandwich walls is reviewed by comparing the plain panel test results only. Figures 7-10 and 7-11 plot the racking stiffness and strength performance of the sandwich walls with increasing wall length. Stiffness information is available for all three lengths but the more important strength values are incomplete. Strength has been shown to govern the design of the walls for the standard 2.4m test panel and relative values of strength and stiffness at 1.2m and 3.6m lengths would indicate that strength is likely to be the governing factor throughout.

The racking performance (in stiffness and strength) of sandwich walls increases with length and vertical loading as seen in figures 7-8 to 7-11. The walls exhibit an over proportional increase in racking resistance for walls longer than 2.4m, especially under vertical loading, which is conclusive with the performance of timber frame walls. However, the limited number of replicate tests makes authoritative comment on the influence of length on sandwich wall performance difficult and back-up testing would need to confirm these trends.

7.3.4 Opening performance
The inclusion of openings always reduced the overall loadbearing ability of the sandwich wall unit as shown in figures 7-12 to 7-17. The reduction in performance was correlated to the area of the opening in relation to the overall area of the wall. The performance drop between 24% and 30% opening was not as severe as between the 16% and 24%, which was thought to be related to the shape of opening in the 2.4m² panel, which was created by two “C” shaped panel halves rather than cut out of the full width panel as in the 30% or 16% opening. In the 2.4m long wall including the window opening, bending the in panel immediately above the bottom of the opening was noticeable. At increasing deflections the upper leading corner and the opposite lower trailing corner of the window opened up and both board and core were torn. The crack opened up with increasing deflection. However, the ultimate failure was induced by the failing of the front fixings at the bottom rail as before in the plain panels. In the 3.6m long walls the window and door opening was formed by panel pieces of varying heights, which formed lintel and parapet wall, arranged instead of the full central panel in the wall. The panel pieces were fixed to the vertical joint and horizontal rails as in the full wall panels. This was different for the 2.4m wall opening, where the aperture was produced by cutting out half of the opening from the leading and other half from the trailing panel, creating two C shaped panel
pieces. The vertical joint was discontinuous through the opening, but remained intact above and below the window, as did the connections to the horizontal rails. Due to the different construction of the opening in the 3.6m long wall assemblies the failures were slightly different and did not constitute of a tearing failure at the opening’s edges rather than a connection failure, as the board was sheared by the screws mainly in the top and bottom joint in the central area.

7.4 Design issues

7.4.1 Basic racking resistance (BRR) and material modification factors

7.4.1.1 Basic racking resistance of structural sandwich walls

The comparison of BRR performance of timber frame and structural sandwich walls is difficult since the employed board materials are either not listed in the design guidance or the sandwich wall system was not tested in a comparable configuration (such as the System 3- OSB panel with nails, which was only at 5kN/stud loading). However, assuming a factor of safety of 1.6, a structural sandwich wall similar to System 2 under 0kN/stud vertical would give a basic racking resistance of about 4.4kN/m. The Code recommends that a 1.6 factor of safety can be used if the employed board material is proven for use in terms of its suitability and durability as a sheathing material. In case of the non-standard gypsum- and cement-based board, the 1.6 factor of safety can be justified, as the boards are weatherproof and reliable and their use is proven as they are employed in semi structural locations such as soffit boards. As such, these board materials are recognized in the building industry and reasonable care and attention can be expected with respect to its fixings. A timber frame wall sheathed with the same category I board material at identical thickness on both sides of the frame would have a basic racking resistance of 2.8kN/m. The increased performance of the structural sandwich wall is for the most part affected by the inherent bottom rail configuration, which is discussed in greater detail in the next section. The performance of a mechanically (i.e. by nails or screws) fixed sandwich wall with a narrow bottom/ top rail arrangement is likely to behave similar to a timber frame wall. The fact that timber frame walls contain compression strong studs is thought to marginally increase their basic racking resistance when compared to the sandwich walls, especially when the walls are vertically loaded. In both timber frame and structural sandwich wall construction, the strength performance generally governs design.

Although plasterboard lining is regarded as partly structural when used as sheathing in timber frame wall assemblies, its contribution to the racking performance of structural sandwich walls in the racking test is negligible. Since the plasterboard is mounted onto battens on the panels’ exposed face for fire protection purposes it rotates with the panel without contributing significantly to the in plane resistance of the wall. This will certainly be different in the practical application of the walls where the plasterboard lining will be tightly aligned with the walls and restricted from rotating by the horizontal boundary constructions of floor and ceiling. Then the plasterboard will be optimally used in prolonging the fire resistance of the walls and simultaneously contributing to the racking performance of the wall assemblies.

Chapter 7- Racking resistance of structural sandwich walls 7-17
7.4.1.2 Material modification factors
The Code proposes a range of material modification factors by which the basic racking resistance of standard wall configuration can be adapted to represent a change

(i) in nail diameter,
(ii) in nail spacing,
(iii) in board thickness.

Since these variation factors address the interaction of board veneer and fixing at the main zone of weakness at the front of the panel, it is felt that the variation factors could safely be used to adapt the basic racking resistance of a structural sandwich wall. The reservation of the use of factors (i) and (ii) to ductile board materials would need to be similarly imposed on structural sandwich walls, although this is thought to be overly severe. Since the brittle board materials used in sandwich walls are thought to be performing as category I sheathings the enhancement factors should be equally applicable. As would the ±25% limitation on variations in board thickness. Since the type of loadbearing behaviour in structural sandwich walls explained in further detail in section 7.4.2.1 would be influential to the extent of improvement the accuracy of the Code design would benefit from a detailed investigation into these factors.

7.4.2 Wall modification factors
7.4.2.1 Vertical load factor
When analysing the findings of the racking test programme it was shown that the design of the bottom rail detail had major impact on the performance level of the wall, especially when the shear wall was vertically loaded. Here the governing factor was the width of the rail allowing the restriction free rotation of the veneers as opposed to the enclosing rail, where the veneers were restricted from moving independently of the rail. This was seen to enhance the basic racking resistance of the sandwich walls when compared to standard timber frame, where the veneers rotate without external restriction. Whilst the narrow rail configuration resembles the set-up encountered in standard timber frame panels, the detrimental effect of vertical load on the sandwich wall performance is very much in contrast to the enhancement in performance encountered in vertically loaded timber frame walls. In the wide, veneer enclosing rail design the performance of the shear wall enhances with vertical load but not to the same extent as encountered in timber frame as shown for stiffness and strength performance in figure 7-18 and 7-19. The timber frame design approach is governed by the strength behaviour of the shear wall, which seems to be similar applicable for structural sandwich walls. However, for both bottom/ top rail configurations the vertical load factor proposed by the Code for the design of timber frame walls (1.77) is not accurately predicting the performance of structural sandwich walls. Figure 7-19 shows that the vertical load performance of structural sandwich walls needs to be adapted depending on the bottom rail connection encountered in the wall. Whilst wide bottom railed walls can be designed using a modification factor, sandwich wall systems with narrow rail connections are not safe to be designed with an enhancement factor and need a new design approach.
In traditional timber frame the in-plane loading of the wall causes a stress at the interface of sheathing and bottom rail, as shown in drawing 7-7. As explained before the performance of the wall is improved by vertical loading since the uplift in the front of the wall and at the leading edge of the trailing sheathing is reduced. The improvement also stems from the strong compression stud in the rear of the wall, which enables much of the load to be transferred through to the rear end of the wall but also allows for the more independent movement of the wall panels. Thereby a larger number of fixings throughout the wall are stressed. Taking this form of behaviour as basic performance level and design background the behaviour of the different sandwich wall types can be discussed and design advice given.

**Drawing 7-7: Stress distribution in design governing interface of bottom rail and sheathing at 0 and 5kN/stud vertical loading**

**A) Sandwich wall system type I**

In classical sandwich wall the narrow bottom rail enables the veneers to rotate freely, similar to timber frame walls. Since the single panels generally react to the loading as one unit and the stress in the bottom rail veneer interface is symmetrical about the vertical joints as shown in drawing 7-8a. When vertical loading is applied (Drawing 7-8b) the compression stress in the rear end of the wall is increased since there are no compression strong elements in the unit to dampen the down throw of the wall. Consequently vertical loading increases the shear stress in the interface of sheathing and bottom rail, which
leads to premature failure. In this type of sandwich wall- bottom rail configuration the use of an enhancement factor is unsafe as illustrated in drawing 7-9, where the vertical load performance of timber frame walls is compared with the vertical load performance encountered in this type of sandwich walls. For these sandwich walls the design would need to be based on the shear resistance of the mechanical connectors at the rear of the wall. Then the shear as caused by the racking loading could be superimposed by the additional vertical load transported through the veneers.

**Drawing 7-8: Racking behaviour of sandwich wall type I-** (a) at 0kN and (b) at vertical loading equivalent to 5kN/stud
Drawing 7-9: Design approach for vertical load factor for sandwich walls of type I with narrow bottom rail design

B) Sandwich wall system type II

In sandwich walls with wide bottom rails and/ or stiffening internal studding the down throw in the back of the wall is reduced, see figure 7-9. Furthermore the wide rail enables the bearing of the veneers at the top of the vertical joint, which adds a second high compression area and further dampens the racking deformation in the veneer at the front of the wall. Both effects combined reduce the uplift in the front of the wall. This effect of reduced uplift in the front of the wall resembles the improvement encountered in timber frame walls with vertical loading. In order to enable the use of the current design approach for structural sandwich walls of type II a rotational restraint effect (see drawing 7-10 and 7-11) could be introduced, which enhances the basic racking resistance of the wall at zero vertical load, due to the additional high compression resistance points. This would also account for the reduced improvement in racking resistance under vertical loading, as the findings have shown that the enhancement in sandwich wall racking performance at vertical load was not in the same range as observed for timber frame as shown in drawing 7- 10b. Therefore this work suggests a design approach as outlined in drawing 7-11; the BRR at zero vertical load is enhanced to account for the reduced uplift encountered but the gain in performance due to vertical loading is reduced. The current vertical load factor is converted by introducing a “rotational restraint factor” at 0kN/stud loading. To account for the enhanced performance at zero vertical load the vertical factor is adjusted for sandwich walls, as shown in drawing 7-11. The exact magnitude of vertical load for the BRR conversion at zero vertical load needs to be examined in further detail. In the light of the current result, the vertical load factor for sandwich walls with
wide bottom rails should be about 1.4 (see figure 7-18). The stiffness vertical load conversion is similar for all panel systems, regardless of rail configuration at about 1.5.

**Sandwich panel Type II**

**Drawing 7-10:** Racking behaviour of sandwich wall type II- (a) at 0kN and (b) at vertical loading equivalent to 5kN/stud

**Drawing 7-11:** Design approach for sandwich wall of type II with wide bottom rail/ and or internal stud reducing down throw in the rear end of the wall
C) Sandwich wall system type III

The third type of sandwich wall systems has not been tested in the scope of the program and represents an extrapolated wall system, aligned with the wall requirements for fire resistance. The work on the fire behaviour of structural sandwich walls established that the internal studding was beneficial to the fire resistance of sandwich walls as they were found to enhance the loadbearing capacity of the walls. Although the sandwich wall systems incorporating these internal studding units have never accounted for the studding in their structural design, it could be shown that their inclusion enhanced performance by increasing the shear capacity of the core. Whilst the increase in shear resistance of the core was seen to not influence on the racking performance of the wall (7.3.2.3), the studding could nevertheless be of advantage to racking if the internal units could be effectively linked to the rails as shown in drawing 7-12. The studding would be most beneficial if located directly at the front of the panel, where its tight connection to the bottom rail could acts as a tension/shear reinforcement of the board and thereby further minimize the shear stress in the first fixings. Together with the compression improvement encountered in the back, by either the wide bottom rail or the abutting internal studding this type of wall would have superior performance at zero vertical load to all the systems described before. Although the BRR at zero vertical load would be markedly increased the benefit of vertical load would be less significant as indicated in drawing 7-13. Similar to the conversion applied before the timber frame design approach would be used to convert the BRR at zero vertical load and reduce the improvement in performance under vertical loading. This panel form could be balanced against the effect of narrow bottom rail design.

![Diagram of Sandwich panel Type III](image)

**Sandwich panel Type III**

Drawing 7-12: Influence of tension reinforcement: Racking behaviour of sandwich wall type II- (a) at 0kN and (b) at vertical loading equivalent to 5kN/stud
7.4.2.2 Length factor

A) No vertical load

The structural sandwich walls exhibited principally similar length effect as in timber frame walls. As in the timber frame, sandwich walls exhibit a reduced performance increase at wall lengths less than 2.4m, and over proportional increase at panel lengths more than 2.4m. The results of the plain sandwich wall tests at zero vertical load have been plotted in figure 7-20 removing the length parameter $L/2.4$ from the performance so that the y-axis is representative of $K_{105}$. It is seen that there is good agreement and that the Code underestimates the improvement for the 3.6m wall length. The underestimation in longer panels is related to the rotational veneer restraint (type II panels) and virtual vertical load effect (panel type III), which with further test results could be correlated to adjust the Code for use with sandwich walls. The current Code approach is thought to be safe for use with sandwich walls especially for wall assemblies longer than 2.4m. The minor differences encountered between timber frame and sandwich walls shorter than 2.4m would need further investigation since the current results are not numerous enough to propose a valid adjustment factor.

B) With vertical load

The fact that the more important strength performance of the wall panels under vertical loading could not be evaluated in the testing programme, hinders detailed examination of the length factor in combination with vertical loading. However, since the vertical load effect reduces with length in timber frame walls it is likely that the rotational restraint and

Drawing 7-13: Accounting for enhanced restraint in design
virtual vertical load factor found in sandwich wall reduces and becomes less predominant. The longer the wall assembly the smaller the differences in behaviour between both types of wall, timber frame and structural sandwich walls.

7.4.2.3 Opening factor
In figure 7-21 the Code prediction for the reduction in stiffness and strength performance of the perforated wall units are compared to the findings. The Code predicted the behaviour of sandwich walls with openings reasonably well. Apart from the stiffness performance of the wall unit including a 30% opening (3.6m wall with window opening) the Code predictions were reasonably accurate and safe. The more important governing strength performance was also predicted reasonably well by the current Code, which can be safely used for approximating the effect of openings in structural sandwich wall performance. Compared to the remaining opening configurations the performance of the unloaded 24% perforated wall was least well predicted by the Code. The reason for this anomaly is quite simple; the Code values are based on the simplest form of framing around an opening using rectangular sheets. For the opening tested this would include particularly weak 0.6 m wide sheathing boards. In the test panel the window has been cut into the boards such that they retain much of their original strength. Previous test work has shown such panels to be much stronger than predicted by the lower bound Code approach and this is confirmed by these tests (Griffiths, 1987). For both wall systems, timber frame and sandwich walls, the accuracy of the prediction would profit from a less simplistic approach to opening influence.

In summary if openings are built-up using rectangular sections, as was the case for the 3.6m walls, the $K_{106}$ modification factors are likely to be appropriate and relatively efficient in use. However, if openings are cut from panels reducing the need for short widths of board a more complex approach to openings will be needed to take full advantage of the improved performance.

7.4.2.4 Height factor
The height factor presented in the code is very limited in scope. The conversion presented in the Code for wall heights between 2.1m and 2.7m is thought to be equally valid for structural sandwich walls and safe in use. However both types of walls, timber frame as well as sandwich walls would profit from a more sophisticated approach as suggested by Enjily, Griffiths (1996), by adopting a shape factor summing the effect of height and length.

7.5 Summary
The racking behaviour of structural sandwich walls is affected by generally the same factors as timber frame walls. In both types of building systems the sheathing board type and thickness and its fixing to the underlying construction are the main influencing factors and determine the racking performance and in plane deflection of the wall. However, due to the different framing structure sandwich walls have a wider range of reactions under vertical loading. Whilst timber frame panels always benefit from vertical
loading structural sandwich walls can be weakened by vertical loading. The effect of vertical loading on structural sandwich walls is dependent from the bottom rail configuration of the wall, which throughout the work has been established as the overriding design issue. Two types of sandwich wall have been distinguished

(i) sandwich walls horizontally linked by rails with a bottom/top rail, which allows the free rotation of the veneers under in plane loading,

(ii) sandwich walls with a bottom rail configuration, which restricts the free rotation of the veneers.

Whilst vertical loading reduces the performance of the type (i) structural sandwich walls, the single panels interlinked by the wider bottom rail (type (ii)) profit from vertical load similar to timber frame.

Although sandwich walls with type (ii) bottom rails have similar performance improvement under vertical load the magnitude of improvement is not the same as in timber frame. This is also related to the rail design and is suggested to be included in the racking design through a “rotational restraint effect”, increasing the performance of the walls at zero vertical load and reducing the enhancing effect of vertical loading. The preliminary results presented in this chapter suggest the vertical load factor at 5kN/ stud vertical load, in timber frame 1.77, would need to be reduced to 1.4 for sandwich walls. This would need confirmative testing. For type (i) sandwich walls the vertical load factor must be omitted, since it is unsafe. For these walls a different design approach under vertical loading has been suggested. The new approach relates to the fact that vertical loading increases the shear loading at the intersection of sheathing the rail, which will need to be carried through the fixings in addition to the stresses induced by the racking load.

The internal studding placed in the walls for enhanced fire resistance could be used to mutually benefit the racking performance if the stud units could be rigidly connected to the bottom rail. In these walls the racking resistance at 0kN vertical load could be further enhanced since the board in the uplift zone of the wall in the front would be reinforced, achieving higher loads at lower deflections, similar to a “virtual vertical load effect”. In the test performance the plasterboard cladding cannot be used to enhance racking resistance of sandwich walls. This is due to the fact that the plasterboard is mounted onto battens and fixed to the wall surface thereby only rotating with the panel, without contributing to the strength of the wall. This is very different in the real wall assembly where the plasterboard lining would be restricted from rotating by the floor and ceiling and therefore contributes to the wall’s racking resistance. The detailed analysis of the fire and racking behaviour of the walls presented in this thesis has the potential to optimise sandwich wall design.

The effect of length, openings and height on sandwich wall performance have been confirmed similar to timber frame and the modification factors suggested in the Code are deemed to be safe for the use with structural sandwich wall assemblies. However, this will need to be confirmed through an enhanced test programme, confirming the trends
laid out in this work. Both type of wall systems, timber frame and sandwich construction, would profit from a more sophisticated approach to account for openings. The simplistic approach adopted in the current Code can underestimate the performance of walls when openings are cut out of full sized boards, forming a C shaped residual board area. The approach adopted for the height conversion is also very limited and as both wall systems are increasingly used in higher rise construction a more sophisticated and versatile design conversion would be of great impact.
### Table 7-1: System 1

<table>
<thead>
<tr>
<th>Overall number of panels at vertical load condition</th>
<th>Panel dimension (m) (width:height)</th>
<th>Configuration tested</th>
<th>Joints</th>
<th>Total Vertical Load (kN)</th>
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<tr>
<td></td>
<td></td>
<td></td>
<td>Top</td>
<td>Bottom</td>
</tr>
<tr>
<td>6 at 0/6 at 25</td>
<td>2.4x2.7 plain</td>
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<td>U-chan</td>
<td>Camlock</td>
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### Table 7-3: System 3

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<th>Overall number of panels at vertical load condition</th>
<th>Panel dimension (m) (width:height)</th>
<th>Configuration tested</th>
<th>Joints</th>
<th>Total Vertical Load (kN)</th>
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<td></td>
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<td>Bottom</td>
</tr>
<tr>
<td>3 at 0/3 at 25</td>
<td>2.4x2.4 plain</td>
<td>Glued Connections</td>
<td>Timber (narrow)</td>
<td>Biscuits</td>
</tr>
<tr>
<td>2 at 0/1 at 25</td>
<td>2.4x2.4 plain</td>
<td>Glued Connections</td>
<td>Timber (wide)</td>
<td>Biscuits</td>
</tr>
<tr>
<td>1 at 25</td>
<td>2.4x2.4 plain</td>
<td>Nailed Connections</td>
<td>Timber (narrow)</td>
<td>Biscuits</td>
</tr>
<tr>
<td>Panel No.</td>
<td>Panel dimension (m) (width x height)</td>
<td>Configuration tested</td>
<td>Joints</td>
<td>Total Vertical Load (kN)</td>
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<td>Racking resistance (kN/mm)</td>
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<td>GRP</td>
<td>Steel</td>
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<td>3.6 x 2.4 door</td>
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Figure 7-3: Comparison of racking performance for all systems (25kN Total Vertical Load)

Figure 7-4: Comparison of stiffness performance for the three systems
Figure 7-5: Comparison of strength performance for the three panel systems

Figure 7-6: Comparison of wall performance depending on soleplate (0kN V.L.) (System 3)
Figure 7-7: Comparison of wall performance depending on soleplate (25kN V.L.)(System 3)
System 2, Panel Nos. 1-0, 1-5, 2-0, 2-5
1.2m and 2.4m plain walls

Figure 7-8: Comparison of racking performance: 1.2m and 2.4m plain walls: Initial stiffness behaviour

Figure 7-9: Comparison of racking performance: 1.2m and 2.4m plain walls: Strength behaviour

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Figure 7-10: Length performance- stiffness behaviour

Figure 7-11: Length performance- strength behaviour
Figure 7-12: Comparison of racking performance: 2.4m walls plain and window: Initial stiffness behaviour at 0 kN vertical load

Figure 7-13: Comparison of racking performance: 2.4m walls plain and window: Initial stiffness behaviour at 25 kN vertical load

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Figure 7-14: Comparison of racking performance: 3.6m walls:
Initial stiffness behaviour at 0 kN vertical load

Figure 7-15: Comparison of racking performance: 3.6m walls:
Initial stiffness behaviour at 35 kN vertical load
Figure 7-16: Opening performance - stiffness behaviour

Figure 7-17: Opening performance - strength behaviour
Figure 7-18: Vertical load factor: Stiffness

Figure 7-19: Vertical load factor: Strength
Figure 7-20: Length Behaviour at zero vertical load

Figure 7-21: Comparison opening performance as predicted by Code and as measured in sandwich walls
Chapter 8

Summary and Conclusions

8.1 Introduction

The objectives of this project, listed in Chapter 1, have largely been achieved. The initial aim of the investigation was to provide a detailed study on the fire and racking behaviour of loadbearing sandwich panels through experimental testing. However, the evaluation of the fire reactions of the composite walls has taken up a greater proportion of the work for two reasons:

(i) the design of sandwich units is driven by their inherent fire behaviour since the extensive damage sustained by the walls in fire environments threatens the life safety of the building occupants,

(ii) the lack of adequate testing techniques to establish the factors influencing sandwich wall fire design necessitated the development of an experimental testing methodology to conduct a scientific parametric study.

The racking performance of sandwich wall elements is generally good as was known prior to the start of the project. The availability of a suitable testing method and the access to wide ranging expertise in the factors influencing the design of timber frame wall construction focussed the racking evaluation of the sandwich walls on the applicability of standard design guidance given in BS 5268: 6.1. Although both investigations have been pursued with separate objectives and independent of each other, the factors influencing wall performance in both loading and fire exposure conditions were seen to have commonalities. The two investigations combined enabled the assessment of panel components and reactions influential in the functioning of the structural sandwich wall units.

These different aspects in one investigation allowed for a more integrated study in the combined structural and fire behaviour of the composite units and represented an approach not reported by other researchers to date. The work involved in achieving such a wide ranging coverage of sandwich panel behaviour and the development of a new testing methodology to the examination of fire performance meant that it was not possible to undertake a more comprehensive fire study in full-scale and also curtailed wider ranging testing into the link established between the different testing scales. The key achievements of this research are the identification and development of a new fire resistance testing approach and its application to use in sandwich panels. The factors important to the modelling process in linking different scales of tests are developed and the fire behaviour linked to the structural degradation of the panel have been identified. Thereby this research brought together previous findings, compared them with the test evidence collected, demonstrated problematic areas and highlighted subjects in need of further investigation.
This Chapter concludes the work in two parts. The first part summarises the findings of the fire investigation and the second part reviews the conclusions related to the racking programme. Recommendations for future work and the original contributions of the work are presented at the end of the chapter.

8.2 Fire behaviour of structural sandwich walls

8.2.1 New fire resistance testing methodology

The fire resistance performance of sandwich walls has been evaluated through a three-scale testing regime. The fire testing methodology in three scales was developed to enable the parametric evaluation of design parameters influencing the fire behaviour of structural sandwich walls. The current assessment method, a full-scale fire resistance test, was found to be inadequate as it merely provides a pass/fail information without examining the true adequacy of a building element.

The developed testing regime consists of a bench-scale electrical heater test, an intermediate-scale furnace fire resistance test and a standard full-scale fire resistance test. The bench-scale Cone calorimeter test is traditionally a reaction-to-fire test and has been developed for this study to generate information about the temperature and failure reactions of structural sandwich walls. The traditional reaction-to-fire test measurement such as heat release rate, mass loss rate, smoke emissions and other auxiliary measurement have been dispensed for this investigation. The modified test employs a specimen of 200mm², while the test area remains at 100mm², as in the standard set-up of the test. The depth of the specimen is also increased from 50 mm to the entire panel depth. An irradiance level of 50 kW/m² was found adequate in modelling the temperature reactions of the walls in vertical fire resistance furnace tests to heating curves based on ISO 834. The bench-scale sample is instrumented with thermocouples in the 50mm area from the central irradiation point, as temperature was established to level off at measuring positions further to the edges of the heated area, especially for the temperatures measured behind the exposed veneer. The cheap bench-scale test contributed the material data for the larger scale vertical furnace tests and established the variability of material responses.

With the recommended changes to specimen size and depth, sandwich wall building products can be tested in the standard bench-scale Cone calorimeter, which will contribute valuable information about their contribution to fire hazard in fire compartments. Based on the findings of this work structural sandwich walls will be able to demonstrate their reaction-to-fire performance levels.

Whilst the full-scale fire resistance test remained in much of its original form, the intermediate-scale furnace tests was enhanced by an additional load/deflection measurement. The increase in deflection was correlated to the stiffness loss within the units and could be linked to the temperature distribution within the panel. The panels for both furnace-based tests have been heavily instrumented with temperature measurement
devices at critical panel locations. Although standard testing does not require this large number of measurements the outcome of the study advocates the more extensive use of temperature monitoring especially in multi-layered units.

The bench-scale test predicts the temperature development in the furnace exposed intermediate- and full-scale samples correctly for the first 20 minutes of exposure. In structural sandwich walls the failure behaviour in these minutes are the most decisive to overall performance. The size of the specimen and its horizontal orientation hinders the secondary board failure effects, so that the bench-scale test underestimates the temperature development for the remaining exposure time. Differing boundary restraint is also a major influencing factor with respect to the accuracy of prediction and the correlation of temperature reaction in the intermediate- and full-scale test. To account for differing boundary restraints in both tests, a restraint factor is suggested. The restraint factor applies to the plasterboard layer as well as exposed board layer and is seen to provide an accurate prediction of the temperature reaction, specifically behind the exposed veneer, in a typical full-scale wall. The restraint factor for the board layers in the various scales depends on the number on fixings and therefore indirectly also to the number of internal framing members to which the board layers can be attached. The more the exposed board layers are restrained to the partition, the better their shielding ability after their decomposition has started. The effectiveness of the fixing is dependent from the board material and its post-degradation strength and further test information would be of great value to further develop the influence of board type, fixing mechanism and failure pattern on the restraint factor. The scaling factors would need further back up testing to provide a robust working figure acceptable for a wider range of materials, covering both veneers and core. The modified bench-scale test regime is well suited to undertake such a detailed study as will be explained in further detail in the recommendations for future work.

The multi-scale test approach enables the failure signatures of the units to be explored, which allows for theoretical models to be adopted. The combination of information on temperature reaction, physical damage, stiffness loss and exposure time enables the theoretical approximation of failure times, hence the fire resistance rating. The correlation established between board failure and marked stiffness loss through the enhanced intermediate scale and modified bench-scale tests test has been vital to the investigation and the modelling approach. Work is still outstanding with respect to thermal deflections of the entire sandwich wall unit in the first minutes of the test, until the loss of the exposed veneer. Whilst the overall failure times can be well approximated, the magnitude of deflection before board failure is still to be determined theoretically. The deflection behaviour of the wall in these initial stages of fire exposure is related to temperature differential through the depth of the unit and relative expansion of the various layers but also the shift in neutral axis of the wall. It is believed that the testing methodology developed through this study can be employed to establish the relevant panel performance parameters, which will be used as input to predictive heat transfer model and also for FEM based fire resistance models. Especially the bench- and intermediate-scale test
regimes would be suited to investigate these effects further as it is felt that any further development into the computational determination of the fire resistance of structural sandwich walls will be based in the principal failure signatures found through this work.

The multi-scale testing programme and the developed behavioural fire resistance model have also been demonstrated to aid the design of sandwich walls. The suggested test scheme, comprising enhanced material testing at bench-scale and intermediate-scale, reduces the need for full-scale tests. Although the current full-scale test cannot be replaced with one single, altered test, it can be complemented by smaller scale testing to identify and better assess the performance and contribution of the individual wall components such that

(i) a much fuller understanding of the factors influencing the behaviour of the structural systems can be identified, so that small changes, such as the change of veneer thickness, can be related to overall fire performance,

(ii) the number of costly full-scale fire tests can be reduced and cheap reduced scale tests can be undertaken to establish the most advantageous material combinations, so that only one final, merely proving test is required to comply with building regulations and/or building authority requirements,

(iii) the influence of the severity of fire exposure on the performance of the wall can be examined and the design requirements for walls can be adjusted accordingly.

Depending on the type of change, testing can be reduced and sometimes even replaced completely by the proposed analytical approximation of failure times. Four categories of changes to wall composition have been identified and the developed three scale testing methodology helps evaluating the effect of the change on the loadbearing ability of structural sandwich walls in fire combining the information. The modelling of the heat build-up in the sandwich unit represents a further step towards the reduction of physical testing. A simple finite difference model shows promising results but needs to be re-evaluated as exact thermal material properties for the specific materials in their various degradation stages become available. The assumed material properties are only indicative and underline the need for adequate material properties.

8.2.2 General fire behaviour of sandwich walls

The fire response of sandwich walls was established through the novel testing methodology and was found to be subdivided into three stages. At the start of the exposure the unit heats up at a moderate rate, rising to approximately 100°C in about 15 minutes. This is followed by a stage of marked temperature gain at a rate of up to 100°C per minute, in the second stage and a reduced, levelling temperature response when the panel is destroyed and saturated with the heat. This temperature response is recurrent throughout the depth of the layered wall unit and triggered by the marked increase of temperature behind the exposed veneer. The onset of temperature reaction throughout the panels is mainly influenced by the degradation characteristics of the veneer facing material and to some extent by the veneer backing internal core material. The internal
core material predominantly influences the progression speed of increased temperature levels throughout the panel.

8.2.3 Board materials

The external board layer is the first panel component exposed to the fire conditions. Its failure mechanisms determine the fire resistance of the entire wall unit. The reaction of the outer wall layer to the fire is dependent on the type of board material used. The overriding failure reaction of mineral based boards is extensive cracking, which promotes the heat infiltration into the panel interior. The cracking is caused by the shrinkage of the board, which is initiated by the heat energy removing the free and combined water from the materials' crystal lattice. In the sandwich wall configuration the gypsum based boards perform better than cement based boards, probably due to the slower crack progression through the depth of the board. In both cases the cracking of the boards is not uniform and depends on the temperature distribution within the board layer. The cracking pattern is arbitrary and characterised by numerous single cracks, which in time join and form an extended damage area.

The thickness of the board is influential on the speed of through-depth cracking. Board surface finish, density and backing core material are also seen to affect the severity of board destruction. Increased thickness, density and extra finishing layers delay cracking, whereas highly insulating synthetic core backing materials accelerate the decomposition. The latter effect is related to two facts. Due to the highly insulating nature of the foam, the heat transported through the board layer is congested at the interface of veneer and core, hastening the temperature build-up in the board layer and thereby also accelerating its dehydration. The decomposition of the first core layers alters the structure of the core and also generates smoke/ toxic gases, both of which promote ignition and damage the board layer. The build-up of gases behind the board can create additional pressure, which accelerates through-depth cracking.

Wood based boards exhibit a distinctively different response to heat; they char and fissure. Minor cracking upon exposure is of less impact as the created char layer expands, closing off minor cracks and thereby dampening the heat input into the panel. However, with ongoing exposure the char layer is impaired by deep fissures, which expose the vulnerable core layer and trigger accelerated decomposition. The wood based boards represent a greater fire hazard prior to through-depth board failure since their surfaces ignite and spread flame. It cannot be used in dwelling application without sacrificial plasterboard. This is not the case with the mineral based boards, which do not ignite, nor release substantial heat in the first stages of the exposure.

With the heating and the failures in the exposed board layer, its material properties change. This affects both the heat build-up in subsequent panel layers and the loadbearing ability of the entire structure. The occurrence of through-depth cracks in mineral based boards is linked to a substantial loss in loadbearing capacity. This has been investigated through fire and structural testing. In structural sandwich walls the loss in veneer can
result in 80% loss in loadbearing ability. In panels clad with wood based boards the reduction in loadbearing ability is more gradual, due to the char formation. Although the loss of board in one panel area reduces the loadbearing ability of the wall to great extent, the results have shown that during fire exposure intact panel parts remote from the area of greatest damage can reduce the overall loss of capacity. This is especially the case when the load can be distributed over the length of the panel by means of a form of distribution beam included in the wall structure, for instance a floor plate.

Although the board becomes ineffective for loadbearing purposes after through-depth cracking has been completed, it can slow down the heat input into the panel interior. In the full-scale wall assembly the upper board areas are most severely affected. The effectiveness of the board in the post- dehydration phase in shielding the heat from the panel interior is dependent on the restraint provided to the board pieces. Internal links and the surrounding test frame enhance the restraint to the exposed board. An increase in restraint and fixings can enhance the shielding ability of the exposed boards. This would be more effective in mineral based board than in wood based boards. Boards with good post-shrinkage strength have improved shielding qualities since the fixings remain effective and reliably fasten the board to the restraint. In this study the gypsum-based board exhibited better post-shrinkage strength.

8.2.4 Core
The internal core reduces the heat build up throughout the panel. The more fire resistant the core, the later the onset of high temperatures within the panel interior and the lower the overall temperature levels throughout the wall. The fire resistance, hence insulation capacity, of the internal core layer is dependent on its decomposition characteristics.

For PUR/ PIR cores the investigation found that the degradation characteristics of the foam, when exposed to the heat behind a veneer layer, were different from the classical decomposition behaviour as described in the literature. The low permeability of the closed cell foam normally promotes a smouldering combustion mode, which progresses slowly and is characterised by extensive charring of the first core layers. When the foam core decomposes behind a veneer layer the combustion mode is altered and instead of the extensive charring the foam alters to a tarry liquid form, which is seen to break into flaming combustion once the veneer protecting it falls off the wall. The flaming combustion of the core contributes to the temperature build-up in the wall. Phenolic cores of higher density show improved charring characteristics and the created char is coherent and rigid and has the ability to restrain parts of the exposed board layer. Inert, inorganic core materials can further reduce the heat build-up in the sandwich wall. The examined mineral wool core sinters, without flaming and therefore does not contribute to the internal wall temperature levels. However, the decomposition of the glue line, needed to adhere the slabstock mineral wool core to the boards, affects on the temperature response of the exposed board layer, comparable to the PUR/PIR effect on the board. The decomposition of EPS cores was found to be the most rapid and damaging to panel performance. This core substrate is likely to melt and form flaming droplets as soon as
the board layer has failed. The severe and rapid decomposition of the EPS core caused the high temperature to proceed through the core in minutes. In sandwich walls the EPS core must not be exposed within the fire resistance period stipulated by the building regulations. Careful detailing of fire stops and panel joints must be ensured to eliminate hidden fire spread and reduce the risk to fire fighters.

The effectiveness of the core in sandwich walls is summarised by two main factors:

(i) its decomposition threshold temperature, i.e. the decomposition characteristics of the glue bond,

(ii) the amount, consistency and coherence of char generated.

The ideal core degrades at high onset temperatures and forms rigid char layers once the decomposition is started. Preferably the charred foam has the ability to provide some form of restraint to the dehydrated board pieces to reduce the amount of board delamination and enhance the boards post-dehydration shielding ability.

The decomposition characteristics of the foams impact on the design of fire stops. Foams with high charring rate require less stringent requirements and are able to protect less damaged panel areas more effectively. In foams with low threshold temperatures, board loss results in rapid flaming combustion and fire stops need to be designed carefully. The PUR/ PIR and especially Phenolic and Mineral wool cores, are likely to be effectively shielded with any fire stop measure. With EPS foams this is not guaranteed and fire stops need to be tested and confirmed in full-scale to ensure sufficient protection.

The loadbearing capacity reduces as the core softens and the charring ability, rigidity and coherence of the char affect the level of loadbearing ability retained in the damaged state of the wall. In loading perpendicular to the plane of the panel the effect of core degradation is especially felt whereas in the vertical loading case the mechanical properties of the foam have less impact. In walls the thermal properties and density of the char affect the temperature build-up and integrity performance of the ambient board, which is effectively protected through the foam layer. In walls with internal veneer linking units the formation of intumescent, temperature effective char can aggravate the deflection of the units as the protective char layer causes a large temperature differential between exposed and protected parts of the units, which results in increased thermal bowing. Both internal units and unexposed board are heated up more vigorously when the core exhibits flaming combustion. This is not avoidable and the presence of char is always an advantage to overall panel performance. Although charring is increasing the temperature differential in the internal units, it protects the stud unit for longer and thereby prolongs the time to the material degradation of the studding unit. This is especially important with respect to the decline in load resisting capacity.

8.2.5 Internal units
A minority of sandwich wall systems employs additional internal veneer linking units. The investigation evaluated the effect of internal studding on the fire/ temperature performance and also on the vertical load and bending capacity of the wall panels. The
study concentrated on the assessment of sandwich walls with mineral based boards and a synthetic core. The investigated wall systems included internal lightweight studding units folded from cold formed perforated steel sheets, tying the veneers to prevent the delamination of the exposed mineral based board in a room/ furnace fire. In a delamination type failure the fire exposed board layer peels from the core and bends towards the heat source, a failure mode commonly encountered in cladding sandwich panels. The findings of this study have established that board delamination is not an accurate representation of the failure behaviour in structural sandwich walls.

**Effect on temperature reaction on wall**

The effect of internal studs on the temperature reaction of sandwich walls depends on the stage of board degradation. The influence of internal studs is dependent on whether the exposed board layer is in the heating up or post-shrinkage stage. Internal studs do not impede or delay the shrinkage related through-depth cracking and therefore do not enhance the shielding ability of the board. Although wood based veneers exhibit different decomposition behaviour, when compared with mineral based boards, the inclusion of studs is similarly ineffective in the initial reaction stages of the wall. In the post shrinkage stage of the board degradation, internal studs are beneficial as they support and restrain the dehydrated and cracked board pieces and thereby slow down the heat exposure of the interior parts of the panel.

Structural wall systems using wood based boards would encounter similar benefits from internal links, once the exposed board has fissured. The number of fixings connecting board and stud unit are of importance to enhance the effectiveness of the restraint. Generally the higher the number of studs and fixings, the better the board restraint and the lower the temperature build-up in the wall unit in the post failure stage of the board. In central parts of walls the enhanced restraint provided through the internal link gradually reduces and in between studs no benefit can be expected. The improving effect of additional restraint along internal studs reduces for board pieces about 200mm away from the internal link. The holes within the studs, although essential for the manufacture, reduce the effectiveness of the internal studding for fire stopping purposes. Normally core degradation proceeds through the stud without delay.

**Effect on loadbearing ability of sandwich wall**

Although internal studding is only included in the sandwich wall to enhance the fire performance of the structure, the internal studding was also established to be beneficial to the loadbearing ability of the sandwich walls at ambient temperatures. In the structural testing programme, the internal units were found to enhance the shear capacity of the core and thereby improved the loadbearing capacity of the sandwich structure. The shear displacements were reduced and the stiffness and strength of the panel units increased. In order to fully exploit the contribution of the internal veneer link, board core and stud unit need to be rigidly connected. The perforation of the web of the studs was found to providing this strong link between the various panel parts as the synthetic core can fully encased the internal units and differential movement between the various panel
components is minimal. In the panels where this composite link could be fully established the increase in mechanical fixings did not improve the stiffness or strength of the units and only affected on the ductility of the failure. In panels filled with non-adhesive cores this form of composite compound with internal units is not likely. Glue is generally only applied to bond the face veneers and the core and additional internal links have only minimal effect on the overall loadbearing ability of the panel.

In fire conditions the wall assembly also profits from the internal links. With the loss of the exposed veneer the loadbearing ability of the sandwich wall drastically reduces and the enhanced shear capacity of the studded core becomes ineffective. However, with the loss of the board the wall load is shifted onto the internal units, which become the main loadbearing member in the wall. Once the sandwich wall unit is impaired the temperature degradation of the internal units govern the fire resistance rating of the wall. The remainder of the sandwich unit influences the full height vertical sections in two ways

(i) the ambient board prevents the buckling of the sections in their weak axis,
(ii) the core encases the units and thereby reduces their fire exposed perimeter of the stud.

Whilst the ambient board of the sandwich wall is likely to remain intact and strong for the entire exposure duration, the core material and its decomposition characteristics have a governing impact on the internal units. The heating of the internal units are linked to the core degradation and a charring, fire resistant core reduces the temperature build-up in the section. The temperature differential between the exposed and unexposed parts of the internal units aggravates the thermal bowing effect, which amplifies the crookedness of the wall and thereby increases the deflection of the wall due to second order effects and reduces loadbearing capacity.

A further study will need to determine the effect of the foam encasement on the durability of the studs in the ambient sandwich panel wall as random examinations of the internal wall cold-formed steel studs exhibited rust damage in the areas of screw fixings. The benefit of the internal cold-formed steel units in structural sandwich walls could be lessened if the self-adhesive foam, or any preparatory measures for the foaming process, degrade the steel section and thereby reduce its performance levels in the case of fire.

8.2.6 Horizontal and vertical jointing
Each sandwich wall system adopts different horizontal and vertical jointing techniques. The jointing options assessed in this study have been described in Chapter 2 and 4. In sandwich panel walls the joint has potential to enable the early penetration of heat into the panel interior thereby short-circuiting the board protection and involving the core prematurely. The joint sections are important to maintaining the integrity of the assembled wall and if the units are full height they can also be used for loadbearing purposes, once the exposed board is damaged. Internal joints are exposed to heat environment in mainly two situations
(i) at the start of the fire, through the gap created between the boards at panel junctions,
(ii) later stages of fire exposure, when one part of the wall is damaged and core degradation progresses horizontally behind the panel veneer.

Whilst the exposure of the joint through the board gap at panel junctions cannot be avoided, the second type of exposure can be lessened by a fire resistant core substrate and enhanced board restraint.

**Horizontal joints**

Two jointing alternatives were assessed in the test programme. A top hat section constructed from single, rectangular GRP components, glued at their interfaces, and placed into the panel behind the veneers and a cold-formed steel channel capping the wall ends. Both jointing options protected the panel interior, although the steel channel needed to be clad by additional plasterboard to perform similar to the GRP material. This was partly because the steel channel was outside the panel and was exposed to the direct heat of the furnace.

In the GRP section there was potential for premature failure of the section as heat caused the glue joint at the intersection of components to fail and the internal glass fibres to delaminate from the matrix. In practice the units will be single extrusions negating the first problem but it is also likely that the area of cross section will be significantly less and this could also affect fire performance. Once the Phenolic matrix has melted, the glass fibres remain without bond and the joint has no residual strength. At this stage the material would not be usable as a horizontal connector or loadbearing unit. In the panel configuration the top and bottom rails are protected from direct heat by the board material, which is well fixed to the joint section by screws and therefore unlikely to fall off during the fire. Direct heat exposure is very unlikely and the rest of the panel will have failed before severe decomposition of the joint would have taken place.

The steel rail u-channel did not disintegrate, but being an open section, was overall much less rigid and stiff and distorted during fire exposure. The greater the stiffness of the top horizontal rail the better its load spreading characteristics. In sandwich walls, where the central part of the assembly is likely to get damaged early in the test, this spreading of load to less damaged wall areas can enhance fire resistance.

**Vertical joints**

Two generic types of vertical joints were assessed within the programme

(i) contiguous joint
(ii) intermittent joint

The contiguous fit gave increased stability to the panel when compared with the intermittent vertical hook mechanism. The fixing (adequate spacing of the fixings is vital and should not be more than 250mm centres) of the board along the height of the contiguous joints prevents the opening and warping of the panel edges, reducing the
exposure of the internal core. The contiguous joint also restrains the board in the post-shrinkage stage, similar to the effect observed with the internal linking units. The heat build-up in the vertical jointing units is similar to the heat build-up in the internal linking units but marginally more severe since the board edges expose part of the joint. In joints manufactured from highly conductive materials, such as steel, the core material degrades remote from the area of direct and most severe exposure. Although the heat build-up is not severe enough to promote hidden fire spread beyond the direct joint area it impairs the bond between core and joint and increases temperature levels within the panel. Whilst the non-perforated contiguous joint can act as fire stop between panels, the intermittent joint does not provide any restriction for core damage to proceed to less involved panel areas.

The influence of the contiguous joint on the fire resistance of the wall is similar to the internal studding. Once the exposed veneer is impaired load proportionally shifts onto the internal members and the stiffness of the vertical jointing sections impacts on the loadbearing ability of the assembly. This fact can be used to reduce the number of internal board links. Two vertical contiguous jointing options were tested in the small-scale test programme. A faceted steel joint and a rectangular, hollow GRP section. The steel joint performed more reliably than the GRP section, which was considerably weakened by the open end at the panel head. Construction tolerances during manufacture were very low and combined with large assembly tolerances purposely built into the design, left the joints very vulnerable to the fire. The internal rails and vertical joints significantly add to the problem of design tolerances and also leave the veneer edge vulnerable to damage during transport and erection. Damage in this area has significant affect on fire performance.

8.2.7 Fire resistance of structural sandwich wall unit in full-scale fire test

The full-scale sandwich wall unit is most severely damaged in the central parts of the assembly, generally where the plasterboard protection has fallen from the wall earliest. The robustness of the plasterboard lining is of paramount importance to the fire resistance behaviour of sandwich wall units. Similar to the veneer effects established earlier, the plasterboard lining relies on restraint to remain cladding the panel after dehydration has been completed. Glass fibre reinforced plasterboard, often termed fire rated plasterboard, has improved protection times, which enhances the fire resistance of structural sandwich walls. Whilst the plasterboard effectively shields the wall from the furnace conditions, the increasing temperature levels heat the sandwich wall components behind the plasterboard lining. This preheating accelerates degradation times of the board and core layers once the plasterboard lining has fallen from the panel. The longer the plasterboard lining remains in place the faster the decomposition of the sandwich assembly once the sacrificial lining is removed.

The edge areas of the test wall are less severely heated due to the enhanced edge restraint of the plasterboard but also partly due to the test boundary conditions, in which ambient air is drawn into the furnace along the sides of the test frame due to the differing pressure conditions. The loading arrangements commonly adopted in full-scale fire resistance
testing allow for the test load to shift away from the damaged central parts of the wall to the less damaged wall areas at the sides, giving improved fire resistance ratings. Fire resistance models have been proposed for three types of sandwich walls

(i) "Classical" sandwich wall (no solid inserts)
(ii) "Classical" sandwich wall with solid edge/ corner infill sections
(iii) Modified sandwich wall, incorporating intermediate cold-formed internal studs

Whilst the fire resistance of type (i) walls is well approximated by establishing the time to the failure of the exposed board layer of the wall, the prediction of fire resistance for types (ii) and (iii) is governed by the degradation of the internal units. Sandwich wall fire behaviour is greatly affected by the full-scale testing regime, which allows a redistribution of loading to less damaged wall areas. This load spreading, facilitated by the test set-up, causes failure of type (ii) sandwich walls to be dominated by insulation and integrity performance as the much reduced charring rate of the timber studs results in high residual loadbearing capacity. In effect these types of sandwich walls are tested at zero vertical load, since a beam-post arrangement, rather than the wall unit itself, supports the vertical wall load. If a point loading scheme was adopted in full-scale fire resistance testing, the ultimate fire resistance of type (ii) walls could also be approximated by the failure time of the board, as for type (i). For internal timber studs placed closer to the panel centre the fire exposure will increase and depending on the dimension of the studding the stability of the wall can become the governing design factor.

For type (iii) panels with internal intermediate steel studding structural testing at ambient temperature has shown the shift of load to internal studding at the moment of veneer loss. This was supported by the similar the fire behaviour patterns of light frame cold-formed steel walls and partly damaged sandwich walls. The fire resistance prediction for these types of wall was therefore developed from two available models for lightweight steel walls. The simulation has shown that the fire resistance of sandwich walls cannot be modelled by one generic cold-formed stud within the assembly. The temperature/ time histories of all studs within the assembly need to be analysed. Whilst the central studs governed the failure of the wall unit, an edge stud best simulated the deflection signature of the wall. This is a further indicator for the load spreading beam function within the current fire resistance test. Edge studs are least affected by the heat, since board pieces are well restrained in these wall areas due to the board embedment in the test rig in addition to the internal linking. The ideal load transfer situation is when the internal units are fully bearing onto horizontal rails. This however cannot be ensured and tolerances are likely. In this case the vertical load needs to be transferred through the gap-spanning board pieces, bridging the load onto the internal units. The likelihood of board pieces remaining in place along joints is high and therefore this can be possible. However, the fire resistance of the wall assembly is then governed by a combination of stud and board performance and the weakest, most likely centrally located board link is likely to initiate overall failure. This should be prevented.
The magnitude of loading is influential to the fire resistance of the unit; the higher the loading the shorter the fire resistance of the wall. Higher loads can be balanced with increased plasterboard protection. The load path within the wall unit needs to be ensured, the weakest link decides on the failure characteristics. Gaps in wall composition between horizontal rail and vertical inserts can effect breakage and premature failure. Wall height is also influential but within the programme only two wall heights, 2.4 and 2.7 m, have been evaluated. The difference in performance between both heights was minimal but is anticipated to become more critical at greater height differences.

8.3 Racking

Structural sandwich walls behave under in plane loading in principle similar to timber frame. As in timber frame the performance of the tension/shear connection between bottom rail and veneer is governing the overall racking resistance of the wall and under racking load, the sandwich wall panel rotates about the base. The uplift of the panel at its connection with the base rail at the leading edge is critical and governing to design. In panels clad with brittle boards the principal weakness is in the board, as the tension causes the shear failure of board, especially when it is fixed at short edge distances. As vertical load is applied the uplift in the front of the wall is reduced delaying failure in the bottom rail. Despite these similarities sandwich walls can exhibit marked performance differences depending on their composition. The governing factor to the performance level of the wall is the design of the horizontal rail. Two generic sandwich wall types could be distinguished in their racking reactions

(i) Sandwich walls employing narrow horizontal rails, which allow the free rotation of the veneers,

(ii) Sandwich walls linking horizontally through a wide rail, which restricts the rotation of the veneers.

The restriction of the sandwich wall veneers was seen to be a major influencing factor to the performance, especially under vertical loading. Whilst the narrow rail configuration resembles the set-up encountered in standard timber frame panels, the detrimental effect of vertical load on the sandwich wall performance is very much in contrast to the enhancement in performance encountered in vertically loaded timber frame walls.

8.3.1 Influence of panel components on racking behaviour

Board layers

The cement and gypsum based board used in the wall panels are brittle boards, which benefit from long edges distances to the bottom and top rail. In elastic facing boards, such as the OSB boards used in one of the wall systems, the edge distance of less importance. The boards used in sandwich walls are likely to be equivalent to Category I sheathing material when used in a 10mm thickness. Comparison between the basic racking resistance of timber frame and sandwich walls is difficult since the employed board
materials are not listed in the current Code of Practice and bottom rail configuration can be the overriding issue.

However, the characteristic board failure observed for the various board materials is similar to the performance of boards on timber frame walls and due to these similarities the increase in board thickness and nail diameter proposed for timber frame sheathings is thought to have similar enhancing effect on the racking performance of structural sandwich walls. Since the cement- and gypsum based board materials tested are thought to be an equivalent of category I boards the enhancement factors should be equally applicable to them, although the brittle plasterboard sheathing is excluded from the enhancement factors in the Code.

Plasterboard sheathing installed to enhance the fire resistance of the sandwich wall units is not deemed to enhance the racking performance of structural sandwich walls in the standard test. As the plasterboard is hung from the wall units, they merely rotate with the walls. In practical applications this is very much different. Here the plasterboard is restrained from free rotation by the horizontal boundary construction of floor and ceiling and through the connection of plasterboard and sandwich wall assembly additional racking resistance is activated.

**Core**

The core material is not significant to wall racking particularly when the overall design of sandwich wall panels enables the direct bearing between the sheathings and the top and bottom rail. In wall systems where the veneers are not bearing onto the bottom rail the core in the rear end of the wall is compressed and enhanced compression properties or additional stiffener in this part of the wall are beneficial to overall performance. In any case the core needs to be stiff enough to prevent the faces from moving independently; a condition usually satisfied in structural sandwich panels.

In the vertical load and horizontal face load conditions the thickness of the core will have an effect on performance and increased thickness would be an advantage although it is most likely that overall depth will be determined by insulation and manufacturing parameters and not structural performance.

**Internal units**

Internal units are not of major influence to panel performance if they are not rigidly connected to the rail. When the mechanical link between internal unit and bottom rail is provided the internal units acts as a stiffening uplift reinforcement to the board; an improvement, which would be especially felt in combination with brittle boards. The internal units would be best placed in the front of the wall, where they could reduce the tension in the board layer and thereby delay the failure in the board. A stud located in the back of wall could be used similar to the timber frame arrangement, to reduce the down throw of the wall at the trailing end and thereby also directly reducing in the uplift in the front of the wall.
With respect to internal veneer linking units the fire and racking design of structural sandwich walls could be united and be used to mutual advantage. A wall including internal links reliably connected to the bottom rail would serve both issues and represents an optimisation of wall design. This type of wall arrangement could furthermore be advantageous as it guarantees the vertical continuity of the walls once the exposed board layer has been destroyed in the fire case.

**Vertical joint**

The stiffness of the vertical joint affects on the performance of the wall unit. The contiguous vertical joints perform well and their stiffness increase the racking performance of the wall units. The intermittent hook system does not provide substantial rigidity but as the single panels link in with the horizontal rails, the weakness in the vertical joint is balanced. Generally there is no requirement for accuracy in the length of the vertical sections from a structural point of view, except that they must not penetrate the recesses for the rails where they could prevent the top rails from bedding down, although there could be benefit from direct bearing and connection of the internal joint and the bottom rail, as concluded before for the internal units in the previous section. Wall assemblies with glued vertical joints perform superior to mechanically fixed vertical joints, since the single panels are rigidly bonded, reducing the differential movement between panels. Glued joints connect the single wall panels to one large wall assembly, which improves the racking performance considerably.

**Horizontal joint**

The horizontal connection between single panels and to the next level of construction is the overriding issue in the racking performance of structural sandwich walls. Both bottom and top rail design are important with respect to the veneer support it provides. Wide, veneer enclosing bottom and top rails allow direct bearing of the veneers at the rear end of the wall and at the vertical joint, both of which reduce the shear stress in the front fixings and further resists the relative movement between single panels. If board edges are free to rotate behaviour becomes very different to timber frame, and vertical load is detrimental to panel performance. Rail connections can range from capping channels, where the veneers cannot rotate independently, to inserted bottom rails where the veneers are free to rotate. The GRP section used in one of the system is an intermediate case.

The overall height of the rail section is important, especially when brittle sheathing are used. A reasonable thickness is needed in the sidewall to take the fixing. Recesses within the panels accommodating the horizontal joints can be a weakness as it leaves the veneers unprotected and prone to damage. This can be avoided during transportation but not during erection when the panel must be positioned and fixed to the bottom rail. Tolerance between the panels and the rails needs to be designed adequately as a too tight a fit increases the likelihood of damage. The robustness of the construction is of great importance, especially in the joint areas. Damage and too large tolerance gaps can impede both the fire and racking performance of the systems.
Fixings
In sandwich walls there are no standardized fixing patterns and a range of mechanical fasteners are used. The fixings need to be chosen so to not damage the sheathing layers. The increase in fixing diameter is thought to have a positive impact on racking resistance especially in combination with an increase in board thickness, since it enhances the bearing area of the fixing in the board. The spacing of the fixings is also of major importance. Whilst the number of fixings are not proportionally increasing the racking resistance as suggested in some simplified design approaches, the edge distance of the fixings are of major importance especially in combination with brittle boards. A minimum spacing should always be maintained to avoid the formation of tensile cracks linking the single fixing points and destroying the shear capacity of the board and thereby reducing racking resistance.

The use of glue as main fixing method is generally penalized by large safety factors due to the unproven long-term performance of the connections. Furthermore the glued panels exhibited sudden and abrupt shear failure in the glue line. In these panels vertical load has the potential to reduce performance especially in combination with narrow soleplates.

8.3.2 Wall performance
Test method. EN 594:
The test method is applicable to sandwich wall testing. Since the wall panels have no internal studding the loading points have to be decided. In this investigation the loading points have been maintained at 600mm centres. Attention needs to be given to the fact that the racking loading point needs to ensure the loading through the veneers.

Reduction method
• Vertical load
With respect to vertical load performance three types of sandwich wall configurations could be distinguished

(i) Sandwich walls using horizontal rail connections, which enable the free rotation of the veneers,
(ii) Sandwich walls with wide horizontal rails and/ or end stud dampen down throw of veneer,
(iii) Sandwich walls with internal stud units, which are rigidly fixed to horizontal rails, reliving uplift stress in board at the front and dampening down throw at the back of the wall.

The vertical load behaviour of structural sandwich wall panels is notably different to that of timber frame walls. The performance at low vertical loads is higher in comparison to that at high vertical loads. This means that when reducing the test data by the method suggested in BS 5268 Section 6.1 the test design racking resistance will be low compared with zero vertical load performance and means that the panels are under-designed at low vertical loads. The difference in racking behaviour have been suggested to be accounted
for a rotational restraint factor, for type (ii) walls and a virtual vertical load factor for type (iii) walls. Based on the findings of this study the vertical load adjustment for sandwich wall systems of type (ii) needs to be reduced from 1.77 in timber frame to 1.4. This factor is only applicable in walls where the veneers are restricted from rotation and must be verified through further testing. In walls where the veneers are free to rotate (Type (i)) the vertical load reduces the performance of the wall assembly and the use of an enhancement factor would be unsafe. In sandwich walls of type (iii) the enhancement factor at zero vertical load is further increased as the rotational restraint factor is supplemented by the virtual vertical load factor since the uplift in the panels is very much reduced due to the tensile reinforcement provided through the internal studding. This also means that the benefit of vertical load is further reduced.

Based on the current design guidance it might be generally better to ignore vertical load in sandwich wall design for low rise housing and use the tested design value at zero vertical load. Longer term it would be advisable to derive a suitable vertical load factor for the different types of sandwich panel for more efficient use. It is important to note that this factor will be closely linked to the design of these panels and that the current design approach is a sensible lower bound solution. If the bearing of the veneer on the rails is omitted a much worse situation could result which could significantly effect the panels ability to resist vertical load. Such a design case needs to be carefully avoided.

**Openings**

If the standard approach used in sheathed timber frames is adopted for sandwich walls whereby all sheathings are rectangular and a window has separate lintol and base sheathings, then the openings modification factor $K_{06}$ of BS 5268 Section 6.1 can be safely and efficiently used. Since testing was limited this needs to be verified.

There is an advantage with the sandwich panel system whereby openings can be cut into panels resulting in C or L shaped sheathings. These enable longer sheathing lengths and reduce the joints. Openings created this way will have less effect on panel performance. However, there is no Code guidance on such a form of construction and a significant test programme would need to be set up to take advantage of the increased stiffness and strength.

**Height**

The height conversion proposed by the Code of Practice is deemed applicable for sandwich wall assemblies. Both structural sandwich walls and timber frame wall design would profit from a more sophisticated adjustment factor, extrapolating performance for a wider range of height variations then currently covered.

**Length**

The racking performance of structural sandwich walls improves with increasing length. This is in accordance with timber frame wall behaviour and in both wall types the improvement is non-proportional. With increasing length the vertical load effect becomes
less predominant and the altered racking behaviour of certain types of structural sandwich walls is less influential. In the light of the restricted data set the length factor is considered adequate for use with sandwich walls. As before back-up testing is required to confirm the findings of this work.

8.4 Original work and contributions

The need to ensure the safe design of novel structural sandwich wall systems has led to the research described in this thesis. The novelty of these types of building systems in the European construction industry and the limited information about their performance characteristics has created the need for in-depth assessment. Maybe even more important was the fact that no adequate assessment methods were available to undertake this type of investigation. In developing a testing regime and assessing the factors influencing the safe design of the walls, especially in fire, this work has not only generated knowledge about the wall system itself, but also contributed a new testing approach, which has the potential to benefit a wider range of traditional, as well as, novel building products. To enable this, the work has

- developed a testing methodology linking three scales of fire testing and structural testing at ambient temperature, which will enable both researchers and manufacturers to isolate key factors to sandwich wall performance and investigate individual components in a full-scale wall construction. The method can also be used to facilitating the research into these individual components on smaller cheaper tests to predict the fire resistance performance of the structural sandwich wall partitions before verifying it in the full-scale furnace test.

- adopted the reaction-to-fire Cone calorimeter test to provide resistance-to-fire information so that the main material components in a structural system can be assessed and critically appraised in terms of their individual contributions and interaction with other materials. The possibility to test several replicates of materials and their combinations, examines the reliability of the fire reactions. As the test set-up only requires small test specimens, products still in development can be assessed cheaply and feedback on required changes can be given to manufacturers.

- developed and enhanced the intermediate scale test to include an insight into the likely structural performance of the sandwich wall panel and by better and more detailed monitoring to verify component contribution and determine the effect of discontinuities.

- critically assessed the current standard practice in fire resistance testing and identified the weakness in the full-sale test approach when used for examining structural sandwich wall system, also suggesting how improvements could be made to give a greater understanding of wall behaviour in monitoring the true adequacy of the building unit.

- established the critical failure stages of the wall units through complementary testing at fire and ambient conditions so that the effect of fire severity on performance, especially collapse times, can be better approximated, enhancing
the versatility of fire performance assessments of building elements. This is thought to become increasingly important in future when the current standard fire resistance exposure will be replaced by more realistic heating regimes.

The work has generated knowledge about

- the influence of material type and composition (for both board and core materials) on fire resistance performance of structural sandwich walls, also studying the effect of altered material properties on the temperature build-up in the walls and suggesting and adapting an iterative analytical technique for predicting the overall fire resistance of the walls.
- the importance of restraint to sacrificial plasterboard and exposed panel veneer to overall performance and the influencing factors to the restraint, including the boundary conditions of the perimeter of the wall, number of fixings and internal units and proposing a conversion model for the different scale furnace tests
- the influence of internal veneer linking units in the fire but also the ambient state of the wall, giving a critical appraisal of the effect of tolerances and connectivity in the wall section on overall performance in fire
- the function and influence of vertical and horizontal jointing in the wall panels, on both the fire racking performance allowing for the optimisation of structural sandwich design to the mutual benefit of fire and structural performance. Also examining the performance of different jointing materials and thereby guiding on suitable jointing materials and systems.
- the applicability of BS 5268: 6.1 for assessing the racking performance of structural sandwich wall assemblies also suggesting alterations to the current Code, especially with respect to vertical load performance, to work with structural sandwich walls

8.5 Recommendations for future investigations

These following areas are thought to warrant further investigation:

- Further development of multi-scale testing methodology by expanding board and core material groups assessed in intermediate-and bench-scale and create generally accessible database to allow the wider ranging performance comparison and optimising the assessment of new materials
- Use testing methodology, especially bench-scale regime, and knowledge gained from the current work to aid the development of suitable board and core materials and optimisation of material combinations
- Use bench-scale test to establish material properties of the degraded panel materials, especially foam and board. The modified bench-scale regime is well suited for this type of assessment as the panel can be damaged to varying degree and the materials subsequently subjected to further structural testing in the various degradation stages. This would be vital to the development of adequate heat transfer models for loadbearing sandwich wall structures, which could be
linked in with FEM fire resistance modelling as degraded material properties could then be assigned to temperature levels.

- Behaviour of various foams Phenolic, Mineral wool, EPS and PUR/PIR in the sandwich wall configuration, i.e. parametric study into the effect of glue line decomposition and the interconnection of board and core material reactions. This could be best facilitated with the modified bench-scale test using one type of board material and complementing visual observations of the failure stages with structural delamination tests. This could be done for a range of exposure times and therefore various glue line destruction stages.

- Modelling of structural fire performance, here especially the modelling of thermal expansion and movements prior to the full-depth board cracking. To achieve this composite sections with and without internal steel studs could be tested in the intermediate-scale regime supported by temperature differential investigations in bench-scale

- Further investigation into the restraint effect on the plasterboard layer, undertaking bench-scale testing on a range of fixing mechanisms, i.e. screws, dabs, nails and so forth. Combine bench-scale testing with structural testing as before and correlate failure in plasterboard layer with post-dehydration strength of board to nail/ screw pull out capacity and strength of dab connection

- The assessment of the durability of internal studding units in the sandwich foam enclosure by monitored damaging of the unit through accelerated aging and assessing the heat transfer and loadbearing capacities of the panel in bench-scale testing and structural testing at ambient temperature

- Use of findings from altered bench-scale set-up to pursue classical reaction-to-fire testing to establish heat release rate, toxic and smoke emissions and so forth, combining both disciplines to comprehensively assess fire hazard of structural sandwich wall construction

- Further racking testing to back-up and explore alterations and amendments to the current design figures in BS 5268:6.1 using the modification factors discussed in the work as starting point and replicating testing to enable factors with statistical consistency to be derived

- Development of a dedicated racking design approach for structural sandwich walls based on the three typified wall assemblies identified through this work
Appendix I

Thermocouple locations

Intermediate-scale tests 1 to 4 (January 2000)

Dimensions in mm

Veneer material-Cape 10 mm

interface "hot" veneer and core

on centre line of test panel

Cold side

interface cold face and core

on unexposed face

Fire side

Core materials

Phenolic, Mineral wool, PIR

80 mm

In panel 4:
P/ = 70 mm

10 mm

10 mm
Intermediate-scale tests 5 to 8 (June 2000)

Dimensions in mm

- Cold side:
  - Veneer material: Pyrok (8mm), Sasmox (10mm), Fels (10mm)
  - Flange of steel stud: studs @ 600 mm centres
  - Core material: Phenolic, PUR

- Fire side:
  - 8 or 10 mm

- Center line of test panel
  - C on stud/interface cold face and core
  - D on unexposed face
Intermediate-scale tests 9 to 10 (February 2001)

Dimensions in mm

Cold side:
- Panel on unexposed face
- Flange of steel stud

Fire side:
- Panel on centre line of test panel
- Panel on stud
- Interface cold face and core
- Interface "hot" veneer and core

Veneer material:
- Pyrok (8mm)
- Sasmox (10mm)

Core material: PIR

Studs @ 600 mm centres

8 or 10 mm
Intermediate scale tests 11-14 (June 2001)

Dimensions in mm

Veneer material
- Pyrok (8mm)
- Sasmox (10mm)

Interface: "hot" veneer and core

Fire side
- Vertical Steel/ Fiberline joint
- Core material PIR

Cold side
- Interface of cold face and core

Joints
- Equal

Veneer on centre line of test panel

Core material on unexposed face
Intermediate-scale tests 17 - 20 (July 2001)

Dimensions in mm

L.A. = Load application point

Veneer material
Pyrok (8mm)
Sasmox (10mm)

Vertical Steel/ Fiberline joint

Fire side
Plasterboard protection
15 mm/ 30 mm fixed to
15 x 45 mm timber battens

Core material
PIR

8 or 10 mm 8 or 10 mm
Intermediate-scale tests 15-16 (August 2001)

Steel top channel

Camlock ca. 100 x 100 mm

Steel studs in one test-panel

Dimensions in mm

Equal

Equal

Equal

Steel bottom channel

Veneer material

Pyrok (8mm)

interface "hot" r and core

on centre line panel

Cold side

Fire side

on unexposed face

Vertical Steel stud

Core material PUR

Camlock connection at centre
U-channels top and bottom

Dimensions in mm: 80 mm, 262.5 mm, 600 mm, 262.5 mm, 1125 mm, 131.5 mm, 300 mm, 300 mm, 131.5 mm

Steel bottom channel
Full-scale fire test (February 2002)
Appendix II  

Furnace details

In the test programme two furnaces were used to conduct the intermediate and full-scale fire tests. The full-scale fire test results on sandwich walls provided by the courtesy of Marshalls Panablok Ltd and Kingspan Insulation were in major parts undertaken at the same full-scale furnace. However a couple of tests have also been conducted at the full-scale fire test facility at Warrington Fire Research Labs. The latter furnace has not been presented here.

**Full-scale furnace at Building Test Centre/ East Leake**

The furnace at the Building Test Centre (British Gypsum) in East Leake is LPG (Liquefied Petroleum Gas\(^1\)) fired. The furnace is resistance controlled by a computer programme created by Dark Star Fire Test Systems Ltd. called “Fire”. In the full-scale fire test undertaken for this work the furnace was operated manually, with the computer programme only logging temperature and pressure data.

The furnace can be used for both vertical and horizontal tests. In the vertical orientation the furnace has the dimensions 3m x 3m x 1m and an active volume of 11.7m\(^3\). The interior of the furnace is lined with 1500-2000kg/m\(^3\) refractory concrete.

The furnace has a total of 26 burners, 13 each side. For tests in vertical orientation a total of 12 burners are used. In horizontal tests this is generally increased to total of 14 burners. The number of burners can be increased and decreased as they are individually operated depending on the type of test.

The maximum exhaust fan volume is about ~425m\(^3\)/min. However dampers are used to keep the pressure and temperature within the furnace at required levels during the tests.

When the furnace is used for horizontal building elements the furnace dimensions are 4 m x 3m x 1.450m with an active furnace volume of 17.4m\(^3\). The furnace can be modified for larger specimens of up to 5m height, although these tests are generally non-loadbearing.

---

\(^1\) Mixture of propane and butane (a by-product of the oil refinement industry)
Observation view ports

Steel frame (after test, most of test specimen removed)

Inside of furnace- showing Burn chamber lined with Refractory brick. Burners located in sidewalls and front wall.

Figure App.II-1: BTC full-scale fire facility with specimen clamped to furnace opening ready for testing

Rams for load application

Steel frame with installed test specimen closing off furnace opening

Figure App.II-2: BTC furnace after test. Test frame with partly destroyed specimen is craned away from burning chamber
Intermediate-scale furnace CERAM/ Stoke-on-Trent

The intermediate-scale furnace at CERAM, Stoke-on-Trent, is natural gas fired and has an active furnace volume of 1.89 m³. The test specimen, normally 1.125 m x 1.125 m, is installed within a test frame and clamped by a special screw mechanism to the 1.4 m x 1.35 m furnace opening. The internal of the furnace is lined with low-density ceramic fibre. A purpose written software package, named “Specview” is linked to the furnace, controlling the temperature curve and also logging the thermocouple temperature and pressure data. The furnace has a single burner with a 293 kWh capability. The flame is baffled to ensure an even distribution of heat. The exhaust duct flow rate is approximately 2 m³/sec. The furnace has been used in a variety of reduced size testing of vertical a range of separating elements, e.g., walls, partitions, penetration seals and short columns. The CERAM furnace has been used as a non-loadbearing exploratory test facility, offering customers the opportunity to rapidly test the insulation performance of prototype products prior to submitting a sample for full certification.

2 Primarily (70 to 90%) formed of methane. Generally a mixture of various hydrocarbon gases: Methane, ethane, propane and butane being the main components.
The furnace is enclosed in an airtight steel container to which hood exhaust is connected.

Figure App.II-3: CERAM furnace during test

Test chamber with exhaust duct

Inside of furnace, lined with low-density ceramic fibre

Moveable steel frame in which the test specimen is installed.

Figure App.II-4: CERAM furnace: Specimen is rolled into test chamber to be clamped to furnace opening
Appendix III

Ultimate buckling load of cold-formed steel studs

In contrary to hot-rolled steel sections, thin plated cold-formed steel units tend to locally buckle under compression. As a consequence these units generally do not reach their full strength, as based in the amount of material in the cross section. The greater the ration of width, $b$, to thickness of the element, $t$, the more likely the formation of buckles at lower loads. The full strength of a perfectly flat compressive cold-formed steel plate element supported on two longitudinal edges can be theoretically developed for a width-to-thickness, $b/t$, ratio of 40. In wider plates the central portions of the plate are unable to resist as much compressive stress as the stabilized outer areas, which results in an overall loss in compressive strength. BS 5950: Part 5 (29) accounts for this effect by reducing the cross-sectional area to an effective cross-sectional area using the term $K$, the local buckling coefficient.

Critical buckling load for stud units used in sandwich walls

In order to establish the critical buckling load for the cold-formed steel sections used as internal veneer linking units in the modified sandwich wall panels the method as described in BS 5950-5:1998 (29) was used. Throughout the test programme two types of internal stud units were used. In recent tests the $\Sigma$- shape stud unit and in the older test panels the I-shaped unit. The change in stud shape, from I to $\Sigma$, was undertaken as the I-shaped unit was no longer produced. The choice of the new shape tried to preserve the stiffness characteristics of the unit as much as possible. The major difference being, that the old I shape was manufactured from a multiply folded cold-steel sheet, which created double leafed flanges. The $\Sigma$- shape stud unit therefore had increased material thickness of 1mm, as opposed to 0.7mm in the old stud units. The foaming process by which the polymerising mixture is inserted between the two boards, necessitates the internal stud to be permeable to the foam mixture. The web of the studs is therefore in both units opened up by 40 x 75mm holes at 90mm centres to allow the foam to expand freely. In the finished panel foam, boards and stud form a tightly connected unit.
Section assumed for critical compression load

Section dimensions/ properties

<table>
<thead>
<tr>
<th>b1</th>
<th>b2</th>
<th>b2'</th>
<th>b3</th>
<th>t</th>
</tr>
</thead>
<tbody>
<tr>
<td>24 mm</td>
<td>70 mm</td>
<td>13 mm</td>
<td>24 mm</td>
<td>0.7 mm</td>
</tr>
</tbody>
</table>

\[ I_{\text{gross}} = 172111.1 \, \text{mm}^4 \]
\[ A_{\text{gross}} = 158.11 \, \text{mm}^2 \]

Material properties
(BS EN 10147)

\[ Y_s = f_y = 220 \, \text{N/mm}^2 \]
\[ f_c = 280 \, \text{N/mm}^2 \]

Effective widths of cold-formed steel plates

23.8mm
12.9mm
9.3mm

Single symmetrical section
BS 5950-5 Section 6.2.4

\[ P_c = 20.03 \, \text{kN} \]

Maximum width to thickness ratio complied
Minimum stiffener dimensions comply
Section 4.6
Splay of stiffener $< 20^\circ$
$\rightarrow$ Complied

Oval hole $40 \times 75$ mm
at $90$ mm centres

Section assumed for critical compression load

<table>
<thead>
<tr>
<th>stiffened</th>
<th>unstiffened edge</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
</tr>
<tr>
<td>unstiffened edge</td>
<td>stiffened</td>
</tr>
</tbody>
</table>

Section dimensions/ properties

<table>
<thead>
<tr>
<th>b1</th>
<th>b2</th>
<th>b2'</th>
<th>b3</th>
</tr>
</thead>
<tbody>
<tr>
<td>48</td>
<td>70</td>
<td>14</td>
<td>7</td>
</tr>
</tbody>
</table>

Effective breadth

- $19.8$ mm
- $19.8$ mm
- $12.5$ mm
- $7$ mm

Material properties

(EN 10147)

- $Y_s = \sigma_y = 220$ N/mm$^2$
- $f_C = 280$ N/mm$^2$

Effective widths of cold-formed steel plates

- $19.8$ mm
- $19.8$ mm
- $12.5$ mm
- $7$ mm

Single symmetrical section

BS 5950-5 Section 6.2.4

$P_c = 19.67$ kN

- $19.8$ mm
- $19.8$ mm
- $12.5$ mm
- $7$ mm

Maximum width to thickness ratio complied

Minimum stiffener dimensions comply
Appendix IX

Heat conduction theory

IX.1 Non-steady, transient heat conduction in semi-infinite solid
The theoretically derived heat reaction of a semi-infinite solid is often employed to solve practical problems. The semi-infinite solid extends to infinity in all but one direction and is characterised by a single surface (see drawing IX-1). The approach provides a useful idealisation for approximating the transient heat response of a finite solid under a range of heating conditions (radiation exposure shown in figure IX-1). This approximation of heat reactions in practical solids is particularly valid for the early portion of the transient heat regime, when the temperatures in the interior layers have yet to be influenced by the change in surface conditions. When the surface of a semi-infinite solid is subjected to a sudden change of conditions (on one side only), transient, one-dimensional conduction will occur within the solid.

\[ T(x,0) = T_i \]
\[ q_0^* = -k \frac{\partial T}{\partial x} \]

Figure IX-1: Semi-infinite solid subjected to constant heat flux

First principles define the transient, linear flow of heat (reduced to one dimension) to

\[ \frac{\partial^2 T}{\partial x^2} = \frac{1}{\alpha} \frac{\partial T}{\partial t} \]

where

\[ T \quad \text{Temperature (°C)} \]
\[ x \quad \text{Distance (in the x-direction) (m or mm)} \]
\[ t \quad \text{Time (seconds)} \]
\[ \alpha = \frac{k}{\rho c} \quad \text{Thermal diffusivity (m}^2/\text{s)} \]
\[ k \quad \text{Thermal conductivity (W/mK)} \]
\[ \rho \quad \text{Density (kg/m}^3) \]
Thermal capacity (at constant pressure) (J/kgK)

In the case of the semi-infinite solid, where the surface temperature is initially at $T_i$ and suddenly increased to $T_o$, three boundary conditions need to be evaluated:

(i) Boundary condition 1: $\theta = 0$ at $t=0$ for all $x$,
(ii) Boundary condition 2: $\theta = \theta_o$ at $x=0$ for $t=0$,
(iii) Boundary condition 3: $\theta = 0$ as $x \to \infty$ for all $t$.

With

$$\theta = T - T_i$$

and the above boundary conditions:

$$\frac{\partial \theta}{\partial \theta_o} = 1 - \text{erf} \frac{x}{2\sqrt{at}}, \quad (2)$$

where the error function is defined as:

$$\text{erfc} \eta = \frac{2}{\sqrt{\pi}} \int_0^\infty e^{-\eta^2} \, d\eta \quad (3)$$

This equation can be used to determine temperature profiles below the heated surface of a “slab” of thickness $L$, heated instantaneously on one face. Closed-form solutions have been obtained for the three most important heating surface conditions:

(i) constant surface temperature,
(ii) constant surface heat flux,
(iii) exposure of surface to a fluid characterized by its temperature and convection coefficient.

In the context of this work, case (ii) is applicable. Any heating regime causes the temperature within the semi-infinite solid to increase with time, as shown in figure IX-2. With time the temperature gradient within a semi-infinite solid decreases (also sketched in figure IX-3) as temperature levels increase throughout the depth of the heated object.

The closed-form solution for the constant surface heat flux exposure case has been established (Carslaw and Jaeger, 1959) so that the temperature-time-depth history within the semi-infinite solid can be determined to:

$$T(x, t) - T_i = \frac{2q''(\alpha t/\pi)^{1/2}}{k} \exp\left(-\frac{x^2}{4\alpha t}\right) - \frac{q''}{k} \text{erfc}\left(\frac{x}{2\sqrt{at}}\right) \quad (4)$$

In section 6.4 the iterative, numerical determination of the temperature profile with time throughout the depth of the exposed board layer has been presented.
\[ T(x,0) = T_i \]
\[ q^*_0 = -k \frac{\partial T}{\partial x} \]

Figure IX-2: Temperature build-up in radiatively heated semi-infinite solid with time

IX.2 Application to sandwich wall configuration heating in the first stage of exposure (i.e before onset of stage II behind exposed veneer)

Figure IX-3 sketches the heating conditions of a sandwich wall assembly in the first minutes of exposure. In these initial minutes of exposure the temperature profile developing throughout the exposed board layer is of special interest. Both, the furnace and cone environment subject the exposed board layer to mainly radiant heat flux, which for this general discussion can be assumed constant. This constant radiative flux heats the exposed surface of the board layer and causes a temperature gradient through the board thickness. With ongoing exposure the temperature levels increase throughout the entire layered wall section.

With respect to wall performance the temperature behaviour of the exposed board layer is of special interest and here analytical predictions could be efficiently used to examine its temperature-time reactions, thereby also allowing for theoretical analysis of influencing factors to wall performance. Of special interest is the speed at which the heat wave is transported through the material as this identifies the depth at which material is undergoing decomposition with great precision. The work has shown that prolonging this time span improves the fire resistance of the entire sandwich wall unit.
If the leading edge of the heat wave is defined by a temperature increase, arbitrarily set to 0.5% of the surface temperature (Drysdale, 1998), the progression of the heat wave in relation to time and depth within the exposed board layer can be determined. Then equation (2) can be solved so that the penetration depth of the heat wave (in mm) can be determined to:

$$\delta = 4\sqrt{a\alpha}$$ , (Drysdale, 1998)  
\hspace{1cm} (5)

Consequently

$$t = \frac{\delta^2}{4^2\alpha}$$
\hspace{1cm} (6)

derives the time until the heat wave reaches a defined material depth.

As shown in equations (5) and (6) and also earlier in Chapter 6: Section 6.4, the material properties of the exposed board layer are of vital importance in the first stage of exposure, namely \(\alpha\), i.e.

(i) density of the board layer (\(\rho\)),

(ii) thermal conductivity (\(k\)),

(iii) thermal capacity (\(c\)).

Whilst the (i) and (ii) are generally known board properties at ambient temperature, the thermal capacity of the board materials used in sandwich panels is not given in product
literature and is unlikely to have been established experimentally for the majority of board products available in the market place. All three factors are potentially influential to the overall performance of the board and figure IX-4 illustrates the delay in time to heat build-up dependent on the thermal diffusivity, \( \alpha \) (m\(^2\)/s), of the exposed board layer. Analysing equations (1.1) and (6) it can be derived that a delay in temperature build-up can be achieved by

(i) increasing the board thickness,
(ii) reducing the thermal conductivity of the board layer,
(iii) increasing the thermal capacity of the board material
(iv) increasing density of the board layer.

![Figure IX-4: Influence of thermal diffusivity on time to heat wave penetration](image)

As mentioned before in Chapter 6: Section 6.4 this theoretical background is only presented as a starting point and in time more adapted and refined models need to be developed to incorporate the effect of dehydration (decomposition in general for non-mineral based materials), shrinkage cracks, change in material properties and change in foam characteristics as the foam layer backing the exposed board decomposes. However, this is thought to warrant potential for further development and gives the possibility to model the first minutes of exposure and the reaction of the influential exposed board layer in more detail. Future work should address these issues and explore possibilities of analytical prediction in general, since the closed form solution can be used to directly establish the temperature build-up in any depth of the exposed board layer. Obviously this type of analysis is only valid for “thermally thick” behaviour and can only be employed for the first minutes of exposure. With adjustment factors for the change in material properties (see also figure IX-3) and enhanced temperature infiltration due to shrinkage cracks, the temperature analysis can be refined and improved.
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