Development of a Thermally-Assisted Piercing (TAP) Process for Introducing Holes into Thermoplastic Composites

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Abstract

Composite parts can be manufactured to near-net shape with minimum wastage of material; however, there is almost always a need for further machining. The most common post-manufacture machining operations for composite materials are to create holes for assembly. This thesis presents and discusses a thermally-assisted piercing process that can be used as a technique for introducing holes into thermoplastic composites.

The thermally-assisted piercing process heats up, and locally melts, thermoplastic composites to allow material to be displaced around a hole, rather than cutting them out from the structure. This investigation was concerned with how the variation of piercing process parameters (such as the size of the heated area, the temperature of the laminate prior to piercing and the geometry of the piercing spike) changed the material microstructure within carbon fibre / Polyetheretherketone (PEEK) laminates.

The variation of process parameters was found to significantly affect the formation of resin rich regions, voids and the fibre volume fraction in the material surrounding the hole. Mechanical testing (using open-hole tension, open-hole compression, plain-pin bearing and bolted bearing tests) showed that the microstructural features created during piercing were having significant influence over the resulting mechanical performance of specimens.

By optimising the process parameters strength improvements of up to 11% and 21% were found for pierced specimens when compared with drilled specimens for open-hole tension and compression loading, respectively. For plain-pin and bolted bearing tests, maximum strengths of 77% and 85%, respectively, were achieved when compared with drilled holes. Improvements in first failure force (by 10%) and the stress at 4% hole elongation (by 18%), however, were measured for the bolted bearing tests when compared to drilled specimens. The overall performance of pierced specimens in an industrially relevant application ultimately depends on the properties required for that specific scenario. The results within this thesis show that the piercing technique could be used as a direct replacement to drilling depending on this application.
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1 Introduction

1.1 Overview

1.1.1 Background

The drive for improved efficiency is currently of primary concern within many industries ranging from aerospace to power generation. This drive stems from a number of sources, but principally it is the realisation that natural resources are becoming depleted and governments are setting stringent goals for their more efficient consumption.

The aerospace industry is one of the key sectors experiencing increasing pressure to improve efficiency in both fuel consumption (driven by national and international emissions targets) and operating costs (driven by the financial requirement to make profit). These improvements are related since fuel costs represent the largest financial outgoing for airlines. For the automotive industry there are also strict external pressures on reducing emissions and driving down the costs of large volume production.

The most effective method of reducing the fuel consumption for vehicles is to reduce their weight. Composite materials have been used in increasing quantities across many industries to reduce the weight of structures and components due to their advantage in specific strength and stiffness over most metals.

Boeing has forecast that 34,000 new aircraft will be required over the next 20 years while Airbus estimated that 10,350 replacement aircraft will be needed in the same timescale (Reinforced Plastics, 2012). These forecasts represent a significant opportunity for composite materials after recent successful applications of large scale composite structures on aircraft (over 50% use of composites, by weight, on the 787 and A350 aircraft).
Composite parts can be manufactured to near-net-shape with minimum wastage of material; however, there is almost always a need for further machining. The most common post-manufacture machining operations for composite materials are to create holes for assembly. Producing holes amounts to approximately 90% of the aerospace industry’s requirements for composites machining (Aerospace Manufacturing, 2012). These holes are primarily used for joining applications, but they may also be used for acoustic damping (e.g. in gas turbine engine nacelles, Figure 1.1) or for transmission of fluids through structures (e.g. venting).

**Figure 1.1: Goodrich Aerostructures DynaRohr acoustic panel with perforations for noise attenuation (taken from Goodrich Aerostructures, 2004)**

Difficulties in accuracy and repeatability of the drilling process can lead to expensive part rejections. Airbus had to delay the entry-to-service date of the A350 XWB by three months because of difficulties in implementing the automated drilling process (Composites World, 2012a), which highlights the significant complexity of drilling composites.

In large composite structures, like those found on aircraft, that require hundreds of thousands of holes, there is a substantial cross-sectional area, and hence weight, increase required to recover the loss in strength due to machining holes. Increasing the weight of a car or aircraft produces a requirement for larger supporting structures, larger engines and ultimately increased fuel consumption. Airbus have previously presented a study that concluded that a
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one kilogram weight saving on a long haul aircraft and a short haul aircraft would save approximately €3500 and €1500 per year (respectively) on fuel consumption (Jessrang, 2012).

Research into alternative joining methods that do not require the machining of holes e.g. adhesive bonding or welding, seeks to overcome the need for drilling operations and to reduce the weight increase incurred when joining structures. There is, however, reluctance across industries using composites (especially in primary and crash absorption structures) to move towards large structural assemblies without mechanical fasteners. Progressive and predictable failure of bolted and riveted structures is currently outweighing the disadvantageous weight penalties and processing requirements. The need for dis-assembly and re-assembly during maintenance is also a key consideration.

However, despite the reluctance to move away from mechanical fastening, research into welding of Thermoplastic Composites (TPCs) is increasing (Ageorges et al., 2001). The ability to re-process thermoplastic composites can provide a significant advantage for their use when compared with Thermosetting Composites (TSCs). TPCs can be re-heated and shaped multiple times using similar techniques to those used for sheet metal working. This can allow short production times for components and hence promote efficient manufacture.

1.1.2 Thermoplastic Composites (TPCs)

To make a change from using TSCs (which are used extensively throughout various industries) to TPCs (which are only recently emerging into specific industries) there needs to be a significant advantage in terms of processing time, weight or material cost (Ruckert et al., 2002). Due to the extended use and research into TSCs, many of the manufacturing problems are either understood or have been identified for research. Conversely, many manufacturers will need to overcome new issues e.g. re-processing effects, if they wish to move towards TPCs (Ruckert et al., 2002).
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Some manufacturers within the aerospace industry have successfully moved towards thermoplastic composites for certain applications. The Airbus A340-600 has been fitted with TPC fixed leading edges (called the J-nose), pylon panels and flap ribs for a number of years due to the faster manufacturing processes possible with thermoplastics (Diaz and Rubio, 2003). In a more recent application, Agusta Westland announced a change from TSCs to TPCs for the horizontal tailplane of their AW169 helicopter (JEC, 2013). The new thermoplastic composite part makes use of the additional toughness provided by thermoplastics and innovative processing methods (co-consolidation of preforms) to provide a 15% weight reduction in comparison to the previous composite tailplane design, at an acceptable cost. With the current focus on increasing production efficiencies for single-aisle aircraft, it is expected that the short cycle times possible for TPCs will become ever more attractive.

Automotive manufacturers are also beginning to realise the potential for thermoplastics in rapid manufacturing of vehicle parts. With the goal to develop manufacturing processes that can allow less than one minute cycle times, TPCs are considered as one of the most favourable options (Leggett, 2013). Research into new technologies that can help overcome the barriers for their use is vital for the potential to be realised. This thesis is concerned with investigating a machining technique that is designed for use on TPCs and to overcome many of the problems when machining them to produce circular holes.

1.1.3 Machining of composite materials

There are a variety of methods used to machine composites and they all possess their respective advantages and disadvantages. Conventional drilling and abrasive water-jet cutting are the most commonly employed techniques, but other methods e.g. laser machining, ultrasonic machining and electrical discharge machining can all be used to produce holes in composite materials.
Conventional drilling relies on a cutting surface that removes small quantities of material (chips) from the parent part. The reinforcing fibres in composite materials abrade the cutting surface quickly and the efficiency of the tool reduces significantly. This can lead to poor hole tolerances or the introduction of damage. Recent developments in composite drill bit technology have included the use of hard, abrasion resistant, materials and changes in the fundamental cutting process. Abrading the surface, instead of cutting, using PolyCrystalline Diamond (PCD) tools provides high quality holes and helps to significantly reduce tool wear. The drill bits eventually need replacing, however, and tooling costs are considerably higher than conventional drill bits.

Abrasive water-jet cutting is another technique for machining holes in composite materials. It uses a high pressure mixture of water and abrasive particulates to abrade the composite material. The water-jet, however, eliminates the need for tooling and tool wear no longer becomes an issue when machining large quantities of holes. The abrasive water-jet cutting technique is commonly used in industry, but is limited by the finite size of parts that can be placed on the machine bed for cutting.

Water-jet cutting can also introduce significant levels of damage (e.g. delaminations) due to the high velocity impact of the water jet. This is especially relevant when initially producing holes in a laminate where no lead in (from an edge) can be used. The significance of the problem is such that many water-jet machines will have a small drill bit installed to drill an initial starting hole before the water-jet is used to cut the full size hole. This significantly increases process times and cost, and introduces the previously highlighted issues with conventional drilling of laminates.

Laser machining is a non-contact machining process that can be applied to composite materials. Some of the advantages of the process are similar to those for abrasive water-jet cutting: there is no tool wear, cutting heterogeneous materials is not an issue and it can be
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easily automated. The laser machining process, however, is not commonly applied in industry for a number of reasons. The cutting process results in a significant heat affected zone, which can considerably degrade the local laminate properties. Decomposition of the resin, under heating, can also generate hazardous fumes. This becomes an increasing issue for large machining operations and for thick laminates.

Ultrasonic machining is another process that can be applied to composite materials to create holes or cavities. The technique uses a water and abrasive particulate mixture (similarly to abrasive water-jet cutting) that is vibrated ultrasonically to erode the target material from the part. Abrasion through micro-chipping gives good hole tolerances and the abrasive mixture helps remove heat from the part to eliminate heating problems. An ultrasonic horn is oscillated to create the erosion process and provides, essentially, a tooling surface. The tooling surface of the horn is, therefore, susceptible to considerable wear and erosion. The micro-chipping that allows this technique to provide good hole tolerances is also a slow process. These issues prevent the large scale industrialisation of this machining technique in its current form.

Electrical discharge machining, also known as spark erosion, is a non-contact method of removing material. A high frequency potential difference is applied between an electrode and the work piece to create an arc and erode material through vaporisation. The technique can be applied to high performance materials that are typically difficult to machine (due to hardness, wear resistance or temperature resistance). It is a non-contact process so no cutting forces are imposed on the part and good machining tolerances can be achieved. The machining mechanism also relies on heat generation and creates a heat affected zone around the machining location. The thermal damage degrades the material properties and can lead to delaminations in composite laminates.

Despite the various processing advantages and disadvantages of the machining techniques described, the fundamental problem with all currently adopted techniques is that they are
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material removal processes. These material removal techniques were all originally designed for homogeneous material systems – not for heterogeneous materials, like composites.

1.1.4 Subtractive, additive and material displacement processes

Material removal, as a manufacturing philosophy, has existed for millennia. Taking large billets of material and cutting, grinding, polishing away material until the final part remains was and, as discussed above, still is common practice. Recent advances have seen the introduction of additive manufacturing techniques – where material is built up to create the exact dimensions of the required part in the first instance. This is now starting to become common practice for homogeneous materials, such as metals and polymers, although it is difficult to apply to the manufacture of composite materials.

Production efficiency is becoming increasingly important in the manufacturing industry and additive manufacturing techniques are primarily limited by deposition rates. Refinements will be made within the additive manufacturing industry to increase the production efficiencies, but another trend has emerged recently for machining that involves a different philosophy – material displacement.

Material displacement techniques can be considered as a technology to be somewhere between additive and removal manufacturing techniques and are growing in popularity within the manufacturing industry, especially for composite material structures (Figure 1.2). The use of Sheet Moulding Compounds (SMCs) and compression or injection mouldable TPCs are increasing. Production efficiencies can considerably improve by taking semi-finished products and stamp-forming or injection moulding them (i.e. displacing material) into the final product form. As industries move towards high production rate manufacture, the largest cost savings are involved with production efficiencies. Until additive manufacturing techniques become mature enough to reach the required production rates, material displacement techniques could provide the required efficiency gains.
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Material displacement techniques are not limited to manufacturing complete structures and have been demonstrated as successful methods of forming holes in composite materials. Examples include moulded-in holes and Thermally-Assisted Piercing (TAP). Moulded-in holes make use of inserts that displace fibres before resin is infused and cured during manufacture. TAP is a technique that displaces both the fibre and resin together by forcing an insert (or spike) through the composite when the resin is heated or un-cured. These techniques are sympathetic to the fibres (which are critical to the performance of composite materials), unlike the material removal machining techniques described previously. The machining techniques currently adopted by industry cut and remove the reinforcing fibres from composite materials to leave the desired hole.

Moulded-in holes can be introduced during the manufacturing process of TSCs (prior to resin infusion). The process is labour intensive (to place individual inserts) so it is difficult to apply
in a cost efficient way in industry and very difficult for thermoplastics that are not easily resin infused (due to their high melt viscosity). TAP of TPCs can be applied at any stage during the manufacture or processing of TPCs (adding to the potential attractiveness of the technique for high volume production industries e.g. automotive and single aisle aircraft manufacturers).

1.1.5 Aim of the current work
TAP has been shown to give structural benefits when applied as an integrated technique with laminate manufacture, or as a post-manufacture machining process. The TAP process, however, is relatively new and has not been extensively studied. There are many process variables that will affect the resultant microstructure within laminates and, thus, the mechanical performance of specimens. The TAP technique is a method that has received little attention and the aim of this work is to develop a TAP process for introducing holes into TPCs and explore the consequences of using the technique on the mechanical behaviour of the material.

1.2 Structure of the Report
The next chapter (Chapter 2) of this thesis presents the findings of a literature review that first looked into processes similar or relevant to the TAP process. The findings of literature concerned with the mechanical performance of specimens machined using similar, material displacement, techniques is then presented. Based on this review the objectives for the present study are developed.

Chapter 3 discusses the development of the TAP rig used in this investigation and the final set-up used to create specimens. The chapter also includes some results of the initial experiments used to analyse and describe the piercing process as the spike travels through a composite material.
Chapter 4 shows the results of the material microstructural analysis for pierced specimens using both microscopy and X-ray analysis. The pierced specimens are compared with a benchmark material displacement technique (conventional drilling) and with respect to pierced specimens when TAP process parameters are varied.

Chapters 5 and 6 present the results of open-hole testing of specimens machined using the TAP process and by conventional drilling (for comparison). The mechanical performance is analysed with respect to the observed microstructure in Chapter 4. A full field surface strain measurement technique (described in Chapter 6) also provides information relating to the role of microstructure on the open-hole mechanical performance.

Chapter 7 describes the findings of tests relevant to joining applications. Pin bearing and bolted bearing test results for pierced specimens are compared with drilled specimens and interpreted in the light of the differences in microstructure.
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2 Literature Review

2.1 Introduction
This chapter presents the findings of previous research that was related to the TAP process and so enables the objectives of the project to be refined. The chapter is separated into two sections that discuss processes relevant to TAP and the mechanical performance of specimens notched using material displacement techniques and how these results compare with drilled specimens. The processes relevant to TAP were reviewed in an effort to determine the key process variables for analysis. Reviewing literature concerned with the mechanical performance of moulded hole and pierced specimens provided information about the expected performance of specimens within this project and the factors that need to be considered when analysing the microstructure and subsequent mechanical performance of notched specimens.

2.2 Review of Processes Relevant to Thermally-Assisted Piercing

2.2.1 Introduction
This section is divided into areas that are relevant to the TAP process. Material displacement techniques for composites were reviewed to understand the advantages, limitations and potential of the processes. Subsequent sections evaluate relevant literature (concerned with drilling, impact, and puncture of composites and textiles and also re-processing effects) to gain an understanding of process variables that are likely to influence the resultant damage to a composite after TAP.

2.2.2 Material Displacement Techniques
Material displacement techniques can be used as alternatives to material removal techniques for producing holes in composites. Moulding holes into a composite laminate during manufacture is one such technique that can be employed. The technique can be implemented at various stages of the manufacturing process depending on the matrix system chosen. For thermosetting matrices, inserts can be placed within the fibre structure before impregnation or before cure (pre-pregs) when the fibres are still free to be displaced around the insert. The
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matrix system can then be fully cured, and the insert removed, to leave a moulded hole that has continuous fibre paths running from one side to the other (Figure 2.1).

Keeping the fibres undamaged as they deform around a hole can lead to strength benefits since the load transfer paths are continuous along the fibres and around the hole. Moulded-in holes within thermosetting matrix composites were first investigated by Chang et al. (1987). The study found that specimens with moulded-in holes can provide up to a 38% increase in notched strength when compared with conventionally drilled specimens and several studies have confirmed the mechanical advantages of moulded-in holes since this initial research (further information is given in section 2.3).

Although there is evidence that moulded-in holes can provide improved strength performance when compared with conventionally drilled holes, the application is very labour intensive. The inserts must be accurately installed and remain in the desired location during manufacture of thermosetting composites. Once the matrix is cured, there is insufficient ductility to allow fibres to be displaced around the insert so no further modifications can be made without damaging the composite.

Conversely, thermoplastic matrix composites can be melted, to allow fibre displacement, and re-solidified to form holes around inserts. So for thermoplastic matrix systems, the inserts can

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Figure 2.1: Illustration of material removal and material displacement effects on continuous fibres
be placed within the fibre structure at any point in the manufacturing process, or any time during the service life of the composite. This allows holes to be formed during manufacture, if required, or immediately prior to assembly and even for repair whilst in service. Moulding-in holes, however, is not a technique that can be easily applied to thermoplastic composites. The high viscosity matrix is often difficult to use with resin infusion techniques, which are common for moulded-in hole production. High viscosity matrices will reduce the ability of fibres to move within them and, hence, lead to significant strain in the fibre where movement is restricted.

Studies by Hufenbach et al. (2010, 2011, 2012 and 2013) have shown that a thermally-assisted piercing technique can be applied to thermoplastic composites either during the consolidation phase of manufacture, or post manufacture. When applied during manufacture the composite was pierced during the consolidation stage (once the laminae were heated) as part of a single pressing and piercing stage. In addition, the investigation also looked into locally heating an area of a pre-consolidated composite (via electrical contact heaters) and piercing using a spike.

The initial studies by Hufenbach et al. (2010) identified that, under pin bearing loading, the maximum load before initial failure of the locally heated and notched specimens was increased by 40% when compared with conventionally drilled holes. This was improved when the piercing was integrated into the consolidation process. The maximum load before initial failure for the pierced holes, under pin bearing loading, increased by 55%, when compared with a conventionally drilled specimen; this load then reduces to a similar load carrying capability as the drilled holes (shown in the force-displacement plot under bearing load, Figure 2.2). The further increase in performance was gained because the whole laminate was in a heated, and hence ductile, state. This allowed flexibility in the fibres across the whole laminate so when they were displaced around a piercing spike there was limited, or no, additional strain placed on the fibres.
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In locally heated piercing experiments, the fibres were effectively clamped by the unheated matrix at the edge of the heated area. This increased fibre strain as they were displaced around a hole and their path lengths increased. The additional strain could have caused fibres to fail prematurely. The fact that they were displaced and not cut, however, still led to an improvement in strength.

In a more recently published study (Hufenbach et al., 2011) the increase in bearing failure load when heating the whole laminate was confirmed. The increase in failure load for pierced holes (when compared to drilled holes) in both of the previous cases, however, does not result directly in an enhanced bearing strength because the laminate thickness may change during piercing. The more recent research by Hufenbach et al. (2011) showed the differences in ultimate bearing load together with the corresponding laminate thickness after piercing (Figure 2.3 – where $s$ is the specimen thickness and $t_o$ is the additional thickness formed around the hole during the process, shown in Figure 2.4). A maximum increase in bearing strength of approximately 5% was obtained from the piercing technique employing a local heating zone before piercing (Hufenbach et al., 2011).
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Figure 2.3: Normalised ultimate loads and bearing strengths of, locally heated, pierced holes (taken from Hufenbach et al., 2011)

Although the integrated process of notching and consolidating the composite laminate was shown to provide increased strength properties, it still limits the technique to within the manufacturing or forming process (as with thermosetting composites). If the thermally-assisted piercing technique can be understood fully, and optimised, it could be applied to thermoplastic composites post manufacture or in-situ, i.e. in-service repair scenarios. This would provide a technique that can directly replace current drilling techniques and produce stronger holes. There are currently no studies in the literature that have investigated the process variables for thermally-assisted piercing in a systematic way. This research aims to fill this gap and, as a starting point, literature from drilling, puncture and impact of composites (and textile fabrics) and processing issues for thermoplastics have been assessed to help to identify the key parameters for the thermally-assisted piercing process.

2.2.3 Thrust Force and Delamination When Drilling
Drilling holes in composites provides a range of challenges in order to produce an accurate hole with minimal damage to the composite. Investigations into the drilling process and drill geometries, and how they can be designed to decrease damage and increase performance
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of a composite, could possibly provide transferable knowledge for a thermally-assisted piercing technique.

There are many studies into the damage introduced to laminates by drilling. Ramkumar et al. (2003), Abrao et al. (2006) and Zitoune and Collombet (2006) all agree that there are three main defects introduced by drilling:

- Delamination due to peel up at the hole entrance.
- Matrix degradation and fibre damage on the hole wall.
- Delamination due to push out of the bottom plies.

Damage assessments of machined holes in composites are commonly expressed in terms of these three defects and techniques for limiting damage usually focus on one or more of them. Delamination within composites is considered the primary form of damage that can result from drilling (Hocheng and Tsao, 2003). A method for calculating the critical thrust force at the onset of delamination crack propagation (due to push out of the bottom plies) was derived by Hocheng and Tsao (2003). The critical thrust force ($F_A$), the load below which delamination will not occur, can be calculated for various drill geometries, e.g. Equation 1 for a conventional twist drill, and is a function of the uncut thickness of the laminate and the material properties.

$$F_A = \pi \left[ \frac{8G_{IC}Eh^3}{3(1-\nu^2)} \right]^{\frac{1}{2}}$$

Equation 1

Where $G_{IC}$ = the critical crack propagation energy (strain energy release rate) per unit area in mode I, $E$ = Young's modulus, $\nu$ = Poisson's ratio and $h$ = thickness of uncut laminate.
Many authors have conducted research into reducing the thrust force due to drilling below the critical level and the associated drilling parameters. Studies by Enemuoh et al. (2001), Davim and Reis (2002) and Franke (2011) agree that the thrust force is also directly dependent upon the feed rate, even though feed rate does not appear in Equation 1, and that reducing the feed rate will, therefore, decrease the likelihood of delamination.

Intelligent drilling systems have been suggested whereby the feed rate would be reduced as the drill bit approaches the final plies of the laminate, where delamination frequently occurs (Dharan and Won, 1999). This would allow fast feed rates to be used for the majority of the drilling process, and would only require a reduction in feed rate for drilling the final, critical, portion. Despite highlighting that thrust force reductions decreased delamination, Kim et al. (2005) warned that low feed rates will still lead to fibre interactions with the drill bit that result in fibre pull out, which subsequently causes delamination.

Keeping the thrust force constant by using a backing plate during drilling was commented on by Hocheng and Tsao (2005) as a way to reduce delamination in the lower plies of a composite. The backing plate supported the last laminae, providing a resistive force as the drill neared exit of the composite and a reduction in the stresses in the remaining plies. The distributed support force provided by the backing plate eliminates the increasing bending stresses in the last plies before the drill emerges from the back face of the laminate, shown schematically in Figure 2.5 a) and b).
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![Diagram of drilling composite laminates: (a) Unsupported; (b) Supported](image)

**Figure 2.5: Drilling composite laminates a) unsupported; b) supported**

Supporting the back face of the work piece was also recommended by Capello (2003) who concluded that the delamination process and mechanisms were much more complex if the work piece was unsupported. Conversely, by limiting the movement of the work piece, i.e. by supporting the back face (and possibly clamping), a significant reduction in delamination is produced and control of the thrust force can be improved.

Studies into drill point geometry and its effect on delamination (Velayudham and Krishnamurthy, 2006), concluded that the drill point also affected the thrust force and, hence, delamination. The results suggested that if the drill point geometry was altered to reduce the thrust force, the feed rate could remain high and, as a result, the machining time would not need to be increased to reduce delamination damage.

Altering the tool geometry for drilling composites was also investigated by Maenpaa et al. (2007). They found that changing the tool geometry could redirect the thrust forces beneficially into the radial direction, reducing the axial (thrust) force on the laminate and decreasing the delamination of the bottom plies during drilling. For example, reducing the drill bit point angle increased the radial force and reduced the thrust force imparted by the drill, as shown in Figure
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2.6 (where the radial force vector increases as drill bit point angle and the axial, thrust, force vector decreases). This can be directly applied to the thermally-assisted piercing process where a radial force, to separate the fibres, is more beneficial than the axial force.

![Diagram of drill point geometry effect on imparted force vectors](image)

**Figure 2.6: Diagram of drill point geometry effect on imparted force vectors**

Other authors, Enemuoh et al. (2001) and Davim and Reis (2002), proposed that an optimised drilling technique for damage-free holes is to increase the (rotational) cutting speed of the drill in addition to reducing the feed rate (as previously identified). Although the lower feed rate will reduce delamination, the increase in cutting speed will lead to additional tool wear. Further studies into thrust force, tool geometry and delamination showed that a consequence of tool wear is an increase in the thrust force required to drill the hole in the laminate (Ramkumar et al., 2003, and Fernandes and Cook, 2005). As the tool wear increases, the efficiency of the drill reduces and consequently more work is required (a larger thrust force or cutting speed). It is this combination that pushes the need for expensive, hard wearing drill materials and coatings to be produced that allow damage-free drilling of composite structures.

For a thermally-assisted piercing process, the feed rate could be altered in an attempt to reduce the thrust force during the process. The literature also suggests that by altering the
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geometry of the spike, the thrust force could be reduced whilst maintaining a higher feed rate. The geometry may also be tailored to provide more beneficial radial forces to separate fibres, rather than axial (thrust) forces that distort fibres in the z-direction and initiate delaminations.

2.2.4 Puncture and Impact of Composites

When heating the composite laminate for thermally-assisted piercing, if the temperature of the matrix is not high enough, then puncture or impact damage may be experienced. Hosur et al. (2003) looked into the effect of heating epoxy composites on damage caused by punching, which was introduced using a split Hopkinson pressure bar technique. The findings showed an increase in temperature (to between approximately 50°C and 80°C, for a system with a matrix glass transition temperature of 114°C) resulted in a reduced level of delamination when compared with the unheated samples. The authors suggest that heating allowed the matrix to become more ductile, which reduced the level of resulting damage. The reduction in delamination is expected to be magnified within a thermoplastic resin system where the ductility can be increased significantly by raising the temperature of the composite.

When looking at impact damage Liu and Raju (2000) suggested that the peak impact force, impactor contact duration with the laminate and the absorbed energy during impact are among the factors that determine the impact characteristics of composite plates. If thermally-assisted piercing is undertaken under quasi-static conditions, however, then the contact duration should not be of significance, so the peak force and absorbed energy could provide information on the major contributors to the hole characteristics.

In the impact of composites under conditions where ductility of the matrix is such as to allow fibre movement, slippage of yarns over each other in woven fabric composites allow significant energy absorption (Termonia, 2004; Zeinstra et al., 2009). The latter investigation (Zeinstra et al., 2009) goes on to suggest that an increase in matrix temperature (for thermoplastic composites) will produce less resistance to puncture and, subsequently, a reduction of inter-
yarn friction and fibre strain. These conclusions were consistent with the study by Kim and Sham (2000), who suggested that increasing matrix ductility allows a smaller damage area and lower maximum load during impact. These studies all point towards the requirement for increased ductility in composites for a thermally-assisted piercing process. This would reduce the energy absorption by fibre interaction and, therefore, also reduce the impact resistance of the composite. Hence, by increasing the temperature of a composite, and thus its ductility, the puncture resistance and damage experienced will be reduced.

Batra et al. (2011) draw the conclusion that in low energy impact, the matrix properties become key to the damage mechanisms since matrix failures often occur before fibres fail. Thermally-assisted piercing of composite panels can be considered low energy impact, which further supports the previous conclusion that increased matrix ductility will affect the resultant damage when piercing.

2.2.5 Fabric and Textile Punching

Heating a thermoplastic composite for thermally-assisted piercing will reduce the viscosity of the matrix. This allows easier fibre movement within the laminate and the fibres are expected to start interacting with each other, and the piercing spike, as observed in the piercing of textiles, which do not contain a restrictive matrix material.

‘Windowing’ of Kevlar fabric in stab and puncture tests was commented on by Mayo et al. (2009) as a spike displaces fibres and yarns, as opposed to fracturing them. Since there is no matrix material to hinder the movement of fibres and yarns, they were displaced around the puncture spike (Figure 2.7). When the fibre displacement became too high, fibre fractures occurred (Figure 2.7). Many of the fibres, however, were shown to displace around the puncture spike. This windowing effect is expected to occur if the viscosity of the matrix within a composite is low enough to permit fibre movement of this magnitude. It is also suggested that energy absorption during the process is due to fibre on fibre friction and fibre on spike
friction (if no damage to the fibres occurs), which agrees with the previously cited study by Termonia (2004).

![Windowing of Kevlar woven fabric after spike puncture](image.png)

**Figure 2.7**: Windowing of Kevlar woven fabric after spike puncture (taken from Mayo et al., 2009)

In woven fabrics, lower friction yarns will not absorb as much energy as higher friction yarns (Duan et al., 2004). This is because with higher friction yarn fabrics the yarns will have greater interaction with other yarns and, hence, more yarns will become involved in the energy absorption process. This is consistent with the impact characteristics of woven fabric composites in studies by Sun et al. (2011). The study found that puncture damage for woven fabrics occurs in three stages: fabric tension, weft and warp yarn slippage, and yarn breakage and pull out. The study concludes that more interlacing points in the fabric will lead to more interactions between yarns, which help to improve puncture resistance. This suggests that using a uni-directional (UD) laminate would minimise the interlacing to zero and, hence, resistance to puncture is also minimised.

Subsequent work by Termonia (2005) on a model for fibre fabric resistance against needle puncture (needle defined as a spike with conical tip and basal angle of 70° on a 0.7 mm radius shaft) determined that puncture occurs in four different stages. The piercing stages were identified using force-displacement plots of a needle piercing a single ply of Kevlar fabric...
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(Figure 2.8) and visualised to show the progression of the spike at the different piercing stages (Figure 2.9):

- A steady increase in the force and needle displacement characterises contact pressure of the needle tip on a fibre (shown up to point ‘a’ in Figure 2.8).
- A sharp decrease in force is caused by the needle tip slipping into an inter-fibre region (points ‘b’ and ‘c’, Figure 2.8).
- Force increases again with displacement as the conical section of the needle interacts with the fabric (point ‘d’, Figure 2.8).
- Finally the force decreases steadily to the frictional force exerted by the fabric on the cylindrical needle section when the conical section exits the back of the fabric (points ‘e’ and ‘f’ in Figure 2.8).

![Figure 2.8: Calculated force-displacement plot for conical tip needle piercing a single ply of plain weave Kevlar fabric (taken from Termonia, 2005).](image)
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![Image](image.png)

**Figure 2.9**: Visualisation of the needle progress at corresponding piercing locations (a-f). Fabric deflections increased by a factor of 5 for visualisation (taken from Termonia, 2005).

Identifying the puncture stages and various damage mechanisms during quasi-static penetration of textiles and composites appears to be well documented within the literature (Nemes et al., 1998, Erlich et al., 2003, and Erkendirci and Haque, 2012). Many of these studies make use of force-displacement data for their analyses, as was also used in the previously mentioned impact study by Liu and Raju (2000). For the current research, the force-displacement response data during thermally-assisted piercing could provide information on the transitions between piercing stages and how the deformation of material occurs.

### 2.2.6 Impactor Geometry

The geometry of the impactor was discussed previously and shown to influence how the thrust force is absorbed by composites and, hence, influence the fibres in a composite or textile. When using a sharp impactor, local yarn failures are dominant around the contact region and damage remains close to this region (Erlich et al., 2003). Conversely, remote yarn failure is the primary damage experienced when a blunt impactor is used and fibre pull-out results from this failure mode (Erlich et al., 2003). The implications of this finding suggest that a sharper pin geometry for puncture will keep any resultant damage localised to within the immediate contact area and will, therefore, be easier to assess via non-destructive testing or microscopy.

This is also a result relevant to the cutting edge radius of drill tools for composites. When the cutting edge radius of the tool becomes larger than the fibre diameter, the ability of the drill to...
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cut the fibres is severely impeded and the cutting mechanism changed from shear to compression (Franke, 2011). This reduced the quality of the hole dramatically since fractured fibres were left protruding from the surface of the hole.

Tan and Khoo (2004) also comment on how projectiles of different geometries at ballistic velocities will puncture and perforate composite laminates with varying degree of damage. The study looked into two shapes of projectile: conical and ogive (Figure 2.10 a) and b), respectively. The results showed that similar damage mechanisms and patterns emerge from the different geometries, although the conical projectile resulted in 33% larger delamination when compared with the ogive-shaped projectile. This suggests that the ogive-shaped projectile will penetrate the laminates with less resistance than a conical indenter and consequently the energy absorbed by the laminate will be less for ogive indenters.

![Figure 2.10: Projectile cross section shapes a) conical; b) ogive](image)

The 33% reduction in delamination damage area is a significant change in damage that can apparently be achieved through the use of different impactor geometry at high velocity. Investigations into low velocity impact of composites show differing results whereby conical indenters penetrated furthest into the laminates, when compared with hemispherical and ogive indenters (Mitrevski et al., 2005). Nevertheless, the specimen absorbed additional energy for the conical indenter, which correlates with the increased damage seen in the ballistic study. Obviously, the mechanisms of damage and energy absorption will vary across the velocity ranges of impact tests, so these findings may not be relevant to the quasi-static nature of the thermally-assisted piercing experiments.
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More recent studies into low velocity indentation of polymeric foams and sandwich panels containing polymeric foams found that flat faced (or truncated) indenters resulted in the largest damage areas, whereas conical indenters exhibited the least damage (Flores-Johnson and Li, 2010 and 2011). The studies also concluded that the foam core density, in addition to the indenter geometry, had a large influence on the indentation resistance.

In summary, published papers relating to drilling performance and impact of composites and textiles agree that the geometry of the drill bit, impactor or indenter will affect the penetration characteristics and consequently the level of resultant damage in a laminate or textile. It is, therefore, hypothesised that the spike geometry used for a thermally-assisted piercing process will have a significant effect on the resultant damage to the composite. Furthermore, the previous conclusion regarding the foam core density influencing indentation resistance damage (Flores-Johnson and Li, 2010 and 2011) raises the point that the matrix will provide significant resistance to the piercing process. This resistance, unlike the density of the foam in the previous study, can be altered by varying the temperature, and hence, the viscosity of the matrix.

2.2.7 Heating and Cooling Rate Effects on Carbon Fibre/PEEK
Increasing the temperature of a thermoplastic composite before piercing has been highlighted in the previous sections as a method that enables displacement of fibres around a piercing spike. The cooling rate, however, will affect the subsequent mechanical and chemical resistance properties of semi-crystalline polymers, such as polyetheretherketone (PEEK) (Cogswell, 1992). Therefore, this may be an important parameter to control during a thermally-assisted piercing process to ensure that the required matrix properties are retained.

Gao and Kim (2000, 2001a, 2001b and 2002) conducted a series of studies into the effect of cooling rate on the crystallinity and consequently the mechanical properties of carbon fibre/PEEK (CF/PEEK) composites. The findings of the studies link the cooling rate with
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crystallinity and spherullite size within the matrix of the composite. Increasing the cooling rate (from 1°C/min to 70°C/min) produced a more amorphous PEEK matrix with higher ductility, but reduced elastic modulus and tensile strength. The rise in ductility resulted in significant improvements in fracture toughness of the matrix since, in general, enhanced plastic deformation enables more energy to be absorbed during fracture.

Despite the improved toughness of a thermoplastic matrix (compared to many early thermoset systems), improvements in composite toughness cannot be realised fully because of the significant reduction in interfacial bond strength between the carbon fibres and the matrix. The more rigid fibres will also restrict the ability of the matrix to deform plastically to the full extent possible, therefore, limiting the benefits achievable through increased matrix ductility. The highly amorphous structure that is produced through high cooling rates will also reduce the chemical resistance of PEEK, and thus increase susceptibility to environmental degradation (Cogswell, 1992).

For the thermally-assisted piercing technique, heating needs to occur to melt the matrix and allow fibre displacement. A further benefit of heating the matrix is the ability to form the surface of the hole wall. Rahman et al. (1999) found that, when drilling a CF/PEEK composite, the matrix was heated at the drill bit cutting point and a smooth finish was generated. The author suggests that the smooth finish was a result of the polymer softening that occurred due to the heating and not the tool geometry. The TAP process will form the inside of the hole surface to the spike surface and could be improved based on the surface finish of the spike.

Although cooling rate effects on crystallinity will change the properties of a composite with a semi-crystalline polymer matrix, introduction of voids during re-heating may also provide a cause for concern. Void content within thermoplastic composites has been studied and identified as a major issue that needs consideration when reprocessing at temperatures above the glass transition temperature (T_g), for amorphous polymer matrices, or the melting point
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(T_m), for semi-crystalline polymer matrices (Ye et al., 2002, and Lu et al., 2004). The flexural strength and Young’s modulus can decrease by approximately 20% following a void content increase from 1% to 12% achieved through heat processing (Henninger et al., 1998).

Void growth can be suppressed by applying a clamping pressure to prevent de-consolidation of the laminate. Consideration, however, needs to be given to possible “squeeze creep” flow that can result from high pressure application to thermoplastic composites at high temperatures (Lu et al., 2004).

In summary, the literature has highlighted that it is important to consider cooling rate when processing CF/PEEK composites, but void content control during re-heating will be a higher priority during the thermally-assisted piercing research. Applying clamping pressure to the laminate during heating can be used to suppress void growth, and also restricts the out-of-plane movement of the laminate when piercing (highlighted previously as an advantage during drilling of laminates by Capello, 2003).

2.2.8 Summary of Processes Relevant to Thermally-Assisted Piercing

Material displacement techniques have been shown to produce increased notched strength and bearing strength when compared with a commonly applied material removal technique (conventional drilling). Moulding-in holes is a material displacement technique that can be applied during the manufacture of thermosetting and thermoplastic composites. Thermally-assisted piercing, however, can be applied to a thermoplastic composite at any point throughout manufacture or service life.

When drilling composites, previous studies have indicated that thrust force has a primary influence on delamination. The feed rate of the drill is identified as the key contributor to thrust force, but the use of a backing plate and control of the drill point geometry can also be used
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to reduce the effective thrust force. This suggests that varying the feed rate and geometry of the spike for thermally-assisted piercing could yield differences in the resultant damage to, and hence mechanical strength of, the laminate.

Puncture and impact studies indicate that heating the composite should allow an increase in ductility such that the fibres are pushed aside by the piercing spike, rather than fracturing. The peak force (analogous to peak thrust force in drilling) and absorbed energy contribute to the hole characteristics in impact and puncture of composites. When low energy impacts are examined, it is typically the matrix properties that are key to the initial damage mechanisms. Reducing the matrix viscosity, however, will cause the energy absorption process to include fibre friction and slippage, which is also exhibited in textile puncture.

During thermally-assisted piercing it is envisaged that the strain in the fibres, and the energy absorption due to slippage and interactions, will be affected by the freedom of the fibres to move in the matrix of the composite. The temperature of the matrix will determine the viscosity, although, the heated area will ultimately restrict the movement of the fibres and their ability to move out of the way of the piercing spike.

Spike geometry was highlighted previously as a variable to influence the thrust force and, hence, the damage inflicted during piercing. The literature on impactor geometries further reinforces this point. Sharp geometry impactors were found to result in more localised damage when compared with blunt impactors on textile fabrics. The conclusions of literature relating to the spike geometry best suited for perforation of laminates varies according to the velocity ranges of interest, however, conical and ogive shaped impactors appear to produce the most desirable results.
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Crystallinity and mechanical properties of a thermoplastic matrix composite material can be affected by the cooling rate imposed. PEEK will change in ductility, elastic modulus and the interfacial bond strength to carbon fibres will change when varying cooling rate due to changes in crystallinity. Despite this, it is the void content that poses the biggest risk to the mechanical performance of thermoplastic matrix composite laminates when reprocessing. It is recommended that clamping pressure is applied to the laminate to suppress void growth and deconsolidation.

In summary the key variables identified from the review for thermally-assisted piercing of thermoplastic composites are:

- Spike feed rate.
- Laminate processing temperature.
- Heated area of the laminate.
- Piercing spike geometry.
- Laminate cooling rate.
- Laminate clamping pressure.

These variables will have varying significance on the resulting properties of a laminate. The following section highlights some of the previous studies into the mechanical properties of specimens notched using material displacement techniques and their relevance to this work.

2.3 Mechanical Properties of Moulded-in and Pierced Hole Composites

2.3.1 Introduction
Previous studies on the strength of moulded-in and pierced holes were reviewed to gain an appreciation of the mechanical performance of specimens notched using the two contrasting hole making techniques and the comparison with conventionally drilled holes. This section is
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separated into different types of test; section 2.3.2 and 2.3.3 consider open-hole tension and compression studies, while section 2.3.4 addresses pin and bolted bearing test studies.

2.3.2 Open-Hole Tension Testing of Moulded-in and Pierced Hole Composites

There are a number of studies that have examined the tensile strength of notched specimens with continuous fibres (i.e. moulded holes) and compared these with the strength of notched specimens with machined (or cut) fibres using a drilling process. When first investigated, by Chang et al. (1987), increases in tensile strength of moulded hole specimens were recorded between 2.7% and 38.3% above that of drilled specimens. The strength increase depended on the fibre material, graphite or Kevlar, and the hole size.

Subsequent studies agreed with the conclusions of this original research that the fibre continuity and local increase in fibre volume fraction close to the hole edge led to an increase in open-hole tensile strength (Ng et al., 2001, and Noda et al., 2012). The effect of local fibre volume content increase has even been shown to increase the net section strength of the notched specimens to a level higher than the un-notched specimen strength (Chang et al., 1987; Langella and Durante, 2008). For these cases (see Figure 2.11), stress is calculated using the cross sectional area at the centre of the specimen i.e. using Equation 2 and Equation 3 for notched specimens and un-notched specimens respectively.

\[
\text{net stress (notched specimens)} = \frac{F}{(W - d)t}
\]

Equation 2

\[
\text{nominal stress (un-notched specimens)} = \frac{F}{Wt}
\]

Equation 3

Here, \( W \) = width of specimen, \( d \) = diameter of hole and \( t \) = thickness of laminate.
The fibre volume content around the edge of the hole will be influenced directly by the number of fibres that have been displaced. Increasing the hole diameter will increase the number of fibres displaced during the process. This would increase the fibre volume fraction at the edge of the hole, suggesting a possible increase in localised strength. Surprisingly, as the diameter of the hole increases, and, hence, the number of displaced fibres, there is a reduction in tensile strength of the specimens (Langella and Durante, 2008). The reduction in strength with increasing hole diameter was found also in studies into the effect of the diameter of hole / width of specimen ratios (D/W) (Lin and Lee, 1992). This shows that the local increase in fibre volume fraction due to fibre displacement does not overcome the overall strength reduction due to the introduction of a larger hole.

Increasing the D/W ratio (by increasing the hole diameter) was found to reduce the strength of moulded hole specimens (calculated using the nominal cross-sectional area), as shown in Figure 2.12 (taken from Lin and Lee, 1992). This was thought to be a result of increasing the proportion of misaligned fibres to undisturbed fibres across the width of the specimen. For the larger holes there is likely to be misaligned fibres that span the full width of the specimen (and are cut at the specimen edges), therefore, reducing the laminate tensile strength considerably (as shown in Figure 2.12). Increasing hole diameter will also increase fibre misalignment.
(Figure 2.13). By increasing hole diameter, the fibres previously running through the centre line of the hole position are displaced over increasingly larger distances. The resultant effect is that the fibres are no longer aligned in the original lay-up directions, which can reduce the stiffness and the strength of the specimens.

![Figure 2.12: Strength reduction factor ($\sigma_{notched}/\sigma_{unnotched}$) of moulded hole specimens (continuous fiber) and drilled hole specimens (discontinuous fiber) for various hole diameter (D) to specimen width (W) ratios. Data points are mean values of three repeat tests (taken from Lin and Lee, 1992)](image)

Figure 2.12 also shows that the strength improvement of moulded hole specimens compared with drilled specimens increases with increasing D/W ratio (with exception of the largest D/W specimen – discussed previously), despite a reduction in the moulded hole strengths. The improvements increase from 39% to 77% (for D/W ratios of 0.1 and 0.29, respectively) before reducing to 27% (for a D/W ratio of 0.4). The reduction in drilled specimen strength was more significant than in the moulded hole specimens, which were likely offset by the increase in fibre volume fraction due to fibre displacement discussed previously.
The misalignment and compaction of fibres, as a result of using moulded-in holes, has been shown in previous literature to affect the notched strength of specimens. Another variable that may affect the results is the laminate properties before notching. Limited literature exists on this topic, but a study by Quinn et al. (2005) concluded that moulded holes for two differing UD ply lay-ups (0/90 and +/- 45) provide an equal 20% increase in open-hole tensile strength when compared with drilled specimens of the same geometry.

In contrast, notching woven fabrics by moulding holes is believed to provide an increased advantage over UD ply laminates when comparing the notched tensile strength with drilled holes (Ng et al., 2001). The woven fabrics provide more interlacing points within the laminate structure that hinder damage growth and provide higher strength and toughness when compared to UD plies. It is also likely that the interlacing points would make fibre displacement more difficult and affect the final fibre microstructure near the hole edge. This may be worth considering for future experiments or comparison of data with laminates made from woven fabrics and UD plies with nominally equivalent properties.

The literature described above demonstrates that moulded holes can be used to provide a notched specimen with increased tensile strength when compared to a conventionally drilled
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specimen. The level of improvement depends on the specimen dimensions and the material used to construct the laminate. Thermally-assisted piercing is a different method of producing holes in composite laminates that may incorporate some levels of fibre damage, but it is envisaged that a strength benefit may still be obtained from the open-hole tension tests when compared with a drilled specimen.

2.3.3 Open-Hole Compression Testing of Moulded-in and Pierced Hole Composites

Compression properties of moulded-in hole specimens have not been studied to the same extent as tensile properties, but remain vital for many loading situations. Previous work has shown that an increase in ultimate strength can be achieved for moulded-in hole specimens when compared with drilled hole specimens under compression (Ghasemi-Nejhad and Chou, 1990a; 1990b). The studies showed that for hole diameters of 3.2 mm, 6.4 mm and 9.6 mm strength increases of 17%, 38% and 60% were observed when compared with similar size drilled holes, respectively (shown in Table 2.1). The findings showed that the strength enhancement increased with increasing hole size and reducing $W/d$ ratio (from 8 to 2.6, where $d =$ hole diameter in these studies), despite reductions in strength for both drilled and moulded hole specimens. This was similar to the findings within the open-hole tension literature, with exception to the significant reduction in tensile strength of moulded hole specimens at $D/W = 0.4$ (equivalent to $W/d = 2.5$).

As mentioned previously, the larger hole size increased the number of fibres compacted into the surrounding volume. The largest hole diameter for the compression tests, however, was 9.6 mm (compared with 19 mm diameter hole used in the open-hole tension investigation). The compounding effect of trimming the displaced fibres at the specimen edges and the significantly larger fibre misalignment angles were likely to have resulted in the more significant reduction shown in the tension results, which was not observed in the compression results.
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Table 2.1: Failure strength, failure strain and percentage strength ($\sigma_{\text{notched}}/\sigma_{\text{unnotched}}$) for drilled and moulded hole specimens with varying hole size (taken from Ghasemi-Nejhad and Chou, 1990a)

<table>
<thead>
<tr>
<th>Laminate</th>
<th>Hole Diameter ($D$)</th>
<th>Failure Strength ($\sigma_t$) (MPa)</th>
<th>Failure Strain ($\epsilon_t$)</th>
<th>Percentage of Strengths ($\sigma_t/\sigma_u$)100 %</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drilled</td>
<td>0.129 (0.328)</td>
<td>54.43 (375.75)</td>
<td>0.52</td>
<td>57</td>
</tr>
<tr>
<td></td>
<td>0.275 (0.653)</td>
<td>42.37 (292.49)</td>
<td>0.33</td>
<td>44</td>
</tr>
<tr>
<td></td>
<td>0.383 (0.973)</td>
<td>32.68 (225.60)</td>
<td>0.18</td>
<td>34</td>
</tr>
<tr>
<td>Molded-in</td>
<td>0.121 (0.307)</td>
<td>63.70 (430.74)</td>
<td>0.64</td>
<td>67</td>
</tr>
<tr>
<td></td>
<td>0.246 (0.525)</td>
<td>58.43 (403.36)</td>
<td>0.56</td>
<td>61</td>
</tr>
<tr>
<td></td>
<td>0.375 (0.953)</td>
<td>52.36 (361.46)</td>
<td>0.49</td>
<td>55</td>
</tr>
</tbody>
</table>

Due to the limited number of studies on the compressive strength of moulded or pierced holes, studies on the compressive strength of drilled holes were also evaluated for repeatable findings. A previous study by Fleck et al. (1995) found that the increase in fibre misalignment in woven composites was a primary cause for reduction in open-hole compressive strength when compared with unidirectional material. The piercing process introduces significant fibre misalignment as a result of material displacement. This is likely to reduce the compression strength when compared with drilled specimens. Both the size of the hole and the size of the heated area will have significant influence on the fibre misalignment and subsequent compressive (and also tensile) strength. This will need to be considered, however, with the increase in strength resulting from the increase in fibre volume fraction near the hole and fibre continuity due to displacement (rather than cutting). The competing effects will influence the resultant mechanical performance and could vary in dominance for the different open-hole tests.

2.3.4 Plain-Pin and Bolted Bearing Testing of Moulded-in and Pierced Hole Composites

One of the conclusions from the literature that was discussed previously for open-hole testing was that the closely packed fibres at the edge of the hole for moulded-in holes increased the
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fibre volume fraction locally. This increased the notched strength of moulded holes when compared with drilled holes. The fibre continuity and increase in fibre volume fraction that gives rise to the increase in notched strength for moulded holes may not necessarily result in an increase in bearing strength since different loading conditions and failure modes will be present. Figure 2.14 shows schematic examples of the different failure modes possible when bearing testing. Failure mode (b) is most relevant to unconstrained pin-bearing while the other mechanisms are seen in finger tight or fully clamped bolted joints.

![Figure 2.14: Failure modes of holes under bearing load](image)

Figure 2.14: Failure modes of holes under bearing load a) net tension failure; b) bearing failure; c) shear out failure; d) cleavage failure. (Re-drawn from Zoghi, 2013)

The first study into moulded-in holes (Chang et al., 1987) showed that an increase in bearing strength when compared to drilled holes can still be achieved. The investigation into bearing strength found that for graphite/epoxy composite laminates with moulded-in holes, the bearing strength was higher than for laminates with drilled holes. The improvement in bearing strength for moulded-in holes identified by Chang et al. (1987) was found by studying the bearing response of a finger tight bolted joint assembly. Subsequent work into the bearing failure by pin loading by Durante and Langella (2009) came to the same conclusions. Both studies identified that the bearing responses of bolted and pin loaded holes were affected, to differing extents, by the specimen geometry under loading.
The effect of W/d and E/d (edge distance / diameter of hole, shown schematically in Figure 2.15) on the pin bearing failure strength of moulded-in and drilled holes was investigated by Durante and Langella (2009). With respect to the W/d of specimens, the results showed that when reducing the width of the specimens (i.e. W/d ratio from 6 to 3) the pin bearing strength of the laminates decreased by 12% for the drilled holes and 15% for the moulded-in holes. Despite withstanding higher loads before failure, the moulded-in holes appeared to be slightly more sensitive to W/d than the drilled holes. This was also identified in the open-hole tension literature (discussed previously, although for a significant increase in hole size) and could be, as before, attributed to the increase in percentage of misaligned fibres across the width of the specimen when reducing W/d.

Figure 2.15: Schematic diagram of bearing test specimen dimensions

In addition to the W/d sensitivity, the bearing response of moulded-in and drilled holes was also concluded to be sensitive to the E/d ratio of the specimens (Durante and Langella, 2009). The failure mode of the drilled holes significantly changed when reducing the E/d ratio. Multiple studies concluded that a transition of failure modes occurred at approximately E/d = 2 for drilled holes (Chang et al., 1987, Lin and Tsai, 1995, and Durante and Langella, 2009). When E/d = 2, the maximum difference in bearing strength between drilled holes and moulded-in holes was also found.
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For E/d ≤ 2, the drilled holes no longer exhibited bearing failure and started to give mixed modes of failure between shear out and net tension (for pin loaded and finger tight bolted joints). This was not the case for moulded-in holes, which exhibited bearing failure for all specimen geometries tested. This suggests that the continuity of fibres was providing additional reinforcement to help reduce the likelihood of shear out and net tension failure.

The change in failure modes for drilled holes was most likely the reason why the increase in strength of moulded-in holes, when compared to drilled holes, was seen for reducing E/d (Lin and Tsai, 1995). The shear out failure of a drilled hole will occur as a predominantly matrix shear failure since the 0° fibres below the hole have been cut during drilling. Shear out failure results in a catastrophic failure that will not resist the larger loads tolerated during bearing failure. Bearing failure allows a mixture of damage mechanisms, including fibre and matrix compression (in addition to matrix shearing), in the 0° plies before failure begins to occur in a progressive manner. So by reducing the E/d ratio, the failure of the drilled holes changes from bearing to shear out and net tension and increases the advantage gained from the fibre continuity of moulded holes.

The previous studies on pierced holes, by Hufenbach et al., showed that an increase in bearing strength of 5% could be achieved for pierced holes over drilled holes. The research appears to find bearing failure for drilled holes at E/d = 2 (for which the gain in bearing strength is quoted). This finding does not agree with the previous research into the failure of drilled holes, which show shear out failure at E/d = 2. The more ductile thermoplastic (used in the Hufenbach et al. studies) may have provided more resistance to shear out failure, but it is not certain whether this would be of sufficient significance to alter the failure mode. The differences between the testing geometries (W/d and E/d ratios) and laminate material will need to be carefully considered when comparing the results of the previous studies with that of the current research.
2.3.5 **Summary of Mechanical Properties of Moulded-in and Pierced Hole Composites**

Keeping fibres continuous (by moulding holes) was shown to increase the strength of notched composites under both tensile and compressive loading when compared to specimens with cut/machined fibres (by drilling holes). The specimen geometry, in particular the size of the hole, was shown to affect significantly the strength increase of the specimens with moulded-in holes. An increase in strength with increasing hole size was shown and suggested to be caused by an increase in fibre volume content near the hole edge. This was offset by an increase in fibre misalignment angle when significantly increasing the hole size and testing in tension, which reduced the overall tensile strength benefit of the moulded specimens.

For open-hole compression, increasing the size of the hole was also found to increase the strength benefit of moulded-in holes when compared with drilled. The competing effects of increasing fibre volume fraction and increasing fibre misalignment as a result of the moulding process are likely to have affected the open-hole tests. The findings suggest that these effects, as a result of piercing under varying process conditions, may provide useful insights into the mechanical performance of the pierced specimens.

Previous studies have shown that an increase in pin and bolted bearing strength can also be achieved for moulded-in hole specimens when compared with drilled. The increase in strength, however, is sensitive to both the W/d and E/d ratios and will also depend on the specimen lay-up. Reducing the W/d ratio reduced the bearing strength improvement. The area over which the fibres are displaced, and how the specimen edges dissect this area, is likely to be causing the variation in performance (since the hole size remained constant). It is likely, therefore, that the size of the heated area when piercing, which will govern the area that fibres can be displaced within, will affect the bearing mechanical properties of pierced specimens.

The literature shows that changing the E/d ratio is likely to affect the comparative analysis between the pierced and drilled specimens. Whilst the retention of fibre continuity prevents a
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weaker shear out failure within the pierced specimens at low E/d values, the drilled specimens significantly reduced in strength. This will need to be considered in the current study. The edge distance will also need to be considered in relation to the size of the heated area, in a similar fashion to the W/d ratio mentioned previously. Whether the end of the specimens intersect through an area of fibre displacement (within the heated area) or not may have a significant impact on the bearing mechanical performance.

2.4 Concluding Remarks
There do not appear to be studies in the literature that have investigated the process variables for thermally-assisted piercing in a systematic way. This research aims to fill this gap and is concerned with establishing the material microstructure after piercing, how it is affected by changing some of the process variables identified and the resultant mechanical performance of specimens after piercing. Reviewing previous literature has provided a number of possible process variables that are likely to affect the material microstructure and subsequent mechanical properties. The heated area, processing temperature and spike geometry were considered to give the most significant changes to the microstructure and were selected to be investigated within this project.

Open-hole tension and compression testing have been used in previous studies to measure the mechanical performance of specimens machined using traditional material removal techniques and material displacement techniques. The tests evaluate the mechanical performance of the remaining material after notching, so they were used within this project to compare the remaining material performance after piercing and drilling. Pin bearing and bolted bearing tests have been studied in the past to determine the strength of notched specimens used in joining applications or applications where the hole is specifically loaded. This can provide a more industry relevant test case if the pierced holes are subsequently used for joining. The influence of specimen geometry was shown to affect the mechanical performance for all types of test, and so will remain constant within the current project to eliminate sources of discrepancy.
The literature findings have allowed the objectives of this project to be developed from the previously stated aim. The specific objectives are:

- Develop a thermally-assisted piercing rig capable of producing pierced specimens with the flexibility to consistently vary the heated area, processing temperature and spike geometry.
- Characterise the resultant material microstructure after piercing and how this is affected by a change in the processing variables.
- Evaluate the mechanical performance of pierced specimens (using open-hole tension, open-hole compression, pin bearing and bolted bearing tests) and how the microstructure after piercing impacted this mechanical performance.

The next chapter presents the development of the piercing rig and how the piercing variables were altered for the investigation. The testing matrix is also presented and how the piercing process (in terms of the spike journey through the laminate) varies as a result of the piercing variables under investigation.
3 Development of a Thermally-Assisted Piercing Rig

3.1 Introduction
A thermally-assisted piercing rig was built to both manufacture specimens for subsequent mechanical testing and for investigating the piercing process. Initial feasibility trials were conducted on a prototype rig in the first instance (Brown et al., 2013). The knowledge gained from the feasibility testing was used to design and build an improved rig with flexibility to vary some of the identified process variables for analysis.

Based on the literature review, the process variables identified for piercing were: spike feed rate, processing temperature, heated area, spike geometry, laminate cooling rate and laminate clamping pressure. Within the current study it was only possible to investigate the effect of varying some of these variables, due to limitations in time and resources. It was decided that the key parameters were the processing temperature, heated area and spike geometry.

This chapter describes the rig that was designed for the investigation. Firstly it contains the details of the materials used throughout the investigation and how laminates were manufactured. Then the TAP set-up is described followed by the verification of the heating set-up. Information about how the heated area, processing temperature and spike geometry variables were selected for the piercing experiments is then shown. The procedure used to pierce specimens is described and the force/displacement data measured by the instrumented piercing rig (during piercing) is presented and discussed.

3.2 Materials and Laminate Manufacture
The composite specimens used for testing were manufactured from CF/PEEK (Tenax® TPUD PEEK-HTS40) pre-impregnated unidirectional tape. A 21 ply lay-up provided nominally 2.9 mm thick, balanced and symmetric, laminates ([0/90]5 / 0]S). The pre-preg material was cut to
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300 mm x 300 mm squares and placed in a picture frame tool for consolidation (Figure 3.1). The tool was placed into a pre-heated press (Figure 3.2) at 400°C and a pressure of 0.8 MPa (following supplier recommendations) was applied for one minute per ply (21 minutes for the laminate) before deactivating the heat input and allowing the laminate to cool under the consolidation pressure to room temperature (with an approximate cooling rate of 1.7°C/min).

Figure 3.1: Picture frame tool used for laminate manufacture

Figure 3.2: Heated press used for laminate manufacture
3.3 Experimental Set-Up for Thermally-Assisted Piercing (TAP)

The TAP rig, shown in Figure 3.3, comprises three independent systems: a clamping and piercing system, heating system and data acquisition system. The clamping (and piercing) and heating systems worked together to heat the laminate and to introduce the pierced hole under repeatable and controlled conditions. The data acquisition system was installed to measure the load and displacement of the spike as it pierced the laminate in order to help to understand the piercing process and how it varied with changing process variables.

The clamping and piercing system comprised two pneumatic actuators and the supporting structure of the rig (highlighted in blue – Figure 3.3). One actuator was used to clamp the laminate and the other was used to drive the piercing spike. Separate pneumatic supplies were used to operate the clamping actuator and spike actuator (highlighted in blue – Figure 3.4). The clamping pressure applied to the specimens during piercing could be varied by changing the supply pressure to the clamping actuator.

The spike used for piercing was attached to an actuator using a drill chuck. The drill chuck allowed spikes of varying geometry to be used as required for piercing. The pneumatic actuation of the spike was pressure driven (rather than constant force or displacement). The initial velocity of the spike prior to contact with the specimen could be controlled by varying the flow rate of the air supply to the actuator. The velocity of the spike and applied force during piercing, however, could not be controlled with the current set-up and varied throughout the piercing process, although the resultant velocity could be monitored continuously.
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Figure 3.3: TAP rig photograph with clamping and piercing system (blue), heating system (red) and data acquisition system (green) outlined

The heating system consisted of a heater control unit and two heater units used to clamp the laminate (from above and below) and heat the required area directly using conduction (see part of the set-up marked in red – Figure 3.3 and Figure 3.4). The heater units had machined grooves into which resistive heater cables were embedded as shown in Figure 3.5 a) and b) respectively.
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Figure 3.4: TAP rig system diagram with colour coded clamping and piercing, heating and data acquisition systems; the colour coding matches Figure 3.3

Figure 3.5: Heater unit parts a) machined grooves in the casing structure; b) heater cable embedded in heater casing structure

The heater control unit was used to set the temperature of the resistive heaters. Process temperature was varied by changing the temperature of the heater units on the control unit. The temperature controllers used thermocouples (integrated within the heater cables) to feedback temperature and control the ramp rate and maximum temperature of the heaters.

The heated area was varied by changing attachments to the heater units. The attachments altered the tooling face (in contact with the specimens) with differing areas of conducting metal.
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(steel) and an insulating ceramic fibre board, Figure 3.6 a), b) and c). To pierce specimens with the maximum heated area, a full metal attachment was used, Figure 3.6 a). The diameter of the largest attachments matched the size of the heater units, so no insulating rings were required. To apply a smaller heated area, smaller metal attachments were used and insulating rings were incorporated to shield the laminate partially from the heaters, Figure 3.6 b) and c). A final insulating ring was placed around the heater units during testing to ensure the region around the heated area remained under pressure for all heated areas tested (to prevent deconsolidation of the composite).

The tooling surfaces on the bottom of the rig also included a machined recess to accept the displaced material during piercing (Figure 3.7). The recess was 0.5 mm deep with a 12 mm diameter. It was cylindrical in shape for the current set-up, but could be easily varied to geometries more suited to specific industrial applications (e.g. countersunk or hemispherical). During the TAP process the total volume of material associated with the production of a 6 mm hole in a 2.9 mm thick plate is approximately 82 mm$^3$, which is the volume of material displaced during piercing. The recess volume in the tooling was approximately 42 mm$^3$ (approximately one half of the hole volume). The remaining material, not formed into the recess, was extruded through the tooling during piercing and removed (by polishing) prior to testing.

![Figure 3.6: Tooling face attachments to heater units a) maximum heated area; b) intermediate heated area (including insulating ring); c) smallest heated area (including largest insulating ring)](image-url)
The data acquisition system (marked in green – Figure 3.3 and Figure 3.4) was used to measure the force and displacement of the spike as it pierced the specimens. A 2.5 kN load cell and 50 mm (stroke) displacement sensor were integrated with a logging unit and laptop to record the data during piercing. The load cell and displacement sensors were calibrated to UKAS standard with accuracies of ± 0.03% (of full range) and ± 0.1% (of full range), respectively. The logging system had a maximum acquisition rate of 50 kHz across a maximum of 4 inputs. The logging rate used for the testing (with 2 inputs) was 1 kHz to ensure adequate capture of the piercing process without generating unreasonably large data sets.

3.4 Thermally-Assisted Piercing (TAP) Rig Heating Tests

3.4.1 Introduction
The TAP rig used in the project was designed and commissioned following piercing trials with a preliminary rig. The preliminary rig used an induction heating system to heat specimens before piercing. The subsequent TAP rig used a conduction heating method (described previously) to improve the control and repeatability of the heating process.

This sub-section describes the calibration tests that were conducted to ensure the repeatability of the heating system was acceptable. Further tests were also conducted to establish the
heater settings required to achieve the desired processing temperatures and heating areas for the variable settings used in the investigation.

3.4.2 TAP Rig Heating Test Set-Up

To determine the heating rate of the laminate, once clamped between the contact heaters, initial calibration tests were conducted. A Carbon Fibre / Polyetheretherketone (CF/PEEK) laminate was manufactured with thermocouples embedded within the plies. There were 6 thermocouples, 0.2 mm in diameter, placed at 7.5 mm intervals from each other between the 11th and 12th ply of the 21 ply laminate (approximately ½ thickness). Another thermocouple was placed between the 16th and 17th ply (approximately ¼ thickness) above the inner most thermocouple, as shown in Figure 3.8.

![Figure 3.8: Schematic of embedded thermocouple locations](image)
The thermocouples were tacked in place whilst laying up the laminate before manufacture. It was envisaged that the exact locations of the thermocouples may deviate, slightly, from the original position during the consolidation process due to material flow and the pressing process. To verify the final positions of the thermocouples, and determine if any movement had occurred, an X-ray was used to image the embedded thermocouples (Figure 3.9). The thermocouples deviated slightly from their original positions, but the X-ray allowed this deviation to be measured and incorporated into the results in the subsequent section.

The initial repeatability tests used 60 mm diameter heated contact surfaces that were primed to a surface temperature of 400°C before clamping the laminate (heater settings of 460°C and 438°C were used for the upper and lower heaters respectively). The laminate was clamped for 2 minutes and tests were repeated 3 times to observe the repeatability.

Figure 3.9: X-ray radiograph of embedded thermocouples within CF / PEEK laminate

The testing matrix for the investigation required the laminate to be pierced at various temperatures and with various sizes of heated area. The heater unit settings were adjusted to achieve the required laminate temperatures for piercing (380°C, 400°C and 450°C). The tooling surface attachments were varied (as described in section 3.3) to vary the diameters of
the heated area (60 mm, 45 mm and 30 mm). For both changes in set-up, the heating profiles of the laminate were measured and are shown subsequently.

3.4.3 TAP Rig Heating Repeatability Test Results
The results of the heating tests show the temperature profile across the heated area at the plotted intervals described previously, Figure 3.10 a) – h). Three sets of repeated tests are shown to observe the repeatability of the set-up. The results show that after initial clamping the heating profiles are repeatable for the duration of the test with negligible deviation.

A 60 mm diameter heated area was desired using the 60 mm heated contact surface. The plots show the initial clamping of the laminate, Figure 3.10 a) (t = 0), and how the temperature then increased as the heat conducted into the laminate, Figure 3.10 b) – h).

Once the laminate was clamped, it experienced a rapid temperature increase. The dashed red line shows the melting temperature of the PEEK matrix (343°C) and after 10 s the laminate temperature was above the melting point at some of the thermocouple locations, Figure 3.10 b).

As the heating time increased, two trends are shown in the data. The centre of the heated area was not directly heated by the contact heater since there was a hole for the spike to pass through for piercing. This is the reason that the centre of the heated area did not heat up as quickly as the locations in direct contact with the heaters, Figure 3.10 b) – d).
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Figure 3.10: Temperature profiles of a heated laminate (60 mm diameter heated area) after various heating times (3 repeated tests)
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At approximately 40 s the difference in temperature between the centre and the maximum was almost negligible at approximately 4°C, Figure 3.10 e). After 40 s of heating the laminate continued to increase in temperature until the heaters were deactivated, after 2 minutes, reaching a final temperature of approximately 425°C, Figure 3.10 f) – h).

The second trend that can be seen within the results is the reducing differences between the laminate temperature at a depth of ¼ thickness and ½ thickness (approximately) with time, Figure 3.10 a) – e). The initial plots show a higher temperature for the thermocouple placed closer to the surface. As the heating time increased to 40 s, however, the difference became negligible, showing approximately constant through thickness temperature, Figure 3.10 e).

The results also show how the temperature decreased towards the edge of the target heated area. After 40 s of heating the radius of the heated area above the PEEK melting temperature was approximately 30 mm (as desired for a 60 mm heated diameter), Figure 3.10 e). When heating time increased, the area heated above the melting temperature increased to a radius of approximately 33 mm after 2 minutes, Figure 3.10 h).

The results indicate that a heating time of 40 s is sufficient to heat the whole target area above the melting temperature of the laminate (343°C) and the majority of the area to the required processing temperature (400°C), shown in Figure 3.10 e. Although the laminate was being heated outside of the required area, it would not be practical to install a method of cooling this region. The gradual reduction of temperature, from the desired 400°C, will need to be considered if comparing experimental results with numerically modelled results. In a simplified numerical case, the matrix temperature would instantly fall to ambient outside of the heated area – this is not realistic because of conduction effects. This would result in a difference in fibre boundary/clamping conditions at the edge of the heated area unless a method of incorporating the temperature reduction is applied.
3.4.4 TAP Rig Heating Test Results for Varying Processing Temperature

The heater unit settings were adjusted for the target laminate temperatures of 380°C and 450°C to vary the process temperature for piercing. The initial repeatability tests described previously confirmed the settings required for the intermediate temperature of 400°C.

The upper and lower heater units were set to 400°C and 385°C, respectively, to achieve the 380°C target temperature. The heating time required to reach 380°C across the 60 mm diameter area was 5 minutes (300 s), as shown in Figure 3.11 a). Trials were conducted to attempt to reduce this heating time, but it was not possible without increasing the diameter of the heated area significantly beyond the 60 mm target.

To achieve the 450°C target temperature, the upper and lower heater units were set to 510°C and 500°C, respectively. This produced a heating profile that reached a consistent temperature of approximately 450°C across the majority of the heated area after 40 s of heating, as shown in Figure 3.11 b). The repeatability of the rig set-up for the 60 mm diameter heated area was previously confirmed so repeat readings were not taken for the processing temperature calibration tests.

![Figure 3.11: Temperature profiles of a heated laminate at various processing temperatures](image-url)
3.4.5 TAP Rig Heating Test Results for Varying Diameter of Heated Area

The 60 mm diameter heated area was tested previously to confirm the repeatability of the rig. The remaining diameters of heated area to be tested were 45 mm and 30 mm. The tooling surface attachments were varied for these tests so 3 repeated tests were conducted to determine the repeatability of the configuration changes.

The results showed good repeatability across the tests, which agreed with the initial results, Figure 3.12 a) and b). The plots also show that a similar heating profile is achieved for the various diameters of heated area; approximately constant temperature before reducing towards the edge of the heated area – confirming that the insulating rings were successful in masking the heater units.

![Temperature profiles of a heated laminate at various diameters of heated area](image)

**Figure 3.12:** Temperature profiles of a heated laminate at various diameters of heated area a) 45 mm; b) 30 mm

The upper and lower heater units were set to 460°C and 438°C, respectively, for the 45 mm diameter heated area (the same used for the 60 mm diameter heated area). The upper and lower heater units were set to 470°C and 438°C, respectively, for the 30 mm diameter heated area (the upper heater requiring a higher pre-set temperature).
3.4.6 Concluding Remarks
The calibration tests showed that good repeatability was achieved for the TAP rig for all heated area configurations. Heater unit settings were determined for all the required processing temperatures and diameters of heated area.

Heating profiles showed that there was little deviation in temperature distribution across all heated areas using the heater settings and heating times described. The gradual reduction in temperature towards the edge of the heated area was inherent in the rig design and would require additional rig modifications (forced cooling) if solved in future investigations. This resulted in gradual boundaries at the edge of the heated area where the laminate temperature becomes lower than the melting temperature.

3.5 Process Variable Selection

3.5.1 Introduction
The processing temperature, heated area and spike geometry were varied for the piercing process to establish firstly any differences in the piercing process, and also what effect the variable had on the resultant material microstructure and mechanical performance of specimens (discussed in subsequent chapters). The following subsections describe the justifications for the variable values chosen. Finally, the process variables are summarised and tabulated to show the test matrix used for the investigation.

3.5.2 Piercing Process Temperature
The processing temperatures used for the piercing experiments were determined by taking into account both the consolidation guidelines provided by the manufacturer and the limitations of the testing rig. The guidelines suggest processing the pre-preg material at 400°C for optimum results (Toho Tenax, 2012). This was, therefore, decided to be the intermediate value used for the temperature tests.
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The melting temperature of PEEK is approximately 343°C. At this temperature, however, the polymer has a high viscosity and is not easily processed. It was decided that 380°C would be the lower limit of the temperature testing to ensure the resistive force on the spike during piercing was not above the maximum application force of the actuator.

The maximum temperature was primarily governed by the rig capability. The heaters installed on the rig had a maximum operating temperature of 600°C. The heaters lost energy into the surrounding structures and when conducting heat through the tooling surface attachments. As a consequence, laminate processing temperatures (measured using thermocouples embedded in the laminate) were somewhat lower than the actual heater temperatures (measured using thermocouples embedded in the heaters). The maximum laminate processing temperature was set as 450°C to run the heaters safely within their limit.

The material supplier provided viscosity data for the VESTAKEEP® PEEK used within the pre-preg material. The data provided were determined using a cone-plate viscometer for multiple angular frequencies, shown in Figure 3.13. These data were extrapolated using exponential trend-lines, assuming a viscosity dependence similar to the general Arrhenius equation for a broad range of Newtonian fluids shown in Equation 4 (Kudra and Strumillo, 1998). The extrapolated data show an average reduction in viscosity of approximately 40% when increasing process temperature from 380°C to 450°C (across the different angular frequencies tested). A 40% change in viscosity was considered to be a reasonable and significant variation for this investigation.

\[
\mu = \mu_0 e^{(E/R)}
\]

\textit{Equation 4}

\(\mu\) = liquid viscosity at the absolute temperature \(\theta\), \(\mu_0\) = liquid viscosity at reference temperature, \(E\) = activation energy and \(R\) = universal gas constant.
3.5.3 Heated Area

The size of the heated area is likely to affect the residual strain locked into the fibres by the process, waviness of fibres and the packing of fibres when displaced during piercing. The diameters of the heated areas (60 mm, 45 mm and 30 mm) were chosen to give a range of fibre strain close to the hole edge based on the approximate analysis outlined below.

Using a beam bending equation, the strain in fibres due to lateral displacement around a hole could be calculated, approximately. The simplified case for calculating the strain assumes a single point load applied to a beam (the fibre) with two fixed supports (Figure 3.14). Here, \( W \) is the applied force and \( L \) is the span of the beam (or length of un-restrained fibre, analogous to the diameter of the heated area in this case). Using the beam bending moment relationship, Equation 5 (Hearn, 1977), maximum deflection can be derived as Equation 6, where \( EI \) is the flexural rigidity, \( y \) is the deflection and \( x \) is the position along the beam span.

![Figure 3.13: Viscosity curves of PEEK resin tested at various angular frequencies (supplied by Toho Tenax)](image)
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![Diagram of carbon fibre as a beam with two fixed supports and a central point load](image)

**Figure 3.14:** Carbon fibre simplified as a beam with two fixed supports and a central point load

\[
\text{Bending Moment (M)} = EI \left( d^2 y \right) dx^2
\]

**Equation 5**

\[
y_{\text{max}} = \frac{WL^3}{192EI}
\]

**Equation 6**

For this approximation the first fibre had a maximum displacement equal to the hole radius (3 mm). Subsequent fibre displacement was approximated by assuming the fibre volume fraction varied from a perfectly packed arrangement \((V_f = 0.9)\) at the hole edge to the parent material fibre volume fraction \((V_f = 0.59)\) at the edge of the heated area with a linear decay. The starting fibre volume fraction and linear decay were assumed since no analysis had been conducted at this point. The approximations were calculated for three sizes of heated area (Figure 3.15). The diameter of the heated areas (60 mm, 45 mm and 30 mm) were chosen to give a range of fibre strains close to the hole edge (based on the approximated values).
The calculations show that the assumed fibre strain is highest for the fibres closest to the hole edge after piercing (Figure 3.16). Carbon fibres typically fail at approximately 1.8% strain. The diameters of the heated area lead to fibre strains that vary from approximately 0.6% strain (33% of the failure strain) to approximately 2.4% strain (133% of the failure strain) based on the approximated model. The piercing process may fracture some of the inner-most fibres close to the hole edge due to frictional contact with the spike, but overall fewer fibres than would be cut during a conventional machining process.
3.5.4 Spike Geometry

The spike geometry used for piercing was varied according to the inclusive angle ($\theta$) of the spike tip (Figure 3.17). All spikes had 6 mm diameter cylindrical shafts and three of the four spike geometries had conical spike tips, Figure 3.17 a). One of the spikes had a tangential ogive tip geometry that is commonly applied to aircraft nose cones or rifle bullets, Figure 3.17 b). The sharpness of the ogive can be defined as the ratio of the arc radius ($R_o$) to the shaft diameter ($D_s$), as shown in Equation 7, $R_o$ and $D_s$ are shown in Figure 3.17 b).

\[
\text{Sharpness of ogive} = \frac{R_o}{D_s}
\]

*Equation 7*

Conical spike angles of 20°, 30° and 40° were used for testing and an ogive sharpness of 6 was chosen that would give a spike tip angle of 47°. The ogive spike tip angle was chosen to be larger than all of the conical spike angles, but this angle reduced and became tangential to the spike shaft (as shown in Figure 3.17 b)).

![Figure 3.17: Schematic of piercing spikes a) conical spike; b) ogive spike](image-url)
3.5.5 Process variable testing matrix
The selected process variable settings are summarised in Table 3.1. To maximise efficiency with time and testing resources, all specimens in the testing matrix were pierced prior to any mechanical testing or analysis. A design of experiments approach was not chosen because not all the variables were being tested. There were also rig limitations (i.e. the maximum processing temperature and the cost of manufacturing additional heating sizes) that prevented the use of a design of experiments methodology.

When varying the heated area the processing temperature and clamping pressure remained constant at 400°C and 0.8 MPa (matching the material supplier manufacturing guidelines). The intermediate conical spike angle, 30°, was used for these tests and remained constant for the heated area and processing temperature variations.

The variation of processing temperature was conducted for the largest diameter of heated area (60 mm). The same spike geometry and clamping pressure were used as for the heated area tests (30° conical spike and 0.8 MPa clamping pressure). Based on previous literature and the fibre strain calculations (in section 3.5.3), the largest heated area for piercing was designed to give the least residual fibre strain (the strain resulting as a consequence of fibre displacement and elongation during piercing). Pierced specimens with the least additional fibre strain (based on the approximation) were, therefore, used for the processing temperature testing.

For the variation of spike geometry, the processing temperature and clamping pressure remained equal to the baseline settings recommended by the material supplier. The diameter of the heated area, however, was reduced to the lowest value (30 mm). The reason for this was due to potential attractiveness of the technique to industry. Discussions with TWI member companies early in the investigation had highlighted that other considerations, e.g. how close the holes could be placed to each other or to the edge of a structure, could be more important
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than the maximum strength of the holes, which was envisaged to be achieved with the largest diameter of heated area. The smallest diameter of heated area was, therefore, chosen to be tested for the change in spike geometry.

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Piercing process variables</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Heated area diameter (D – 2D)</td>
</tr>
<tr>
<td>D1</td>
<td>Drilled</td>
</tr>
<tr>
<td>T1</td>
<td>60 mm (2D)</td>
</tr>
<tr>
<td>T2 (A1)</td>
<td>60 mm (2D)</td>
</tr>
<tr>
<td>T3</td>
<td>60 mm (2D)</td>
</tr>
<tr>
<td>A2</td>
<td>45 mm (1.5D)</td>
</tr>
<tr>
<td>A3</td>
<td>30 mm (D)</td>
</tr>
<tr>
<td>S1</td>
<td>30 mm (D)</td>
</tr>
<tr>
<td>S2</td>
<td>30 mm (D)</td>
</tr>
<tr>
<td>S3</td>
<td>30 mm (D)</td>
</tr>
</tbody>
</table>

3.6 Experimental Procedure for Piercing

The piercing process for all variable settings relied on four key stages: initial rig heat-up, clamped laminate heating, piercing and cool-down, Figure 3.18 a) – d). The heaters were activated and raised to their target temperature prior to clamping and piercing. Preliminary studies were completed for the rig to determine the tooling surface temperatures and heating times required for the different sizes of heated area and temperature when piercing (section 3.4).
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The tooling surfaces were heated to the target temperature (measured manually with a thermocouple) and the laminate was clamped and heated. After the pre-determined heating time (established during preliminary testing – section 3.4) the spike actuator was activated and the spike was driven through the laminate to displace the heated material and produce the hole.

The heaters were de-activated after the spike had reached maximum travel (and piercing was completed). The clamping pressure was sustained while the heaters and laminate cooled to ambient conditions (at approximately 9°C/min). The spike was then retracted and the clamping pressure released to enable the pierced specimen to be accessed.

Figure 3.18: Schematic diagram of the four key piercing process stages: a) initial rig heat-up; b) clamped laminate heating; c) piercing; d) cool-down
Panels used to produce drilled specimens were manufactured with a cooling rate of approximately 2°C/min, but the pierced specimens were reprocessed and cooled at a faster cooling rate (9°C/min). There was no forced cooling ability during the manufacturing or TAP processes. The larger thermal mass of the platens used for panel manufacture inherently led to a slower cooling rate. Although the crystallinity of PEEK laminates can be significantly influenced by a change in cooling rate (as highlighted in Chapter 2), previous studies by Gao and Kim (2000, 2001a and 2001b) primarily looked into differences in cooling rates between 1°C/min and > 2000°C/min. Interpolation of literature data suggests that no more than a 2% difference in elastic modulus or yield strength is observed when varying the cooling rate from 2°C/min to 9°C/min (Gao and Kim, 2001a). This is not expected to affect the results within this investigation significantly, but it would need to be considered in future work if a modified piercing process included forced cooling.

Load (resistive force)/displacement data for the piercing process were recorded during piercing tests. Resistive force on the spike was expected to vary according to the viscosity of the matrix and the heated area, which affects how easily fibres are displaced. The spike geometry would govern the contact surface area between the laminate and the spike and was also likely to influence the response during piercing. The load/displacement response was used in this investigation to identify key process stages and how the displacement of material varied with the change of process variables. The responses could also be used to assess subsequent manufacturing conditions.

3.7 Load/Displacement Response of Spike during Piercing

3.7.1 Introduction
The load/displacement responses of the spike were measured during piercing. Typical responses when varying the heated area, processing temperature and spike geometry when piercing are discussed. Calculated projected contact areas between the spike and the laminate during piercing are presented to help identify the key stages of piercing in relation to
the geometry of the spikes. This helped with the analysis and comparison of load/displacement responses for the different piercing process variables.

3.7.2 Load/displacement response for varying diameter of heated area

Three sets of load/displacement data are plotted for the 30 mm, 45 mm and 60 mm heated area diameters to show the repeatability of the results (Figure 3.19 a) – c), respectively). The repeatability is good for all three heated areas tested and typical load/displacement responses for varying diameter of heated area are shown in Figure 3.19 d) (indicated with a dotted line on the repeatability figures).

The plots show how the resistive force on the spike first increases non-linearly with respect to the displacement. This is followed by a linear increase in force with displacement before a plateau (at a spike displacement of approximately 11 mm). A subsequent reduction of the force follows as the spike continues to move through the laminate. The data show a ‘knee’ of varying magnitude in the force at approximately 14 – 15 mm spike displacement and the force continues to decrease as the spike reaches the end of its stroke. The data for all three heated areas show steps in the resistive force on the spike (between approximately 18 – 22 mm spike displacements). A final frictional resistance on the circular spike shaft is then observed at the end of the piercing process.

There appears to be a difference in the magnitude of the ‘knee’ that occurs between approximately 14 mm and 15 mm spike displacement. Decreasing the heated area, to a diameter of 45 mm, increased the ‘knee’ to show a slight increase in force before reducing again. A similar, but enlarged, effect is shown for the smallest diameter of heated area (30 mm) where a larger increase in force is observed.
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Figure 3.19: Force/displacement response of spike when piercing with various diameters of heated area at a temperature of 400°C (dotted lines indicating data used for representative comparison) a) three repeated data sets for 30 mm diameter; b) three repeated data sets for 45 mm; c) three repeated data sets for 60 mm; d) typical responses for varying heated areas

When piercing the heated laminates with a conical spike (for all heated areas) there are four key stages of piercing. The projected contact area of the spike with the laminate during piercing was calculated and shows these key stages (Figure 3.20). The spike makes initial contact with the laminate and there is a non-linear increase in projected contact area with displacement (Stage 1). The projected contact area then linearly increases with displacement when the spike tip has emerged from the back of the laminate (Stage 2). The vertical length of the spike determines the displacement at which the maximum projected contact area is reached. When the displacement is equal to the vertical spike length, the parallel sides of the spike begin to enter the top of the laminate (Stage 3). The projected contact area then reduces to zero as the spike continues to exit the bottom face of the laminate. The final stage of the piercing process is the full exit of the spike from the back face of the laminate (Stage 4). At this point the projected contact area is zero and only the parallel sides of the cylindrical shaft are in contact with the laminate.
The calculated projected contact area shows a similar trend to the measured force/displacement response of the spike during piercing. The change in resistive force is evidently affected by the projected contact area of the spike with the laminate. There are additional effects recorded during piercing that indicate further interactions between the spike and the laminate. The ‘knee’ in the data, observed when the resistive force on the spike was decreasing, appears to occur at the same displacement for all heated areas. The displacement at which the ‘knee’ occurs matches, approximately, the point where the shoulder of the spike emerges from the back face of the laminate. It could, therefore, be caused by the final compaction of material into the tooling face recess and subsequent extrusion of excess material.

The magnitude of the ‘knee’ appears to decrease when increasing the diameter of the heated area. The change in heated area will affect the overall volume into which the fibres can be displaced. Increasing this volume may have reduced local fibre packing due to fibre displacement when piercing (Figure 3.21). Subsequently, the final compaction and extrusion
of the material as the spike shoulder exits the bottom of the laminate would require a lower force.

Figure 3.21: Schematic illustration of the reduction in local fibre volume fraction that results from fibre rearrangement and packing into a larger heated volume (assuming a constant volume fraction gradient between the hole edge and the heated area edge).

Compressing material into the clearance space between the spike and the hole in the tooling (Figure 3.22) may also contribute to the magnitude of the ‘knee’, but this remains constant for all tests throughout this investigation. The subsequent steps in the data are expected to be due to a ‘stick-slip’ occurrence as the spike increases in velocity with a reducing resistive force. As mentioned in the rig set-up, the spike actuator is pressure controlled so the spike displacement rate can vary during piercing.
The key stages in the force/displacement plots appear to occur at the same locations as the projected contact area plot. The piercing stages are shown to be primarily dictated by the laminate thickness and the geometry of the spike. The change in heated area affected the resistive force on the spike during the piercing stages. It is also worth noting that the laminate thickness was smaller than the vertical length of the spike for this investigation. Stages 2 and 3 would occur in reverse order if this was not the case and this would need to be considered when applying the process to thicker laminates or larger spike angles.

### 3.7.3 Load/displacement response for varying process temperature

Three sets of load/displacement data are plotted for the 380°C, 400°C and 450°C processing temperatures to show the repeatability of the results (Figure 3.23 a) – c), respectively. The repeatability is good for all three tested heated areas and typical spike load/displacement responses for different piercing process temperatures are shown in Figure 3.23 d) (indicated with a dotted line on the repeatability figures). Similar features can be seen for the three plots. There appears to be no significant differences in the process stages identified previously.
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Figure 3.23: Force/displacement response of spike when piercing with various processing temperatures and a heated area diameter of 60 mm (dotted lines indicating data used for representative comparison) a) three repeated data sets for 380°C; b) three repeated data sets for 400°C; c) three repeated data sets for 450°C; d) typical responses for varying temperatures

Varying the piercing process temperature will change the viscosity of the matrix and is shown to affect the resistive forces on the spike during piercing. The initial resistance to deformation is shown to vary across all the processing temperatures tested. The lowest processing temperature (380°C) shows the most significant increase in resistive force on the spike with displacement. Increasing the temperature to 400°C shows a reduction in the initial rate of force increase with displacement and a decrease in maximum force. Increasing the processing temperature to 450°C shows a further reduction in the rate of force increase and a more significant reduction in peak force.

The diameter of the heated area was shown to have a more significant effect on the magnitude of the knee than the processing temperature. The viscosity of the matrix will have reduced with increasing processing temperature and allow the matrix to be displaced (and extruded)
more easily, reducing the knee. This has not, however, affected the magnitude of the knee as significantly as the change in heated area.

The peak force is shown to reduce when increasing the processing temperature during piercing (following the same trend as the initial rate of increasing force). The viscosity of the matrix would have reduced for the higher processing temperatures. Reducing the viscosity increases the mobility of the matrix and allows it to flow more easily when displaced by the spike. It would also allow the fibres to move more easily within it and lead to the overall reduction in maximum forces required for piercing – as shown in the data.

3.7.4 Load/displacement response for varying spike geometry
The final process parameter varied within the current investigation was the spike geometry. The load/displacement data was recorded for conical spike geometries of 20°, 30° and 40° spike angles and an ogive geometry. The key piercing stages identified previously were done so for conical spike geometries. Ogive spikes have the same four process stages, but the projected area in contact with the laminate varies significantly from that of a conical spike during piercing (Figure 3.24).

The initial projected contact area for the ogive spike (Stage 1) increases in a similar, non-linear, fashion to the conical spike. When the ogive spike tip exits the bottom of the laminate, however, the increase in projected contact area does not become linear (as seen for the conical spike). The projected contact area immediately starts to plateau and reaches a maximum at approximately 7.5 mm spike displacement, half the vertical spike length (Stage 2). The projected contact area then reduces at an increasing rate up to the point where the shoulder of the spike meets the top of the laminate. After this point the projected contact area decreases at a reducing rate (Stage 3). This occurs until the parallel edges of the cylindrical spike have reached the bottom of the laminate and the projected contact area has
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reduced to zero. The final stage of the piercing process (Stage 4) is the frictional contact between the spike shaft and the hole edge, as shown for the conical spikes.

Figure 3.24: Schematic of ogive spike projected contact area during TAP stages

Three sets of load/displacement data are shown for the 20°, 30°, 40° and ogive spike geometries in Figure 3.25 a) – d), respectively. The repeatability of each data set is shown to be good and typical load/displacement responses for the tested spike geometries are shown in Figure 3.25 e) (indicated with a dotted line on the individual figures). The data show the various piercing stages occurring at different spike displacements, governed by the spike geometry (as described for conical and ogive spikes). For the conical spikes, reducing the spike angle (or increasing the sharpness of the spike) resulted in the piercing stages occurring at larger spike displacements. The same relationship for the projected contact area is shown for spike geometry (Figure 3.26).
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Figure 3.25: Force/displacement response of spike when piercing with various spike geometries and a heated area diameter and processing temperature of 30 mm and 400°C, respectively (dotted lines indicating data used for representative comparison) a) three repeated data sets for a 20° spike; b) three repeated data sets for a 30° spike; c) three repeated data sets for a 40° spike; d) three repeated data sets for an ogive spike; e) typical responses for various spike geometries
The maximum projected contact area of the spike during piercing will increase for increasing spike angles. Consequently, the peak force (or maximum resistance to piercing) should follow this trend and show higher maximum forces for the larger spike angles. This is the case for the conical spikes if the maximum force is taken at the first plateau in the data. The 20° spike, however, shows a significant increase in resistive force at the knee that was discussed previously. It appears that although the early resistive force on the spike reduces for increasing spike angle, the compaction and extrusion of material is delayed and subsequently increased further at the point where the spike shoulder exits the bottom of the laminate. This is also shown for the ogive spike, which has a tangential transition from the spike angle to the shaft surfaces but shows the largest compaction and extrusion force required at the end of the process.

3.8 Concluding Remarks
The TAP rig was built with flexibility to test some of the key process variables identified when reviewing previous related literature. The heated area, processing temperature and spike geometry when piercing were chosen to be varied within the current investigation. The variable settings were selected based on the composite material supplier recommendations, testing rig capabilities, early approximations and relevant literature.
Calculated projected contact areas between the spike and laminate when piercing helped identify key piercing stages that were confirmed by the measured force/displacement responses. The data also showed an additional, final compaction and extrusion, stage during the piercing process that may be significant to the resultant mechanical performance of the pierced specimens.

The force/displacement responses varied significantly for the variables tested. The overall relationship between the force/displacement responses and variation of the diameter of heated area, processing temperature and the spike geometry is summarised in Table 3.2. The ogive spike is considered to have the smallest spike angle and approximately follows the summarised trends.

<table>
<thead>
<tr>
<th>Table 3.2: Summary of spike forces when varying piercing process variables</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Initial peak force</strong> (at 1st plateau)</td>
</tr>
<tr>
<td>Increasing heated area</td>
</tr>
<tr>
<td>Increasing temperature</td>
</tr>
<tr>
<td>Increasing spike angle</td>
</tr>
</tbody>
</table>

The next chapter presents and discusses the material microstructure that is produced by the piercing process. The techniques used to analyse the specimens are discussed and representative pierced specimens are compared with conventionally drilled specimens to highlight the difference in structure as a result of the process. The material microstructure, including fibre orientation and volume fraction, will affect the mechanical performance of specimens and this is discussed in relation to the findings.
4 Characterisation of Material Microstructure of Pierced Laminates

4.1 Introduction
The development of the TAP rig and the range of piercing variables were described in the previous chapter. The load/displacement responses of the spike, measured in the previous chapter, suggested that the resultant fibre microstructure after piercing varied with the piercing parameters such as the diameter of heated area and processing temperature. This chapter analyses the microstructure after piercing with various process conditions and discusses this in relation to the previous results. Optical microscopy and X-ray Computed Tomography (CT) were used to image the specimens for analysis and provide details of the material microstructure. These methods are described in the next section. The microstructure of a representative pierced specimen is then discussed and compared with that of a drilled specimen as a reference. Finally, the effect of changing the diameter of the heated area and the processing temperature when piercing on the material microstructure is presented and discussed.

4.2 Analysis Methods

4.2.1 Optical Microscopy
Optical microscopy was carried out at TWI using an Olympus BX41M-LED microscope. The specimens used for microstructural analysis were sectioned and polished (following the procedure outlined in Table 4.1) along the $0^\circ$ and $90^\circ$ ply directions to reveal the local fibre structure from the edge of the hole and through the heated volume, as shown in Figure 4.1. The photomicrographs were used to observe local fibre structure, void content and resin rich regions. The fibre volume fraction was determined from the photomicrographs and using image analysis software (ImageJ). The specimens were manufactured into a cross-ply configuration (as mentioned previously), which varied the ability to polish the fibres. When the fibres were parallel to the section plane (the $0^\circ$ plies), the surface finish was excellent (see the fibres as long ellipses in Figure 4.2), while the fibres perpendicular to the section plane (the $90^\circ$ ply) did not show quite such a good finish (also shown in Figure 4.2).
Table 4.1: Grinding a polishing procedure for pierced and drilled specimens prior to microscopy analysis

<table>
<thead>
<tr>
<th>Time</th>
<th>Force / sample (N)</th>
<th>Alumina grinding paper grit size</th>
<th>Wheel speed (rpm) (clockwise)</th>
<th>Specimen speed (rpm) (anti-clockwise)</th>
</tr>
</thead>
<tbody>
<tr>
<td>As required to polish to desired section plane</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>30</td>
<td>120</td>
<td></td>
<td>100</td>
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<tr>
<td>30</td>
<td>4000</td>
<td></td>
<td>100</td>
<td>100</td>
</tr>
</tbody>
</table>

Polishing

<table>
<thead>
<tr>
<th>Time (s)</th>
<th>Force / sample (N)</th>
<th>Polishing wheel</th>
<th>Lubricant / diamond compound</th>
<th>Wheel speed (rpm) (clockwise)</th>
<th>Specimen speed (rpm) (clockwise)</th>
</tr>
</thead>
<tbody>
<tr>
<td>30</td>
<td>45</td>
<td>Short nap cloth</td>
<td>3 micron</td>
<td>100</td>
<td>100</td>
</tr>
<tr>
<td>30</td>
<td>45</td>
<td>Short nap cloth</td>
<td>1 micron</td>
<td>100</td>
<td>100</td>
</tr>
<tr>
<td>300</td>
<td>20</td>
<td>Napless cloth</td>
<td>0.25 micron finish OPS</td>
<td>100</td>
<td>100</td>
</tr>
</tbody>
</table>

Figure 4.1: Schematic diagram showing the 0° and 90° cutting planes for the sectioned specimens
The reduction in polishing quality of the perpendicular fibres limited the ability of the image analysis software when thresholding to create a binary image of fibres and matrix, as shown in Figure 4.3, even though the image was cropped so the analysis was only conducted on the perpendicular fibres. An alternative method to determine the volume fraction was to determine the centroid location for each of the fibres and use these points to create artificial fibres for analysis. The artificial fibres were plotted, according to the centroid locations, and resized until the image matched the original photomicrograph, Figure 4.4 a) and Figure 4.4 b). The artificial fibre plots were used to determine the approximate fibre volume fraction within drilled and pierced specimens at various locations along the cross section.

**Figure 4.2:** Photomicrograph of cross-ply laminate showing the difference in polishing quality depending on the fibre orientation

**Figure 4.3:** Binary image of fibres perpendicular to the section plane (inverted to show the black fibres)
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Figure 4.4: Images of fibre representations used for fibre volume fraction determination a) artificial fibres representing the true fibre distribution; b) artificial fibre outlines overlaid on photomicrograph

When plotting the artificial fibres there were areas of overlap where multiple fibres are plotted in close proximity. This arose where one fibre appeared as two fragments in the binary image and, therefore, provided two centroid locations. Both locations were replotted as separate overlapping artificial fibres. While this phenomenon was recognised, in order to keep the process consistent for all the analysed images the duplicated fibres were not removed manually.
Residual fibre strains, as a result of piercing, were approximated in the previous chapter and the calculation suggested that some fibres (at the hole edge) were likely to fracture during the process. The polished specimens only show the fibres across a two-dimensional plane. It was, therefore, not possible to find fibre fractures using this method. Instead, a burn off test was conducted on a specimen pierced with the largest heated area diameter. A specimen with the largest diameter of heated area (60 mm) was loaded into a furnace at 600°C and visually inspected every 10 minutes until the PEEK resin was no longer present (this occurred after 40 minutes). The fibres remained for visual and microscopic inspection.

4.2.2 X-Ray Computed Tomography
The X-ray CT analysis was conducted on a Zeiss XRADIA 520 Versa machine and using the AVIZO software. Sub-fibre diameter resolution (<10 μm) was required for this analysis to image the fibre paths and orientations after piercing. The size of the specimen, however, needed to be as large as possible to image a significant region over which to observe the fibre structure.

The specimen was cut from a laminate pierced using a 30 mm diameter heated area, 400°C processing temperature, 30° spike geometry and 0.8 MPa clamping pressure (type A3, listed in Chapter 3). The overall dimensions of the specimen were approximately 3 mm x 3 mm x 3 mm and incorporated the edge of the hole, as shown in Figure 4.5.

Figure 4.5: Schematic diagram of the specimen used for X-ray CT analysis
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The X-ray CT set-up is shown in Figure 4.6. An X-ray source directs the X-rays through the specimen and onto a scintillator, which is imaged using a camera system. The specimen is rotated and a separate image is taken at each rotation angle (details below). The series of images are used to compute a three-dimensional volume for analysis. This three-dimensional volume is cylindrical in shape due to the rotation of the specimen.

![X-ray CT set-up](image)

**Figure 4.6: Schematic diagram of X-ray CT set-up and computed data**

The scan settings were selected based on some initial trials to give the best image resolution with minimum noise. The source voltage and current were 40 kV and 74 µA, respectively. The specimen was placed approximately 15 mm from the source and approximately 19 mm from the detector. The settings resulted in a pixel size of approximately 1.5 µm (within the fibre diameter). The exposure time was set to 8 seconds and the specimen was imaged 3201 times over a 360° rotation. This resulted in a total scan time of 12.37 hours.

4.3 Material Microstructure of Pierced Laminates

When drilling a hole in a composite laminate the resulting fibre structure surrounding the hole remains unchanged. The fibres will remain in a 0° and 90° orientation for the laminates used
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in the current investigation, as shown schematically in Figure 4.7 a). When piercing a specimen the fibres are displaced, which modifies the local material microstructure surrounding the hole, as shown schematically in Figure 4.7 b).

![Figure 4.7: Schematic diagram of the resultant fibre structure in a composite laminate a) for drilled specimens; b) for pierced specimens](image)

Visual inspection of pierced specimens showed a ‘cross’ feature that suggests internal changes in microstructure, Figure 4.8 a). This is verified by a two-dimensional X-ray of the hole that shows lighter regions containing fewer fibres and darker regions where the fibres have been compacted, Figure 4.8 b).

![Figure 4.8: Typical pierced hole (6 mm diameter) showing changes in the microstructure a) photograph showing external visual features; b) two-dimensional X-ray showing displacement of fibres around the hole](image)
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Optical photomicrographs of the drilled specimens, at 0° and 90° section planes (Figure 4.9 a and b), show that the fibre orientations remain unaltered up to the edge of the drilled hole. The fibres orientated in the same direction as the sectioned plane are preferentially polished along their lengths and show as brighter features within the image (as mentioned previously). Fibres perpendicularly orientated to the sectioned plane were not polished as readily and, therefore, appear in darker layers (plies) or regions within the images.

The resultant microstructure observed within a pierced specimen sectioned along the 0° and 90° planes is shown in Figure 4.9 c) and d), respectively. Significant ply distortions are evident and the plies have clearly been deformed in the z-axis (through the thickness) of the laminate, with the travelling direction of the spike. The resultant effect of the TAP process is a fibre structure that has to accommodate the deformation of material as the spike travels through the thickness of the laminate. The 0° and 90° plies were distorted to accommodate the displacement of fibres and matrix during the piercing process. When sectioned along the 0° plane, the 0° plies were shown to significantly reduce in thickness towards the hole edge. This is coupled with the local thickening of the 90° plies to fill the volume.
Two mechanisms caused the observed resultant fibre structure. Firstly, the 0° fibres were displaced from their original position at the centre of the hole (location 1 on Figure 4.10). These fibres were bunched and compressed against the edge of the spike and gather in volume as the spike displaces more material (location 2 on Figure 4.10). The increased volume of fibres built up at the edge of the spike at location 2 was reciprocated with a reduction in material at location 3. Secondly, at location 3 the 0° ply fibres were displaced in both the x and z directions. For the current case, a cross-ply laminate, the reduction of material in one location for 0° plies was complementary to the build-up of material in the 90° plies in the same x-y...
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location. This resulted in the structure observed - thinning and thickening of alternate plies near the hole edge.

![Diagram of the piercing process with indicated locations 1, 2, and 3, looking at the x-y plane and z-y plane.]

Figure 4.10: Schematic diagram of the piercing process, with indicated locations 1, 2 and 3, looking at the x-y plane and z-y plane

X-ray CT data was reconstructed into a three-dimensional volume showing the resultant material microstructure after piercing. Imaging software allowed the data to be viewed in x-z, y-z or x-y planes, as shown in Figure 4.11 a), b) and c) respectively. The CT images confirm the suggestions made using the microscopy and show that the fibres (shown in white) in one ply are displaced around the hole to leave resin rich regions (shown in black). These resin rich regions, however, are filled with displaced fibres from the adjacent plies near the edge of the hole (as suggested from the microscopy).
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Figure 4.11: Schematic diagrams and CT images of a pierced specimen (sections taken at approximate mid-planes in all directions) a) cut along the x-z plane; b) cut along the y-z plane; c) cut along the x-y plane

The advantage of the CT reconstruction is that multiple axes can be viewed and analysed quickly and easily to reveal the internal structure of the specimen. The fibre structure close to the hole edge in the y-z plane is shown in Figure 4.12, whilst also showing how this corresponds with the fibre structure in the x-z and x-y planes. The images show significant ply deformation in the z-axis at the hole edge, shown in Figure 4.12 a). The plies appear to be
spread down the hole edge in the direction of the piercing spike, in addition to the lateral displacement around the hole.

The images of the microstructure at approximately 0.25 mm and 0.5 mm from the hole edge show that the plies become significantly less distorted in the z-axis and the displacement eventually becomes completely in the x and y plane, shown in Figure 4.12 b) and c). This agrees with the structure shown in the previous micrographs, Figure 4.9 c) and d).

This z-axis displacement of the fibres was not considered in the fibre strain approximation in the previous chapter and may contribute to the tendency for fibre fracture. These fibre fractures can be seen, even for the largest diameter of heated area, once the PEEK matrix was burnt off to reveal the fibres (Figure 4.13). The original locations of the individual fibres were not possible to determine as they were likely to move when not held together by the matrix. The fractured fibres, however, all appear to be located in close proximity to the hole edge (which agrees with the CT data).
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Figure 4.12: Multi-plane X-ray CT images of a pierced specimen with schematic diagrams of the y-z cut plane locations a) at the hole edge; b) 0.25 mm (approximately) from the hole edge; c) 0.5 mm (approximately) from the hole edge
The fractured fibres shown in the pierced specimen were likely to be caused by elongation as the path length increased around the hole or compression of the fibres when in contact with the spike. It is suggested that the number of fractured fibres as a result of piercing will be lower than the total number of fibres cut when drilling (or as a result of any other traditional machining technique).

The displacement of material when piercing was shown to compact fibres into the volume surrounding the hole in the two-dimensional X-ray, Figure 4.8 b). The fibre volume fractions of drilled and pierced specimens were calculated, as described previously, using an image analysis technique on 90° sections. A total of 15 images were analysed for each specimen. The image locations are shown in Figure 4.14 for a drilled and pierced specimen. The pierced specimen had a heated area diameter of 30 mm (specimen type A3 – see Chapter 3 for the full piercing settings). The locations incorporated areas near the hole edge, within the heated
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area and outside of the heated area. Three through thickness locations were used to provide additional data at each distance from the hole centre.

The calculated fibre volume fractions are shown in Figure 4.15 at discrete locations from the hole centre. Linear trend lines were used to show how the fibre volume fraction varied across the specimens. The fibre volume fraction data was averaged for each distance from the hole centre and shown in Table 4.2. An overall average fibre volume fraction within the drilled specimen is also shown in the data. This was used to verify the accuracy of the technique when compared to the manufacturer specified fibre volume fraction for the material (59%).

![Figure 4.14: Schematic diagram showing the locations used for volume fraction analysis of drilled and pierced specimens along 90° sections](image)
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![Graph showing fibre volume fraction versus distance from hole centre for drilled and pierced specimens.]

**Figure 4.15**: Measured fibre volume fraction using microscopy and image analysis software at various locations of drilled and pierced specimens

**Table 4.2**: Average fibre volume fraction determined using microscopy and image analysis at various locations from the hole edge (with standard errors)

<table>
<thead>
<tr>
<th>Distance from the hole centre [mm]</th>
<th>20</th>
<th>15</th>
<th>10.5</th>
<th>6</th>
<th>3.25</th>
<th>Average</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fibre volume fraction (%)</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Drilled</td>
<td>59 ±1</td>
<td>56 ±1</td>
<td>57 ±2</td>
<td>55 ±3</td>
<td>61 ±1</td>
<td>58 ±1</td>
</tr>
<tr>
<td>Pierced</td>
<td>58 ±1</td>
<td>54 ±1</td>
<td>59 ±3</td>
<td>63 ±2</td>
<td>72 ±2</td>
<td></td>
</tr>
</tbody>
</table>

The fibre volume fraction values of the drilled specimen show that the average value using the artificial fibre analysis technique agreed with the ply fibre volume fraction quoted by the manufacturer (58% for the microscopy analysis compared with 59% quoted by the manufacturer). The data also show that the fibre volume fraction of the drilled specimen did not significantly increase towards the hole edge and the trend line remained approximately constant at 58% fibre volume fraction. The pierced specimens, however, showed a significant increase in the fibre volume fraction towards the hole edge. The average fibre volume fraction was measured to be approximately 58% outside of the heated area and increased to approximately 72% near the hole edge. The significant increase in fibre volume fraction close to the hole edge was likely to have a positive impact on the mechanical performance of the specimens under loading. Increasing the local fibre volume fraction will increase the local stiffness of the material. The piercing process, however, displaces fibres from other locations...
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within the specimen, which could reduce the fibre volume fraction in these areas. This could decrease the local stiffness and have a negative effect on the mechanical performance.

In addition to local effects throughout the laminate, the overall thickness of the region close to the hole must increase to accommodate the displaced material, as shown in Figure 4.9 c) and d). A recess in the tooling jig allowed for the additional material to form a local thickness increase around the pierced hole. The dimensions of this volume could, in principle, be altered according to requirement by varying the dimensions of the recess (e.g. countersunk or hemispherical in geometry).

4.4 Material Microstructure for Varying Piercing Process Settings

4.4.1 Varying Heated Area
The resultant fibre structure of the pierced laminates, exposed through 90° cross-sections, is shown to change when varying the heated area in Figure 4.16 a) – c). Increasing the heated area appears to increase the formation of resin rich regions and voids within the laminate structure, which can be associated with an overall decrease in fibre volume fraction near the hole. The resin rich regions and voids were caused by the displacement of fibres from these regions, not by a thickness increase (an increase in laminate thickness was not observed). This agrees with the instrumented spike data, which suggested a reduction in fibre packing (showed by a decrease in the resistive force at the knee when piercing) with increasing diameter of heated area.
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Figure 4.16: Photomicrographs of 90° sections through pierced specimens for varying diameter of heated area at 400°C processing temperature a) 30 mm diameter heated area; b) 45 mm diameter heated area; c) 60 mm diameter heated area

Increasing the diameter of the heated area, however, is likely to reduce the strain imposed on the displaced fibres. This was suggested in the fibre strain calculations in the previous chapter. As a consequence, the number of fractured fibres is likely to reduce with increasing heated area. A reduction in fibre fractures should improve the mechanical performance.

4.4.2 Varying Processing Temperature
The fibre structure of pierced specimens exposed through 90° cross-sections is shown for varying processing temperature in Figure 4.17 a) – c). Increasing the processing temperature when piercing appears to increase the formation of resin rich regions and voids within the laminate structure. Increasing the processing temperature, and reducing the resin viscosity, will promote the ease of fibre displacement, which was suggested by the force/displacement response of the spike during piercing (a reduction in peak force at 11 mm spike displacement).
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Increasing the ease of movement, or mobility, of the fibres is likely to lead to larger fibre displacement and a reduction in fibre fractures – improving the mechanical performance. The restricted fibre movement at low processing temperatures will increase the local strain in fibres displaced by the spike. As a consequence, more of these fibres are likely to have fractured. The increase in resin rich regions and void content for increasing temperature, however, is likely to reduce the mechanical performance.

4.5 Concluding Remarks
Optical microscopy and X-ray CT analysis show that the microstructure of the cross-ply laminates changes significantly as a result of piercing. There was a significant displacement of material in the x, y and z directions of the laminate. Fibres were displaced from the hole region into the surrounding volume to increase the local fibre volume fraction. The
displacement of fibres from other locations, however, leads to areas of low fibre volume fraction, resin rich regions and voids.

The effect of increasing the diameter of the heated area and processing temperature on the microstructure are summarised in Table 4.3. The variation of microstructure was expected to affect the mechanical performance of pierced specimens significantly. The next chapter presents and discusses the results of open-hole testing of pierced and drilled specimens. The differences in resultant microstructure after both methods of creating holes is commented on with respect to the mechanical performance. The effect of varying the heated area and processing temperature on the mechanical performance is also presented.

<table>
<thead>
<tr>
<th>Change of process variable</th>
<th>Microstructural feature</th>
<th>Effect on feature</th>
</tr>
</thead>
<tbody>
<tr>
<td>Increasing heated area</td>
<td>Voids and resin rich regions</td>
<td>↑↑</td>
</tr>
<tr>
<td></td>
<td>Fibre volume fraction</td>
<td>↓↓</td>
</tr>
<tr>
<td></td>
<td>Fibre fractures</td>
<td>↓</td>
</tr>
<tr>
<td>Increasing temperature</td>
<td>Voids and resin rich regions</td>
<td>↑</td>
</tr>
<tr>
<td></td>
<td>Fibre volume fraction</td>
<td>↓↓</td>
</tr>
<tr>
<td></td>
<td>Fibre fractures</td>
<td>↓↓</td>
</tr>
</tbody>
</table>
5  Mechanical Properties of Pierced Laminates Under Open-Hole Loading

5.1  Introduction
In this chapter, open-hole tension and compression tests have been used to investigate and compare the mechanical performance of pierced specimens with drilled specimens. The heated area and processing temperature were varied for these open-hole tests. The effect of changing spike geometry was investigated using tests more relevant to joining applications (discussed subsequently in Chapter 7). Varying the heated area and processing temperature were assumed to have a larger impact on the overall specimen performance since a considerable proportion of the specimen was affected. The piercing spike will only make contact with the local material surrounding the hole during piercing. The spike geometry was, therefore, expected to have more impact on the performance of the holes under joint loading applications.

This chapter presents the tension and compression results separately with respective summaries of the findings. The set-up and procedures are presented, force/displacement responses are discussed and the ultimate strength of pierced specimens are shown, as percentages of drilled specimen strength (for comparison). The results are discussed in relation to the force/displacement response of the spike during piercing and resultant structure discussed previously in Chapters 3 and 4. A final optimisation study was conducted based on the test results. The most favourable piercing conditions were chosen and the results are compared with the other tests within this chapter.

5.2  Open-Hole Tension Properties of Pierced Laminates

5.2.1  Open-Hole Tension Specimen Manufacture and Geometry
Open-hole tensile testing was undertaken following the test standard ASTM 5766/D5766M/11 (ASTM, 2011). The same specimen material and manufacturing method were used as described in Chapter 3 (cross-ply CF/PEEK, approximately 2.9 mm thick). The diameters of
heated area and processing temperatures used for pierced specimen manufacture were presented in Chapter 3. A reduced version of the piercing process variables table is presented here for ease of reference showing the specimens used for open-hole testing, Table 5.1. Nominal specimen dimensions for tension testing are shown in Figure 5.1. The drilled specimens were cut to size and then centrally machined to produce 6 mm holes so as to give a width/hole diameter ratio (W/d) of 6, requiring a specimen width of 36 mm. Pierced specimens of the same geometry were manufactured by first piercing the panel in multiple locations and then cutting the specimens from the panel. If the pierced specimens were pre-cut, as for the drilled specimens, then the larger heated areas would extend outside of the specimen geometry.

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Piercing process variables</th>
<th>Heated area diameter (D – 2D)</th>
<th>Processing temperature (T – 1.20T)</th>
<th>Piercing spike angle (θ - 2θ)</th>
<th>Clamping pressure (P)</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1</td>
<td>Drilled</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>T1</td>
<td>60 mm (2 D)</td>
<td>380°C (T)</td>
<td>30° (1.5 θ)</td>
<td>0.8 MPa (P)</td>
<td></td>
</tr>
<tr>
<td>T2 (A1)</td>
<td>60 mm (2 D)</td>
<td>400°C (1.05 T)</td>
<td>30° (1.5 θ)</td>
<td>0.8 MPa (P)</td>
<td></td>
</tr>
<tr>
<td>T3</td>
<td>60 mm (2 D)</td>
<td>450°C (1.20 T)</td>
<td>30° (1.5 θ)</td>
<td>0.8 MPa (P)</td>
<td></td>
</tr>
<tr>
<td>A2</td>
<td>45 mm (1.5 D)</td>
<td>400°C (1.05 T)</td>
<td>30° (1.5 θ)</td>
<td>0.8 MPa (P)</td>
<td></td>
</tr>
<tr>
<td>A3</td>
<td>30 mm (D)</td>
<td>400°C (1.05 T)</td>
<td>30° (1.5 θ)</td>
<td>0.8 MPa (P)</td>
<td></td>
</tr>
</tbody>
</table>
5.2.2 Open-Hole Tension Testing Procedure

Five specimens were tested for each specimen group. Specimens were un-tabbed with a clamped length of 50 mm in each grip of the test machine, leaving a gauge length of 100 mm, Figure 5.1. A 250 kN Instron (Model no. 2527-113) with parallel hydraulic grips, Figure 5.2, was used for the tests. The applied force and cross-head displacement were measured for all tests. The measured cross-head displacement was used for all displacement measurements that are presented within this report. A constant displacement rate of 1 mm/min was applied until ultimate failure of each specimen.
5.2.3 Force/Displacement Response of Pierced Laminates Under Tensile Load

The force/displacement responses for drilled and pierced specimens during open-hole tension testing are shown in Figure 5.3 and Figure 5.4. The testing data for drilled specimens and pierced specimens with varying diameter of heated area (30 mm, 45 mm and 60 mm) are shown in Figure 5.3 a) – d), respectively. Representative specimen plots (indicated with a dashed line) from each data set were plotted on a single axis for comparison, Figure 5.3 e). The majority of the data show an almost linear increase in force with displacement until a catastrophic, and ultimate, failure of the specimen.
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Figure 5.3: Force/displacement plots of open-hole tensile tests for drilled specimens and specimens with varying diameter of heated area and constant processing temperature (400°C) (dotted lines indicating data used for representative comparison) a) Drilled specimens; b) 30 mm diameter; c) 45 mm diameter; d) 60 mm diameter; e) Typical representative plots.

The drilled specimen results, in Figure 5.3 a), fall into two groups. The reason for this is that open-hole tensile tests were conducted in the initial feasibility trials of the project and used an identical test set-up except for the gripping method. Wedge action grips were used during preliminary tests and the difference in compliance is shown in the data for the specimens tested (leading to ultimate failure displacements in excess of 3 mm). Subsequent testing of two specimens, as part of the complete testing programme, showed similar ultimate failure forces to the earlier tests and provided data for comparison with the pierced specimens.
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The force/displacement data show good repeatability throughout the specimen groups in terms of failure force, displacement at failure and stiffness. The mean stiffness for each specimen type are shown in Table 5.2. The stiffness of each specimen was measured between an applied force of 10 kN and 35 kN. The stiffness of the 60 mm and 45 mm heated area diameters are similar to the drilled specimens (38-40 kN/mm). The specimens pierced with a 30 mm diameter heated area reduced slightly in stiffness (to 35 kN/mm).

Table 5.2: Mean specimen stiffness for pierced specimens with varying heated area and drilled specimens during open-hole tension tests (with standard errors)

<table>
<thead>
<tr>
<th>Diameter of heated area</th>
<th>Stiffness (kN/mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drilled</td>
<td>38.4 ± 0.7</td>
</tr>
<tr>
<td>30 mm</td>
<td>34.6 ± 0.9</td>
</tr>
<tr>
<td>45 mm</td>
<td>40.4 ± 0.4</td>
</tr>
<tr>
<td>60 mm</td>
<td>38.5 ± 0.7</td>
</tr>
</tbody>
</table>

Figure 5.4 shows the force/displacement responses of drilled and pierced specimens for varying processing temperature and constant heated area. They are presented in a similar way to the previous sets of specimens. The testing data for drilled specimens and pierced specimens with varying processing temperature (380°C, 400°C and 450°C) are shown in Figure 5.4 a) – d), respectively. The drilled data are repeated for ease of comparison. The representative plots, as described previously, were plotted on the same axis and are shown in Figure 5.4 e).

The data show similar results to those for varying diameter of heated area. The repeatability of each specimen group is good and the comparison plots, in Figure 5.4 e), show similar stiffness for the pierced and drilled specimens for all variable settings. The mean stiffness of each specimen type are also shown to be similar in Table 5.3 (stiffness measured over the same range as previously measured, 10 kN to 35 kN). This suggests that any change in the material structure is not significantly affecting the deformation of the overall specimens under
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loading. Any differences in strength, therefore, are likely to be caused by architectural changes in local, stress concentrated, regions.

Figure 5.4: Force/displacement plots of open-hole tensile tests for drilled specimens and specimens with varying processing temperature and constant diameter of heated area (60 mm) (dotted lines indicating data used for representative comparison) a) Drilled specimens; b) 380°C; c) 400°C; d) 450°C; e) Typical representative plots.
Table 5.3: Mean specimen stiffness for pierced specimens with varying processing temperature and drilled specimens during open-hole tension tests (with standard errors)

<table>
<thead>
<tr>
<th>Processing temperature</th>
<th>Stiffness (kN/mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drilled</td>
<td>38.4 ± 0.7</td>
</tr>
<tr>
<td>380 °C</td>
<td>37.4 ± 0.8</td>
</tr>
<tr>
<td>400 °C</td>
<td>38.5 ± 0.7</td>
</tr>
<tr>
<td>450 °C</td>
<td>38.3 ± 0.7</td>
</tr>
</tbody>
</table>

5.2.4 Strengths of Pierced Laminates Under Tensile Load

Figure 5.5 shows the open-hole tensile strengths for the TAP specimens compared to drilled specimens; the results are summarised in Table 5.4. Open-hole tensile strength increased by between 1% and 10% for pierced specimens when compared with drilled. For a 30 mm diameter heated area, the TAP specimens had a strength 5% higher than the drilled specimens; this remained the same for a 45 mm diameter heated area, but reduced to only 1% improvement for a 60 mm diameter heated area.

![Figure 5.5: Open-hole tensile strengths of pierced specimens as a percentage of drilled specimen strength showing the effects of varying the diameter of the heated area and processing temperature (with standard error bars).](image)

The effect of varying the piercing process temperature on the ultimate tensile strength of specimens is also shown in Figure 5.5 and Table 5.4. The lowest processing temperature (380°C) shows a pierced specimen strength with a 6% improvement over the drilled strength.
Increasing the piercing process temperature to 400°C showed a reduction in the strength improvement to 1%. A further increase in processing temperature to 450°C, however, resulted in an increase in pierced specimen strength to a 10% improvement of the drilled specimen strength.

Table 5.4: Ultimate tensile strength of pierced and drilled specimens, also calculated as percentages of drilled specimen strength (with standard errors).

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Heated area diameter</th>
<th>Processing temperature</th>
<th>Open-hole tensile strength (MPa)</th>
<th>Open-hole tensile strength (% of drilled)</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1 (Drilled)</td>
<td>-</td>
<td>-</td>
<td>412 ± 7</td>
<td>100 ± 2</td>
</tr>
<tr>
<td>A3</td>
<td>30 mm</td>
<td>400°C</td>
<td>431 ± 7</td>
<td>105 ± 2</td>
</tr>
<tr>
<td>A2</td>
<td>45 mm</td>
<td>400°C</td>
<td>431 ± 6</td>
<td>105 ± 2</td>
</tr>
<tr>
<td>T1</td>
<td>60 mm</td>
<td>380°C</td>
<td>436 ± 8</td>
<td>106 ± 2</td>
</tr>
<tr>
<td>T2 (A1)</td>
<td>60 mm</td>
<td>400°C</td>
<td>415 ± 7</td>
<td>101 ± 2</td>
</tr>
<tr>
<td>T3</td>
<td>60 mm</td>
<td>450°C</td>
<td>454 ± 13</td>
<td>110 ± 3</td>
</tr>
</tbody>
</table>

The results show that the open-hole tensile strength has similar sensitivity to varying heated area as it does for varying processing temperature when piercing. A small reduction in fibre fractures when increasing the heated area is likely to have offset the reduction in strength due to reducing local fibre volume fraction (and increasing resin rich regions and void content). Increasing the heated area will have also increased the volume over which fibre misalignment and waviness occurs, which would have reduced the specimen strength.

Increasing the processing temperature before piercing is assumed to reduce the viscosity of the matrix and increase fibre mobility. The increased mobility of the fibres was shown to significantly increase the formation of voids and resin rich regions (section 4.4.2), hence reducing the local fibre volume fraction around the hole. This is likely to have reduced the strength of the specimens when increasing the processing temperature from 380°C to 400°C.
Increasing the mobility of the fibres, however, will also reduce fibre fracture, improving the open-hole tensile strength. It is possible that at the 450°C processing temperature there has been a shift in dominance from the reduction in strength due to a fibre volume fraction decrease to an increase in strength due to reduced fibre fractures.

5.2.5 Summary of Open-Hole Tension Properties of Pierced Laminates
The stiffness of pierced specimens did not significantly differ with varying diameter of heated area and processing temperature from that of drilled specimens. The open-hole tensile strength of pierced specimens was, however, affected by varying the diameter of heated area and processing temperature. The effects of changing the diameter of the heated area and processing temperature on the open-hole tensile strengths are summarised in Table 5.5.

Table 5.5: Summary of how increasing the diameter of the heated area and increasing the processing temperature when piercing affected the Open-Hole Tensile Strength (OHTS). (Brackets indicate the more dominant effect when increasing the processing temperature from 380°C to 400°C)

<table>
<thead>
<tr>
<th>Change of process variable</th>
<th>Effect on OHTS</th>
</tr>
</thead>
<tbody>
<tr>
<td>Increasing heated area</td>
<td>↓</td>
</tr>
<tr>
<td>Increasing temperature</td>
<td>↑ (↓)</td>
</tr>
</tbody>
</table>

5.3 Open-Hole Compression Properties of Pierced Laminates

5.3.1 Open-Hole Compression Specimen Manufacture and Geometry
Open-hole compression testing was undertaken following the standard BS ISO 12817:2013 (Method 2) (BSI, 2013a). The specimens were cut from panels manufactured as described previously for the tension tests. The diameters of heated area and processing temperatures used for specimen manufacture were the same as those re-iterated for the tensile testing procedure (Table 5.1). Nominal specimen dimensions for compression testing are shown in Figure 5.6. Using the same procedure as described for the tension specimens, drilled specimens were cut to size and then centrally machined to produce 6 mm holes so as to give
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a width/hole diameter ratio (W/d) of 6, requiring a specimen width of 36 mm. Pierced specimens of the same geometry were manufactured by first piercing the panel in multiple locations and then cutting the specimens from the panel.

![Schematic of nominal specimen dimensions for open-hole compression testing](image)

**Figure 5.6: Schematic of nominal specimen dimensions for open-hole compression testing**

5.3.2 Open-Hole Compression Testing Procedure

Five specimens were tested for each specimen group. Specimens were un-tabbed with a clamped length of 40 mm in each grip of the test machine, leaving a gauge length of 45 mm, Figure 5.6. The gauge length was chosen to ensure buckling did not occur. The same test machine was used for compression testing as described and shown for tensile testing (Figure 5.2). The applied force and cross-head displacement were measured for all tests. A constant displacement rate of 1 mm/min was applied until ultimate failure of the specimens.

5.3.3 Force/Displacement Response of Pierced Laminates Under Compressive Load

The force/displacement responses for drilled and pierced specimens during open-hole compression testing are shown in Figure 5.7 and Figure 5.8. The testing data for drilled
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specimens and pierced specimens with varying diameter of heated area (30 mm, 45 mm and 60 mm) are shown in Figure 5.7 a) – d), respectively. Representative plots (indicated using the same method as for the tension results - with a dashed line) from each data set were plotted on a single axis for comparison, Figure 5.7 e). The data show an almost linear increase in force with displacement until a catastrophic failure of the specimen.

The results show, similarly to the tension results, that there is good repeatability of the failure force, displacement at failure and stiffness for all the specimen groups. The mean stiffness for each specimen type are shown in Table 5.6. The stiffness of each specimen was calculated between an applied force of 15 kN and 30 kN. The stiffness of specimens does not vary significantly for the different heated areas and are also similar to those for the drilled specimens.

Figure 5.8 a) – d) show the testing data for drilled specimens and pierced specimens with varying processing temperature (380°C, 400°C and 450°C), respectively. The representative plots were plotted on the same axis and are shown in Figure 5.8 e). The data show, similarly to the previous results, repeatable failure force, displacement at failure and stiffness. The mean stiffness of the specimen types are shown in Table 5.7 and show similar stiffness for the drilled and pierced specimens for all combinations of heated area and temperature.

In agreement with the tensile test results, the similarity in stiffness under loading across all piercing conditions and for drilled specimens suggests no difference in the overall specimen deformation under loading. This suggests that the rearrangement in material structure during piercing is having a more dominant effect on the local strains in stress concentrated regions.
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Figure 5.7: Force/displacement plots of open-hole compression tests for drilled specimens and specimens with varying diameter of heated area and constant processing temperature (400 °C) (dotted lines indicating data used for representative comparison) a) Drilled specimens; b) 30 mm diameter; c) 45 mm diameter; d) 60 mm diameter; e) Typical representative plots.

Table 5.6: Mean specimen stiffness for pierced specimens with varying heated area and drilled specimens during open-hole compression tests (with standard errors)

<table>
<thead>
<tr>
<th>Diameter of heated area</th>
<th>Stiffness (kN/mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drilled</td>
<td>57.0 ± 0.5</td>
</tr>
<tr>
<td>30 mm</td>
<td>60.2 ± 0.9</td>
</tr>
<tr>
<td>45 mm</td>
<td>59.1 ± 0.9</td>
</tr>
<tr>
<td>60 mm</td>
<td>56.9 ± 1.2</td>
</tr>
</tbody>
</table>
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Figure 5.8: Force/displacement plots of open-hole compression tests for drilled specimens and specimens with varying processing temperature and constant diameter of heated area (60 mm) (dotted lines indicating data used for representative comparison) a) Drilled specimens; b) 380°C; c) 400°C; d) 450°C; e) Typical representative plots.

Table 5.7: Mean specimen stiffness for pierced specimens with varying processing temperature and drilled specimens during open-hole compression tests (with standard errors)

<table>
<thead>
<tr>
<th>Processing temperature</th>
<th>Stiffness (kN/mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drilled</td>
<td>57.0 ± 0.5</td>
</tr>
<tr>
<td>380 °C</td>
<td>57.1 ± 1.2</td>
</tr>
<tr>
<td>400 °C</td>
<td>56.9 ± 1.2</td>
</tr>
<tr>
<td>450 °C</td>
<td>56.8 ± 2.1</td>
</tr>
</tbody>
</table>
5.3.4 Strengths of Pierced Laminates Under Compressive Load

The open-hole compressive strength of pierced specimens are shown in Figure 5.9 and summarised in Table 5.8. The results show a 12% improvement for a 30 mm diameter heated area (compared to drilled specimens), falling to an improvement of 4% for a 45 mm diameter. The compression strength of the pierced specimens, however, reduced for the 60 mm diameter heated area specimens to 79% of the drilled hole strength.

![Figure 5.9: Open-hole compression strengths of pierced specimens as a percentage of drilled specimen strength showing the effects of the varying the diameter of the heated area and processing temperature (with standard error bars).](image)

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Heated area diameter</th>
<th>Processing temperature</th>
<th>Open-hole compression strength (MPa)</th>
<th>Open-hole compression strength (% of drilled)</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1 (Drilled)</td>
<td>-</td>
<td>-</td>
<td>375 ± 7</td>
<td>100 ± 2</td>
</tr>
<tr>
<td>A3</td>
<td>30 mm</td>
<td>400°C</td>
<td>419 ± 10</td>
<td>112 ± 3</td>
</tr>
<tr>
<td>A2</td>
<td>45 mm</td>
<td>400°C</td>
<td>389 ± 10</td>
<td>104 ± 3</td>
</tr>
<tr>
<td>T1</td>
<td>60 mm</td>
<td>380°C</td>
<td>274 ± 12</td>
<td>73 ± 3</td>
</tr>
<tr>
<td>T2 (A1)</td>
<td>60 mm</td>
<td>400°C</td>
<td>298 ± 5</td>
<td>79 ± 1</td>
</tr>
<tr>
<td>T3</td>
<td>60 mm</td>
<td>450°C</td>
<td>306 ± 7</td>
<td>81 ± 2</td>
</tr>
</tbody>
</table>
Development of a Thermally-Assisted Piercing (TAP) process for introducing holes into thermoplastic composites

The effect of varying the processing temperature on the compressive strength of the pierced specimens is also shown in Figure 5.9 and Table 5.8. For the maximum diameter of heated area tested (60 mm), the open-hole compression strength of the pierced specimens increased from 73% to 79% of the drilled specimen strength with an increase in piercing process temperature from 380°C to 400°C. Increasing the piercing process temperature to 450°C resulted in a small increase in the compression strength of pierced specimens to 81% of the drilled specimen strength – i.e. still considerably below the drilled hole strength.

The open-hole compression test results show a similar, but more significant, trend to the tension results when varying the diameter of the heated area. When increasing the heated area there will be the conflicting effects of increasing void content and resin rich regions (reducing the fibre volume fraction) and decreasing fibre fractures. The compression tests will, however, be more significantly affected by void content, which promotes ply buckling. The larger reduction in strength is also likely to be caused by the increase in length of fibre over which misalignment and waviness will occur, promoting fibre buckling.

The increase in compression strength of pierced specimens with increasing process temperature was a consistent trend, unlike that shown for the tensile strengths. The suggested increase in freedom of fibres to be displaced and increased mobility of the matrix is likely to have reduced fibre fractures and improved the open-hole compression strength. This was offset by the increase in voids and resin rich regions and reduction in fibre volume fraction. A reduction in strength improvement was not observed for the intermediate temperature (400°C), as it was for the open-hole tension results. The consistent reduction in fibre fractures is suggested to reduce the likelihood of fibre buckling, which is more dominant than the effect of fibre volume fraction for compressive strength.
5.3.5 Summary of Open-Hole Compression Properties of Pierced Laminates
The stiffness of all specimens was approximately the same for varying heated area and processing temperature and did not significantly differ from the stiffness of the drilled specimens. The effects of changing the diameter of the heated area and processing temperature on the open-hole compressive strengths are summarised in Table 5.9.

Table 5.9: Summary of how increasing the diameter of the heated area and increasing the processing temperature when piercing affected the Open-Hole Compressive Strength (OHCS)

<table>
<thead>
<tr>
<th>Change of process variable</th>
<th>Effect on OHCS</th>
</tr>
</thead>
<tbody>
<tr>
<td>Increasing heated area</td>
<td>▼▼▼</td>
</tr>
<tr>
<td>Increasing temperature</td>
<td>▼</td>
</tr>
</tbody>
</table>

5.4 Comparison of Open-Hole Tension and Compression Results
Open-hole testing was used to investigate the effect of changing the diameter of heated area (with constant temperature) and processing temperature (with constant diameter of heated area). Open-hole tension and compression results show that the stiffness of the specimens was similar for all variables tested. The ultimate tensile and compressive strengths, however, considerably varied when changing the diameter of the heated area and processing temperature.

The changes in open-hole performance of the pierced specimens are suggested to be linked to the variation in microstructural features presented in Chapter 4. A summary of the resultant effects of the piercing conditions, in terms of the microstructure and open-hole strength, is shown in Table 3.2. The table shows arrows that represent an increase (up) or decrease (down) in the occurrence of a feature and the impact on the open-hole strength (more arrows indicating increased severity of the effect). The resultant effects are given individually followed by an ‘overall effect’ (the sum of the individual effects). The arrows in brackets refer to the effects seen when initially increasing the processing temperature from 380°C to 400°C.
Table 5.10: Summary of how increasing the diameter of the heated area and increasing processing temperature when piercing affected the microstructural features and open-hole strengths. (Brackets indicate the more dominant effect when increasing the processing temperature from 380°C to 400°C)

<table>
<thead>
<tr>
<th>Change of process variable</th>
<th>Microstructural feature</th>
<th>Effect on feature</th>
<th>Effect on OHTS</th>
<th>Effect on OHCS</th>
</tr>
</thead>
<tbody>
<tr>
<td>Increasing heated area</td>
<td>Voids and resin rich regions</td>
<td>↑ ↑</td>
<td>↓</td>
<td>↓ ↓ ↓ ↓</td>
</tr>
<tr>
<td></td>
<td>Fibre volume fraction</td>
<td>↓</td>
<td>↑</td>
<td>↑</td>
</tr>
<tr>
<td></td>
<td>Fibre disruption</td>
<td>↓</td>
<td>↑ ↑</td>
<td>↑</td>
</tr>
<tr>
<td></td>
<td>Overall effect</td>
<td>-</td>
<td>↓</td>
<td>↓ ↓ ↓ ↓</td>
</tr>
<tr>
<td>Increasing temperature</td>
<td>Voids and resin rich regions</td>
<td>↑</td>
<td>↓</td>
<td>↓ ↓ ↓ ↓</td>
</tr>
<tr>
<td></td>
<td>Fibre volume fraction</td>
<td>↓ (↓)</td>
<td>↓ (↓ ↓)</td>
<td>↓</td>
</tr>
<tr>
<td></td>
<td>Fibre disruption</td>
<td>↓ ↓</td>
<td>↑ ↑ ↑</td>
<td>↑ ↑</td>
</tr>
<tr>
<td></td>
<td>Overall effect</td>
<td>-</td>
<td>↑ (↓)</td>
<td>↑</td>
</tr>
</tbody>
</table>

The results indicate that a small heated area and high processing temperature would give the best open-hole strengths of pierced specimens. This combination of variables is suggested to reduce fibre fractures, void content, resin rich regions and the area of fibre misalignment and waviness. The trends in the strength data were used to set the conditions for a set of specimens pierced under optimised settings. This was a final optimisation test case used to validate the suggested preferential piercing conditions for open-hole performance.

5.5 Open-Hole Testing Optimisation Study

5.5.1 Optimisation study specimen manufacture, geometry and test set-up
Specimens were cut from panels manufactured using the same material and method described for the previous tests. The diameters of heated area and processing temperatures used for specimen manufacture were selected based on the findings mentioned for the previous tests and are shown in Table 5.11. The smallest heated area (30 mm) and highest processing temperature (450°C) were suggested to give the best open-hole performance. Following the microscopy analysis (discussed previously in Chapter 4), the clamping pressure (P) was increased for the optimised specimens to 1.7 MPa in order to reduce the likelihood of
void formation. The spike angle was also reduced to give the sharpest spike for piercing, which was assumed to result in reduced through thickness deformation and fewer fibre fractures.

Table 5.11: Optimised piercing process variables used for open-hole testing optimisation study specimen manufacture

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Piercing process variables</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Heated area diameter (D – 2D)</td>
</tr>
<tr>
<td>O1 (Optimised)</td>
<td>30 mm (D)</td>
</tr>
</tbody>
</table>

Specimen dimensions, hole size and preparation all remained consistent with that used for the previous open-hole testing. Five specimens were tested for each specimen group and the grip lengths, and subsequent gauge lengths, were the same as those used in the respective open-hole tests. The same test machine was used for the optimised tests as described previously and a constant displacement rate of 1 mm/min was applied until ultimate failure of the specimens. Applied force and cross-head displacement were measured throughout all tests.

5.5.2 Load/displacement response of laminates pierced using optimised piercing conditions under tensile load

Figure 5.10 shows the load/displacement data for the specimens pierced using optimised conditions under tensile load and how these compare to the previous results. Figure 5.10 a) shows that there is good repeatability of the failure forces and displacements for all specimens tested. A representative load/displacement response for the optimised testing was overlaid onto the previous typical responses for varying diameter of heated area and processing temperature in Figure 5.10 b) and c), respectively. The results show that there is no significant difference in the responses between the optimised specimens and those tested previously;
there is an approximately linear increase in displacement with applied force followed by catastrophic failure of the specimens. The mean stiffness of the optimised specimens have been added to the previously presented data in Table 5.12. The stiffness was measured over the same force range as for the other tensile specimens (10 kN to 35 kN). The optimised specimens have a comparable stiffness to the previous specimens and, therefore, show no significant advantage in the response to loading.

Figure 5.10: Force/displacement plots of open-hole tension tested specimens (dotted line indicating data used for representative comparison) a) Specimens pierced using optimised piercing conditions; b) Typical representative plots for drilled specimens, specimens pierced with varying heated area and specimens pierced using optimised conditions; c) Typical representative plots for drilled specimens, specimens pierced with varying processing temperature and specimens pierced using optimised conditions.
Table 5.12: Average specimen stiffness for pierced specimens with varying heated area and processing temperature, drilled specimens and specimens pierced with optimised piercing conditions during open-hole tension tests (with standard errors)

<table>
<thead>
<tr>
<th>Diameter of heated area</th>
<th>Stiffness (kN/mm)</th>
<th>Processing temperature</th>
<th>Stiffness (kN/mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drilled</td>
<td>38.4 ± 0.7</td>
<td>Drilled</td>
<td>38.4 ± 0.7</td>
</tr>
<tr>
<td>30 mm</td>
<td>34.6 ± 0.9</td>
<td>380 °C</td>
<td>37.4 ± 0.8</td>
</tr>
<tr>
<td>45 mm</td>
<td>40.4 ± 0.4</td>
<td>400 °C</td>
<td>38.5 ± 0.7</td>
</tr>
<tr>
<td>60 mm</td>
<td>38.5 ± 0.7</td>
<td>450 °C</td>
<td>38.3 ± 0.7</td>
</tr>
<tr>
<td>Optimised</td>
<td>39.6 ± 0.8</td>
<td>Optimised</td>
<td>39.6 ± 0.8</td>
</tr>
</tbody>
</table>

5.5.3 Strength of laminates pierced using optimised piercing conditions under tensile load

The strength of the specimens pierced under optimised conditions have been added to the previous results and are shown in Figure 5.11. The data is also summarised in Table 5.13 with the previous data repeated for ease of reference. The data show that there is no significant increase in strength of the optimised specimens to 111% of the drilled strength (from a maximum of 110% previously).

Figure 5.11: Open-hole tensile strengths of pierced specimens as a percentage of drilled specimen strength showing the effects of the TAP process variables, heated area and temperature, and when pierced using optimised variable settings (with standard error bars).
Table 5.13: Ultimate tensile strength of pierced and drilled specimens, also calculated as percentages of drilled specimen strength (including optimised specimen results) (with standard errors).

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Heated area diameter</th>
<th>Processing temperature</th>
<th>Open-hole tensile strength (MPa)</th>
<th>Open-hole tensile strength (%) of drilled</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1 (Drilled)</td>
<td>-</td>
<td>-</td>
<td>412 ± 7</td>
<td>100 ± 2</td>
</tr>
<tr>
<td>A3</td>
<td>30 mm</td>
<td>400°C</td>
<td>431 ± 7</td>
<td>105 ± 2</td>
</tr>
<tr>
<td>A2</td>
<td>45 mm</td>
<td>400°C</td>
<td>431 ± 6</td>
<td>105 ± 2</td>
</tr>
<tr>
<td>T1</td>
<td>60 mm</td>
<td>380°C</td>
<td>436 ± 8</td>
<td>106 ± 2</td>
</tr>
<tr>
<td>T2 (A1)</td>
<td>60 mm</td>
<td>400°C</td>
<td>415 ± 7</td>
<td>101 ± 2</td>
</tr>
<tr>
<td>T3</td>
<td>60 mm</td>
<td>450°C</td>
<td>454 ± 13</td>
<td>110 ± 3</td>
</tr>
<tr>
<td>O1 (Optimised)</td>
<td>30 mm</td>
<td>450°C</td>
<td>456 ± 10</td>
<td>111 ± 2</td>
</tr>
</tbody>
</table>

The optimised conditions applied the highest processing temperature with the smallest diameter of heated area and increased clamping pressure by a factor of 2 (the spike angle was also reduced). This was envisaged to reduce the number of fibre fractures (by reducing matrix viscosity) and reduce the formation of resin rich regions and voids by increasing the clamping pressure and reducing the heated area. This has not, however, appeared to increase the performance of the specimens under tensile load.

5.5.4 Load/displacement response of laminates pierced using optimised piercing conditions under compressive load

Figure 5.12 shows the load/displacement data for the optimised compression tests in the same way as previously shown for the tension tests. Figure 5.12 a) shows that there is good repeatability of the failure forces and displacements for all specimens tested. A representative load/displacement response for the optimised tests was, again, overlaid onto the previous typical responses for varying diameter of heated area and processing temperature in Figure 5.12 b) and c), respectively. The results do not agree with the tension tests and show that there is a difference in the responses between the optimised specimens and those tested previously. The mean stiffness of the optimised specimens has been added to the previously
presented data in Table 5.14. The optimised specimens have a significantly higher stiffness than the drilled specimens and the specimens previously tested (for varying heated area and processing temperature).

Table 5.14: Average specimen stiffness for pierced specimens with varying heated area and processing temperature, drilled specimens and specimens pierced with optimised piercing conditions during open-hole compression tests (with standard errors)

<table>
<thead>
<tr>
<th>Diameter of heated area</th>
<th>Stiffness (kN/mm)</th>
<th>Processing temperature</th>
<th>Stiffness (kN/mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drilled</td>
<td>57.0 ± 0.5</td>
<td>Drilled</td>
<td>57.0 ± 0.5</td>
</tr>
<tr>
<td>30 mm</td>
<td>60.2 ± 0.9</td>
<td>380°C</td>
<td>57.1 ± 1.2</td>
</tr>
<tr>
<td>45 mm</td>
<td>59.1 ± 0.9</td>
<td>400°C</td>
<td>56.9 ± 1.2</td>
</tr>
<tr>
<td>60 mm</td>
<td>56.9 ± 1.2</td>
<td>450°C</td>
<td>56.8 ± 2.1</td>
</tr>
<tr>
<td>Optimised</td>
<td>65.7 ± 1.2</td>
<td>Optimised</td>
<td>65.7 ± 1.2</td>
</tr>
</tbody>
</table>
5.5.5 Strength of laminates pierced using optimised piercing conditions under compressive load

The compression strength of the specimens pierced under optimised conditions are presented in the same way as those presented previously for the tension results. The strength of the optimised specimens have been overlaid onto the previous results in Figure 5.13. The data is also summarised in Table 5.15. The data show that there is a significant increase in strength of the optimised specimens to 121% of the drilled strength (from a maximum of 112% previously).

![Figure 5.13: Open-hole compression strengths of pierced specimens as a percentage of drilled specimen strength showing the effects of the TAP process variables, heated area and temperature, and when pierced using optimised variable settings (with standard error bars).](Image)
Table 5.15: Ultimate compression strength of pierced and drilled specimens, also calculated as percentages of drilled specimen strength (including optimised specimen results) (with standard errors).

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Heated area diameter</th>
<th>Processing temperature</th>
<th>Open-hole compression strength (MPa)</th>
<th>Open-hole compression strength (% of drilled)</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1 (Drilled)</td>
<td>-</td>
<td>-</td>
<td>375 ± 7</td>
<td>100 ± 2</td>
</tr>
<tr>
<td>A3</td>
<td>30 mm</td>
<td>400°C</td>
<td>419 ± 10</td>
<td>112 ± 3</td>
</tr>
<tr>
<td>A2</td>
<td>45 mm</td>
<td>400°C</td>
<td>389 ± 10</td>
<td>104 ± 3</td>
</tr>
<tr>
<td>T1</td>
<td>60 mm</td>
<td>380°C</td>
<td>274 ± 12</td>
<td>73 ± 3</td>
</tr>
<tr>
<td>T2 (A1)</td>
<td>60 mm</td>
<td>400°C</td>
<td>298 ± 5</td>
<td>79 ± 1</td>
</tr>
<tr>
<td>T3</td>
<td>60 mm</td>
<td>450°C</td>
<td>306 ± 7</td>
<td>81 ± 2</td>
</tr>
<tr>
<td>O1 (Optimised)</td>
<td>30 mm</td>
<td>450°C</td>
<td>454 ± 13</td>
<td>121 ± 4</td>
</tr>
</tbody>
</table>

The compression strength of composite materials is commonly susceptible to unsupported plies, which promote ply buckling. A small reduction in fibre fractures and a significant decrease in the formation of resin rich regions and voids were expected to result from piercing under the optimised conditions. This is suggested to have caused the increased improvement in compression strength when compared with the insignificant improvement in tensile strength.

5.5.6 Summary of open-hole testing optimisation study

There was a negligible increase in tensile strength for the specimens pierced under optimised conditions. This is likely to be due to the predominant reduction in voids and resin rich areas as a result of the process conditions; tensile strength of composite laminates being more strongly affected by fibre fractures and volume fraction.

The compressive strength of specimens pierced with optimised process settings showed a considerable increase in strength to 121% of the drilled specimen strength. This is likely to have been primarily influenced by the reduction in resin rich regions and voids, but also contributed to by a decrease in fibre fractures.
5.6 Concluding Remarks

Open-hole testing was conducted to investigate the effects of changing the TAP conditions on the mechanical performance of pierced specimens. The results have shown that both the heated area and the processing temperature can have significant impacts on the resultant mechanical performance and this can be linked to the differences in microstructure previously seen.

Optimised piercing conditions were selected based on the tests conducted and were shown not to affect the open-hole tension results, but significantly increase the open-hole compression results (in terms of specimen stiffness and strength). This is suggested to be associated with competing microstructural features (void content, resin rich regions and fibre fractures) that affect compression properties more significantly than tension properties.

In order to further understand how the change in microstructure of the pierced specimens affected the open-hole strengths of specimens, a Digital Image Correlation (DIC) technique was used to map the strain distribution over the specimen surfaces during testing. The next chapter presents and discusses this data, with consideration to that presented so far.
6 Digital Image Correlation (DIC) of Pierced Specimens Under Open-Hole Loading

6.1 Introduction

The previous chapter contained results of the open-hole mechanical testing and discussed the differences seen with respect to the material structure observed in Chapter 4. This chapter gives the results of an optical analysis technique that was used to measure the deformation of specimens during loading. The technique is called Digital Image Correlation (DIC) and was applied to specimens during the open-hole testing previously presented (verification testing of this technique is presented in Appendix A.

Under mechanical loading the local deformation of the specimens around the hole can be attributed to the local material microstructure. The previous chapters have linked the microstructure observed using various characterisation techniques with the overall mechanical performance of the specimens. This chapter presents the results of the DIC analysis and discusses how the previously presented microstructure may have led to differences in strain distribution, and hence mechanical performance, of drilled and pierced specimens. During the course of the work, a preliminary Finite-Element (FE) model was constructed in collaboration with the TWI modelling team in an attempt to compare numerical predictions of the strain field around a pierced hole and a drilled hole with the DIC measurements. As this was preliminary work, the modelling is presented in Appendix B.

6.2 DIC Set-Up

A GOM – ARAMIS three-dimensional DIC system was used in the current investigation during open-hole testing. The DIC system comprises two cameras that are focussed onto the surface of a specimen during loading (Figure 6.1). The cameras were 5 megapixel resolution (2448x2050) with 75 mm (Titanar A75) lenses. The camera CCD sensors were 2/3 inch with a pixel size of 3.45 micron. The surface under observation has a painted speckle pattern
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applied (Figure 6.2). The DIC ARAMIS software triggers the cameras to take a series of images during testing and tracks the deformation of the speckle pattern when the specimens are loaded. The measured deformations can then be interpreted as strains and plotted along one-dimensional profiles or as colour contour plots representing the strains over the two-dimensional surface of interest (thus eliminating the need for multiple individual strain gauges).

Figure 6.1: Photograph of the DIC set-up used for open-hole testing

Figure 6.2: Photograph of open-hole tensile test specimen with painted speckle pattern for DIC analysis (hole diameter is 6 mm)
6.3 DIC Procedure

The testing procedures for the open-hole tests were described in the previous chapter (Chapter 5). DIC was used for one specimen from each specimen group for varying heated area and processing temperature. DIC analysis was also implemented for the specimens pierced under the optimised piercing conditions and also for drilled specimens. Multiple analyses within each specimen grouping were not conducted due to the additional time required to set-up the DIC equipment and time constraints when testing.

The DIC system was set to acquire images at a rate of 1 Hz and the analysis was conducted using facet settings of 19 x 19 pixels with 2 pixel overlap. These settings are within the mid-range of the system and allow for good accuracy and fast computation (GOM, 2007). Each facet refers to an area of the specimen surface over which the deformation is computed and averaged. Increasing the size of the facets will reduce computation time, but increases the area over which the software takes an average. Conversely, smaller facets provide more data points, but at a substantial increase in computation time.

The manufacturer’s quoted accuracy for the system was ± 0.05% strain. The DIC technique can be susceptible to a number of errors that include optical issues (due to lighting conditions), the quality of the painted speckle pattern and the quality of adhesion between the paint and the specimens. For this reason, verification testing was conducted to compare the DIC accuracy with strain gauges (Appendix A). The test concluded that the DIC data was accurate for strain measurements larger than 0.1%. This agreed with the manufacturer’s quoted accuracy and, based on these findings, the longitudinal strains are included in this investigation and the transverse strains omitted due to typical values of <0.1%.

Computational masks were also applied to the specimens during processing to eliminate any erroneous calculations around the edge of the hole. The two cameras will record images from different perspectives and will each look into the machined hole from a different angle. The
edge of the hole can, therefore, become problematic during the processing and lead to errors in the results. A mask was applied to prevent the software performing the analysis at the edge of the hole, as shown in Figure 6.3. This can also be observed in the data results and is the reason the data does not continue completely to the edge of the hole.

![Figure 6.3: Image showing the near-hole region masked from DIC computational analysis (6 mm hole diameter)](image)

### 6.4 DIC of Pierced Specimens Under Tensile and Compressive Load

#### 6.4.1 Tensile Loading

The longitudinal strain field (in the y direction i.e. 0°) around pierced and drilled holes are represented using contour plots and shown in Figure 6.4 a) – h) for a tensile load of 25 kN. At 25 kN the specimens do not show any significant signs of damage and it is assumed that the deformation is primarily elastic.

The strain fields around pierced holes for 30 mm, 45 mm and 60 mm diameter heated areas (at 400°C processing temperature) are shown in Figure 6.4 a) – c), respectively. The strain fields around pierced holes for processing temperatures of 380°C, 400°C and 450°C (with a 60 mm diameter heated area) are shown in Figure 6.4 d) – f), respectively. Finally, the strain fields around a hole pierced using optimised piercing conditions (presented in Chapter 5) and a drilled hole are shown in Figure 6.4 g) and h), respectively.
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Figure 6.4: DIC contour plots showing the longitudinal strain of pierced and drilled specimens at 25 kN applied tensile load a) pierced specimen (30 mm diameter heated area); b) pierced specimen (45 mm diameter heated area); c) pierced specimen (60 mm diameter heated area); d) pierced specimen (380°C processing temperature); e) pierced specimen (400°C processing temperature); f) pierced specimen (450°C processing temperature); g) pierced specimen (optimised piercing conditions); h) drilled specimen
The contour plots do not show any significant differences in the strain distributions for increasing diameter of heated area or increasing processing temperature when piercing. All pierced specimens, however, showed a similar distribution of strain around the hole. The pierced specimen data show two distinct regions, or ‘halos’, of higher strain that run across the full width of the specimens (in most cases) and are aligned towards the top and bottom of the pierced holes (as highlighted by the red dotted lines in Figure 6.5 a)). The drilled specimen, by contrast, showed two concentrated lobes of higher strain located on both sides of the hole (as highlighted in Figure 6.5 b)).

![Figure 6.5: DIC contour plots highlighting the regions of larger strain a) pierced specimen (optimised piercing conditions); b) drilled hole](image)

The longitudinal strain along the mid-plane for pierced specimens (using optimised conditions) and drilled specimens, highlighted in Figure 6.4 g) and h) (dotted line), are shown in Figure 6.6 for a tensile load of 25 kN. The plots show how for drilled specimens the longitudinal strain significantly increased towards the hole edge (3 mm from the hole centre). The pierced specimens, however, show only a small increase in strain towards the hole edge along the mid-plane profile. For the pierced specimens, if there were any differences in the longitudinal strain distributions for the varying processing parameters, these differences were too small to be discernible.
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Figure 6.6: Longitudinal strain (along the indicated profiles in Figure 6.4 g) and h) calculated using DIC data for a specimen pierced using optimised piercing conditions and a drilled specimen at 25 kN tensile load

The halo regions of larger strain for the pierced specimens may have been caused by the rotation of the 0° fibres from the 0° direction during piercing. At the top of the hole the fibres are likely to be at the largest misalignment angle before rotating back to the 0° direction, as shown in Figure 6.7 (an annotated version of Figure 4.8 b)). The stiffness of this region will significantly reduce as a result of the change in fibre angle and it appears to correlate with the regions of larger strain in the DIC data (despite any increase in fibre volume fraction).

The increase in longitudinal strain near the hole edge for drilled specimens (shown by the two lobes) reflects the effect of the stress concentration due to the hole. This agrees with DIC data measured for drilled holes in previous studies (Caminero et al., 2013 and 2014). The increase in local fibre volume fraction near the hole edge for pierced specimens will increase the local stiffness when compared with the drilled specimens, thus reducing the local strain and effectively removing the stress concentration. The redistribution of strain away from the hole edges may contribute to the formation of the two halos either side of the hole, as seen in the contour plots. The reduction in the stress concentration at the mid-plane helped to delay fracture within pierced specimens and, hence, increased the ultimate strength of the TAP specimens compared with drilled specimens.
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Figure 6.7: X-ray image of pierced hole showing the displacement of fibres and the approximate location of the maximum fibre misalignment angle

6.4.2 Compressive Load

The longitudinal strain fields around pierced and drilled holes for a compressive load of 25 kN are represented using contour plots and shown in Figure 6.8 a) – h). The strain fields around pierced holes for 30 mm, 45 mm and 60 mm diameter heated areas (at 400°C processing temperature) are shown in Figure 6.8 a) – c), respectively. The strain fields around pierced holes for processing temperatures of 380°C, 400°C and 450°C (with a 60 mm diameter heated area) are shown in Figure 6.8 d) – f), respectively. Finally, the strain fields around a hole pierced using optimised piercing conditions and a drilled hole are shown in Figure 6.8 g) and h), respectively.

The contour plots for the pierced specimens show similar strain distributions to those seen for the tensile testing. The halos of larger strain can be seen again and appear to increase in magnitude (compressive strain is shown as negative values) with increasing diameter of heated area and they appear to reduce in magnitude with increasing temperature. In a similar fashion to the tensile testing results, the drilled specimen does not show the same strain distribution and instead there are concentrated lobes of higher strain located on each side of the hole.
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**Figure 6.8**: DIC contour plots showing the longitudinal strain of pierced and drilled specimens at 25 kN applied compressive load a) pierced specimen (30 mm diameter heated area); b) pierced specimen (45 mm diameter heated area); c) pierced specimen (60 mm diameter heated area); d) pierced specimen (380°C processing temperature); e) pierced specimen (400°C processing temperature); f) pierced specimen (450°C processing temperature); g) pierced specimen (optimised piercing conditions); h) drilled specimen

The longitudinal strain for pierced specimens (using optimised conditions) and drilled specimens along the mid-plane profile, highlighted in Figure 6.8 g) and h) (dotted line), are shown in Figure 6.9 for a compressive load of 25 kN. The plots show how for drilled specimens the longitudinal strain significantly increased towards the hole edge. The pierced specimens
also show an increase in strain towards the edge of the hole, but this is not as significant as that shown for the drilled specimens.

![Graph showing strain along the profile](image)

**Figure 6.9**: Longitudinal strain (along the indicated profiles in Figure 6.8 g) and h) calculated using DIC data for a specimen pierced using optimised piercing conditions and a drilled specimen at 25 kN compressive load

The strain contours for the compressive tests are essentially the mirror-image of the results for tension, as would be expected under elastic deformation. The DIC contour plots for compression, however, show that the strains in the halo regions increase with increasing heated area and decrease with increasing processing temperature for the pierced specimens, which is slightly different to the tensile test results. For the tension tests, a load of 25 kN produced an extension of approximately 0.6 mm on a gauge length of 100 mm i.e. 0.6% (see Figure 5.4). For the compression tests, the 25 kN load produced approximately 0.4 mm displacement on a gauge length of 45 mm i.e. 0.9% strain (see Figure 5.7). This will have accentuated the differences in the material microstructure, as a consequence of piercing, to a greater extent than for the tensile tests. This is also likely to have amplified the geometric stress concentration near the hole edge, shown in Figure 6.9.

The halo regions were discussed previously within the tensile testing results (section 6.4.1). They are likely to have been caused by the angle of the fibres after they were displaced around the hole. The open-hole compressive strengths of the pierced specimens were previously shown to be more significantly affected by the change in heated area and processing
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temperature when compared with the open-hole tensile strengths. The compressive DIC results, when compared with the tensile results show that the differences in microstructure, as a consequence of piercing at different temperatures and heated areas, become more significant for increasing strain (and hence deformation). This would suggest that even larger differences could be expected during plastic deformation prior to failure.

The difference in loading effects between tension and compression could also contribute to resultant strengths. Although the rotated fibres (as a result of displacement during piercing) are likely to reduce the stiffness of the specimens at the halo regions, the deformation of the displaced fibres away from the hole under compressive loading is likely to have supported adjacent fibres (Figure 6.10 a)). This would reduce the likelihood of micro-buckling in the direction of the hole (Figure 6.10 b)), which is a typical failure feature found when compression loading drilled specimens (discussed in Soutis et al., 1991).

Figure 6.10: Schematic diagram showing the difference in suggested loading effects during open-hole compression testing a) pierced specimen; b) drilled specimen

Within the scope of the work, a preliminary Finite Element (FE) model was constructed to attempt to replicate the re-distribution of material during the piercing process and to compare the FE modelling results to the DIC measurements. The piercing process itself was not simulated, but the resulting distribution of material was generated based on an assumed fibre
structure to give a first approximation for future development. The model is at the preliminary stage and incorporated many simplifications and assumptions. It has been included as an appendix to this thesis (Appendix B).

6.5 Concluding Remarks

DIC analysis was used to supplement the open-hole testing data and link the mechanical performance with the structure observed after piercing and drilling. The analysis showed significant differences in the strain distribution around the hole when comparing pierced and drilled specimens.

The pierced specimens showed distinct halo regions of higher strain (under tensile and compressive loading) that can be attributed to reduced stiffness as a result of the fibre angle in these regions. The pierced specimens also showed significantly lower strains near the hole than those for the drilled specimen in the same region. The displacement (and compaction) of fibres during the piercing process increases the fibre volume fraction in these regions and, therefore, increases the stiffness. By increasing the fibre volume fraction near the hole, the strain under loading is reduced and the onset of damage is delayed. The delay in damage initiation and fracture is suggested to have led to the additional strength of pierced specimens observed in the previous chapter.
7 Mechanical Properties of Pierced Specimens for Joint Applications

7.1 Introduction

The open-hole testing of pierced and drilled specimens showed how the microstructure created when piercing affected the mechanical performance of specimens in tension and compression. The most common use of machined holes within the composites industry, however, is for mechanical fastening using bolts or rivets. The loading case for these applications varies significantly from open-hole loading where the whole structure is under stress. Instead, for mechanically fastened joints the load is concentrated on the inner surface of the hole leading to high local stresses.

This chapter describes plain-pin bearing and bolted bearing tests that were used to determine the mechanical properties of the pierced specimens in situations more relevant to a bolted joint. In a similar fashion to the open-hole testing, the results of the pierced specimens are directly compared with drilled specimens, which are used as the benchmark throughout. For the open-hole testing the diameter of the heated area and processing temperature were varied. As explained in Chapter 3, the spike geometry was varied for the two types of bearing tests reported in this chapter. A set of specimens pierced using optimised piercing parameters was also tested as a final optimisation study, as presented for the open-hole tests.

The differences in the two types of bearing tests are described in detail within the sections of this chapter. The key difference is that the plain-pin bearing tests did not apply a clamping force to the top and bottom surfaces of the specimens so the test provided a measure of the material resistance to pin loading. The results from these tests indicated how the material microstructure differences impacted the performance of the specimens when loading the hole. The bolted bearing tests applied a clamping force to the joint configuration that prevented through thickness deformation and helped to distribute the load through a larger area.
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surrounding the hole. This is a more industrially relevant loading case and was used to give an assessment of how the pierced holes would perform in a realistic bolted joint application.

7.2 Plain-Pin Bearing Properties of Pierced Laminates

7.2.1 Plain-Pin Bearing Specimen Manufacture and Testing Set-Up
Plain-pin bearing strength tests were conducted following the standard BS ISO 12815:2013 (BSI, 2013b) where possible. The same specimen material and manufacturing method were used as described in Chapter 3 (cross-ply CF/PEEK, approximately 2.9 mm thick). The tested specimen types, including the variation of spike geometry used to create the pierced specimens, are shown in Table 7.1.

As described for the open-hole tests, the drilled specimens were first cut to size and then a 6 mm diameter hole was drilled for testing. Pierced specimens were pierced to introduce 6 mm diameter holes prior to cutting the specimens from the panel (to ensure the heated area was not disrupted by the specimen edges). The specimen dimensions were nominally 36 mm wide (W), 120 mm long with a 6 mm diameter hole (d) and a distance from hole centre to top edge (E) of 28 mm (edge distance/hole diameter ratio, E/d, = 4.67 and W/d = 6), as shown in Figure 7.1. The test jigging was manufactured according to the standard to allow un-clamped pin loading of the specimen in tension (Figure 7.2).
Table 7.1: Process variables used for bearing tests specimen manufacture

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Piercing process variables</th>
<th></th>
</tr>
</thead>
</table>
|                      | Heated area diameter  
(D − 2D) | Processing temperature  
(T − 1.20T) | Piercing spike angle  
(θ - 2θ) | Clamping pressure  
(P) |
| D1                   | Drilled                   |  |
| S1                   | 30 mm (D)                | 400°C (1.05T) | 20° (θ) | 0.8 MPa (P) |
| A3                   | 30 mm (D)                | 400°C (1.05T) | 30° (1.5θ) | 0.8 MPa (P) |
| S2                   | 30 mm (D)                | 400°C (1.05T) | 40° (2θ) | 0.8 MPa (P) |
| S3                   | 30 mm (D)                | 400°C (1.05T) | Ogive | 0.8 MPa (P) |

Figure 7.1: Schematic diagram of plain-pin bearing specimens
7.2.2 Plain-Pin Bearing Testing Procedure

Five repeats were tested for each specimen type and a grip length of 50 mm was used for the pin-loading jig and the specimens. The same test machine was used for compression testing as described and shown for open-hole testing. The applied force and cross-head displacement were measured for all tests. A constant displacement rate of 1 mm/min was applied to all specimens. For the specimens pierced using the 20° and ogive spike geometries, the test was stopped after initial/first failure for two of the specimens. This allowed visual inspection of the specimens after initial failure for the significantly different spike geometries. The remaining specimens were tested until the applied load reduced to below 50% of the maximum applied load prior to failure (according to the standard).

Load/displacement responses of the pierced and drilled specimens are presented and discussed before the ultimate strengths are shown. The local thickness increase for pierced specimens affects the area over which the bearing force is applied and, hence, the calculated
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bearing strengths. The ultimate strengths for pierced specimens were calculated using both the local specimen thickness and the nominal specimen thickness, as illustrated in Figure 7.3.

The strength values will obviously depend upon the specimen thickness used during calculation. The local thickness provides the area over which the force is being applied during the tests. The nominal thickness, however, gives a strength result for a directly comparable structure to the drilled specimens (i.e. the pierced and drilled holes were applied to the same original specimen dimensions). Both sets of results were, therefore, included and considered.

![Figure 7.3: Schematic of local specimen thickness and nominal specimen thickness used for bearing strength calculations]

7.2.3 Load/Displacement Response of Pierced Laminates Under Plain-Pin Bearing Load
The load/displacement responses of the drilled specimens are shown in Figure 7.4 a). The responses of pierced specimens for 20°, 30°, 40° and ogive spike geometries under plain-pin loading are shown in Figure 7.4 b) – e), respectively. Finally, representative data for each of the aforementioned specimen types were plotted on the same axes for comparison in Figure 7.4 f) (the dotted lines in the previous plots show the re-plotted representative data).
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Figure 7.4: Load/displacement responses of plain-pin bearing loaded specimens (dotted lines indicating data used for representative comparison) a) drilled; b) 20° conical spike; c) 30° conical spike; d) 40° conical spike; e) ogive spike; f) typical representative responses

The initial load/displacement responses are linear for all specimens (after an initial bedding in process for the pierced specimens) until a failure point is reached. At this point the pierced specimens and drilled specimens behave differently. The drilled specimens consistently show a severe single failure at maximum load before the test was stopped (once the load had fallen to below 50% of the maximum applied load). The pierced specimens, however, show a more progressive series of failures that gradually reduce the loading capability. This suggests that the differences in microstructure between drilled and pierced specimens are significantly affecting the damage propagation and failure mechanisms of the specimens. Table 7.2,
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however, shows that the mean specimen stiffness for all spike geometries are approximately the same prior to failure or the onset of damage. The stiffness of each specimen was measured between an applied force of 2 kN and 5 kN.

<table>
<thead>
<tr>
<th>Spike angle</th>
<th>Stiffness (kN/mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drilled</td>
<td>24.5 ± 0.6</td>
</tr>
<tr>
<td>20°</td>
<td>25.1 ± 0.6</td>
</tr>
<tr>
<td>30°</td>
<td>25.0 ± 0.2</td>
</tr>
<tr>
<td>40°</td>
<td>24.1 ± 1.0</td>
</tr>
<tr>
<td>Ogive</td>
<td>24.6 ± 0.2</td>
</tr>
</tbody>
</table>

There does not appear to be any significant differences between the load/displacement responses of the specimens pierced with different spike geometries. All specimens show consistent progressive failure until the damage becomes steady. The load/displacement response of the drilled specimens show a single, catastrophic, failure and a significant load drop from this point. In line with the testing standard, the test was stopped at this point so it is not possible to comment on whether the damage progression becomes steady or not. The differences between failure of the pierced and drilled specimens, however, is evident. The more progressive failure nature of the pierced specimens could increase the energy absorption during failure, which would be beneficial to crash impact structures or primary structures where it is advantageous for progressive failure rather than catastrophic failure.

7.2.4 Failure Mechanisms of Pierced Laminates Under Plain-Pin Bearing Load
The failed drilled specimens are shown in Figure 7.5 and exhibit a similar level of damage throughout. Failed pierced specimens (for 20° spike angle) are shown in Figure 7.6. Two specimens (on the left hand side) show no visual damage where the test was stopped after the initial load drop during testing. The remaining three specimens show the final damage that resulted from the prolonged testing. The drilled specimen testing was stopped after the first
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(catastrophic) failure and, subsequently, show less damage than that of the fully tested pierced specimens.

Figure 7.5: Failed drilled specimens loaded in plain-pin bearing (where specimens are 36 mm wide)

stopped after first load drop

Figure 7.6: Failed pierced specimens (for 20° spike angle) loaded in plain-pin bearing. Two left-hand specimens had tests stopped after first load drop (where specimens are 36 mm wide)
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The images of the failed specimens show that the pierced specimens do not show significant signs of damage under visual inspection after initial failure. For the same initial failure event, the drilled specimens show significantly more damage. The displacement of the 0° fibres around the hole will have promoted 0° ply splitting as the pin progressed in the 0° direction. This is likely to have caused the more progressive failure nature for the pierced specimens.

7.2.5 Strengths of Pierced Laminates Under Plain-Pin Bearing Load

The normalised ultimate pin bearing strengths of the pierced specimens, calculated using the local specimen thicknesses as percentages of the drilled specimen strengths, are shown in Figure 7.7 a). The strength data, including the drilled specimen strengths, are shown in Table 7.3. The same data for the pierced specimens calculated using the nominal specimen thicknesses are shown in Figure 7.7 b) and Table 7.3.

The specimen data include the results from all five specimens tested. The previously presented load/displacement responses show that the first failure was at the maximum force applied to the specimens. The specimens that were stopped after first failure, therefore, were included in the calculated bearing strengths presented here. The results show that the pierced specimen strengths (calculated using the local specimen thickness) vary from 61% to 65% of the drilled specimen strength for increasing piercing spike angle and the ogive spike.

The pierced specimen strengths (calculated using the nominal specimen thicknesses) were between 71% and 77% of the drilled specimen strength when varying the piercing spike angle and for the ogive spike. The results calculated using the nominal specimen thicknesses show a higher percentage of the drilled strength when compared with the stresses calculated using the local thicknesses. This was expected due to the reduction in area over which the stress calculation is made.
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Figure 7.7: Ultimate strengths of pierced specimens under plain-pin bearing load (error bars showing standard error) a) calculated using local specimen thickness; b) calculated using nominal specimen thickness

Table 7.3: Ultimate strengths of drilled and pierced specimens under plain-pin bearing load (pierced specimen strengths calculated using local and nominal specimen thicknesses) (with standard errors)

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Piercing spike angle</th>
<th>Calculated using local specimen thickness</th>
<th>Calculated using nominal specimen thickness</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Pin bearing strength (MPa)</td>
<td>Pin bearing strength (% of drilled)</td>
</tr>
<tr>
<td>D1 (Drilled)</td>
<td>-</td>
<td>618 ± 1</td>
<td>100 ± 0</td>
</tr>
<tr>
<td>S1</td>
<td>20°</td>
<td>376 ± 2</td>
<td>61 ± 0</td>
</tr>
<tr>
<td>A3</td>
<td>30°</td>
<td>397 ± 2</td>
<td>64 ± 0</td>
</tr>
<tr>
<td>S2</td>
<td>40°</td>
<td>388 ± 8</td>
<td>63 ± 1</td>
</tr>
<tr>
<td>S3</td>
<td>Ogive</td>
<td>399 ± 5</td>
<td>65 ± 1</td>
</tr>
</tbody>
</table>

The plain-pin bearing tests showed that the pierced specimen strengths (for all spike geometries) were significantly lower than the drilled specimen strengths. Although the rearrangement of fibres around the hole provided an increase in the open-hole performance of specimens, it is likely that this fibre re-arrangement led to the reduction in strength for this loading case. The fibres within the drilled specimens will remain in the 0° and 90° orientations up to the edge of the hole (as verified in Chapter 4). The resultant fibre structure after piercing, however, was shown to arrange the fibres into tangential orientations to the hole surface. The
subsequent loading of the material in the pin-bearing tests will, therefore, load the fibres differently for the different hole making techniques.

For the drilled specimens the 0° fibres will be loaded longitudinally to give fibre dominated behaviour in terms of stiffness and behaviour in the near hole region, as shown in Figure 7.8 a). The pierced specimens, under pin loading, will only have the displaced 90° fibres remaining in the loading region and these fibres will be loaded transversely to their orientation, which results in matrix dominated behaviour within this region, Figure 7.8 b). It is, therefore, not surprising that the ultimate strength of the pierced specimens did not match that of the drilled specimens under pin loading.

![Schematic of fibre orientations in specimens with indicated pin loading direction](image)

**Figure 7.8: Schematic of fibre orientations in specimens with indicated pin loading direction**

a) drilled specimens; b) pierced specimens

The change of spike angle did not consistently affect the plain-pin bearing strengths for the pierced specimens. The spike angle of 20° provided a slight reduction in pin bearing strength results when compared with the other specimen types. There does not appear to be a significant trend within the results, however, so it is unclear why this reduction was found.
7.2.6 Summary of Plain-Pin Bearing Properties of Pierced Laminates
The pierced specimens had a similar stiffness to the drilled specimens and also showed a slight bedding in process at the start of loading, which was not seen for the drilled specimens. The failure response for the pierced specimens showed a more progressive failure than the drilled specimens. The drilled specimens showed a significant, and catastrophic, reduction in loading capability after failure.

The ultimate strengths of the pierced specimens were not significantly affected by the change in spike geometry. The bearing strengths of all the pierced specimens were, however, significantly lower than for the drilled specimens. This was suggested to be due to the change in loading of the fibres, which were parallel to the loading direction for drilled specimens and perpendicular to the loading direction for pierced specimens.

7.3 Bolted Bearing Properties of Pierced Laminates

7.3.1 Bolted Bearing Specimen Manufacture and Testing Set-Up
Bolted bearing tests followed the standard ASTM D5961/D5961M/13 (Procedure A) (ASTM, 2013) where possible. The same specimen material and manufacturing method were used as for the plain-pin bearing tests (cross-ply CF/PEEK, approximately 2.9 mm thick). The tested specimen types were the same as those used for the plain-pin bearing tests, shown in Table 7.1. The specimens were manufactured in the same way as that described for the previous testing; drilled specimens were cut from a panel and then drilled and pierced specimens were pierced first and then cut from the panel. The specimen dimensions for testing were nominally 36 mm wide and 135 mm long with a 6 mm diameter hole and a distance from the hole centre to top edge of 18 mm (E/d = 3 and W/d = 6), as shown in Figure 7.9. The test jigging was manufactured according to the standard and provided a fixed clamping area (12 mm in diameter) to the specimens (Figure 7.10).
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Figure 7.9: Schematic diagram of bolted bearing specimens

Figure 7.10: Bolted bearing testing set-up a) photograph of the testing configuration; b) schematic diagram of the jigging and specimen with the applied loading directions
7.3.2 Bolted Bearing Testing Procedure

A total of five specimens were tested for each specimen type. The loading bolt was torqued to 3 Nm to provide the clamping force during testing. The same test machine used for the pin bearing tests was used for these tests and a grip length of 50 mm was used for both the jigging and the specimens. The applied force and cross-head displacement were measured for all tests and a constant displacement rate of 1 mm/min was applied to all specimens.

In a similar fashion to the pin bearing testing, two of the five specimen tests for each specimen group were stopped after first failure. This allowed first failure data to be measured and subsequent visual inspection and microscopy to determine the initial mode of failure. The remaining specimens, not stopped after first failure, were tested until the applied load dropped significantly or to zero indicating complete failure of the specimen.

Load/displacement responses of the pierced and drilled specimens are presented and discussed and then the first failure data is shown. The first failure microscopy analysis is also presented and the findings are discussed with comparisons between the pierced and drilled specimens. The ultimate strength data is shown in a similar way to the plain-pin bearing section; the strengths are calculated using local and nominal thickness dimensions. The stress at 4% hole elongation was also calculated for local and nominal specimen thickness and is presented with the ultimate strength data.

For some joint applications the design strength of a fastened hole can be calculated as the stress at 4% deformation of the hole (Kelly and Hallstrom, 2005). Many structures, especially within aerospace, are replaced after a pre-requisite deformation (or hole elongation). This reduces the likelihood of a catastrophic failure during operation and can, in some instances, allow the structure to be repaired and returned to use in operation (saving significant costs). The stress was calculated using the applied force required to achieve 4% hole elongation.
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The bedding in process was taken into account for the applicable specimens by offsetting the displacement origin, as shown in Figure 7.11.

![Figure 7.11: Diagram of how the values are determined for calculating stress at 4% hole elongation.](image)

7.3.3 Load/Displacement Response of Pierced Laminates Under Bolted Bearing Load

The load/displacement responses of the drilled specimens are show in Figure 7.12 a). The responses of pierced specimens for 20°, 30°, 40° and ogive spike geometries under bolted bearing loading are shown in Figure 7.12 b) – e), respectively. Finally, representative data for all specimen types were plotted on the same axes for comparison in Figure 7.12 g) (the dotted lines in the previous plots show the re-plotted representative data).

The load/displacement responses show that the initial loading behaviour for all the specimens show good repeatability up to the first failure/load drop. All the pierced specimens have an initial bedding in process (except for the ogive spike data) before exhibiting a linear increase in displacement with increasing applied load, as shown for the drilled specimens. The mean stiffness of the pierced and drilled specimens are shown in Table 7.4. The stiffness of each specimen was measured between 4 kN and 8 kN applied force. The table shows how all of the pierced specimens have approximately the same stiffness despite the change in spike geometry (approximately 22 kN/mm). The drilled specimens, however, show a significantly lower stiffness than the pierced specimens (16.5 kN/mm).
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![Force vs Displacement Graphs for Various Piercing Geometries](image)

**Figure 7.12**: Load/displacement responses of bolted bearing loaded specimens (dotted lines indicating data used for representative comparison) a) drilled; b) 20° conical spike; c) 30° conical spike; d) 40° conical spike; e) ogive spike; f) typical representative responses

**Table 7.4**: Mean specimen stiffness for drilled and pierced specimens with varying piercing spike geometry during bolted bearing tests (with standard error)

<table>
<thead>
<tr>
<th>Spike angle</th>
<th>Stiffness (kN/mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drilled</td>
<td>16.5 ± 0.4</td>
</tr>
<tr>
<td>20°</td>
<td>21.5 ± 0.3</td>
</tr>
<tr>
<td>30°</td>
<td>21.7 ± 0.4</td>
</tr>
<tr>
<td>40°</td>
<td>21.5 ± 0.6</td>
</tr>
<tr>
<td>Ogive</td>
<td>22.0 ± 0.3</td>
</tr>
</tbody>
</table>
The drilled specimens show a larger strength recovery after first failure when compared with all the pierced specimen groups. The applied load then decreases until final failure of the specimens. After the initial failure event, the pierced specimens (except two for the ogive spike) show smaller strength recoveries until a final failure event occurs. The final failures of the specimens are shown to occur at different displacements for all the specimen groups. The ogive spike geometry test data, however, only shows a strength recovery in one of the specimens tested to final failure. The first failure of the remaining two specimens occurs at the ultimate strength and the applied load decreases until ultimate failure.

The load/displacement responses of the pierced and drilled specimens show good repeatability in most cases up to final failure of the specimens. A decrease in the stiffness of drilled specimens was shown for the bolted bearing tests; this was not shown for the pin bearing tests. It is likely that the reduction in the E/d ratio for bolted bearing tests has affected the deformation of material under loading and, hence, the stiffness. The E/d ratio was highlighted in previous literature (Chapter 2) to significantly affect how specimens deform and fail under bearing load. In the literature, drilled specimens were shown to reduce in strength more significantly than moulded-in hole specimens when reducing the E/d ratio. This was a result of cutting the fibres and increasing the deformation of material loaded by the bolt/pin before shear out failure occurred. This agrees with the lower stiffness values shown for drilled specimens within this investigation, which are, therefore, likely to lead to different failure mechanisms to the pierced specimens.

7.3.4 Failure Mechanisms of Pierced Laminates Under Bolted Bearing Load
Failed drilled specimens are shown in Figure 7.13. Two specimens (on the left hand side) show no visual damage where the test was stopped after the initial failure during testing. The remaining three specimens show the final damage that resulted from the prolonged testing. The drilled specimens show small regions of bearing failure at the top of the hole (Figure 7.14) before what appears to be primarily shear out failure of the specimens.
Failed pierced specimens (for the ogive spike) are shown in Figure 7.15 as examples of the failure damage for all pierced specimen groups. The specimens are laid out in the same way as for the drilled specimens; the two specimens stopped after first failure are shown on the left. The fully failed specimens show varying types of failure including net tension (specimen 2C38), cleavage (specimen 2C39) and a combination of cleavage and shear out (specimen 2C40). There is, however, common failure within the three specimens in the halo regions that were highlighted in the DIC analysis when open-hole testing.

Figure 7.13: Failed drilled specimens loaded in bolted bearing (where specimens are 36 mm wide)

Figure 7.14: Failed drilled specimen loaded in bolted bearing showing initial bearing failure (where the specimen is 36 mm wide)
The final failure of the pierced specimens occur at varying displacements, which is most likely influenced by differences in final failure mode (shown in Figure 7.15). The distance from the hole centre to the end of the specimen was set according to the standard (18 mm). The heated area, however, incorporated the re-distribution of fibres, which occurred over a significant portion of this distance (15 mm radius). The proximity of the heated area boundary and the edge of the specimen may have influenced the final fracture mechanism. This is an area that needs further consideration in future work.

In contrast to the pin-bearing results, the bolted configuration prevented through thickness expansion and limited the progression of damage from the edge of the hole. The clamping force applied by the torqued bolt significantly changed the failure response when compared with the pin loaded specimens. This was shown by the strength recoveries observed for almost all of the specimens tested (with exception of the two ogive spike specimens).
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When compared with the drilled specimen responses, the pierced specimens show a steady damage progression instead of a significant strength recovery (except for the two ogive spike specimens). This is, again, likely to be an effect of the size of the heated area, but related to the size of the clamped area. The images of the failed pierced specimens show fractures through areas that propagate from the hole. These fractures are continuing within the clamped area of the test. The failed drilled specimens do not show this as significantly. There appears to be initial damage around the hole edge, but subsequent loading until final failure shows significantly more damage and fracture of the laminate outside of the clamped area. This suggests that after an initial failure event the pierced specimens continue to fail in the same region and a stable propagation of damage occurs until ultimate failure. For the drilled specimens, however, the initial failure is arrested and the final failure location is moved to the unclamped area outside of the jigging.

7.3.5 First Failure of Pierced Laminates Under Bolted Bearing Load
The first failure forces of the pierced specimens for varying spike geometries are shown in Figure 7.16 and Table 7.5. The first failure forces did not significantly change when increasing the conical spike angle from $20^\circ$ to $40^\circ$ (95% to 97% of the drilled hole first failure force). There is only a single first failure data point for the ogive spike due to the failure response of the specimens. The majority of the specimens within this group did not show a further increase in applied load during testing so the data was used for the ultimate strength calculation rather than the first failure properties, as specified in the testing standard (ASTM, 2013).
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Figure 7.16: First failure forces of pierced specimens under bolted bearing load (with standard error bars – ogive spike has one useful data point, hence shown with zero error)

Table 7.5: First failure forces of drilled and pierced specimens under bolted bearing load (with standard errors – * indicating that only 1 data point was measured for this value, hence zero error)

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Piercing spike angle</th>
<th>First failure force (kN)</th>
<th>First failure force (% of drilled)</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1 (Drilled)</td>
<td>-</td>
<td>10.8 ± 0.1</td>
<td>100 ± 1</td>
</tr>
<tr>
<td>S1</td>
<td>20°</td>
<td>10.2 ± 0.3</td>
<td>95 ± 3</td>
</tr>
<tr>
<td>A3</td>
<td>30°</td>
<td>10.3 ± 0.1</td>
<td>96 ± 1</td>
</tr>
<tr>
<td>S2</td>
<td>40°</td>
<td>10.5 ± 0.2</td>
<td>97 ± 2</td>
</tr>
<tr>
<td>S3</td>
<td>Ogive</td>
<td>10.5*</td>
<td>97*</td>
</tr>
</tbody>
</table>

Optical microscopy was used to investigate the first failure mechanisms in the drilled and pierced specimens. The specimens were sectioned along the 0° ply direction and polished to the centre of the bearing surface, as shown in Figure 7.17. The post failure microstructure of the drilled specimen is shown in Figure 7.18 a) and a higher magnification of a failure feature in Figure 7.18 b). The equivalent post failure region in a pierced specimen (pierced using optimised settings – discussed subsequently) is shown in Figure 7.18 c) and at higher magnification in Figure 7.18 d).
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Figure 7.17: Schematic of first failure specimen section profiles (shown by the dotted line) and the viewing direction (shown by the arrows)

Figure 7.18 Optical microscopy of drilled and pierced specimens sectioned after first failure under bolted bearing loading a) low magnification of drilled specimen; b) high magnification of drilled specimen showing fibre micro-buckling and resultant kink bands as first failure mechanisms; c) low magnification of pierced specimen; d) high magnification of pierced specimen showing matrix cracking as the first failure mechanism.
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The photomicrographs show distinct differences in the first failure mechanism between the drilled and pierced specimens. The drilled specimen has clear kink-bands that continue through adjacent plies. Fibre micro-buckling is also evident in the 0° plies. The pierced specimen shows a matrix dominated failure with matrix cracking progressing from the bearing surface (on the right hand side of the image). The matrix cracks are shown to propagate through the alternating plies into the volume of the laminate. The pierced specimens also show considerable fibre damage. This fibre damage was not evident in the microscopy after piercing so it was likely to have been caused by the crushing of material during the bearing tests.

The photomicrographs also show an increase in thickness of both specimens near the hole edge. Inspection of the test jigging revealed a small chamfer around the hole edge of the jigs. A small part of the material was, therefore, not clamped and was forced into this chamfer during failure to cause the local increase in thickness.

The first failure forces showed no significant differences when varying the piercing spike geometry and were all slightly lower than the drilled specimen first failure force. The change in spike geometry is, therefore, not significantly affecting the initial damage load for the pierced specimens.

The drilled and pierced specimens showed different first failure modes that affect how damage subsequently propagates. The drilled specimen showed fibre micro-buckling as the first failure mechanism. The loading responses show how this mechanism was arrested and a strength recovery was observed before final failure at a significantly higher load. The pierced specimen showed matrix cracking as the first failure mechanism, which was a result of matrix compression failure. This is normally associated with a lower force than that required for fibre micro-buckling. The first failure forces, however, show that the pierced specimens have almost the same first failure force as the drilled specimens. This retention of mechanical performance
is suggested to be caused by the increase in volume fraction near the hole as a result of the piercing process.

The matrix cracking in the pierced specimen was also shown to propagate away from the bearing surface and was likely to be arrested by the fibres to give the small strength recoveries shown in the loading response. The failed specimen images (Figure 7.15), however, show that the cracks subsequently propagated through the halo region and along the displaced 90° fibres to cause final failure.

7.3.6 **Strengths of Pierced Laminates Under Bolted Bearing Load**

The ultimate bolted bearing strengths of the pierced specimens calculated using the local and nominal specimen thicknesses (as percentages of the drilled specimen strengths) are shown in Figure 7.19 a) and b), respectively. The strength data, including the drilled specimen strengths, are shown in Table 7.6. The specimen data include the results from the three specimens tested to ultimate failure. The other two specimens of each type were stopped at first failure. Subsequent strength recoveries were observed for the majority of specimens after first failure so the ultimate failure loads were not equal to the first failure loads (in contrast to the pin bearing testing).

The results show that the pierced specimen strengths (calculated using the local specimen thickness) vary from 66% to 69% of the drilled specimen strength for varying conical piercing spike angle and 57% for the ogive spike. The pierced specimen strengths (calculated using the nominal specimen thicknesses) were between 76% and 80% of the drilled specimen strength when varying the conical piercing spike angle and 66% for the ogive spike. The results calculated using the nominal specimen thicknesses show a higher percentage of the drilled strength when compared with the stresses calculated using the local thicknesses. This was, as mentioned for the pin bearing tests, expected due to the reduction in area over which the stress calculation was made.
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Figure 7.19: Ultimate strengths of pierced specimens under bolted bearing load (error bars showing standard error) a) calculated using local specimen thickness; b) calculated using nominal specimen thickness

Table 7.6: Ultimate strengths of drilled and pierced specimens under bolted bearing load (pierced specimen strengths calculated using local and nominal specimen thicknesses) (with standard errors)

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Piercing spike angle</th>
<th>Calculated using local specimen thickness</th>
<th>Calculated using nominal specimen thickness</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Bolted bearing strength (MPa)</td>
<td>Bolted bearing strength (% of drilled)</td>
</tr>
<tr>
<td>D1 (Drilled)</td>
<td>-</td>
<td>928 ± 7</td>
<td>100 ± 1</td>
</tr>
<tr>
<td>S1</td>
<td>20°</td>
<td>639 ± 22</td>
<td>69 ± 2</td>
</tr>
<tr>
<td>A3</td>
<td>30°</td>
<td>609 ± 4</td>
<td>66 ± 0</td>
</tr>
<tr>
<td>S2</td>
<td>40°</td>
<td>633 ± 20</td>
<td>68 ± 2</td>
</tr>
<tr>
<td>S3</td>
<td>Ogive</td>
<td>527 ± 9</td>
<td>57 ± 1</td>
</tr>
</tbody>
</table>

The stress at 4% hole elongation for the pierced specimens, calculated using the local and nominal specimen thicknesses (as percentages of the drilled specimen performance), are shown in Figure 7.20 a) and b), respectively. The strength data, including the drilled specimen stresses, are shown in Table 7.7. The point at which the hole was elongated by 4% occurs prior to any failure event, so the data include the results from all five specimens tested. The results show that the pierced specimen stresses at 4% hole elongation (calculated using the
local specimen thickness) vary from 99% to 101% of the drilled specimen stress for varying piercing spike geometries.

The stress at 4% hole elongation for the pierced specimens (calculated using the nominal specimen thicknesses) were between 115% and 118% of the drilled specimen stress when varying piercing spike geometry. As with all the previous results, the results calculated using the nominal specimen thicknesses show a higher percentage of the drilled performance when compared with the stresses calculated using the local thicknesses.

![Graph showing stress at 4% hole elongation for pierced specimens under bolted bearing load](https://via.placeholder.com/150)

**Figure 7.20:** Stress at 4% hole elongation for pierced specimens under bolted bearing load (error bars showing standard error) a) calculated using local specimen thickness; b) calculated using nominal specimen thickness

The ultimate strength results for the bolted bearing tests showed higher percentages of the drilled strengths than those shown for the plain-pin bearing tests. The clamping force applied by the torqued bolt under loading prevented through thickness expansion of both the pierced and drilled specimens. The pierced specimens, however, were more likely to expand in the through thickness direction as a result of the re-orientation of plies in this direction close to the hole (shown in the X-ray CT images in Chapter 4). This would promote a through thickness expansion under loading and clamping the laminate (preventing this from occurring) would delay the onset of delamination and failure.
The ultimate bearing strengths for all pierced specimens were lower than that for drilled specimens. In a similar fashion to the pin bearing results, this is a result of the fibre structure after piercing and propagation of damage through the matrix within the clamped region, as mentioned previously. For the ultimate strengths calculated using both the local and nominal specimen dimensions, there was no significant difference for the various conical spikes. There was, however, a reduction in strength shown for the ogive spike.

In Chapter 3 the ogive spike was considered to have the smallest overall spike angle (i.e. the sharpest spike) despite having the largest tip angle (>40°). The apparent reduction in strength of the ogive spike when compared with the pin bearing results is more likely, in fact, to be a result of a less significant increase in strength when compared with the other spike geometries. A reduction in spike angle decreased the resistance force on the spike and, therefore, most likely produced less through thickness (z-axis) deformation of material. This would reduce the through thickness expansion under loading and decrease the reinforcing effect of the clamping applied in the bolted bearing test.

### Table 7.7: Stress at 4% hole elongation for drilled and pierced specimens under bolted bearing load (pierced specimen stresses calculated using local and nominal specimen thicknesses) (with standard errors)

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Piercing spike angle</th>
<th>Calculated using local specimen thickness</th>
<th>Calculated using nominal specimen thickness</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Bearing stress at 4% hole elongation (MPa)</td>
<td>Bearing stress at 4% hole elongation (%) of drilled</td>
</tr>
<tr>
<td>D1 (Drilled)</td>
<td>-</td>
<td>262 ± 8</td>
<td>100 ± 3</td>
</tr>
<tr>
<td>S1</td>
<td>20°</td>
<td>260 ± 3</td>
<td>99 ± 1</td>
</tr>
<tr>
<td>A3</td>
<td>30°</td>
<td>261 ± 1</td>
<td>100 ± 0</td>
</tr>
<tr>
<td>S2</td>
<td>40°</td>
<td>264 ± 6</td>
<td>101 ± 2</td>
</tr>
<tr>
<td>S3</td>
<td>Ogive</td>
<td>265 ± 2</td>
<td>101 ± 1</td>
</tr>
</tbody>
</table>
The stresses at 4% hole elongation were all taken from within the elastic (linear) loading region of the testing. As a result of this, the results were expected to agree with the stiffness data shown in the load/displacement analysis. This was confirmed with the stresses at 4% hole elongation calculated using the nominal specimen dimensions; an increase in the 4% hole elongation stress was shown for all pierced specimens when compared with drilled. The calculations using local specimen dimensions, however, showed approximately equivalent performance (as a result of the larger area used for the calculations).

An increase in all pierced specimen performance values was shown when changing the method of calculation from using the local thickness to the nominal thickness. This was highlighted previously due to the reduction in loading area used to calculate the strengths. The differences show that in some cases the pierced specimens under-perform or out-perform the drilled specimens when using the local or nominal dimensions, respectively. Both sets of data were included since both calculation methods are valid. From a scientific point of view, the local bearing area has been increased and should be incorporated into the calculation. From an industrial application point of view, however, the overall structure dimensions remain the same and the increase in thickness is a result of the process that should not be included within a like-for-like comparison against a drilled structure.

7.3.7 Summary of Bolted Bearing Properties of Pierced Laminates
The load/displacement plots show that for the bolted bearing tests, similarly to the pin bearing tests, most of the pierced specimens exhibited bedding in processes when initially loaded. The loading responses also showed that pierced specimens had higher stiffness than drilled specimens prior to first failure. The pierced specimens subsequently show a small strength recovery (except for the ogive spike specimens) after initial failure. The drilled specimens show more significant strength recoveries after initial failure when compared with the pierced specimens.
The first failure forces of the pierced specimens show no significant differences when varying the spike geometry. The pierced specimens all showed slightly lower first failure forces when compared with the drilled specimens. The pierced specimens exhibited matrix cracking as the initial failure mode, whilst drilled specimens failed initially due to fibre micro-buckling. The difference in failure mode can be attributed to the change in fibre structure in the loading region. As discussed for the pin bearing tests, the 0° fibres in the drilled specimens were loaded parallel to their orientation whereas the displaced fibres in the pierced specimens were loaded mostly perpendicular to the fibre orientation.

The images of the failed specimens also show differences in how the initial damage propagated through the material. After first failure of the pierced specimens the cracks continued to propagate away from the primary bearing surface and along the halo regions until ultimate failure. The drilled specimens, however, showed an initial failure at the hole edge before an ultimate failure in a location outside of the clamped area, which was likely to cause the significant strength recoveries observed.

The ultimate strengths of all the pierced specimens (calculated using local and nominal specimen dimensions) were significantly lower than that for the drilled specimens. There was also no distinct trend in the variation of results for changing spike geometry. The stress at 4% hole elongation also showed no significant variation for the different spike geometries used. The values calculated using local specimen dimensions were similar for pierced and drilled specimens. The stresses calculated using nominal specimen dimensions, however, showed significantly higher results than the drilled specimens, agreeing with the stiffness data.

7.4 Pin-Bearing and Bolted Bearing Test Optimisation Study

7.4.1 Optimisation study specimen manufacture, geometry and test set-up
Specimens were cut from panels manufactured using the same material and method described for the previous tests. The specimens used for this optimisation study were the
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same as those used in the open-hole testing optimisation study. The heated area diameter, processing temperature, piercing spike angle and clamping pressure are reiterated in Table 7.8. The heated area, processing temperature and clamping pressure were selected based on the results of the open-hole testing and microscopy findings. The smallest spike angle (20°) was chosen for the optimised testing based on the bolted bearing strength. Although the ultimate strength differences were not large enough to be considered significant, the mean strength was slightly higher for the 20° spike.

Table 7.8: Optimised piercing process variables used for bearing test optimisation study specimen manufacture

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Piercing process variables</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Heated area diameter (D – 2D)</td>
</tr>
<tr>
<td>O1 (Optimised)</td>
<td>30 mm (D)</td>
</tr>
</tbody>
</table>

Specimen dimensions, hole size and preparation all remained consistent with that used for the previous pin and bolted bearing tests. Five specimens were tested for each specimen group and the grip length, and corresponding gauge lengths, were the same as those used in the respective bearing tests. The same test machine was used for the optimised tests as described previously and a constant displacement rate of 1 mm/min was applied until ultimate failure of the specimens. Applied force and cross-head displacement were measured throughout all tests.

7.4.2 Load/displacement response of laminates pierced using optimised piercing conditions under plain-pin bearing load

The load/displacement response of specimens pierced using optimised piercing conditions under plain-pin bearing load are shown in Figure 7.21 a). The data shows that the response to loading is repeatable. Figure 7.21 b) shows the typical responses to loading for all the specimens tested previously and a typical optimised specimen. The optimised specimens
deform and fail in the same progressive way as the other pierced specimens; not catastrophically like the drilled specimens. The mean stiffness of all the specimens tested under pin bearing load, including the optimised specimens, are shown in Table 7.9 (the stiffness of the optimised specimens was measured over the same applied load range as used previously). The stiffness of the optimised specimens is not significantly different to the previous results.

![Load/displacement responses of plain-pin bearing loaded specimens](image1.png)

**Figure 7.21**: Load/displacement responses of plain-pin bearing loaded specimens (dotted line indicating data used for representative comparison) a) optimised piercing conditions; b) typical representative responses

<table>
<thead>
<tr>
<th>Spike angle</th>
<th>Stiffness (kN/mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drilled</td>
<td>24.5 ± 0.6</td>
</tr>
<tr>
<td>20°</td>
<td>25.1 ± 0.6</td>
</tr>
<tr>
<td>30°</td>
<td>25.0 ± 0.2</td>
</tr>
<tr>
<td>40°</td>
<td>24.1 ± 1.0</td>
</tr>
<tr>
<td>Ogive</td>
<td>24.6 ± 0.2</td>
</tr>
<tr>
<td>Optimised</td>
<td>25.1 ± 0.4</td>
</tr>
</tbody>
</table>

**Table 7.9**: Mean specimen stiffness for drilled and pierced specimens with varying piercing spike geometry and optimised piercing conditions during pin bearing tests (with standard error)
7.4.3 Strengths of laminates pierced using optimised piercing conditions under plain-pin bearing load

The ultimate pin bearing strengths of the optimised specimens were calculated using the local specimen dimensions and the nominal specimen dimensions and are shown with the previous test data in Figure 7.22 a) and b), respectively. The strength data is also reiterated with the addition of the optimised results in Table 7.10. When using the local specimen dimensions, the pin bearing strength of the specimens pierced under optimised piercing conditions was 67% of the drilled strength. The ultimate strength of the optimised specimens calculated using the nominal dimensions was 77% of the drilled hole strength.

![Figure 7.22: Ultimate strengths of pierced specimens under plain-pin bearing load (error bars showing standard error) a) calculated using local specimen thickness; b) calculated using nominal specimen thickness](image)

There was no significant change in the ultimate strengths when previously varying the spike geometry. This was also the case for the optimised piercing conditions, which showed only a slight increase in strength when compared to the 20° spike strengths (although not significantly different to most of the previous results).
Table 7.10: Ultimate strengths of drilled and pierced specimens under plain-pin bearing load (pierced specimen strengths calculated using local and nominal specimen thicknesses) (with standard errors)

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Piercing spike angle</th>
<th>Calculated using local specimen thickness</th>
<th>Calculated using nominal specimen thickness</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Pin bearing strength (MPa)</td>
<td>Pin bearing strength (% of drilled)</td>
</tr>
<tr>
<td>D1 (Drilled)</td>
<td>-</td>
<td>618 ± 1</td>
<td>100 ± 0</td>
</tr>
<tr>
<td>S1</td>
<td>20°</td>
<td>376 ± 2</td>
<td>61 ± 0</td>
</tr>
<tr>
<td>A3</td>
<td>30°</td>
<td>397 ± 2</td>
<td>64 ± 0</td>
</tr>
<tr>
<td>S2</td>
<td>40°</td>
<td>388 ± 8</td>
<td>63 ± 1</td>
</tr>
<tr>
<td>S3</td>
<td>Ogive</td>
<td>399 ± 5</td>
<td>65 ± 1</td>
</tr>
<tr>
<td>O1 ( Optimised)</td>
<td>20°</td>
<td>415 ± 6</td>
<td>67 ± 1</td>
</tr>
</tbody>
</table>

7.4.4 Load/displacement response of laminates pierced using optimised piercing conditions under bolted bearing load

The load/displacement responses of specimens pierced using optimised piercing conditions under bolted bearing load are shown in Figure 7.23 a). A typical response from the optimised specimens is also shown with the previously presented data in Figure 7.23 b). The data show that the specimens pierced using optimised conditions still show a similar bedding in process to the other tested specimens when initially loaded. The repeatability of the initial loading response is good for the optimised specimens and agrees with the previous specimens.
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The stiffness of the optimised specimens prior to first failure is shown, with the previous data, in Table 7.11 (again, the stiffness was measured over the same applied load range as before). The data shows that the stiffness of the optimised specimens is not significantly different to the stiffness of pierced specimens with varying spike geometry. The stiffness does, however, remain significantly higher than for the drilled specimens.

Table 7.11: Mean specimen stiffness for drilled and pierced specimens with varying piercing spike geometry and optimised piercing conditions during bolted bearing tests (with standard error)

<table>
<thead>
<tr>
<th>Spike angle</th>
<th>Stiffness (kN/mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Drilled</td>
<td>16.5 ± 0.4</td>
</tr>
<tr>
<td>20°</td>
<td>21.5 ± 0.3</td>
</tr>
<tr>
<td>30°</td>
<td>21.7 ± 0.4</td>
</tr>
<tr>
<td>40°</td>
<td>21.5 ± 0.6</td>
</tr>
<tr>
<td>Ogive</td>
<td>22.0 ± 0.3</td>
</tr>
<tr>
<td>Optimised</td>
<td>22.2 ± 0.3</td>
</tr>
</tbody>
</table>

7.4.5 First failure of laminates pierced using optimised piercing conditions under bolted bearing load

The first failure force of the specimens pierced using optimised conditions are shown, with the previously presented first failure forces, in Figure 7.24. The data is also summarised in Table 7.12 and shows a significant increase in first failure force to 110% of the drilled hole performance when compared with the other specimen groups.
As mentioned previously, there was no significant change in the first failure forces when varying the piercing spike angle. The change in piercing conditions to the optimised settings, however, showed a significant increase in the first failure force and suggests that the change in processing temperature and clamping pressure have had an advantageous effect on the initial failure load. It is likely that the increase in processing temperature helped reduce fibre fractures, whilst the increase in clamping pressure reduced void content (therefore increasing fibre volume fraction) to improve the first failure load.
7.4.6 **Strengths of laminates pierced using optimised piercing conditions under bolted bearing load**

Figure 7.25 a) and b) show the ultimate bolted bearing strengths of all the tested specimens calculated using the local and nominal specimen thicknesses (as percentages of the drilled specimen strengths), respectively. The strength data, including the drilled specimen strengths, are shown in Table 7.13. For the strengths calculated using the local specimen dimensions, the bolted bearing strength increased to 74% of the drilled strength for the specimens pierced using the optimised piercing conditions. The ultimate strength of the specimens pierced under optimised piercing conditions was 85% of the drilled hole strength when calculated using the nominal specimen dimensions.

![Figure 7.25](image)

Figure 7.25: Ultimate strengths of pierced specimens under bolted bearing load (including specimens pierced under optimised conditions) (error bars showing standard error) a) calculated using local specimen thickness; b) calculated using nominal specimen thickness
Table 7.13: Ultimate strengths of drilled and pierced specimens under bolted bearing load, including specimens pierced under optimised conditions (pierced specimen strengths calculated using local and nominal specimen thicknesses) (with standard errors)

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Piercing spike angle</th>
<th>Calculated using local specimen thickness</th>
<th>Calculated using nominal specimen thickness</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Bolted bearing strength (MPa)</td>
<td>Bolted bearing strength (% of drilled)</td>
</tr>
<tr>
<td>D1 (Drilled)</td>
<td>-</td>
<td>928 ± 7</td>
<td>100 ± 1</td>
</tr>
<tr>
<td>S1</td>
<td>20°</td>
<td>639 ± 22</td>
<td>69 ± 2</td>
</tr>
<tr>
<td>A3</td>
<td>30°</td>
<td>609 ± 4</td>
<td>66 ± 0</td>
</tr>
<tr>
<td>S2</td>
<td>40°</td>
<td>633 ± 20</td>
<td>68 ± 2</td>
</tr>
<tr>
<td>S3</td>
<td>Ogive</td>
<td>527 ± 9</td>
<td>57 ± 1</td>
</tr>
<tr>
<td>O1 (Optimised)</td>
<td>20°</td>
<td>685 ± 12</td>
<td>74 ± 1</td>
</tr>
</tbody>
</table>

Figure 7.26 a) and b) show the stress at 4% hole elongation for all the tested specimens calculated using the local and nominal specimen thicknesses (as percentages of the drilled specimen strengths), respectively. The strength data, including the drilled specimen strengths, are also shown in Table 7.14. The stresses calculated for the optimised specimens using local specimen dimensions remained consistent with the other pierced specimens at approximately 100% of the drilled hole performance. The stress at 4% hole elongation for the specimens pierced under optimised piercing conditions was slightly lower than the previous specimens at 113% of the drilled hole strength when calculated using the nominal specimen dimensions.
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Figure 7.26: Stress at 4% hole elongation for pierced specimens under bolted bearing load (including specimens pierced under optimised conditions) (error bars showing standard error) a) calculated using local specimen thickness; b) calculated using nominal specimen thickness

Table 7.14: Stress at 4% hole elongation for drilled and pierced specimens under bolted bearing load, including specimens pierced under optimised conditions (pierced specimen stresses calculated using local and nominal specimen thicknesses) (with standard errors)

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Piercing spike angle</th>
<th>Calculated using local specimen thickness</th>
<th>Calculated using nominal specimen thickness</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Bearing stress at 4% hole elongation (MPa)</td>
<td>Bearing stress at 4% hole elongation (% of drilled)</td>
</tr>
<tr>
<td>D1 (Drilled)</td>
<td>-</td>
<td>262 ± 8</td>
<td>100 ± 3</td>
</tr>
<tr>
<td>S1</td>
<td>20°</td>
<td>260 ± 3</td>
<td>99 ± 1</td>
</tr>
<tr>
<td>A3</td>
<td>30°</td>
<td>261 ± 1</td>
<td>100 ± 0</td>
</tr>
<tr>
<td>S2</td>
<td>40°</td>
<td>264 ± 6</td>
<td>101 ± 2</td>
</tr>
<tr>
<td>S3</td>
<td>Ogive</td>
<td>265 ± 2</td>
<td>101 ± 1</td>
</tr>
<tr>
<td>O1 (Optimised)</td>
<td>20°</td>
<td>259 ± 2</td>
<td>99 ± 1</td>
</tr>
</tbody>
</table>

The specimens pierced using optimised processing conditions showed a small increase in ultimate strength when compared with the other pierced specimens. The change in spike geometry was expected to have a small effect on the local microstructure within pierced specimens. The ultimate failure of the pierced specimens, however, was identified to be within the halo regions away from the initial failure location. The microstructure in the halo regions,
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affecting the propagation of damage and ultimate failure, would be more affected by the heated area, processing temperature and clamping pressure – as suggested by the results. On the other hand, the stress at 4% hole elongation was slightly reduced for the optimised specimens when compared to the previously tested specimens with varying spike geometry.

7.4.7 Summary of plain-pin and bolted bearing test optimisation study
For the plain-pin bearing tests, the load/displacement response and the stiffness of the specimens pierced using optimised piercing conditions showed no significant differences to the results for the previously tested pierced specimens. The ultimate strength of the optimised specimens also showed negligible difference in performance when compared with the previously tested pierced specimens.

For the bolted bearing tests the specimens pierced using optimised conditions showed no difference in stiffness to the other pierced specimens tested. There was, however, a significant increase in the first failure force when compared with the drilled specimens. This increase in first failure force is suggested to be caused by the increase in fibre volume fraction near the hole as a result of the piercing process.

The subsequent ultimate strength of the specimens pierced using optimised conditions showed a slight improvement when compared with the other pierced specimens, but were still lower than the drilled specimen strength. Despite an increase in first failure force, the propagation of damage was suggested previously to continue reducing performance until ultimate failure within the pierced specimens (as opposed to the drilled specimens when a change in failure location allows a strength recovery).

The stress at 4% hole elongation for the pierced specimens using optimised conditions was, similarly to the other pierced specimens, equal to the drilled specimens (as expected from the
equal stiffness) when calculated using the local specimen dimensions. If calculated using the nominal specimen dimensions there was a significant improvement in the stress at 4% hole elongation when compared with the drilled.

7.5 Concluding Remarks
Plain-pin and bolted bearing tests were used to determine the mechanical performance of pierced specimens for joining applications. The results showed that the failure responses of pierced specimens were progressive and steady for all cases (prior to the final failure for bolted specimens). This differed to the response of drilled specimens under plain-pin loading, which failed in a catastrophic failure event. The progressive failure nature of the pierced specimens under pin loading could be utilised for energy absorption in crash structures or structures where immediate catastrophic failure poses the highest risk e.g. primary structures.

The ultimate strengths of pierced specimens under pin and bolted bearing loads were not comparable to the drilled specimen strengths (generally 15% to 20% lower). The initial failure modes were determined for the bolted bearing tests and show matrix damage for the pierced specimens and fibre micro-buckling for the drilled specimens. The differences in fibre orientation around the holes as a result of piercing and drilling are suggested to cause the differences in first failure modes. The propagation of this damage is likely to have resulted in the subsequently lower pierced specimen strengths.

The first failure force and stress at 4% hole elongation show that the pierced holes can outperform or produce comparable results to the drilled specimens under bolted bearing load. Design loads for structures and parts often use deformation or an initial damage event as the key performance indicator. Using these criteria, it has been shown that the piercing technique (using optimised piercing conditions) could be used as a direct replacement to drilling without detriment to the joint performance.
8 Concluding Remarks

8.1 Concluding Remarks
The aim of this work was to develop a TAP process for introducing holes into TPCs and to investigate the consequences of the technique on the mechanical behaviour of the material. The review of previous literature culminated in the refinement of more specific objectives for the investigation. The specific objectives were:

- Develop a thermally-assisted piercing rig capable of producing pierced specimens with the flexibility to consistently vary the heated area, processing temperature and spike geometry.
- Characterise the resultant material microstructure after piercing and how this is affected by a change in the processing variables.
- Evaluate the mechanical performance of pierced specimens (using open-hole tension, open-hole compression, pin bearing and bolted bearing tests) and how the microstructure after piercing impacted this mechanical performance.

Following initial tests, a rig was designed and built to investigate the effect of TAP on the mechanical performance of TPC specimens. The investigation has shown that the rearrangement of material during piercing significantly changes the material microstructure. It can introduce regions associated with improved strength and stiffness (high fibre volume fraction) and regions of reduced mechanical performance (voids and resin rich regions).

The open-hole strength of pierced specimens was shown to be significantly affected by the change in microstructure resulting from varying the heated area and processing temperature. By optimising the parameters, within the testing limitations of this investigation, strength improvements of up to 11% and 21% of the drilled hole performance were shown for open-hole tension and compression, respectively.
Under more joint applicable loading, varying the spike geometry was not shown to affect the mechanical performance as significantly as shown for the heated area and processing temperature with open-hole tests. Optimising the piercing conditions in the same way as for the open-hole tests, however, showed that the first failure force and ultimate strengths could be significantly improved – indicating a greater effect on mechanical performance related to the heated area and processing temperature. Despite a lower ultimate strength for pierced holes in a bolted joint test (85% of the drilled hole strength under optimised conditions) the first failure force and stress at 4% hole elongation showed significant improvements when compared with drilled holes (110% and 113% of drilled hole performance, respectively), which could be used as more critical performance indicators in service.

The overall performance in an industrially relevant application, however, ultimately depends on the properties required for that specific scenario. With current industry trends tending towards increasing production efficiencies, process related benefits become of significant importance and the TAP process has the potential to offer advantages in this area.

8.2 Benefits to Industry

The TAP process offers a method of producing holes in thermoplastic composite structures to offer improved retention of mechanical strength (in tension and compression) when compared with conventional drilling. In addition to the resultant performance benefits of the process, there are many other potential benefits that could be exploited by using this technique to machine holes:

- Incorporating an array of spikes into a tooling jig would potentially offer a faster, and hence cheaper, multi-hole machining process.
- The recurring cost of replacing tooling could be significantly reduced due to the limited wear on the spike (which is non-rotating). The machined steel spike is also considerably cheaper than the alternative of a diamond-coated drill bit.
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- The process moulds the inside hole surface to the spike (i.e. tooling) surface – thus tolerances could possibly be controlled to higher and more repeatable standards to reduce expensive part rejections.
- The likelihood of exposed fibres within the inside hole surface is significantly lower than when drilling/cutting. This is a significant consideration for lightning strike performance.
- As a non-dust generating process the health and safety considerations and Foreign Object Debris (FOD) concerns are reduced.
- There is potential to tailor the process to create a material structure according to a predetermined performance requirement – increasing the structural efficiency of parts.
- The process could be considered for subsequent fastening if first failure is of significant importance (or as a technique to install a non-conventional fastening system e.g. self-piercing fastener). There is also the possibility of installing inserts or sensors in a single process.
- The process could be used to create non-circular or non-rotationally symmetric holes. This would allow fastener systems that can only be fitted in a single configuration or to create holes of varying geometry.
- Sympathetically piercing a structure using the TAP technique could limit damage to embedded sensor/cable/fibre optic networks or meshes within the laminated structure.

The above list shows that there are number of possible process-related benefits, in addition to some of the performance based benefits, that should be considered with regard to the TAP technique. In many engineering applications it is not always the strongest component or material that is used for the application. It is often the approach that provides the most efficient solution to the problem in terms of weight, cost or time that is used as the ultimate metric of choice.
8.3 Future Work

There are three main areas highlighted within this investigation for potential future work. These are related to: (i) the halo regions of larger strain observed during the DIC analysis of open-hole tests; (ii) the material displaced into the tooling recess during piercing; and (iii) modelling the piercing process and subsequent mechanical properties.

The halo regions were first identified within the DIC analysis of the open-hole tests. They were also identified within the bolted bearing tests where there was a tendency for the pierced specimens to fail along the halo areas. These regions are therefore important in relation to the propagation of damage and failure of the pierced specimens. Further analysis could identify how the damage develops within these regions and whether the TAP process could be varied to limit, delay or prevent its occurrence.

The material displaced into the recess of the tooling during piercing could not be investigated in sufficient depth within this work. The material microstructure was shown to be similar to the material within the original volume of the specimens at the same distance from the hole edge, but it is unclear whether this extruded material provides any advantages. Further work could be conducted to establish the influence of the additional material volume by machining it away and carrying out the same mechanical tests. This could then clarify whether including or omitting the additional volume in strength calculations provides the most accurate values. In addition, the extent of fibre fracture and the flow within the extruded material may provide further insight into the TAP process.

The final, and most significant, area of further work involves modelling of the piercing process and developing a numerical model for predicting the mechanical properties of pierced specimens. This investigation was primarily focused on building a practical capability and conducting mechanical tests to establish the performance of pierced specimens. Development of a model that can simulate the process and mechanical performance would allow rapid
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variation of piercing parameters and refinement of the TAP process. The work conducted within this investigation provides the results from a wide range of testing configurations to validate future numerical models.

Specific research into the above areas would supplement the work described within this investigation and provide additional knowledge on key areas of the TAP process. This would be of interest and relevance to both academic fields and industrial applications relevant to the TAP process.
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References


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Goodrich Aerostructures (2004) High performance composites (01/05/2004) – A close up of a DynaRohr acoustic panel, showing both the core build up and the perforations that help attenuate noise energy [Online]


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**Sintex - Wausaukee Composites** (2015) Liquid Compression Molding [Online]  


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Appendix A

Digital Image Correlation (DIC) System Verification
A Digital Image Correlation (DIC) system was used to measure the strain field over the surface of specimens during open-hole testing. The technique is non-intrusive and can be used to visualise two-dimensional contour plots of the strain field or to plot the strain along predetermined profiles over the specimen surface (as were presented and discussed in the thesis).

This appendix describes the verification testing that was conducted to confirm the accuracy of the system. Strain gauges were applied to the surface of aluminium and composite specimens and the strain data was compared with that measured by the DIC system. Two materials were tested to determine any variation due to the material. Both strain gauging and DIC require the application of a measuring medium to the surface of the specimen. Strain gauges must be adhered to the surface correctly for the strain to be measured accurately. For the DIC approach, a random speckle pattern must be painted onto the surface of the specimen for the camera system to track (a more detailed overview of the DIC system is included in section 6.2 of the thesis).

Generally, it is difficult to bond to thermoplastic composites, so there was potential for problems with the strain gauge bonding and adhesion of the paint for DIC. The following sections describe the testing set-up and procedure used, followed by the results and discussion in terms of the potential impact on the data presented in the thesis. The data are used to compare the strain measurement techniques. The effect of the loading force on the strain, therefore, is not discussed within this appendix (this is discussed within the thesis).
DIC Verification Testing Set-Up and Procedure

Tension and compression tests were conducted using the same set-up and procedure as described in the thesis. Specimen geometries and hole size (for both aluminium and composite specimens) were also kept the same. The acquisition rates of the two systems were different due to the memory requirements. The DIC system required two photograph images per data sample while the strain gauges generated significantly smaller numeric data files. The acquisition rates were approximately 1 Hz and 4 Hz for the DIC and strain gauge measurements respectively.

For the verification tests, 1 mm gauge length strain gauges were bonded to the surface of the specimens as shown in Figure A - 1 a) – d). Each strain gauge measured in a single direction and they were positioned to measure the far field strains (1 and 2) and strains close to the hole (3 and 4) in the 0° loading (longitudinal) direction and the 90° (transverse) direction. Strain gauges 1 and 3 (in Figure A - 1) were positioned to measure strain in the longitudinal direction and gauges 2 and 4 in the transverse direction.

The DIC system requires a speckle pattern to be painted on the surface in order to optically measure local displacements and convert them into local strain measurements. The pattern was painted over the strain gauges once they were bonded (as shown in Figure A - 1). The DIC system was set-up to measure strains at approximately 1 mm intervals across the surface of the specimens. The analysis software allowed the strain to be outputted at specific locations on the specimen surfaces. The images were used to select the closest point possible to the strain gauge locations for the comparison data.

The strain gauge accuracy was specified according to the manufacturer calibration as ± 1% of the maximum strain (5%). The strain accuracy was, therefore, ± 0.05% strain, which was the same as the accuracy quoted for the DIC system. The accuracy values are quoted for
controlled working conditions, but both systems are sensitive to environmental changes. Aluminium and composite specimens were tested to determine any difference in results that might arise from isotropic and composite materials. The composite specimens were the same material, lay-up and geometry as those used within the main investigation. The strain data was measured for each test until a significant plastic deformation in the aluminium specimens or a catastrophic failure occurred for the composite specimens.

**Figure A - 1** Photographs of strain gauged and speckle painted specimens used for DIC verification tests (specimen widths were nominally 36 mm) a) Aluminium specimen tested in tension; b) Aluminium specimen tested in compression; c) Composite specimen tested in tension; d) Composite specimen tested in compression.
DIC Verification Testing Results

The results of the verification testing are presented and discussed in this section. The figures show the DIC data measured during loading, including the recorded data points. The strain gauge data does not show the individual data points due to the higher data acquisition rate.

The longitudinal strains measured for the aluminium specimen in tension are shown in Figure A - 2 a) and c). The strain gauge and DIC longitudinal strains measured in the far field region, away from the hole, show good agreement throughout loading, Figure A - 2 a). The longitudinal strains measured near the hole, however, show an increasing difference in strain with increasing load, Figure A - 2 c).

The combined errors for the two techniques could give rise to a maximum difference of approximately 0.1% strain. The more likely explanation for the difference, however, is that the location used for the DIC output is slightly different in location to the centre of the strain gauge measurement area. In an area of high stress concentration, a very small difference in measurement locations would provide significant and increasing differences in measured strain.

The transverse strains measured for the aluminium specimens in tension are shown in Figure A - 2 b) and d). The data show that for low values of strain the precision of the DIC system reduces and the noise in the data appears to be within the manufacturer’s stated value of $\pm 0.05\%$ strain. The DIC transverse strain near the hole (Figure A - 2 d)), however, does appear to show a constant shift compared to the strain gauge results, which correlates with the previous suggestion of a slight difference in measurement location.
Figure A - 2 Strain measured using the DIC and strain gauge systems for the aluminium specimen under tension a) Far field longitudinal strain; b) Far field transverse strain; c) Near hole longitudinal strain; d) Near hole transverse strain.

The longitudinal strain data measured for the aluminium specimens in compression are shown for locations 1 and 3 in Figure A - 3 a) and c), respectively. The transverse strains for the same specimen at locations 2 and 4 are shown in Figure A - 3 b) and d), respectively. The data show, for most cases, that the DIC strain measurement is approximately within ± 0.05% of the strain gauge data. In some instances, for low values of strain (seen in the far field transverse strain measurement, Figure A - 3 b)), the difference in the results is greater than 0.05% and the trend of the DIC results does not agree with the strain gauge results. The strain gauge placed next to the hole, measuring longitudinal strain, is clearly showing erroneous results (Figure A - 3 c)). The comparison of the data, therefore, is not possible here.
Figure A-3 Strain measured using the DIC and strain gauge systems for the aluminium specimen under compression. a) Far field longitudinal strain; b) Far field transverse strain; c) Near hole longitudinal strain; d) Near hole transverse strain.

The longitudinal strain data for tensile loading of the composite specimen are shown for the far field region and near the hole in Figure A-4 a) and c), respectively. The transverse strain data for the far field and near hole regions are shown in Figure A-4 b) and d), respectively.
APPENDIX – A

Digital Image Correlation (DIC) System Verification

Figure A - 4 Strain measured using the DIC and strain gauge systems for the composite specimen under tension a) Far field longitudinal strain; b) Far field transverse strain; c) Near hole longitudinal strain; d) Near hole transverse strain.

The strain gauge results and the DIC results are in good agreement for the longitudinal strains. For the transverse strains, however, there is very poor agreement between the DIC and the strain gauge results for both the uniform field and near-hole transverse strains.

The final set of data was measured for open-hole compression testing of a composite specimen. The longitudinal strains measured for increasing load in the far field region and near the hole are shown in Figure A - 5 a) and c), respectively. The transverse strains for the far field and near hole region are shown in Figure A - 5 b) and d), respectively.
APPENDIX – A  Digital Image Correlation (DIC) System Verification

Figure A - 5 Strain measured using the DIC and strain gauge systems for the composite specimen under compression a) Far field longitudinal strain; b) Far field transverse strain; c) Near hole longitudinal strain; d) Near hole transverse strain.

The longitudinal strain in the far field region, measured using DIC, showed very good agreement with the strain gauge data. The longitudinal strain near the hole was slightly different for the DIC data and the strain gauge data. The difference in the data is likely to have been caused, again, by a small misalignment of where the DIC data was extracted from and where the strain gauge was measuring.

Once again, however, the transverse strain measurements show significant discrepancies between the DIC and strain gauge data. The DIC technique used here does not appear to provide reliable results at low strains (approximately < 0.1%). As many of the transverse strain measurements on the composite specimens are in this strain regime, it has been
concluded that only the longitudinal strain measurements will be used when discussing the DIC results.

**Concluding Remarks**

Verification testing was conducted on aluminium and composite specimens to provide confidence that the DIC system strain measurements agreed with data measured by a more commonly accepted technique, namely strain gauging. The data show that the longitudinal strain measured using the DIC technique agreed with the strain gauge data for the far field regions. The transverse strain measurements, however, were too small to be reliable. Consequently, no transverse strain measurements using the DIC equipment have been included within Chapter 6 where the DIC results on pierced specimens are presented.
Appendix B

Preliminary Finite Element Model and its Relationship to the DIC Results
Preliminary Finite Element Model and its Relationship to the DIC

Results

Introduction

A basic Finite Element (FE) model was produced to replicate the re-distribution of material during the piercing process and to determine whether the results measured using the DIC system could be numerically replicated. The piercing process was not simulated and the resultant distribution of material was generated based on an assumed fibre architecture to give a first approximation for future development.

The assumed fibre architecture and material properties were generated in the first instance using a Matlab script. The data was then used by the Numerical Modelling and Optimisation (NMO) section at TWI to compile and run the model within the Abaqus software. This section presents how the assumed fibre architecture and material properties were generated, before inputting into the model, and how the modelling results compare with those measured using DIC.

Model Set-Up and Parameters

The approach used to generate the assumed fibre distribution after piercing was the same as that described in Chapter 3.5.3 of the thesis to approximately predict the fibre strains. A beam bending equation was used to define the deformation of a built-in beam (or representative carbon fibre) for a maximum central displacement equal to the hole radius (3 mm). This provided the profile of a representative carbon fibre, which experienced the largest displacement during piercing (Figure B – 1).
The volume fraction of the local area next to the hole was assumed to be the maximum possible for a square packed array of fibres (0.785 – reduced from the early assumption of 0.9, in Chapter 3, based on the measured fibre volume fraction in Chapter 4). A square packing was used and only one-dimensional motion of the in-plane fibres (Figure B – 2). The fibre volume fraction was assumed to reduce linearly along the mid-plane up to the edge of the heated area (as indicated in Figure B – 1), where the fibre volume fraction is equal to that of the parent material (0.59). The separation of the representative fibres at the hole edge was, therefore, assumed to be zero and increased with decreasing volume fraction according to Equation 1, for a square array (Hull, 1981).
APPENDIX – B  Preliminary Finite Element Model and its Relationship to the DIC Results

\[ s = 2 \left( \frac{\pi}{4V_f^2} - 1 \right) r \]

Equation 1

Where \( s \) = the separation of fibres, \( V_f \) = fibre volume fraction and \( r \) = radius of fibres.

The fibre separation was used to determine the maximum displacement of the representative fibres (along the mid-plane) for the beam bending calculation. This fibre displacement reduced to zero at the edge of the heated area (to give a straight, non-deformed, fibre). The distance between the representative fibres was then used to calculate the local fibre volume fractions within the whole heated area by re-arranging Equation 1 for \( V_f \).

The local gradient of each representative fibre was calculated to provide the local fibre angles associated with the fibre volume fractions within the heated area. This data was calculated over a square of 1000 x 1000 elements (where each horizontal element was a representative fibre), Figure B – 3.

Figure B – 3: Contour plots showing the calculated fibre architecture over 1000x1000 elements in a 0° ply for a first approximation finite element model a) fibre angle distribution; b) fibre volume fraction distribution (where the pierced hole is located in the bottom left corner).
The assumed fibre architecture was used to set-up the stiffness properties of a pierced unidirectional ply in an Abaqus software computation. A sub-routine was used to assign the local stiffness properties based on the local fibre volume fraction according to Equation 2 and Equation 3 (Halpin-Tsai equations) and the local fibre orientation according to Equation 4 – Equation 7 (derived from the transformed reduced stiffness matrix) (Hull, 1981). The same ply lay-up ([0/90]_s / [90]) was created and TWI’s NMO section conducted a linear-elastic simulation based on the same specimen geometries used for tensile and compressive testing. Plastic deformation and fracture were not included in the analysis.

\[
\frac{M}{M_m} = \frac{(1 + \xi \eta V_f)}{(1 - \eta V_f)}
\]

\text{Equation 2}

\[
\eta = \frac{\left(\frac{M_f}{M_m}\right) - 1}{\left(\frac{M_f}{M_m}\right) + \xi}
\]

\text{Equation 3}

Where \( \xi = 0.2 \) (by empirical fit), \( M_f = \) modulus of the fibre \( (E_f, G_f, \nu_f) \), \( M_m = \) modulus of the matrix \( (E_m, G_m, \nu_m) \) and \( M = \) modulus of the composite.

\[
\frac{1}{E_x} = \frac{1}{E_1} \cos^4 \theta + \left(\frac{1}{G_{12}} - \frac{2\nu_{12}}{E_1}\right) \sin^2 \theta \cos^2 \theta + \frac{1}{E_2} \sin^4 \theta
\]

\text{Equation 4}

\[
\frac{1}{E_y} = \frac{1}{E_1} \sin^4 \theta + \left(\frac{1}{G_{12}} - \frac{2\nu_{12}}{E_1}\right) \sin^2 \theta \cos^2 \theta + \frac{1}{E_2} \cos^4 \theta
\]

\text{Equation 5}
APPENDIX – B Preliminary Finite Element Model and its Relationship to the DIC Results

\[
\frac{1}{G_{xy}} = 2\left(\frac{2}{E_1} + \frac{2}{E_2} + \frac{4v_{12}}{E_1} - \frac{1}{G_{12}}\right)\sin^2 \theta \cos^2 \theta + \frac{1}{G_{12}}(\sin^4 \theta + \cos^4 \theta)
\]

Equation 6

\[
v_{xy} = E_x \left[\frac{v_{12}}{E_1} (\sin^4 \theta + \cos^4 \theta) - \left(\frac{1}{E_1} + \frac{1}{E_2} - \frac{1}{G_{12}}\right)\sin^2 \theta \cos^2 \theta\right]
\]

Equation 7

Where \(E_x\) = longitudinal modulus, \(E_y\) = transverse modulus, \(G_{xy}\) = shear modulus and \(v_{xy}\) = Poisson’s ratio for the loading direction and \(E_1\) = longitudinal modulus, \(E_2\) = transverse modulus, \(G_{12}\) = shear modulus and \(v_{12}\) = Poisson’s ratio for the local fibre direction (\(\theta\)).

The material data for a drilled specimen were also provided for comparison with the DIC measurements. The fibre orientation and fibre volume content were assumed to be the same as the parent material and the manufacturer’s quoted mechanical properties were used. The local deformations of the specimens were outputted in terms of percentage strain at 25 kN tensile and compressive loads for direct comparison with the DIC data.

**Comparison of Finite Element Model Output with DIC results**

Mid-plane longitudinal strains measured using DIC and calculated using the FE model are shown for pierced specimens and drilled specimens, at an applied tensile load of 25 kN, in Figure B – 4 a) and b), respectively. The measured and calculated data appear to show significant differences for the pierced specimen, but good correlation for the drilled specimen. The calculated strains away from the pierced hole edge are slightly overestimated in the FE model and the increase in strain towards the hole edge cannot be directly compared due to the DIC masking in that region (mentioned previously in section 6.3 of the thesis).
Mid-plane longitudinal strains measured using DIC and calculated using the FE model are shown for pierced specimens and drilled specimens, at an applied compressive load of 25 kN, in Figure B – 5 a) and b), respectively. The measured and calculated data appear to show some areas of correlation for the pierced and drilled specimens.

The preliminary FE model data shows some slight agreement with the DIC measured data along the mid-plane for the linear-elastic analysis conducted. This shows that the assumed fibre architecture is giving a reasonable representation of the resultant fibre architecture after
piercing along the mid-plane. There are, however, considerable assumptions that have been made within the preliminary FE model.

The input fibre architecture is approximated based on a two-dimensional displacement of material within each ply. The microstructural analysis, in Chapter 4, showed that this is not the case. The displacement of fibres occurs in the x, y and z directions of the laminate and this would need to be incorporated into the model to improve further accuracy. This would give a better representation of the true structure in terms of the fibre deformations and the locations and magnitude of the resin rich regions.

The two-dimensional fibre paths were calculated using a beam bending equation, which does not exactly match the fibre profiles observed in the pierced specimens. The calculated local fibre angles, shown in Figure B–3 a), reach a maximum prior to the fibre reaching the hole. In reality, the local fibre angle in the 0° plies appeared to reach a maximum at a location slightly inside the top of the hole, Figure 6.7 (leading to the generation of the halo regions of larger strain).

The model also significantly differs from the real specimens in terms of the fibre integrity. The model assumes the fibres remain intact and without residual stresses or fractures as a result of the piercing process. This has been shown not to be the case when piercing the specimens (as shown in Chapter 4). Significant fibre fractures occur and there is a high likelihood of residual fibre stresses near the hole that could affect the resultant deformation and strength of the specimens under loading.

The influence of the local thickness increase of the laminate around the hole was also not considered in the analysis. The significance of the material formed into the tooling recess is something that should be studied in future investigations. This could impact the industrial
attractiveness of the TAP technique depending on whether removing the formed volume affects the mechanical performance.

Concluding Remarks

A first approximation FE model was used to calculate the strain under tension and compression loading. The data shows some agreement with the DIC data along the specimen mid-plane for equal loading forces. This model, however, incorporates many assumptions that need to be considered when developed in the future, but it acts as a good starting point for a further work package.

References

Appendix C

Published Papers
Investigation into the mechanical properties of thermoplastic composites containing holes machined by a thermally-assisted piercing (TAP) process

N.W.A. Brown, C.M. Worrall, S.L. Ogin & P.A. Smith

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Investigation into the mechanical properties of thermoplastic composites containing holes machined by a thermally-assisted piercing (TAP) process

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Abstract A thermally assisted piercing (TAP) process has been investigated as an alternative to current methods of machining holes in thermoplastic composites. The spike force/displacement responses during piercing were affected by both the processing temperature and the size of the heated area, as were the resultant microstructure and subsequent mechanical performance. Overall, the results suggest that for advanced manufacturing of thermoplastic composites, good tensile and compressive open-hole properties are produced in the TAP process when using small heated areas and higher temperatures.

Keywords Machining, Mechanical properties, Open-hole, Thermally assisted piercing, Thermoplastic


Introduction

Significant advantages can be gained by using fiber-reinforced polymers (FRPs), but one of the key barriers preventing the uptake of FRPs in some industry sectors is the complexity and cost of manufacturing and processing, including such processes as machining holes. Conventional drilling is most favored in industry as a relatively inexpensive method of machining holes (when compared to laser or water-jet machining) and can be used on large, non-flat structures without significant complications. If drilling parameters are not carefully selected and controlled, however, then delaminations can result from the drilling process, potentially reducing mechanical performance.1-3

As an alternative to material removal processes such as drilling, material displacement processes (e.g. molding-in holes) can be used, where significant increases in tensile strength have been found and are a consequence of fiber continuity and local increases in fiber volume fraction close to the hole.4,6 Compression properties of molded hole specimens tend to show increased ultimate compression strengths (when compared with drilled hole specimens), but not to the same extent as tensile strength increases.5,6 It is well known that fiber misalignment plays a role in compression strength and it may be that the compressive strength of notched specimens could, therefore, be sensitive to local fiber waviness and misalignments resulting from material displacement processes. This will reduce the strengthening effects associated with fiber continuity and increased fiber volume fraction. Bearing strengths can also be improved for molded-in hole specimens.5,10 The disadvantage of this approach is that molded-in holes are labor-intensive and often not commercially viable for large-scale production, despite the improvement in performance offered.

With the recent revival of interest in thermoplastic composites, the issue of rapid production of holes may be solved using a thermally assisted piercing (TAP) process. This, like molded-in holes, is a material displacement process that can be used to form holes in thermoplastic composites at various stages of manufacture or subsequent processing.11 A number of studies have shown that an improvement in bearing strength of 5% can be achieved for pierced specimens when compared with drilled specimens.10-14 The aim of the present study is to investigate how the open-hole performance of the pierced specimens varies when changing two of the key process parameters – size of heated area and piercing temperature.

Experimental methods

Specimen manufacture

Twenty-one ply laminates of carbon fiber-reinforced polyetheretherketone (PEEK) (Tenax TPUD PEEK-HTS40, with a
nominal fiber volume fraction of 0.59) pre-impregnated unidirectional tape has been used with the configuration [(0/90)5 / 0]s. The laminates were pressed at 400 °C under a consolidation pressure of 0.91 MPa, based on material supplier recommendations. The pressure was applied for 1 minute per ply (21 min for the laminate) before deactivating the heat input and allowing the laminate to cool under the consolidation pressure to room temperature, at an approximate cooling rate of 1.7 °C/min. This produced a laminate thickness of approximately 2.9 mm (measured after manufacture).

Specimens to be tested with drilled holes were cut to size before drilling (the specimen dimensions are shown in Table 1). Holes were first drilled to 5.7 mm diameter before being reamed to a final diameter of 6.0 mm to ensure minimal damage to the specimens. The feed rate and drill speed were set within the manufacturer’s guidelines (0.2–0.4 mm/revolution and 45–90 m/min, respectively). A solid carbide drill and reamer were used to machine the holes to ensure minimal wear on the tooling. For the TAP specimens, the laminates were pierced before machining the specimens to the final dimensions to ensure that the heating area did not fall outside of the specimen width. The TAP was achieved using a piercing rig manufactured at TWI with flexibility to modify process variables prior to piercing (see Figure 1). The equipment used pneumatic actuators to clamp the laminate throughout the process and, once the laminate was sufficiently heated, to drive a 6-mm-diameter conical piercing spike through the laminate. The spike was driven pneumatically under constant pressure and was instrumented with a load cell and displacement sensor to record the force/displacement response during piercing.

The piercing process for all variable settings relies on four key stages: (a) initial rig heat-up; (b) clamped laminate heating; (c) piercing, and (d) cool down (see Figure 2). The setup follows a similar configuration to that used in preliminary testing, presented previously by the authors, but now with a greater degree of control. The heated area is varied by attaching tooling surfaces that conduct the heat from the heater units. The tooling surfaces vary in diameter according to the size of the heated area required and incorporate a recess (approximately 500-μm deep and with a diameter of 12 mm) into which the material from the hole is displaced. An insulating surface was integrated into the design to partially mask the heater units for tests that required heated areas smaller than the size of the heater units. A final insulating ring was placed around the heater units that provided a constant overall clamping area for all tests. During the TAP process the total volume of material associated with the production of a 6-mm-diameter hole in a
2.9-mm-thick plate is approximately 82 mm$^3$, which is the volume of material displaced during piercing. The recess volume in the tooling was approximately 42 mm$^3$ (approximately one-half of the hole volume). The remaining material, not formed into the recess, is extruded through the tooling during piercing (Figure 3). Some of this material is sheared off due to the interaction between the spike and the tooling hole and the remainder is removed by polishing prior to testing. Where the tooling holes are present, allowing the spike to penetrate the specimen, the laminate is likely to expand and may even delaminate. This was not possible to observe, however, due to the clamping jigs surrounding this region. It is assumed that the clamping jigs prevent any through thickness expansion in the surrounding material during heating and piercing.

Baseline conditions used for piercing were similar to the original laminate manufacturing conditions. The variations of TAP process parameters used are shown in Table 2. The diameter of the heated area was increased from a minimum value $D$ to 1.5$D$ and finally 2$D$. A starting temperature for piercing $T$ was increased to 1.05$T$ and finally 1.20$T$. The spike geometry used for testing was a conical-tipped cylinder with an internal spike angle of 30°, as used in the initial study. The clamping pressure used $P$ was similar to the manufacturing guidelines for the parent laminate.

### Table 2 Range of TAP variables investigated

<table>
<thead>
<tr>
<th>Specimen designation</th>
<th>Diameter of heated area</th>
<th>Piercing temperature</th>
<th>Clamping pressure</th>
<th>Quantity (each test)</th>
</tr>
</thead>
<tbody>
<tr>
<td>A1</td>
<td>$2D$</td>
<td>1.05$T$</td>
<td>$P$</td>
<td>5</td>
</tr>
<tr>
<td>A2</td>
<td>$1.5D$</td>
<td>1.05$T$</td>
<td>$P$</td>
<td>5</td>
</tr>
<tr>
<td>A3</td>
<td>$D$</td>
<td>1.05$T$</td>
<td>$P$</td>
<td>5</td>
</tr>
<tr>
<td>T1</td>
<td>$2D$</td>
<td>1.20$T$</td>
<td>$P$</td>
<td>5</td>
</tr>
<tr>
<td>T2</td>
<td>$T$</td>
<td></td>
<td>$P$</td>
<td>5</td>
</tr>
<tr>
<td>D1</td>
<td>Drilled</td>
<td></td>
<td></td>
<td>5</td>
</tr>
</tbody>
</table>

Test methods

Testing was conducted to confirm the size of the heated zone during the piercing process using a laminate manufactured using the same material and the same lay-up as the final test specimens. Thermocouples were embedded at various distances from the center of the heated area and in the center of the laminate thickness (between the 11th and 12th plies). An additional thermocouple was placed at approximately one-quarter of the thickness of the laminate (i.e. between the 16th and 17th plies), again at the center of the heated area.

Open-hole tension and compression testing followed ASTM 5766/D5766M/11 and BS ISO 12817:2013, respectively. Nominal specimen dimensions are shown in Table 1. Specimens were centrally drilled or pierced to produce 6-mm holes and a constant displacement rate of 1 mm/min was applied until ultimate failure. Five specimens were tested for each test condition.

Tested specimens were analyzed using a combination of reflected light microscopy of specimen cross sections and digital image correlation (DIC) measurements during the tests, which have been used in previous work on the effect of holes. The GOM–ARAMIS three-dimensional DIC system used consisted of two cameras (controlled by the ARAMIS...
Results and discussion

Investigation of the temperature distribution prior to piercing

The embedded thermocouples enabled the radial and through-thickness temperature distributions to be ascertained. Figure 4(a) shows the measured radial temperature distributions for the target piercing temperature of 1.05\(T\), for the different heated diameters of \(\text{D}, 1.5\text{D}, \text{and } 2\text{D}\). In each case, the temperatures are approximately equal to 1.05\(T\) at the center of the heating area and are initially constant until they decrease to the melting temperature \(T_m\) at the edge of the required heated area (corresponding to the different diameters). The time to reach these temperatures was 40 s in all cases, and this was repeated for all tests using these processing conditions. Figure 4(b) shows the temperature profiles for the target piercing temperatures of \(T, 1.05T,\) and \(1.20T\). The temperature profiles are approximately constant before decreasing to \(T_m\) at the edge of the heated area. All processing temperatures remained below the degradation temperature of the polymer so that no degradation is expected for any of the processing times.

Piercing spike force/displacement response for varying diameter of heated area and varying process temperatures

Figure 5 shows three examples of the spike force/displacement response for the three heated area diameters \(D, 1.5D, \) and \(2D\) with a processing temperature of 1.05\(T\); Figure 5(d) shows a comparison of typical results for each diameter.

Software) and was set to calculate the strain data at approximately 1-mm intervals across the surface of the specimens. The DIC analysis was conducted using facet settings of 19 \(\times\) 19 pixels with 2 pixel overlap. These settings are within the mid-values of the system and allow for good accuracy and fast computation.\(^{22}\) The DIC analysis was conducted during mechanical testing of the pierced specimens of A3 type (see Table 2), and conventionally drilled specimens, under both tension and compression loading.
The shoulder of the spike exits from the bottom of the laminate. The force on the spike subsequently reduces and approaches a steady value due to frictional sliding of the spike shaft on the hole wall (steps in the data are likely to be caused by a ‘stick-slip’ effect). The knee in the force/displacement curve is likely to be caused by the final compaction of material into the tooling surface recess and subsequent extrusion of material through the bottom of the tooling. Increasing the diameter of the heated area increases the total heated volume into which fibers can be displaced and compacted and reduces the local fiber packing; this is shown schematically in Figure 6. With the fibers less tightly packed (i.e. for a larger heated area), the resistance to further compaction and extrusion will decrease, thus reducing the knee in the force/displacement curve.

The force/displacement responses of the spike when piercing show similar features for the three diameters and the key piercing stages have been identified previously by considering the projected contact area of a conical spike as it pierces a laminate. The same start point for the origin of the spike position is used as for the previous work (i.e. when the spike makes contact with the laminate). The resistive force initially increases and reaches a maximum at approximately 11-mm spike displacement (equal to the conical spike tip length) for all diameters of heated area. There is a subsequent reduction in the force before a ‘knee’ is observed and the force either briefly increases or remains constant. The knee feature was not captured in previous work, but is captured here due to the improved test rig and data logging rate (1 kHz). The knee is most prominent for the smallest heated area diameter $D$ and reduces with increasing heated area. The knee occurs, for the three diameters, at a displacement of approximately 14–15 mm, which corresponds to the displacement at which the shoulder of the spike exits from the bottom of the laminate. The force on the spike subsequently reduces and approaches a steady value due to frictional sliding of the spike shaft on the hole wall (steps in the data are likely to be caused by a ‘stick-slip’ effect). The knee in the force/displacement curve is likely to be caused by the final compaction of material into the tooling surface recess and subsequent extrusion of material through the bottom of the tooling. Increasing the diameter of the heated area increases the total heated volume into which fibers can be displaced and compacted and reduces the local fiber packing; this is shown schematically in Figure 6. With the fibers less tightly packed (i.e. for a larger heated area), the resistance to further compaction and extrusion will decrease, thus reducing the knee in the force/displacement curve.

Three examples of the force/displacement data measured for varying process temperatures $T$, 1.05$T$, and 1.20$T$, for a heated diameter of 2$D$, are shown in Figure 7(a)–(c); Figure 6(d) shows typical examples for comparison. The data show the same piercing stages as those observed for varying diameter of heated area. As the processing temperature increases, the maximum resistive force on the spike reduces and the knee feature reduces in magnitude. Similar ‘stick-slip’ features to those seen for change in heated area are also shown for the final frictional sliding stage of piercing. The reduction in the maximum spike force with increasing temperature is likely to be related to the reduction in viscosity of the matrix, which allowed the fibers to be displaced more easily, and the increase in mobility of the matrix. The knee feature also reduced with increasing temperature as the matrix provided less resistance to the extrusion process at the end of piercing.
TAP microstructure

The changes in fiber architecture as a consequence of drilling and TAP are shown schematically in Figure 8(a) and (b); the planes marked 0° and 90° in Figure 8(a) are parallel and perpendicular to the loading direction of the specimens, respectively. Reflected light photomicrographs of drilled specimens, for 0° and 90° planes (Figure 9(a) and (b), respectively), show, as expected, that the fiber orientations remain unaltered up to the edge of the drilled hole, which is on the right-hand side of the images. Figure 9(c) and (d) show the architecture observed for a TAP specimen sectioned along the 0° and 90° planes (again, the edge of the pierced hole is on the right of the images). Significant 0° and 90° ply distortions are evident and the plies have clearly been displaced through the thickness of the laminate, in the piercing direction. In the 0° plane cross section, the 0° plies were found to reduce significantly in thickness toward the hole edge. This was coupled with a local thickening of the 90° plies to fill the volume. Similarly, a 90° plane cross section shows the 90° plies thinning at the hole edge – this time with a local thickening of the 0° plies (Figure 9(d)).

Two mechanisms, shown schematically in Figure 10, are causing the observed microstructure. Firstly, the 0° fibers are displaced from their original position at the center of the hole (location 1 in Figure 10). These fibers are bunched and compressed against the edge of the spike and the fiber volume fraction increases as the spike displaces more material (location 2 in Figure 10). The increased volume of fibers building up at the edge of the spike at location 2 is complemented by a reduction in material at location 3. Secondly, at location 3, the 0° ply fibers are displaced in both x- and z-directions. For the current case, a cross-ply laminate, the reduction of material in one location in the 0° plies is complemented by the buildup of material in the 90° plies in the same x–y location. This results in the architecture observed, thinning and thickening of alternate plies near the hole edge.

In addition to local effects throughout the laminate, the overall thickness of the region close to the hole increases to accommodate the displaced material (as can be seen in Figure 9). A recess in the tooling jig allowed for the additional material to form a local thickness increase around the hole. Both fibers and matrix are displaced into the recess and the resultant microstructure is similar to that found in the adjacent material (as shown in Figure 9(c) and (d)). The fibers within this region are likely to be discontinuous as a result of shearing of the material extruded into the tooling hole. In commercial applications, the dimensions of this recess could be altered as required e.g. countersunk or hemispherical in geometry. In later sections, the calculated strengths for open-hole tests have used the nominal dimensions of the specimens, neglecting this local thickness increase.
The resultant fiber architecture of the pierced laminates, exposed through 0° cross sections, is shown to change when varying the heated area (Figure 11(a)–(c)) and the processing temperature (Figure 12(a)–(c)). Displacement of fibers from some regions and compaction into other regions produced resin-rich areas, high fiber volume fraction areas and, in some cases, voids where neither fibers nor matrix remained after piercing. Increasing the heated area appears to increase the formation of resin-rich regions and voids within the laminate structure (Figure 11(a)–(c)), which can be associated with a decrease in fiber volume fraction near the hole. The resin rich regions and voids were caused by the displacement of fibers from these regions, not by an increase in laminate thickness. This agrees with the instrumented spike data, which suggested a reduction in fiber packing (showed by a decrease in the resistive force at the knee when piercing).

Increasing the ease of movement, or mobility, of the fibers at high processing temperatures leads to larger fiber displacement and reduced fiber fracture, whereas restricted fiber movement at low processing temperatures increased the local strain and led to more fiber fractures. Figure 13 shows an area close to the hole edge for a processing temperature of 400 °C where fractured fibers were revealed by burning away the matrix. A simplified calculation of the fiber elongation due to in-plane displacement was carried out by approximating the shape of the fiber to that of a beam in bending. The total fiber strains due to displacement are shown in Table 3 for the three diameters of heated area and confirm that fiber fractures are most likely for the smallest heated area for which the calculated fiber strain is highest. The actual displacement of fibers, which will be both in-plane and out-of-plane, will lead to larger fiber strains than those calculated. The following sections present data on the mechanical behavior of the drilled and pierced specimens.

Open-hole tension and compression results

Figure 14 shows typical force/displacement responses for the drilled and pierced specimens under tensile loading. The responses suggest that the overall stiffness of the drilled and pierced specimens were very similar before final catastrophic failure occurred.

The open-hole tensile strength improvements for the TAP process compared to drilled specimens are shown in Figure 15 for the heated diameters D, 1.5D, and 2D and one process temperature 1.05T, and for three process temperatures T, 1.05T, and 1.20T and one heated diameter 2D. The open-hole tensile strength increased by between 1 and 10% for the pierced specimens when compared with the drilled specimens. With regard to temperature, the lowest processing temperature T shows a pierced specimen strength with a 6% improvement over the drilled strength, reducing to a strength improvement of 1% at 1.05T, and increasing to a 10% improvement at 1.20T. The reduction in strength will be caused by the reduction in fiber volume fraction around the hole with increasing void content and the development of resin-rich regions. A change in dominant effects is then suggested to occur with increasing temperature where the reduction in fiber fractures, due to increased fiber mobility, offsets any reduction in strength due to a reduction in the fiber volume fraction. This results in the increase in open-hole tensile strength shown for the highest processing temperature.

Figure 16 shows a similar force/displacement response for the compression tests where the stiffness of the drilled and TAP specimens remained approximately the same under compression testing up to catastrophic failure. Figure 17 shows the open-hole compressive strength of the pierced specimens compared to the drilled specimens; there is a 12% improvement for a heated area diameter of D (compared to the drilled specimens), falling to an improvement of 4% for 1.5D. However, for a heated area of 2D, the compression strength of the pierced specimens reduced to 79% of the drilled hole strength. The effect of varying the processing temperature on the compressive strength of the pierced specimens is also shown in Figure 17 for a diameter of 2D; the strength of the TAP specimens increased from 73 to 81% of the drilled specimens as the temperature increased from T to 1.20T.
respectively. These figures show the longitudinal strain (i.e. the $y$-direction strain) measured using DIC for the drilled and pierced specimens, respectively, at 50% of their failure loads.

The general effect of fiber removal (drilling) or fiber displacement (TAP process) on the strains local to the hole is shown in Figure 18(a) and (b) for tension and compression, respectively. These figures show the longitudinal strain (i.e. the $y$-direction strain) measured using DIC for the drilled and pierced specimens, respectively, at 50% of their failure loads.
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Figure 13 Fractured fibers close to the hole edge as a result of piercing (exposed by burning off the PEEK resin). Source: Copyright TWI Ltd

Figure 14 Typical applied force against cross-head displacement response for drilled and pierced specimens under tensile loading

Figure 15 Open-hole tensile strengths of pierced specimens as a percentage of drilled specimen strength showing effects of the TAP process variables, heated area, and temperature (with standard errors)

Table 3 Approximate maximum fiber strain calculated for various diameters of heated area

<table>
<thead>
<tr>
<th>Diameter of heated area (mm)</th>
<th>Approximated fiber strain (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>60</td>
<td>0.6</td>
</tr>
<tr>
<td>45</td>
<td>1.1</td>
</tr>
<tr>
<td>30</td>
<td>2.4</td>
</tr>
</tbody>
</table>

Figure 16 Typical applied force against cross-head displacement response for drilled and pierced specimens under compression loading

Figure 17 Open-hole compressive strengths of pierced specimens as a percentage of drilled specimen strength showing effects of the TAP process variables, heated area, and temperature (with standard errors)

Figure 18 DIC measurements of strain as a function of horizontal distance from the hole center for drilled and pierced specimens at 50% of their respective maximum loads to failure. Data measured for drilled and pierced specimens at 50% of their respective maximum loads under tension. a Tension and b compression
along a horizontal line from the center of the hole to the edge of the specimen. For both tension and compression loading, the drilled specimens show an increased longitudinal strain near the hole edge, reflecting the effect of the strain concentration at the hole, as found in previous studies.\textsuperscript{20,21} For the TAP specimen, fiber continuity around the hole (shown schematically in Figure 10) reduces the stress concentration. However, additional microstructural effects also occur during the TAP process which lead to complex dependencies on heated area and TAP temperature.

Increasing the diameter of the heated area and processing temperature is both likely to lead to the increased development of voids and resin-rich regions, with a reduction in the local fiber volume fraction adjacent to the hole, but also a reduction in the number of fiber fractures as a consequence of the TAP process. In addition, increased resin and fiber mobility can lead to fiber misalignment and fiber waviness. A summary of the expected changes in the microstructural features is shown schematically in Table 4 for increasing diameter of heated area and increasing processing temperature; the anticipated effect of these changes on the open-hole tension and compression strengths is also indicated. Overall, the experimental results suggest that for manufacturing purposes, if both good tensile and compressive open-hole properties are to be retained, the best TAP combination is the smallest diameter and the highest temperature.

### Conclusions

A TAP technique was used to form holes in thermoplastic composites to establish the effect of changing process conditions on the open-hole performance. The results show a complex interaction of the effect of heated area and processing temperature on open-hole tension and compression results. Overall, the results show that for advanced manufacturing of thermoplastic composites, good tensile and compressive open-hole properties are produced when using small heated areas and higher temperatures.

### Acknowledgements

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### References

BEHAVIOUR OF THERMOPLASTIC COMPOSITES WITH HOLES MACHINED USING A THERMALY-ASSISTED PIERCING PROCESS

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Keywords: Composites, Holes, Machining, Piercing, Thermoplastic

ABSTRACT
Thermally-Assisted Piercing (TAP) is an alternative method, to conventional machining techniques, of producing holes in Thermoplastic Composites (TPCs). The technique is designed to be sympathetic to the continuous fibres within composite structures. This can help retain more of the original structural efficiency that make composite materials attractive to so many industries. The aim of the current work is to investigate how key process variables affect the mechanical performance of pierced specimens. Open-hole tension and compression tests were conducted and show that improvements of 5\% and 12\%, respectively, can be achieved for the smallest heated areas used for piercing. Increasing the heated area reduced the performance of the tension and compression tested specimens. The compression specimens showed a greater reduction in strength for the increase in heated area. This observation supports the suggestion that fibre waviness dominates the performance of the specimens when varying the diameter of the heated area used for piercing.

1 INTRODUCTION
Fibre Reinforced Plastics (FRPs) have become increasingly popular within many industries attempting to improve structural efficiencies and achieve weight savings. The use of FRPs, with high strength and stiffness to weight ratios, allows lighter structures to be manufactured without trading off mechanical performance. Despite the mechanical advantages that can be gained by using FRPs instead of conventional materials, there are still many barriers that prevent this step change being implemented by some industry sectors. One of these barriers is the cost and complexity of manufacturing and processing FRPs.

Thermosetting Composites (TSCs) have been more commonly used for structures due to their increased mechanical properties over Thermoplastic Composites (TPCs). This structural efficiency advantage has, previously, been the primary factor for TSC popularity over TPCs. TPCs can be heated and processed/formed using similar techniques to those applied when working sheet metal. This has become an attractive prospect for high volume manufacturing industries e.g. automotive and single aisle aircraft production.

The application of TPCs has recently increased due to the ability to increase production efficiencies and reduce manufacturing costs. The ability to melt the thermoplastic matrix also makes TPCs more attractive for welded joints and potential re-cycling of structures – a growing factor for many industries. Although TPCs can be welded it is almost inevitable that, as with TSCs, they will require bonding or mechanical fastening to other components.

Mechanical fastening, adhesive bonding and welding are all potential methods of joining TPCs to construct larger assemblies. Mechanical fastenings are usually adopted for applications that require disassembly or progressive and reliable failure. The majority of fastening systems are accepted by a
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recess or hole within the structure to make an interlock. All currently used machining methods for fastener holes (e.g. drilling, water-jet cutting and laser machining) rely on material removal techniques that cut and remove load bearing fibres.

As an alternative to machining, material displacement processes can be applied to composites to create holes for fastening without removing the load carrying fibres from the structure. Moulding-in holes is a method used to displace fibres around an insert and into the surrounding volume to retain the load carrying fibre paths. The retention of fibre continuity around the hole permits higher open-hole and bearing strength through a reduction in stress concentrations around the hole when compared to conventional drilling [1] [2].

Thermally-Assisted Piercing (TAP) is another technique that can be used to create holes in TPCs [3]. This material displacement technique transports fibres away from the desired hole location, rather than cutting and removing them completely. The re-processing ability of TPCs allows this technique to be applied at various stages within the manufacture of components to retain fibre continuity [4]. The aim of the current investigation is to build on previous work and identify how key process variables within TAP affect the resultant mechanical properties of pierced specimens.

This paper briefly describes the TAP technique used to create specimens for mechanical testing. Open-hole tension and compression tests are described and the results of testing pierced and drilled specimens are presented. Results are compared and discussed for various diameters of the heated area when piercing, using the drilled specimens as the baseline, with future work outlined.

2 THERMALLY-ASSISTED PIERCING (TAP)

Previous work included a feasibility study of the TAP process using a preliminary prototype piercing rig [3]. For the current study a new TAP rig was manufactured with flexibility to vary the process variables (Figure 1). The heated area, temperature when piercing, clamping pressure and spike geometry can be varied before piercing. The equipment uses pneumatic actuators to clamp the laminate throughout the process and to drive the piercing spike once the laminate is heated sufficiently.

The piercing process for all combinations of variables relied on four key stages: initial rig heat-up, clamped laminate heating, piercing and cool-down (Figure 2 a-d). The heated area is varied by attaching tooling surfaces that conduct the heat from the heater units. The previous piercing rig used an induction heating method for rapid, non-contact, heating. This was changed to a conduction method for the current rig in order to accurately control the heated area when piercing. Future industrial application may find it advantageous to revert to an induction heating method for energy efficiency and process speed advantages.

The tooling surfaces vary in diameter according to the size of heated area required. An insulating surface was integrated to mask the heater units for tests that required heated areas smaller than the size of the heater units. A final insulating ring was placed around the heater units and provided a constant overall clamping area for the tests.
The composite specimens used for testing were manufactured from carbon fibre reinforced Polyetheretherketone (CF/PEEK) (Tenax® TPUD PEEK-HTS40) pre-impregnated unidirectional tape. A 21 ply lay-up provided nominally 2.9 mm thick, balanced and symmetric, laminates ([0/90]_s), which were pressed at 400°C under a consolidation pressure of 0.91 MPa. The pressure was applied for one minute per ply (21 minutes for the laminate) before deactivating the heat input and allowing the laminate to cool under the consolidation pressure to room temperature (with an approximate cooling rate of 1.7 °C/min).

The temperature and clamping pressure used when piercing were similar to the original laminate manufacturing conditions. The spike geometry remained the same throughout the current experiments, 30° inclusive angle (Figure 3) with a 6 mm diameter circular shaft (producing a 6 mm diameter hole – Figure 4).
3 OPEN-HOLE TESTING

3.1 Introduction

Open-hole tension and compression tests were conducted to determine the mechanical performance of pierced specimens with varying size of heated area. Testing was performed on a 250 kN, screw-driven Instron using hydraulic grips and a constant displacement rate of 1 mm/min to ultimate failure of the specimens. Specimens were nominally 36 mm wide with 6 mm holes, and either 200 mm or 125 mm long for tension or compression tests, respectively.

The current study investigated the effect of varying the heated area on the open-hole performance of pierced specimens. The diameter of the heated area was increased from a baseline value (D) by 50% (to 1.5 D) and then by another 50% of the original diameter (to 2 D). Testing was conducted on 5 specimens for each size of heated area.

3.2 Open-hole tension results

Figure 5 shows the ultimate strength of the pierced specimens plotted as percentages of the drilled specimen strength. The results show that for the baseline heated area diameter (D) the ultimate strength of the specimens was approximately 105% of the drilled specimen strength. Increasing the diameter of the heated area by 50% (to 1.5 D) showed no difference in the ultimate strength of the specimens. Increasing the heated area diameter to 2 D reduced the strength of the pierced specimens slightly to 101% of the drilled hole strength.

A possible explanation of these results is the following. Increasing the heated area diameter increased the length of fibre that was free to move during piercing. This is likely to cause increased fibre waviness which would have a tendency to reduce the mechanical performance when compared with
the smaller heated area. During the piercing process fibres are displaced and compacted into the heated volume to create a hole (increasing fibre volume fraction). Reducing the heated area is, therefore, likely to increase the packing of fibres since the same volume of fibres are displaced into a smaller heated volume.

![Figure 5: Open-hole tensile strength of pierced specimen as a percentage of drilled specimens (varying diameter of heated area).]

3.3 Open-hole compression results

The open-hole compression strengths of pierced specimens, as percentages of drilled specimens, are shown for varying diameter of heated area in Figure 6. The results show that for the baseline diameter of the heated area (D) the pierced specimen strength was 112% of the drilled specimen strength. Increasing the heated area to 1.5 D decreased the pierced specimen strength to 104% of the drilled specimen strength and a further increase in heated area to 2 D produced a decrease in the strength of pierced specimens to 79% of the drilled hole strength.

![Figure 6: Open-hole compressive strength of pierced specimens as a percentage of drilled specimens (varying diameter of heated area).]
The open-hole compression tests show a significant trend: increasing the heated area reduced the ultimate strength of the specimens. This agrees with the results of the open-hole tension tests that show a modest reduction in strength with increasing heated area size. Compression tests are more sensitive to fibre waviness due to the onset of fibre buckling. It is, therefore, likely that an increase in fibre waviness for the larger heated areas has led to larger reductions in strength under compression. The effect of reduced fibre packing (and hence volume fraction) for larger heated volumes during piercing is also likely to have contributed to the observed reduction in strength for increasing diameter of heated area.

4 CONCLUDING REMARKS

The size of the heated area when piercing can have a significant effect on the ultimate strength of pierced specimens under open-hole loading. The ultimate tensile strength of pierced specimens was 105% of the drilled hole strength for a baseline heated area and increasing the heated area led to a small reduction in strength. For compression testing, the maximum compressive strength of pierced specimens was initially 112% of the drilled specimen strength for the smallest heated area, but doubling the heated area led to a 43% strength reduction from this value.

The results in this study show that the open-hole strength of specimens can be improved by reducing the size of the heated area. It is suggested that reducing the size of the heated area both constrains the fibre waviness to a smaller volume of material and increases the fibre packing (and hence fibre volume fraction); consequently, the open-hole strength is higher for smaller heated areas. Future work aims to quantify the waviness and local volume fractions within pierced specimens and investigate the sensitivity of strength to other process variables.

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REFERENCES


INVESTIGATION OF PROCESS-RELATED DAMAGE DURING THERMAL PIERCING OF A THERMOPLASTIC COMPOSITE

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1 Introduction

Over the last decade, Fibre Reinforced Plastics (FRPs) have seen further significant uptake into structural applications within the aerospace, automotive and energy sectors. The primary drivers for this increase in FRP use are the specific properties (i.e. strength and stiffness to weight ratios) that are achieved when compared to conventional materials. Despite the mechanical advantages that can be gained by using FRPs instead of conventional materials, there are still many barriers that prevent this step change being implemented by some industry sectors. One of these barriers is the cost and complexity of manufacturing and processing FRPs.

Advances in polymer technology have led to extensive development of high performance Thermoplastic Composites (TPCs) which not only exhibit mechanical properties comparable to Thermosetting Composites (TSCs), but can be formed and pressed under heating using similar approaches to sheet metal processing techniques. These rapid production and processing techniques are enabling TPCs to enter high production rate industry sectors (e.g. automotive) that were previously not favoured for TSCs. It is almost inevitable that TPC components will need to be joined, either to each other or to other materials. As with thermosetting composites, the introduction of holes or cut-outs for mechanical fastening leads to associated challenges.

Machining holes for mechanical fastening of composites is a significant issue within the composites industry and is still fraught with problems. Current machining processes include, among others, conventional drilling, laser machining and abrasive water-jet cutting. These are all material removal processes that rely on cutting the fibres and matrix to create a hole. Cutting fibres in this way removes the load transfer paths in this region, to the detriment of structural performance.

Conventional drilling is currently the most commonly employed material removal technique. It is a relatively inexpensive method of machining holes (when compared to laser or water-jet machining) that can be used on large, non flat, structures without significant complications. Despite this, the damage inflicted on a composite structure when drilling holes gives rise to stress concentrations that reduce the mechanical performance. If drilling parameters are not carefully selected and controlled then delaminations, at the top and bottom surfaces of the laminate, and degradation of the hole wall can result from the drilling process [1-3]. These problems provide a need for research into new methods and processes that can lead to cost savings via weight reductions, processing time reductions or fewer part rejections.

As an alternative to machining, material displacement processes can be applied to composites to create holes for fastening without removing the load carrying fibres from the structure. Moulding-in holes is a method used to displace fibres around an insert and into the surrounding volume to retain the load carrying fibre paths. The retention of fibre continuity around the hole permits higher notched and bearing strength through a reduction in stress concentrations around the hole when compared to conventional drilling [4] [5].

Thermal piercing is another material displacement process that can be used to displace fibres around a hole in TPCs at various stages of manufacture or subsequent processing [6]. In this process, multiple
interactions occur between the piercing pin and the composite laminate which will damage fibres, affect (local) fibre architecture and volume fraction, and influence hole surface quality. In this regard, the aim of the present study is to determine the feasibility of a thermal piercing technique to replace conventional machining processes for producing holes in TPCs. Specifically, an investigation has been undertaken into the variables associated with a thermal piercing process. The process is used to produce holes in post manufacture TPC parts for mechanical fastening.

The structure of the paper is as follows. First the results are reported from feasibility trials in which the notched strength of thermally pierced specimens is compared with conventionally drilled specimens. Following this preliminary study, an instrumented piercing rig was designed, which enables the force-displacement response to be monitored during the penetration process. Initial results are presented which enable the load-displacement response to be compared for different spike angles.

2 Feasibility Trials
Feasibility trials were undertaken to assess the ability of the thermal piercing technique to produce holes in a TPC. The three stage technique consisted of a heating stage, with the laminate under a restraining pressure (Fig. 1a.), a piercing stage (Fig. 1b.) and a cooling stage, under continued restraining pressure (Fig. 1c).

The experimental set-up comprised a compression system to restrain through thickness expansion of the laminate when it was locally heated, a heat input in the form of an induction coil and a pneumatically driven piercing spike. Once the laminate was at the required temperature (~350°C) the locally molten area of the laminate was pierced.

A cross ply, continuous carbon fibre reinforced Polyetheretherketone (PEEK) composite was used for the experiments. With a melting temperature of 343°C the laminates were locally heated to above their melting temperature (~350°C) before piercing. The laminates were 2.1 mm thick and pierced using a 6 mm diameter, untreated, stainless steel spike with a 30° spike angle (Fig. 2).

The notched tensile strength of the thermally pierced laminates was compared with conventionally drilled specimens (Fig. 3). The tests were conducted according to ASTM 5766/D5766M. The resultant notched strength of the specimens was approximately 8% stronger than the conventionally drilled specimen strengths.

The thermally pierced specimens were sectioned and polished across the diameter of the hole (Fig. 4) to reveal the hole internal surface and the resultant laminate structure after piercing. Using Scanning Electron Microscopy (SEM) (Zeiss 1455EP) there was significant damage seen as a consequence of the piercing process (Fig. 5, 6). Large regions of fibre fracture were seen around the hole surface due to the interaction with the piercing spike as it travelled through the laminate. There was also considerable ply distortion around the hole edge where molten material was forced away from the hole region. This material would have been molten during the initial period of piercing. Since the heat input was deactivated immediately prior to heating, the laminate is constantly cooling whilst the piercing spike is travelling through the laminate. Therefore, matrix cracking can occur at the hole edge where the laminate temperature has locally reduced to below its melting temperature while the spike is continuing to displace material (Fig. 7).

3 Instrumented Piercing
Subsequent to the feasibility trials, instrumented piercing tests were conducted to evaluate the force-displacement characteristics of various spike geometries. The experimental set-up was modified from the feasibility trials to enable measurement of both the resisting force on the spike and the displacement of the spike through the laminate. The set-up can be broken into three main parts: the clamping system, the heating system and the instrumented piercing system (Fig. 8).

The clamping system was a manufactured steel frame that allowed the clamping force (applied pneumatically) to be transmitted around the instrumented piercing system and onto the laminate surface. The method of heating, as mentioned previously, was via induction that could provide low heat up times within susceptor materials. Cross-ply, continuous carbon fibres were used here, so the
laminates could undergo induction heating relatively easily. With induction as the heating method, the clamping system included two ceramic blocks that were used to apply pressure onto the composite. This prevented the clamping surfaces heating more rapidly than the composite, which would occur if the clamping material was a susceptor metal. The piercing system uses a pneumatic cylinder, housed within the clamping frame (Fig. 9), to drive the instrumented piercing spike through the laminate when molten. The system records the resistive force data and the displacement of the spike.

The instrumented piercing experiments were carried out in a similar way to the trials. First the laminates were clamped under pneumatic pressure before being locally heated to above their melting temperature (~350°C). Once the target temperature had been reached, the piercing spike was driven through the laminate while measuring the resistive force and corresponding displacement of the spike. The spike diameter was 6 mm and constructed from untreated stainless steel (as used previously). For the instrumented piercing, three different spike geometries were used with spike angles of 20°, 30° and 40°. Cooling under pressure and ambient conditions was continued until the laminate was below the glass transition temperature and the spike was removed prior to releasing the clamping pressure.

The force and displacement measurements were taken for two sets of instrumented piercing experiments for each spike geometry. The individual spike geometry plots for the 20°, 30° and 40° spike geometries (Fig. 10, Fig. 11 and Fig. 12 respectively) were plotted to show their relationship to the spike length (determined by the spike angle).

Similar features can be seen in the force-displacement plots for all spike geometries tested. The initial piercing stage of the process is shown by a non-linear increase in force as the spike makes contact with the top of the laminate. The contact area continues to increase as the spike tip progresses through the laminate and results in the increasing resistive force measured.

The spike tip will exit the back of the laminate as the displacement increases further. This leads to a change in the contact area and a modification of the force-displacement curve.

The force then reaches a maximum before reducing. The point at which the peak force occurs appears to be at a value of displacement at which the parallel sides of the piercing spike meet the top surface of the laminate (indicated with a dashed line on Fig. 10, Fig. 11 and Fig. 12).

After the maximum force, the parallel sides of the spike are beginning to enter the top of the laminate. The parallel sides provide no contribution to the projected contact area between the spike and the laminate. Therefore, from this point onwards the projected contact area is decreasing while the spike continues to exit the back of the laminate. This is associated with a reduction in resistive force on the spike with increasing displacement (Fig. 10, Fig. 11 and Fig. 12).

Subsequently, the spike will fully exit the back of the laminate and the parallel sides of the spike pin will remain as the only contact region between the outside of the spike and the inside of the hole. At this point the projected contact area has reduced to zero and the measured force is principally due to the friction between the spike surface and the hole wall.

Comparing the force-displacement data for the three spike geometries shows that the spike geometry influences the peak force during piercing and the corresponding displacement at which it occurs (Fig. 13). For the sharpest spike geometry, 20°, the peak force measured is lower than the force measured for the 30° spike, which in turn is lower than the force for the largest spike angle of 40°. This trend is expected since the projected area in contact with the laminate is directly related to the spike angle. Reducing the spike angle will, therefore, reduce the maximum projected contact area between the spike and the laminate and lead to a reduction in maximum resistive force on the spike (Fig. 13).

The location of the peak force is also governed by the geometry of the spike. The length of the conical section of the spike (before the spike becomes parallel) will be determined by the spike angle. For smaller spike angles, this length will be increased and the peak force will be experienced at a larger
displacement than for larger spike angles. This expected trend can be seen in the data (Fig. 13) where the peak force is not only lower for the 20° spike, but also occurs at a larger displacement than for the 30° spike. The same trend is apparent when comparing the data for the 30° and 40° spikes.

After the spike emerged from the back face of the laminate and only frictional forces were measured, it seemed reasonable to assume that the frictional force should be the same for all three spike geometries, since the shaft diameter is the same in each case. The measured data does not support this, however, and can be seen to show varying frictional values (Fig. 13) in this final stage. This may be because of elastic contraction of the hole around the spike. The heat input was deactivated upon piercing so the laminate continued to reduce in temperature for the duration of the piercing stages. For larger spike angles the spike length is shorter and, hence, the piercing process occurs over a smaller displacement of the spike. For a constant spike displacement rate, this reduction in vertical displacement will decrease the time for the piercing process to be completed. The shorter time means that the laminate will not locally cool to the same extent as for piercing with sharper spike geometries. This would allow more viscous flow of the higher temperature matrix and result in a reduced elastic contraction around the spike and a lower final frictional force, as seen in Fig. 13.

The effect of matrix cooling over the piercing process may also be manifested in the total work done by the spike to produce the hole (Table. 1). The work done, derived from the area under the load-displacement curves (from initial piercing to the exit of the conical spike section at the back face), reduces for increasing spike angle. Since the matrix does not cool to the same extent when piercing with a larger spike angle, it remains less viscous and requires less work to displace when compared with smaller spike angle geometries.

4 Concluding Remarks
Initial feasibility trials have shown that an increase in tensile notched strength of 8% can be achieved for thermally pierced laminates when compared to conventionally drilled laminates. To investigate the parameters involved in the thermal piercing process, an instrumented rig has been designed to enable the load-displacement response to be monitored during piercing. Preliminary experiments have shown that the piercing process consists of the following distinct phases:

- Initial piercing of the laminate with the spike tip progressing through the thickness
- A secondary stage, where the spike tip has emerged from the back face of the laminate and the resistive force on the spike appears approximately linear with spike displacement
- Immediately prior to the parallel sides of the spike reaching the top surface of the laminate the peak resistive force on the spike is reached
- The spike exits from the back face of the laminate, reducing the resistive force on the spike due to decreasing projected contact area
- Frictional sliding occurs between the parallel sides of the spike and the hole wall once the spike has fully exited the laminate.

A smaller spike angle leads to a smaller maximum resistive force on the spike. The spike angle also dictates the spike length, which governs the location of the maximum resistive force in the force-displacement results.

Future work will include an investigation of the effects of thermal piercing spike angles on the strength of the laminates.

5 Acknowledgements
This work has been undertaken as part of an Engineering Doctorate in Micro- and NanoMaterials and Technologies at the University of Surrey. The authors are pleased to acknowledge the financial support of the EPSRC (EP/G037388/1) and TWI’s Member Companies through the Core Research Programme.

6 References


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**Fig. 1.** Schematic of 3 stage thermal piercing process.

**Fig. 2.** Schematic of 30° piercing spike.

**Fig. 3.** Normalised tensile notched strength.

**Fig. 4.** Schematic of specimen sectioning for microscopy.
Fig. 5. SEM micrograph of hole wall (1)

Fig. 6. SEM micrograph of hole wall (2)

Fig. 7. SEM micrograph of hole edge with resultant laminate structure.
Fig. 8. Schematic of instrumented piercing set up.

Fig. 9. Photograph of instrumented piercing test rig.
Fig. 10. Force-displacement of 20° spike thermal piercing.

Fig. 11. Force-displacement of 30° spike thermal piercing.

Fig. 12. Force-displacement of 40° spike thermal piercing.

Table 1. Approximate average work done for thermal piercing with various spike geometries.

<table>
<thead>
<tr>
<th>Spike Angle (°)</th>
<th>Approximate Total Work Done (J)</th>
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<tr>
<td>20</td>
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<tr>
<td>30</td>
<td>7.4</td>
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<tr>
<td>40</td>
<td>4.6</td>
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Fig. 13. Force-displacement of thermal piercing with various spike geometries.