Moderately Three-Dimensional Separated and Reattaching Turbulent Flow

by

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Summary

A detailed experimental study of two-dimensional and three-dimensional separated flow behind a normal fence, mounted at the front of a long splitter plate, is reported. The Reynolds number, based on the height of the fence above the splitter plate, was 3800. Results from numerical simulations of the two-dimensional flow, involving eddy viscosity ‘k-ε’ and Reynolds stress turbulence models, are also presented.

A systematic approach has been used to extend earlier work on two-dimensional coplanar separated flows to three-dimensional flows using a downstream facing v-shaped separation line. The ‘arms’ of this swept geometry are wide enough to provide two spanwise-invariant regions where the flow is two-dimensional, but is not coplanar because of the existence of lateral velocity. In these regions, six of the mean rates of strain are non-zero compared to the four that are non-zero in the unswept flow. In the central part of the flow, where the two lateral inflows meet and decelerate, the flow is three-dimensional and all nine mean rates of strain are non-zero.

Extensive use of pulsed-wire anemometry techniques to determine mean and fluctuating quantities in an unswept separated flow and in the spanwise invariant region of the swept flow revealed that, for the sweep angle considered (10 degrees), the two separation bubbles are very similar in most respects. For the swept case, the bulk of the flow is convected sideways (parallel to the separation line) at a roughly uniform velocity, with the lateral velocity going to zero rapidly near the surface. The component of vorticity perpendicular to the separation line at separation and the additional strain rates such as $\frac{\partial W}{\partial y}$ associated with the swept flow appear to have only a small effect on turbulence levels.

In the central, fully three-dimensional part of the flow, significant increases in the bubble size were observed, owing to the swelling effect of the two lateral inflows. The increased size of the bubble was accompanied by dramatic increases in Reynolds normal and shear stresses. Examination of the extra rates of strain in the central region indicated that the strain ratio $(\frac{\partial W}{\partial z})/(\frac{\partial U}{\partial y})$ was having a critical role in governing the turbulence levels.
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<td>aspect ratio (length of separation line/fence height above splitter plate, $h_f$)</td>
</tr>
<tr>
<td>$B$</td>
<td>blockage ratio (height of tunnel/fence height, $h$)</td>
</tr>
<tr>
<td>$C_f$</td>
<td>time-mean skin friction coefficient ($C_f = 2\tau_w / \rho U_{ref}^2$)</td>
</tr>
<tr>
<td>$C_f'$</td>
<td>time-mean skin friction coefficient based on the component of free-stream velocity in the direction normal to the leading edge ($C_f' = 2\tau_w / \rho U_0^2$)</td>
</tr>
<tr>
<td>$C_{fx}$</td>
<td>component of time-mean skin friction coefficient in the $x$-direction ($C_{fx} = 2\tau_{wx} / \rho U_{ref}^2$)</td>
</tr>
<tr>
<td>$C_{fx}'$</td>
<td>component of time-mean skin friction coefficient in the $x'$-direction based on the component of free-stream velocity in the direction normal to the leading edge ($C_{fx}' = 2\tau_{wx}' / \rho U_0^2$)</td>
</tr>
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<td>$C_{fxz}$</td>
<td>component of time-mean skin friction coefficient in the $z$-direction ($C_{fxz} = 2\tau_{wx} / \rho U_{ref}^2$)</td>
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<td>$C_{fxz}'$</td>
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</tr>
<tr>
<td>$C_{fN}$</td>
<td>time-mean skin friction coefficient based on the local value of the maximum reversed velocity, $U_N$ ($C_{fN} = 2\tau_w / \rho U_N^2$)</td>
</tr>
<tr>
<td>$C_p$</td>
<td>time-mean static pressure coefficient ($C_p = 2(p - p_{ref}) / \rho U_{ref}^2$)</td>
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<td>$C_p'$</td>
<td>time-mean static pressure coefficient based on the component of free-stream velocity in the direction normal to the leading edge ($C_p' = 2(p - p_{ref}) / \rho U_0^2$)</td>
</tr>
<tr>
<td>$f$</td>
<td>frequency</td>
</tr>
<tr>
<td>$h$</td>
<td>height of normal fence ($h=2h_f+t$)</td>
</tr>
<tr>
<td>$h$</td>
<td>height of step for backward facing step geometry or thickness of plate for bluff thick plate geometry</td>
</tr>
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<td>$h_f$</td>
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<td>height of the separation bubble defined as the vertical distance from the splitter plate at which $U=0.95U_{max}$</td>
</tr>
<tr>
<td>$k$</td>
<td>turbulence kinetic energy ($k = (u^2 + v^2 + w^2)/2$)</td>
</tr>
<tr>
<td>$p$</td>
<td>time-mean static pressure on splitter plate</td>
</tr>
<tr>
<td>$p_{ref}$</td>
<td>reference static pressure taken in the undisturbed flow upstream of the separation</td>
</tr>
<tr>
<td>$q^2$</td>
<td>sum of the normal Reynolds stresses ($q^2 = (u^2 + v^2 + w^2)$)</td>
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Re_{h_f} \quad \text{Reynolds number based on height above splitter plate, } h_f

Re_h \quad \text{Reynolds number for backward facing step geometry and bluff thick plate geometry, based on step height and plate thickness, respectively}

Re_N \quad \text{Reynolds number based on the local value of the maximum reversed velocity } U_N \text{ and the height from the splitter plate at which this maximum velocity occurs, } y_N

Re_x \quad \text{Reynolds number based on the local value of the maximum reversed velocity } U_N \text{ and the axial distance from the attachment position}

t \quad \text{thickness of splitter plate}

U \quad \text{mean velocity in the streamwise (x) direction}

U' \quad \text{mean velocity in the direction normal to the separation line (chordwise velocity)}

U_{ref} \quad \text{free-stream velocity}

U_0 \quad \text{component of the free-stream velocity in the direction normal to the separation line.}

U_{\text{max}} \quad \text{local maximum value of streamwise velocity}

U_{\text{min}} \quad \text{local minimum value of streamwise velocity}

U_{\text{max}}' \quad \text{local maximum value of chordwise velocity}

U_{\text{min}}' \quad \text{local minimum value of chordwise velocity}

U_N \quad \text{local value of the maximum reversed streamwise velocity}

U_{N'} \quad \text{local value of the maximum reversed chordwise velocity}

U_f \quad \text{friction velocity } (U_f = \sqrt{\tau_w / \rho})

U^+ \quad U \text{ normalised by } U_f

\overline{u^2} \quad \text{streamwise (x) normal Reynolds stress}

\overline{u'^2} \quad \text{chordwise (x') normal Reynolds stress}

\overline{uv} \quad \text{Reynolds shear stress in streamwise (x)/vertical (y) direction}

\overline{u'v} \quad \text{Reynolds shear stress in chordwise (x')/vertical (y) direction}

\overline{uw} \quad \text{Reynolds shear stress in streamwise (x)/lateral (z) direction}

\overline{u'w'} \quad \text{Reynolds shear stress in chordwise (x')/lateral (z') direction}

V \quad \text{mean velocity in the direction normal to the surface of the splitter plate (vertical (y) direction)}

\overline{v^2} \quad \text{normal Reynolds stress normal to surface of splitter plate}

\overline{vw} \quad \text{Reynolds shear stress in vertical (y)/lateral (z) direction}

\overline{vw'} \quad \text{Reynolds shear stress in vertical (y)/lateral (z') direction}

W \quad \text{mean velocity in the lateral (z) direction}

W \quad \text{width of the separation line}

W_0 \quad \text{component of the free-stream velocity in the direction parallel to the separation line}
Nomenclature

\( W' \) mean velocity in the lateral (\( z' \)) direction

\( \overline{w^2} \) spanwise (\( z \)) normal Reynolds stress

\( \overline{w'^2} \) fencewise (\( z' \)) normal Reynolds stress

\( x \) streamwise distance downstream of separation line

\( x' \) chordwise distance downstream of separation line

\( x'' \) streamwise distance downstream of separation line measured on the \( z=0 \) plane

\( X_a \) streamwise attachment length

\( X_r \) streamwise reattachment length (used only when flow is two-dimensional)

\( X_r' \) chordwise reattachment length (used only when flow is two-dimensional)

\( x^* \) relative downstream/chordwise position (\( x/X_a \) or \( x'/X_r' \))

\( y \) normal (vertical) distance from splitter plate

\( y_c \) normal distance from the splitter plate at which \( U' = (0.67 U' + U_{min}') \) where \( \Delta U' = U_{max}' - U_{min}' \). This corresponds to the shear layer centreline.

\( y_N \) normal distance from the splitter plate at which maximum reversed velocity occurs

\( y^* \) non-dimensional distance from wall based on \( U_r \) (\( y^* = y U_r / \nu \))

\( z \) spanwise distance from centre of tunnel, measured normal to the sidewalls of the tunnel

\( z' \) distance from the centre of the tunnel at the separation point, measured parallel to the separation line.

Greek symbols

\( \alpha \) rotation about the y-axis

\( \alpha \) sweep angle of the fence

\( \nu \) kinematic viscosity of air

\( \mu \) dynamic viscosity of air

\( \rho \) density of air

\( \tau_w \) magnitude of the wall shear stress

\( \tau_{wx} \) component of wall shear stress in the streamwise direction

\( \tau_{wx}' \) component of wall shear stress in the chordwise direction

\( \tau_{wz} \) component of wall shear stress in the spanwise (\( z \)) direction

\( \tau_{wz}' \) component of wall shear stress in the spanwise (\( z' \)) direction

\( \theta \) rotation about the z-axis

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1. Introduction

1.1 General

Turbulent separation occurs in a wide variety of industrial, environmental and scientific situations, and in structural terms it constitutes one of the more complex of turbulent flows. Typical examples of internal flows in which separation can be an important consideration are those in engine inlets, ducts, fans and compressors. External flows such as those around buildings and structures, and in flows over ground and river-bed terrains, may also be strongly influenced by the presence of separated regions. From a structural point of view, the response of building surfaces to strong pressure fluctuations produced by separation can be a vitally important consideration. Regions of separated flow can also play a key role in aircraft aerodynamics. Flow separation on the wings may be accompanied by radical changes in the time-mean and unsteady aerodynamic loads and also affect aircraft manoeuvrability and controllability.

A feature shared by the vast majority of separated flows of practical concern is that they are three-dimensional in the time-mean. In contrast, nearly all of the investigations of separated flows have been made in nominally two-dimensional coplanar separations. Many geometries have been used and a range of the most important ones is given in Figure 1.1. (The figure showing the fence and splitter plate arrangement contains more detail than the other figures, as this was the geometry used for the present investigation). Although the various geometries appear quite different from one another, the separated flows generated by them have much in common. The separated shear layer starts out as a boundary layer on the bluff body and, if this is not turbulent, transition occurs shortly after separation except for very low Reynolds number flows. The thickness of the separated shear layer increases with downstream distance, as fluid is entrained from above and below the shear layer. This can be seen in Figure 1.1e.

The entrainment process on the side of the shear layer closest to the wall is accompanied by low pressure on this surface. This low pressure causes the shear layer to curve downwards and attach to the surface at some downstream location. For two-dimensional flow, the separating streamline is also the attaching streamline, causing a closed separation 'bubble'. Consequently, only for two-dimensional flow is it correct to use the term 'reattachment' streamline. At reattachment, some of flow moves downstream and some moves back upstream to make up for the flow which is being entrained into the shear layer, leading to a reversed flow region underneath the separation bubble.
Given the complexity of separated flows, it is important to adopt a systematic approach to extend this earlier work on nominally two-dimensional coplanar flows to three-dimensional flows. Such an approach was laid out by McCluskey, Hancock & Castro (1992), leading to three types of adjacent region, denoted A, B and C, which are shown in Figure 1.2. Region A is a spanwise-invariant region \( \partial / \partial z = 0 \) where the separation line has been swept at some incidence angle to the flow. This region is two-dimensional, but is not coplanar because of the existence of a lateral velocity \( W \). In this region six of the mean strain rates of strain are non-zero compared to the four that are non-zero in the unswept flow. Flow visualisation studies by McCluskey, Hancock & Castro (1992) suggested that the flows in regions B and C were broadly diverging \( \partial W / \partial z > 0 \) and converging \( \partial W / \partial z < 0 \), respectively, and were symmetrical about the central plane. The flow in these regions is three-dimensional and all nine mean rates of strain are non-zero. (Because of the symmetry of the flow, \( \partial U / \partial z, \partial V / \partial z, \partial W / \partial x \) and \( \partial W / \partial y \) are zero on the central plane).

McCluskey, Hancock & Castro (1992) and Hancock & McCluskey (1997) made measurements of mean flow variables and of Reynolds stresses in a swept invariant region flow, at 25° sweep, but no measurements were made in the three-dimensional regions of the geometry shown in Figure 1.2. Although a limited number of workers have made measurements of pressure and mean velocity in swept invariant flow, the measurements of Reynolds stresses in this type of flow by McCluskey, Hancock & Castro (1992) were the only published ones in existence at the beginning of the present investigation.

1.2 Objectives

The principal objective of the present study is to make an experimental investigation of turbulent three-dimensional separated and attaching flow, by systematic departure from the two-dimensional coplanar case with extensive use of pulsed-wire anemometry. The framework suggested by McCluskey, Hancock & Castro (1992) is used. The normal fence and splitter plate geometry shown in Figure 1.1e was used to generate the separation, because for a given blockage ratio (or step ratio for the backward facing step) this geometry produces a larger separation bubble. This is useful in terms of minimising spatial resolution problems, and also reduces the chance of probe interference effects. This model geometry has also been studied extensively at the University of Surrey for nominally two-dimensional flows.

The first stage of the work was to make a baseline set of measurements of mean and fluctuating quantities in an unswept, coplanar flow. This was felt necessary as the work by McCluskey, Hancock & Castro (1992) suggested that the vast majority of previous
investigations of nominally two-dimensional coplanar flow had been made in rigs in which the aspect ratio was far too small, by a factor of between two and ten. The second and third stages of the work was to consider region A and C flow, shown in Figure 1.2. This was done using a backward facing v-shaped separation line, where the width of the flow was sufficient for each of the two side flows to contain a spanwise-invariant region. A mild sweep angle of 10 degrees is considered. This geometry along with the unswept flow can be seen in the table below. Also shown in this table are the mean rates of strain occurring in the regions of interest.

<table>
<thead>
<tr>
<th>experimental configuration and areas considered</th>
<th>mean rates of strain present</th>
</tr>
</thead>
<tbody>
<tr>
<td>unswept flow</td>
<td>$\frac{\partial U}{\partial x}$  $\frac{\partial U}{\partial y}$  _ _</td>
</tr>
<tr>
<td></td>
<td>$\frac{\partial V}{\partial x}$  $\frac{\partial V}{\partial y}$  _ _</td>
</tr>
<tr>
<td></td>
<td>_ _ _ _</td>
</tr>
<tr>
<td>v-configuration fence</td>
<td>$\frac{\partial U}{\partial x}$  $\frac{\partial U}{\partial y}$  _ _</td>
</tr>
<tr>
<td></td>
<td>$\frac{\partial V}{\partial x}$  $\frac{\partial V}{\partial y}$  _ _</td>
</tr>
<tr>
<td></td>
<td>$\frac{\partial W}{\partial x}$  $\frac{\partial W}{\partial y}$  _ _</td>
</tr>
<tr>
<td>v-configuration fence</td>
<td>$\frac{\partial U}{\partial x}$  $\frac{\partial U}{\partial y}$  $\frac{\partial U}{\partial z}$</td>
</tr>
<tr>
<td></td>
<td>$\frac{\partial V}{\partial x}$  $\frac{\partial V}{\partial y}$  $\frac{\partial V}{\partial z}$</td>
</tr>
<tr>
<td></td>
<td>$\frac{\partial W}{\partial x}$  $\frac{\partial W}{\partial y}$  $\frac{\partial W}{\partial z}$</td>
</tr>
</tbody>
</table>

Table 1.1 Experimental configurations used for present investigation

Measurements of mean velocities and Reynolds stresses made in the unswept flow and in the Region A flow are compared to elucidate the effects of sweep angle. A specially designed near-wall probe has been used to allow the effects of gradients such as $\partial W/\partial y$ to be measured. In the swept flow, $\partial W/\partial y$ was approximately the same magnitude as $\partial U/\partial y$ close to the wall. Parameters such as the reattachment length, and the resulting surface topology are also compared for the two flows.
1. Introduction

Detailed measurements of mean velocity on and around the central plane of the swept v-configuration fence allowed the determination of all nine mean rates of strain in this region. It appears that changes in turbulence structure in the various flow regions can be broadly related to these sets of mean strain rates. Special attention is paid to strain rates associated with lateral convergence/divergence \((∂W/∂z)\) and streamline curvature \((∂V/∂x)\) which are known to have a large effect on the turbulence structure of shear flows.

This study is primarily concerned with an experimental investigation of the physics of the flow in the central region of the v-configuration fence. However, some computations of the unswept flow and the swept (spanwise-invariant) flow were made using two turbulence models, namely an eddy viscosity 'k-ε' model and a Reynolds stress model. For this work, the commercial code of CFX-F3D was used.
1. Introduction

Figure 1.1 Various geometries used for separated flows

- a) Backward-facing step
- b) Blunt plate
- d) Profiled body with splitter plate
- d) Triangular body with splitter plate
- e) Normal fence with splitter plate

Figure 1.2 Regions A, B and C downstream of separation line, taken from McCluskey, Hancock & Castro (1992)
2. Literature Survey

2.1 Unswept Separation

Extensive literature has been published concerning nominally two-dimensional coplanar separated flow. As previously discussed in the introduction, there are several experimental configurations which generate this kind of separation and a wide range of measurement techniques have been adopted. The following literature survey of two-dimensional separated flow is largely an updated version of the survey carried out by Wolf (1987).

2.1.1 The Static Pressure Distribution Beneath a Separation Bubble

The static pressure distribution on the solid surface beneath a separation bubble is qualitatively the same for the flow past a backward-facing step, a normal fence with splitter plate, a blunt-nosed body and the flow at a pipe entrance (Wolf, 1987). For all of these flow regimes, the pressure distribution has been determined by many different workers. A static pressure coefficient which has been used widely is:

\[ C_P = \frac{P - P_{\text{ref}}}{0.5\rho U_{\text{ref}}^2} \]  

(2.1)

The reference conditions in this coefficient are taken in the free-stream flow upstream of the separation process. For the two-dimensional separation bubble formed behind a normal fence with splitter plate, typical distributions of this parameter with downstream position are given in Figure 2.1 (taken from Castro and Haque, 1987). Inside the bubble, large mean suction (negative pressures) occur, as fluid is entrained into the shear layer from the region bounded by the solid surface. The pressure difference between the free-stream and the recirculating region causes the shear layer to curve towards the solid boundary and eventually reattach to form the separation bubble. As the reversed flow travels back upstream it encounters an adverse pressure gradient at around \( x/X_f = 0.4 \) due to the presence of the fence, which causes the secondary separation.

It is also possible to renormalise the wall-static-pressure data using a pressure coefficient defined by:

\[ \bar{C}_P = \frac{C_P - C_{p_{\text{min}}}}{1 - C_{p_{\text{min}}}} \]  

(2.2)
This was first suggested by Roshko & Lau (1965), and it has since been shown that when applied to pressure data collected by many workers, distributions can be made to collapse together very closely, as shown in Figure 2.2. This includes work carried out using all of the bluff body separation geometries shown in Figure 1.1 and backward-facing step geometries for thin boundary layers at separation. Investigations that have used this coefficient include Smits (1982), Hillier et al (1983), Westphal & Johnston (1984), Ruderich & Fernholz (1986) and Jaroch & Fernholz (1989). However, this coefficient is certainly not a universal function for separated flows since it has been shown that it does not take account of the effects of a strong imposed pressure gradient. Also, pressure profiles plotted in this way from experiments using a backward facing geometry do not collapse with profiles from other geometries if the boundary layer at separation is thick. Adams, Johnston and Eaton (1984) showed that thicker initial boundary layers give rise to lower reattachment pressures and lower peak pressures. It was also reported that for initial boundary layers of the same thickness, the profiles of $\tilde{C}_p$ against $x/X$, are independent of the state of the boundary layer at separation.

A flow with an imposed pressure gradient was studied by Devenport (1985). The configuration used was a backward-facing step in a pipe, with and without a centrebody downstream of the step. The effect of adding this centrebody was to decrease the length of the separation bubble by up to 70% and the two resulting distributions of wall static pressure coefficient, $\tilde{C}_p$, plotted against the downstream position were not collapsible even if $x$ was normalised using the reattachment length.

Various other parameters have been considered to describe the pressure recovery in the second half of the bubble. For example the non-dimensional pressure rise to reattachment:

$$\tilde{C}_{pr} = \frac{C_{pr} - C_{ps}}{1-C_{ps}} \quad (2.3)$$

where $C_{ps}$ and $C_{pr}$ are the values of the pressure coefficient at separation and reattachment, respectively. A second example is the peak pressure rise, shown below:

$$\tilde{C}_{p_{\text{max}}} = \frac{C_{p_{\text{max}}} - C_{p_{\text{min}}}}{1-C_{p_{\text{min}}}} \quad (2.4)$$
where $C_{p\min}$ and $C_{p\max}$ are the minimum and maximum values of the pressure coefficient, respectively. Some of the values of the above parameters from measurements by other workers are given below:

<table>
<thead>
<tr>
<th>Worker</th>
<th>Geometry</th>
<th>$\bar{C}_{pr}$</th>
<th>$\bar{C}_{p\max}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Hillier, Latour &amp; Cherry (1983)</td>
<td></td>
<td>0.32</td>
<td>0.40</td>
</tr>
<tr>
<td>Ruderich &amp; Fernholz (1986)</td>
<td></td>
<td>0.34</td>
<td>0.39</td>
</tr>
<tr>
<td>Castro &amp; Haque (1987)</td>
<td></td>
<td>0.36</td>
<td>0.39</td>
</tr>
<tr>
<td>Hancock (1994)</td>
<td></td>
<td>0.29</td>
<td>0.36</td>
</tr>
<tr>
<td>Hillier, Latour &amp; Cherry (1983)</td>
<td></td>
<td>0.32</td>
<td>0.39</td>
</tr>
<tr>
<td>Roshko &amp; Lau (1965)</td>
<td></td>
<td>0.32</td>
<td>0.40</td>
</tr>
<tr>
<td>Ota &amp; Itasaka (1975)</td>
<td></td>
<td>0.33</td>
<td>0.39</td>
</tr>
<tr>
<td>Hillier, Latour &amp; Cherry (1983)</td>
<td></td>
<td>0.32</td>
<td>0.37</td>
</tr>
<tr>
<td>Chandrsuda (1976) ($\delta/h=0.04$)</td>
<td></td>
<td>0.32</td>
<td>0.36</td>
</tr>
<tr>
<td>Adams, Johnston Eaton (1984) ($\delta/h=0.04$)</td>
<td></td>
<td>0.28</td>
<td>0.34</td>
</tr>
<tr>
<td>Adams, Johnston Eaton (1984) ($\delta/h=1.0$)</td>
<td></td>
<td>0.19</td>
<td>0.25</td>
</tr>
<tr>
<td>Hillier, Latour &amp; Cherry (1983)</td>
<td></td>
<td>0.28</td>
<td>0.34</td>
</tr>
</tbody>
</table>

Table 2.1 $\bar{C}_{pr}$ and $\bar{C}_{p\max}$ determined by various workers

The values of peak pressure rise and pressure rise to reattachment are reasonably constant for the bluff body geometries and the backward-facing step geometries with thin initial boundary layers. As has already been mentioned, work performed by Adams, Johnston and Eaton (1984) has shown that if the boundary layer thickness (based on the step height) at separation is increased, lower pressure at reattachment and lower peak pressure occur. This is apparent if the backward-facing geometry with the initial boundary layer thickness, $\delta/h$,
of 1 is considered. Although the boundary layer thickness of the geometry with the profiled nose was not measured by Hillier, Latour and Cherry (1983), the reduced values of $\tilde{C}_{pr}$ and $\tilde{C}_{p_{\max}}$ suggest that $\delta/h$ was larger for this geometry than the others that were considered by these workers, which are also shown in the table.

The effect of Reynolds number on the distribution of mean-pressure has been studied by Castro and Haque (1987), Cherry, Hillier & Latour (1984), Nakamura & Ozono (1987) and Hancock (1994). All of these investigations employed bluff-body separations, and the results of all these workers suggest that the $C_p$ distribution is virtually independent of Reynolds number provided the Reynolds number is sufficiently high. The results taken from the Castro and Haque paper are shown in Figure 2.1.

The effect of free-stream turbulence on the pressure distribution beneath the separation bubble has been investigated by Cherry (1982), Nakamura and Ozono (1987) and Castro and Haque (1988). Results from these papers suggest that increasing the level of free-stream turbulence intensity results in a reduction in the pressure at separation and the minimum pressure. The subsequent pressure recovery is also significantly more rapid. However, Castro and Haque (1988) showed that the renormalising parameter suggested by Roshko and Lau (1965), $\tilde{C}_p$, still collapses pressure profiles taken in experiments with and without free-stream turbulence, provided the downstream distance is non-dimensionalised by the reattachment length.

In the paper by Nakamura & Ozono, the effects of turbulence scale were explored using the blunt plate geometry. Values of $C_p$ were found to be insensitive to changing integral length scale up to approximately $L_x/h \approx 2$ (where $L_x$ is the integral length scale of the $u$-component free-stream velocity and $h$ is the thickness of the plate). This is in agreement with Hillier and Cherry (1981). With further increases in turbulence scale, the mean pressure distribution on the plate beneath the separation bubble asymptotes towards the smooth flow condition. It is suggested by these workers that turbulence of very large scale is equivalent to a flow with slowly fluctuating velocity, and hence it can no longer influence the bluff body mean flow effectively.

2.1.2 The Reattachment Length
2.1.2.1 Effect of Blockage and Aspect Ratio on Reattachment Length

Studies of blockage effects using the normal fence and splitter plate flow configuration have been carried out by several workers. These include: Smits (1982), Hancock & Castro
(1983), Ruderich & Fernholz (1986) and Castro & Haque (1987). In all of these investigations the reattachment length and the bubble height were found to decrease with an increase in blockage.

The aspect ratio is defined as the tunnel width or the distance between the end plates to the height of the step or the blunt plate. If the aspect ratio is too small, then the end effects from the tunnel walls mean that the nominally two-dimensional flow is in fact three-dimensional. These effects have received much less attention than blockage ratio effects. This may partially be due to Brederode (1975) who investigated the effects of end-plates and aspect ratio on the two-dimensionality of the flow past a forward-facing step, a blunt plate and a backward-facing step. He concluded that the flow was two-dimensional in his terms if the aspect ratio was greater than 10.

However, in a later study of the turbulent structure in a nominally two-dimensional separation formed on the normal flat fence and splitter plate arrangement, Ruderich & Fernholz (1986) concluded that their aspect ratio of 23 was too small for the effect of the side wall to be negligible. Later Jaroch & Fernholz (1989), suggested that their aspect ratio of 63 was still not large enough to avoid these three-dimensional effects at the centre of the tunnel. More recently, surface flow visualisation studies carried out by Hancock, McCluskey & Castro (1992) have suggested that the aspect ratio, \( \frac{W}{h_{ft}} \), should be at least 120 for a spanwise invariant region to exist in the central region and 150 for an invariant width of about 1 bubble length.

One of the difficulties in assessing the effect of blockage and aspect ratio on the reattachment length is that there are very few investigations where a systematic study of each component has been undertaken. In fact, it appears that nearly every study has changed both variables simultaneously. Previous attempts to decipher the independent effect of blockage, \( B \), and aspect ratio, \( A \), on \( X_r \), have invariably been forced to plot two-dimensional graphs (\( X_r \) against \( B \) or \( A \)) using, effectively, data with three variables. This has lead to very crude relationships between the flow variables.

On the other hand, if a three-dimensional graph is plotted of all the data collected on this type of separated flow, a very complex yet well defined contour showing the variation of \( X_r \) with \( B \) and \( A \) becomes apparent. This can be seen in Figure 2.3. Unfortunately, the paucity of investigations using an aspect ratio greater than 60 and a blockage ratio greater than a few percent mean that assumptions must be made in this region. However, it would seem inconceivable that the effect of blockage at high aspect ratios is not to reduce the reattachment length, and the effect of aspect ratio seems only to be significant at low values for the total range of blockage ratios presented. Using these guidelines an interpretation of the results is
given by the author in Figure 2.4. This is not meant to be a quantitative guide to determining the reattachment length given a certain experimental configuration, but rather to show the degree of complexity in the relationship between $B$ and $A$ with $X_r$ that seems probable.

The implied two-dimensional graphs of blockage against $X_r$ at a constant aspect ratio would appear to vary significantly over the range of blockage values considered. As already mentioned, at a constant blockage ratio, the effect of aspect ratio on $X_r$ seems to be most significant when $A$ is less than approximately 50. For the investigations that have used values of aspect ratio in the region where it is affecting $X_r$, it is likely that the flow will not have been genuinely two-dimensional, and the reattachment line will not have been straight.

2.1.2.2 Effect of Free-Stream Turbulence on Reattachment Length

There have been very few investigations on the effects of free-stream turbulence on separated and reattaching flows. These are: Hillier & Cherry (1981), Kiya, Sasaki, & Arie (1984), Nakamura & Ozono (1986), and Castro & Haque (1988). All of these papers reported that the length of the separation bubble was reduced considerably with increasing turbulence intensity but there was no significant effect of changing the integral length scale of the free-stream for low scale ratios (the scale ratio is defined as the ratio of the integral length scale of the $u$-component free-stream velocity to a physical scale of the separation geometry). For example, in the case of the flow past a bluff thick plate, Hillier & Cherry (1981), found a 45% reduction in bubble length, when the turbulence intensity in the free-stream was raised from about 0.1% to 6.5%. They also showed that the bubble length was insensitive to changes of turbulence scale up to around $L/h=2$.

2.1.2.3 Influence of Flow Conditions at Separation on Reattachment Length

Adams, Johnston & Eaton (1984) investigated the backward-facing step flow in great detail. The experiments were motivated by the observation that differences in reported reattachment lengths for backward-facing step flows are as large as 20-50% under nominally the same flow conditions. The effects of expansion ratio, the step-height Reynolds number, and the non-dimensional upstream boundary-layer thickness and state were investigated.

It was found that the primary variable affecting the location of reattachment is the boundary layer state, not the boundary layer thickness; the bubble became about 30% longer when the
boundary layer was turbulent rather than laminar. The bubble length for a transitional boundary layer at separation was about halfway between the turbulent and laminar cases.

In this report, no explanation was given for this behaviour. Since it is known that the effect of free-stream turbulence is to reduce the length of the separation bubble due to the increased turbulent mixing in the shear layer, it may seem surprising that an initially turbulent boundary layer results in a longer bubble than a laminar boundary layer of similar thickness. However, it can be explained if previous studies of plane mixing layers are considered. For these shear layers, the downstream development is known to be very sensitive to the state of the boundary layer on the splitter plate (Bradshaw, 1966 and Wygnanski & Fiedler, 1970). This sensitivity is apparent in downstream measurements of the thickness of the mixing layer and of the peak levels of turbulence.

The spreading rate of a mixing layer starting from a turbulent boundary layer is characterised by two distinct spreading rates. However, in the same facility, the asymptotic spreading rate of an initially turbulent layer is usually found to be much lower than the value corresponding to a mixing layer which began as a laminar boundary layer (Ho & Heure, 1984). The development of Reynolds stresses is also different. For an initially turbulent layer the stresses rise monotonically with increasing downstream distance until they reach an asymptotic value. For an initially laminar boundary layer the initial rate of increase in Reynolds stresses is higher than the initially turbulent case. These stresses rise significantly above the asymptotic value of the turbulent case before reaching a plateau and then settling back to approximately the same constant value characteristic of fully developed turbulent mixing layers.

Consequently, the idea that increased levels of turbulence in a separated shear layer cause a shorter reattachment length is not in conflict with the evidence that an initially laminar boundary layer for the backward-facing step flow causes a shorter bubble. Although the levels of turbulence in the very early part of the separated shear layer will be lower than the corresponding turbulent case, the levels will soon become be higher. These higher levels of turbulence then go on to increase entrainment within the shear layer which, in turn, increases the curvature towards the lower surface and result in early reattachment.

During this investigation of Adams, Johnston & Eaton (1984), the effect of varying the Reynolds number based on step height, \( Re_h \), was also studied. \( Re_h \) did have an effect on the reattachment length distinct from the effects of transition. For laminar and turbulent initial conditions, it was found that increasing the Reynolds number from 8000 to 36,000 resulted in a steady increase in the reattachment length. This observed behaviour of \( X_r \) for
turbulent conditions was not at all well understood as it was assumed that, if the Reynolds number was high enough, the flow would become Reynolds number independent. For the flow past a normal fence with splitter plate, Hancock (1994), found that the reattachment position was virtually unchanged for Reynolds number changes in the range $2000 < Re_{h} < 12700$, though $u^2$ changed significantly.

### 2.1.3 The Steady and Unsteady Skin-Friction Beneath a Separation Bubble

The mean levels of skin friction in the recirculating region have been studied by several workers. These include Chandrsuda and Bradshaw (1980), Ruderich and Fernholz (1986), Castro and Haque (1988), Adams and Johnston (1988), Dijali & Gartshore (1991), Fernholz (1994) and Hancock (1994).

Profiles of $C_f$ against downstream position taken from Hancock (1994) using Reynolds numbers, based on fence height, of 2000, 3600, 6800 and 12700 are shown in Figure 2.5. The general features of these profiles taken at different Reynolds numbers are the same - starting from the reattachment point and moving towards the fence, the skin friction coefficient becomes negative as the flow above the surface of the splitter plate accelerates in the negative $x$-direction due to the favourable pressure gradient. The skin friction coefficient reaches a minimum value about halfway along the separation bubble and then returns to zero at the secondary separation, caused by the adverse pressure gradient close to the fence. Between the secondary separation line and the fence, the value of $C_f$ remains positive as the flow above the surface here moves in the positive streamwise direction. Downstream of reattachment the time-mean skin-friction continues to increase sharply reaching a maximum value several reattachment lengths downstream.

It is clear from this figure that $C_p$, unlike the static pressure profiles and the reattachment length, is dependent on the Reynolds number. The magnitude of the stress decreases markedly as the Reynolds number increases. This behaviour was also observed by Castro and Haque (1987). The results of Hancock (1994), which were performed over a much lower Reynolds number range also suggest that although the reattachment position was quite insensitive to Reynolds number, the position of the secondary separation line was not. In the Figure 2.5, the secondary separation bubble can be seen to get longer with decreasing Reynolds number.

In order to compare their data with that of other workers, Castro & Haque plotted minimum $C_f$ values, renormalised using the minimum negative velocities occurring above the surface at the appropriate axial location, against a Reynolds number based on that velocity ($U_{x}$) and the
distance between the minimum $C_f$ position and $X_r$. This approach was first suggested by Adams, Johnston & Eaton (1984). The results from these two investigations along with those of Chandra & Bradshaw (1981), Devenport (1985) and Ruderich & Fernholz (1986) are shown in Figure 2.6. The results from all of these workers lie on a line having a slope not far from -0.5, consistent with the idea that the boundary layer has certain laminar-like features. This aspect of the reversed flow will be discussed in more detail in a later section. The results from the work of Castro and Haque (1987) which incorporated free-stream turbulence are also plotted in Figure 2.7. This led the separation bubble to decrease in length by about 15%, but plotting $C_f$ in terms of $U_N$ seems to collapse these results with the measurements made in the flow without free-stream turbulence and also with the measurements of other workers.

Adams Johnston & Eaton (1984) suggest that the absolute value of the reversed-flow skin friction drops by a factor of 50% when the upstream boundary layer undergoes transition. Turbulent boundary layers lead to longer reattachment distances and, from the scaling of Roshko & Lau (1965), weaker pressure gradients. A weaker negative value of $\partial p/\partial x$ would have the effect of decreasing skin friction. Once the boundary layer at separation is turbulent, the effect of changing the boundary layer thickness on the skin friction values within the recirculation was small, but in the recovery region thinner upstream boundary layers lead to thinner downstream boundary layers and thus to higher values of $C_f$.

It was also shown that whilst the mean value of skin friction changes dramatically as transition occurs, the values of $(rms)$ levels of skin friction fluctuations, $c_f'$, change continuously as a function of boundary layer thickness, $(\delta/h)$. Adams, Johnston & Eaton suggest that the reason for this may be a result of the lower levels of $\overline{uv}$ present in the shear layer above the reattachment zone for thicker initial shear layer thicknesses.

The distribution of $c_f'$ found by Adams, Johnston & Eaton with an initial boundary layer that was laminar agrees qualitatively with that found by Ruderich & Fernholz (1986), Dijali & Gartshore (1991),and Hancock (1994). Results taken from Hancock (1994) at various Reynolds numbers are presented in Figure 2.7. The value of $c_f'$ rises steadily from separation to reach a maximum just upstream of reattachment. At reattachment the level is still high and further downstream it slowly decays. Increasing Reynolds number results in a reduction in the $rms$ of the wall shear stress.

The reason for these fluctuations in the skin friction is related to the large structures which are convected in the separated shear layer. As these impinge on the wall in the reattachment region, $X_r$ oscillates widely (Wolf, 1987). Downstream from the reattachment position, $c_f'$
remains high because of the frequent flow reversals (Wolf, 1987). Devenport (1985) found that the flow close to the wall intermittently reversed in direction up to almost two bubble lengths downstream of separation in the flow in a sudden pipe expansion. Ruderich and Fernholz (1986) and Dijali & Gartshore (1991), studied bluff body separations and recorded instantaneous flow reversals to a downstream distance of around 1.5 reattachment lengths.

2.1.4 The Static-Pressure Fluctuations and Pressure Spectrum Beneath a Separation Bubble

The distribution of the $r_{ms}$ of static-pressure fluctuations is qualitatively very similar to the distribution of the level of skin-friction fluctuations. Kiya et al (1991), and Cherry, Hillier & Latour (1984) found that the fluctuations increase from a minimum near separation to a maximum slightly upstream of reattachment.

In order to understand the physical processes underlying the pressure fluctuations, Cherry, Hillier & Latour (1984) combined smoke flow visualisations and instantaneous pressure records taken at the reattachment position. It was found that quasi-periodic bursts of several cycles duration can occur in the surface pressure signal, and that positive departures from the mean at a given location were often accompanied by the visualisation showing that the tapping was located below an inroad of irrotational fluid, between the vortical structure which has just been shed from the bubble and the next, forming in the second half of the reattachment zone. This agrees qualitatively with two-dimensional discrete-vortex simulations of Kiya et al (1982) that show that positive and negative pressure fluctuations correspond to the absence or presence of large-scale vortex clouds, respectively.

Pressure and velocity signals were also recorded together with the pressure transducer at around reattachment and a hot-wire directly above on the edge of the shear layer where the turbulence intensity was 2.5%. The pseudoperiodicity that can be shown by the pressure signal, was also observed in the velocity traces. The two signals were found to be largely in anti-phase as expected for pressure and velocity fluctuations on opposite sides of a vortex structure. However extreme pressure fluctuation did not always correspond to extreme velocity fluctuations. This reflects a random modulation in the vertical location of the effective centre of vorticity: that is, a strong fluctuation in pressure, say, can indicate the presence of a strong disturbance or that it happens to be convected close to the surface (Cherry, Hillier & Latour, 1984). A high correlation between streamwise velocity fluctuations and surface fluctuations cannot always be expected as three-dimensional effects will also be important.
2. Literature Survey

The downstream evolution of the surface pressure spectrum was determined by Kiya et al (1991) and Cherry, Hillier & Latour (1984) and Wolf (1987) so that the frequency of the fluctuations that contain a major part of the energy could be found. All of these workers observed a peak in the spectra close to separation at a frequency \( f_X/U_{ref} \approx 0.1 \) and a further peak at a higher frequency further downstream. The work of Kiya et al, Cherry, Hillier & Latour and Wolf suggested that this latter peak occurred at a frequency \( f_X/U_{ref} \approx 0.6, 0.7, \) and 0.8 respectively. The peak frequency close to separation was attributed to a low frequency flapping motion of the separated shear layer. The second peak, at a frequency \( f_X/U_{ref} \approx 0.6-0.8 \) has also been observed in frequency spectra of the streamwise velocity fluctuations carried out by Dijali & Gartshore (1991) at the outer edge of the shear layer. This frequency reflects the pseudoperiodic formation and shedding of large-scale vortices from the separation bubble already discussed.

Kiya et al (1991) suggest that the large-scale vortex shedding and the low-frequency flapping motion are probably inherent properties of the separation bubbles formed behind a salient edge. However, Ruderich & Fernholz (1986) found no evidence of this low frequency flapping motion when they used the normal fence and splitter plate arrangement.

2.1.5 The Backflow Beneath a Separation Bubble

Flow in the near-wall region is accelerated by the favourable pressure gradient away from the reattachment point towards the step or fence. Near this solid boundary, the pressure gradient is slightly adverse because of the stagnation point created as the flow reaches the wall/step. This then results in reseparation of the flow just ahead of the obstacle.

Workers that have measured velocity close to the wall beneath the separation bubble include: Adams Johnston & Eaton (1984), Adams and Johnston (1988), Ruderich & Fernholz (1986), Ota & Itasaka (1976), Kim, Kline & Johnston (1980), Jaroch & Fernholz (1989), Devenport & Sutton (1991), Chandrsuda & Bradshaw (1981), and Bradshaw & Wong (1972). The maximum reverse flow velocity in the recirculation zone is typically 0.3\( U_{ref} \) in the flow past a thin normal fence with splitter plate, and 0.2\( U_{ref} \) in the flow past a backward-facing step.

When considering the backflow beneath a separation bubble, it is useful to consider a set of coordinates with the origin at the reattachment point and the positive direction pointing towards the fence or wall. This distance from reattachment towards the wall is non-dimensionalised by the reattachment length, giving:
In the favourable pressure gradient region, Adams Johnston & Eaton (1984) found that the distribution of maximum reversed velocity with downstream position followed the power law relationship:

\[ U_N = c(\tilde{X})^m \text{ (with } m=0.7) \]  \hspace{1cm} (2.6)

For this type of velocity variation for the external stream velocity, which occurs when a favourable pressure gradient exists, Falkner & Skan (1930) obtained a solution of the boundary layer equations for steady two-dimensional flow. The Falkner-Skan profile for laminar flow with \( m=0.7 \) is shown in Figure 2.8 along with the experimental results of Adams Johnston & Eaton (1984) at various downstream locations. Given the large amount of scatter in the data, the experimental values fit the Falkner-Skan profile reasonably well, although all of the data except the profile at \( X^*=0.67 \) lie slightly above the laminar profile. The behaviour of this profile would be expected to be different from the others as it is the only profile which occurs in the adverse pressure gradient closer to the fence than reattachment. The degree of collapse of the other profiles would also not be expected to be perfect as the pressure gradient changes with downstream position. However, what these profiles suggest is that the effective Reynolds number of the flow close to the wall is low, and the shear stress, \( \overline{uv} \), is not playing an important part in momentum transfer in this region.

Many workers have also plotted their near wall data using wall coordinates. The results of Adams Johnston & Eaton (1984) are plotted in Figure 2.9, and although not plotted here, the results of Ruderich & Fernholz (1986) and Devenport and Sutton (1991) show the same features. The data of all workers at various downstream positions lie well below the turbulent log law and all data begin to collapse on the \( U^*=y^* \) line beneath the log law region.

Because the law of the wall does not apply to the near wall flow under a separation bubble, attempts have been made to develop near wall models for use in calculation methods. The most successful of these was suggested by Simpson (1983). Simpson proposed that very near the wall, viscosity must have an effect and the relationship

\[ \frac{U}{U_t} = \frac{yU_t}{v} \]  \hspace{1cm} (2.7)
must hold. The sublayer thickness was arbitrarily chosen to be 2\% of the height at which the maximum reversed velocity occurs, \( y_N \). From experimental data, Simpson then observed that the normalised mean velocity, \( U/U_N \), was approximately a function of \( y/y_N \). It is shown that the overlap layer between these two scalings must be independent of viscosity and be logarithmic in \( y/y_N \). The relationship is shown below:

\[
\frac{U}{U_N} = -C \ln \left| \frac{y}{y_N} \right| - D
\] (2.8)

In order to force \( U/U_N \) to be -1 at \( y=y_N \), Simpson modified this to be:

\[
\frac{U}{U_N} = A \left( \frac{y}{y_N} - \ln \left| \frac{y}{y_N} \right| - 1 \right) - 1 \quad \text{(for } y>0.02y_N) \] (2.9)

This equation produces \( U/U_N=-1 \) and \( \partial U/\partial y=0 \) at \( y/y_N =1 \) and the term \( Ay/y_N \) contributes little to \( U/U_N \) near the wall.

The use of only one empirical constant is a striking feature of this equation, and the curve that it represents has been shown by several workers to reasonably describe the mean velocity profiles for \( 0.02<y/y_N<1.0 \) if the constant \( A \) is given the value 0.3. The performance of this near wall model will be assessed in later chapters.

2.1.6 Reynolds Stresses

2.1.6.1 Reynolds Number Effects

Up until 1994, it was assumed by workers in the field of separated flows, that Reynolds stresses normalised by \( U_{ref}^2 \) in the separated shear layer were independent of Reynolds number provided the Reynolds number was high enough. Workers using the normal fence and splitter plate arrangement including Castro & Haque (1987), Ruderich and Fernholz (1986), Jaroch & Fernholz (1984) and Hancock, McCluskey & Castro (1992) produced profiles of the various Reynolds stresses which differed widely when non-dimensionalised using \( U_{ref}^2 \) or \( \Delta U^2 \) (\( \Delta U^2 = (U_{max} - U_{min})^2 \)). In contrast, mean flow aspects of all these measurements were very similar.

This disagreement was initially thought to be associated with measurement techniques or with the different aspect ratios that had been employed in the various investigations. To
resolve some of these questions, Hancock and Castro (1993) carried out a detailed study of the turbulence structure in a nominally two-dimensional separation formed behind a normal fence with splitter plate for a range of aspect ratios. It was found that for aspect ratios of $W/h_f = 20, 40$ and $60$ the vertical profiles of $\frac{u^2}{U_{ref}^2}$ were very similar.

In view of these findings, Hancock (1994) then carried out an investigation of Reynolds number dependence of mean and fluctuating properties. A strong systematic variation of $\frac{u^2}{U_{ref}^2}$ was found, and values of $\frac{u^2_{\text{max}}}{U_{ref}^2}$ near reattachment against Reynolds number are plotted in Figure 2.10. Also plotted in this figure are the results of Ruderich and Fernholz (1986). It can be seen that $\overline{u^2}$ varies particularly strongly at low Reynolds numbers. Hancock also observed that over the lower range of Reynolds number $\overline{u^2}$ varies linearly with $\log(Re_{h_f})$, shown by the solid line in Figure 2.10.

The work by Hancock (1994) has largely explained the differences between workers’ Reynolds stress results. However, it does not explain why $\overline{u^2}$ increases with increasing Reynolds number. This question has yet to be answered, but Hancock suggests that it is associated with the reduced effect of the fluctuating extra strain imposed by the recirculating flow on the underside of the mixing layer at low Reynolds numbers.

2.1.6.2 Downstream Development of Reynolds Stresses

Many authors have made direct comparisons between the plane mixing layer and the shear layer bounding a separation bubble. Indeed, Chandrsuda & Bradshaw (1981), state that 'measurements show that the mixing layer bounding a separation bubble with a thin initial laminar boundary layer is not greatly different from a plane mixing layer'. The Reynolds stress data of Castro & Haque (1987), however, suggest that the turbulence structure of the separated shear layer differs from that of a plane mixing layer in a number of ways.

Castro & Haque and Ruderich & Fernholz normalise their Reynolds stress data using two different scaling parameters. When the free-stream velocity ($U_{ref}$) is used, all curves of the maxima of fluctuating quantities rise in the first half of the separation bubble reaching a maximum approximately in the middle of the reverse-flow region and then fall again in the reattachment region. However, a scaling parameter which is often used for separated flows and plane mixing layers is the velocity difference $\Delta U$ between the maximum velocity in the separated shear layer, $U_{\text{max}}$, and the minimum velocity of the reverse flow, $U_{\text{min}}$. Owing to displacement effects $U_{\text{max}}$ increases over $U_{ref}$, and due to the reverse-flow, $U_{\text{min}}$ reaches
values of about $0.3 U_{ref}$. Hence, using $\Delta U$ to normalise leads to a different distribution of the dimensionless fluctuating quantities, and makes them more directly comparable with those in mixing layers.

Figure 2.11 shows the downstream variation of the maximum values of the normal Reynolds stresses normalised in this way taken from Castro & Haque (1987) and Hancock, McCluskey & Castro (1992). The former used a Reynolds number based on the fence height, $Re_{hf}$, of 11000 and the latter of 1600. Plane mixing layer values are also presented on the left hand side of the figure. At the higher Reynolds number, the maxima rise fairly monotonically with downstream position, reaching a peak value at around the reattachment position. This type of variation is also shown by the Reynolds stress measurements of Ruderich and Femholz (1986) and Kiya & Sasaki (1983) who also used Reynolds numbers in excess of 10000, based on $h_f$ for the work of Ruderich and Femholz, and the thickness of the plate, $h$, for the work of Kiya & Sasaki. In contrast, the results of Hancock, McCluskey & Castro (1992) display stress levels that remain roughly constant along the separation, rising only slightly near the reattachment position. The variation in the downstream development of the Reynolds stresses also appears to be Reynolds number dependent.

In the early part of the shear layer, strong streamline curvature may be expected to reduce turbulent stresses and overall growth rates. Castro and Haque argue that the high values of Reynolds stresses measured in this region of their high Reynolds number flow suggest that there must be another mechanism that augments the stresses. It is suggested that this mechanism may be a low frequency 'flapping' of the shear layer. If a flapping of the shear layer occurred, it may be thought of as the $U(y)$ profile oscillating in the vertical direction and the high gradients of $\partial U/\partial y$ would result in a large increase in axial stress. This 'flapping' argument is also supported by measurements of the growth of the separated shear layer compared to that of the plane mixing layer. The vorticity thickness of the shear layer, $A$, is defined as:

$$A = \frac{1}{d(U/U_{ref}) \left| \frac{dy}{max} \right|}$$ (2.10)

This has been determined by many workers over a wide range of Reynolds numbers, including Castro & Haque (1987), Ruderich & Fernholz (1986), Kiya et al (1982) and Hancock and McCluskey (1997). The results of all these workers are shown in Figure 2.12 and suggest that the growth of $A$ with downstream distance is not linear as it is for the plane mixing layer. The initial growth rate is substantially higher than the plane mixing layer value.
but this rate decreases as reattachment is approached until it is below the rate of the plane mixing layer. After reattachment, the growth rate rapidly increases. The substantially higher vorticity thickness in the early part of the separated shear layer is consistent with the idea of a 'flapping' shear layer in this region.

Castro & Haque also compared profiles of the Reynolds stresses across the separated shear layer with the plane mixing layer. This was done using the mixing layer scaling parameter, \(- (y - y_c) / \Lambda\), where \(y_c\) is the shear layer centreline and \(\Lambda\) is the vorticity thickness of the shear layer. The shear layer centreline is defined as the line along which the mean velocity is \((0.61 \cdot U + U_m)\) and the vorticity thickness is defined in Equation 2.10. Reynolds stresses referred to the local shear layer direction and plotted using this parameter showed that turbulent quantities measured in the high Reynolds number flow of Castro & Haque are very different from plane mixing layer values. Only in the early part of the flow do the stress profiles have shapes even qualitatively similar to those of the corresponding plane mixing layer profiles and even there the behaviour is different near the wall. Although \(u'v'\) profiles are not too far from plane mixing layer values close to separation, the normal stresses are much larger in this region.

Hancock and McCluskey (1997) also compared profiles of Reynolds stresses made at \(x/X = 0.5\) in a separated shear layer and in a plane mixing layer, using \(y_c\) to normalise the height. At the lower Reynolds number, \(Re_{hf}\), of 1600 it was found that the profiles of the normal stresses agreed very well with the plane mixing layer values on the high velocity side, and peak levels were also comparable. However, as with the work carried out at higher Reynolds number, the low velocity side was substantially different, with higher normal stresses occuring below the separated shear layer. In agreement with Castro and Haque’s work, the profiles of \(u'v'\) were reasonably similar for the two flows across the entire shear layer.

Particularly for the peak levels of normal Reynolds stresses, the level of agreement between measurements made in a separated shear layer and in a plane mixing layer seems dependent on the Reynolds number. In contrast, profiles of shear stress, \(u'v'\), within the first half of the separation bubble seem to agree reasonably well for the two flows over a range of Reynolds numbers.

Castro & Haque (1987) also looked at turbulence structure parameters. The low velocity side of the separated shear layer was characterised by much lower values of \(u'v'/\overline{q^2}\) than the plane mixing layer, indicating that the shear stress production is relatively low. A likely cause of
the general reductions in $\bar{uv}/q^2$ is that the separated shear layer entrains highly turbulent fluid on its low velocity side. Although this fluid presumably has significant shear stress, Castro and Haque suggest that it will generally be uncorrelated with the stress in the local turbulent fluid. Hence, the turbulent kinetic energy will be increased substantially whilst the shear stress is not.

2.1.6.3 Effect of Free-stream Turbulence on Reynolds Stresses

The detailed response to free-stream turbulence of the Reynolds stresses in a shear layer bounding a separation region is still being debated. The purpose of the work carried out by Castro & Haque (1988) was to obtain detailed measurements within the separated shear layer so that direct comparisons with data obtained in the case of very low free-stream turbulence could be made.

As has already been mentioned, free-stream turbulence enhances shear layer entrainment rates and reduces the distance to reattachment. However it also significantly modifies the turbulence structure of the shear layer. Axial development of the maximum values of the various Reynolds stresses made in the flow with and without free-stream turbulence, normalised using $(\Delta U)^2$, were found to be qualitatively very similar with the major difference being that the peak value of the normal stress $v^2$ was significantly lower with free-stream turbulence than in its absence. The reasons for this behaviour are not known.

Cross-stream profiles of $u^2$, plotted using the mixing layer scaling parameter, $-(y - y_c)/\Lambda$, at various downstream stations were found to be higher across the whole flow in the presence of free-stream turbulence. This was not the case for the other components on the high velocity side of the flow. Castro & Haque suggest that this could be evidence that free-stream turbulence acts to increase the 'flapping' of the shear layer. As was already mentioned, the effect of this type of motion would be most dominant on the axial component because of the 'shaking' of the mean velocity gradient.
2.1.7 Space-Time Correlations

Autocorrelation measurements have been made at the outer edge of the separated shear by Kiya & Sasaki (1983), and within the shear layer and separation bubble by Castro & Haque (1987). These results suggest that at the centre of the shear layer the velocity timescales in the upstream part of the flow are dominated by lower-frequency components. This very long timescale component is most noticeable just after separation, where its frequency differs most from that corresponding to the size of the vortex structures in the shear layer.

Spanwise correlation measurements have been made at the outer edge of the shear layer using two hot wires by Cherry, Hillier & Latour (1984). Spanwise scale was shown to increase with downstream position up to the reattachment position, beyond which only a much reduced growth remained. At no downstream location did the crossing point of the spanwise cross-correlation coefficient, $R_{uu}$, exceed one reattachment length.

2.1.8 The Flow Downstream of Reattachment

Near wall measurements downstream of reattachment have been made in detail by Bradshaw & Wong (1971), Kim, Kline & Johnston (1980), Chandsuda & Bradshaw (1981) and Jaroch & Fernholz (1989). These results have clearly demonstrated the complicated nature of the flow in the reattachment region and its effect on the slow non-monotonic return of the shear layer to the ordinary boundary-layer state.

Measurements of axial velocity profiles by all of these workers have shown that a marked dip below the universal law of the wall occurs several reattachment lengths downstream of reattachment. Bradshaw & Wong, for example, found that this dip persisted to a distance of about 50 step heights downstream of the step (about 8 reattachment lengths). An explanation for this dip is given by Chandsuda & Bradshaw - the reason is that the existence of the standard logarithmic law is normally proved by assuming the length scale of the flow is proportional to $y$, whilst at and downstream of reattachment, the length scale will be roughly constant, except near the surface. Qualitative use of mixing length arguments shows that a larger length scale implies a smaller velocity gradient for a given shear stress - that is a dip below the logarithmic law.

Adams, Johnston & Eaton (1984) have also shown that the recovery process downstream of reattachment is dependent on flow conditions at separation. The downstream boundary layer thickness resulting from the separation of a thick turbulent boundary layer is thicker than the corresponding thickness shown for laminar initial conditions. The growth rates of the
boundary layers downstream of reattachment were also different among the various initial boundary layers studied. While the laminar case and the thick turbulent case display growth rates which were very similar, the thin turbulent case was found to grow faster indicating a high entrainment rate.

2.2 Swept Separated and Reattaching Flows

An obvious modification to the unswept configuration is to sweep the separation line at some incidence angle. Provided the flow is wide enough, the flow will be two-dimensional but not coplanar because of the existence of the lateral velocity. This type of flow is referred to as spanwise-invariant. There are still many gaps in the understanding of the simpler unswept flow, and consequently there have been very few investigations using the swept configuration. Details of the investigations which have been performed are given in the table below.

<table>
<thead>
<tr>
<th>Worker</th>
<th>Flow Configuration</th>
<th>Sweep Angle</th>
</tr>
</thead>
<tbody>
<tr>
<td>Horton (1968)</td>
<td>swept flat plate with adverse pressure gradient</td>
<td>26.5°</td>
</tr>
<tr>
<td>Selby (1982)</td>
<td>swept backward facing step</td>
<td>0° - 38°</td>
</tr>
<tr>
<td>Wolf (1987)</td>
<td>swept bluff thick plate</td>
<td>0° - 45°</td>
</tr>
<tr>
<td>McCluskey, Hancock &amp; Castro (1991)</td>
<td>swept normal fence with splitter plate</td>
<td>0° - 25°</td>
</tr>
</tbody>
</table>

Table 2.2 Summary of previous investigations of swept separated flows

2.2.1 Surface Streamline Patterns

Experiments to determine the surface streamline pattern beneath the separation bubble using oil/paint mixtures have been performed by all of the above workers. A good example of the results of these experiments is shown in Figure 2.13a, b and c taken from McCluskey, Hancock and Castro (1991). The general effects of increasing sweep angle that are shown here are representative of those displayed by flows using other geometries such as the backward facing step and the bluff thick plate. With increasing sweep angle the effect of the
wind tunnel walls extends further into the flow. The extent of the spanwise invariant region measured parallel with the fence decreases even though the total span of the fence exposed to the flow has increased. This work prompted McCluskey, Hancock and Castro to suggest that for the unswept flow the aspect ratio based on the height of the fence above the splitter plate should be at least 120 for a spanwise invariant pattern to begin to exist. For moderate sweep angles, this value should be even higher.

Wolf (1987) studied swept separation over a range of sweep angles using a bluff plate with an aspect ratio based on plate thickness and model span in the unswept flow of around 50 (this increases with increasing sweep). This work found that the distance from the leading edge to reattachment measured perpendicular to the separation line, $(X_r')$, was almost constant in the spanwise invariant region at sweep angles of 0, 15 and 30 degrees, and slightly reduced at 45 degrees. The work by Selby (1982), that used a swept backward-facing step configuration with an unswept aspect ratio based on step height of 30, found that the reattachment length remained constant up to around 38 degrees. More recent experiments carried out by Fernholz, Janke Kalter and Schober (1993) using a swept backward-facing step geometry with a much larger aspect ratio (100 based on the step height and the model span in the unswept flow) also showed that $X_r'$ remained constant up to around 40 degrees.

McCluskey, Hancock & Castro observed an approximately constant reattachment length for sweep angles of 0, 10 and 25 degrees but also noticed that the distance to the secondary separation increased slightly at sweep angles greater than 10°. The limiting streamlines of this secondary separation were also repeatedly observed to be closely parallel to the fence, rather than towards the secondary separation line as might be expected, suggesting that the motion in the direction normal to the fence is very weak.

2.2.2 The Static Pressure Distribution Beneath the Swept Separation Bubble

If the pressure coefficient that was used for the unswept flow (Equation 2.1) is also used for the swept flow, then the effect of increasing the sweep angle on the static pressure distribution can be seen in Figure 2.14 (taken from Wolf, 1987, with $x'$ being the chordwise position). With increasing sweep, the minimum pressure coefficient becomes less negative and the position at which it occurs moves slightly towards reattachment. The pressure recovery also becomes less steep, and the reattachment takes place at a higher pressure with increasing sweep.
However workers studying swept flow have also normalised the pressure measurements beneath the separation bubble using the free-stream velocity component normal to the separation line.

\[ C_p = \frac{p - p_{ref}}{0.5\rho(U_{ref}\cos\alpha)^2} \]  

(2.11)

Using this coefficient, the results from Wolf (1987) collapse very well at least up to 30 degrees as shown in Figure 2.15. The reason for the slight departure of the profile at 45 degrees is because no spanwise invariant region existed at this sweep angle.

### 2.2.3 The Static-Pressure Fluctuations and Pressure Spectrum Beneath a Separation Bubble

The chordwise distribution of the \( rms \) value of the static pressure fluctuations, normalised using the free-stream velocity component normal to the separation line is virtually identical for the various sweep angles considered by Wolf (1987). As with the mean pressure distributions, the only slight departure occurs at the highest sweep angle where the flow is influenced by the wind tunnel wall. The fluctuations increase from a minimum near separation to a maximum slightly upstream of reattachment (about 0.85\( X_r' \)).

Spectra of the static-pressure fluctuations are also presented by Wolf (1987) at various chordwise positions for the swept cases. The corresponding spectra for the unswept flow are also presented for comparison purposes. The low frequency contribution which has been shown by many workers to feature in the unswept flow is also evident in all of the swept cases. Interestingly, the non-dimensional frequency, \( fX_r'/U_{ref} \), at which this occurred increased from around 0.1 for the unswept flow (agreeing with Kiya et al (1991) and Cherry, Hillier & Latour (1984)) to 0.33 for the 45 degree sweep. Further downstream close to reattachment, the peak that has been observed at higher frequencies in the unswept flow is also present for the swept cases. Unlike the low frequency contribution, the frequency at which this higher component occurs stays constant at around \( fX_r'/U_{ref}=0.8 \) for sweep angles up to 45 degrees.
2. Literature Survey

2.2.4 Skin Friction Measurements on the Plate Beneath the Swept Separation Bubble

As far as the author of this literature survey is aware, Wolf (1987) and Femholz, Janke Kalter and Schober (1993), are the only workers to have published skin friction measurements beneath a swept separation bubble. Wolf found that the magnitude of the skin-friction coefficient at a given chordwise position beneath the separation bubble rose gradually with increasing sweep angle and so did the magnitude at reattachment, clearly showing the presence of spanwise flow along the reattachment line. At 15° sweep, the magnitude of $C_f$ was still increasing steeply at the most downstream measuring station, $2X_r$. At 30° sweep angle the skin friction had almost reached a constant level at the same station, and at 45°, the distribution showed a maximum at about 1.5$X_r$ from separation and began to decrease further downstream.

The skin friction component normal to the leading edge, $C_{f_n}$, based upon the free-stream velocity component normal to the leading edge was plotted against chordwise location, $x'/X_r$, by both sets of workers.

$$C_{f_n} = \frac{\tau_{wx}}{0.5\rho(U_{ref}\cos\alpha)^2}$$  \hspace{1cm} (2.12)

In both cases it was found that the profiles of skin friction plotted in this way collapse very well with the unswept results up to a sweep angle of 30 degrees. Profiles from Wolf (1987) are shown in Figure 2.16. Both investigations found that the spanwise component of skin friction increased with downstream position inside the bubble, reaching a maximum value somewhere around the reattachment position. $C_{f_n}$ also rose with increasing sweep angle at a given chordwise position.

2.2.5 Mean Velocity Measurements

Measurements of mean velocity inside a swept separation bubble in the directions perpendicular and parallel to the separation line are presented by Wolf (1987), McCluskey, Hancock & Castro (1991) and Hancock & McCluskey (1997). Measurements made by the former at sweep angles of 0, 15, 30 and 45 degrees have shown that vertical profiles of chordwise velocity normalised by the local external velocity component in the chordwise direction are invariant with sweep angle. The maximum velocity component towards the leading edge at each angle of sweep was found to occur about half-way along the separation bubble and was about $0.3U_o$, where $U_o=U_{ref}\cos\alpha$. 

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Measurements of velocity profiles parallel with the fence made by these sets of workers have shown that for a given sweep angle, the spanwise velocity is almost constant with vertical and downstream position except close to the surface where the velocity falls to zero. Thus the bulk of the fluid is convected with a roughly uniform velocity, \( W_0 \). \((W_0 = U_{ref} \sin \alpha)\). Furthermore, Hancock & McCluskey showed that when the chordwise velocity, \( U' \), was plotted against the spanwise velocity, \( W' \), in a polar plot, it can be seen that in the outer part of the flow, not only does \( W' \) vary linearly with \( U' \) for a given vertical profile, but also these variations at different downstream locations coalesce. It was shown that \( U' \) and \( W' \) vary according to a 'velocity triangle' originally observed for three-dimensional boundary layers.

The same results were also presented in terms of the variation of flow direction with vertical position, \( y \). The vertical height was non-dimensionalised by \( y_c \), which for swept flows is defined as \( U' = (0.67 \Delta U' + U'_{min}) \). Hancock & Mccluskey identified three regions: an inner region where the flow angle is roughly constant at the local wall value, a middle region where the flow direction changes substantially and an outer region where the direction is almost constant, this time in the free-stream direction. The extents of the inner, middle and outer regions are roughly \( 0 < y < 0.2y_c \), \( 0.2y_c < y < y_c \) and \( y > y_c \), respectively.

### 2.2.6 Reynolds Stress Measurements

The investigation carried out by McCluskey, Hancock and Castro considered the unswept fence and a 25 degree swept fence. This study used the pulsed-wire technique which meant that Reynolds stress measurements could be made. The Reynolds stresses were presented using the \( x' \)-\( z' \) coordinate system, where the \( x' \) and \( z' \) axes are aligned perpendicular and parallel to the fence, respectively. Comparisons of maximum values of \( u'^2 \), \( w'^2 \) and \( u'v' \) normalised by \((AU')^2\), where \( U' \) is the velocity perpendicular to the fence, show that there was a striking lack of effect of cross-flow on the stress levels. Normalised by, say, the velocity upstream of the fence, the stress levels are substantially less in the swept case, suggesting that the cross-flow is relatively unimportant over most of the separation. It is also suggested in this report that the \( v'^2 \) measurements in the swept flow, which do not agree well with those made in the unswept flow, may be in error because of the limited response cone of the probe. Since that report was written, it has been established that measurements of \( v'^2 \) that involve using the sensor wires lying at different heights within a boundary layer, as was done during the investigation by McCluskey, Hancock and Castro, can also lead to serious errors in measurements of Reynolds stresses.
Mean velocity measurements from this investigation showed that $\frac{dW}{dy}$ was small compared to $\frac{dU}{dy}$ except very close to the splitter plate. This perhaps explains why the turbulence structure of the two types of flow are comparable further out. Close to the wall, where measurements have not previously been made because of probe size, it is expected that the turbulence structure in the swept and unswept flow will be different because of the effect of this extra strain rate on the generation terms. During this investigation, it is hoped to test this theory by making mean and fluctuating measurements close to the splitter plate using a through-wall pulsed-wire probe.
Fig. 2.1 Static pressure distribution on the splitter plate taken from Castro & Haque (1987)

Fig. 2.2 Static pressure distribution on the splitter plate using coefficient suggested by Roshko & Lau (1965)
Fig. 2.3 Effect of blockage and aspect ratio on $X_r$

Fig. 2.4 Effect of blockage and aspect ratio on $X_r$ (author's interpretation)
Fig. 2.5 Skin friction profiles taken from Hancock (1994)

Fig. 2.6 Wall-friction in the second half of the reversed-flow region
Fig. 2.7 RMS of wall shear stress fluctuation taken from Hancock (1994)
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![Graph showing near-wall velocity profiles taken from Adams et al (1984)](image1)

**Fig. 2.8** Near-wall velocity profiles taken from Adams et al (1984)

![Graph showing near-wall velocity profiles in wall coordinates taken from Adams et al (1984)](image2)

**Fig. 2.9** Near-wall velocity profiles in wall coordinates taken from Adams et al (1984)
2. Literature Survey

Fig. 2.10 Variation of maximum axial stress near attachment with Reynolds number

Fig. 2.11 Maximum normal Reynolds stresses
Fig. 2.12 Development of the vorticity thickness with downstream position
Fig. 2.13 Surface streamline pattern for a) unswept flow b) 10° swept flow c) 25° swept flow (taken from Hancock, McCluskey & Castro, 1992). F and S denote focal point and saddle point singularities, respectively.
Fig. 2.14 Profiles of $C_p$ against chordwise position for various sweep angles.

Fig. 2.15 Profiles of $C_p'$ against chordwise position for various sweep angles.
Fig. 2.16 Variation of chordwise skin friction coefficient based on the velocity normal to the separation line at various sweep angles (Wolf, 1987)
3. Governing Equations and Some Aspects of Modelling

3.1 The Governing Equations

3.1.1 The Continuity and Momentum Equations

The discussion included in this chapter is only intended to assist the discussion sections of the present thesis. For a more detailed description of the equations of motion see, for example, Townsend (1976).

For any constant density flow through a control volume, the mass flow in must equal the mass flow out. By summing the net mass flow out of such a control volume and equating it to zero the continuity equation can be obtained and is shown below in tensor notation:

\[ \frac{\partial U_i}{\partial x_i} = 0 \]  \hspace{1cm} (3.1)

By consideration of all the forces acting on the control volume, and momentum flux into and out of the control volume, the Navier-Stokes momentum equations are derived. Performing a Reynolds decomposition and time averaging these equations produces the Reynolds-averaged momentum equation shown below:

\[ \frac{\partial U_i}{\partial t} + U_j \frac{\partial U_i}{\partial x_j} = -\frac{1}{\rho} \frac{\partial p}{\partial x_i} + \nu \frac{\partial^2 U_i}{\partial x_j^2} - \frac{\partial \bar{u}_i u_j}{\partial x_j} \]  \hspace{1cm} (3.2)

where \( \bar{u}_i u_j \) are the Reynolds stresses.

3.1.2 The Reynolds Stress Transport Equations

The Reynolds stress transport equations are derived from the Navier-Stokes equations. The transport equation for \( \bar{u}_i u_j \) along with interpretation of the various terms is given overleaf:
3. Governing Equations and Some Aspects of Modelling

\[
\frac{\partial}{\partial t} \bar{u}_j u_j + U_k \frac{\partial}{\partial x_k} (\bar{u}_j u_j) = \left( \frac{\partial}{\partial x_k} \bar{u}_j \frac{\partial U_i}{\partial x_k} + \frac{\partial}{\partial x_k} \bar{u}_i \frac{\partial U_j}{\partial x_k} \right)
\]

\[
- \frac{\partial}{\partial x_k} \left[ \bar{u}_j \bar{u}_k - \nu \frac{\partial}{\partial x_k} (\bar{u}_j u_j) + \frac{\rho}{\rho} (\bar{u}_i \delta_{jk} + \bar{u}_j \delta_{ik}) \right]
\]

\[
+ \frac{\rho}{\rho} \left( \frac{\partial \bar{u}_i}{\partial x_j} + \frac{\partial \bar{u}_j}{\partial x_i} \right) - 2 \nu \frac{\partial \bar{u}_i}{\partial x_k} \frac{\partial \bar{u}_j}{\partial x_k}
\]

1. Rate of change of \(\bar{u}_j u_j\) with time + advection by the mean flow.
2. Rate of generation of \(\bar{u}_j u_j\) by the effects of mean strain. Reynolds stress is generated by interaction of the existing Reynolds stresses with the mean rates of strain. It is this mechanism that transfers energy from the mean flow to the turbulence.
3. Diffusion of \(\bar{u}_j u_j\) in three components:
   (a) 'turbulent' diffusion involving triple products.
   (b) 'viscous' diffusion.
   (c) 'pressure' diffusion.
   These are spatial re-distribution terms. Turbulent diffusion represents transport of the Reynolds stresses by the turbulence itself and pressure diffusion represents transport by the fluctuating pressure. Viscous diffusion is negligible except very close to a solid surface or in certain flows at low Reynolds number.
4. The pressure-strain correlation. These terms are the product of fluctuating pressure and the fluctuating rate of strain. Physically, these terms represent the redistribution, by pressure fluctuations, of energy amongst the Reynolds normal stresses so as to make them more isotropic and to reduce the Reynolds shear stresses.
5. Viscous destruction of \(\bar{u}_j u_j\). This term is equal to the rate at which the fluctuating rates of strain in the turbulence do work against fluctuating viscous stresses. In this way, turbulent energy is transferred to thermal internal energy.
When the three equations for the normal stresses are summed and the result is divided by two, the transport equation for the turbulent kinetic energy is formed. The pressure strain term discussed above sums to zero leaving:

\[
\frac{\partial k}{\partial t} + U_k \frac{\partial k}{\partial x_k} = -\frac{\partial U_i}{\partial x_k} \frac{\partial U_i}{\partial x_k} - \frac{\partial}{\partial x_k} \left( \frac{1}{2} \overline{u_i u_i} - \nu \frac{\partial k}{\partial x_k} + \frac{\rho}{\partial x_k} \right) - \frac{\partial u_i}{\partial x_k} \frac{\partial u_i}{\partial x_k}
\]

(3.4)

where \( k = \frac{1}{2} q^2 = \frac{1}{2} \overline{u_i u_i} \) (summed) and the terms described briefly are:

1. Rate of change of \( k \)
2. Rate of production of \( k \) by interaction with the mean strain rates.
3. Diffusion of \( k \) in three components:
   (a) 'turbulent' diffusion involving triple products.
   (b) 'viscous' diffusion.
   (c) 'pressure' diffusion.
4. Viscous distinction of \( k \) - symbol \( \epsilon \) (dissipation)

Ideally, all these terms would be measured. However, in order to measure turbulent diffusion it is necessary to determine gradients involving triple products. Due to the scatter in the third order moments, this was not possible in the present investigation. Also, no measurements of the rate of dissipation, \( \epsilon \), were made and since the turbulent diffusion terms could not be calculated, the method of determining dissipation from the out of balance term in the energy budget was not possible. Consequently, the physics of the flows in the present thesis have largely been investigated by considering the production terms of the transport equations. For ease of discussion in the later chapters the following table displays the production terms of Reynolds normal and shear stresses measured in the present investigation in their fully expanded state. The full production term for the turbulent kinetic energy transport equation is also included.
3. Governing Equations and Some Aspects of Modelling

Reynolds Stress | Production Term
--- | ---
\( \overline{u^2} \) | \( 2 \left( -u^2 \frac{\partial U}{\partial x} - uv \frac{\partial U}{\partial y} - uw \frac{\partial U}{\partial z} \right) \)

\( \overline{v^2} \) | \( 2 \left( -uv \frac{\partial V}{\partial x} - v^2 \frac{\partial V}{\partial y} - vw \frac{\partial V}{\partial z} \right) \)

\( \overline{w^2} \) | \( 2 \left( -uw \frac{\partial W}{\partial x} - vw \frac{\partial W}{\partial y} - w^2 \frac{\partial W}{\partial z} \right) \)

\( \overline{uv} \) | \( -u^2 \frac{\partial V}{\partial x} - v^2 \frac{\partial U}{\partial y} - uv \left( \frac{\partial U}{\partial x} + \frac{\partial V}{\partial y} \right) - uw \frac{\partial V}{\partial z} - vw \frac{\partial U}{\partial z} \)

\( \overline{uw} \) | \( -u^2 \frac{\partial W}{\partial x} - w^2 \frac{\partial U}{\partial z} - uw \left( \frac{\partial U}{\partial x} + \frac{\partial W}{\partial z} \right) - uv \frac{\partial W}{\partial y} - vw \frac{\partial U}{\partial y} \)

\( \overline{vw} \) | \( -v^2 \frac{\partial W}{\partial y} - w^2 \frac{\partial V}{\partial z} - vw \left( \frac{\partial V}{\partial y} + \frac{\partial W}{\partial z} \right) - uv \frac{\partial V}{\partial x} - uv \frac{\partial W}{\partial x} \)

\( k \) | \( -u^2 \frac{\partial U}{\partial x} - v^2 \frac{\partial V}{\partial y} - w^2 \frac{\partial W}{\partial z} - uv \left( \frac{\partial U}{\partial x} + \frac{\partial V}{\partial y} \right) - uw \left( \frac{\partial U}{\partial z} + \frac{\partial W}{\partial z} \right) - vw \left( \frac{\partial V}{\partial z} + \frac{\partial W}{\partial z} \right) \)

Table 3.1 Generation terms of the measured Reynolds normal and shear stresses

The swept geometry used in the present investigation has led to the introduction of different sets of ‘extra’ strain rates in different regions of the flow as shown in Figure 1.2. Some of these strain rates are known to have an unexpectedly large influence on the turbulence structure. This feature is examined in greater detail in the following section.

3.2 Effects of Extra Rates of Strain

It is well known that certain strain rates other than the principal shear, \( \partial U / \partial y \), can have a large effect on the turbulence structure of a shear layer. For example, streamline curvature, \( \partial V / \partial x \), has the effect of either stabilising the flow (reducing turbulent kinetic energy) or destabilising the flow (increasing turbulent kinetic energy) depending on the orientation of the streamline curvature. If it is convex with respect to the low-velocity side it is stabilising, and if it is concave with respect to the low-velocity side it is destabilising. Consequently, the separated shear layer resulting from the normal fence with splitter plate, can be viewed as a mixing layer subjected to stabilising streamline curvature as well as fluctuating strain by the recirculating flow. Laterally divergent and convergent shear layers exhibit similar
intensifying and suppressing effects, rather like the destabilising and stabilising effects of
curvature.

If, for example, a thin shear layer is considered and an ‘eddy viscosity’ formula of the type

\[ \overline{uv} = -\nu, \frac{\partial U}{\partial y} \]  

(3.5)

is used to predict the effect of adding an extra rate of strain \( \partial V/\partial x \) to the existing ‘simple
shear’ \( \partial U/\partial y \), the increase in shear stress is given by:

\[ 1 + \frac{\partial V/\partial x}{\partial U/\partial y} \]  

(3.6)

In fact it has been found that the real factor of increase after a prolonged region of streamline
curvature is much larger, of order:

\[ 1 + 10 \frac{\partial V/\partial x}{\partial U/\partial y} \]  

(3.7)

for small values of the ‘rate of strain ratio’ \( (\partial V/\partial x)(\partial U/\partial y) \). More generally, since it is
known that other strain rates such as \( \partial W/\partial z \) can have a large effect on turbulence structure,
Bradshaw (1973) suggests that a simple formula for qualitatively predicting the effects of
weak extra strain rates on turbulence quantities is:

\[ F = 1 + \lambda \frac{e}{\partial U/\partial y} \]  

(3.8)

where \( e \) is any mean strain rate other than \( \partial U/\partial y \) and \( \lambda \) is the ‘unexpected largeness’ of the
effect of extra strain rates and is of the order 10.

3.3 Computational Models

3.3.1 Standard \( k-\varepsilon \) Model

The Reynolds stresses in the Reynolds averaged momentum equation of Equation 3.2 are
modelled using Boussinesq’s eddy viscosity assumption, which expresses the Reynolds stresses in terms of mean strain rates:
3. Governing Equations and Some Aspects of Modelling

\[
\overline{u_i u_j} = -v_t \left( \frac{\partial U_i}{\partial x_j} + \frac{\partial U_j}{\partial x_i} \right) + \frac{2}{3} \delta_{jk} k
\]  

(3.9)

where the eddy viscosity,

\[
v_t = C_\mu \frac{k^2}{\epsilon}
\]  

(3.10)

is determined using the turbulent kinetic energy and the dissipation rate which are found from modelled transport equations which can be written as:

\[
\frac{Dk}{Dt} = \frac{\partial}{\partial x_j} \left( \left( v + \frac{v_t}{\sigma_k} \right) \frac{\partial k}{\partial x_j} \right) + P_k - \epsilon
\]  

(3.11)

\[
\frac{De}{Dt} = \frac{\partial}{\partial x_j} \left( \left( v + \frac{v_t}{\sigma_\epsilon} \right) \frac{\partial \epsilon}{\partial x_j} \right) + C_{e1} \frac{\epsilon}{k} P_k - C_{e2} \frac{\epsilon^2}{k}
\]  

(3.12)

where,

\[
P_k = -u_i u_j \frac{\partial U_i}{\partial x_j} = \nu_{\text{eff}} \left( \frac{\partial U_i}{\partial x_j} + \frac{\partial U_j}{\partial x_i} \right) \frac{\partial U_i}{\partial x_j}
\]  

(3.13)

\(\nu_{\text{eff}}\) is the effective viscosity defined by:

\[
\nu_{\text{eff}} = \nu + v_t
\]

The values of the empirical constants appearing in Equations 3.11-3.13 are given below:

\[
\begin{align*}
C_\mu &= 0.09, \quad C_{e1} = 1.44, \quad C_{e2} = 1.92, \quad \sigma_k = 1.0, \quad \sigma_\epsilon = \frac{k^2}{(C_{e2} - C_{e1})\sqrt{C_\mu}}
\end{align*}
\]

When the standard \(k-\epsilon\) model is used, the boundary condition for a wall is provided using the logarithmic law of the wall. This will be discussed in section 3.3.4.
3.3.2 Near-Wall (Low Reynolds Number) $k$-$\varepsilon$ Model

Near-wall models use the viscous sub-layer law to provide the wall boundary conditions, rather than the more limited logarithmic law of the wall which ceases to exist in strongly perturbed flows, for example. These models are also called low Reynolds number models because they introduce a Reynolds number dependence. This dependence asymptotes to the standard $k$-$\varepsilon$ model, which is independent of Reynolds number, as the Reynolds number increases.

Although several near-wall models have been proposed (a review is given by Patel, 1985), the only option within CFX-F3D was the model of Launder & Sharma (1974). This involves a damping of the eddy viscosity when the turbulent Reynolds number is low, a modified definition of $\varepsilon$ so that it goes to zero at walls and modifications of the source terms in the $\varepsilon$ equation. Equations 3.10, 3.11 and 3.12 describing the standard $k$-$\varepsilon$ model become:

modified eddy viscosity:

$$ v_t = C_\mu f_\mu \frac{k^2}{\varepsilon} \quad (3.14) $$

modified $k$-transport equation:

$$ \frac{Dk}{Dt} = \frac{\partial}{\partial x_j} \left[ \left( v + \frac{v_l}{\sigma_k} \right) \frac{\partial k}{\partial x_j} \right] + P_k - \varepsilon - D \quad (3.15) $$

modified $\varepsilon$-transport equation:

$$ \frac{D\varepsilon}{Dt} = \frac{\partial}{\partial x_j} \left[ \left( v + \frac{v_l}{\sigma_\varepsilon} \right) \frac{\partial \varepsilon}{\partial x_j} \right] + C_{\varepsilon 1} \frac{\varepsilon}{k} P_k - C_{\varepsilon 2} f_2 \frac{\varepsilon^2}{k} + E \quad (3.16) $$

The functions $f_\mu$, $f_2$, $D$ and $E$ are defined by:

$$ f_\mu = \exp \left( \frac{-3.4}{\left( 1 + \frac{R_e}{50} \right)^2} \right) \quad (3.17) $$
3. Governing Equations and Some Aspects of Modelling

\[ f_2 = 1 - 0.3 \exp(-R_t^2) \]  
(3.18)

\[ D = 2v \left( \frac{\partial^2}{\partial x_i^2} \right)^2 \]  
(3.19)

\[ E = 2v\nu_i \left( \frac{\partial^2 U_i}{\partial x_j \partial x_k} \right) \left( \frac{\partial^2 U_j}{\partial x_i \partial x_k} \right) \]  
(3.20)

where the local Reynolds number, \( R_t \), is defined by

\[ R_t = \frac{\rho k^2}{\mu \epsilon} \]  
(3.21)

### 3.3.3 Reynolds Stress Model

In Reynolds stress turbulence models, the eddy viscosity hypothesis is not invoked. Instead, transport equations for the individual Reynolds stresses which appear in the Reynolds averaged momentum equation (Equation 3.2) are solved. The transport equation for \( \overline{u_i u_j} \) is given in Equation 3.2, along with an interpretation of the various terms: of these terms, 3a, 3c, 4 and 5 must be modelled, and the way in which this is carried out in CFX-F3D is explained below.

#### 3.3.3.1 Modelling the Pressure Strain Correlation

The pressure-strain term, \( \phi_{ij} \), of the Reynolds stress model without any corrections for the effect of solid boundaries on turbulence structure, is written as the sum of two parts; a 'slow' return to isotropy term, \( \phi_{ij,1} \) (Rotta, 1972) and a 'rapid' term that tends to isotropise the turbulence production tensor, \( \phi_{ij,2} \) (Naot et al, 1970).

\[ \phi_{ij} = \phi_{ij,1} + \phi_{ij,2} \]  
(3.22)

In this expression:

\[ \phi_{ij,1} = -C_{ij} \frac{\epsilon}{k} \left( \overline{u_i u_j} - \frac{2}{3} \delta_{ij} \overline{\epsilon} \right) \]  
(3.23)
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\[ \phi_{ij,2} = -C_{2s} \left( P_{ij} - \frac{2}{3} \delta_{ij} P_k \right) \]  
\[ (3.24) \]

where

\[ P_{ij} = \left\{ \frac{u_i u_k}{\partial x_k} + \frac{u_j u_k}{\partial x_k} \right\} \]  
\[ (3.25) \]

\[ P_k = -u_i u_j \frac{\partial U_i}{\partial x_j} \]  
\[ (3.26) \]

and

\{C_{1s} = 1.8, \, C_{2s} = 0.6\}

3.3.3.2 Modelling Turbulent Diffusion

For the turbulent diffusion term, CFX-F3D uses the assumption that the rate of transport of Reynolds stresses by diffusion is proportional to the gradients of Reynolds stresses. The gradient-type model suggested by Daly and Harlow, 1970 is used:

\[ -u_i u_j u_k = C_s \frac{k}{\varepsilon} u_i u_j \frac{\partial U_i}{\partial x_i} \]  
\[ (3.27) \]

where

\{C_s = 0.22\}

3.3.3.3 Modelling Dissipation

The exact equation for the dissipation, \( \varepsilon \), contains a number of complex correlations, for which assumptions need to be made. Using a similar model for the diffusion term as in Equation 3.25, CFX-F3D uses an \( \varepsilon \)-equation of the following form:

\[ \frac{D\varepsilon}{Dt} = \frac{\partial}{\partial x_i} \left\{ \frac{C_s \frac{k}{\varepsilon} u_i u_j}{\sigma_e \varepsilon} \frac{\partial \varepsilon}{\partial x_j} \right\} + C_{\varepsilon 1} \frac{\varepsilon}{k} u_i u_j \frac{\partial U_i}{\partial x_j} - C_{\varepsilon 2} \frac{\varepsilon^2}{k} \]  
\[ (3.28) \]
where all constants have the same values as in the \( \varepsilon \)-equation used for the \( k-\varepsilon \) model.

### 3.3.4 Boundary Conditions at Wall for \( k-\varepsilon \) Model and Reynolds Stress Model

When the near-wall model of Launder & Sharma (1974) is implemented, the equations are integrated to the wall through the laminar sublayer. The standard no-slip conditions with zero values of \( k \) and \( \varepsilon \) are therefore used. The standard \( k-\varepsilon \) model and the Reynolds stress model avoid the need to integrate the model equations right down to the wall by making use of the universal behaviour of near-wall flows at high Reynolds number. This approach assumes that, at a point with wall distance, \( y_t \), just outside the viscous sublayer, the velocity components parallel to the wall follow the logarithmic law of the wall and the turbulence is in local equilibrium, so that the production, \( P_k \), is equal to dissipation, \( \varepsilon \). With these assumptions, the resultant velocity parallel to the wall, \( U_t \), the kinetic energy, \( k_t \), and the dissipation rate, \( \varepsilon_t \), at point \( y_t \) are related to the resultant friction velocity, \( U_f \), by the following relations:

\[
\frac{U_t}{U_f} = \frac{1}{\kappa} \ln \left( \frac{U_f y_t}{v} \right)
\]

\[
k_t = \frac{U_f^2}{\sqrt{C_\mu}}
\]

\[
\varepsilon_t = \frac{U_f^3}{k y_t}
\]

where

\[ \{ E = 9.793, \ \kappa = 0.4187 \} \]

When using the standard \( k-\varepsilon \) model, the equation for the turbulent kinetic energy is solved in the control volume immediately adjacent to the wall. From this the value of \( U_f \) may be obtained. When the Reynolds stress model is used, \( U_f \) is implied from the near-wall values of \( \overline{uv} \). For the Reynolds stress model, near-wall values of the Reynolds stresses are
specified in terms of proportions of $k$. Gibson & Launder (1978) propose the following ratios:

$$\frac{\overline{u^2}}{k} = 1.1, \quad \frac{\overline{v^2}}{k} = 0.25, \quad \frac{\overline{w^2}}{k} = 0.65, \quad \frac{\overline{uv}}{k} = 0.26$$

### 3.3.5 Further Modelling Considerations

#### 3.3.5.1 Modification to Dissipation Equation of $k$-$\varepsilon$ Model to Allow for Excessive Production Upstream of Bluff Bodies

Computations by Djalili et al (1991) and Apsley (1994) over a bluff thick plate and by Atkinson et al (1991) over a normal fence with splitter plate using the standard $k$-$\varepsilon$ model show a large amount of turbulence on the front face. Downstream of separation the predicted stress levels are much higher than measured, and the distance to reattachment consequently much shorter. The high level of turbulence on the front face is a consequence of imposing a log law to this surface in conjunction with the eddy viscosity assumption. These imply that within the log law region:

$$\nu_t = \kappa y u_t$$

and if a simple shear layer is considered, the production of turbulence, $P_k$, is given by:

$$P_k = \kappa y u_t \left( \frac{\partial U}{\partial y} \right)^2$$

which means that production within the log law is inevitably implied.

Previous workers have tried to suppress this spurious levels of turbulence on the front face by increasing the level of dissipation in this region. This has often been done by implementing modifications to the $\varepsilon$ equation proposed by Hanjalic and Launder (1980) for strongly accelerated flows. It was developed as a result of the observation that the decay by dissipation of grid turbulence passed through a contraction is much higher than without the contraction. As there are only normal strains acting in the flow away from the walls, and since this phenomenon has not been observed when the turbulence was subjected to shear, Hanjalic and Launder concluded that normal strains are more effective than shear strains in
promoting energy transfer from large to small scales, and thus the rate of dissipation (Leschziner & Rodi, 1981).

The modification adopted by workers such as Apsley (1994) and Leschziner & Rodi (1981) involves a replacement of the "production of dissipation" term by the expression

\[ P_e = \frac{\varepsilon C_p {\bar{p}}}{k} \]  

(3.34)

by the expression

\[ P_e = \frac{\varepsilon}{k} \left[ C_p {\bar{p}} - C_{\varepsilon} \nu_s S_{ns}^2 \right] \]  

(3.35)

where \( C_p - C_{\varepsilon} = C_1 \) (\( C_1 = 1.44 \), \( C_1 = 2.24 \) and \( C_1 = 0.8 \)) and the dissipation is preferentially increased in response to normal strains if \( C_{\varepsilon} > C_1 \). \( S_{ns} \) is the shear strain in the direction of the mean streamline.

The calculation of Apsley (1994) was for a separation generated by a bluff rectangular plate of thickness, \( h \), and it was found that introducing the modification to the dissipation equation lead to a reattachment length of 5.35\( h \). The standard model returned a reattachment length that was less than half of this value (2.25\( h \)) and experimental work carried out using the same blockage ratio suggested a length of 4.7\( h \). Furthermore, the unphysical levels of turbulence occurring on the upstream side of the plate, particularly close to the separating edge, were totally removed using the modified model. The modification was also adopted in the work of Djilali, Gartshore & Salcudean (1991), who also considered the bluff rectangular plate and found that the modified model gave a reattachment length that was only 10% less than the experimentally observed value of 4.7\( h \).

### 3.3.5.2 Streamline Curvature Corrections for \( k-\varepsilon \) Model

Computational studies such as Launder, Priddin & Sharma (1977) have shown that the sensitivity of the turbulence structure to streamline curvature is not reproduced if the standard \( k-\varepsilon \) model is used. Modifications to sensitise the standard model to curvature effects have appeared in different forms. Simple models used empirical relations developed by Bradshaw (1973) to operate on the eddy viscosity, such as that shown in Equation 3.8. This type of
correction has been applied successfully to certain flows, but the value of \( \lambda \) is not constant, and so the modification is not really universal.

A more universal curvature correction has been used by Leschziner & Rodi (1981) and Apsley (1994), and is based on the algebraic stress model of Gibson (1978). A set of algebraic equations governing the transport of the Reynolds stresses are used, omitting the convection and diffusion terms. This omission reflects the assumption of local stress equilibrium. On the assumption of local equilibrium of turbulence energy, \( P_k = \epsilon \), these equations simplify to:

\[
\frac{\overline{u_iu_j}}{k} = \frac{1 - \zeta}{\omega\epsilon} P_{ij} - \frac{2}{3\alpha} \delta_{ij} (1 - \omega - \zeta)
\]  

(3.36)

where \( \omega \) and \( \zeta \) are constants having values 1.5 and 0.6, respectively. If this equation is re-written in streamline (s, n) coordinates, three equations are formed for \( P_n \), \( P_s \) and \( P_{ns} \) from which the stresses \( \overline{u_n^2} \), \( \overline{u_s^2} \) and \( \overline{u_nu_s} \) can be evaluated provided the mean flow, \( k \) and \( \epsilon \) are known (s is tangential to the local streamline and n is normal to it). However, it is only the shear stress \( \overline{u_nu_s} \) that plays an important role in the momentum equations in a curved shear layer. Consequently, Leschziner & Rodi (1981) combine the three equations for \( P_n \), \( P_s \) and \( P_{ns} \) with the equation above, and using certain simplifications produced an equation relating \( \overline{u_nu_s} \) to the respective rate of strain \( (\partial U_s / \partial n + U_s / R_c) \):

\[
-u_nu_n = -\frac{K_1K_2}{1 + 8K_1^2 \frac{k^2}{\epsilon^2} \left( \frac{\partial U_s}{\partial n} + \frac{U_s}{R_c} \right) R_c} \frac{k^2}{\epsilon} \left( \frac{\partial U_s}{\partial n} + \frac{U_s}{R_c} \right)
\]  

(3.37)

where

\[
K_1 = \frac{1 - \zeta}{\omega}, \quad K_2 = \frac{2}{3} \frac{(1 - \omega - \zeta)}{\omega} \quad \text{and} \quad R_c \text{ is radius of curvature}
\]

Comparison with \( v_t = C_p k^2 / \epsilon \) implies that:
3. Governing Equations and Some Aspects of Modelling

\[ C_\mu = \frac{-K_1 K_2}{1 + 8 K_1^2 \frac{k^2}{\varepsilon^2} \left( \frac{\partial U_1}{\partial n} + \frac{U_1}{R_c} \frac{U_1}{R_c} \right)} \]  

(3.38)

This variable, \( C_\mu \), reduces the levels of turbulence when the curvature is stabilising.

Leschziner and Rodi (1981) modelled the recirculating zone in an annular jet, with and without the curvature correction. As expected, the correction lead to a significant reduction in \( C_\mu \) in the curved shear layer bordering the recirculating zone and more realistic profiles of centreline velocity. Computations of Apsley (1994) of a separated flow caused by a bluff rectangular plate showed that this reduction of the shear stress in the curved shear layer resulted in the reattachment length being about 10% longer than when the standard model was used. Atkinson et al (1991), modelling the normal fence and splitter plate geometry, also found that the curvature correction of Leschziner & Rodi (1981) only slightly reduced the turbulence levels and slightly increased the distance to reattachment.

3.3.5.3 Modification to the Reynolds Stress Model to Allow for Wall Effects on the Turbulence Structure

In a simple shear flow, the proximity of a rigid wall causes the normal stress perpendicular to the wall to be damped whilst that parallel to the wall is enhanced, thus increasing the anisotropy. The magnitude of the shear stress is also diminished. The pressure strain terms as represented in section 3.3.3.1 do not account for effects of wall proximity on the flow; the modified pressure field leads to additional terms. A number of formulations for these have been proposed, but those of Gibson & Launder (1978) are the only ones available with CFX-F3D. The additional term suggested by these workers is known as the wall reflection term, \( \phi_{ij,w} \), and is shown below:

\[
\phi_{ij,w} = \left\{ \begin{array}{c} C_1 \frac{\varepsilon}{k} \left( \frac{u_k u_m n_k n_m \delta_{ij}}{2} - \frac{3}{2} \frac{u_k u_l n_k n_j}{2} \right) \\ + C_2 \left( \phi_{km,2} n_k n_m \delta_{ij} - \frac{3}{2} \phi_{lk,2} n_k n_j - \frac{3}{2} \phi_{jk,2} n_k n_i \right) \end{array} \right\} F(x_n) \]  

(3.39)

where

\[ \{ C_1 = 0.5, \ C_2 = 0.3 \} \]

The function \( F \) is defined as:
where $C_w$ has the value 0.065. This term is inversely proportional to the distance from the nearest wall, $x_n$, and proportional to the turbulence lengthscale, $k^2/e$.

Parameswaran, Yang & Chok (1994) investigated the performance of the Reynolds stress model with and without these wall reflection terms, using a backward facing geometry. It was found that they had no effect on the reattachment length, but that both models performed well compared to the $k$-$\varepsilon$ model (experimental: $6.2h$, $k$-$\varepsilon$ model: $4.86h$ and RSM: $5.61h$). However, it was also noted that peak values of $v^2$ within the separation bubble were better predicted using the model without wall reflection terms than with them. Lien and Leschziner (1994) also investigated the effect of using the wall reflection terms proposed by Gibson & Launder (1978), but the results were compared with computations using a different set of wall reflection terms proposed by Craft & Launder (1992). It was found that the Gibson & Launder model resulted in a bubble that was $6.3h$ long as opposed to the experimental length of around $8h$. The benchmark $k$-$\varepsilon$ model was around $6h$, but the Craft & Launder model performed best in producing a bubble that was around $7.3h$ long. The reason for the shorter reattachment length associated with the Gibson & Launder wall reflection terms compared to the Craft & Launder terms is not given.

Obi, Peric & Scheuerer (1991) also observed that the Reynolds stress model with the Gibson & Launder wall reflection terms predicted unphysical features around the reattachment process behind a backward facing step despite returning mean velocity profiles within the separation bubble that were close to the experimental results. The separation streamline was shown to double-up as the flow approached the wall, reattaching at an angle of opposite sign to that of the streamlines in the shear layer remote from the wall. Lien & Leschziner (1994) showed that downstream of reattachment, predictions using the second order closure technique with these wall reflection terms share the same defect as the standard $k$-$\varepsilon$ model: an insufficient rate of momentum recovery. This is largely because levels of shear stress were too low close to the wall, and consequently high $u$-momentum fluid was not entrained in this area.
4. Experimental Apparatus

4.1 Wind Tunnel

The wind tunnel used for all of the experimental work carried out during the present investigation was the 1.53m x 0.5m blower tunnel in the Department of Mechanical Engineering at the University of Surrey. A diagram of this tunnel is shown in Figure 4.1. The working section and contraction were specially designed for the present work, replacing the original contraction and (2' x 2'6") working section. The criteria for the new working section was that it should be wide enough to avoid end effects in the present flow while providing adequate working section height and flow velocity. A flow quality study at the beginning of the investigation revealed that at the inlet to the working section, the cross-section velocity uniformity is better than ±1%. Measurements with a hot-wire probe indicated that the free-stream turbulence intensity is less than 0.3%. The maximum wind speed attainable with no model in the tunnel is approximately 8m/s, although the majority of the present work was performed with a free-stream velocity of 5.8m/s.

It was realised early in the project that during the course of the experimental work measurements using the field-probe would have to be made at different vertical, lateral and axial positions within the tunnel. A streamwise slot in the roof allowed the probe to be traversed both along the working section and vertically. A sliding roof slat arrangement allowed continuous traverses to be made in the lateral direction.

4.2 Traverse

The fence and splitter plate arrangement was placed at the centre of the tunnel as shown in Figure 4.1, resulting in equal blockage effects for the separation bubbles above and below the plate. This meant that the volume above the splitter plate and below the wind tunnel roof was approximately 1.5 x 1.5 x 0.25 metres. It was necessary to design a traverse system that could position a probe anywhere within this volume with a desired precision for the three axes of 0.05mm. This level of precision was particularly important for the vertical axis, as the small fence height (usually 10 mm) resulted in strong vertical gradients of mean velocity and other variables. As well as these axes, two rotation axes had to be incorporated to allow measurements to be made at different orientations in the x-y plane and the x-z plane. This is to allow the determination of vertical and lateral velocity, V and W, and also the Reynolds stresses $\overline{v^2}$, $\overline{uv}$, $\overline{w^2}$ and $\overline{uw}$. Details of this procedure are given in the next chapter. The required motion, both in the three directions and in the two rotations is shown in Figure 4.1.
4. Experimental Apparatus

4.2.1 Horizontal Axes

A diagram showing the general features of the complete traverse system is given in Figure 4.2. One of the earliest considerations was how to achieve smooth and precise linear movement for the \( x \) and \( z \) axes. Previous traverses used in the EnFlo laboratory have used two guide rails and a moving carriage. However, thermal expansion of the carriage or a non-centralised driving force can result in these systems 'sticking'. A cheaper and more reliable system involved using one guide rail with self-aligning linear bearings and leaving one side of the carriage unconstrained in a lateral sense. The vertical position was maintained using a roller bearing which could travel along a flat rail.

As the rails were simply supported beams it was imperative that bowing should be minimised. This was achieved by mounting the rails on 50 x 50mm steel box section tubing, with a wall thickness of 5mm. Also, to ensure structural rigidity, these two axes were built into a welded frame and sub-frame arrangement. This also made the whole traverse modular, and the \( x \), \( y \) and \( z \) axis traverses could exist as separate entities. Another consideration for these two axes and the vertical axis was the method of driving the carriages. The cheapest method of achieving linear motion is simply by using a piece of threaded bar and a driven nut. The problem with this is that accuracy is lost due to backlash. To overcome this, leadscrews and ball followers were used on all linear axes which, although an order of magnitude more expensive, do not suffer backlash errors. The choice of motors to drive these axes will be discussed in section 4.2.4.

4.2.2 Vertical Axis

The difficulties in designing the two horizontal axes largely arose because they were simply supported at either end. The vertical traverse involved careful design because it was effectively a built-in vertical strut supporting a mass at variable heights. Two vertical guide rails were employed and a leadscrew between them was used to drive a carriage which supported the rotational traverse gear. The carriage, bearing housings for the leadscrew and base into which the supports were built were made of aluminium to keep the weight of the traverse down to a minimum.

4.2.3 Rotational Axes

Initially, the rotation equipment for the \( y \)-axis was designed. The chosen arrangement can be seen in Figure 4.3. The vertical probe support was attached to an anti-backlash gear which, in turn, was secured to the inner race of a roller bearing. This was then housed in the
aluminium vertical traverse carriage as shown in Figure 4.2. The motor to drive this motion was bolted on the underside of the carriage. Because of the position of the z-axis motor, rotation about the y-axis was limited to a span of around 180 degrees.

For the rotation about the z-axis, the traverse was initially designed in such a way that the probe could be held parallel with the splitter plate, and could be rotated about the probe axis to allow measurements to be made at different angles in the x-y plane. This system can also be seen in Figure 4.3, and it was used for a limited number of experiments. However, as will be discussed in Chapter 5, taking measurements of Reynolds stress with the probe in this orientation resulted in large errors, and it was realised that the pulsed-wire must always be held in the x-z plane with the sensor-wires aligned normal to the splitter plate. Consequently, a simple probe holder was designed such that the pulsed-wire probe could be held in the x-z plane at a range of angles from the y-axis.

### 4.2.4 Electrical Hardware for Traverse

For all motions, linear and rotary, some kind of motor and position recording system was required. Stepper motors are a well proven positioning technique, but there are many disadvantages with their use. For example, their jerky operation is not ideally suited for use with sensitive instruments such as a pulsed-wire probe, and their positioning system is open loop. If the motor does not carry out the total number of instructed revolution increments there is no closed loop controller to relay this omission to the operator, i.e. the positioning system offered by stepper motors is not absolute.

It was therefore decided to use d.c. motors in conjunction with a position recording device such as a linear potentiometer or an optical encoder. Both of these methods have the advantage that they are absolute. For financial reasons, optical encoders were used for this traverse. These had four thousand pulses per revolution, providing a positioning resolution for the three linear axes of 0.00125mm. The angular resolution for the two rotation axes is 0.00025 degrees.

Brushless d.c. motors were chosen for their smooth running and low current consumption. For positioning systems such as this, the choice of motor for a given situation depends on inertia matching of the motor to the driven system as well as power requirements. To calculate these values, it was necessary to specify parameters such as the diameter and pitch of the leadscrews, and the desired speed of the traverse.
For all axes, the position recording device cannot operate alone to provide an effective positioning system; a controller is also required to allow commands to be carried out precisely and smoothly. A programmable logic controller was used for this task, which incorporated a three-term controller (proportional, integral and derivative). However, these controllers were extremely expensive and the budget for this investigation did not allow one controller per motor to be purchased. Instead, a multiplexing circuit was designed which would allow controller share-strategy to be implemented. From each motor there were several links back to the controller. These included signals from the encoder, from the Hall effect current sensor and also from a thermocouple. All of these were low current signals and so could be multiplexed using chip-based switches. From the driver to the motors there were only the power cables. These had to be multiplexed using high current relays. Both kinds of switching device were mounted on five printed circuit boards; one for each motor. These boards could be activated using an 8-bit address line.

The electronic circuits and the programs to select the desired motor, update the controller settings and to control the position of the traverse were designed by Tom Lawton (a technician in the Mechanical Engineering Department of the University of Surrey). These programs were written using an Apple Macintosh computer and LabVIEW software. Communication between the computer and the controller was via an RS-422 serial link.

4.3 Fence and Splitter Plates

Wolf (1987), showed that the flow behind a bluff body was very sensitive to imperfections in the separation line. It was thus necessary to design a fence with precisely manufactured sharp edges. The early experiments used a fence with a height of 13.3mm with a splitter plate thickness of 3.3mm giving $h_f = 5\text{mm}$. However, the majority of experiments used a fence height, $h_p$, of around 10mm. For the unswept flow the fence on the splitter plate had an overall height of 23.7mm with the same splitter plate giving $h_f = 10.2\text{mm}$. For the swept flow, because of the difficulty of reproducing the unswept fences exactly, the overall height of the fence was 23.5mm leading to a fence height, $h_p$, of 10.1mm. A diagram showing the dimensions of this fence is shown in Figure 4.4.

The splitter plate for the unswept geometry can be seen in Figure 4.5a. The forward section of the plate is 0.5m long with a 350mm long slot in the centre to accommodate the through-wall and the shear stress probes. This slot starts only about 10mm downstream of the edge of the fence to allow measurements to be made close to separation. The slot is 20mm wide on the upper surface and is stepped midway through the 3.3mm thick plate, to allow stepped insert pieces to sit in the slot. The inserts for this slot were carefully machined to fit flush...
with the surface, and were manufactured in a range of lengths so that measurements at a wide range of downstream positions could be made. One of these inserts was manufactured with a 0.8mm diameter static pressure tapping in it, and a second insert was manufactured so that the through-wall probe and the shear stress probe could be mounted under the splitter plate, with the head of the probe flush with the surface. This insert also had fine lines etched on the surface to allow the probes to be positioned at different angles to the flow.

To elongate the splitter plate, a second plate of length 1m was placed downstream of the first. Thin adhesive tape was used to minimise the effect of the join. Both of these plates were supported at the centre of the tunnel on a series of 13mm diameter ‘legs’, the height of which could be adjusted. To allow for the blockage of these ‘legs’ (approximately 3.5% of the cross-sectional area under the splitter plate), the second plate was inclined slightly to the flow direction. For the unswept flow using the fence with $h_f=10.2\text{mm}$, this fine adjustment made it possible to have reattachment lengths above and below the splitter plate that agreed to within about 3%.

The splitter plate geometry used for the swept case can be seen in Figure 4.5b. The lateral position of the slots was largely determined using flow visualisation experiments to determine likely planes of interest ($z=-40, 0, 40, 80, 120$ and $380\text{mm}$). Flow visualisation results also suggested the approximate region where the flow was spanwise-invariant ($z=380\text{mm}$). Because of the weakening effect of these slots on the splitter plate, the slot could not be machined as close to the leading edge as with the unswept plate. At all lateral positions the slot began around 33mm downstream of the separation line. Because of the increased length in the separation bubble close to the centre, slots in this region were 450mm long as opposed to 300mm long in the spanwise-invariant region flow.

The splitter plate for the swept geometry was supported on the same ‘legs’ as the unswept geometry. To allow for the blockage of these legs, a 0.5m long flap was added downstream of the front portion of the splitter plate and inclined upwards at an angle of around 2.5 degrees. Careful adjustment of this flap lead to the length of the separation bubble above and below the fence at three lateral stations, $z=0, 380$ and $-380\text{mm}$, agreeing to within 5%.

4.4 Twin-Tube Device for Determining the Attachment Position

Preliminary measurements of reattachment length for the unswept and the swept flows were made with a differential twin-tube probe that was first proposed by Castro & Fackrell (1978). This device was constructed by soldering together two lengths of hypodermic tubing of 1.5 mm outside diameter and drilling a small hole in each tube on opposite sides of the
axis of symmetry. The probe was mounted on the surface of the splitter plate parallel to the fence and the reattachment point was taken as the position at which the measured pressure difference was zero. Flow visualisation checks indicated that the twin-tube probe determined the reattachment position to an accuracy of around ±3%.

4.5 Pulsed-Wire Anemometry

4.5.1 Field Probe

The term 'field' probe will be used to describe pulsed-wire probes that were used to measure velocity moments away from the near wall region. Several types of these probe were used during the present investigation. Although the probe head (the pulsed-wire, sensor-wires and wire supports) have remained unaltered, the design of the probe body has been changed considerably over the duration of the work. The reason for these changes will be discussed in Chapter 5. The probe head is a standard design used at the University of Surrey, and a diagram showing important dimensions is shown in Figure 4.6. The pulsed-wire and the two sensor-wires are around 6mm long and the distance between the a sensor-wire and the pulsed-wire is about 0.8mm (measured normal to the plane parallel to the wires). The two sensor-wires are offset from each other in a manner described in the diagram. This is to avoid one sensor-wire being in the wake of the other when the probe is held at zero degrees to the flow. The thickness of the two sensor-wires and the pulsed-wire is 2.5μm and 5μm, respectively. For measurements made with the field probe, the duration of the pulse was set at 4 μs. The current to the two sensor-wires, that act as simple resistance thermometers was set at 2mA. The orientation of the probe shown in the diagram, with the sensor-wires vertical and the pulsed-wire horizontal, was that used for the majority of experiments during the present investigation. Because of the physical geometry of the probe, this meant that the pulsed-wire could only be traversed down to a height of around 4mm off the splitter plate. In fact, measurements with the field probe were not made below 5mm, as a precautionary measure. In order to make measurements closer to the wall, a new pulsed-wire was designed at the beginning of the present work. This will be referred to as the through-wall probe.

4.5.2 Through-Wall Probe

The design requirements for this device were straightforward. The body of the probe had to be as small as possible to minimise disturbance to the flow below the splitter plate, and the height of the pulsed-wire had to be adjustable between the limits of approximately 0.1 and 10mm above the surface.
A general diagram of the final design is given in Figure 4.7. The general features of the probe were based on a through-wall probe designed by Terry Laws (a former technician at the University of Surrey). However, the present probe is approximately half the size of this earlier design. The vertical motion is controlled manually using the barrel of a micrometer, and the support wires are built into a piston which can slide within the probe body. The geometry of the probe head is similar to that of the field probe. The pulsed-wire is approximately 6mm long and the two sensor-wires are about 5mm long. However, the separation between the sensor-wires and the pulsed-wire, measured normal to the plane parallel to the wires, is somewhat larger than the field probe at around 1mm. This obviously has implications for the maximum possible yaw angle that can be achieved with this probe, as will be discussed in the next chapter. The thicknesses of the sensor-wires and the pulsed-wire are the same as that used for the field probe.

4.5.3 Shear Stress Probe

The probe head of the shear stress probe is shown in Figure 4.8. The pulsed-wire now lies in the same plane as the two sensor-wires, parallel with the surface approximately 0.1mm above it. The thickness of the wires is the same as that used for the other probes, although the lengths are considerably shorter. Because of the narrow width of this probe, and the resulting difficulties in making accurate angular movements, a probe housing was manufactured with a diameter of 13.5mm which then fitted into a modified insert piece which could slide along the slots in the plate with the probe head held flush with the surface. The method of calibrating this probe will be discussed in the following chapter.

4.5.4 Anemometer and Data Acquisition Hardware

A Pela Flow Instruments pulsed-wire anemometer was used during the investigation. Data acquisition was achieved by means of an Apple Macintosh computer fitted with a National Instruments multi-purpose LabNB data acquisition card. Associated software will be discussed in the following chapter. The differentiated signal from the sensor-wires, along with the ‘time-of-flight’ signal were also monitored using an oscilloscope.
4. Experimental Apparatus

Fig. 4.1 Wind Tunnel

Fig. 4.2 General Diagram of the Traverse
4. Experimental Apparatus

Fig. 4.3 Rotation Axes

Fig. 4.4 Fence

anti-backlash gears

toothed belt

motor

pulley

Cutaway section exposing toothed belt

probe

2

3.3

5.8

10.1

23.5

60
4. Experimental Apparatus

Fig. 4.5a Splitter plate used for the unswept separation

Fig. 4.5b Splitter plate used for the swept separation
4. Experimental Apparatus

Fig. 4.6 Head of Field Probe

Fig. 4.7 Through-Wall Probe

Fig. 4.8 Head of Shear Stress Probe
5. Experimental Techniques

5.1 Oil Flow Visualisation

Oil flow visualisation techniques were employed to elucidate the surface streamline characteristics of the unswept and swept separated flow, and to aid in the aligning of the fence and splitter plate in the wind tunnel. The time-mean nature of the technique meant that the location of the mean attachment line could be determined as well as indicating the extent of the spanwise-invariance of the flow. The position of other flow phenomena such as foci and saddle points, were also visible.

Comparisons of different compositions of mixture showed that the clearest results were obtained if paraffin oil and fine titanium dioxide were mixed in a ratio of 7:1 (by mass). To remove the need for photographic recording of the results, the mixture was sprayed on to tracing paper which was stuck down to the splitter plate. The wind tunnel was then left to run for approximately ten minutes, giving the titanium dioxide enough time to coagulate into bold filament lines. Careful drying and fixing of these 'plates' meant that a permanent original could be kept.

5.2 Ink Dot Visualisation

An alternative flow visualisation technique which enables the attachment position to be determined accurately (±2mm) is the ink dot method described by Langstone and Boyle (1982). A grid of soluble ink dots was drawn on a sheet of tracing paper which was then stuck on to the splitter plate. A solvent for the ink (methyl salicylate) was then sprayed evenly over the tracing paper using an atomiser. The flow immediately above the surface causes the solvent to move and this action along with the gradual dissolving of the ink results in streamlines being traced on the paper. The time taken for the dots to become velocity vectors varied from one experiment to another, because of unavoidable variations in the size of the ink dots and the thickness of the solvent. However, five minutes was typical when using a free-stream velocity of around 6m/s. After this period of time the tracing paper was removed from the flow, dried for twelve hours and then photocopied.

5.3 Smoke Flow Visualisation

A very limited number of smoke visualisation experiments were carried out using the swept geometry. An argon-ion laser rated at 4 watts connected to a cylindrical lens which produced a 40 degree diverging sheet of light was used to illuminate various planes within the flow.
5. Experimental Techniques

The three planes considered were the $z=0\text{mm}$ plane, the $z=380\text{mm}$ plane and the $y=5\text{mm}$ plane. Because of a slightly scratched lens this sheet of light was slightly divergent. Use of the sheet in the $y=5\text{mm}$ plane, where the laser had to be held outside the wind tunnel resulted in a beam that was around 2-3 mm thick close to the centre of the tunnel. When the laser was used to illuminate the flow in the $z=0$ and $z=380$ planes, the laser was held only 0.25m above the splitter plate, resulting in a narrower beam of around 1mm thickness over the height of the separation bubble.

For the two vertical planes, a smoke generating machine was used to inject smoke approximately 0.6m upstream of the fence via a 30mm diameter tube with the centre of the pipe at approximately the same height as the splitter plate. For the $y=5\text{mm}$ plane, the smoke was injected into the tunnel across half of the span of the tunnel, via a horizontally mounted 30mm diameter tube place approximately 0.6m upstream of the fence with a series of 10mm diameter holes drilled along its length with a spacing of 20mm. Both smoke injection systems were very crude as the wake effects are likely to have caused disturbances to the separated flow.

A video film was made of the various cross-sectional views, and most experiments were carried out using a Reynolds number of about 400 (based on $h_f$), corresponding to a free-stream velocity of 0.6m/s.

5.4 Measurements of Surface Pressure

Measurements of the time-mean static pressure on the splitter plate were made using a 0.8mm internal diameter pressure tapping drilled into an insert for the longitudinal slots. This insert fitted flush with the surface of the splitter plate, and could be traversed the length of the slots. The pressure difference between this pressure tapping and the static pressure reading taken from the reference pitot-static tube upstream of the separation was recorded using a Furness Controls micromanometer.

5.5 Calibrating the Through-Wall and Field Probe

The field probe and through-wall probe were calibrated at the beginning of each new run, and a calibration was not used for more than around 8 hours. A measurement at the beginning and end of a run at the same location was used to determine the amount of calibration drift. In the case of the field probe, which was often run for around 8 hours using
the automated traverse, the measurement at the end of the run in the free-stream was generally less than 1% different than that made at the start.

The field probe was generally calibrated on the lateral centreline of the wind tunnel, 100mm above the surface of the splitter plate and around 300mm upstream of the fence. It was calibrated against a pitot-static tube that was also positioned upstream of the fence and well clear of the wind tunnel wall boundary layer. When calibrating the through-wall probe the fence was removed from the splitter plate, and the probe was positioned such that the pulsed-wire was around 13mm off the surface of the plate and around 20mm back from the leading edge of the 3mm thick splitter plate which was fitted with a rounded leading edge to minimise the chance of separation occurring.

The same calibration law was used for the through-wall probe and the field probe. The third order fit is given below:

$$U = A\left(\frac{1}{T}\right) + B\left(\frac{1}{T}\right)^3$$

A least squares fitting program was used to fit this curve to the calibration points. An example of a velocity calibration for the field probe and the through-wall probe is given in Figure 5.1 and 5.2. The velocity ranges for the positive and negative wires for both probes is typical of those used. The third order fit works well over the entire velocity range for the field probe, but the low speed range of the through-wall probe is not adequately described by the third order fit. At low speeds, the calibration overpredicts the magnitude of the velocity, affecting the accuracy of the near-wall measurements. A correction has been applied to these measurements as described in section 5.6.

5.6 Correcting the Measurements Made with the Through-Wall Probe

Histograms for the through-wall measurements were not recorded, and consequently they were reconstructed assuming Gaussian turbulence. This histogram was used to determine the number of samples at a range of velocities. The next step was to determine the velocity that these values actually represented, had a better calibration fit been used. This was done by fitting a sixth order polynomial to the calibration points and using the 'time of flight' values used to calculate the original velocities to determine the correct values. A comparison of the original third order fit and the high order polynomial fit to the measured calibration points can be seen in Figure 5.2. This process of shifting the points on the histogram on the velocity axis means that the shape of the histogram is no longer Gaussian. It also means that
the spacing of the discretised velocity range is no longer uniform. These points are illustrated
in Figure 5.3. In order to determine the new values of the mean velocity and the velocity
variance, a modified version of the trapezium rule had to be implemented:

\[
\bar{U}_{\text{new}} = \frac{\sum_{i=1}^{i=N} ((U'_{i} + U'_{i+1})/2) \times (n_{i} + n_{i+1})/2}{\sum_{i=1}^{i=N} n_{i}} \quad (5.2)
\]

\[
\overline{u'^2}_{\text{new}} = \frac{\sum_{i=1}^{i=N} ((\bar{U}_{\text{new}} - (U'_{i} + U'_{i+1})/2)^{2} \times (n_{i} + n_{i+1})/2)}{\sum_{i=1}^{i=N} n_{i}} \quad (5.3)
\]

In these terms, \( U'_{i} \) is the corrected velocity and \( n_{i} \) is the number of samples at that corrected
velocity. Having implemented these corrections, it was found that no mean velocity
measurement made by the through-wall probe had been changed by more than \( \pm 0.01 U_{\text{ref}} \).
However, the local mean velocities measured by the through-wall probe were small because
of the proximity to the wall, and so the correction typically resulted in a change in \( U \) of
around \( \pm 5\% \). This figure also applies to the typical change of the local value of \( \bar{u'^2} \).

5.7 Calibrating the Shear Stress Probe

The pulsed-wire shear stress probe was mounted in a plate in the floor of the wind tunnel
approximately 1m downstream of a 6mm diameter ‘tripping’ wire to ensure a fully-turbulent
boundary layer, and calibrated against a Preston tube, using the calibration of Patel (1965).
The 1.25mm diameter Preston tube was placed approximately 40mm to the side of the
pulsed-wire skin-friction probe with the end of the Preston tube in contact with the surface of
the metal housing and approximately 10mm to the side of a surface static pressure tapping.
A third order polynomial was used for the calibration,

\[ \tau_w = A \left( \frac{1}{T} \right) + B \left( \frac{1}{T^2} \right) + C \left( \frac{1}{T^3} \right) + D \]  

(5.4)

using a least squares fitting program as for the velocity calibration, though here the formally correct averages of 1/T were used.

### 5.8 Angle Calibration of the Velocity and Shear Stress Probes

The yaw response of the field probe, the through-wall probe and the shear stress probe are ideally cosinusoidal. The actual yaw response of these probes, in both the x-z and the x-y plane, is given in Figures 5.4, 5.5, 5.6 and 5.7. The yaw response of the field probe extends to better than ±80 degrees for the x-z plane. On the basis of this range and the physical geometry of the probe, this figure also applies to the rotation in the x-y plane. This range could not be fully tested because of the mounting arrangement of the probe. The yaw response of the shear stress probe is also very good with a maximum yaw angle of at least ±85 degrees. The response of both of these probes is very close to the cosine curve, although the wake of the upstream sensor wire on the field probe is visible at an angle of around 25 degrees.

The yaw response of the through-wall probe is less satisfactory, with a maximum yaw angle of around ±75 degrees. Also at certain angles, wake effects from the vertical wire upstream of one of the pulsed-wire supports (which forms part of the structure that holds the sensor wires) can be seen. This structure can be seen in Figure 4.7. In hindsight, it would have been more sensible to locate these vertical wires that support the sensor wires directly to the side of the vertical pulsed-wire supports, and not upstream of them. In order to minimise the effect of the limited yaw response, the angles used to determine the various velocity moments using the through-wall probe were chosen not to exceed ±45 degrees of the local flow direction. Of course this does not totally eliminate the problem as instantaneous measurements may be well beyond the acceptable response of the probe. Further discussion of the effect this yaw response will have on the velocity moments will be discussed in section 5.11.
5.9 Time-Averaged Pulsed-Wire Measurements

The software used during the present investigation for time-averaged velocity data acquisition from a pulsed-wire was written and developed by Dr. D. Heist and Mr. P. Hayden. The program measured first, second and third order velocity moments. Conversion of the 'time of flight' pulsed-wire signal to velocity was evaluated from the probe calibration curve as already discussed. Mean and time averaged turbulence quantities, measured using the field probe, the through-wall probe and the shear stress probe were formed from 5000 samples taken at a frequency of around 30Hz. The corresponding sampling time of 170 seconds was felt to be sufficient, largely on the basis of the smooth nature of the resulting profiles of the mean and fluctuating quantities at typically 15 to 20 positions.

With this software, the time-averaged quantities were calculated on line, and usually only the final averages were retained. However, histograms of the all of the samples making up the individual measurements made with the shear stress probe, and selected measurements with the field probe were retained.

5.10 Deduction of the First, Second and Third Order Velocity Moments

In this section, reference will be made to measurements of velocities in the x-z plane. However, the same approach applies equally to measurements of velocity in the x-y plane.

If the probe axis is at an angle $\alpha_i$ to the flow direction, then the instantaneous velocity measured by the probe is:

$$U_{\alpha_i} = U \cos \alpha_i + W \sin \alpha_i$$  \hspace{1cm} (5.5)

Time averaging gives:

$$\bar{U}_{\alpha_i} = \bar{U} \cos \alpha_i + \bar{W} \sin \alpha_i$$  \hspace{1cm} (5.6)

and the fluctuation, by subtraction, is:

$$U_{\alpha_i} - \bar{U}_{\alpha_i} = (U - \bar{U}) \cos \alpha_i + (W - \bar{W}) \sin \alpha_i$$  \hspace{1cm} (5.7)
5. Experimental Techniques

i.e.

\[ u_{\alpha_i} = u \cos \alpha_i + w \sin \alpha_i \]  \hspace{1cm} (5.8)

so that

\[ \overline{u^2 \alpha_i} = \overline{u^2 \cos^2 \alpha_i} + 2 \overline{uw \cos \alpha_i \sin \alpha_i} + \overline{w^2 \sin^2 \alpha_i} \]  \hspace{1cm} (5.9)

\( \overline{U}, \overline{W}, \overline{u^2}, \overline{w^2} \) and \( \overline{uw} \) are determined from least squares fits of Equations 5.6 and 5.9 from the measured \( \overline{U_{\alpha_i}} \) and \( u^2_{\alpha_i} \). Since there are three unknown stress components in Equation 5.9, three probe angles were required. However to reduce uncertainties, five angles were considered in both the x-y and the x-z planes. Care was also taken not to choose angles beyond the yaw and pitch response of the probe.

5.11 Estimation of Errors Associated With Pulsed-Wire Measurements

The principal sources of error associated with pulsed-wire measurements of velocity moments can be split into the following groups:

1) imperfect fit of the calibration curve to the calibration points;
2) pulsed-wires inability to measure velocities below around 0.2m/s;
3) departure from the ideal cosinusoidal yaw/pitch of the probe, and
4) yaw/pitch response of the pulsed-wire probe not extending to ±90 degrees.

The mean error between the calibration points and the calibration curve was generally around 1.5% for the field probe and the shear stress probe. This figure was slightly higher for the through-wall probe because of the poor fit in the low velocity range, but errors associated with this have been corrected as shown in section 5.6 of this chapter. The time-of-flight counter on the pulsed-wire anemometer unit was limited to a maximum time-of-flight of around 4\( \mu \)s, which corresponded to a minimum measurable velocity of about 0.12m/s in the case of the field probe and about 0.2m/s for the through-wall probe. Heat tracers taking longer than this to travel between the pulsed-wire and one of the sensor wires are assigned zero velocity. This inability to measure very low velocities means that the histogram has a missing 'slice' around the zero velocity, with the number of samples within this slice.
accumulated in a spike at zero velocity. As it happens, the errors in mean velocity and higher order moments are almost always small if not negligible.

Workers including Bradbury and Castro (1971), Bradbury (1976) and Castro and Cheun (1982) have investigated the effect of departure from the ideal cosinusoidal yaw response and the effect of the yaw response not extending to ±90 degrees. The majority of these workers have used probes whose angular response is better described by the empirical relationship:

\[ \frac{T_0}{T_0} = \cos \theta + \varepsilon \sin \theta \]  

(5.10)

with the constant \( \varepsilon \) at around 0.1. This additional term to the cosine relationship allows for the effect of diffusion, which tends to cause the yaw response to lie somewhat above the ideal cosine-law response. However, all of the probes used in the present investigation have yaw and pitch responses considerably closer to the ideal response than this; for the probes used \( \varepsilon \) was in the range 0< \( \varepsilon <0.03 \). These previous studies have also tended to use probes with a maximum yaw angle of around 70 degrees whereas the range was better than 80 degrees for the probes used here. The effect of these differences on the errors will be discussed.

Bradbury and Castro (1971) simplify the analysis by considering the influence of the limited yaw response and departures from the cosine law separately, when the probe is aligned to the flow such that its three wires are normal to the x-direction (Figure 5.8). Castro and Cheun (1982) take this work further by considering the effect of limited yaw response and departures from the cosine law together as well as separately. Also, this analysis is carried out on the errors arising when the Reynolds stresses are determined from several measurements taken at several probe orientations to the local flow direction.

All of these studies assume that the deviation from the cosine law, and consequently the value of \( \varepsilon \), is the same in the x-y plane as it is in the x-z plane, since it has been found that \( \varepsilon \) is not very sensitive to the velocity vector location in the x-y-z space. The analysis is also based on two-dimensional flow and assumes that the receptivity volume of each sensor wire is represented by a cone. All workers assume normally distributed turbulence, and the majority of the analysis is based on isotropic turbulence. Both sets of workers assume that missed tracers are counted as zero velocity rather than being ignored altogether. The receptivity of the cone is shown in Figures 5.8.
During the present investigation, measurements of all velocity moments have generally been made using a fitting routine being applied to five separate measurements made at different orientations to the flow. Only for the unswept flow will measurements be presented based on a single orientation approach. However certain aspects of the analysis by Bradbury and Castro (1971), Bradbury (1976) and Castro and Cheun (1982) can be used to help quantify some of the errors associated with measurements of mean and fluctuating quantities presented in this thesis. Since $\epsilon$ in Equation 5.10 is so small for probes used during the present investigation, only the analysis of the errors associated with the limited yaw/pitch response will be considered.

For the axial velocity measurements made in the unswept and the swept flow, the results based on the zero degree orientation from the data set in the $x$-$y$ and the $x$-$z$ plane were in very good agreement with the values returned from the fitting routine, based on all probe orientations, indicating that the response of the probe was closely cosinusoidal. This was true of the measurements made with the through-wall probe and the field probe. Since this is the case, the analysis of the errors of axial velocity may partly be examined using the analysis of Bradbury & Castro (1971) that is based on single orientation measurements. Because this analysis is based on flow in the streamwise direction, it can only be used very broadly with the results from the swept geometry. This analysis would suggest that typical errors in $U$ for the field probe and the through-wall probe would be around 1.5% and 2.5% of $U$, respectively. The difference arises from the greater yaw response of the field probe.

For both probes, in the unswept and swept flow typical scatter of the individual mean velocity measurements at the various probe orientations about the fitted curve is around ±2%. It would seem that error bars of around ±0.02$U_{ref}$ are probably appropriate for all axial velocity measurements presented in this thesis. Based on the very good ‘fits’ of the mean velocity measurements at the various probe orientations in the $x$-$y$ and the $x$-$z$ plane, the error bars associated with the measurements of $U$ is probably also appropriate for the measurements of $V$ and $W$.

For the velocity variance measurements at the various probe orientations in the $x$-$y$ and the $x$-$z$ plane, the scatter about the fitted curve was generally about ±5%. The good ‘fits’ meant that the values of axial stress returned by the fitting routine were generally within 5% of the values measured directly as part of the multiple orientation data set. This was true for the unswept and the swept flow and for the through-wall and the field probe. For the swept flow, peak values of $u^2$ determined from the multiple orientation measurements in the $x$-$y$ plane were typically around 5-10% higher than those from the $x$-$z$ plane. This was largely because the fits for the $x$-$y$ data set tended to result in higher predicted axial stresses than the
single orientation result. For the unswept flow, the typical difference in the peak axial stresses determined using the x-y data set and the x-z data set was larger at around 15% even though the fits for both sets of data were very good. A partial explanation for this difference is that the stresses from the x-y data set are believed to be too high possibly because of loose wires on the probe.

In this thesis, the presented values of $u^2$ for the swept flow will be based on the measurements from the ‘fits’ of the data in the x-z plane. For the unswept flow the preferred data set is based on single orientation measurements, that display peak values that lie between those determined from the multiple orientations in the x-y and the x-z plane. For the presented values of axial stress, error bars of around ±5% are probably appropriate. Error analysis of $u^2$ measurements performed by Castro & Cheun, based on three probe orientations, suggests that this figure is not unreasonable. In this work, it was shown that for a probe with a maximum yaw angle of 70 degrees, peak errors in $u^2$ were around 5%.

The work of Castro & Cheun may also be used to assess the possible errors associated with the Reynolds stresses $v^2$, $w^2$, $uv$ and $uw$ presented in this thesis. However, since most of the analysis is based on the assumption that the turbulence is isotropic, that the flow is two-dimensional and that the orientations used are symmetrical about the flow direction at 0, 45 and -45 degrees, the results can only be used as a guide for the present work. The use of symmetrical angles was not easily possible in the present investigation, except for the measurements in the x-z plane in the unswept flow, because of the changes in flow direction with position in the flow.

Using the three probe orientation approach, Castro and Cheun showed that when the turbulence intensity of the flow is around 50%, which is approximately the level occurring at the position at which the stresses are a maximum for the swept and the unswept flow, a probe with a maximum yaw/pitch angle of 70 degrees leads to errors in $v^2$ (or $w^2$) of around 20%. Based on this work alone, an estimate of the possible errors in $v^2$ and $w^2$ for the field probe used during this investigation, with a maximum yaw/pitch angle of around 80 degrees, is around 10%, since a probe with a maximum yaw/pitch angle of 90 degrees would record zero error. However, to allow for other errors caused by factors such as loose wires on the pulsed-wire probe, error bars of around ±15% are probably appropriate for the field probe measurements of $v^2$ and $w^2$ in both flows. Measurements of $w^2$ made with the through-wall probe were only made in the swept flow. The through-wall probe has a narrower yaw response than the field probe, but operates in regions where the turbulence intensity is very high, where the analysis of Castro and Cheun suggest that errors become

75
very small. Consequently, typical errors for $w^2$ made in the swept flow close to the wall may be similar to the field probe measurements.

The predicted errors in $uv$ and $uw$ at different turbulent intensities were found to be strongly dependent on the value of the maximum yaw/pitch angle. By increasing this angle from 60 to 70 degrees, the peak in the positive error, that occurred when the turbulence intensity was around 20%, was reduced from around 80% to 30%. At higher turbulence intensities, say 50%, the probe with the 70 degree maximum yaw/pitch angle was shown to produce errors of around 20%. This work suggests that errors in $uv$ for the field probe used during this investigation, with a maximum yaw/pitch angle of at least 80 degrees, could be around 10%. However, to account for other problems associated with the measurements in the $x$-$y$ plane, such as possible loose wires, a sensible value for the error bars for the measurements of $uv$ with the field probe in both flows is around ±15%.

$uw$ in the unswept flow should be zero and in the mildly swept flow it is very small, making discussion of relative errors very difficult. In the unswept flow, predicted values of $uw$ at the various streamwise stations considered were generally less than 2% of the maximum axial stress at the corresponding downstream station. The scatter in $uw$ in the unswept flow was similar in magnitude to the predicted values in the swept flow, and so the latter will not be discussed in detail.
5. Experimental Techniques

Fig. 5.1 Typical velocity calibration for the field probe

Fig. 5.2 Typical velocity calibration for the through-wall probe
Fig. 5.3 Change in Histogram Shape and Spacing of Discretised Velocity Range Due to Correction of Velocity Calibration

Fig. 5.4 Angle calibration in the $x$-$z$ plane of the field probe
5. Experimental Techniques

Fig. 5.5 Angle calibration in the x-y plane of the field probe

Fig. 5.6 Angle calibration in the x-z plane of the through-wall probe
Fig. 5.7 Angle calibration of the shear stress probe

Fig. 5.8 Response cone of pulsed-wire probe
6. Preliminary Measurements and Development Work

6.1 Unswept Flow ($h_f = 5$ mm)

Measurements of $U$ and $\overline{u^2}$ at five downstream locations, $x/X_p = 0.25, 0.5, 0.75, 1.0$ and 1.25 were taken with the unswept geometry with the 5mm high fence. Some of these results will be discussed in conjunction with the measurements made with the larger fence in the next chapter. In this section certain aspects of these early measurements will be discussed that resulted in changes to the methods of measurement and improvements to the pulsed-wire probes.

Using the smaller fence, measurements with the twin-tube device suggested that close to the centre of the tunnel, reattachment to the splitter plate took place at around $x=124$mm ($24.8h_f$) above the splitter plate and at around $x=100$mm ($20h_f$) below. However, it is believed that the 'legs' supporting the splitter plate probably shortened the separation bubble beneath the plate, and that the plate was reasonably well aligned with the flow. All measurements were taken above the splitter plate. The velocity profile at $x/X_p = 1$ is easily identifiable as $\partial U / \partial y$ must be zero at $y=0$. However, initial experiments showed that the velocity profile which displayed the zero velocity gradient at the surface of the plate took place at only 105mm ($21h_f$). It was initially believed that the running of other wind tunnels in the same building caused pressure changes at the end of the working section, thus reducing the reattachment length. This explanation was made plausible as the wind tunnel used for this investigation has no 'isolating length' after the working section.

A systematic study of the behaviour of $X_r$ was then carried out with the closest wind tunnel running at various speeds, but no appreciable change in the reattachment length took place. The only other parameter which varied between investigations using flow visualisation techniques and the pulsed-wire probe was the physical presence of the probe itself. A detailed investigation into the effect of the presence of the probe held at various orientations within the flow was then performed and indeed indicated changes in $X_r$ as previously observed. Further measurements were then made to quantify the change in $X_r$ with probe movement in the $x$, $y$ and $z$ directions. Potentially, this involved a large amount of measurement, but it was found that a method could be used that greatly reduced the task.

The twin-tube probe was left on the surface of the plate at the reattachment position occurring with no obstruction in the flow. At the reattachment position, the pressure difference measured by the twin-tube probe, $\Delta p_{\text{twin-tube}}$, is zero. The traversing gear was then used to move a dummy pulsed-wire probe in three axes around the reattachment point at the centre of
the tunnel, and the changes in the pressure difference measured by the twin tube device were recorded. Measurements taken with the twin-tube probe showed that the variation of pressure difference measured with the twin-tube probe with downstream position was approximately linear in the region $0.85X_r<x<1.15X_r$ both with and without the dummy pulsed-wire probe present. Furthermore, the slope of this graph, $d(\Delta p_{\text{twin-tube}})/dx$, was the about the same in both cases. Consequently, this slope was used to convert the pressure changes measured by the twin tube device at $X_r$ (without dummy probe) into the reattachment length with the dummy probe at a certain location. This was done in the manner described below:

$$X_r(\text{with dummy probe}) = X_r(\text{without dummy probe}) - \frac{(\Delta p_{\text{twin-tube}})_{\text{with dummy probe}}}{d(\Delta p_{\text{twin-tube}})/dx}$$

(6.1)

This process was considerably quicker than determining the reattachment length by moving the twin-tube probe every time the pulsed-wire was moved, although it was not precise because of the slight non-linearity in the variation of $\Delta p_{\text{twin-tube}}$ with $x$.

Figure 6.1a shows that as the probe was moved from a height of 150mm to 6mm above the plate directly above the twin tube probe the reattachment length decreased by around 11%. The slope of the graph at low $y$ values is an indication of the sensitivity of the flow to probe intrusion. Figure 6.1b shows the effect on $X_r$ of moving the probe in a streamwise direction over the twin tube device at a constant height of 10mm. The probes presence appears to have a noticeable shortening effect on $X_r$ even when it is held 100mm downstream of reattachment measured without a probe in the flow. Moving the probe upstream of the twin tube device results in a rapid recovery of $X_r$. The reattachment length reaches the pre-intrusion level before the probe has reached the fence. Fig 6.1c indicates that this flow regime is also sensitive to disturbances which occur at a spanwise displaced location. Probe positions 150mm away from a given point cause the reattachment length there to decrease.

All of these results have serious practical implications for the present investigation and for previous studies of flow separation and reattachment carried out using similar fence geometries and dimensions. For example, judging from the presented axial velocity profiles, the separated flow generated by Castro and Haque (1987) was probably shortened by the presence of the probe by around 4%. For the work of Ruderich and Fernholz (1986), this figure is probably around 7%. These discrepancies were not commented upon in any of the investigations.
To reduce the magnitude of the effect of the probe body, a new probe was designed such that it could be held at 45° to the vertical, with the head of the probe further upstream than the top of the probe. With this arrangement, it was found that when the head of the probe was directly above \( X_r \) (without probe), the reattachment length only decreased by around 6%. This was considered acceptable, and the results obtained with this probe will be discussed in the next chapter.

Another problem encountered during the investigation with the smaller fence was the measurements of the vertical velocity moments determined from several measurements at different orientations in the \( x-y \) plane. As discussed in section 4.23, the traverse had been specially designed for these measurements so that the pulsed-wire could be held parallel with the splitter plate and rotated about the \( z \)-axis. This rotation, \( \theta \), is shown in Figure 4.3. This has been the method employed by previous workers in the field of separated flows to obtain vertical velocity moments. With the stem of a standard probe (shown in Figure 4.6) aligned on the \( z \)-axis with the pulsed-wire normal to the surface, the sensor wires are held at different heights from the surface of the plate because of the offset of the wires. For a standard probe with an offset angle of around 30 degrees, this height difference is as much as 1mm. If the axial velocity profile at the centre of the separation bubble formed behind the 5mm high fence is considered, this height difference can correspond to a velocity difference of about \( 0.1U_{ref} \) in the region of highest shear. Closer to the fence at say \( x/X_r=0.25 \), this vertical displacement can represent an axial velocity change of \( 0.15U_{ref} \).

In many places within the separation, the offset wires may cause only minor errors for the mean axial velocity, \( U \). The centre of the probe is taken as the measuring point, and the axial velocity here will be the average of the two velocities corresponding to the heights of the sensor wires either side of it, one point 0.5mm above and the other 0.5mm below. However, the Reynolds stresses may be expected to have a large error associated with them as can best be explained using an example. If the probe is stationed in the flow at \( x/X_r=0.25 \) at a vertical height such that the centre of the probe is at the height at which the mean axial velocity is zero, then one sensor wire will be above this, measuring a positive velocity, and one sensor wire will be beneath it, measuring a negative velocity. Assuming a vertical spacing of the two sensor wires of 1mm and using appropriate values of mean velocity and velocity variance measured by each sensor wire, histograms may be constructed for each wire. A Gaussian form is used here as shown in Figure 6.2a. In this figure, the supposed true histogram is shown as well as two other histograms displaced in velocity according to \( \pm \partial U / \partial y \Delta y \) where \( \Delta y \) is the sensor wire off-set.
6. Preliminary Measurements and Development Work

Assuming the probe is oriented such that the positive sensor wire is measuring the positive velocities, then the histogram that the measuring program will construct is shown in Figure 6.2b. It can be seen that the effect of the vertically displaced sensor wires is to increase the velocity variance. Using this analysis applied to the point at which \( U = 0 \) at \( x/X_r = 0.25 \) for the flow with a fence height of 5mm and a free-stream velocity of 6m/s the real value of \( \frac{\overline{u^2}}{U_{ref}^2} \) of 0.03 would be measured at 0.0385 (an error of greater around 28%). If the probe had been rotated through 180 degrees about the y-axis, so that the positive wire was reading negative velocities, the constructed histogram would have looked like Figure 6.2c. The measured value of \( \frac{\overline{u^2}}{U_{ref}^2} \) would have been 0.0246 (about 18% less than the real value). Of course both of these constructed histograms correctly predict the zero mean velocity.

To demonstrate this point, experiments were later carried out at \( x/X_r = 0.25 \) in the spanwise-invariant region of the swept flow with \( h_f = 5 \)mm (using axes aligned with the fence), using the probe held in three orientations to the flow: with the sensor wires held parallel to the splitter plate with a vertical displacement between the wires (with the positive wire of the probe measuring the positive and then the negative direction) and with the probe held in the flow with the pulsed-wire parallel to the splitter plate. In the case of this third orientation, the sensor wires are vertical and the offset between the wires occurs in the lateral direction where there should be no velocity gradients. The results of this investigation are shown in Figure 6.3 and the effect discussed above using a Gaussian turbulence model is shown very clearly. Where the peak axial stress occurs, which corresponds to the position at which \( \frac{\partial U}{\partial y} \) is a maximum, the horizontally held probe may measure \( \frac{\overline{u^2}}{U_{ref}^2} \) to be 30% greater or 45% less than the expected value.

These errors were obviously not acceptable for the present work and so no measurements have been made with the probe in this horizontal orientation. The error analysis associated with the measurement of \( \overline{v^2} \) or \( \overline{uv} \) using multiple orientations in the \( x-y \) plane with the probe held horizontally is difficult to make. Depending on the probe orientations considered and the physical geometry of the probe, the vertical spacing between the two sensor wires will change. Also, depending on the size of the separation bubble and the location within it, the effect of the vertically displaced sensor wires will vary. Many previous workers have used the probe in this orientation to make sets of measurements in the \( x-y \) plane. These results, especially those which have been made with fence heights that are not significantly larger than the offset distance between the sensor wires must therefore be treated with caution.

During the present investigation, \( V, \overline{v^2} \) and \( \overline{uv} \) have been determined using multiple orientation measurements in the \( x-y \) plane, but in a way that avoids the sensor wires being
held at different heights. This type of rotation can best be explained with the aid of Figure 4.6. To obtain measurements in the x-y plane, the whole probe must be slanted upstream or downstream, and the probe support must then be moved in the x and y directions to accommodate the resulting translation of the probe head.

6.2 Swept Flow \((h=5\text{mm})\)

Ideally, the vertically held probe rotated at several angles would have been used in conjunction with the fitting routine to determine the lateral mean velocities and fluctuations in the swept flow with \(h=5\text{mm}\). However, as with the unswept flow, the reattachment length was very sensitive to the obstruction caused by the probe in this orientation. Consequently, the slanted probe had to be employed which resulted in axial corrections having to be made when the probe rotated. Also, when measurements were made at the centre of the swept fence, the angled probe resulted in an asymmetry of the measuring device which was found to affect the flow itself. Figure 6.4 demonstrates this problem. The angles considered were -60, -130, 60, and 130 degrees. If angles are omitted from the measurements, the resolved value of \(W\) changes dramatically.

This difficulty in measuring lateral velocity moments using the angled probe suggested that a vertically held probe had to be used. To prevent this probe orientation having a significant effect on the length of the separation it was decided to recommence the experimental program using a 10mm high fence. This increase in the fence height meant the aspect ratio, \(W/h_f\) of the unswept geometry would be 150 rather than 300, which was considered just sufficient. It was also decided to design a new pulsed-wire with a narrower body than had previously been used.
6. Preliminary Measurements and Development Work

Fig. 6.1a Variation of $X_r$ with probe movement in y-direction (probe at $x=X_r, z=0$)

Fig. 6.1b Variation of $X_r$ with probe movement in x-direction (probe at $y=10\text{mm}, z=0$)

Fig. 6.1c Variation of $X_r$ with probe movement in z-direction (probe at $x=X_r, y=10\text{mm}$)
6. Preliminary Measurements and Development Work

Fig. 6.2a Histogram of the two sensor wires held at different heights in the flow $x/X_r = 0.25$ ($h = 5\text{mm}$)

Fig. 6.2b Constructed histogram of the measurements made by the positive and negative sensor wires (with positive wire measuring positive velocity)

Fig. 6.2c Constructed histogram of the measurements made by the positive and negative sensor wires (with positive wire measuring negative velocity)
6. Preliminary Measurements and Development Work

Fig. 6.3 Effect of probe orientation on measurement of axial stress \((x/X_r=0.25, \text{spanwise invariant region})\)

Fig. 6.4 Comparison of \(W\) at \(z=0, x/X_r=1.25\) determined with various sets of probe orientations
7. Unswept Separation

7.1 Introduction

The main focus of the present work is to investigate the separated flow in the spanwise-invariant region of the swept flow and also in the fully three-dimensional flow region close to the plane of symmetry of the v-configuration fence. However, the large number of data sets obtained by previous workers in the unswept geometry provided an ideal means of testing the measurement techniques that would be used throughout the present investigation. The fact that measurements were made using two fence heights and two Reynolds numbers in flows with large aspect ratios also meant that some questions regarding the Reynolds number dependence of the stresses and appropriate scaling parameters for mean velocity profiles could be addressed.

7.2 Surface Flow Visualisation

Figure 7.1 and Figure 7.2 are scaled photocopies (×0.707) of surface flow visualisation experiments carried out using the 10mm high fence with a free-stream velocity of 5.8m/s, giving a Reynolds number of around 3800 based on $h_f$. This was the same free-stream velocity that was used for all pulsed-wire and pressure measurements using this fence. The first of these figures was carried out close to the wind tunnel wall and the latter comes from the central portion of the tunnel in the spanwise-invariant region. An interpretation of the surface streamline pattern over the entire lateral extent of the flow is also given in Figure 7.3. In these figures, the letters F and S correspond to focus and saddle singularities, respectively, and the number subscripts identify individual singularities to aid comparison with later flow visualisation experiments with the v-configuration fence.

The reattachment length, measured in the spanwise-invariant region, is approximately $23h_f$. Later experiments with the twin tube probe, to check splitter plate alignment, showed that with a symmetrical separation bubble above and below the splitter plate, $X_r$ was closer to $21.4h_f$. This figure is about 86% of that recorded with the smaller fence, presumably because of the higher blockage. The location of the secondary separation line is difficult to determine as the flow visualisation technique has not worked well in the near-wall region. However, near-wall velocity measurements at the centre of the tunnel suggest that it occurs closer to the fence than $x=2.8h_f$.

Hancock, McCluskey and Castro (1992) suggest that the aspect ratio, $W/X_r$ for the normal fence and splitter plate geometry should be about 4.3 for a spanwise-invariant region to
begin to exist. The results from the present investigation suggest that an aspect ratio, $W/X_r$, of 6.7 produces a region of spanwise invariance that is approximately $2.2X_r$ wide, implying that a spanwise-invariant region would begin to exist with the width of the tunnel being $4.5X_r$ ($W/h_f=95$). This level of agreement with the work of Hancock, McCluskey and Castro (1992) certainly seems to add weight to the argument that almost all detailed studies of this type of flow have been made in configurations where the aspect ratio was significantly too low. Of the previous investigations of unswept separated flow, Jaroch and Fernholz (1989) used the highest aspect ratio and this figure was only 2.6.

The topological features which are displayed in these figures have been shown by many workers to characterise separated and reattached flow. The most obvious of these features is the two foci located at about $x=2.9h_f$ and $2.5h_f$ from the tunnel walls. These foci are a result of the reversed flow encountering the secondary separation bubble; the fluid converging on this line is deflected laterally and accumulates in a focus which lifts off the surface because of the converging streamlines and forms a vortex. The focus is therefore the 'footprint' of the vortex left on the plate's surface.

The surface streamline pattern close to the fence has not been captured very well using this visualisation technique, because of the low values of shear stress in this region. Consequently, the positions of the two saddle point singularities close to the focal points that are shown in Figure 7.3 are only approximate. Preliminary visualisation experiments with the 5mm high fence, suggested that these saddle points occur at approximately the same downstream location as the foci points and approximately one reattachment length from the tunnel walls.

7.3 Static Pressure Measurements

The static pressure coefficient, $\tilde{C}_p$, suggested by Roshko and Lau (1965) is plotted against $x/X_r$ in Figure 7.4 along with the results from three other workers (the definition of this coefficient is given in Equation 2.2). Agreement between these sets of data is very good, suggesting that the non-dimensionalising parameter takes account of factors such as blockage ratio, aspect ratio and free-stream turbulence. This agreement also lends support to the idea that the reattachment length is an important lengthscale for this flow. This point will be discussed in more detail in the following section.
7.4 Mean Velocity Measurements

The presence of the field probe around the reattachment position caused the separation bubble, generated using the 10mm high fence, to shorten by around 4%. The desired downstream locations of $x/X_r=0.25$, 0.5 0.75, 1.0 and 1.25 were based on this shortened separation bubble and consequently the relative downstream locations based on the unperturbed value of $X_r$ for the measurements with the field probe were $x/X_r=0.24$, 0.48, 0.73, 0.96 and 1.20. Because the field probe was held directly above the through-wall probe (at $y=15\text{mm}$) for measurements with the latter, these relative downstream locations are also approximately correct for the through-wall measurements.

Axial mean velocity profiles taken at these five positions downstream of the fence are shown in Figure 7.5. The free-stream velocity upstream of the fence has been used to normalise, and so the maximum velocity in these profiles gives an indication of the acceleration of the flow because of the blockage in the tunnel caused by the separation bubble. Although not shown in Figure 7.5, axial velocity profiles were also determined at $x/X_r=0.12$, 0.36, 0.6, 0.84 and 1.08. This large number of profiles in the downstream direction meant that values of the velocity gradient $\partial U/\partial x$ could be determined. Consequently, solution of the continuity equation was possible, and the resulting profiles of vertical velocity are shown in Figure 7.6, along with the values deduced using the multiple probe orientation approach. The results are generally consistent to within about ±3% of $U_{ref}$, which is considered satisfactory given the rather intrusive nature of some of the probe orientations necessary to calculate vertical velocity moments and the possible errors associated with the velocity calibration of the probe. Differences may also be due to the fact that the measurements of $V$ using the multiple orientation approach were carried out in a separation bubble that was approximately 10% longer than the symmetrical bubble used for the axial velocity measurements used to determine the velocity gradient $\partial U/\partial x$.

More information about the bubble geometry can be obtained from the streamlines shown in Figure 7.7. These were obtained by integrating the ten axial velocity profiles outward from the wall. Also included in this figure is the locus of $U=0$, the locus of maximum turbulence energy and the locus of $\eta=0$ (where the latter corresponds to the shear layer centreline, and will be described later).

Previous workers have tended to normalise the vertical scaling of axial velocity profiles using the reattachment length. Axial velocity profiles scaled in this way at five downstream locations determined by several workers are shown in Figures 7.8a, b, c, d and e. Also included in these figures are the results determined during the present investigation using the smaller fence ($h_f=5\text{mm}$). Because the blockage ratio is different in each of the experiments,
the maximum axial velocity at each downstream location, $U_{max}$, has been used to scale the profiles rather than $U_{ref}$. The velocity profiles at each of these downstream locations generated using both fence heights during the present investigation and the profiles taken from Castro & Haque (1987) collapse very well using $X_r$ to scale the vertical axis. However, the profiles of Hancock & McCluskey (1997) suggest that the height of the separation bubble in their investigation, based on $X_r$, is greater than in the other cases.

A possible explanation for this unexpected variation of bubble shape is if the splitter plate used in the experiments carried out by McCluskey was not aligned parallel to the flow. Plate misalignment would perhaps have the effect of changing the reattachment length without significantly altering the bubble height. For example, if the reattachment length in the McCluskey experiments was 10% longer than measured, then the velocity profiles in all cases collapse very convincingly. Work carried out by Wolf (1987) suggests that for the separated flow over a blunt rectangular plate, the symmetry of the flow either side of the plate is extremely sensitive to plate misalignment. To account for a change in $X_r$ of around 10%, the misalignment of the fence and splitter plate would only have to be around 0.5°.

This explanation can only be made tentatively, as it is not certain if the profiles determined during the present investigation using the smaller fence were made in a flow with the splitter plate aligned very precisely (see section 6.1). The argument can only be based on the level of collapse between the velocity profiles of the present investigation with $h_j$=10mm and those of Castro and Haque, where flow symmetry was carefully checked using adjustable flaps at the downstream edge of the splitter plate.

The fact that $X_r$ collapses profiles of mean velocity in the present work with those of Castro & Haque, suggests that the shape of the separation bubble (the ratio of its length to its height), has a universal nature. If we define the height of the bubble, $H_b$, as the vertical position where $U=0.95U_{max}$, then the values of $X_r/H_b(x=0.5)$ for the two fence heights considered during the present investigation and that considered by Castro & Haque lie between 5.82 and 5.9.

Although the work by Wolf (1987) showed that the reattachment length was highly sensitive to plate misalignment, the variation in the height of the separation bubble with plate incidence was not considered. To assess the ability of this dimension as a scaling parameter, the axial velocity profiles of the present investigation were plotted against those taken from Castro & Haque and Hancock & McCluskey using the local value of $H_b$ to scale the height of the profiles. Figures 7.9a, b, c, d and e show these profiles measured at around $x/X_r$=0.25, 0.5, 0.75, 1.0 and 1.25, respectively. The good agreement between these sets of results,
including the profiles of Hancock & McCluskey suggest that the local height of the separation bubble is a parameter that can also account for plate misalignment.

Another way in which the results of the present investigation can be compared with those of other workers is to consider the growth of the shear layer with downstream position. Previous workers including Castro & Haque (1987), Ruderich & Fernholz (1986) and Hancock & McCluskey (1997) have measured this growth in terms of the increase in vorticity thickness, \( \Lambda \), that was defined by Brown & Roshko (1974) as:

\[
\Lambda = \frac{U_{\text{max}} - U_{\text{min}}}{(\partial U/\partial y)_{\text{max}}} = \frac{\Delta U}{(\partial U/\partial y)_{\text{max}}}
\]  

The results from the present investigation are plotted alongside the results of these workers in Figure 7.10. The continuous line in this figure represents the growth of a plane mixing layer as determined by Johnson (1990). The growth rate of the separated shear layers is not linear as it is with a plane mixing layer; the initial growth rate is higher than in the plane mixing layer, but decreases in the second half of the separation bubble before suddenly increasing again around reattachment. Presumably the sudden departure in growth rates of the two types of shear layer around reattachment is associated with the impingement process and the subsequent slow development into a turbulent boundary layer.

7.5 Near-Wall Velocity Measurements

Preliminary work included near-wall measurements in a separation bubble that was not symmetrical on either side of the splitter plate: the separation bubble on the upper side was about 10% larger than in later work. In this flow, measurements were carried out at downstream locations \( x/X_r = 0.24, 0.36, 0.48, 0.60, 0.72, 0.84, 0.96 \) and 1.20, based on the unperturbed reattachment length. Later work in the symmetrical bubble included near-wall measurements at downstream locations \( x/X_r = 0.24, 0.48, 0.72, 0.96 \) and 1.20. In this section, the results presented will be from the symmetrical bubble, except at downstream locations \( x/X_r = 0.36, 0.60, 0.84 \). However, comparison of near-wall velocity profiles at the other downstream locations for both separation bubbles showed very good agreement, and consequently it is believed the presented data from the longer bubble would not be significantly different had the measurements been made in the symmetrical flow.

When discussing the longitudinal variation of near-wall variables, it is useful to have a coordinate system that follows the direction and development of the mean flow. Such a
coordinate system was proposed by Adams Johnston & Eaton (1984). The downstream location is defined as:

\[ \bar{X} = \frac{(X_r - x)}{X_r} \]

(7.2)

so that \( \bar{X} \) is zero at the reattachment position. Using this parameter, the development of the maximum reversed-flow velocity determined during the present investigation is compared with results from a backward-facing step flow of Adams, Johnston & Eaton (1984), a normal fence and splitter plate flow of Ruderich & Femholz (1987) and an axisymmetric backward-facing step flow of Devonport & Sutton (1991) in Figure 7.11. In this figure the value of the maximum reversed velocity at a given downstream location, \( U_{nmax} \), has been normalised by the maximum reversed velocity in the flow, \( U_{nmax} \). For the backward facing step flow, this value is around 0.2 \( U_{ref} \) for the sudden pipe expansion it is around 0.16 \( U_{ref} \) and for the fence and splitter plate arrangement it is around 0.3 \( U_{ref} \).

Agreement between the sets of data made in the two-dimensional backward-facing step flow and in the fence and splitter plate arrangement is very good, particularly in the second half of the separation bubble. The downstream location of the maximum reversed velocity for these geometries is also shared at around \( \bar{X} = 0.45 \) \( (x/X_r = 0.55) \). The results from the axisymmetric backward-facing step flow suggest that the maximum reversed velocity for this configuration occurs slightly further upstream at \( \bar{X} = 0.5 \) \( (x/X_r = 0.5) \), although this is based on a very limited number of data points. Unfortunately, the profiles of maximum reversed velocity taken in the two-dimensional and the axisymmetric backward-facing step geometries do not extend as far upstream as the measurements made in the fence and splitter geometry. It is perhaps in this region where differences may be expected, as the separation process in the step-type geometries is very different from the fence geometry.

The broad similarity between these profiles suggests that the near-wall behaviour of the flow beneath separation bubbles, generated using different flow geometries, may have certain universal characteristics, at least in the sense of mean velocities. Unfortunately, the paucity of reliable near-wall measurements has meant that very few comparisons of results made in different kinds of separation have been made. To investigate this point further, near-wall profiles of axial velocity from the present work are plotted in Figure 7.12, along with those of Adams, Johnston & Eaton (1984) and Devenport & Sutton (1991). In this figure, the axial velocity measurements have been scaled on the local maximum reversed velocity, \( U_{nmax} \), and the vertical position has been scaled on the height at which \( U_n \) occurs, \( y_n \). The degree of collapse, particularly between the data from the present investigation with that of Devonport & Sutton, certainly adds weight to the argument that there are certain characteristics of the reversed flow beneath a separation bubble that are universal.
Adams Johnston & Eaton (1984) also point out that a collapse of the profiles scaled in this way can be used to infer information about the skin friction coefficient. The collapse of the profiles in Figure 7.12 suggest that:

\[ \frac{U}{U_N} = f\left(\frac{y}{y_N}\right) \]  

(7.3)

and since

\[ U_t^2 = v \frac{\partial U}{\partial y}_{y=0} \]  

(7.4)

it is possible to write:

\[ U_t^2 = vU_N \left[ f'\left(\frac{y}{y_N}\right)\right]_{y=0} \left(\frac{1}{y_N}\right) \]  

(7.5)

If \[ f'\left(\frac{y}{y_N}\right)\] is some constant \( K \), say, then:

\[ U_t^2 = K \frac{vU_N}{y_N} \]  

(7.6)

Rearranging this expression gives

\[ C_{f_N} = 2 \left(\frac{\text{Re}_N}{K}\right)^{-1} \]  

(7.7)

where \( C_{f_N} = 2\tau_w / \rho U_N^2 \) and \( \text{Re}_N = U_N y_N / \nu \).

The measurements from Devonport and Sutton (1991) seem consistent with this relationship as can be seen in Figure 7.13. However, the results of Adams Johnston & Eaton are best described by the relationship:

\[ C_{f_N} \propto \text{Re}_N^{-0.5} \]

During the present investigation, measurements of shear stress were not made in the unswept flow. However, they were made in the spanwise-invariant region of the 10 degree swept flow, and work by Wolf (1987) suggests that for such a sweep angle, the component of skin friction in the direction normal to the fence is approximately independent of sweep angle.
provided the component of free-stream velocity normal to the fence is used to non-dimensionalise. For the present work, this implies that the component of shear stress in the direction normal to the fence in the unswept flow should be a factor of \((1/\cos^2(10^\circ))\) higher in magnitude than those measured in the swept flow. These scaled values of shear stress have permitted the plotting of \(C_{fN}\) against \(Re_N\) in the unswept flow, and this has been done in Figure 7.13. The results from the present investigation are approximately described by the relationship:

\[
C_{fN} \propto Re_N^{-0.8}
\]

Since the curve of \(U\) against \(y\) is fairly flat around the position at which the maximum reverse velocity occurs, errors associated with values of \(y_N\) and consequently \(C_{fN}\) may be substantial. However, these errors are probably not large enough to explain the differences in slope of \(C_{fN}\) against \(Re_N\) shown in Figure 7.13. If the variation in slope of the various data sets is genuine, it suggests that the relationship expressed in Equation 7.3 is not a universal scaling law for the backflow immediately above the surface.

Several workers have also plotted skin friction coefficient, \(C_{fN}\), in the second half of the separation bubble against a Reynolds number based on the local maximum reversed velocity, \(U_N\), and the distance \((X-x)\). The results from the present investigation along with those of Castro & Haque (1987) and Adams, Johnston & Eaton (1984) are plotted in this way in Figure 7.14. Also plotted in this Figure are the minimum \(C_f\) values of several other workers, renormalised using the minimum negative velocities occurring above the surface at the appropriate axial location, and plotted against the Reynolds number based on that velocity and the distance between the minimum \(C_f\) position and \(X_r\).

The data from all geometries collapse reasonably well, and it is pleasing that the results from the present investigation at several downstream locations collapse on to approximately the same line having a slope with a value of around -0.5. For the flow of Adams, Johnston & Eaton (1984), the fact that \(C_{fN} \propto Re_x^{-0.5}\) and \(C_{fN} \propto Re_N^{-0.5}\) means that \(y_N\) must be proportional to downstream position in the downstream half of the separation bubble. However, because the results of the present investigation suggest that \(C_{fN} \propto Re_N^{-0.8}\) and \(C_{fN} \propto Re_x^{-0.5}\) this relationship is obviously not universal for all separated flows.

Using the skin friction values inferred from the swept flow, the near-wall velocity data can also be plotted in wall coordinates. This is done in Figure 7.15 and the linear and logarithmic regions of a standard turbulent boundary layer are also plotted for reference. Nearer the wall
than the log law region, Adams, Johnston & Eaton (1984) and Devonport and Sutton (1991) have shown that the near-wall velocity profiles in the recirculating region should collapse on the $U^+ = y^+$ line. It is believed that the points in Figure 7.15 that lie above this line are probably the result of experimental error. The most likely cause of these errors is associated with the measurement of the distance from the wall. Very close to the wall, any displacement errors caused by, say, slight bowing of the pulsed-wire will have a significant effect.

Outside the viscous sublayer, data at all downstream locations considered during this investigation lie well below the normal turbulent log law, even downstream of reattachment at $x/X_r = 1.2$. As pointed out by Chandrsuda & Bradshaw (1981), qualitative use of mixing length arguments shows that such a dip below the log law - a smaller velocity gradient for a given shear stress - implies that the lengthscale of the near-wall flow is larger than in a standard turbulent boundary layer. Figure 7.15 also shows that no collapse of the near-wall velocity profiles is observed if wall coordinates are used.

Because the law of the wall does not apply to the near-wall flow under a separation, attempts have been made to develop near-wall models for use in calculation methods. The most successful of these was suggested by Simpson (1983) and details of the model are given in section 2.1.5 of the literature survey chapter of the present thesis. The final equation proposed by Simpson is given by:

$$\frac{U}{U_N} = A \left( \frac{y}{y_N} - \ln \frac{y}{y_N} - 1 \right)^{-1} \quad \text{(for } y > 0.02 y_N) \quad (7.8)$$

where $A$ is an empirical constant, that Simpson’s results suggest should be around 0.3. Using this value for the constant, the near-wall results from the present investigation are plotted against this equation in Figure 7.16. In the range $0.02 < y / y_N < 1$ the agreement between the experimental results and Simpson’s curve is exceptionally good. These limits to the vertical range, over which the equation can usefully be applied, agree with those found by Simpson (1983), Adams, Johnston & Eaton (1984) and Devonport & Sutton (1991) although for the results of the latter it was found that the equation fitted the experimental data better if $A = 0.4$ was used.
7.6 Reynolds Stress Measurements

Detailed investigations of the turbulence structure of a separated flow generated using a normal fence with splitter plate have been made by numerous workers. However, Reynolds stress measurements made by these workers, even those that used the more reliable pulsed-wire anemometry, did not concur even though the mean flow parameters were in reasonably good agreement. This disparity was thought to be associated with end effects in the nominally two-dimensional flow, but a detailed examination of the effect of aspect ratio on the Reynolds stresses by Hancock & Castro (1993) found this not to be so even for flow widths as narrow as one attachment length. It was not until Hancock (1994) carried out an investigation of Reynolds number effects on the Reynolds stresses that the reason for the poor agreement between the various workers’ results was explained.

A strong systematic variation of $\overline{u^2}$ with Reynolds number was found and this is summarised in Figure 7.17. Also plotted in this Figure are the results from the present investigation with the 5mm and 10mm high fence. It is reassuring to see that both results broadly follow the same trend as the results of Hancock (1994) and Ruderich & Femholz (1986). The Reynolds stress measurements from these investigations can also be plotted together using mixing layer scaling as suggested by Hancock (1994). For a mixing layer, the non-dimensional axial distance is $x/\theta_0$, where $\theta_0$ is the momentum thickness at separation, and the axial velocity is scaled on the velocity difference $(U_{\text{max}} - U_{\text{min}})$ denoted by $\Delta U$, where $U_{\text{max}}$ and $U_{\text{min}}$ are the maximum and minimum of $U(y)$. Now, for the laminar boundary layer on the front of the fence, it would be expected that $\theta_0/h_f \propto Re_{h_f}^{-0.5}$, and so the mixing layer non-dimensional length, $x/\theta_0$, becomes:

$$\frac{x}{h_f} Re_{h_f}^{0.5}$$  \hspace{1cm} (7.9)

$\overline{u^2}_{\text{max}}$, normalised by $(\Delta U)^2$ from the present investigation along with the results of several other workers is plotted against this parameter in Figure 7.18. The mixing layer scaling cannot be expected to work near to or downstream of $x/X_f=1$ and so only data within the length of the separation bubble is plotted. Some of the results of Hancock (1994) were taken at one downstream position with different free-stream velocities to change the axial scaling parameter, and some are from experiments where the free-stream velocity was kept constant and the downstream station was varied.
Most of the data from this investigation seem to collapse onto the solid line shown in the figure. However, the results of the present investigations and those of Hancock & McCluskey (1997), which were obtained in flows where the free-stream velocity was kept constant and the axial position was changed, seem to indicate that there is a systematic trend at low values of $x \text{Re}_{h_f}^{0.5}/h_f$. This of course implies that there are other parameters that must be important in the scaling of the downstream position that are not accounted for by the simple mixing layer scaling.

Unfortunately, the work carried out by Hancock (1994) was a preliminary study, and consequently only the Reynolds number effects on the axial stress, $\overline{u^2}$, were considered. The table below showing the maximum in the values of the various Reynolds stresses at around $x/X_r=0.5$ suggest that although the variation of $\left(\overline{u^2}/U_{ref}^2\right)_{\max}$ with Reynolds number is monotonic, the variation of the other Reynolds stresses is not.

<table>
<thead>
<tr>
<th>worker</th>
<th>$\left(\overline{u^2}/U_{ref}^2\right)_{\max}$ at $x^*=0.5$</th>
<th>$\left(\overline{v^2}/U_{ref}^2\right)_{\max}$ at $x^*=0.5$</th>
<th>$\left(\overline{w^2}/U_{ref}^2\right)_{\max}$ at $x^*=0.5$</th>
<th>$\left(-\overline{uv}/U_{ref}^2\right)_{\max}$ at $x^*=0.5$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ruderich &amp; Fernholz</td>
<td>0.13</td>
<td>0.03</td>
<td>0.047</td>
<td>0.020</td>
</tr>
<tr>
<td>(Re$_{h_f}$=14000)</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Castro &amp; Haque</td>
<td>0.092</td>
<td>0.064</td>
<td>0.064</td>
<td>0.027</td>
</tr>
<tr>
<td>(Re$_{h_f}$=11000)</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>present</td>
<td>0.063</td>
<td>0.025</td>
<td>0.039</td>
<td>0.022</td>
</tr>
<tr>
<td>(Re$_{h_f}$=3800)</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Hancock &amp; McCluskey</td>
<td>0.052</td>
<td>0.036</td>
<td>0.045</td>
<td>0.011</td>
</tr>
<tr>
<td>(Re$_{h_f}$=1600)</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

Table 7.1 Maximum Reynolds stresses at various Reynolds number

However, it is known that the measurements of $\overline{v^2}$ and $\overline{uv}$ made during the investigations of Hancock & McCluskey and Castro & Haque, were performed using the pulsed-wire probe with the sensor wires held parallel to the splitter plate and separated vertically by a distance that depended on the offset angle of the probe and the probe angles used. As discussed in section 6.1 of this thesis, measurements of Reynolds stresses using this probe orientation can lead to significant errors. Particularly the results of the investigation by Hancock &
McCluskey, that used a very small fence \((h=3.6\text{mm})\) and consequently resulted in strong vertical velocity gradients, should be viewed with caution.

Profiles of \( \overline{u^2} \), \( \overline{v^2} \), \( \overline{w^2} \), \( -\overline{uv} \), and \( k \) non-dimensionalised using \( U_{ref}^2 \), at five downstream locations, are shown in Figures 7.19, 7.20, 7.21, 7.22, and 7.23. The peak values of the Reynolds stresses are given in Figure 7.24. The measurements of \( \overline{v^2} \) and \( -\overline{uv} \) were made in a separation bubble that was about 10% longer than the symmetrical bubble used for the other Reynolds stress measurements. However, peak values of axial stress made in the longer bubble are typically only around 5% higher than the preferred data set from the symmetrical flow and so it is believed that the presented profiles of \( \overline{v^2} \) and \( -\overline{uv} \) would not have been greatly different had they been made in a symmetrical bubble. The figure showing the \( -\overline{uv} \) profiles also includes wall values measured using the shear stress probe (determined from the measurements in the invariant region of the swept flow as already mentioned). The measurements made with the two probes certainly seem consistent with one another.

The peak values of axial stress, non-dimensionalised using \((\Delta U)^2\), at these five downstream locations are plotted in Figure 7.25. Included in this figure are results obtained using the smaller fence and results from other workers using the same geometry but at different Reynolds number. The three profiles at the lower Reynolds number share the same shape, with \( \overline{u_{max}^2}/(\Delta U)^2 \) being higher near separation and reattachment than in the centre of the bubble. These three profiles also suggest that over this Reynolds number range, Reynolds number effects seem to be higher near reattachment than elsewhere. At the higher Reynolds number, \( \overline{u_{max}^2}/(\Delta U)^2 \), rises all the way to reattachment in a monotonic fashion.

The downstream variation in the maximum value of all the Reynolds stresses, normalised by \((\Delta U)^2\) is given in Figure 7.26. The asymptotic values that occur in a plane mixing layer are also included (taken from Johnson, 1990). Numerous workers have compared Reynolds stress measurements made in a separated shear layer with those from a plane mixing layer. However, because of the Reynolds number dependence of the stresses in the separated shear layer, the level of agreement between results from the two types of flow has varied widely as the range of Reynolds numbers considered by previous workers has spanned approximately an order of magnitude. Consequently, it must be emphasised that comparisons of the stresses made in the present investigation with the plane mixing layer results of Johnson (1990) are specific to this investigation. Any agreement between the peak Reynolds stresses at the various downstream locations with those in the plane mixing layer is fortuitous.
As with $\overline{u^2}_{\text{max}} / (\Delta U)^2$, both $\overline{v^2}_{\text{max}} / (\Delta U)^2$ and $\overline{w^2}_{\text{max}} / (\Delta U)^2$ reach a minimum value at about $x/X_j=0.5$ before rising to around the reattachment point where they begin to fall again. At all downstream positions, $\overline{u^2}_{\text{max}} / (\Delta U)^2$ is higher than $\overline{w^2}_{\text{max}} / (\Delta U)^2$ which is in turn higher than $\overline{v^2}_{\text{max}} / (\Delta U)^2$. The peak values of $\overline{u^2}$, $\overline{w^2}$ and $-\overline{uv}$ close to the centre of the bubble are very close to the plane mixing layer data, but the peak in $\overline{v^2}$ is only around 60% of the asymptotic value for this stress found by Johnson. This may be associated with the effect of the splitter plate which would tend to restrict fluctuations in the direction normal to the fence, or may be indicative of measurement error.

Profiles of the Reynolds stresses are also plotted in Figures 7.27, 7.28, 7.29, 7.30 and 7.31 with the corresponding asymptotic profiles from the plane mixing layer data of Johnson (1990). In these profiles, the vertical scale is $\eta = -(y - y_c)/\Lambda$. The vorticity thickness, $\Lambda$, has already been discussed in section 7.4 of this chapter, and $y_c$ is the vertical height above the splitter plate at which the mean velocity is $(0.61 \Delta U + U_{\text{min}})$. By analogy with the plane mixing layer, this represents the centre of the separated shear layer. The locus $\eta=0$ can be seen in Figure 7.7. Since the measurements of $\overline{v^2}$ and $-\overline{uv}$ were made in a separation bubble that was around 10% longer than the symmetrical bubble, the values of $y_c$ and $\Lambda$ used in the vertical scaling of these profiles is slightly different to those used for the other Reynolds stress profiles.

Any similarities between the separated shear layer and the plane mixing layer are likely to take place on the high velocity side away from the influence of the flow near the splitter plate. This is indeed the case, although even here the Reynolds stresses at all downstream locations are higher in the separated shear layer than the mixing layer. As observed by Castro & Haque (1987), the low velocity side of the separated shear layer close to separation is very different from the low velocity side of the mixing layer. Castro & Haque suggest that this difference is consistent with the idea that the fluid in the near-wall region is likely to originate from the reattachment region rather than being typical of the extreme low velocity side of a plane mixing layer.
Fig. 7.1 Surface streamlines in the unswept flow close to the wind tunnel wall. F and S denote focal point and saddle point singularities, respectively.
Fig. 7.2 Surface streamlines in the unswept flow at the centre of the wind tunnel. F and S denote focal point and saddle point singularities, respectively.
7. Unswept Separation

Fig. 7.3 Surface streamline pattern for unswept flow

F and S denote focal point and saddle point singularities, respectively.

Fig. 7.4 Static pressure distribution on the splitter plate
7. Unswept Separation

Fig. 7.5 Axial velocity profiles at various downstream positions

Fig. 7.6 Mean vertical velocity profiles at five downstream locations
7. Unswept Separation

Fig. 7.7 Mean streamlines in the unswept flow (note different scales for x and y)

Fig. 7.8a Mean velocity measurements at around x/Xr=0.25 using Xr to non-dimensionalise
Fig. 7.8b Mean velocity measurements at around $x/X_r=0.5$ using $X_r$ to non-dimensionalise

Fig. 7.8c Mean velocity measurements at around $x/X_r=0.75$ using $X_r$ to non-dimensionalise
Fig. 7.8d Mean velocity measurements at around $x/X_r=1.0$ using $X_r$ to non-dimensionalise.

Fig. 7.8e Mean velocity measurements at around $x/X_r=1.25$ using $X_r$ to non-dimensionalise.
7. Unswept Separation

Fig. 7.9a Mean velocity measurements at around $x/X_r=0.25$ using $H_b$ to non-dimensionalise.

Fig. 7.9b Mean velocity measurements at around $x/X_r=0.5$ using $H_b$ to non-dimensionalise.
7. Unswept Separation

Fig. 7.9c  Mean velocity measurements at around \(x/X_r=0.75\) using \(H_b\) to non-dimensionalise

Fig. 7.9d  Mean velocity measurements at around \(x/X_r=1.0\) using \(H_b\) to non-dimensionalise
Fig. 7.9e Mean velocity measurements at around $x/Xr=1.25$ using $H_b$ to non-dimensionalise

Fig. 7.10 Development of the vorticity thickness with downstream position
7. Unswept Separation

**Fig. 7.11** Distribution of maximum reverse flow velocity within the separation

**Fig. 7.12** Near wall velocity profiles within the separation bubble
Fig. 7.13 Variation of skin friction coefficient, $C_{Fn}$, with wall-layer Reynolds number, $Re_N$

Fig. 7.14 Wall-friction in second half of the separation bubble
Fig. 7.15 Near wall velocity profiles plotted in wall coordinates

Fig. 7.16 Near wall velocity profiles within the separation bubble compared to Simpson (1983)
Fig. 7.17 Variation of maximum $u^2$ near reattachment with Reynolds number

$$0.06 \log(Re_{hf}) - 0.15$$

Fig. 7.18 Variation of maximum $u^2$ in mixing layer variables
Fig. 7.19 $u^2$ profiles at five downstream locations

Fig. 7.20 $v^2$ profiles at five downstream locations
7. Unswept Separation

Fig. 7.21 $\bar{w}^2$ at five downstream locations

Fig. 7.22 $\bar{uv}$ profiles at five downstream locations
Fig. 7.23 $k$ profiles at five downstream locations

Fig. 7.24 Development of maximum Reynolds stresses normalised by $U_{ref}^2$
7. Unswept Separation

Fig. 7.25 Downstream development of maximum $u^2$ at various Reynolds number

Fig. 7.26 Development of maximum Reynolds stresses normalised by $(\Delta U)^2$ (Re=3800)
7. Unswept Separation

Fig. 7.27 $\bar{u}^2$ profiles at five downstream locations compared to plane mixing layer values

Fig. 7.28 $\bar{v}^2$ profiles at five downstream locations compared to plane mixing layer values
Fig. 7.29 $\overline{w^2}$ profiles at five downstream locations compared to plane mixing layer values

Fig. 7.30 $\overline{uv}$ profiles at five downstream locations compared to plane mixing layer values
Fig. 7.31 $k$ profiles at five downstream locations compared to plane mixing layer values.
8. Spanwise-Invariant Region of the Swept Flow

8.1 Surface Flow Visualisation

Flow visualisation studies with the unswept fence suggested that the aspect ratio, $W/h_p$, for this flow had to be around 95 for a spanwise-invariant region to begin to exist. The decision to use the larger fence, $h_f=10\text{mm}$, for both the unswept and swept flow, because of reasons discussed in Chapter 6, meant that the aspect ratio of each arm of the v-configuration fence was only 78. Consequently, it was important to carry out flow visualisation experiments with this geometry to assess the extent, if any, of a spanwise-invariant region in these two side flows. Generally, results in this chapter will be presented in axes aligned with the fence as opposed to tunnel axes (i.e. $x'$, $z'$ rather than $x$, $z$).

Scaled photocopies ($\times 0.707$) of surface streamlines from the swept flow in the region close to the tunnel wall, close to the spanwise-invariant region and close to the plane of symmetry are given in Figures 8.1, 8.2 and 8.3, respectively. An interpretation of the entire surface streamline pattern on the splitter plate behind the swept fence is given in Figure 8.4. As with the unswept flow, there is a width of approximately 30 fence heights next to the wind tunnel walls that is characterised by a curved attachment line, and a corner vortex. Also, there is an approximately equal width next to the axis of symmetry that is dominated by the three-dimensional region, where the two lateral flows meet.

Assessing the spanwise invariance of the remaining portion is very difficult, especially as the flow visualisation experiments performed poorly in the low shear stress region close to the fence. However, it was decided that the $z=380\text{m}$ plane, that lies at the centre of this region would be used to obtain measurement with which to compare data from the unswept flow. Analysis of Figure 8.2 and 8.4 suggests that the reattachment streamline at this lateral station is approximately parallel to the separation line, indicating invariance with lateral position, $z'$. A line of constant $z$ rather than $z'$ was chosen for the spanwise-invariant region simply because it was easier to traverse the probe in the $x$ rather than the $x'$ direction and because all of the slots in the splitter plate were in the streamwise direction. This did mean that different downstream locations in the spanwise-invariant region were taken at slightly different $z'$ positions, but this is believed to be unimportant as the flow is reasonably spanwise-invariant over this lateral range. In the spanwise-invariant region, the relative downstream position within the bubble, $x/X_r(x^*)$, will be the same as the relative chordwise position $x'/X_r'$. Analysis of the streamline pattern at this lateral station suggests that the length of the bubble, measured normal to the fence, was approximately $20.7h_f$ (around 3% shorter than the
unswept separation bubble). Unfortunately, measurements with the field probe and the through-wall probe were made at relative downstream positions, $x/X_r$, based on an incorrect value of $X_r$. The reattachment length based on the downstream position at which $\partial U/\partial y = 0$ rather than $\partial U'/\partial y = 0$ was used for these measurements, which resulted in a bubble length that was about 98% of the true value. Both definitions of the reattachment length can be seen in Figure 8.2. In addition to this error, the desired downstream locations of $x/X_r=0.25, 0.5, 0.75, 1.0$ and $1.25$ were based on the reattachment length determined with the field probe. The presence of the field probe around reattachment caused a shortening of the separation bubble of around 3%. Consequently, the relative chordwise locations considered for the measurements in the spanwise-invariant flow (with the field probe and the through-wall probe) were approximately $x'/X_r'=0.24, 0.48, 0.71, 0.95$ and $1.19$ based on the unperturbed reattachment length. However, for ease of comparison with the unswept data, figures of $0.25, 0.5, 0.75, 1.0$, and $1.25$ will often be used. For any other scaling purposes, the unperturbed reattachment length will be used.

8.2 Static Pressure Measurements

Several workers have shown that for mild sweep angles such as 10 degrees, profiles of the chordwise development of static pressure, non-dimensionalised using the dynamic head based on the free-stream velocity component in the direction normal to the separation line ($U_0$) should collapse with unswept results. Data from the invariant region of the swept flow is plotted with the results from the unswept flow in this way in Figure 8.5. The general character of the two curves is the same but in the unswept flow, the minimum pressure coefficient is approximately 10% lower than in the swept flow and the position at which it occurs is slightly further upstream. The pressure coefficient at reattachment is the same for both flows.

Wolf (1987) and McCluskey, Hancock & Castro (1991) showed that the static pressure beneath a swept separation bubble is extremely sensitive to any three-dimensional effects from the wind tunnel walls. The results from the investigation of Wolf suggest that for a 10 degree swept fence, the chordwise distribution of static pressure will only be independent of lateral position at around $2.5X$, away from the tunnel walls. For the present investigation, the pressure tappings were only around 1.8 attachment lengths away from the tunnel wall. However, since the work of Wolf suggests that minimum pressure becomes lower with proximity to the wall, and the present results suggest that the pressure readings in the swept flow are higher than they should be, it seems unlikely that the $z=380$mm plane is suffering from three-dimensional effects associated with the wall.
Given that the minimum static pressure reading becomes more negative away from the plane of symmetry (discussed in section 9.3), it is possible that the reason the minimum pressure in the spanwise-invariant region is not as low as expected may be associated with three-dimensional effects arising from the central region of the v-configuration fence. This point will be discussed further in following sections.

8.3 Wall Shear Stress Measurements

Distributions of the chordwise, $x'$, and spanwise, $z'$, components of the skin friction are given in Figure 8.6. The reattachment length determined from the position at which the chordwise component of skin friction is zero is in very good agreement with flow visualisation experiments at around $x' = 20.7h_r$. The position of the secondary separation line, measured normal to the fence, appears to be around $x' = 3.5h_r(0.17X'_r)$. In the unswept flow, near-wall measurements at $x'/X'_r = 0.125$ showed that this downstream location was still within the primary reversed flow region, and so it appears that the secondary separation bubble is longer for mildly swept separations than for the unswept flow.

Skin friction measurements of Wolf (1987), made behind a bluff plate, showed that the chordwise position of the minimum value of the chordwise component of skin friction was insensitive to sweep angles up to 45 degrees; the value found in this work was around $x'/X'_r = 0.64$. It was also shown that the chordwise position at which the minimum in the spanwise component of skin friction occurred did vary with sweep angle. At sweep angles of 15 and 30 degrees, the minimum took place at around the reattachment position, and at higher angles it took place some distance downstream of this station. For the present flow the position at which the minimum value of the chordwise component of skin friction agrees well with the work of Wolf at around $x'/X'_r = 0.60$. The minimum value of the spanwise component of skin friction in the spanwise-invariant region took place at around the reattachment position.

8.4 Mean Velocity Measurements

Profiles of chordwise velocity, non-dimensionalised using the component of free-stream velocity normal to the fence, at five locations for the swept and unswept flow are given in Figures 8.7a-e. The negative values of $U'$ in the near-wall region of the swept flow in Figure 8.7d are a consequence of $X_r$ being taken at the position at which $\partial U'/\partial y = 0$ rather than $\partial U'/\partial y = 0$. 
In general, it appears that differences between the chordwise velocity profiles in the two flows arise from the vertical scaling. Despite the fact that the swept separation bubble is around 4% shorter than the unswept separation, all the velocity profiles of Figures 8.7a-e, indicate that the height of the swept separation is greater. The chordwise variation in height of the separation bubble, $H_b$, defined as the vertical location at which $U' = 0.95U_{\text{max}}'$, is shown in Figure 8.8. In this figure the chordwise measurement positions, $x'/X_r'$, based on the unperturbed flow have been used. Although the height of the separation bubble for the two flows is approximately the same close to separation, around reattachment the swept bubble is about 12% higher than its unswept counterpart. If the chordwise velocity profiles are replotted using the local value of the bubble height to scale the height off the splitter plate, the degree of collapse between the swept and unswept data is greatly improved. These profiles can be seen in Figures 8.9a-e.

It was initially believed that this change in the shape of the swept separation bubble may have been a consequence of the flow not being totally two-dimensional. As will be discussed in the following chapter, the separation in the fully three-dimensional region close to the plane of symmetry is characterised by a change in the size and shape of the bubble. However, a comparison of the chordwise development of the mixing layer centreline, $y_c$, between an unswept separation and one swept at 25 degrees made by Hancock & McCluskey (1997) also showed that $y_c$ was approximately 10% larger in the swept flow.

For the invariant region of the swept flow the centreline of the shear layer is defined as the height at which the chordwise velocity, $U'$, is $0.67\Delta U' + U_{\text{min}}'$ where $\Delta U' = U_{\text{max}}' - U_{\text{min}}'$. The variation of this height with chordwise position for the present swept flow is compared with the unswept results in Figure 8.10. As with the height of the bubble, $H_b$, the height of the shear layer centreline for the two flows is very similar close to separation, but the difference increases with chordwise location. Around reattachment, $y_c$ is approximately 10% larger for the swept flow than the unswept case. It appears that although the attachment length is relatively insensitive to sweep angle, the vertical extent of the separation is not.

Although the chordwise development of the height of the shear layer centreline is not the same for the two flows, Hancock & McCluskey (1997) found that the chordwise development of the thickness of the shear layer (measured using the vorticity thickness, $\Lambda$) was insensitive to the introduction of a sweep angle of 25 degrees. The chordwise vorticity thickness is defined as:
\[ \Lambda' = \frac{U_{\text{max}}' - U_{\text{min}}'}{(\partial U'/\partial y)_{\text{max}}} = \frac{\Delta U'}{(\partial U'/\partial y)_{\text{max}}} \]  

The chordwise growth of the shear layers for the unswept and swept flow is shown in Figure 8.11, where it can be seen that the behaviour of the two flows is very similar. In terms of the chordwise development of the shear layer, the effect of introducing the sweep angle has been to alter the position of the shear layer without greatly affecting the shear layer itself. The reason for this shift in position is not known. However, since the separation bubble has become shorter and higher, thus decreasing the radius of curvature of the flow, it may be anticipated that if stabilising streamline curvature plays a part in the turbulence structure, turbulent kinetic energy may be lower for the swept flow.

Near-wall profiles of chordwise velocity in the two flows at three chordwise locations are given in Figure 8.12. In this figure, the chordwise velocity has been non-dimensionalised using the maximum reversed velocity, \( U_N' \), and the height has been scaled using the vertical position at which this velocity occurs, \( y_N \). The chordwise development of \( y_N/X_r' \) and \( U_N'/U_0 \) in the unswept flow and in the spanwise-invariant region of the swept flow can be seen in Figure 8.13 and 8.14, respectively. Figure 8.12 suggests that as with the unswept flow, the near-wall velocity profiles scaled in this way, collapse at different downstream locations within the swept flow. Furthermore the level of collapse with the profiles from the unswept flow is very good. This suggests that the near-wall model suggested by Simpson (1983), developed for use in calculation methods of unswept separated flows, may also be applicable to the near-wall chordwise velocity profiles of swept flows.

Profiles of vertical velocity, \( V \), at approximately the same five chordwise locations for the two flows are shown in Figures 8.15a-e. At approximate chordwise stations \( x'/X_r' = 0.25 \), 1.0 and 1.25, the profiles are very similar for the two flows. At the other chordwise locations, \( x'/X_r' = 0.5 \) and 0.75, there are differences in the behaviour of the profiles from the two types of flows that are probably too large to be the result of measurement error. As the chordwise development of \( U' \) is so similar for the two flows, this difference in the vertical variation of \( V \) may be associated with an insufficient aspect ratio for the swept flow and the resulting lateral gradients of \( W' \).

Profiles of lateral velocity, \( W' \), are given in Figure 8.16. In this figure the lateral velocity is normalised using the component of free-stream velocity in the direction parallel to the fence, \( W_0 \). Broadly, the lateral velocity is invariant with chordwise or vertical position, except very close to the splitter plate where the lateral velocity obviously goes to zero. In agreement with
the work of Hancock & McCluskey (1997), the bulk of the flow is convected sideways at a roughly uniform velocity, \( W_0 \).

However, there are subtle differences from the work of Hancock & McCluskey. At around \( x'/X_r = 0.25 \), the present results suggest that in the region \( 2<y/h_f<3.5 \), there is a distinct dip in the profile of \( W''(y) \). However, this sudden change in \( \partial W'/\partial y \) at \( x/X_r = 0.25 \) becomes even more pronounced in the profiles of \( W''(y) \) for the lateral stations in the three-dimensional region of the \( v \)-configuration fence (not presented). Consequently, it seems likely that this feature is associated with the limited aspect ratio of the present investigation and residual three-dimensional effects from the central region.

Other features of the swept flow can be seen if wind tunnel axes are considered. Vertical profiles of lateral velocity in the direction normal to the wind tunnel wall, \( W \), at five chordwise locations are shown in Figure 8.17. A particularly interesting feature of these profiles is the positive \( W \) in the outer part of the separation. This is an inviscid phenomenon and is a result of the flow accelerating in the \( x' \) direction because of the blockage effect of the separation process. This increase in \( U' \) gives a positive contribution to \( W \). In the work of Hancock & McCluskey, this feature was not observed. However, in order to capture the small positive flow angles in the outer part of the separation, it is necessary to have an accurate 'zero' reference angle in the flow upstream of the separation process. It is believed that for the work of Hancock & McCluskey, this was probably not performed adequately and the profiles of \( W \) were forced to zero at some height above the splitter plate.

Another approach to representing the streamwise and spanwise velocity data is to use 'polar' plots, suggested by Johnston (1960) for use in three-dimensional boundary layers. \( U \) is plotted against \( W \) in Figures 8.18 for five chordwise locations in the swept flow. Also plotted in these figures is the flow direction at the surface of the splitter plate measured using the shear stress probe. Except at \( x/X_r = 0.25 \), the near-wall flow angle predicted using the shear stress probe and the velocity probe at \( y/h_f = 0.0385 \) agree to within one degree. At \( x/X_r = 0.25 \), the shear stress values \( C_{fx} \) and \( C_{fx} \) have very small magnitudes (\( U \) and \( W \) are consequently also small values at the lowest velocity measuring station), and so small absolute errors made by either probe lead to large errors in flow direction (12 degrees at \( x/X_r = 0.25 \)).

Hancock & McCluskey (1997) found that \( U \) and \( W \) varied according to a 'velocity triangle' that Johnston observed for a three-dimensional boundary layer - i.e. that \( U \) and \( W \) were proportional to one another in an inner layer, implying that the flow is coplanar in this region, and that \( U \) and \( W \) varied linearly with one another in the outer part of the flow.
Furthermore it was found that this linear variation of $U$ with $W$ coalesced at different downstream locations. Although the present results support the findings of Hancock & McCluskey in terms of an approximately coplanar inner region, they do not imply a linear variation of $U$ with $W$ in the outer part of the flow, and they also suggest that the measurements in the outer part of the flow, plotted in polar form, do not precisely agree at different downstream locations.

In Figure 8.19, the consequence of using an incorrect reattachment length is apparent. The near-wall flow direction at the chordwise location that was thought to be the reattachment position is at 100 degrees to the $x'$-direction. Reference to the flow visualisation results of Figure 8.2 show that the actual reattachment line lies parallel with the separation line (90 degrees to the $x'$-direction). In terms of flow direction, Hancock & McCluskey (1997) suggest that the swept flow may be viewed in terms of three vertical layers: a near-wall region where the flow is approximately constant at the local wall value, a middle region where the flow direction changes substantially and an outer region where the flow direction is again almost constant, this time in the free-stream direction. The vertical extent of these inner, middle and outer layers proposed by Hancock & McCluskey (1997) were $0 < y < 0.15y_c$, $0.15y_c < y < y_c$ and $y > y_c$, respectively. Reference to Figure 8.19 suggests that these vertical layers are also broadly appropriate for the present results.

### 8.5 Reynolds Stress Measurements

In this section, all fluctuating quantities will be in axes aligned with the fence ($x'$, $y$ and $z'$ axes) and consequently the component of the free-stream velocity in the direction normal to the fence, $U_0$, will be used to normalise these. It should be remembered that for the unswept flow the $x'$ and $z'$ axes are the same as the $x$ and $z$ axes, respectively, and that $U_0$ is the same as $U_{ref}$. Profiles of $\overline{u'^2}$, $\overline{w'^2}$, $\overline{v'^2}$, $-\overline{uv}$ and $k$ are given in Figures 8.20, 8.21, 8.22, 8.23 and 8.24 for the swept invariant and the unswept flow, with the height scaled by $h_r$. The peak values of these profiles at the various chordwise locations are shown in Figure 8.25. Measurements of $\overline{u'w'}$ for the swept flow are not presented as it was found that in axes aligned with the fence, levels were not significantly larger than the typical scatter in the measurements made in the unswept flow, where values should be zero. As discussed in section 5.11 of this thesis, the measurements of $\overline{u'^2}$ presented in this chapter should have error bars of around $\pm5\%$ associated with them. The errors associated with the presented measurements of $\overline{v'^2}$, $\overline{w'^2}$, $\overline{uv}$ are around $\pm15\%$. 

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The differences between the profiles of $\overline{u'^2}$ in the two flows are negligible within the separation bubble. At around $x'/X_s' = 1.0$ and 1.25 the near-wall values made with the through-wall probe for the swept flow seem to be slightly higher than the unswept flow, but they are also higher than those indicated with the field probe at its lowest measuring station. Extrapolating the $\overline{u'^2}$ profile made with the field probe suggests that the near-wall values for the swept flow are higher than in the unswept flow but not to the extent suggested by the through-wall measurements. Even though the measurements in both flows should have error bars of around ±5% associated with them, the fact that both probes suggest that the near-wall stresses are higher in the swept flow suggest that this is a genuine difference between the two flows. The peak values of $\overline{u'^2}$ for the two flows are very similar at all chordwise positions considered.

The near-wall profiles of $\overline{w'^2}$ at $x'/X_s' = 1.0$ and 1.25 (Figure 8.21d and e) made with the through-wall probe are spuriously high when compared to measurements with the field probe. The measurements made with the field probe are felt to be more reliable as the maximum yaw angle for this probe was around 10 degrees higher than for the through-wall probe. Also, the through-wall probe was susceptible to developing loose pulsed-wires and sensor wires. In general, differences between profiles of $\overline{w'^2}$ made in the unswept and swept flow are largest at chordwise locations $x'/X_s' = 1.0$ and 1.25 where $\overline{w'^2}$ is higher for the swept flow.

The measurements of $\overline{w'^2}$ for the unswept flow are believed to be somewhat low because peak values of $\overline{u'^2}$ from the data set used to calculate $\overline{w'^2}$ are typically around 7% lower than the preferred data set. The preferred data set is a set of single orientation measurements whose peak values lay approximately between those determined from the multiple orientations in the $x$-$y$ and the $x$-$z$ plane. However, the fact that the profiles of $\overline{w'^2}$ in the two flows are very similar in the early part of the flow and become increasingly different with downstream position suggests that the broad difference in the behaviour of $\overline{w'^2}$ for the two flows is probably genuine. Interestingly, the difference in the profiles also extends to the high velocity side of the shear layers, suggesting that the differences may not solely be associated with the high values of $\partial W/\partial y$ that occur close to the splitter plate.

As pointed out by Hancock & McCluskey (1997), there is another significant difference between the unswept and the swept flows that may be expected to alter the turbulence structure of the two shear layers as well as the velocity gradient $\partial W/\partial y$ close to the splitter plate. For the swept flow, there is $x'$-wise as well as $z'$-wise vorticity fed into the shear layer.
at separation. The \( x' \)-wise component results from the non-zero value of lateral velocity gradient \( \partial W'/\partial n \) on the front and back faces of the fence (where \( n \) here is the normal distance from the vertical face of the fence). However, if the differences in \( \overline{w'^2} \) profiles at around \( x'/X' = 1.0 \) and 1.25 are genuine it seems unlikely that they arise because of the extra component of vorticity at separation for the swept flow, as \( \overline{w'^2} \) closer to separation has not been affected.

Comparisons of profiles of \(-\overline{uv}\) and \(\overline{v^2}\) made in the two flows and shown in Figures 8.22 and 8.23 will have to be made in the light of possible errors associated with these measurements. For the unswept flow, the measurements were performed in a separation bubble that was about 10\% longer than the symmetrical bubble (as discussed in Chapter 7) and a loose pulsed-wire is believed to have resulted in stress measurements that were slightly high (peak values of axial stress made in the longer bubble are typically around 5\% higher than the preferred data set from the symmetrical flow). For the swept flow, profiles of axial stress determined using the same five probe orientations to determine \(\overline{v^2}\) are typically between 5 and 10\% higher than the results obtained at zero degree orientation, indicating imperfect 'fits'. The presented shear stress results from the swept flow are also in tunnel axes and not fence axes. This is because measurements of \(\overline{v^2}\) and \(-\overline{uv}\) from the swept flow were determined from multiple orientation measurements made in the \(x-y\) and not the \(x'-y\) plane. However since rotation of axes gives:

\[
\overline{u'v} = \overline{uv}\cos 10 + \overline{vw}\sin 10 \quad (8.2)
\]

\[
\overline{vw'} = \overline{vw}\cos 10 - \overline{uv}\sin 10 \quad (8.3)
\]

it is possible to write

\[
\overline{u'v} = \frac{\overline{uv}}{\cos 10} + \frac{\overline{vw}}{\tan 10} \quad (8.4)
\]

Computational results have shown that values of \(\overline{vw'}\) are typically around -0.02\(\overline{uv}\). This means that for the swept flow, \(\overline{u'v} = 1.012\overline{uv}\). In the light of possible measurement errors from other sources, the presented shear stress data from the swept flow in tunnel axes may also be viewed as the data in fence axes.
Profiles of $v^2$ in the two flows are very similar in the second half of the separation bubble and downstream of reattachment. At around $x'/X_r' = 0.25$ and 0.5, on the other hand, peak levels are at least 50% higher in the swept flow. These differences are probably too large to be explained by errors in the measurements. However, with only the unswept and the moderately swept data to examine, possible reasons for the differences in the profiles of $v^2$ are difficult to assess. Since the differences are only apparent in the upstream half of the separation bubble, they may be related to the extra component of vorticity that is fed into the early part of the shear layer of the swept flow.

At approximate chordwise locations $x'/X_r' = 0.25$, 1.0 and 1.25, profiles of $\overline{uv}$ for the two flows are very similar over the entire vertical extent of the profiles. At $x'/X_r' = 0.5$ and 0.75 the profiles of $-\overline{uv}$ are similar on the high velocity side of the shear layer, but the peak value is significantly lower in the swept case particularly midway along the bubble where the peak shear stress in the swept flow is around 40% of the value in the unswept flow. However, this sudden departure in the levels of shear stress at this chordwise location is not seen in the chordwise development of the shear layer thickness, which is very similar for the two flows. This, and the knowledge that the shear stress measurements have large error bands associated with them suggests that the difference between the flows at this chordwise position may not be genuine.

Despite quite significant differences in measurements of $w'^2$ downstream of reattachment and in measurements of $v^2$ in the first half of the separation for the unswept and the swept geometry, which can be seen clearly in Figure 8.25, profiles of turbulent kinetic energy, $k$, are very similar for the two flows as can be seen in Figures 8.24a-e. Differences between the profiles from the two flows are largely caused by the increased height of the swept separation bubble. Peak values of $k$ at the various chordwise locations in the two flows are generally within 10% of one another.
Fig. 8.1 Surface streamlines in the swept flow close to the wind tunnel wall
F and S denote focal point and saddle point singularities, respectively
Fig. 8.2 Surface streamlines in the swept flow around the spanwise invariant region
Fig. 8.3 Surface streamlines in the swept flow around the plane of symmetry F and S denote focal point and saddle point singularities, respectively.
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.4 Surface streamlines behind v-configuration fence
F and S denote focal point and saddle point singularities, respectively

Fig. 8.5 Static pressure distribution in the spanwise invariant region and in the unswept flow
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.6 Profiles of the components of skin friction in the $x'$ and $z'$ direction
Fig. 8.7a Comparison of chordwise velocity profile at around $x^* = 0.25$ in the spanwise invariant region of the unswept flow and 10 degree swept flow.

Fig. 8.7b Comparison of chordwise velocity profile at around $x^* = 0.5$ in the spanwise invariant region of the unswept flow and 10 degree swept flow.
Fig. 8.7c Comparison of chordwise velocity profile at around $x^*=0.75$ in the spanwise invariant region of the unswept flow and 10 degree swept flow.

Fig. 8.7d Comparison of chordwise velocity profile at around $x^*=1.0$ in the spanwise invariant region of the unswept flow and 10 degree swept flow.
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.7e Comparison of chordwise velocity profile at around $x^* = 1.25$ in the spanwise invariant region of the unswept flow and 10 degree swept flow

Fig. 8.8 Chordwise development of the height of the unswept and swept separation bubbles
Fig. 8.9a Comparison of chordwise velocity profile at around $x^* = 0.25$ in the spanwise invariant region of the unswept flow and 10 degree swept flow.

Fig. 8.9b Comparison of chordwise velocity profile at around $x^* = 0.5$ in the spanwise invariant region of the unswept flow and 10 degree swept flow.
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.9c Comparison of chordwise velocity profile at around $x^*=0.75$ in the spanwise invariant region of the unswept flow and 10 degree swept flow

Fig. 8.9d Comparison of chordwise velocity profile at around $x^*=1.0$ in the spanwise invariant region of the unswept flow and 10 degree swept flow
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.9c Comparison of chordwise velocity profile at around $x^*=1.25$ in the spanwise invariant region of the unswept flow and 10 degree swept flow.

Fig. 8.10 Chordwise development of the shear layer centreline the unswept and swept separation bubbles.
Fig. 8.11 Development of the vorticity thickness with chordwise position

Fig. 8.12 Near-wall velocity profiles within the separation bubble
Fig. 8.13 Chordwise variation of $y_n/X_r'$ for the unswept flow and the spanwise invariant region of the swept flow

Fig. 8.14 Chordwise variation of $U_n/U_0$ for the unswept flow and the spanwise invariant region of the swept flow
Fig. 8.15a Comparison of vertical velocity profile at around $x^* = 0.25$ in the spanwise invariant region of the unswept flow and 10 degree swept flow

Fig. 8.15b Comparison of vertical velocity profile at around $x^* = 0.5$ in the spanwise invariant region of the unswept flow and 10 degree swept flow
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.15c Comparison of vertical velocity profile at around $x^*=0.75$ in the spanwise invariant region of the unswept flow and 10 degree swept flow

Fig. 8.15d Comparison of vertical velocity profile at around $x^*=1.0$ in the spanwise invariant region of the unswept flow and 10 degree swept flow
Fig. 8.15e Comparison of vertical velocity profile at around $x^*=1.25$ in the spanwise invariant region of the unswept flow and 10 degree swept flow.
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.16 Lateral velocity profiles at 5 chordwise locations in the spanwise invariant region (fence axes)

Fig. 8.17 Lateral velocity profiles at 5 chordwise locations in the spanwise invariant region (tunnel axes)
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.18 Polar plot of results in the spanwise-invariant region at five chordwise locations

Fig. 8.19 Flow direction at five chordwise locations (fence axes)
Fig. 8.20a Comparison of $u'^2$ profiles at around $x^*=0.25$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow

Fig. 8.20b Comparison of $u'^2$ profiles at around $x^*=0.5$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow
Fig. 8.20c Comparison of $u'^2$ profiles at $x^*=0.75$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow.

Fig. 8.20d Comparison of $u'^2$ profiles at around $x^*=1.0$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow.
Fig. 8.20e Comparison of $u'^2$ profiles at around $x^* = 1.25$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.21a Comparison of $\overline{w^2}$ profiles at around $x^*=0.25$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow.

Fig. 8.21b Comparison of $\overline{w^2}$ profiles at around $x^*=0.5$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow.
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.21c Comparison of $w_2^2$ profiles at around $x^* = 0.75$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow.

Fig. 8.21d Comparison of $w_2^2$ profiles at around $x^* = 1.0$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow.
Fig. 8.21e Comparison of $w'^2$ profiles at around $x^* = 1.25$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.22a Comparison of $\bar{v}^2$ profiles at around $x^*=0.25$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow

Fig. 8.22b Comparison of $\bar{v}^2$ profiles at around $x^*=0.5$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow
Fig. 8.22c Comparison of $v^2$ profiles at around $x^*=0.75$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow.

Fig. 8.22d Comparison of $v^2$ profiles at around $x^*=1.0$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow.
Fig. 8.22e Comparison of $\bar{v}^2$ profiles at around $x^*=1.25$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.23a Comparison of $\overline{uv}$ profiles at around $x^*=0.25$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow

Fig. 8.23b Comparison of $\overline{uv}$ profiles at around $x^*=0.5$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow
Fig. 8.23c Comparison of $-\bar{uv}$ profiles at around $x^*=0.75$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow

Fig. 8.23d Comparison of $-\bar{uv}$ profiles at around $x^*=1.0$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow
Fig. 8.23e Comparison of $-\bar{uv}$ profiles at around $x^*=1.25$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow.
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.24a Comparison of $k$ profiles at around $x^*=0.25$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow.

Fig. 8.24b Comparison of $k$ profiles at around $x^*=0.5$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow.

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Fig. 8.24c Comparison of $k$ profiles at around $x^*=0.75$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow

Fig. 8.24d Comparison of $k$ profiles at around $x^*=1.0$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow
8. Spanwise-Invariant Region of the Swept Flow

Fig. 8.24e Comparison of k profiles at around $x^*=1.25$ in the spanwise-invariant region of the unswept flow and 10 degree swept flow

Fig. 8.25 Development of maximum Reynolds stresses
9. Three-Dimensional Region of V-Configuration Fence

9.1 Surface Flow Visualisation

The broad features of the surface streamline pattern behind the v-configuration fence are shown in Figure 8.4. Greater detail in the central, three-dimensional region of the flow is given in Figure 8.3. In the lateral range \(-150\text{mm} > z > -765\text{mm}\) the streamline pattern of the whole flow presented in Figure 8.4 is actually based on the reflected pattern from one side of the v-configuration fence. However, flow visualisation experiments in both of the lateral flows showed that the flow was closely symmetrical about the central plane in this lateral range. Only very close to the symmetry plane were there small departures from the symmetry condition. Further checks on the symmetry of the flow were made with measurements of attachment length made with the twin-tube probe at lateral stations \(z=0\), \(-380\) and \(380\text{mm}\), above and below the splitter plate. With careful adjustment of the trailing-edge flap on the splitter plate, agreement between like stations was within 5%.

While not necessary, symmetry has the advantage that it provides some additional (time-mean) checks on the measurements and is also convenient for boundary conditions for any future computational work. In addition to the symmetry of the flow, another striking feature of the surface streamline pattern is the growth of the separation bubble as the central plane is approached. Quantitative analysis of this change in size of the separation bubble with lateral position will be given in the following section.

Where the attachment lines from the two lateral flows meet, streamlines can be seen to move upstream and downstream forming a saddle point. Other topological features in the central region are not obvious from the figures presented here, but flow visualisation studies using the smaller fence suggested that there may be a focal point on either side of the symmetry plane, close to the separation line.

9.2 General Mean Features of the 3-D Region of the Swept Flow

In previous chapters, surface pressure and skin friction measurements have been discussed before the mean velocity measurements. However, for the three-dimensional region of the flow, it is instructive to discuss some of the changes in shape of the separation bubble before analysing the \(C_p\) and \(C_f\) data. Also, in the following discussion the attachment length, \(X_a\), will be discussed rather than the reattachment length, \(X_r\), for reasons that will be given shortly.
Establishing the position of the attachment line in a spanwise-invariant flow is a relatively straightforward task because the flow direction at attachment is known \textit{a priori} (parallel to the separation line). In the general case this task is more complicated because both its direction and position have to be established. Surface visualisation techniques such as those used here are especially useful in this regard in that the whole surface is covered. Obtaining the attachment line from measurements of surface shear stress (or gradients of mean velocity) would require a large number of closely spaced measurements. In order to compare the surface flow observations with a more limited number of measurements, the approach here has been to measure the position at which \( \tau_z \) (or \( \partial U / \partial y \)) is zero and the position at which the surface streamline is tangent to the \( z \)-direction. (The correct attachment streamline and the streamline which is at a tangent to the \( z \)-direction on the \( z=380\text{mm} \) plane is shown in Figure 8.2). Because the cross flow is mild in these experiments, the position at which the \( \tau_z \) is zero is only about 3% upstream of the true position of \( X_a \), except on \( z=0 \) where it is of course coincident. Table 9.1 is a comparison of the three measurements at each lateral station.

<table>
<thead>
<tr>
<th>lateral position ( z ) (mm)</th>
<th>( X_a/h_f ) (flow visualisation - using attachment streamline)</th>
<th>( 'X_a'/h_f ) (flow visualisation - using streamline that is tangent to ( z ) )</th>
<th>( 'X_a'/h_f ) (( C_f=0 ))</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>25</td>
<td>25</td>
<td>25.9</td>
</tr>
<tr>
<td>40</td>
<td>24.5</td>
<td>23.8</td>
<td>24.4</td>
</tr>
<tr>
<td>80</td>
<td>23.5</td>
<td>22.5</td>
<td>23</td>
</tr>
<tr>
<td>120</td>
<td>23</td>
<td>22</td>
<td>22</td>
</tr>
<tr>
<td>200</td>
<td>22</td>
<td>21.5</td>
<td>21.3</td>
</tr>
<tr>
<td>380</td>
<td>21</td>
<td>20.5</td>
<td>20.5</td>
</tr>
</tbody>
</table>

Table 9.1 Correct values of \( X_a \) at various lateral positions with values (\( 'X_a' \)) determined using the position at which \( \partial U / \partial y = 0 \) and \( C_f=0 \)

The agreement between the position at which \( \tau_z=0 \) and where the surface streamline is tangent to \( z \) is very close, giving added confidence to both sets of measurements.

As has already discussed in previous chapters, the presence of the field probe had a not insignificant effect on the attachment length when the probe was near the attachment position. Because measurements were required at the attachment position, the probe was placed slightly upstream of the true position given in Table 9.1. Since the through-wall probe
measurements were carried out with the field probe also in the flow, measurements around attachment were also slightly upstream of the true position given in Table 9.1. A further complication is that the measurement stations (supposedly at $x/X_a=0.25, 0.5, 0.75, 1.0$ and $1.25$) were based on $X_a$ determined using the position at which $\partial U/\partial y=0$, rather than the true value from the flow visualisation results. The actual downstream positions used, based on the unperturbed value of $X_a$ (determined using the proper attachment streamline) are given in Table 9.2.

<table>
<thead>
<tr>
<th>lateral position z(mm)</th>
<th>$x/X_a$ (desired)</th>
<th>$x/X_a$ (position with through-wall probe)</th>
<th>$x/X_a$ (position with field probe)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>0.25, 0.5, 0.75, 1.0, 1.25</td>
<td>0.25, 0.5, 0.75, 1.0, 1.25</td>
<td>0.25, 0.5, 0.75, 1.0, 1.25</td>
</tr>
<tr>
<td>40</td>
<td>0.25, 0.5, 0.75, 1.0, 1.25</td>
<td>0.24, 0.48, 0.72, 0.96, 1.2</td>
<td>0.24, 0.48, 0.72, 0.93, 1.2</td>
</tr>
<tr>
<td>80</td>
<td>0.25, 0.5, 0.75, 1.0, 1.25</td>
<td>0.23, 0.47, 0.70, 0.94, 1.17</td>
<td>0.23, 0.47, 0.70, 0.91, 1.17</td>
</tr>
<tr>
<td>120</td>
<td>0.25, 0.5, 0.75, 1.0, 1.25</td>
<td>0.23, 0.46, 0.69, 0.93, 1.16</td>
<td>0.23, 0.46, 0.69, 0.91, 1.16</td>
</tr>
<tr>
<td>200</td>
<td>0.25, 0.5, 0.75, 1.0, 1.25</td>
<td>0.24, 0.47, 0.71, 0.94, 1.18</td>
<td>0.24, 0.47, 0.71, 0.93, 1.18</td>
</tr>
<tr>
<td>380</td>
<td>0.25, 0.5, 0.75, 1.0, 1.25</td>
<td>0.24, 0.47, 0.71, 0.95, 1.18</td>
<td>0.24, 0.47, 0.71, 0.95, 1.18</td>
</tr>
</tbody>
</table>

Table 9.2 Downstream positions used for the measurements with the through-wall probe and the field probe.

The attachment location used for the field probe measurements is as much as 9% shorter than the true value of $X_a$ at certain lateral stations. However, if these locations are compared to the perturbed attachment length (where the attachment length is shortened by about 3 or 4% by the presence of the probe) these stations are typically only around 5% upstream of attachment. Finally, perhaps it ought to be mentioned that over the course of the measurements, the position of the attachment line was not always precisely as given in Table 9.1. In these cases, the position of the probe was adjusted to give the same relative positions as in Table 9.2.
As in previous chapters, the downstream position, \( x/X_a \), based on the unperturbed values of the attachment length will generally be used throughout this chapter. However, for certain figures, where the consequences are unimportant, the nominal downstream locations of \( x/X_a = 0.25, 0.5, 0.75, 1.0 \) and 1.25 will be used for simplicity. The unperturbed values of \( X_a \) will be used for any other scaling purposes.

The variation in length of the separation bubble against lateral position is plotted in Figure 9.1. Also plotted in this figure is the variation in height of the separation, taken as the vertical position at approximately the middle of the bubble (close to \( x/X_a = 0.5 \)) at which \( U = 0.95U_{\text{max}} \). The length of the separation bubble on the central plane is about 20% greater than in the spanwise-invariant region, but the height in the centre is about 40% larger than in the invariant region. The bubble on the central plane is not only larger than in the invariant region, but also has a different shape. However, the shape of the bubble at \( z = 0 \) has also changed in another sense. Figure 9.2 compares the variation in bubble height with lateral position at around \( x/X_a = 0.5 \) and \( x/X_a = 1.0 \). Close to attachment, the bubble at the centre is 70% higher than in the invariant region compared to the 40% increase at the bubble centre. At both of these downstream locations it appears that the height of the bubble changes significantly over a fairly narrow region; a lateral half width (from the centre plane) of around 120mm, i.e. approximately 0.5 \( X_{a_0} \), where \( X_{a_0} \) is the attachment length on the centre. The attachment length, on the other hand, changes fairly steadily over the entire lateral range.

The reason for the growth of the separation bubble near the centre is associated with the necessary streamline pattern for this flow configuration. McCluskey et al (1991) showed that only when the flow is spanwise-invariant are the separating and attaching streamlines connected. This is the case for the spanwise-invariant region of the unswept and the swept flow and so the term 'reattachment position' can be used for these cases. However, in the central region of the v-configuration fence, the lateral inflows from the two sides of the 'v' mean that the separating streamlines remain above the surface allowing a mass outflow equal to the side inflows. This is illustrated in Figure 9.3, and this feature of the present flow is the reason why the term 'attachment position' will be used during the present chapter.

### 9.3 Static Pressure Measurements

The variation of the surface pressure is closely associated with the shape of the separation bubble. The minimum pressure within the separation bubble dictates the degree of curvature of the separated shear layer and consequently the attachment length. Since the shape of the bubble is known to change dramatically with lateral station it may be expected that the lateral
variation of surface pressure may also be considerable. Profiles of pressure coefficient, $C_p$, at $z=0, 40, 80, 120, 200$ and $380$mm are shown in Figure 9.4a, using the fence height to normalise the $x$-distance. The same data is plotted in Figure 9.4b, but using the local attachment length to normalise the $x$-distance.

Although not shown in these figures, measurements of surface pressures were also made on the $z=-40$mm plane, to assess symmetry of the flow. It was found that even though the surface streamline pattern in the central region looked closely symmetrical about the $z=0$ plane, (later confirmed using measurements of mean lateral velocity, $W$) the pressures in the upstream half of the separation bubble at $z=-40$mm were approximately $5\%$ lower than at $z=40$mm. It appears that slight asymmetry may have a larger effect on the surface pressure than the mean velocity.

Generally, the results show the expected trends, with the minimum pressure on the central plane, where the attachment length is longest, not being as low as at other lateral stations where the bubble is shorter. As with the lateral variation of $X_a$, the lateral variation of the minimum values from the profiles in Figure 9.4 extends all the way from the central plane to the spanwise-invariant region. It also appears that the pressure downstream of attachment in the central region is lower than that downstream of the spanwise-invariant region. This may be caused by streamwise vorticity originating within the separation which is then convected out of the separation by the flow between the separation and attachment streamlines.

### 9.4 Wall Shear Stress Measurements

Profiles of $x$- and $z$-component shear stress at the various lateral stations are given in Figures 9.5 and 9.6, respectively. One of the most encouraging features of the profiles of shear stress is the very low values of $C_{fx}$ on the central plane ($z=0$), supporting the claim that the flow was closely symmetrical about this axis. The profiles are also particularly useful in describing the near-wall flow direction in regions where the flow visualisation technique did not function well. For example, the profile of $C_{fx}$ on the centreline suggests that at this lateral position, there appears to be no secondary separation process. Also, the profiles of $C_{fx}$ suggest that very close to the fence, the flow just above the surface has a component of velocity in the positive $W$ direction (away from the central plane) in the lateral range $0mm<z<120mm$.

The lateral variation in the maximum of the two components of shear stress, $(-C_{fx})_{\text{max}}$ and $(-C_{fz})_{\text{max}}$, within the separation is given in Figure 9.7. There is a region close to the centre
9. Three-Dimensional Region of V-Configuration Fence

plane where \((-C_{fr})_{max}\) increases rapidly away from the central plane \((0<z<120\text{mm})\), and then an outer region where values only change slowly. However, \((-C_{fr})_{max}\) varies in a non-monotonic fashion and is strongly dependent on lateral position all the way to the nominally spanwise-invariant region. The value of \((-C_{fr})_{max}\) is higher in the spanwise-invariant region than on the central plane and the minimum value occurs on the \(z=80\text{mm}\) plane.

As will be seen later, the lateral variation of \((-\overline{uv})_{max}\) at the various downstream stations considered behaves in quite a different way from \((-C_{fr})_{max}\), with maximum values occurring on the central plane and minimum values on the \(z=200\) and \(z=380\text{mm}\) planes. However, the dissimilar nature of the lateral variation of Reynolds shear stress and the surface shear stress is perhaps not surprising as Hancock (1994) demonstrated that these two variables can behave independently of one another in unswept separated flows. It was shown that increasing the Reynolds number leads to increased values of \(u^2\) (and presumably increased levels of \(\overline{uv}\)) and decreased values of \((-C_{fr})_{max}\). Future work will have to consider the relationship between the inner and outer flow.

9.5 Mean Velocities and Extra Rates of Strain

Profiles of axial velocity taken at five streamwise locations are compared at six lateral stations in Figures 9.8a-e. The shapes of \(U(y)\) at a given downstream position are fairly similar at different lateral positions but extend substantially further over a fairly narrow central region. Close to the fence, this region extends to only around \(\pm 0.2 X_{ao}\), whereas near attachment the corresponding figure is nearer \(\pm 0.5 X_{ao}\). These widths are comparable with the bubble height, implying that lateral gradients of velocity and the associated extra strain rates are likely to be significant.

These graphs also show that within the separation bubble, the maximum reverse velocity in the upstream half of the separation bubble is considerably higher on the central plane than on the other lateral stations. At \(x/X_a=0.25\), this value is approximately 30% of \(U_{ref}\) at \(z=0\text{mm}\), which is about 60% higher than in the spanwise-invariant region. Further downstream at \(x/X_a=0.75\), the peak reverse velocity is very similar in these two planes. The reason the flow does not decelerate as much in the upstream half of the separation bubble on the central plane as on other lateral planes is associated with the absence of a secondary separation process at \(z=0\text{mm}\). The secondary separation process on the other planes is a result of the adverse pressure gradient upstream of approximately \(x/X_a=0.4\) within the separation bubble.
Figure 9.4a shows that the magnitude of this adverse pressure gradient on the surface becomes greater away from the central plane. In fact, the measurements suggest that the static pressure on the central plane is virtually constant upstream of $x/X_a = 0.4$. Based on the wall shear stress measurements, it appears that on the central plane, the reverse flow maintains its negative $U$-momentum until very close to the fence before decreasing to zero.

This maintenance of reverse flow momentum until close to the fence was also observed during the smoke flow visualisation experiments on the central plane. These experiments revealed that close to separation, the larger bubble near the central plane resulted in the flow shortly after separation retaining a larger vertical velocity than at $z=380\text{mm}$, reducing the chance of structures carrying positive $U$-momentum from reaching the near-wall region. In fact, analysis of the histograms of $U$ in the near-wall region at $x/X_a = 0.25$ show that on the central plane virtually 100% of the samples at each point beneath $y/h_f = 1.5$ were in the negative direction. In contrast, the histogram of $U$ at $x/X_a = 0.25$ in the spanwise-invariant region showed that at $y/h_f = 0.5$, only about 93% of the samples were in the negative direction (unfortunately the variation of the reverse flow factor closer to the fence is not known as the histograms were not retained for the measurements made with the through-wall probe).

Also, at $x/X_a = 0.25$, the vertical extent of the reverse flow, measured in terms of $y_0/H_b$, where $y_0$ is vertical position at which $U = 0$, is approximately 0.6 on the central plane and only 0.5 in the invariant region. This of course means that the profiles cannot be made to collapse by scaling the vertical height on a lengthscale of the flow such as $X_a$ or $H_b$. Using the $U$-velocity data it is also possible to determine the downstream development of the centre of the shear layer and also the growth rate of the shear layer on the various lateral planes. However, before doing this it is instructive to look at the $V$ and $W$ velocity field, with particular attention paid to the strain field (diverging or converging) introduced to the flow by the lateral velocity gradients.

Profiles of vertical velocity are given in Figures 9.9a-e for the five downstream positions and six lateral stations considered. Given that these profiles should have error bars of around $\pm 0.02U_{ref}$ there is very little difference between the results on the five lateral planes away from the $z=0\text{mm}$ plane. However on this central plane, the increased size of the separation bubble results in the flow direction in the early part of the separation to be further from the axial direction than at other lateral stations and this leads to a higher maximum vertical velocity at $x/X_a = 0.25$. Further downstream at $x/X_a = 0.5$, the different shape of the separation on the centreline, means that whilst at other lateral stations the flow direction in the outer part of the separation is marginally towards the splitter plate, resulting in negative vertical velocities, the vertical velocity on the central plane at this downstream position remains positive. This increased vertical velocity away from the surface is a result of the impingement
of the two lateral inflows meeting and 'swelling' the separation bubble at the centre. This can also be seen in the profiles at downstream locations \( x/X_c = 0.75, 1.0 \) and 1.25, where the vertical velocity on the centre plane is always less negative than on other lateral planes.

Lateral mean velocity profiles at five downstream locations and at various lateral stations are given in Figures 9.10a-e. Typical agreement between measurements of \( W \) made with the through-wall and the field probe is \( \pm 0.01 U_{ref} \) at the lowest measuring station of the field probe. Also, the very low levels of lateral velocity on the central plane (generally less than \( \pm 0.01 U_{ref} \)) support the claim that the flow was closely symmetrical about this plane. The lateral velocity is also plotted with the axial and vertical velocity in vector form in Figure 9.11. In this diagram the profiles are plotted at downstream locations \( x/X_a = 0.25, 0.5, 0.75 \) and 1.0, and also at lateral stations \( z = 0, 40, 80 \) and 120mm. The red lines are the resultant vectors. This diagram demonstrates the 'moderate' three-dimensional nature of the mean flow, with the velocity vectors broadly in the streamwise direction over a large portion of the flow.

The maximum lateral velocity towards the central plane, \( (\cdot W)_{max} \), at the various lateral stations and at the five downstream locations are given in Figure 9.12. At all of these downstream locations the value of \( (\cdot W)_{max} \) increases away from the symmetry plane and reaches the highest value in the spanwise-invariant region. As expected, \( (\cdot W)_{max} \) increases most rapidly near the \( z=0 \)mm plane. Close to the fence, however, this increase in \( (\cdot W)_{max} \) occurs over a lateral range of about 200mm, whereas near attachment \( (\cdot W)_{max} \) has reached 90% of its highest value at only about \( z=75 \)mm. This is probably because the smaller scales of mixing close to separation provide comparatively less stress-driven lateral motion near the relatively more distant splitter plate.

The fact that \( (\cdot W)_{max} \) decreases monotonically with proximity to the central plane at the five positions considered reinforces the ideas developed from the surface flow visualisation experiments that the centre of the v-configuration fence is characterised by two laterally convolving flows. However, reference to the three-dimensional velocity vector plot of Figure 9.11, and the vertical profiles of \( W \) in Figures 9.10a-e, shows that this is only the case in the flow region close to the splitter plate. Beyond a certain height, that is dependent on both downstream and lateral position, the lateral velocity is positive and therefore moving away from the \( z=0 \)mm plane. The cause of this positive lateral velocity is an inviscid effect and has already been discussed in the previous chapter.

If, instead, the variation in \( W \) with \( z \) (at constant \( x'' \)) is considered, a more complex variation can be seen. In Figure 9.13a-c, the variation of \( W \) with \( z \) and \( y \) is shown at three \( x'' \)
positions. These downstream positions \((x'')\) are measured with respect to the \(z=0\) plane, as shown in the illustration on Figure 9.13a. The values in these plots were determined by interpolating data, in both the vertical and streamwise sense, from the measuring stations. At the station closest to the fence \((x/X_a=0.25\) on \(z=0\)), \(\partial W/\partial z\) on the central plane is only negative over a vertical range \(0<y<1h_f\). Further downstream, at \(x''=200\)mm, this range is larger at \(0<y<3.5h_f\). Above these positions, \(\partial W/\partial z\) is positive.

Because of this complex lateral strain field, it is perhaps clearest to illustrate the broad variations in space in pictorial form. Figure 9.14 displays contours of \(\partial W/\partial z\) on the \(z=0\)mm, \(z=40\)mm and \(z=80\)mm plane. Also plotted in this figure are the shear layer centreline, the locus of \(k_{max}\) and the locus of \(U=0\) for the three planes. As before, the shear layer centreline, \(y_c\), is defined as height at which the axial velocity is \((0.67\Delta U + U_{min})\) where \(\Delta U = U_{max} - U_{min}\).

What is immediately apparent from this figure is the rapid change in lateral strain field with lateral position. In the first half of the separation bubble on the central plane, the shear layer centreline passes through a region of laterally diverging flow with a maximum positive value of \(\partial W/\partial z\) that is comparable in magnitude to the most negative value. However this region of 'strongly' diverging flow is only significant over a lateral half-width of approximately \(0.15X_{a_0}\). In fact the shear layer centreline on the central plane does not pass through a region of laterally converging flow at any downstream position within the separation bubble. Only on the low velocity side of the shear layer and particularly in the near-wall region is the flow subjected to lateral convergence on the central plane.

Unfortunately, the limited number of streamwise locations means that no strain rates could be determined closer to the separation line. Consequently it is not certain if the region of 'strongly' positive \(\partial W/\partial z\) on the central plane extends to the \(z=40\)mm plane nearer separation. However, in the region downstream of around \(x/X_a=0.25\) on this plane there is only a weakly positive lateral strain on the outer part of the flow. However, like the central plane the shear layer centreline on the \(z=40\)mm plane is generally subjected to a diverging regime (albeit a moderate strain field) at the majority of downstream locations. This is in contrast to the \(z=80\)mm plane where the flow is generally converging, except towards the latter part of the bubble.

The lateral strain rate, \(\partial W/\partial z\), is by no means the only 'extra strain rate' that changes with lateral position in the v-configuration fence (see Figure 1.1 in introduction chapter). Some of these strains together with the change in size of the bubble with lateral position and the corresponding variation of \(\partial U/\partial y\), are likely to have a significant effect on the turbulence.
structure. However, it has been shown by numerous workers, such as Keffer (1965), that lateral convergence/divergence can have an unexpectedly large effect on the turbulence structure, and so this term will be discussed further in some detail.

In order to assess the likely effects of the lateral strain rates on the turbulence structure, it is more useful to look at the ratio of \((\partial W/\partial z)/(\partial U/\partial y)\) than the absolute value of the lateral strain. The streamwise variation of this ratio along the shear layer centreline at the various lateral positions is given in Figure 9.15. As discussed in Chapter 3, Bradshaw (1973) suggests that a simple formula for qualitatively predicting the effects of weak extra strain rates on turbulence quantities is:

\[
1 + \lambda \frac{e}{\partial U/\partial y} \tag{9.1}
\]

where \(\lambda\) is the 'unexpected largeness' of the effect of extra strain rates and is of the order 10. Since the value of \((\partial W/\partial z)/(\partial U/\partial y)\) varies between about 0.05 and -0.02, between different lateral planes and downstream positions, it may be expected that the lateral strain rate may produce changes in the turbulence structure leading to significant changes in Reynolds stresses.

The fact that the lateral gradients are large enough to significantly affect the turbulence structure, also means that they are likely to alter the growth rate of the shear layer. However, it is by no means obvious as to what net effect the lateral strain rates will have. For example, the vortex stretching imposed by a laterally diverging flow such as the one that occurs in the upstream part of the flow on the central plane will have an intensifying effect in terms of the turbulence that will lead to increased turbulent mixing and an increased growth. However, simple continuity arguments, assuming constant entrainment, imply that a diverging flow would lead to a negative value of \(\partial U/\partial x\) or \(\partial V/\partial y\). A negative value of \(\partial V/\partial y\) suggests a decrease in growth rate. An added complication in the present flow is that on the lateral planes, \(z=40\text{mm}\) and \(z=80\text{mm}\), the shear layer is subjected to convergence for some way downstream of separation, and then divergence later in the shear layer development.

As in previous chapters, the growth of the shear layer will be expressed in terms of the gradient thickness:

\[
\Lambda = \frac{U_{\text{max}} - U_{\text{min}}}{(\partial U/\partial y)_{\text{max}}} = \frac{\Delta U}{(\partial U/\partial y)_{\text{max}}} \tag{9.2}
\]
The streamwise variation of this parameter, normalised using the local $X_a$, is plotted in Figure 9.16. Further analysis of this plot will be given in the following section, in conjunction with analysis of the Reynolds stresses. However, it is clear that the average growth rate over the length of the bubble is higher on the central plane than at other lateral stations. It seems that of the two arguments relating to the effect of divergence on the growth rate of the shear layer, the increased turbulent mixing is dominant. At the other lateral positions, the average growth rate of the shear layer over the length of the separation bubble is very similar. In the second half of the bubble and downstream of attachment, the growth rate on the $z=40\text{mm}$ plane is slightly higher than at other stations.

The streamwise variation in the vertical position of the centre of the shear layer, $y_c$, at the various lateral positions is given in Figures 9.17a and b. In the first of these figures, $y_c$ has been non-dimensionalised using the fence height and in the second figure the attachment length has been used. As with the earlier plot showing the lateral variation of $H_b$, Figure 9.17a shows that the increased height of the separation bubble occurs over a narrow lateral range. Around attachment, $y_c$ changes significantly over a lateral half width of approximately 120mm and close to separation this figure is around 40mm. On the central plane, the height of the shear layer is significantly larger than at other lateral stations. Around attachment, for example, $y_c$ is about 75% larger than in the spanwise-invariant region and close to separation it is around 30% larger.

The increase in height of the shear layer centreline on the $z=0$ plane is associated with the swelling effect of the two lateral inflows. It also seems that the necessary outflow from under the separating streamline in the central region, resulting from the lateral inflows, affects the height of the shear layer around attachment more than around separation. Figure 9.17b shows that the streamwise development of the shear layer centreline on the $z=80$, 120, 200 and 380mm planes is very similar. Only on the $z=40\text{mm}$ and the central plane is the shape very different, with the shear layer being raised in the latter part of the bubble.

The change in the size of the separation bubble with lateral position, and in particular the change in the variation of the shear layer centreline height may suggest that streamline curvature effects may be significantly different near the centre. As with $\partial W/\partial z$, the strain rate associated with curvature, $\partial V/\partial x$, is known to have a large effect on the turbulence structure. Calculated values of $(\partial V/\partial x)/(\partial U/\partial y)$ on the shear layer centreline at the various lateral stations are shown in Figure 9.18. Although values of this parameter in the early part of the shear layer are typically around -0.03 (indicating a significant stabilising curvature), the downstream variations are very comparable on the different lateral planes. Consequently, streamline curvature is unlikely to explain large variations with lateral position.
9.6 Reynolds Stress Measurements

Profiles of $u^2$, $w^2$, $v^2$, $-uv$, $uw$ and $k$ taken at five downstream locations are compared at six lateral stations in Figures 9.19 to 9.24, respectively. The peak values of $u^2$, $v^2$, $w^2$, $-uv$ and $k$ are plotted against lateral position in Figures 9.25, 9.26, 9.27, 9.28 and 9.29. Peak values of $uw$ are not presented because of the very low magnitude of these stresses, and the large relative errors ($\pm 0.003 U^2_{ref}$) associated with the measurements. Values of $uw$ on the central plane are comparable to the scatter in the measurements made in the unswept flow where $uw$ should also be zero. Elsewhere in the flow the values of $uw$ are not significantly larger than the values obtained in the unswept flow and so quantitative analysis of the results is difficult. Qualitatively, it appears that whilst on most lateral stations the value of $uw$ is positive, on the $z=40$mm plane it is negative.

The presented measurements of $u^2$ are those resulting from the fitting routine involving the five probe orientations in the $x-z$ plane. This evaluation was chosen because the values were virtually identical to those determined at the ‘zero angle’ probe orientation from the data set, indicating very good fits. However, the $u^2$ results determined using the zero angle probe orientation from the $x-y$ data set indicated that the peak values on the $z=120$ and $z=200$mm planes at most streamwise stations were around 10% higher than the presented values. Now, the fact that $u^2$ determined from the zero angle orientation and from all five probe angles from the $x-y$ data set were not entirely consistent (peak values of $u^2$ from the latter being typically around 5% higher) means that the confidence in these measurements was not as high as those from the $x-z$ plane. However, due to the slight uncertainty of the presented values of $u^2$ at $z=120$ and $z=200$mm, discussion of $u^2$ in this region will obviously be rather cautious.

Unfortunately, owing to the scatter in the profiles of $v^2$, very little can be said about the lateral variation in the peak values of this stress, except that it would appear that close to separation, levels are higher on the central plane than elsewhere, and around attachment peak values are comparable at the various lateral stations except at $z=120$mm and $z=200$mm, where they are approximately 30% lower. The broad trend with $z$ is not unlike the trends in $u^2$, $w^2$ and $-uv$, however.

Reference to Figures 9.25, 9.26, 9.27, 9.28 and 9.29 suggests that the lateral variation in peak values of $u^2$, $w^2$, $-uv$, and $k$ are broadly similar. Midway along the separation
bubble, the highest levels occur on the central plane, with a gradual reduction in the levels with distance away from the central plane, before rising again towards the spanwise-invariant region. $\overline{u^2}$, $-\overline{uv}$ and $k$, for example, reach a minimum at approximately $z=200\text{mm}$, whereas for $\overline{w^2}$ this minimum is closer to the central plane at $z=80\text{mm}$. This type of variation in $\overline{w^2}$ is also present closer to separation at $x/X_a=0.25$, and the variation in $\overline{u^2}$, $-\overline{uv}$ and $k$ at this location share the feature that the level on the central plane is significantly higher than on other lateral planes.

Further downstream, the lateral variation in peak values of $\overline{u^2}$, $\overline{w^2}$, and $k$ show that levels on the $z=0$ and $z=40\text{mm}$ plane are comparable. In fact, at attachment, peak levels of $\overline{u^2}$ and $k$ are actually higher on the $z=40\text{mm}$ plane than on the central plane. Peak values of $\overline{w^2}$ are also higher on the $z=40\text{mm}$ plane than on the central plane at $x/X_a=0.75$, 1.0 and 1.25. Peak values of $-\overline{uv}$, on the other hand, are higher on the central plane than on other lateral stations at all downstream positions. Beyond the $z=40\text{mm}$ plane, the lateral variation in the peak values of $\overline{u^2}$, $-\overline{uv}$ and $k$ at lateral stations $x/X_a=0.75$, 1.0 and 1.25, are qualitatively similar to the variation at stations closer to separation, with a minimum occurring at around $z=200\text{mm}$. For $\overline{w^2}$, the lateral variation beyond $z=40\text{mm}$ in the second half of the bubble is also similar to the variation in the first half of the bubble, except the minimum seems to occur further from the central plane.

In simple terms, the increased growth rate of the shear layer close to the central plane implies higher levels of turbulence and in particular higher levels of shear stress in this region. Broadly speaking, the Reynolds normal stresses, shear stress and $k$ are indeed higher on the central plane than elsewhere. In order to investigate the reasons why the stresses are higher on the central plane it is instructive to first recall the shape of the bubble.

As already discussed in the previous section, the shape of the first half of the separation bubble on the central plane is very different to that on other planes. Specifically, the vertical extent of the reverse flow region, $y_0$, compared to the height of the separation bubble, $H_b$, was larger. In effect the shear layer close to the central plane has simply been displaced outwards, with the result that, close to separation, the peak value of $\partial U/\partial y$ on the central plane is very similar to the value on other lateral planes. In fact, at $x/X_a=0.25$ the value of $\partial U/\partial y$ on the central plane is $3\%$ less than that on the $z=40\text{mm}$ plane and $20\%$ less than the value in the spanwise-invariant region. It would appear that the reason for the increased stresses close to the central plane are not directly linked to changes in $\partial U/\partial y$. (Close to
separation, the shear layer growth rate is higher on the central plane largely because \( \Delta U \) is higher, rather than \( \frac{\partial U}{\partial y} \) being lower).

Since \( \frac{\partial W}{\partial y} \) is typically around 0.1 \( \frac{\partial U}{\partial y} \) over a large vertical portion of the flow on lateral planes other than the central plane, it would seem likely that this strain rate is associated with the large structural changes in turbulence with proximity to the central plane. However, the previous chapter concerned with the comparison of measurements made in the unswept and the swept invariant flow showed that this term appeared to play an insignificant role in the turbulent structure of the swept flow. In the previous section of the present chapter, it was also argued that the strain rate associated with streamline curvature, \( \frac{\partial V}{\partial x} \), is unlikely to explain the large lateral variations in turbulent structure.

In the previous section, analysis of the variation in lateral velocities has shown that the central region (particularly the central plane itself, close to separation) is characterised by positive values of \( \frac{\partial W}{\partial z} \) that are likely to be significant in terms of the turbulence structure. The streamwise variation of the term \( \frac{(\partial W / \partial z)(\partial U / \partial y)}{\partial U / \partial y} \) on the shear layer centreline at various lateral stations has already been presented in Figure 9.15. At \( x/X_a = 0.25 \), the value of \( \frac{(\partial W / \partial z)(\partial U / \partial y)}{\partial U / \partial y} \) is around 0.05 on the central plane and approximately zero on the \( z=40 \text{mm} \) plane. As previously mentioned, this might imply a roughly 50% increase in the turbulent kinetic energy. Inspection of Figure 9.29 shows that the increase in peak level of turbulent kinetic energy at \( x/X_a = 0.25 \) between the \( z=0 \) and \( z=40 \text{mm} \) plane is indeed close to this figure.

The lateral variation of the strain ratio, \( \frac{(\partial W / \partial z)(\partial U / \partial y)}{\partial U / \partial y} \), at the height at which \( k_{\text{max}} \) occurs is shown in Figure 9.30. As the height at which \( k_{\text{max}} \) occurs is generally very close to the height at which \( (-\overline{uv})_{\text{max}} \) occurs, the profiles shown in this figure would not have been very different had the values been determined at the height of the latter. Unfortunately, because of the limited number of measuring stations considered, \( \frac{\partial W}{\partial z} \) cannot be determined at all areas in the flow. However, from the restricted data available in Figure 9.30, it appears that the lateral variation of this strain ratio is qualitatively similar to the lateral variation of \( (-\overline{uv})_{\text{max}} \) and \( k_{\text{max}} \) shown in Figures 9.28 and 9.29, respectively.

The marginally converging nature of the flow on the \( z=200 \text{mm} \) plane may also be partially responsible for the reduced levels in peak turbulent kinetic energy on this plane compared to the \( z=380 \text{mm} \) plane. (At all stations, \( k \) is approximately 20% lower on the \( z=200 \text{mm} \) plane). However, as already discussed, there is also a possibility that the presented values of \( \overline{u^2} \) on the \( z=200 \text{m} \) plane may be 10% too low, due to some kind of measurement error.
So far, only peak values of turbulent kinetic energy have been discussed. It is also informative to consider which parts of the profiles of turbulent kinetic energy are affected by the three-dimensional nature of the flow close to the central plane. Figures 9.31a-e show profiles of $k/(\Delta U)^2$ taken at the five streamwise locations and six lateral stations, using the mixing length scaling $\eta = (y - y_c)/\Lambda$ for the vertical axis. Despite the significant lateral variations in turbulence structure at the centre and on the low velocity side of the shear layer, the vertical range $(y - y_c)/\Lambda > 0.25$ is characterised by profiles of $k$ that are reasonably invariant with lateral position. Furthermore, in this part of the bubble, the profiles of $k$ do not differ greatly from the plane mixing layer profiles.
Fig. 9.1 Growth of the separation bubble as the centreline is approached

Fig. 9.2 Variation of bubble height with lateral position
Fig. 9.3 Illustrative diagram showing the growth of the separation bubble close to the 3-d region of the v-configuration flow

Fig. 9.4a Static pressure distribution at various lateral stations
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Fig. 9.4b Static pressure distribution at various lateral stations

Fig. 9.5 Profiles of skin friction, $C_{f_x}$, at various lateral stations
Fig. 9.6 Profiles of skin friction, \( C_{z} \), at various lateral stations

Fig. 9.7 Lateral variation of \( (-C_{fx})_{\text{max}} \) and \( (-C_{fz})_{\text{max}} \)
Fig. 9.8a Axial mean velocity profiles at around $x^*=0.25$ and at various lateral stations

Fig. 9.8b Axial mean velocity profiles at around $x^*=0.5$ and at various lateral stations
Fig. 9.8c Axial mean velocity profiles at around $x^*=0.75$ and at various lateral stations

Fig. 9.8d Axial mean velocity profiles at around $x^*=1.0$ and at various lateral stations
Fig. 9.8e Axial mean velocity profiles at around $x^* = 1.25$ and at various lateral stations
Fig. 9.9a Vertical velocity profiles taken at around $x^*=0.25$ and at various lateral stations

Fig. 9.9b Vertical velocity profiles taken at around $x^*=0.5$ and at various lateral stations
Fig. 9.9c Vertical velocity profiles taken at around \( x^* = 0.75 \) and at various lateral stations

Fig. 9.9d Vertical velocity profiles taken at around \( x^* = 1.0 \) and at various lateral stations
Fig. 9.9e Vertical velocity profiles taken at around $x^*=1.25$ and at various lateral stations.
Fig. 9.10a  Lateral mean velocity profiles at around $x^*=0.25$ and at various lateral stations

Fig. 9.10b  Lateral mean velocity profiles at around $x^*=0.5$ and at various lateral stations
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Fig. 9.10c  Lateral mean velocity profiles at around $x^*=0.75$ and at various lateral stations

Fig. 9.10d  Lateral mean velocity profiles at around $x^*=1.0$ and at various lateral stations
Fig. 9.10e  Lateral mean velocity profiles at around $x^*=1.25$ and at various lateral stations
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Fig. 9.11 Velocity vectors in the 3-d region behind the v-shaped fence
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Fig. 9.12 Variation of maximum lateral velocity with lateral position

Fig. 9.13a Variation of W with lateral position at \( x^" = 62.5 \) mm and at various heights
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Fig. 9.13b Variation of $W$ with lateral position at $x''=120\text{mm}$ and at various heights

Fig. 9.13c Variation of $W$ with lateral position at $x''=200\text{mm}$ and at various heights
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Fig. 9.14 Contour plots of $\partial W/\partial z$
Fig. 9.15 Downstream variation of strain ratio, \((dW/dz)/(dU/dy)\), along the shear layer centreline, at various lateral positions.

Fig. 9.16 Downstream growth of the shear layer at various lateral positions.
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Fig 9.17a Streamwise variation of the height of the shear layer centreline

Fig 9.17b Streamwise variation of the height of the shear layer centreline
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Fig. 9.21b $v^2$ profiles at around $x^*=0.5$ and at various lateral stations
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Fig. 9.21c $v^2$ profiles at around $x^* = 0.75$ and at various lateral stations

Fig. 9.21d $v^2$ profiles at around $x^* = 1.0$ and at various lateral stations
Fig. 9.21c $\overline{v^2}$ profiles at around $x^*=1.25$ and at various lateral stations
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Fig. 9.22a \( \bar{u} \bar{v} \) profiles at around \( x^* = 0.25 \) and at various lateral stations

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Fig. 9.22c -$\overline{uv}$ profiles at around $x^*=0.75$ and at various lateral stations

Fig. 9.22d -$\overline{uv}$ profiles at around $x^*=1.0$ and at various lateral stations
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Fig. 9.22e  $\overline{-u'v'}$ profiles at around $x^*=1.25$ and at various lateral stations
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Fig. 9.23b $\bar{u}w$ profiles at around $x^*=0.5$ and at various lateral stations
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Fig. 9.23d $\overline{uw}$ profiles at around $x^*=1.0$ and at various lateral stations
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9. Three-Dimensional Region of V-Configuration Fence

Fig. 9.24a Profiles of $k$ at around $x/X_a=0.25$ and at various lateral stations

Fig. 9.24b Profiles of $k$ at around $x/X_a=0.5$ and at various lateral stations
Fig. 9.24c Profiles of $k$ at around $x/X_c=0.75$ and at various lateral stations

Fig. 9.24d Profiles of $k$ at around $x/X_c=1.0$ and at various lateral stations
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Fig. 9.25 Variation of maximum \( \bar{u}^2 \) with lateral position (note false origin)
9. Three-Dimensional Region of V-Configuration Fence

Fig. 9.26 Variation of maximum $\bar{v}^2$ with lateral position (note false origin)

Fig. 9.27 Variation of maximum $\bar{w}^2$ with lateral position (note false origin)
Fig. 9.28 Variation of maximum $\overline{\nu v}$ with lateral position

Fig. 9.29 Variation of maximum $k$ with lateral position (note false origin)
Fig. 9.30 Lateral variation of strain ratio, \((dW/dz)/(dU/dy)\), at the height at which \(k_{\max}\) occurs.
9. Three-Dimensional Region of V-Configuration Fence

Fig. 9.31a Profiles of turbulent kinetic energy at $x/X_a=0.25$ and at various lateral stations

Fig. 9.31b Profiles of turbulent kinetic energy at $x/X_a=0.5$ and at various lateral stations
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Fig. 9.31c Profiles of turbulent kinetic energy at $x/X_a=0.75$ and at various lateral stations

Fig. 9.31d Profiles of turbulent kinetic energy at $x/X_a=1.0$ and at various lateral stations
Fig. 9.31c Profiles of turbulent kinetic energy at $x/X_r=1.25$ and at various lateral stations
10. Computational Results

10.1 Specifying the Problem (Unswept Flow)

Since the flow on and upstream of the front face of the fence is laminar, turbulence models are only required after separation. Ideally, the calculations involving the Reynolds stress model and the $k$-$\varepsilon$ model would have been performed in such a way that the flow upstream and downstream of the fence could be treated separately within a single calculation. Only downstream of the fence would the turbulence model have been ‘turned on’, and upstream of the fence a laminar calculation would have been performed. However, this zonal modelling approach proved extremely difficult to implement using CFX-F3D because of restrictions on source code accessibility. Consequently, for calculations involving the Reynolds stress model, the turbulence model has been left ‘on’ upstream of the fence, and the unphysical imposition of the log law boundary layer on the front face has been tolerated. For the calculations involving the eddy viscosity based ‘$k$-$\varepsilon$’ model, leaving the turbulence model switched ‘on’ upstream of the fence was not possible as is well known that this model results in spurious levels of turbulence on the front face of bluff bodies.

As has already been discussed in section 3.3.5.1, several workers have allowed the flow on the fence to be calculated supposing an eddy viscosity, and then modified the dissipation to counteract this shortcoming. Although this modification has been shown to reduce the erroneous levels of turbulence upstream of the fence, it is not a very physical approach. One method of ensuring a physically reasonable calculation upstream of the fence when using the $k$-$\varepsilon$ model is to force eddy viscosity to zero in this region. Unfortunately, this was not easy to implement using the commercial software of CFX-F3D. Instead, profiles of $U$ and $V$ on the plane represented by the dashed line in Figure 10.1, directly above the fence, were determined by first running the entire computation as a laminar problem. These profiles, scaled on the free-stream velocity used in the turbulent calculation, were then used as the input conditions for the calculations using the $k$-$\varepsilon$ model. In this way, the difficulties associated with using the eddy viscosity based turbulence model in the region upstream of the fence were circumvented. There are, however, consequential issues that need to be addressed.

The free-stream velocity employed in the laminar calculation (0.3m/s) was chosen such that the bubble length was approximately $30h_f$. It was found that profiles of $U$ and $V$ at the ‘slicing’ plane, determined using this flow speed and then scaled up to allow for the higher free-stream speed used in the actual flow, resulted in a turbulent separation bubble that was
10. Computational Results

approximately the same length as found experimentally. Strictly, the approach of using the scaled velocity field of the laminar flow for the input conditions of the turbulent calculation will only give self consistency in pressure on and around the slicing plane if the flows in the vicinity are the same in both cases. The boundary layer leaving the front face will have been thicker than it should have been, but given that the thickness is very small compared with the scale of the bubble, it was supposed that this error would not be significant.

It was found that the velocity field upstream of the fence determined from the laminar solution was close to that given from the Reynolds stress calculation, and computations with the standard k-ε model using inflow conditions from these later calculations showed only slight changes in reattachment length and stress levels. Although it would have been more satisfying to have been able to set eddy viscosity to zero upstream of the fence in a single calculation of the whole flow field, the errors arising in the present ad hoc arrangement will be small compared with those arising from allowing the spuriously high levels of turbulence on the upstream side of the fence to remain.

Runs carried out with the low Reynolds number model of Launder & Sharma also required a small amount of ‘tripping’ turbulence above the fence, at (a) in Figure 10.1 and an artificially low value of molecular viscosity so as not to inhibit turbulence altogether immediately downstream of the normal fence. The profiles of k and ε chosen as input conditions for the low Reynolds number model were based on experimental work carried out by Hancock (private communication) on plane mixing layers. Early runs using standard properties of air resulted in non-converged solutions with imperceptible levels of turbulence across the whole computational domain. Eddy viscosity had been damped to such an extent in the early part of the shear layer, that transition simply did not occur. The reason why the eddy viscosity can be damped away from walls is because the term $f_{\mu}$ in the present model is expressed in terms of a turbulent Reynolds number and does not contain a ‘distance to nearest wall’ function (see Equation 3.17) as with the models of Lam and Bremhorst (1981) and Reynolds (1976), for example. Consequently, runs using the low Re-number model during this investigation have been made using $\mu=0.5 \times 10^5 \text{kgm}^{-1}\text{s}^{-1}$ to increase globally the value of $Re_r$.

Initial tests showed that reducing the viscosity still further to $\mu=0.1 \times 10^5 \text{kgm}^{-1}\text{s}^{-1}$ only increased the peak values of k by around 10% and the profiles of mean velocity were only changed significantly close to the wall. It was also shown that the low Reynolds number solution was quite insensitive to changes in the levels of turbulence on the input plane.
10.2 Specifying the Problem (Swept Flow)

The invariant region of the swept flow is two-dimensional but not coplanar. Consequently, it can be modelled using a mesh that is only one cell deep in the lateral direction, as with the unswept flow, but with periodic boundary conditions on the lateral faces and prescribed velocities of $U$, $V$ and $W$ at the inlet. Using this approach, the fence remains in the same plane as the upstream faces of the cells, and the lateral velocity at the inlet implies the sweep of the fence. The profiles of $U$, $V$ and $W$ used for the input conditions for the standard and low Reynolds number runs were also determined by scaling up the velocities above the fence of a laminar problem (carried out using $U_{ref}=0.3\text{m/s}$). For the low Reynolds number case, the same input profiles of $k$ and $\varepsilon$ and the same value of molecular viscosity were used as for the unswept flow.

10.3 Computational Domain and Boundary Conditions

To allow comparisons to be made with the experimental work, the computational domain considered was based on the physical dimensions of the wind tunnel and model. The longitudinal extent of the computational domain and the mesh employed depended on the modelling strategy. For the approach involving the input velocity profiles, the domain started at the downstream position of the fence where the input profiles were introduced, whereas runs not employing this approach were started $20h_f$ upstream of the fence. However, both strategies employed domains that extended approximately $20h_f$ downstream of the expected reattachment position. This was partly to ensure that the measurement station at $x/X_r=1.25$ was captured, but also to minimise the influence of the imposed zero-gradient outflow conditions on the flow region of interest. The physical dimensions of the two domains are shown in Figures 10.2a and b, which also illustrate the boundary conditions and the number of blocks used. These figures apply to the work using the unswept and the swept geometry.

From these diagrams it can be seen that the modelling approach adopted implied an infinitely thin splitter plate. The height of the fence above the surface of the splitter plate was the same as the experimental arrangement, but the 3mm thick splitter plate used during the experimental work meant that the stagnation point on the front face occurred 11.5mm below the top of the fence and not 10mm below as was the case for most of the computational work. However, runs performed using the standard $k-\varepsilon$ model with the fence and splitter plate having the same dimensions as the experimental rig showed that this made very little difference. Using the correct geometry the size of the separation bubble (length and height) increased by only around 5% and peak stresses were only around 3% higher than in the idealised case.
The pressure boundary condition from CFX-F3D, applied at the outflow of the domain, implies that zero streamwise gradients are imposed on velocity and all other transported variables - e.g. $k$ and $e$. A no-slip condition was used on the top boundary (wind tunnel roof), though the boundary layer was only very poorly resolved. Symmetry conditions were applied to the flow centreline upstream of the fence. For the swept flow, as has already been mentioned, the lateral planes surrounding the computational domain were specified as periodic. This means that all variables and hence all coefficients, have the same value at both ends of the computational domain, i.e. all flow variables leaving the outlet periodic boundary are set to be equal to those entering the inlet periodic boundary.

### 10.4 Grid spacing

The meshes used for the various turbulence models and modelling strategies are shown in Figures 10.3a, b and c. All computations were made with rectangular non-uniform meshes. Since both the standard $k$-$e$ model and the Reynolds stress model make use of ‘law of the wall’ boundary conditions, the centre of the cell closest to the wall should be at a vertical height off the surface that corresponds to the beginning of the log law region; around $y^+=30$. However, using a typical experimental value of shear stress at the centre of the separation bubble to determine the height at which $y^+=30$, showed that the first cell would have to be 4mm high for the cell centre to be at the desired height. Of course, it should be higher still at other downstream positions where the magnitude of the shear stress is lower. Since the height of this first cell dictates the size of the cell around the top of the fence (growth/reduction of cells along a given axis should be less than 1.2 to minimise truncation errors), it was felt that this size of cell next to the wall would result in a mesh that was too coarse in regions that were likely to be sensitive to grid density.

As a compromise between grid density and the optimum height of the cells next to the wall, wall-cells were used with the top of the cells at $y^+=30$ (using the experimental value of shear stress at $x/X_c=0.5$) and consequently the cell-centres within the buffer layer at $y^+=15$. Using this cell size in conjunction with a geometric progression meant that the height of the cells at the top of the fence were around 0.5mm high ($0.05h_s$) which was considered adequate. It is of course the case that the logarithmic law of the wall is inappropriate for the reverse flow boundary layer, and the difficulties associated with the first cell are a consequence of this inappropriateness. It is also a limitation of the code that finer meshes cannot be used such that the boundary condition is applied at the appropriate cell.
For the low Reynolds number $k$-$\varepsilon$ model a much finer near-wall grid was required. Following standard practice, the cells next to the wall were positioned with the cell centres at about $y^+=1$. Using a symmetric geometric progression meant that the cell heights at the top of the fence were also very fine at around $0.1\text{mm}(0.01h_p)$.

The $x$-direction spacing of the mesh was also kept very fine close to the fence for all turbulence models used. In order to prevent the total number of cells becoming too large it was necessary to use a fairly coarse grid around the reattachment position. These large $x$-direction cells also had the unfortunate consequence that due to the fine $y$-direction cell size close to the splitter plate, the aspect ratio of the cells in this region became very large. Grid independence studies were performed to assess the effects of factors such as this.

### 10.5 Grid Independence Tests

As already discussed, the vertical size of the cells at the top of the fence is fixed by the necessary size at the wall. The longitudinal size of the cells in this region was chosen so that the aspect ratio of the cells was approximately unity. During the work to assess the effects of grid density on these models, the dimensions of the cells close to the fence were therefore not altered. All grid independence studies were carried out using only the unswept geometry.

To assess the effects of grid density on the solution using the standard $k$-$\varepsilon$ over the whole domain, the number of cells in the $x$-direction in blocks 2 and 5 was increased by about 30%. This resulted in the longitudinal size of the cells around attachment to decrease from around 3mm to 2mm. The number of $y$-direction cells in blocks 4, 5 and 6 was also increased by about 30%. With this finer mesh, it was found that the solution using the standard $k$-$\varepsilon$ model was virtually identical to that generated using the coarser mesh. All results presented in this thesis will be based on the coarser mesh.

For the Reynolds stress model (with and without wall reflection terms), the mesh shown in Figures 10.3a was made finer by increasing the number of $x$-direction cells in blocks 2 and 5 by around 55% and by increasing the number of $x$-cells in blocks 3 and 6 by 25%. The number of cells in the $y$-direction in blocks 4, 5 and 6 was changed in the same way as that used for the fine mesh in the standard $k$-$\varepsilon$ model. Comparisons of profiles of $U$ and $k$ within the separation bubble revealed virtually no difference between the solutions with the coarse and fine mesh. Detailed examination of velocity profiles around attachment (based on the coarse solution) suggested that the finer mesh actually produced a marginally longer
separation bubble (around 3% longer). Also, comparisons of the peak levels of turbulent kinetic energy at the respective attachments positions revealed that the finer mesh generated levels that were around 3% higher than the coarse mesh.

The grid independence studies using the Reynolds stress model were particularly important as these computations used only a first order accurate differencing scheme to model the convective terms of the momentum equations (Upwind Differencing Scheme). This was because of lack of convergence with higher order schemes. The results from the independence study suggest that the coarse mesh does not lead to large numerical diffusion effects and consequently all subsequent runs were carried out using the coarser mesh, and it is the results from these meshes that will be presented.

The finer mesh used to test grid independence for the standard $k$-$\epsilon$ model used with the input velocity profiles specified at the ‘slicing’ plane was very similar to that used for the $k$-$\epsilon$ model used over the whole domain. However, the number of $x$-direction cells in blocks 2 and 4 of Figure 10.2b next to the output plane were also increased by around 55%. It was found that these changes had almost no effect on any of the flow variables. The unmodified mesh for the low Reynolds number model with the input profiles was thought to be sufficiently fine in the $y$-direction, and so the changes in mesh density were made to the $x$-direction cells. The number of cells in the $x$-direction in the blocks next to the input plane were increased by 60% and the number in the blocks next to the output plane were increased by 40%. Once again, these changes to the original mesh made virtually no difference to the solution and presented results will be those obtained using the coarser mesh.

### 10.6 Convergence Criteria

For both swept and unswept runs, a gridcell was chosen within the recirculating region at the approximate height at which the maximum reversed velocity occurs. Values of all flow variables at this point, along with the normalised value of the global mass source residual term were monitored. When the former were not changing significantly with further iterations the solution was adjudged to be adequately converged. Typically, this meant that the first four decimal places of the flow variables were unchanging with each iteration. Furthermore, all results presented in this thesis were taken from runs with a value of normalised global mass residual term of less than $3 \times 10^{-5}$. (The global mass source residual term is determined by calculating the difference between the mass flow in and mass flow out of each cell, and summing these differences over the whole computational domain. This figure is normalised using the mass flow rate through the modelled wind tunnel).
10.7 Results

10.7.1 Unswept Flow

Table 10.1 shows the distance to reattachment in terms of the fence height for the six modelling strategies considered. These values were based on the downstream position at which $\partial U/\partial y = 0$ at the surface of the plate. Abbreviated names for the various calculations are also shown in this table, and these will generally be used throughout the discussion of the results for the sake of brevity.

<table>
<thead>
<tr>
<th>modelling strategy</th>
<th>abbreviated name</th>
<th>reattachment length, $X/h_f$</th>
</tr>
</thead>
<tbody>
<tr>
<td>experimental</td>
<td></td>
<td>21.4</td>
</tr>
<tr>
<td>standard $k$-$\varepsilon$ model (whole domain)</td>
<td>K1</td>
<td>9.4</td>
</tr>
<tr>
<td>standard $k$-$\varepsilon$ model (with input profiles at slicing plane from laminar solution)</td>
<td>K2</td>
<td>17</td>
</tr>
<tr>
<td>standard $k$-$\varepsilon$ model (with input profiles at slicing plane from RSM solution)</td>
<td>K2s</td>
<td>16.2</td>
</tr>
<tr>
<td>low Re no $k$-$\varepsilon$ model (with input profiles at slicing plane from laminar solution)</td>
<td>K3</td>
<td>20.5</td>
</tr>
<tr>
<td>Reynolds stress model (without wall-reflection terms)</td>
<td>S1</td>
<td>17</td>
</tr>
<tr>
<td>Reynolds stress model (with wall-reflection terms)</td>
<td>S2</td>
<td>17.5</td>
</tr>
</tbody>
</table>

Table 10.1 Calculation cases and reattachment length for the unswept flow

The input velocity profiles of $U$ and $V$ determined from the laminar solution and used for cases K2 and K3 are compared with the computed profiles above the fence using the Reynolds stress and standard $k$-$\varepsilon$ models in Figure 10.4a and b, respectively. The input profile of turbulence kinetic energy, $k$, used just for the low Reynolds number $k$-$\varepsilon$ model is also given in Figure 10.4c.
In broad terms, the velocity profiles determined from the laminar solution are comparable to those determined using S1 and K1. The level of tripping turbulence used for K3 is coincidentally close to that given for the Reynolds stress calculation and both are very small compared with that of case K1. However, this high level of turbulence in the profile resulting from the K1 calculation, which has been generated on the upstream face of the normal fence, has not affected the mean velocity profiles significantly above the fence. This means that the reason why K1 fails to predict the length and height of the separation bubble anywhere near experimental values is not associated with unrealistic trajectories of the velocity vectors above the fence, but rather the high levels of turbulence which are fed into the early part of the shear layer. In fact, the close agreement between the $U$ and $V$ profiles determined using K1 and the input profiles determined using a laminar solution mean that, in effect, the main change introduced using the ‘sliced domain’ approach has been to simply remove the excess levels of turbulence above the fence.

Figure 10.5 shows how $V/U$ varies with $x$ at three heights above the splitter plate, for cases K2, K3, S1 and the laminar calculation. These illustrate that in the vicinity of the fence, the flow direction determined using the laminar calculation is very similar to that determined using the Reynolds stress calculation. Also, the streamwise development of $V/U$ calculated using the $k$-$\varepsilon$ model with input velocity profiles (K2 and K3) and the laminar calculation are very similar for several fence heights downstream of separation. Both of these points indicate that the input velocity profiles determined from the laminar calculation are not drastically in error. Further confidence in the profiles of velocity used for the input conditions of cases K2 and K3 come from the fact that computations with the standard $k$-$\varepsilon$ model using inflow conditions from the Reynolds stress calculation (case K2s) showed only slight changes in reattachment length and stress levels; the resulting reattachment length and bubble height were about 5% smaller and the velocity at the edge of the bubble was about 5% higher. Also the stresses were about 10% higher, though when scaled against the higher velocity the differences were negligible.

An overall view of mean flow features generated using the various turbulence models and modelling strategies is given in Figures 10.6a-e. Since the K2s calculation was performed simply to show that the input velocity profiles obtained from the laminar solution were not grossly in error, results from this run will not be presented. These figures in conjunction with Table 10.1 show that the standard $k$-$\varepsilon$ model without the input velocity profiles returns the worst result. The bubble length is only around 45% of the experimental value, and the vertical extent of the recirculating region at the centre of the bubble extends to around $y=1h_f$ when the experimental results suggest it should be around $y=1.5h_f$. 
10. Computational Results

The dramatic effect of omitting the computational domain upstream of the fence, and starting the standard $k$-$\varepsilon$ model from approximated velocity profiles at the 'slicing' plane can be seen in Figure 10.6b. The separation bubble length is around 80% of the experimental value, and the vertical extent of the recirculating region at the centre of the bubble is close to the experimental value. Also, the inclusion of low Reynolds number processes has had a significant effect on the computations with specified input velocities. The bubble length shown in Figure 10.6c is about 95% of that measured experimentally and although the height of the bubble, $H_b$, is about 5% lower than the experimental result, the vertical extent of the recirculating region at the centre of the bubble is actually 10% higher.

It is believed that there are several factors that could contribute to the growth of the bubble when using the Launder and Sharma modification to the $k$-$\varepsilon$ model (case K3). One reason could be the high resolution of the initial portion of the separated shear layer afforded by the very fine grid spacing around the fence. The highly dense grid in this region results in a far higher resolution than that used for the standard model with wall functions. Another possibility is associated with the damping of eddy viscosity that occurs even away from solid surfaces when using the present form of the low Reynolds number $k$-$\varepsilon$ model. Even with the artificially low value of molecular viscosity used (see section 10.1), development of turbulence in the shear layer is rather slower than it should be, and consequently the mixing process is underpredicted, contributing to the elongation of the bubble.

In the case of the two Reynolds stress calculations, determining the reattachment position was very difficult. Profiles of $U$ around reattachment had an obviously spurious peak close to the wall which meant that the downstream position at which $\partial U/\partial y = 0$ was difficult to locate. The presented values are those based on an interpretation of the $U$ profiles where the peak has been ignored. These values are considerably shorter than the values based on the position at which the surface shear stress is zero. Based on the downstream position at which $\tau_{wx} = 0$, the reattachment position for the model with and without the wall reflection terms is $23.6h_f$ and $21.0h_p$, respectively.

Analysis of the results showed that the sign of the surface shear stress was not always the same as the sign of the axial velocity determined at the cell centre above the surface. This can occur when using Reynolds stress models, because the surface shear stress is determined from $\rho \overline{u'v'}$ and is of course decoupled from the mean velocity profile. However, the main contribution to the production term of the transport equation for $\overline{uv}$ is $\sqrt{\rho} \partial U/\partial y$ and so it may be expected that the sign of the shear stress at the wall should be the same as the sign of
the near-wall velocity gradient. The fact that in near reattachment this is not the case suggests that the near-wall modelling of the transport equation for $\overline{uv}$ is poor.

The variation of static pressure against downstream position (scaled by $h_f$) is given in Figure 10.7a. As in previous chapters, the pressure coefficient is defined as:

$$C_p = \frac{2(p - p_{ref})}{\rho U_{ref}^2}$$

The reference conditions here are taken upstream of the fence in the undisturbed wind tunnel flow. In the case of the computations whose domain started from the fence, the reference pressure upstream has been obtained from the S1 run. The reason this pressure was used instead of that determined using the standard $k-e$ model, was because the size of the separation bubble resulting from the higher order closure model was very similar to that for cases K2 and K3. The pressure drop across the whole computational domain resulting from the implementation of K1 was slightly lower than the Reynolds stress run because of the smaller separation bubble and thus lower blockage.

The static pressure distribution beneath the separation is usually a good indicator of the general shape of the separation bubble. For example, a low minimum pressure often indicates a shortened bubble as is clearly demonstrated by the profile generated using K1. However, the minimum pressure resulting from the other models is not as low as that measured, and this does not correspond to bubble lengths which are longer than the expected value. For the higher order closure problem, the failure to adequately predict the relationship between minimum pressure within the separation bubble and reattachment length may, at least in part, be associated with the poor modelling of the flow around reattachment above the splitter plate. Presumably, if the separation bubble has a different shape to that predicted experimentally, the balance of negative pressure beneath the bubble and the length of the bubble is also altered. Figure 10.7b shows the variation of surface pressure with downstream distance using the reattachment length to scale the $x$-axis. All model variants except K1 determine the pressure coefficient at reattachment reasonably well.

Profiles of skin friction against streamwise position are given in Figures 10.8a and b. Since no measurements of skin friction were made in the unswept flow, the presented results are taken from the spanwise-invariant region of the swept flow in the direction normal to the fence, and scaled (for $x$) on reattachment length. To keep the second figure simple, the profiles generated using the Reynolds stress model have been scaled using the reattachment length based on the position at which the extrapolated value of $\overline{uv}$ on the surface is zero.
None of the models adequately capture the change in sign of $C_f$ which should occur at around $x=3h_p$, indicating the presence of a secondary separation line, and it is the eddy viscosity based models that perform best in terms of returning a reasonable value for the negative peak in skin friction. In terms of the position of the minimum skin friction value, K1 fares particularly badly, predicting a location of around $x=0.3X_r$ downstream of the fence when the experimental value is approximately in the middle of the bubble. The low Reynolds number model exhibits a variation in shear stress near reattachment not seen in the other predictions or the experimental results. The reason for this complex downstream variation is not known.

$U$ profiles determined using the different turbulence models at five downstream locations are given in Figures 10.9a, b, c, d and e. Interestingly the excessive levels of turbulence generated on the front fence with case K1, which do not lead to major differences with case S1 in mean velocity profiles above the fence, have a profound effect on the mean flow at $x/X_r=0.25$. At this location and further downstream at $x/X_r=0.5$ and 0.75, K1 seriously underpredicts the height of the separation bubble. Midway along the bubble, for example, the height of the bubble, defined as the vertical position at which $U=0.95U_{max}$, is 80% of the experimental value. This figure would be considerably worse but for the excessive levels of turbulence present in the shear layer (convected downstream from the around the top of the fence) that tend to ‘round off’ the top of the velocity profile. As already mentioned, the height at which the mean axial velocity is zero at $x/X_r=0.5$ is only about 65% of that obtained experimentally.

In the early part of the flow, the Reynolds stress model, with and without wall reflection terms, performs best in predicting axial velocity profiles. The maximum reversed velocity is well predicted at $x/X_r=0.25$, and the general shape of the profile is in close agreement with experimental data. At locations further downstream, this model consistently underpredicts the maximum reversed velocity, although the outer flow is well represented. The Reynolds stress model’s chief failing is around reattachment. A distinct non-monotonic variation of $U$ with $y$ can be seen at $x/X_r=1$ and 1.25. The reason for this variation is not yet known, although it appears that the wall reflection terms succeed in lessening the size of the erroneous peak. In contrast, the computations started with input velocity conditions (K2 and K3) perform reasonably well over the entire downstream range considered here. The exception to this is the low Reynolds number model at $x/X_r=0.25$, which predicts a maximum reversed velocity that is only about 60% of the experimental value. Also, the axial velocity profiles at $x/X_r=1.25$ show that all of the eddy viscosity based models seriously underpredict the rate of momentum recovery after the reattachment process.
Profiles of vertical velocity, $V$, at the same five downstream locations are shown in Figure 10.10a-e. Considering the relatively low values of vertical velocity in comparison with axial velocity, all model variants perform reasonably well in determining the peak values. The general shape of these profiles is also adequately captured if the vertical scale is compared to the local bubble height.

To give a broad picture of the turbulence levels returned using the various models, the same velocity vector diagrams of Figure 10.6 are shown in Figure 10.11, but with the colour scale now indicating levels of turbulence kinetic energy. When viewing these diagrams, it should be borne in mind that the experimental work indicated that the peak level of turbulent kinetic energy anywhere in these computational domains should be around $k=2.4 \text{ m}^2\text{s}^{-2}$ and this should occur around $x/h_f=16$ and $y/h_f=1.75$. For more detailed information, Figures 10.12a-e show profiles of $k$ at five downstream positions, using the different calculation strategies.

Figure 10.11a shows that K1 has returned excessively high levels of $k$ upstream of the normal fence corner because of the unphysical production in this region. This figure also shows how the high level of turbulence in this area is fed into the early part of the shear layer, and causes higher peak levels further downstream. At $x/X_f=0.25$ and 0.5, the peak levels of $k$ are roughly twice as large as determined experimentally.

K2 and K3, on the other hand, generally perform well in terms of capturing peak levels of $k$. Close to separation, the standard $k$-$\varepsilon$ model does rather better than the low Reynolds model because of the latter's slow development of turbulence in the early part of the shear layer. This feature of the low Reynolds number model is clearly seen in Figure 10.11c, where turbulence in the shear layer is not transported adequately to the near-wall region. Further downstream, however, at stations $x/X_f=0.5$, 0.75, 1.0 and 1.25, the standard model predicts peak levels of $k$ which are generally between 15 and 25% too high. For the low Reynolds number model, this range is around 0-15%. Neither of these model variants adequately capture the vertical extent of the profiles of $k$. In fact, K1 performs best in this respect. However, this is simply a consequence of the extremely high levels of turbulence being fed into the shear layer near separation.

The Reynolds stress model fares particularly badly in predicting the height of the $k$-profiles, even though the height of the mean velocity profiles is approximately the same as that determined using the specified input models. This feature of the higher order closure model indicates an inadequacy of the modelled diffusion term in the Reynolds stress transport equations. However, in terms of predicting the peak value of $k$ at the different downstream locations, the Reynolds stress model performs very well. Except at $x/X_f=0.25$, the model
predicts peak levels of $k$ which are generally around 5% greater than experimental values. Since the primary role of the wall reflection terms is to redistribute energy among the normal stress components, it is not surprising that the profiles of $k$ determined using S1 and S2 are very similar.

The secondary effect of the wall reflection terms is to reduce shear stress, and this can be seen in Figures 10.13a-e. Peak values with wall reflection terms are typically around 10% lower than without them, except at $x/X_r=0.25$ where the two values are comparable. The values obtained with the model without wall reflection terms are generally in very good agreement with experimental results. The exception is at $x/X_r=0.25$, where the disagreement in peak values between the experimental profile and the both computational models is around 15%. It is the lower values of $\overline{uv}$ resulting from the implementation of wall reflection terms that causes the reattachment length to be slightly longer than when these terms are omitted. These figures also suggest case K2 and K3 perform reasonably well in predicting peak levels of shear stress. Levels from K2 are rather higher than experimental values in the first half of the separation bubble, and the vertical extent of the profile closest to separation from K3 is very limited. For the latter, it is the low values of shear stress on the low velocity side of the shear layer that leads to reduced entrainment and a longer separation bubble. As with the turbulence kinetic energy, levels of shear stress generated using K1 have been significantly overpredicted at all downstream locations.

Although profiles of turbulent kinetic energy and shear stress determined using the Reynolds stress model with and without the wall reflection terms are very comparable, profiles of the Reynolds normal stresses are certainly not. Profiles of $\overline{u^2}$, $\overline{v^2}$ and $\overline{w^2}$ are given in Figures 10.14, 10.15 and 10.16, respectively. It is clear that the model with wall reflection terms, although displaying advantages at certain positions within the flow, also leads to worse predictions of stress levels at others.

In terms of the $\overline{u^2}$ profiles, S2 predicts more realistic peak values close to separation but tends to overpredict the peak values elsewhere in the flow. However, at most locations, the effect of introducing the wall reflection terms on the peak values is typically only a change of around 10%. Both models share the failing that they underpredict the near-wall levels of $\overline{u^2}$, particularly around reattachment, and both models seriously underpredict the vertical extent of the stresses even though the height of the mean velocity profiles is typically only around 10% lower than experimental values. Close to separation, both models also underpredict the height at which the peak $\overline{u^2}$ occurs, despite the mean velocity profiles agreeing very well with the experimental results.
Close to separation the inclusion of wall reflection terms results in the reduction of peak levels of $\overline{v^2}$ by around 10%, but close to reattachment, the figure is around 40%. The reason why the effect of the wall reflection terms is larger at reattachment than near separation is because of the larger turbulent length scale, $k^3/\varepsilon$, around reattachment. The magnitude of the wall reflection terms is dependent upon this length scale as well as the distance to the nearest wall as can be seen in Equation 3.40. These changes result in the profiles becoming closer to the experimental values around $x/X_r=0.5$, but later in the flow, the significant reductions in values of $\overline{v^2}$ mean that they are lower than they should be.

In contrast, the inclusion of wall reflection terms has made a significant improvement to the vertical profiles of $\overline{w^2}$ at all locations within the separation bubble. At $x/X_r=0.5$, 0.75 and 1.0, the peak values of $\overline{w^2}$ returned by the model with wall reflection terms are within 10% of the experimental values. At $x/X_r=0.25$, the error is larger at around 30%, but the general shape of the profile has been captured. Downstream of reattachment at $x/X_r=1.25$, however, the Reynolds stress model with and without wall reflection terms returns profiles of $\overline{w^2}$ that differ from the experimental profile in terms of peak value and general shape. Although measurements of $\overline{w^2}$ were not made any closer to the splitter plate than 0.5$h_r$, it appears that the profile rises to a peak value very close to the wall, before falling monotonically with increasing height. In contrast the Reynolds stress models predict profiles of $\overline{w^2}$ with a severely restricted vertical extent (around 70% of the experimental value), and with the peak stress occurring much further from the plate at around 1.5$h_r$. The poor prediction of $\overline{w^2}$ at this position could be an indication that the redistribution term is not functioning well in the recovery region.

### 10.7.2 Swept Flow

Results in this section will generally be presented using axes aligned perpendicular and parallel to the fence ($x'$ and $z'$ axes). Measurements will consequently be normalised using the component of free-stream velocity in the $x'$ or $z'$ direction ($U_0=0.985U_{ref}$ and $W_0=0.174U_{ref}$).

Experimental work showed that the reattachment length, measured normal to the fence, was virtually the same in the unswept and 10 degree swept separation. The swept bubble was 20.7$h_f$ long and the unswept bubble 21.4$h_f$ long. This represents a difference of only 3% which could easily have been caused by slight misalignment of the splitter plate. All model
variants predicted virtually no effect on the reattachment length of introducing the 10 degree sweep angle, and so Table 10.1 from the previous section is also appropriate for the discussion of the swept calculations.

Chordwise distributions of static pressure coefficient, scaled using $0.5pU_o^2$ are given in Figure 10.17a and b. Experimental results suggest that beneath the unswept separation bubble, the minimum value of this pressure coefficient is approximately 10% lower than in the swept flow and the position at which it occurs is slightly further upstream. However, all of the model variants indicate that the chordwise variation of this pressure coefficient in the swept and unswept flow is virtually identical. Previous work by Wolf (1987), has shown that for the separated flow over a blunt rectangular body, the downstream distribution of the pressure coefficient based on the velocity component normal to the leading edge, and plotted against chordwise position is approximately independent of sweep angle up to 30 degrees. As discussed in section 8.2, it may be that the nominally spanwise-invariant region of swept flow was actually too close to the three-dimensional region of the swept-v configuration fence. Consequently, it seems likely that all of the model variants have correctly predicted the invariance of the pressure profiles with moderate sweep angle.

Components of skin friction in the $x'$ and $z'$ directions are shown in Figures 10.18 and 10.19, respectively. Both of these coefficients are based on $U_o$. All models return profiles of chordwise shear stress, which are virtually identical to the profiles determined using the unswept fence. The invariance of these profiles with moderate sweep angle also agrees with work carried out by Wolf (1987). In terms of the general predictive performance of $\tau_{wz}'$, the discussion of the results with the unswept flow also applies to the swept results. $\tau_{wz}'$ profiles are less well predicted. The low Reynolds number $k-\varepsilon$ model drastically underpredicts the magnitude of $\tau_{wz}'$ in the reattachment region, and all models perform poorly close to the fence.

All models return profiles of $U'$ and $V$, normalised using $U_o$, that are virtually identical to the results obtained in the unswept flow. These profiles can be seen in Figures 10.20 and 10.21. The slight growth in height of the separation bubble with sweep angle that was apparent from the experimental results has not been predicted by any of the model variants. It is uncertain if the experimentally observed increase in height of the swept separation bubble is genuine or associated with residual three-dimensional effects from the central or the tunnel wall region.
Profiles of $W'/W_o$ at five downstream locations in the swept flow are given in Figures 10.22a-e. As discussed in section 9.4, the experimental results suggest that, broadly speaking, the lateral velocity is invariant with chordwise or vertical position, except very close to the splitter plate where the lateral velocity obviously goes to zero. In agreement with the work of Hancock & McCluskey (1996), these results suggest that the bulk of the flow is convected sideways at a roughly uniform velocity, $W_o$. The slight dip in the $W'(y)$ profile at $x'/X'=0.25$ is believed to be a result of an insufficient aspect ratio, resulting in the flow not being exactly spanwise-invariant. Despite this slight shortcoming in the experimental work, it is pleasing to see that all of the model variants except K3, reasonably predict this bulk sideways motion. The low Reynolds number $k$-$\epsilon$ model, however, does not perform well, showing a much deeper penetration of the no-slip condition on $W'$, especially near reattachment.

Figures 10.23a-e show the lateral velocities in tunnel axes at five chordwise locations. All model variants capture the general shape of the profiles, although the low Reynolds number model consistently underpredicts the magnitude of the peak negative lateral velocity. In general the Reynolds stress models and K2 return the most satisfactory lateral profiles. Both of the Reynolds stress models capture the approximate vertical position at which the peak lateral velocity occurs, and the model with wall reflection terms predicts the minimum lateral velocity at all five locations to within about 0.03$U_{ref}$. All model variants capture the positive $W$ outside the separation bubble, arising from the acceleration of the flow over the fence and bubble.

Polar plots of $U$ and $W$ in tunnel axes are given in Figures 10.24a-e. In these figures, it should be noted that for the calculations involving wall functions (K1, K2, S1 and S2), the apparently coplanar near-wall region is simply a result of linear interpolation of the velocity data between the top of the near-wall cell and the wall. For the low Reynolds number calculation, the predicted coplanar near-wall region is associated with the implicit assumption that the eddy viscosity is isotropic. This implies that very near the surface the shear stress vector is always in the direction of the velocity vector. If the pressure gradients $\partial p/\partial x$ and $\partial p/\partial z$ are small then the flow is locally coplanar.

All model variants indicate that particularly close to separation the variation of $U$ with $W$ in the outer part of the flow is not precisely linear, as do the experimental results. Also, although the agreement of this part of the profile at different chordwise stations is closer than for the present experimental results, it would be wrong to say they coalesced. Overall, the best prediction is given by K2, the Reynolds stress predictions showing profiles with complex shapes at the apex of the triangle, that are not present in the experimental results.
This behaviour is particularly severe for the model without the wall reflection terms and occurs at all downstream stations considered. It is associated with the erroneous near-wall behaviour of the $U$ profiles resulting from the Reynolds stress model. This erroneous behaviour is particularly evident in the profiles of $U$ around attachment, but also occurs to a lesser degree further upstream.

Profiles of $k$ and $u'v'$ determined using the swept fence geometry with all models variants are almost exactly the same as the corresponding unswept profiles, provided the component of free-stream velocity in the direction normal to the fence is used as reference. This is also true for the Reynolds normal stresses in axes aligned with the fence, determined using the Reynolds stress model. Because of the virtually identical nature of these profiles, the figures of the computational result in the unswept flow may also be viewed as the results for the swept flow. (Of course, the non-dimensionalising parameter should now be $U_0$).

The mild lateral flow that is introduced to the flow with small sweep angles appears to have virtually no effect on the turbulence structure. In the flowfield away from the splitter plate, this does not seem surprising as the flow is convected fairly uniformly in the lateral direction, and consequently no strong strain rates are introduced. However, close to the surface, where gradients such as $\partial W / \partial y$ are typically around $0.5 \partial U / \partial y$, the turbulent structure may be expected to be different in the two cases. The streamwise vorticity originating from the fence, arising from the cross-flow, is another feature of the swept flow that may be expected to produce differences with the unswept flow, but which apparently does not at moderate sweep angles. Future investigations will have to consider more extreme sweep angles to determine if the various models predict the same levels of independence.
10. Computational Results

Fig 10.1 Illustrative diagram showing computational domain (not to scale)

Fig. 10.2a Dimensions and boundary conditions of computational domain used for laminar calculation, standard k-ε model and Reynolds stress model

Fig. 10.2b Dimensions and boundary conditions of computational domain used for 'sliced' domain approach (with standard and low Re number k-ε model)
10. Computational Results

Fig. 10.3a Grid arrangement for standard $k$-$\varepsilon$ model and Reynolds stress model

Fig. 10.3b Grid arrangement for 'sliced' domain approach with standard $k$-$\varepsilon$ model

Fig. 10.3c Grid arrangement for 'sliced' domain approach with low Reynolds number $k$-$\varepsilon$ model
10. Computational Results

Fig. 10.4a Comparison of U profiles at x*=0 for unswept flow

Fig. 10.4b Comparison of V profiles at x*=0 for unswept flow

Fig. 10.4c Comparison of k profiles at x*=0 for unswept flow

Fig. 10.5a Comparison of development of V/U with x for unswept flow at y=25mm with and without turbulence model

Fig. 10.5b Comparison of development of V/U with x for unswept flow at y=50mm with and without turbulence model

Fig. 10.5c Comparison of development of V/U with x for unswept flow at y=150mm with and without turbulence model
Fig. 10.6a Velocity vectors and U for unswept flow (standard $k-\varepsilon$ model over whole domain)

Fig. 10.6b Velocity vectors and U for unswept flow (standard $k-\varepsilon$ model with input profiles)
Fig. 10.6c Velocity vectors and U for unswept flow (low Re no k-ε model with input profiles)

Fig. 10.6d Velocity vectors and U for unswept flow (Reynolds stress model over whole domain without wall reflection terms)
Fig. 10.6e Velocity vectors and U for unswept flow (Reynolds stress model over whole domain with wall reflection terms)
10. Computational Results

Fig. 10.7a $C_p$ against $x/h$, for unswept flow

Fig. 10.7b $C_p$ against $x/X_r$, for unswept flow

Fig. 10.8a $C_f$ against $x/h$, for unswept flow

Fig. 10.8b $C_f$ against $x/X_r$, for unswept flow
10. Computational Results

Fig. 10.9a Comparison of U profiles at $x^*=0.25$ for unswept flow

Fig. 10.9b Comparison of U profiles at $x^*=0.5$ for unswept flow

Fig. 10.9c Comparison of U profiles at $x^*=0.75$ for unswept flow

Fig. 10.9d Comparison of U profiles at $x^*=1.0$ for unswept flow

Fig. 10.9e Comparison of U profiles at $x^*=1.25$ for unswept flow
10. Computational Results

Fig. 10.10a Comparison of V profiles at x*=0.25 for unswept flow

Fig. 10.10b Comparison of V profiles at x*=0.5 for unswept flow

Fig. 10.10c Comparison of V profiles at x*=0.75 for unswept flow

Fig. 10.10d Comparison of V profiles at x*=1.0 for unswept flow

Fig. 10.10e Comparison of V profiles at x*=1.25 for unswept flow
Fig. 10.11a Velocity vectors and k for unswept flow (standard k-ε model over whole domain)

Fig. 10.11b Velocity vectors and k for unswept flow (standard k-ε model with input profiles)
10. Computational Results

Fig. 10.11c Velocity vectors and k for unswept flow (low Re no $k$-$\varepsilon$ model with input profiles)

Fig. 10.11d Velocity vectors and k for unswept flow (Reynolds stress model over whole domain without wall reflection terms)
Fig. 10.11e Velocity vectors and k for unswept flow (Reynolds stress model over whole domain with wall reflection terms)
10. Computational Results

Fig. 10.12a Comparison of k profiles at $x^*=0.25$ for unswept flow

Fig. 10.12d Comparison of k profiles at $x^*=1.0$ for unswept flow

Fig. 10.12b Comparison of k profiles at $x^*=0.5$ for unswept flow

Fig. 10.12e Comparison of k profiles at $x^*=1.25$ for unswept flow

Fig. 10.12c Comparison of k profiles at $x^*=0.75$ for unswept flow
10. Computational Results

Fig. 10.13a Comparison of \( \overline{uv} \) profiles at \( x^*=0.25 \) for unswept flow

Fig. 10.13b Comparison of \( \overline{uv} \) profiles at \( x^*=0.5 \) for unswept flow

Fig. 10.13c Comparison of \( \overline{uv} \) profiles at \( x^*=0.75 \) for unswept flow

Fig. 10.13d Comparison of \( \overline{uv} \) profiles at \( x^*=1.0 \) for unswept flow

Fig. 10.13e Comparison of \( \overline{uv} \) profiles at \( x^*=1.25 \) for unswept flow
10. Computational Results

Fig. 10.14a Comparison of $u'$ profiles at $x^*=0.25$ for unswept flow

Fig. 10.14b Comparison of $u'$ profiles at $x^*=0.5$ for unswept flow

Fig. 10.14c Comparison of $u'$ profiles at $x^*=0.75$ for unswept flow

Fig. 10.14d Comparison of $u'$ profiles at $x^*=1.0$ for unswept flow

Fig. 10.14e Comparison of $u'$ profiles at $x^*=1.25$ for unswept flow

Fig. 10.14f Comparison of $u'$ profiles at $x^*=0.75$ for unswept flow
Fig. 10.15a Comparison of $\bar{v}$ profiles at $x^*=0.25$ for unswept flow

Fig. 10.15b Comparison of $\bar{v}$ profiles at $x^*=0.5$ for unswept flow

Fig. 10.15c Comparison of $\bar{v}$ profiles at $x^*=0.75$ for unswept flow

Fig. 10.15d Comparison of $\bar{v}$ profiles at $x^*=1.0$ for unswept flow

Fig. 10.15e Comparison of $\bar{v}$ profiles at $x^*=1.25$ for unswept flow
10. Computational Results

Fig. 10.16a Comparison of $\bar{w}$ profiles at $x^*=0.25$ for swept flow

Fig. 10.16b Comparison of $\bar{w}$ profiles at $x^*=0.50$ for swept flow

Fig. 10.16c Comparison of $\bar{w}$ profiles at $x^*=0.75$ for swept flow

Fig. 10.16d Comparison of $\bar{w}$ profiles at $x^*=1.00$ for swept flow

Fig. 10.16e Comparison of $\bar{w}$ profiles at $x^*=1.25$ for swept flow
10. Computational Results

Fig. 10.17a $C_p'$ against $x'/h$, for the swept flow

Fig. 10.17b $C_p'$ against $x'/X'$, for the swept flow

Fig. 10.18a $C_{t e}'$ against $x'/h$, for the swept flow

Fig. 10.18b $C_{t e}'$ against $x'/X'$, for the swept flow

Fig. 10.19a $C_{w}'$ against $x'/h$, for the swept flow

Fig. 10.19b $C_{w}'$ against $x'/X'$, for the swept flow
10. Computational Results

Fig. 10.20a Comparison of chordwise velocity profiles at $x^*=0.25$ for the swept flow

Fig. 10.20b Comparison of chordwise velocity profiles at $x^*=0.5$ for the swept flow

Fig. 10.20c Comparison of chordwise velocity profiles at $x^*=0.75$ for the swept flow

Fig. 10.20d Comparison of chordwise velocity profiles at $x^*=1.0$ for the swept flow

Fig. 10.20e Comparison of chordwise velocity profiles at $x^*=1.25$ for the swept flow
10. Computational Results

Fig. 10.21a Comparison of vertical velocity profiles at $x^*=0.25$ for the swept flow

Fig. 10.21b Comparison of vertical velocity profiles at $x^*=0.5$ for the swept flow

Fig. 10.21c Comparison of vertical velocity profiles at $x^*=0.75$ for the swept flow

Fig. 10.21d Comparison of vertical velocity profiles at $x^*=1.0$ for the swept flow

Fig. 10.21e Comparison of vertical velocity profiles at $x^*=1.25$ for the swept flow
10. Computational Results

Fig. 10.22a Comparison of lateral velocity profiles at \( x^* = 0.25 \) for the swept flow

Fig. 10.22b Comparison of lateral velocity profiles at \( x^* = 0.5 \) for the swept flow

Fig. 10.22c Comparison of lateral velocity profiles at \( x^* = 0.75 \) for the swept flow

Fig. 10.22d Comparison of lateral velocity profiles at \( x^* = 1.0 \) for the swept flow

Fig. 10.22e Comparison of lateral velocity profiles at \( x^* = 1.25 \) for the swept flow
10. Computational Results

Fig. 10.23a  Lateral mean velocity profiles at $x^*=0.25$ for the swept flow

Fig. 10.23b  Lateral mean velocity profiles at $x^*=0.5$ for the swept flow

Fig. 10.23c  Lateral mean velocity profiles at $x^*=0.75$ for the swept flow

Fig. 10.23d  Lateral mean velocity profiles at $x^*=1.0$ for the swept flow

Fig. 10.23e  Lateral mean velocity profiles at $x^*=1.25$ for the swept flow
10. Computational Results

Fig. 10.24a Polar plot of $U$ against $W$ at $z=380$mm and $x^*=0.25$ for the swept flow

Fig. 10.24b Polar plot of $U$ against $W$ at $z=380$mm and $x^*=0.5$ for the swept flow

Fig. 10.24c Polar plot of $U$ against $W$ at $z=380$mm and $x^*=0.75$ for the swept flow

Fig. 10.24d Polar plot of $U$ against $W$ at $z=380$mm and $x^*=1.0$ for the swept flow

Fig. 10.24e Polar plot of $U$ against $W$ at $z=380$mm and $x^*=1.25$ for the swept flow
11. Conclusions and Further Work

11.1 Conclusions
11.1.1 General Measurement Techniques

• Separated flows have been shown to be very sensitive to probe interference effects. Using a standard pulsed-wire probe with the main body of the probe having a diameter of 0.05\(X_r\), the length of the separation bubble could be shortened by more than 10% when the probe was held vertically just upstream of reattachment with the probe head immediately above the surface. With the probe so close to the surface, it was found that the reattachment length at a given point was prone to interference effects even when the probe was one reattachment length away from the point in the axial or lateral direction.

• By redesigning the standard probe so that it measured the axial velocity moments when the probe was slanted forward in the flow, at 45 degrees to the y-axis, the interference effect could be reduced by more than 50%.

• It has been shown that if the pulsed-wire probe is held in a shear layer with the two sensor wires at different heights in the shear layer, large errors in Reynolds stresses can occur. The histogram of the samples measured by each sensor wire will be either stretched or compressed depending on the sign of the velocity measured by each sensor wire. Since this orientation has been used by many workers to determine \(\bar{uv}\) and \(\bar{\nu^2}\) in separated flows, their results must be treated with caution.

11.1.2 Unswept Separation

• The reattachment length, \(X_r\), of the unswept separation was 21.4\(h_f\).

• The present work supports the findings of Hancock, McCluskey and Castro (1992) that suggest the aspect ratio of the fence and splitter plate arrangement, \(W/X_r\), must be greater than around 4.5 for a spanwise-invariant region to exist. Virtually all previous investigations of separated flow behind a fence with a splitter plate have been made in flows with aspect ratios that are considerably less than this figure. Of the previous investigations of unswept separated flow, Jaroch and Fernholz (1989) used the highest aspect ratio and this figure was only 2.6.

• Provided that the flow either side of the splitter plate is symmetrical, it appears that the reattachment length is capable of collapsing profiles of axial velocity within the separation bubble, taken in flows with different values of blockage ratio, aspect ratio and free-stream turbulence. This implies that the shape of symmetrical separation bubbles (the ratio of
length to height) generated using the fence and splitter plate geometry has a universal nature.

- It has also been shown that the local bubble height, \( H_b \), defined as the height off the splitter plate at which \( U = 0.95U_{max} \) is capable of collapsing axial velocity profiles taken in flows where the plate alignment and consequently the symmetry of the separation bubbles above and below the plate is less certain.

- The near-wall velocity measurements beneath the separation bubble of the present investigation approximately adhere to the relationship \( U/U_N = f(y/y_N) \) and collapse with data of other workers such as Adams, Johnston & Eaton (1984) and Devenport & Sutton (1991). Furthermore it has been shown that the relationship, \( C_{fN} \propto Re_N^{-1} \) does not apply to the present results and this implies that the relationship \( U/U_N = f(y/y_N) \) is not valid right down to the surface of the splitter plate.

- The peak \( u^2 \) measurements made around the reattachment position using the 5mm and 10mm high fences plotted against \( Re_{hf} \) lie close to the curve described by the empirical relationship:

\[
\frac{u^2_{max}}{U_{ref}^2} = 0.06 \log(Re_{hf}) - 0.15
\]

proposed by Hancock (1994) for the range \( 500 < Re_{hf} < 5000 \), as do the results of Ruderich & Fernholz (1986) and Hancock (1994). This relationship may be a useful tool for checking measurement accuracy for future investigations.

- At the low Reynolds numbers considered during the present investigation, the peak values of all the Reynolds normal stresses, normalised using \( (\Delta U)^2 \), initially fall with downstream position, reaching a minimum close to \( x/X_p = 0.5 \) before rising to a peak value close to the reattachment position. Peak values of \( -\bar{uv} \), on the other hand, rise monotonically all the way from separation to reattachment.

### 11.1.3 Spanwise-Invariant Region of the Swept Flow

- Flow visualisation studies revealed that using a v-configuration geometry with an aspect ratio, based on \( h_p \), of each arm of 78 resulted in approximately spanwise-invariant flow on the planes that were approximately halfway between the tunnel walls and the centre of the tunnel (\( z = \pm 38h_p \)).
The length of the swept separation bubble in the nominally spanwise-invariant region, measured normal to the fence, was approximately 20.7\(h_f\) (around 3% shorter than the unswept separation bubble).

The magnitude of the minimum pressure coefficient, \(2(p - p_{ref})/\rho U_0^2\), beneath the swept separation bubble in the nominally spanwise-invariant region was approximately 10% smaller than beneath the unswept separation bubble. This suggested that the \(z=380\)mm plane of the swept flow suffered from residual three-dimensional effects from around the central plane. It may be that surface pressure is a more stringent test of spanwise-invariance than other mean flow parameters.

Close to separation, the height of the unswept and swept separation bubble is very similar. However, around reattachment the swept bubble is around 12% higher than its unswept counterpart. This change in shape of the separation bubble is believed to be genuine as it was also observed by Hancock & McCluskey (1997).

Profiles of \(U'/U_0\) against \(y/H_b\) at various chordwise positions in the unswept and swept invariant flow were virtually identical.

Although introducing a mild sweep angle changed the chordwise development of the height of the shear layer centreline, \(y_c\), properties such as the chordwise development of the shear layer thickness, \(A\), were virtually unchanged.

Profiles of \(U'/U_N\) against \(y/y_N\) at various chordwise positions within the separation bubble for the unswept and swept invariant flow were very similar. This suggests that the near-wall model suggested by Simpson (1983), developed for use in calculation methods of unswept separated flows, may also be applicable to the near-wall chordwise velocity profiles of swept flows.

Broadly, the lateral velocity, \(W'\), is invariant with chordwise or vertical position, except very close to the splitter plate where the lateral velocity obviously goes to zero. In agreement with the work of Hancock & McCluskey (1997), the bulk of the flow is convected sideways at a roughly uniform velocity, \(W_0\).

In the spanwise-invariant region, the lateral velocity taken in the direction normal to the flow rig centreline (as opposed to parallel to the fence) is negative (towards the central plane) except in the outer part of the flow where it is positive (away from the central plane). This is an inviscid phenomenon and is a result of the flow accelerating in the \(x'\)-direction because of the blockage imposed by the separation bubble. This increase in \(U'\) gives a positive contribution to \(W\).

Polar plots of \(U\) and \(W\) from the present investigation support the findings of Hancock & McCluskey (1997) that the flow is approximately coplanar very close to the splitter plate. However, they do not imply a linear variation of \(U\) with \(W\) in the outer part of the flow.
Also, unlike the work of Hancock & McCluskey, the present results suggest that the measurements in the outer part of the flow, plotted in polar form, do not precisely coalesce at different chordwise locations.

- Generally, the Reynolds stresses, normalised using $U_0^2$ were very similar in the unswept and the mildly swept flow, even close to the surface where $\partial W / \partial y$ is of the same order of magnitude as $\partial U / \partial y$ for the swept flow. However, at $x' / X_0 = 0.25$ and $0.5$ peak levels of $\overline{v^2}$ are at least 50% higher in the swept flow. Since the differences are only apparent in the upstream half of the separation bubble, they may be related to the extra component of vorticity that is fed into the early part of the shear layer of the swept flow.

### 11.1.4 Three-Dimensional Region of the Swept Flow

- The length of the separation bubble on the central plane, as defined by the attachment line, is about 20% longer than in the invariant region and the attachment length changes fairly continuously between these two lateral stations. The height of the bubble, on the other hand, changes significantly over a fairly narrow region (approximately in the range $0 < z < 0.5 X_{a_0}$) and is 40% greater on the central plane than in the invariant region. The increase in height and length of the separation bubble with proximity to the central plane is associated with the necessary streamline pattern for this flow configuration. In the spanwise-invariant region the separation streamline is the same as the attaching streamline whereas in the central region, the lateral inflows from the two sides of the ‘v’ mean that the separating streamlines remain above the surface allowing a mass outflow equal to the side inflows. This process tends to ‘swell’ the central region causing a higher and longer separation bubble.

- Close to attachment, the bubble on the central plane is 70% higher than in the invariant region compared to the 40% increase at the bubble centre. The swelling effect of the two side flows appears to have more of an affect in the latter part of the bubble than close to separation.

- The minimum pressure on the central plane, where the separation bubble is longest, is not as low as at other lateral stations where the bubble is shorter. As with the lateral variation of $X_a$, the lateral variation of the minimum static pressure extends all the way from the central plane to the spanwise-invariant region.

- The lateral variation of $(-\tau_{\text{mean}})_{\text{max}}$ behaves in quite a different way from $(-\overline{uv})_{\text{max}}$, highlighting the independence of the inner and outer flow for separated flows, noted by Hancock (1994).
11. Conclusions and Further Work

- Within the separation bubble, the maximum reverse velocity in the upstream half of the separation bubble is considerably higher on the central plane than on the other lateral stations. At $x/X_s=0.25$, it is approximately 30% of $U_{ref}$ at $z=0$ mm, which is about 60% higher than in the spanwise-invariant region. This is associated with the absence of a secondary separation process at $z=0$ mm. The reverse flow is able to maintain its negative $U$-momentum until very close to the fence before changing direction and moving away from the central plane.

- At all downstream positions considered away from the central plane, the lateral velocity, $W$, was positive (away from the central plane) in the outer part of the flow. This is an inviscid effect as previously noted for the invariant region.

- Although $(-W)_{max}$ at the various stations considered increased monotonically away from the central plane, the variation of $W$ with $z$ at constant $x''$ (where $x''$ is that measured on the $z=0$ mm plane) showed a more complex variation. On the central plane the flow was converging in a fairly narrow vertical range close to the splitter plate, and the outer flow was diverging. Peak positive values of $\partial W/\partial z$ were very similar to the magnitude of the minimum negative values of $\partial W/\partial z$.

- The region of diverging flow was limited to the region close to the central plane ($z<0.15 X_{d}$) and the highest values occurred on the central plane just downstream of separation.

- The region of diverging flow close to the central plane resulted in increased levels of turbulent mixing in this region and consequently the average growth rate over the length of the bubble is higher on the central plane than at other lateral stations.

- The lateral variation in peak values of $\overline{u^2}$, $\overline{w^2}$, $(-uw)$, and $k$ were broadly similar. In the first half of the separation bubble, the highest values occur on the central plane. At $x/X_s=0.5$, there is a gradual reduction in the levels with distance away from the central plane, before rising again towards the spanwise-invariant region. Around attachment the lateral variation of the peak Reynolds stresses is similar to that further upstream but now the values on the central plane and the $z=40$ mm plane are comparable. In fact, at attachment, peak levels of $\overline{u^2}$ and $k$ are actually higher on the $z=40$ mm plane than on the central plane.

- Peak values of $k$ at $x/X_a=0.25$ were about 50% higher on the central plane than on the $z=40$ mm plane despite the fact that the peak value of $\partial U/\partial y$ for the two planes were virtually identical. However, the value of $(\partial W/\partial z)/(\partial U/\partial y)$ on the central plane close to the position at which the peak in $k$ occurred was 0.05, whereas it was almost zero on the $z=40$ mm plane. Since this strain ratio is known to play an important part in the turbulence
structure of a flow it seems probable that the diverging flow on the central plane close to separation is the primary cause of the high turbulence in this region.

- Broadly speaking, the lateral variation of \((\partial W/\partial z)/(\partial U/\partial y)\) on the shear layer centreline over the entire lateral range at all downstream locations is qualitatively very similar to the lateral variation of peak turbulent kinetic energy.

- The mixing layer parameter, \((y - y_c)/\Lambda\), succeeds in bringing together the profiles of \(k/(\Delta U)^2\), taken at different lateral stations, outside \((y - y_c)/\Lambda \approx 0.25\). Furthermore, in this vertical range the profiles concur with those from the plane mixing layer.

### 11.1.5 Computational Work (Unswept Flow)

- ‘Slicing’ the computational domain appears to have led to satisfactory calculations for the \(k-\varepsilon\) model, by removing excess turbulence upstream of the point of natural transition. Using this ‘sliced domain’ approach means that no modification needs to be made to enlarge the dissipation in a \(k-\varepsilon\) calculation to counteract erroneously generated turbulence that will arise if the eddy viscosity is allowed to be non-zero where it obviously should be.

- The ‘sliced’ domain approach with the standard \(k-\varepsilon\) model resulted in a separation bubble that was about 80% of the experimental value, and peak values of \(k\) which were generally between 15 and 25% too high.

- At the Reynolds number of the experiments, the near-wall model of Launder & Sharma had such a large effect as to suppress the turbulence altogether. This was because the eddy viscosity damping term, \(f_\mu\), in the model used is expressed in terms of a turbulent Reynolds number and does not contain a ‘distance to nearest wall’ function, which resulted in too much damping away from solid boundaries.

- The Launder Sharma model had to be run using an artificially low value of molecular viscosity, in order to reduce the effect of the eddy viscosity damping term. A threshold value of 0.3\(\nu\) was necessary to promote transition, and even then the development of turbulence in the early part of the shear layer was slower than it should have been, resulting in a separation bubble that was longer than predicted using the other model variants.

- The Reynolds stress model with and without the wall reflection terms of Gibson & Launder (1978) resulted in separation bubbles that were about 80% of the experimental
11. Conclusions and Further Work

value. However, determining the reattachment length using both of these models was very difficult as the predicted shear stress failed to concur with the mean velocity field.

- The Reynolds stress model with and without wall reflection terms predicted peak levels of \( k \) that were in very good agreement with experimental values. Except at \( x/X_f=0.25 \), peak levels were typically only 5-10% greater than experimental values.

- The effects of adding wall reflection terms to the pressure strain redistribution term are not confined to the near-wall region. In fact, the effects extend across the entire vertical extent of the profiles of the Reynolds stresses. Broadly speaking, the wall reflection terms lead to marginally better predictions for \( \overline{v^2} \) and \( \overline{w^2} \), with little to choose between them as far as \( \overline{u^2} \) and \( \overline{uv} \) are concerned.

11.1.6 Computational Work (Swept flow)

- All model variants predicted a reattachment length, measured normal to the separation line, that was negligibly different from the unswept case. Furthermore, using \( U_o \) to normalise, all model variants predicted profiles of \( U' \) and \( V \) that were virtually identical to the unswept case. Profiles of \( C_p' \) and \( C_p'' \) were also virtually identical in the two flows.

- In broad agreement with the experimental work, all model variants except the low Reynolds number \( k-\varepsilon \) model predicted that in the swept case, the bulk of the flow is convected sideways at a roughly uniform velocity, \( W_o \). The low Reynolds number \( k-\varepsilon \) model, predicted a much deeper penetration of the no-slip condition on \( W' \), especially near reattachment.

- Vertical profiles of \( k \) and \( \overline{uv} \) determined using the swept fence geometry with all model variants are almost exactly the same as the corresponding unswept profiles, provided the component of free-stream velocity in the direction normal to the fence is used to normalise. This is also true for the normal Reynolds stresses in axes aligned with the fence, determined using the second order closure model (with and without wall reflection terms). In agreement with the experimental data, all models indicate that the mild lateral flow that is introduced to the flow with small sweep angles appears to have virtually no effect on the turbulence structure.
11.2 Further Work

An obvious extension to the present work would be to consider a similar backward facing v-shaped geometry but with a greater sweep angle. With this type of flow, provided the aspect ratio was large enough, the effect of the lateral flow on the spanwise-invariant region could be investigated further. If very high sweep angles were considered, the reported breakdown of the independence with sweep angle of certain properties of the separation bubble, such as the chordwise reattachment length at around 40 degrees, could be examined. The reasons for the variation of chordwise profiles of $C_f'$ and $C_p'$ with sweep angles greater than around 30 degrees could also be investigated. It would be interesting to see what changes in the turbulence structure accompanied such changes in mean properties of the swept separation bubble.

An increased sweep angle would also lead to higher magnitudes of $\partial W/\partial z$ in the central region, and presumably even more dramatic lateral variations of the Reynolds stresses. With such a flow, the role of the strain ratio, $(\partial W/\partial z)/(\partial U/\partial y)$, in describing the turbulence structure of these types of flows, would perhaps be clearer.

Using a forward facing v-shaped geometry may also be useful in determining the effects of lateral convergence/divergence on separated flows. Although the present geometry was designed to provide a region of converging flow close to the central plane, it transpired that the flow was diverging over a large vertical portion of the flow. Presumably a forward facing v-shaped geometry, with the outer part of the flow accelerating in the direction normal to the fence would lead to converging flow over a certain vertical range. If negative values of $\partial W/\partial z$ were present, it would be interesting to see if this resulted in a suppressing effect on the Reynolds stresses.

Concerning the computational work, a more appropriate model needs to be developed and tested for the near-wall flow. The model of Simpson (1983) appears to be an excellent starting point for both the unswept and the swept flow, at least for mild sweep angles.
References


References


References


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References


Appendix A

Numerical Implementation of CFX-F3D

Linearised transport equations are derived by integrating the discretised transport equations over control volumes (cells). An iterative technique is used to solve the equations in order to capture the non-linearity of the underlying differential equations, and it is used at two levels: an inner iteration is used to solve the spatial coupling for each variable and an outer iteration solves for the coupling between variables. Thus each variable is taken in sequence, regarding all other variables as fixed, a discrete transport equation for that variable is formed for every cell in the flow domain and the problem is handed over to a linear equation solver which returns the updated values of the variable. The non-linearity of the original equations is simulated by reforming the coefficients of the discrete equations, using the most recently calculated values of the variables, before each inner iteration. (The present investigation has used the ‘Algebraic Multi-grid’ solver for the velocity equations and ‘line relaxation’ for the $k$, $\varepsilon$ and Reynolds stress transport equations).

The treatment of pressure is slightly different from the above procedure, since it does not obey a transport equation. Instead, simplified versions of the discrete momentum equations are used to derive a functional relationship between a correction to the pressure and corrections to the velocity components in each cell. Substitution of this equation into the continuity equation leads to an equation linking the pressure correction with the continuity error in the cell. This set of simultaneous equations is passed to a linear equation solver. The solution is used both to update pressure and to correct the velocity field through the functional relationship in order to enforce mass conservation. In CFX-F3D, pressure is solved using the SIMPLEC (Van Doormal and Raithby, 1984) variation of the SIMPLE (Patanker and Spalding, 1972) pressure correction method. However, in order to implement the SIMPLEC algorithm, normal velocity components on control-volume faces must be approximated somehow from the velocity components at control volume centres. CFX-F3D achieves this using the Rhie-Chow (1983) interpolation formula instead of adopting the usual approach of having a staggered grid.

CFX-F3D discretises all equations in space using second order centred differencing apart from the advection terms, described below, and the convection coefficients obtained using the Rhie-Chow interpolation formula. During the present investigation, the method of discretisation chosen for the advection terms using the standard and low Reynolds number $k$-$\varepsilon$ model computations was the QUICK (quadratic upwind differencing) scheme for all equations, except the $k$ and $\varepsilon$ equation which used the HYBRID (combination of upwind and
central differencing) scheme. Computations involving Reynolds stress closure used the UDS (upwind differencing scheme) to model the convective terms of the momentum equations and the HYBRID scheme was used for the Reynolds stress transport equations and the $\epsilon$ equation.
Moderately Three-Dimensional Separated and Reattaching Turbulent Flow

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Abstract — Pulsed-wire measurements of velocity moments and surface shear stress have been made in a mildly converging three-dimensional separated flow, formed downstream of a 'v-shaped' separation line. The flow was symmetrical and wide enough for it to asymptote in the lateral direction to spanwise invariance, thereby providing well defined boundary conditions for the lateral inflow, unaffected by end effects, as well as linking this study to the far more extensively studied two-dimensional separated flow. Large effects occur across the whole bubble height, whereas in the degenerate case of spanwise invariance the effects of cross flow are confined to fairly near the surface. Even in this mild case large lateral gradients arise in the stresses, and small extra rates of strain have a large effect. The flow can be divided into two subregions where the effects differ substantially in each.

1. Introduction

Studies of separated flows largely fall into two broad categories — those in which the mean flow is nominally two-dimensional, and those where the mean flow is highly three-dimensional and therefore considerably more complex. For the latter, comparatively little is known regarding the physics of the turbulence structure, and, moreover, there is no systematic link between the 'simple' case of the former — studied for obvious reasons — and the more general. Therefore, the present study is an experimental investigation of turbulent three-dimensional separated flow, by systematic departure from the two-dimensional case by the introduction of lateral, stress-driven motion. The framework employed here is as outlined by McCluskey et al. [1] and comprises three flow types; a) a spanwise-invariant flow in which the flow is two-dimensional but not co-planar, b) stress-driven divergent flow, and c) stress-driven convergent flow. In b) and c) all nine rates of extra strain are generally non zero. In a) three are zero.

The spanwise invariant case has been considered by Hancock and McCluskey [2], where the separated flow was generated downstream of a straight but swept separation line. They found that in this degenerate case the effects of three-dimensionality are confined to an inner layer, roughly 15% of the bubble height, whereas the flow in the middle and outer layers is largely convected in the spanwise direction. In contrast, the present results show that this is not the case even for mild departure from spanwise invariance; the whole bubble is strongly affected. Thus, studies of spanwise invariant flows (of which the two-dimensional co-planar case is a special case), though important precursors, are of limited use in understanding general three-dimensional separated flow.

The present flow was generated behind a 'v-shaped' sharp separation line, of mild sweep (±10°). A convenient way of illustrating the configuration is by means of a plot of the surface streamlines, as shown in figure 1. The flow width was deliberately large enough for the adjacent side regions to contain spanwise invariant flow, thereby providing well defined upstream boundary conditions for the lateral in-flow. Figure 2 gives an illustrative sketch of
salient streamlines (and also shows how the opposite case of type b) can be generated). Some earlier measurements were reported by Hardman and Hancock [3].

2. Experimental Details

The separation was formed behind a vertical flat plate ('fence') mounted on the front of a splitter plate, as illustrated in figure 3. This was positioned at the centre of a working section 500mm high. The half fence height, \( h_f \), was 10mm above the splitter plate, which was 3mm thick. The working section width was 1500mm (i.e. 750mm from the symmetry plane to the side walls) and was wide enough to provide flanking regions of spanwise-invariant flow before the effects of the side-wall flows became significant. The free stream velocity, \( U_{ref} \), was 5.8 m/s, and the Reynolds number based on fence height \( (h_f) \) was 3800.

Measurements within the bubble down to a height of 5mm off the splitter plate were made using a miniature pulsed-wire probe. Measurements nearer the plate down to a height of 0.5mm were made using a through-wall pulsed-wire probe. Both of these devices had pulse and sensor wire lengths of about 6mm and separations of about 0.5mm, the latter dimension restricting the maximum velocity to about 8 m/s. Close conformity to a cosine behaviour spanned at least ±70 degrees in yaw and ±80 degrees in pitch. In order to measure velocity moments up to fourth order, each point in the field required five probe rotation angles, at typical intervals of 15 degrees. Preliminary measurements were used to determine suitable orientation angles and care was taken not to exceed the pulsed-wire's cone of receptivity. Surface shear stress was measured by means of a pulsed-wire shear stress probe calibrated in a turbulent boundary layer against Preston tubes.

3. Results

Unless otherwise stated, the mean velocity and Reynolds stress measurements are presented using 'wind tunnel' axes as opposed to axes aligned with the fence (i.e. \( x, z \) rather than \( x', z' \)) as shown in figure 4, where \( z \) is measured from the symmetry plane and \( x \) is measured from the fence. \( y \) is vertical, measured from the splitter plate surface. \( X_A \) is the distance to attachment, and the position along the bubble, \( x/X_A \), is denoted by \( x^* \). \( X_{Ao} (= 250 \text{mm}) \) denotes the attachment length at \( z = 0 \). Velocities are denoted by \( U, V \) and \( W \) in the \( x-, y- \) and \( z \)-directions and by \( U' \) and \( W' \) in the \( x'- \) and \( z'- \)directions. Fluctuations are denoted by the lower case. In the figures all velocities are normalised by the upstream free stream reference velocity, \( U_{ref} \).

3.1 Mean flow

Separating streamlines only reattach when \( \partial W/\partial z \) is zero – i.e. only in a spanwise invariant flow are separating and attaching streamlines connected (McCluskey et al. [1]). For converging flow the separating streamlines remain above the attaching streamlines because of the lateral inflow (figure 2). The growth of the bubble as the central plane is approached is clearly shown in figure 5, where the length (as defined by \( X_A \)) and height of the separation bubble at various lateral stations are compared. The height of the bubble is defined as the vertical distance above the splitter plate at which \( U = 0.95 U_{ref} \) and in figure 5 it is presented at two downstream positions, \( x^* = 0.5 \) and 1.0. On the central plane the bubble is about 20% longer than in the spanwise invariant region, but the height at \( x^* = 0.5 \) is about 40% larger at \( z = 0 \) than it is in the invariant region. At attachment, the bubble on the central plane is about 70% higher than the invariant region. The bubble at the centre is not only larger than in the invariant region, but also has a different shape. Figure 5 is an example of
how in contrast to the spanwise invariant region the effects of three-dimensionality are *not* in general confined to an inner layer, but occur across the whole bubble.

The size of the bubble has reduced to almost that of the side regions by about $z = 0.8X_{A_0}$ ($z = 200\text{mm}$). In contrast, the surface pressures show a more gradual lateral variation as can be seen in figure 6, where $C_P = (p-p_{ref})/0.5\rho U_{ref}^2$. The general shape of these profiles taken at different lateral stations is very similar but, not surprisingly, the value of the minimum pressure becomes less negative as the central plane is approached. The focus shown in figure 1 (which terminates the secondary separation line near the fence at larger $z$) is presumably a consequence of the negative $\partial p/\partial z$. The change in pressure and other quantities with $z$ is probably partly associated with the lateral inflows providing some of the entrainment required by the overlying mixing layer, which in the absence of any lateral inflow, is supplied entirely from around attachment – i.e. less fluid has to come from attachment to make up for the fluid being entrained in the mixing layer (see Yang et al.[3]). This inflow would also be expected to make the separation bubble longer. The flow is not spanwise invariant until about $z = 1.5X_{A_0}$.

Figures 7a, 7b and 7c show $U$, $V$ and $W$, respectively, at the downstream position of $x^* = 0.5$ for the six lateral stations. The streamwise mean velocity, $U$, is virtually unchanging with $z$ outside $z = \pm 0.2X_{A_0}$, whereas the adjustment in lateral velocity, $W$, extends further than $z = X_{A_0}$. The maximum reversed velocity at this station is also about 15% *higher* at $z = 0$ than it is in the spanwise invariant region. $W$ does not exceed about $0.18U_{ref}$ and the lateral flow is mild in the sense that maximum $W$ is, say, an order of magnitude less than the maximum $U$. Positive $W$ in the outer part of the separation is an inviscid effect; in the spanwise invariant region the velocity $U'$, perpendicular to the fence, increases giving a positive contribution to $W$. (The scatter in $W$ measured on $z = 0$ is typical and is equivalent to a resolution of better than $\pm 0.5$ degrees, and a calibration accuracy of around $\pm 1\%$). It is also interesting to note that, unlike the other lateral stations, on the central plane at $x^* = 0.5$, the $V$ profile remains positive as the two lateral inflows meet to give $W = 0$, which requires an increase in one or both of $U$ and $V$.

Figure 8 shows the variation of negative peak in $W$ with $z$ at $x^* = 0.25$, 0.5 and 1.0. As expected, $W$ decreases in magnitude most rapidly near $z = 0$ with the highest lateral velocities occurring in the spanwise invariant region. Interestingly, the change of $W$ with $z$ is much more gradual near the separation line where the length scale of the bounding mixing layer is ‘small’ and more rapid near the attachment line where the scale is ‘large’. This is probably because the early part of the mixing layer provides comparatively less stress-driven lateral motion near the relatively more distant splitter plate surface, or perhaps more likely, relatively *less* resistance to lateral motion driven by a negative $\partial p/\partial z$. The variation of $W$ with lateral position at downstream positions $x^* = 0.5$ and $x^* = 1.0$ at two heights is given in figure 9. Close to the splitter plate (up to a height of around $1.5h_\ell$) the variation of $W$ with $z$ is monotonic, with the maximum inwards lateral velocity occurring in the spanwise invariant region. However, lateral profiles taken at the highest measuring stations ($y = 4h_\ell$ and above) show that there is a positive value of $\partial W/\partial z$ on the central plane with a fairly constant outwards lateral velocity occurring at other lateral stations, arising from the inviscid effect noted earlier. The general features of these profiles is the same at other downstream stations within the bubble. In the vertical range $1.5h_\ell < y < 4h_\ell$, the lateral variation of $W$ at the various downstream stations becomes more complex. The behaviour of $V$ and $W$ suggest the presence of counter-rotating roughly streamwise vortex-like structures, one on each side of the central plane, $z = 0$. It seems likely that these partly originate from the lateral inflow.
from the side regions, and from the focal structure seen in the surface streamlines, and then convected downstream beneath the separating streamlines (figure 2).

Though not shown here the flow near the surface is locally co-planar, as observed by Hancock and McCluskey [2] for the spanwise-invariant case, and W varies linearly with U over the outer part of the bubble, but between these the variation does not conform to the velocity triangle previously observed.

3.2 Reynolds stresses

Figure 10 and 11 show profiles of $u\bar{v}$ and $q^{2}/2$ at various lateral stations at $x^{*} = 1.0$. These figures are another clear demonstration of how the three-dimensional nature of the flow extends across the whole bubble. The lateral variation of the maximum values of these quantities are shown in figures 12 and 13. Figure 12 also shows the maximum of the x-component skin friction component ($-C_{fx}$)$_{max}$.

These figures suggest that the flow can be split into two parts: a central region of width roughly $\pm 0.8X_A$ ($\pm 200$mm) and an intermediate region. In the latter — a perturbation of the spanwise-invariant flow — $q^{2}/2$ and ($-C_{fx}$)$_{max}$ all decrease in proportion as the centre is approached, the individual stresses decreasing in approximate proportion. A decrease in stress levels is not surprising in view of compression of lateral vorticity by negative $\partial W/\partial z$. Between the planes $z = 380$ and $z = 200$ the stresses have decreased by about 20%. On the $z = 200$mm, the strain rate $\partial W'/\partial z'$, with $W'$ and $z'$ taken as parallel to the fence (figure 4), is such that $(\partial W'/\partial z')_{max}/(\partial U/\partial y)_{max}$ is around 0.004 in magnitude. This degree of sensitivity clearly shows that the extra strain rates have a relatively large effect.

Inside $z = 200$mm, the behaviour is rather more complex. $u\bar{v}$ almost doubles in size, rather than continuing to decrease, while the wall shear_stress decreases by a relatively small amount, rising only slightly near the centre line. $q^{2}/2$ behaves in a more complex way, showing at $x^{*} = 0.5$ and 1.0 clear non-monotonic variations with steep gradients. Comparable variations are seen in the individual direct stresses. This complex variation may be associated with the changes in sign of $\partial W/\partial z$ which occur around the central plane (figure 9), but in this central region rates of strain other than $\partial W/\partial z$ are likely to be important.

Figure 14 is a summary of the variation of $q^{2}/2$ against lateral position at downstream positions $x^{*} = 0.5$ and 1.0, at two heights, $y/h_f = 1$ and 4. At both of these downstream stations there appears to be a vertical range over which the variation in $q^{2}/2$ with lateral position is monotonic with the turbulent kinetic energy decreasing away from the central plane, qualitatively like the shear stress in figure 12, and a range over which this variation is more complex. It is likely that this behaviour is associated with the increase in bubble size close to the central plane, and the effect this has on the dominant production term $u\bar{v}U/\partial y$. Above a height of around 3.5$h_f$ the profiles of $U$ against $y$ at $x^{*} = 0.5$ (Figure 7a) away from the centre-line have almost reached their maximum value, whereas on the centre plane $\partial U/\partial y$ does not approach zero until around 5.5$h_f$. Conversely, close to the splitter plate - say $y = 1h_f$ - the decrease in $q^{2}/2$ inside $z = 40$ may be a consequence of the decrease in $\partial U/\partial y$ with $z$ (at this height) seen in figure 7a.

4. Concluding Remarks

This mild flow, symmetrical about the geometric symmetry plane, can be split into two regions, a central region (inside $z = 0.8X_A$) and a region intermediate between it and the spanwise-invariant flow (lying outwards of $z = 1.5X_A$). In this intermediate region, the convergence is 'weak' $(\partial W/\partial z)_{max}/(\partial U/\partial y)_{max}$ was no more than about -0.004 in the present case) decreasing all the Reynolds stresses in approximately fixed ratios, the decreases
implying a large effect of the extra rates of strain. Given the approximate similarity of the mean flow and the 'simple' reduction in the stresses, it seems to be useful to think of this region as one of perturbed spanwise-invariant flow, differing primarily in that the three-dimensional effects extend across the whole bubble and are no longer confined to an inner layer near the surface as they are for the spanwise-invariant case (Hancock and McCluskey [2]). Presumably, a perturbed region will exist for stronger sweep angles. The flow in the central region in contrast is much more complex. The bubble height and attachment length increase rapidly near the centre plane and the stresses also change rapidly, some non-monotonically, with lateral position. This behaviour is probably associated with the presence of roughly streamwise vortex structures originating in the lateral inflow and elsewhere.

5. Acknowledgements
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6. References
Fig. 1 Surface streamlines. 'SL' is separation line; 'F' and 'S' foci and saddle singularities. Free-stream flow from top to bottom.

Fig. 2 Illustrative sketch of salient streamlines

Fig. 3 Fence and splitter plate arrangement (not to scale)

Fig. 4 Separation line and axes

Fig. 5 Attachment length $X_A$ and bubble height $H$.

Fig. 6 Pressure coefficient, $C_p$
Appendix B

Fig. 8 Variation of peak in -W with z.

Fig. 7 Mean velocities U, V, W, at x*=0.5. Symbols as in a).

Fig. 9 Variation of W with z at two downstream locations and two heights.

Fig. 10 -uv at x* = 1. Symbols as in fig. 6.
Fig. 11 $\dot{q}^{2/2}$ at $x^*=1$ (Symbols as in fig. 6)

Fig. 12 Maximum in $-\overline{uv}$, variation with $z$, and max. $-C_{fl}$

Fig. 13 Maximum in $\dot{q}^{2/2}$

Fig. 14 Variation of $\dot{q}^{2/2}$ with $z$ at two downstream locations and two heights