Chapter 4 - Experimental Facilities

4 EXPERIMENTAL FACILITIES

This chapter describes the laboratory facilities used in the present study. A description of the water tunnel is given, including details of the modifications made to the hardware and software for this project. The instrumentation utilised is presented along with the calibrations and corrections undertaken. The various surfaces investigated as part of this study were mapped using close-range photogrammetry to determine various surface roughness parameters. The facilities and techniques used are described at the end of this chapter.

4.1 DESCRIPTION OF THE UTAS WATER TUNNEL

The UTAS Water Tunnel (Figure 4.1) is located in the School of Engineering Hydraulics Laboratory at the University of Tasmania, Hobart, Australia. It was designed specifically for the study of freshwater biofilms. The water tunnel is of a closed loop, recirculating design and is based on the principles of wind tunnel boundary layer research facilities. Detailed descriptions of the original calibration of the water tunnel are given in Barton (2007) and Sargison et al. (2009) which demonstrate the suitability of the facility for the study of freshwater biofilms.

The UTAS Water Tunnel can be used to measure local skin friction and total drag force on large test plates which are suspended from the roof of the working section. The local interaction of the surface of the installed test plate with the near-wall turbulent boundary layer was investigated to determine the mean velocity and turbulence characteristics. The water tunnel has the capability to investigate a variety of different surfaces including smooth painted surfaces, artificially roughened surfaces, and biological surfaces.

The water tunnel is equipped to measure temperature, pressure, velocity, and total drag using slow response instruments, and velocity in a two dimensional plane using a fast response instrument.

4.2 UTAS WATER TUNNEL COMPONENTS

The water tunnel is driven by a Regent horizontal split-case pump (model 350-S16) and 7 kW AC 3-phase motor, designed for low head and high flow. The freestream velocity in the working section ranged from 0.3 m/s to 2.0 m/s.
De-swirl vanes are fitted immediately downstream of the pump and at the start of the return pipe to reduce swirl and secondary flows introduced by the pump. There are two diffusers (second stage fitted with vanes) to increase the flow area to the cross-section required for the two-dimensional contraction and to reduce losses in the flow conditioner. The first stage diffuser also transforms the flow from a circular to a rectangular cross-section. Cascading bends are used to turn the flow and are fitted with vanes to ensure an even flow distribution.

The flow conditioner is installed upstream of the contraction at the maximum cross-section to reduce energy losses. It consists of two sections of honeycomb and steel mesh, separated by 300 mm. The honeycomb consists of closely packed 60 mm long circular tubes of 6 mm diameter and is used to straighten the flow and reduce turbulence in the working section. A stainless steel mesh of 3.15 mm square aperture is used to both hold the honeycomb in place and to reduce turbulence. The two-dimensional contraction has a contraction ratio of 3:1 and is 2 m in length. It accelerates the flow to the working section, reduces turbulence, and creates uniform flow. The boundary layer is tripped using a 3 mm brass welding rod attached to the four walls of the working section. The trip is located at the beginning of the working section, 600 mm upstream of the leading edge of the test plate, to ensure a fully turbulent boundary layer over the test plate.

It is expected that the freestream turbulence intensity will be higher than those typically experienced in wind tunnels (usually less than 0.5%). The contraction ratio is the single most
important parameter in determining the freestream turbulence intensity. In small low-speed wind tunnels, it is normally between 6:1 and 9:1 (Mehta & Bradshaw 1979). Due to space and cost limitations, the contraction ratio of the UTAS Water Tunnel is 3:1. Schultz and Swain (1999) used a contraction ratio of 4:1 with similar flow management devices and freestream velocities and measured a freestream turbulence intensity of 2.5 – 3%. The freestream turbulence intensity was measured using the LDV system and found to be approximately 1%.

The working section is constructed of 30 mm thick Perspex sheet. Figure 4.2 details the dimensions and plug locations of the working section (Barton 2007). Test plates measuring 997 mm long x 597 mm wide form the roof of the working section and are attached to a Perspex backing plate which in turn is suspended by 4 flexures attached to the working section lid. The Perspex lid is securely clamped to the lid of the working section and sealed using a rubber seal.

This allows the working section roof cavity to be completely filled with water and hence avoids distortions of the suspension arrangement due to temperature gradients. The test plates used in the current study are detailed in Section 4.3. A load cell was attached to the Perspex backing plate to measure the total drag on the plate, and is discussed in more detail in Section 4.4.4.

![Figure 4.2 Working section details with dimensions in mm (Barton 2007)](image)
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The current study required several major modifications to be made to the existing water tunnel and instrumentation including: installation of a cooling system to control the water temperature; installation of a linear actuator to control Pitot probe movements during a boundary layer traverse; installation of a two-dimensional Laser Doppler Velocimeter; and installation of a porthole constructed of high quality optical glass. The LabVIEW software used to control the water tunnel, instrumentation and data acquisition was also upgraded.

The original water tunnel specification did not include a cooling system to control the temperature of the recirculating water in the water tunnel due to funding limitations. However, provision was made in the design to allow a cooling system to be fitted at a later stage. There were two primary reasons for the installation of the cooling system: Reynolds number control, and environment control. The temperature of the water in the water tunnel could not be maintained at a constant temperature due to heat input from the 7 kW pump and ambient air temperature which can exceed 30 °C in summer.

The inability to maintain constant temperature means that fluid properties such as density and viscosity are variable. It is imperative that a constant Reynolds number is maintained during a boundary layer traverse to ensure that the mechanisms for interaction between the surface being tested and the near-wall boundary layer are maintained. It is also vital that measurements are able to be completed at the same Reynolds number for different test plates to allow results to be compared. However, varying flow speed to maintain constant Reynolds number introduces control issues.

The second reason for installing the cooling system was to provide a controlled environment for the biofilm species grown on the test plates. Perkins (2006) grew the primary diatom species found in the Tarraleah Canal system, *G. tarraleahae*, under five different temperatures ranging from 5-20 °C. Growth was observed for all temperatures, and maximum algal growth was found to occur at 20 °C. It was recommended that the temperature of the water tunnel be maintained at 15-16 °C to allow a balance between maximum algal growth rate and replication of natural conditions.

The cooling system was installed in February 2007 and allows the water temperature to be maintained at a steady temperature ± 0.5 °C. The temperature of the tunnel is monitored by a separate thermometer installed in the flow conditioner. Water from the tunnel is taken out upstream of the pump and directed through a heat exchanger which cools the water to the desired temperature, and then returns the water to the tunnel.
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4.3 TEST PLATE DETAILS

The test plates used in the water tunnel were constructed of stainless steel and measured 997 mm long, 597 mm wide, and 3 mm thick and were designed to be used in the UTAS Water Tunnel and deployed in Tarraleah No.1 Canal. For field deployment, the test plates were mounted to aluminium backing plates measuring 1010 mm long, 600 mm wide, and 5 mm thick and installed in purpose built racks on the canal walls. The test plates were projected approximately 60 mm into the flow away from the wall. The field installation was developed by Barton (2007) with the aim to grow flow conditioned biofilms. Six large test plates were used; three were coated with Jotamastic 87 (Figure 4.3a); and three were sandgrain roughened plates (Figure 4.3b). A fine grit horticultural propagating mix was used to prepare the sandgrain roughened test plates, hereafter referred to as the rough plates. The surface preparation technique and particle size distribution are given in Section 3.3.

One of each type of plate remained in the lab as a control or reference plate, and are referred to as SP Lab (for the smooth plate) and RP Lab (for the rough plate). The remaining plates (SP1, SP2, RP1, and RP2) were deployed in Tarraleah No.1 Canal at Transition 4 and Pond No.1. The field locations are detailed in Section 3.4.

![Figure 4.3 (a) Smooth plate coated with Jotamastic 87; (b) Rough plate with fine grit (k ~ 1.5 mm)](image-url)
4.4  MEASURED VARIABLES & INSTRUMENTATION

The following sections detail the variables that were measured and the instrumentation that was
installed on the water tunnel for the present study. Where appropriate, the methodology used to
determine dependent variables such as the velocity from the pressure measurements is also
given. The general flow characteristics of the UTAS Water Tunnel have been described in detail
by Barton (2007) and Sargison et al. (2009) and will not be repeated here. However, some
repeatability and uncertainty analyses were conducted as part of the present study, and these
results are included.

4.4.1 Temperature

Temperature measurements were taken concurrently with all other measurements. This enables
the water density and viscosity to be determined and hence the relevant Reynolds numbers may
be accurately obtained and monitored for each set of measurements. A platinum resistance
temperature probe (model Pt100 MIR-HPC-TX RTD) with 4-20 mA output is installed upstream
of the working section in the flow conditioner. The output temperatures were calibrated with a
thermometer placed in the working section under both zero flow and flow conditions.

Water density, $\rho$, and dynamic viscosity, $\mu$, are determined from the measured temperature, $T$, in
degrees Celsius using the following equations, which are polynomial fits to tabulated data for
water at atmospheric pressure:

$$
\rho = 5.673 \times 10^{-5} T^3 - 8.850 \times 10^{-3} T^2 + 0.07771 T + 999.8
$$

Equation 4.1

$$
\mu = 6.870 \times 10^{-7} T^2 - 5.264 \times 10^{-5} T + 0.001773
$$

Equation 4.2
4.4.2 Pressure

Pressure measurements were obtained using three Validyne variable reluctance pressure transducers (model DP15), which measure the pressure difference between two pressure sources (see Figure 4.4). A key feature of the Validyne transducers is the exchangeable diaphragm (pressure sensing element) which allows the pressure range to be selected to match the experimental conditions. The #24 diaphragm used to measure the Pitot/static pressure differential had a full scale pressure of 2.2 kPa giving a resolution of 1.1 Pa.

The pressure transducers were calibrated regularly using a water-filled manometer mounted to the frame of the water tunnel. The differential water level was adjusted manually and the corresponding voltage level recorded in both the positive and negative range. The estimated accuracy of the manometer readings is ± 0.5 mm H2O. A typical calibration chart is given in Figure 4.5. A calibration curve (line of best fit) is fitted to the data and used to convert the output voltage from the transducers to a pressure (mm H2O).

The pressure differential across the contraction was measured concurrently with the majority of measurements using a dedicated pressure transducer and was used to remove any temporal variations during long measurement periods. During this study, a number of different pressures
were required to be measured simultaneously. To achieve this, a solenoid switching arrangement with eight channels was used to scan each connected pressure with one of the pressure transducers, using the upstream contraction pressure as the reference pressure. The solenoid switching was controlled using the LabVIEW software, which is described in more detail in Section 4.5.1.

\[ u = \frac{P_{\text{Pitot}} - P_{\text{static}}}{2g} \]

Figure 4.5 Sample pressure transducer calibration chart

### 4.4.3 Mean Velocity from Pressure Measurements

Time averaged mean velocity was measured using a Pitot probe and static pressure tapping located in the floor of the working section. The static wall pressure tapping was located in the same plane as the Pitot probe, but offset 50mm longitudinally from the Pitot probe centreline in the spanwise direction to enable the calculation of the local velocity without flow disturbance from the Pitot probe, as shown in Figure 4.6.

The velocity, \( u \), can be determined from the stagnation pressure measured by the Pitot probe, \( P_{\text{Pitot}} \) and the static pressure, \( P_{\text{static}} \) measured at the static pressure tapping by applying the Bernoulli equation. The pressures in Equation 4.3 are pressure heads with units of metres.
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\[ u = \sqrt{2g\left(P_{\text{Pitot}} - P_{\text{static}}\right)} \quad \text{Equation 4.3} \]

Figure 4.6 Pitot probe, inserted through one of the three plugs in the bottom of the working section, and associated static pressure tapping in same plane as the tip of the Pitot probe

The streamwise velocity using a Pitot probe and static wall tapping, several corrections are required to account for the effects of viscosity, velocity gradient, the presence of a wall, and turbulence.

A correction for the viscous interaction between the freestream and the stagnation fluid is required if the Reynolds number based on the Pitot probe internal diameter, \( Re_d \), is less than 100 (Chue 1975). As \( Re_d \) exceeded 100 in all instances for the current measurements boundary layer, a viscosity correction was not required.

In a shear flow, such as at a wall, the transverse velocity gradient causes the streamlines to deflect towards the region of lower velocity, and the probe to register a stagnation pressure higher than the stagnation pressure at the geometric centre of the probe (Chue 1975; McKeon et al. 2003). This error dominates all other Pitot probe errors and is corrected using an apparent shift in location of the centre of the probe. The data was corrected for this effect using the method proposed by McKeon et al. (2003).

In the region \( y > 2d_p \) (free shear), the probe location correction, \( \Delta y \), is given by Equation 4.4. This correction gives zero displacement for zero shear and asymptotes to the traditional MacMillan correction for large shear (\( \alpha \to 1 \)).

\[ \frac{\Delta y}{d_p} = 0.15 \tanh\left(4\sqrt{\alpha}\right) \quad \text{for} \quad \alpha = \frac{du}{dy} \frac{d_p}{2u} \quad \text{Equation 4.4} \]
In the region $y < 2d_p$, a wall correction, $\delta_w$, is applied instead of the free shear correction where:

$$\frac{\delta_w}{d_p} = 0.120 \quad \text{for} \quad 8 < \frac{d_p u^*}{\nu} < 110$$

Equation 4.5

A separate turbulence correction is unnecessary when the MacMillan or McKeon et al. (2003) corrections are applied. All probe measurements were also corrected for small temporal changes in test section velocity using the pressure differential across the contraction. When the boundary layer profiles obtained using Pitot probe pressure measurements were analysed using Bradshaw’s Method (see Section 6.2.2) the correction to the origin was made by fitting Spalding’s equation to the data.

A portable linear actuator was installed under the working section to facilitate the measurement of boundary layer velocity and turbulence profiles with Pitot probes, as recommended by Barton (2007). The linear actuator is driven by an Automated Motion Systems HGD Series Stepping Motor Drive. The actuator can be set to a resolution of either 0.005 mm or 0.01 mm over a total range of 220 mm. It is powered by a separate power supply and the actuator location is controlled directly by LabVIEW programs, which are discussed in detail in Section 4.5. The actuator can be located at each of the three plugs installed in the bottom of the working section. Previously the probe had to be moved manually to each new boundary layer position, taking a considerable amount of time and requiring the operator to be present for the entire traverse. The installation of the linear actuator and development of the measurement software automates the process.

The Pitot/static pressure differential was measured at 51 locations throughout the boundary layer using logarithmic interval spacing, with the probe moved by the automatic linear actuator with a minimum step of 0.01 mm. The first measurement was taken with the Pitot probe resting against the test plate and the final measurement was taken in the freestream at 80 mm from the wall. Each measurement was recorded at a sampling rate of 1 kHz.

### 4.4.4 Total Drag

A floating element force balance arrangement was used to determine the total drag acting on each test plate. The use of floating element force balances to determine the skin friction coefficient is discussed in general terms in Section 6.1.1, including the issues associated with its
use. This section describes the floating element force balance installed on the UTAS Water Tunnel, and details the corrections applied to the measured data.

The test plates form the roof of the working section and are attached to a Perspex backing plate which in turn is suspended by four stainless steel flexures attached to the working section lid (see Figure 4.7). A MTI Weight Systems single ended shear beam load cell (model MTI-4856-SB) was attached to the lid of the working section and linked by a load transfer rod to the Perspex backing of the test plate. The flexures ensure a one-dimensional transfer of force through the load transfer rod to the load cell. The load cell is connected to a Mann Industries strain gauge transmitter, which in turn is connected to the data acquisition system.

![Figure 4.7 Working section and floating element force balance (Barton et al. 2007)](image)

The output of the load cell is a voltage, which is calibrated by applying a known force to the load cell. The load cell was calibrated in situ using a thin steel cable and a system of low friction pulleys. Weights were added in 50 gram increments, with a zero load reading taken between each loaded reading, and the voltage output recorded. An example load cell calibration curve is given in Figure 4.8. A calibration was completed each time a test plate was changed over.

The concept of the virtual origin is detailed in Section 6.1.1, and Equation 6.20 - Equation 6.23 were used to determine the drag on the test plate for various flow speeds. A correction also had to be applied to compensate for pressure forces acting on the ends of the plate arising from the
longitudinal flow acceleration due to wall boundary layer growth through the working section. The pressure difference between the ends of the plate was measured using static wall tappings, which was multiplied with the cross-sectional area of the end of the plate (597 mm \times 3 \text{ mm}) to obtain the force correction. The force correction was typically 0.3 N (Barton 2007).

The test plates are bolted onto the Perspex backing plate using counter sunk bolts with hexagonal indentations (for tightening). Barton (2007) examined the effect of the fastening bolts on the measured drag by comparing two Perspex test plates – one of which had been glued to a backing plate, the other was attached using counter sunk bolts. It was concluded that the bolts do not make a significant contribution to the measured drag.

Barton et al. (2007) and Barton (2007) examined the effects described by Winter (1977), which are discussed in Section 6.1.1, including the effect on the boundary layer of the necessary gap around the edges of the test plate. The effect of the gap around the test plate was to cause leakage of flow between the mainstream and the cavity above the test plate, which caused a discontinuity in the development of the boundary layer at the leading edge of the test plate. This is evidenced by a change in the boundary layer shape factor, $H$, which is shown in Figure 4.9. Evidence of secondary flows was also observed within the working section, which is shown in a pressure profile plot at the surface of the test plate in Figure 4.10. The edge effects are due to
both longitudinal corner vortices and the movement of water between the edges of the test plate and the roof cavity in the working section; however, they are confined to regions within 50 mm of the sidewall (Barton et al. 2007).

The floating element force balance was designed with a nominal 1.5 mm gap around the edges of the test plate to ensure that the force balance was floating. The actual gap varied from 1 – 2 mm on the various edges of the test plate. The alignment of the test plate in the vertical direction was checked to ensure that the test plate sits at the same height as the surrounding water tunnel roof. The uniformity of the gaps around the test plate was vital in minimising the errors associated with misalignment. Repeatability tests on the smooth plate and the artificially roughened plate were conducted to examine the uncertainty in the drag measurements, with the results given in Section 6.3.

*Figure 4.9 Boundary layer shape factor at three different Re, showing the discontinuity in the boundary layer at the leading edge of the test plate (Barton 2007)*
4.4.5 Laser Doppler Velocimeter (LDV)

All LDV measurements were taken using Dantec Dynamics FlowExplorer optics, a BSA signal processor, and BSA Flow Software (version 4.10). The operating principles of LDV systems are given in Section 5.2 and the characteristics of the system are given in Table 4.1.

The measurement distance was nominally 280 mm from the front of the lens. The flow was seeded with 10 µm hollow glass spheres, and also had many natural seeding particles. The

\[
\frac{\Delta P_p}{\Delta P_{Re f}} = \frac{P_{total} - P_{static}}{P_1 - P_2}
\]

![Figure 4.10 Pressure variations at the test plate surface for different Re, at the mid section of the test plate (Barton et al. 2007)](image)

Table 4.1 Characteristics of Dantec FlowExplorer LDV System

<table>
<thead>
<tr>
<th></th>
<th>Streamwise Velocity Component U</th>
<th>Wall Normal Velocity Component V</th>
</tr>
</thead>
<tbody>
<tr>
<td>Wavelength</td>
<td>660 nm</td>
<td>785 nm</td>
</tr>
<tr>
<td>Frequency Shift</td>
<td>80 MHz</td>
<td>80 MHz</td>
</tr>
<tr>
<td>Fringe Spacing</td>
<td>3.257</td>
<td>3.935</td>
</tr>
<tr>
<td>Number of Fringes</td>
<td>30</td>
<td>30</td>
</tr>
<tr>
<td>Measurement Volume</td>
<td>0.1013 mm x 0.1008 mm x 1.1013 mm</td>
<td>0.1205 mm x 0.1199 mm x 1.205 mm</td>
</tr>
<tr>
<td>Beam Half-Angle</td>
<td>5.71°</td>
<td>5.71°</td>
</tr>
<tr>
<td>Beam Diameter</td>
<td>2.5 mm</td>
<td>2.5 mm</td>
</tr>
</tbody>
</table>
position of the laser measuring volume was controlled by a two-axis traverse (in the wall normal and streamwise directions). The potential sources of error for LDV measurements are discussed in Section 5.2.1 and the commissioning of the system is detailed in Section 5.2.2.

4.5 DATA ACQUISITION AND CONTROL

Two separate data acquisition and control systems were used in the current study. The LDV system came with its own software program, BSA Flow Software, which was used to control the position of the traverse system and data acquisition using the laser. The remaining aspects of the water tunnel, including the pump and all of the other instrumentation, were controlled using LabVIEW.

4.5.1 LabVIEW

LabVIEW is a graphical programming language developed by National Instruments and was used in conjunction with a National Instruments 12 bit PCI 6025E acquisition card and a National Instruments SBC100 pin shielded connector block. LabVIEW programs were written specifically for the current study, but were based on existing programs written by Barton (2007). The water tunnel was set up to take two different categories of measurements: boundary layer pressure profiles and total drag measurements. The pump control and data acquisition processes for each measurement type are described in the following sections.

4.5.1.1. Boundary Layer Pressure Profiles

A three stage LabVIEW program was written to obtain boundary layer pressure profiles. Stage 1 involved physically setting up the Pitot probe in the linear actuator to ensure that the tip of the probe was resting against the test plate for Preston tube measurement of the wall shear stress. The software allowed minor adjustments to be made to probe position, before defining a starting position for the traverse. Stage 2 occurred simultaneously with Stage 1, and controlled the pump speed to achieve a constant Reynolds number. Stage 3 was the data acquisition stage of the program, and was completed using a series of logical ‘for’ loops:

1. The Pitot probe was moved to the required y position from a preloaded matrix of measurement points;
2. Once in position, the first port on the solenoid was opened. A delay of 20 seconds was built into the program to allow the pressures to settle after the solenoid was operated before taking measurements;

3. The data (pressures, temperature, and pump speed) were recorded for a set amount of time (usually between 20 and 60 seconds) at a sampling rate of 1 kHz;

4. The mean of each data type and the raw data were then saved to separate files;

5. The active port on the solenoid was then closed, and the next port opened and steps 2 – 5 repeated for the required number of ports; and

6. Once all of the data at each y-location was measured, the traverse would move the Pitot probe to the next location in the preloaded matrix of measurement points. Steps 2-5 were repeated for each measurement point.

Data was typically collected at 50 – 60 locations in the boundary layer, with the final measurement taken in the freestream at 80 mm from the wall.

4.5.1.2. Drag Measurements

Drag measurements were conducted at 50 rpm intervals in pump speed from 150 – 650 rpm at 1 kHz. The load cell was sensitive to temperature variation, thus zero flow measurements were taken between each flow measurement. The solenoid arrangement was used to measure several pressures simultaneously with the drag measurements, including the contraction pressure differential and the pressure upstream and downstream of the test plate. Drag measurements were obtained using the following procedure:

1. Zero flow measurement;

2. Start pump and set to pre-determined speed from speed matrix;

3. Wait pre-determined time from delay matrix;

4. Data acquisition for flow measurement;

5. Pump off. Wait pre-determined time from delay matrix; and

6. Repeat steps 1-5.
4.5.2 LDV Velocity Profiling Procedure

Velocity profiles were obtained using the following procedure:

1. Situate the laser measurement volume to be in the freestream;
2. Adjust the freestream velocity to the desired level (e.g. 2.0 m/s);
3. Find the wall ($y = 0$) by moving the measurement volume towards the test plate until a zero velocity spike is noted in the streamwise component velocity histogram. This indicates that the centre of the measurement volume is within about one half of the measurement volume diameter of the wall;
4. Move the probe until the wall signal dominates. This point serves as the origin for the traverse; and
5. Begin data collection using the pre-loaded matrix of measurement points.

Occasionally the traverse had to be started a short distance away from the wall to avoid overloading the photomultipliers. This was particularly an issue with the white coloured smooth plates, probably due to reflection off the surface.

4.6 PHOTOGRAMMETRIC SURFACE CHARACTERISATION

As discussed in Chapter 2, the skin friction caused by a surface is related to the roughness character of that surface. Close-range photogrammetric techniques were used to obtain three dimensional measurements of the surface topography of the large test plates used in the water tunnel. The process allowed highly repeatable photography of the surface over time. Cross-sectional profiles of the surfaces could be extracted and roughness information obtained which could then be related to the measured drag and boundary layer profiles. The photogrammetry techniques described here were developed by Bendall (2005), Osborn et al. (2005), Barton (2007) and Barton et al. (2010). The primary reason for using photogrammetry to obtain the roughness information is that data can be obtained very quickly without interfering with the biofilms.

4.6.1 Camera and Analysis Software

Previous studies (Barton 2007; Barton et al. 2010; Osborn et al. 2005) have utilised film photography to obtain the surface topography models, however, the process to obtain the models
is laborious and requires a skilled operator to manually measure the three-dimensional coordinates using an analytical stereo-plotter. Digital photogrammetric techniques are available and were used in the present study.

The camera used was a Nikon D200 with a Nikkor 14 mm super wide angle lens. The camera was calibrated using a planar array of 100 points exposed onto film using a precision vector-plotter (Protel P/L, Hobart, Tasmania, Australia). The coordinates of the points on the array were measured using a stereo comparator (Stecometer; Carl Zeiss JENA, Jena, Germany). The array was photographed from eleven positions; the camera was then calibrated using 3DM CalibCam (ADAM Technology, Australia), which generated values for focal length, principal point of symmetry, radial and decentring lens distortions. Residuals for the control points from the calibration adjustment showed root mean square errors of 2 μm for \( x \) and \( y \), and 37 μm for \( z \).

The software used to produce the three-dimensional models of each surface was 3DM Analyst (version 2.2a+, ADAM Technology, Australia). The control points in each photograph are digitised by the user; the software then takes the digital stereopairs and automatically matches relevant points on the surface, as shown in Figure 4.11. The software automatically matches the pixels in the images to generate the \( X, Y, Z \) coordinates of the surface. Each model consisted of at least 15,000 points.

![Figure 4.11 Left and right photographs showing the control points (green) and automatically matched points (yellow)](image-url)
4.6.2 Set-up for Photographs

The rig used to support the camera was described in detail by Barton (2007) and Bendall (2005) and is only summarised briefly here. The general arrangement of the photogrammetry rig is given in Figure 4.12. The test plates were screwed to a clean 1100 mm x 700 mm x 12 mm aluminium base plate using the same arrangement used to attach the test plate in the field and in the water tunnel (see Figure 4.13). The base plate contained three bevelled brass plugs with ball bearings seated in each of these plugs to allow the accurate and repeatable location of the photography reference plate. Machined threaded rods attached to the photography reference plate are then located over each of the ball bearings to suspend the photography reference plate over the test plate without touching the test plate. The photography reference plate consisted of a 10 mm thick aluminium plate with windows for viewing the test plate and ribbing to strengthen and reduce any bending in the reference plate due to the weight of the camera and its support frame (see Figure 4.14). Bevelled brass plugs were also installed in the reference plate to locate the camera support frame in each of the windows in the reference plate.

![Figure 4.12 General arrangement of the photogrammetry rig, adapted from (Barton 2007)](image-url)
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The camera support frame incorporated a 100 mm x 130 mm viewing window (see Figure 4.15). Surrounding the window is an array of eight control points for reference to construct the three-dimensional model of the surface from the stereophotographs. A sliding cross beam was used to maintain the required distance between the test plate and the camera, and to obtain repeatable left and right photographs by aligning the cross beam with the marks on the camera support frame. The camera support frame could be located in six positions on the photography reference plate, as shown in Figure 4.17.

The lighting of the test plate surface during photography was critical in achieving good models to minimise reflections from the wet biofilm surface. Reflections resulted in poor matching between the left and right photograph. Figure 4.18 shows a photograph of a fouled surface with gross reflection issues.

Figure 4.13 Base plate with test plate  
Figure 4.14 Photography reference plate  
Figure 4.15 Camera support frame sitting on photography reference plate  
Figure 4.16 Lights in position for photography
Bendall (2005) studied the reflections encountered when photographing wet surfaces, and the techniques that could be used to mitigate such reflections. A photography room was established with blacked-out windows to remove uncontrolled light sources. Having two light sources pointing from opposite sides of the plate allows for a more even distribution of light across the study area and reduces the frequency and severity of shadowing effects (see Figure 4.16). The angle of the incident light is also important and Bendall (2005) found that illumination angles between 45 and 55 degrees provided the best distribution of light across the test surface. To minimise reflections the angle of the incident light should be set to the Brewster angle for an air/water interface, which is approximately 53°. All photographs of wet surfaces were taken with a light incidence angle of 50° in the present study. Bendall (2005) also used a polarising filter to further minimise reflections. Such a filter would not easily fit the lens system used in the current study, thus reflections were minimised purely by controlling the incident light.
4.6.3 Data Analysis

The output from 3DM Analyst was the $X,Y,Z$ coordinates of points (in mm) of the photographed surface. Five cross sections 20 mm apart in the direction of flow were extracted from the data to examine the roughness characteristics. The resolution of the cross sections was 0.5 mm in the $X$ (flow) direction.

A sample cross-section of a rough surface is given in Figure 4.19, showing the roughness parameters defined in Table 4.2. The cross section profile is presented plotted about its mean, along with a positive transform of the results to allow the determination of the number of peaks per sample length. The roughness and statistical parameters were developed by Barton (2007) according to the methods of Whitehouse (2002).

The final parameter used to describe the surface roughness was a peak count. The peak count gives an indication of the number of roughness elements within the sample profile length. Barton (2007) adapted the method described by Whitehouse (2002), which involved positively transforming the data and observing where peaks fall above the $R_q$ (rms) value. To qualify as a peak, the $Z$(+ve) profile must begin below the rms line. Thus the next peak cannot be counted until the profile has again fallen below the rms line.

Table 4.2 Definition of roughness and statistical parameters for photogrammetry

<table>
<thead>
<tr>
<th>Roughness Parameters</th>
<th>Statistical Moments</th>
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<tbody>
<tr>
<td>$R_a$ Mean of absolute deviation of profile from mean line</td>
<td>$1^{st}$ (mean) Arithmetic mean</td>
</tr>
<tr>
<td>$R_q$ Root mean square parameter</td>
<td>$2^{nd}$ (variance) Spread of the distribution</td>
</tr>
<tr>
<td>$R_h$ Max depth of profile below mean line of sample length</td>
<td>$3^{rd}$ (skewness) Symmetry of the profile about the mean</td>
</tr>
<tr>
<td>$R_p$ Max height of profile above mean line of sample length</td>
<td>$4^{th}$ (kurtosis) Sharpness or flatness of a profile</td>
</tr>
<tr>
<td>$R_t$ Max peak to valley height of profile of sample length</td>
<td></td>
</tr>
</tbody>
</table>

The maximum peak to valley height of the sample length, $R_t$, was used as the primary roughness parameter for each test surface (both smooth and rough substrates) and was also used to compare the physical roughness with the drag coefficient and roughness functions in Chapter 9. The $R_t$
parameter was chosen as Barton (2007) found that it correlated the best with the equivalent sandgrain roughness determined from the boundary layer and drag measurements in a previous study. The parameter has slightly different meanings, depending on the substrate type. For a smooth substrate, \( R_t \) represents the maximum biofilm thickness for the sample length. However, for a rough substrate, the value of \( R_t \) will depend on where the biofilm grows. If the biofilm grows mostly on the peaks of the clean profile, \( R_t \) will increase. However, if the biofilm grows mostly in the valley, \( R_t \) will decrease. Thus for the rough substrates, comparing the \( R_t \) values for clean and biofouled substrates in the same location on each plate gives an indication of whether the biofilm has increased the surface roughness or smoothed the surface out. This is discussed further in Chapter 9.

When analysing the topography of a surface, there are two scales that need to be considered: roughness and waviness (Whitehouse 2002). The definition of roughness versus waviness is given in Figure 4.20. Whether the data consists of roughness only, or roughness and waviness depends on the sampling length chosen.

A cross section with both roughness and waviness is shown in Figure 4.21(a) from Andrewartha et al. (2006). A polynomial regression line was fitted to the data and then subtracted from the data to remove the waviness, allowing analysis of the true roughness character (Andrewartha et
al. 2006). Figure 4.21(b) shows the same cross section corrected for waviness. This method is outlined in Whitehouse (2002) as a type of filter. Following Barton (2007), the order of the polynomial was determined by observation, with a minimum number of inflections intended. The wavelength of the surface waviness was typically over tenfold the wavelength of the surface roughness and filtering was required only on a minority of profiles.

Figure 4.21 (a) Cross section with long wavelength undulation, (b) Cross section corrected for long wavelength undulation (Andrewartha et al. 2006)
4.6.4 Accuracy and Precision of Photogrammetry

The precision of the photogrammetry measurements was determined by examining replicate models of the same surface, derived from independent pairs of photographs. Ten pairs of photographs were taken at window E (see Figure 4.17) for a clean rough plate, and at window B for a fouled rough plate. Unfortunately, two pairs of photographs for the fouled plate did not produce satisfactory models, so eight models have been used to derive the statistics reported here for the fouled plate. Five cross sections 20 mm apart in the direction of flow were extracted from each model. The coordinates of the corresponding points in each cross section were compared, and the mean and standard deviation determined. Each cross section consists of 181 points, thus a total of 905 points were observed ten times each. A sample of the output is given in Table 4.3 for the clean rough plate.

Table 4.3 Statistics for 10 repeated observations of a set of points on a clean rough plate

<table>
<thead>
<tr>
<th>Point No.</th>
<th>Mean Height [mm]</th>
<th>Standard Deviation [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>90.534</td>
<td>0.101</td>
</tr>
<tr>
<td>2</td>
<td>90.474</td>
<td>0.111</td>
</tr>
<tr>
<td>3</td>
<td>90.559</td>
<td>0.108</td>
</tr>
<tr>
<td>4</td>
<td>90.637</td>
<td>0.069</td>
</tr>
<tr>
<td>5</td>
<td>90.519</td>
<td>0.067</td>
</tr>
</tbody>
</table>

The average standard deviations for the height of the 905 observed points were 88.2 µm for the clean rough plate and 241.4 µm for the fouled plate. The statistics for each cross section are given in Table 4.4 and Table 4.5. The average and standard deviation of each roughness parameter was also determined for both plates, and are given in Table 4.6 and Table 4.7. A sample cross section is shown in Figure 4.22 and Figure 4.23 for the rough plate and the fouled plate respectively.

The 95% confidence interval for the $R_t$ parameter was determined from the repeatability tests by multiplying the standard error with the two-tailed $t$ value for the relevant degree of freedom (Coleman & Steele 1995). The 95% confidence interval for a clean rough substrate and a biofouled surface were ± 3.0% and ± 8.5%, respectively.
Table 4.4 Statistics for 10 repeated measurements of each cross section on a clean rough plate

<table>
<thead>
<tr>
<th>Cross Section</th>
<th>Mean Height [mm]</th>
<th>Mean Standard Deviation [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Y = 20 mm</td>
<td>90.355</td>
<td>0.100</td>
</tr>
<tr>
<td>Y = 40 mm</td>
<td>90.433</td>
<td>0.101</td>
</tr>
<tr>
<td>Y = 60 mm</td>
<td>90.542</td>
<td>0.083</td>
</tr>
<tr>
<td>Y = 80 mm</td>
<td>90.492</td>
<td>0.078</td>
</tr>
<tr>
<td>Y = 100 mm</td>
<td>90.642</td>
<td>0.079</td>
</tr>
</tbody>
</table>

Table 4.5 Statistics for 8 repeated measurement of each cross section of a fouled plate

<table>
<thead>
<tr>
<th>Cross Section</th>
<th>Mean Height [mm]</th>
<th>Mean Standard Deviation [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Y = 20 mm</td>
<td>95.250</td>
<td>0.223</td>
</tr>
<tr>
<td>Y = 40 mm</td>
<td>95.252</td>
<td>0.235</td>
</tr>
<tr>
<td>Y = 60 mm</td>
<td>95.221</td>
<td>0.252</td>
</tr>
<tr>
<td>Y = 80 mm</td>
<td>94.992</td>
<td>0.245</td>
</tr>
<tr>
<td>Y = 100 mm</td>
<td>94.704</td>
<td>0.252</td>
</tr>
</tbody>
</table>

Table 4.6 Statistics for the roughness parameters [mm] for 10 repeated surface models of a clean rough plate

<table>
<thead>
<tr>
<th></th>
<th>$R_a$</th>
<th>$R_q$</th>
<th>$R_p$</th>
<th>$R_s$</th>
<th>$R_t$</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Mean</td>
<td>Std Dev</td>
<td>Mean</td>
<td>Std Dev</td>
<td>Mean</td>
</tr>
<tr>
<td>Total</td>
<td>.40</td>
<td>.13</td>
<td>.49</td>
<td>.04</td>
<td>1.54</td>
</tr>
<tr>
<td>Y = 20 mm</td>
<td>.38</td>
<td>.12</td>
<td>.46</td>
<td>.01</td>
<td>1.32</td>
</tr>
<tr>
<td>Y = 40 mm</td>
<td>.30</td>
<td>.10</td>
<td>.39</td>
<td>.01</td>
<td>1.05</td>
</tr>
<tr>
<td>Y = 60 mm</td>
<td>.32</td>
<td>.10</td>
<td>.39</td>
<td>.02</td>
<td>1.06</td>
</tr>
<tr>
<td>Y = 80 mm</td>
<td>.24</td>
<td>.08</td>
<td>.30</td>
<td>.01</td>
<td>.74</td>
</tr>
<tr>
<td>Y = 100 mm</td>
<td>.22</td>
<td>.07</td>
<td>.28</td>
<td>.01</td>
<td>.76</td>
</tr>
<tr>
<td>Average</td>
<td>.29</td>
<td>.09</td>
<td>.37</td>
<td>.01</td>
<td>.99</td>
</tr>
</tbody>
</table>

Table 4.7 Statistics for the roughness parameters [mm] for 8 repeated surface models of a fouled plate

<table>
<thead>
<tr>
<th></th>
<th>$R_a$</th>
<th>$R_q$</th>
<th>$R_p$</th>
<th>$R_s$</th>
<th>$R_t$</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Mean</td>
<td>Std Dev</td>
<td>Mean</td>
<td>Std Dev</td>
<td>Mean</td>
</tr>
<tr>
<td>Total</td>
<td>.63</td>
<td>.25</td>
<td>.74</td>
<td>.11</td>
<td>1.99</td>
</tr>
<tr>
<td>Y = 20 mm</td>
<td>.32</td>
<td>.12</td>
<td>.42</td>
<td>.06</td>
<td>1.17</td>
</tr>
<tr>
<td>Y = 40 mm</td>
<td>.28</td>
<td>.10</td>
<td>.35</td>
<td>.05</td>
<td>1.01</td>
</tr>
<tr>
<td>Y = 60 mm</td>
<td>.41</td>
<td>.15</td>
<td>.49</td>
<td>.05</td>
<td>.97</td>
</tr>
<tr>
<td>Y = 80 mm</td>
<td>.39</td>
<td>.14</td>
<td>.50</td>
<td>.05</td>
<td>1.03</td>
</tr>
<tr>
<td>Y = 100 mm</td>
<td>.29</td>
<td>.11</td>
<td>.37</td>
<td>.07</td>
<td>.95</td>
</tr>
<tr>
<td>Average</td>
<td>.33</td>
<td>.12</td>
<td>.42</td>
<td>.03</td>
<td>1.03</td>
</tr>
</tbody>
</table>
The three-dimensional models for the fouled plate repeatability study showed reasonable correlation for the top third of the window, and generally poor matching for the bottom two-thirds of the window. The cross-section at $Y = 20$ mm (Figure 4.23) was the best cross-section of the five extracted, and accordingly had the lowest standard deviation (see Table 4.5). The section of plate that was photographed for repeatability purposes was heavily fouled and didn’t exhibit much colour differentiation which increased the difficulty in obtaining reasonable roughness information from the photogrammetry data. Plates with light fouling where the substrate showed through and gave some colour variation gave much better models of the surface than plates that were heavily fouled.

Figure 4.22 Ten sets of observations of the same cross section at $Y = 180$ mm on a clean rough plate
Figure 4.23 Eight sets of observations of the same cross section at $Y = 180$ mm on a fouled rough plate
5 MEASURING TURBULENCE

The measurement of unsteady velocity fluctuations and Reynolds stresses is vital in advancing the understanding of the interaction of freshwater biofilms with flowing water, specifically the mechanisms for increased drag (Barton 2007; Schultz & Swain 1999). The UTAS Water Tunnel has not previously had the instrumentation required to measure turbulence; therefore a literature review of the available techniques was undertaken (see Section 2.3). An attempt was made to measure turbulence using a Pitot probe closely coupled to Validyne pressure transducers and is detailed in Section 5.1. A description of the system eventually adopted to measure turbulence, Laser Doppler Velocimetry, is given in Section 5.2.

5.1 INVESTIGATION OF TURBULENCE MEASUREMENT USING PITOT PROBES

An extensive investigation was undertaken to determine whether or not Pitot probes closely coupled to Validyne differential pressure transducers (described in Section 4.4.2) could be successfully used to measure turbulence. A mathematical model was used to estimate the natural frequency of the system and experimental studies were conducted to determine frequency spectra, turbulence intensity and mean velocity profiles for different system configurations.

There are two main considerations to maximise the frequency response of a pressure measuring system: (1) the inertia of the fluid; and (2) the viscous losses due to the tube entry and friction in the tube. Ideally, the pressure sensing element should be located as closely as possible to the tip of the Pitot probe to minimise these effects. The miniature pressure sensors currently available with a small enough diameter to fit inside a Pitot probe have a fixed high pressure range (130 kPa), which is unsuitable for this application.

5.1.1 System Setup

A Validyne variable reluctance pressure transducer was coupled externally to the end of the Pitot probe. This was used as an alternative to a miniature pressure transducer located near the tip of the probe. A key feature of the Validyne transducers is the exchangeable diaphragm (pressure sensing element) which allows the pressure range to be selected to match the experimental conditions.
The Pitot probe and static pressure lines were connected as close as physically possible to the pressure transducer. The pressure fittings for the transducers were modified to minimise the volume of water between the tip of the Pitot probe and the transducer diaphragm. Four different Pitot probes were used in this study to determine the effects of different tube diameters and lengths. Table 5.1 details the measurement system designation and includes a description of each probe. Note that the 2.0 mm probe was only used in a final attempt to obtain an acceptable frequency response, and was not used for mean velocity profiles.

The experimental measurements were conducted on a smooth plate, SP Lab, which is used as the reference plate for all water tunnel investigations. Boundary layer traverses were completed 865 mm downstream from the leading edge of the smooth test plate, at plug 3 (refer to Figure 4.2), following the procedure given in Section 4.5.1.1. Measurements were obtained for several different freestream velocities, ranging from 1.2 – 2.0 m/s, resulting in a Reynolds number range of $1.0 \times 10^6 < Re_l < 1.8 \times 10^6$.

### 5.1.2 Mathematical Model

The mathematical model used to describe the physical system was developed by Arndt and Ippen (1970) and is shown schematically in Figure 5.1 as a simple mass-spring oscillation system. This model does not account for the static pressure tapping side of the differential pressure transducer or for the movement of fluid in the static pressure tapping line (caused by the deflection of the diaphragm), however, it should be adequate to estimate the expected frequency response of the different Pitot probes.

<table>
<thead>
<tr>
<th>Designation</th>
<th>Tube Diameter [mm]</th>
<th>Tube Length [mm]</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.7 mm</td>
<td>0.7</td>
<td>520</td>
<td>Continuous 0.7 mm tube through support stem</td>
</tr>
<tr>
<td>1.0 mm</td>
<td>1.0</td>
<td>520</td>
<td>Continuous 1.0 mm tube through support stem</td>
</tr>
<tr>
<td>1.0 mm hollow stem</td>
<td>1.0</td>
<td>25</td>
<td>1.0 mm tip connected to 4.4 mm internal diameter stem</td>
</tr>
<tr>
<td></td>
<td>4.4</td>
<td>495</td>
<td></td>
</tr>
<tr>
<td>2.0 mm</td>
<td>2.0</td>
<td>-</td>
<td>Continuous 2.0 mm tube through support stem</td>
</tr>
</tbody>
</table>

Table 5.1 Measurement system designation
The natural frequency of a clamped circular diaphragm, $f_d$, is given by Equation 5.1, where $E_d$ is the Young’s modulus for the diaphragm; $h_d$ is the thickness of the diaphragm; $n_d$ is Poisson’s ratio; $r_d$ is the radius of the diaphragm; and $\rho_d$ is the density of the diaphragm. The natural frequency of the system, $f_{\text{system}}$, shown in Figure 5.1 is given by Equation 5.2, where $L$ and $d$ are the length and diameter of the pressure line, respectively. This expression does not include any damping and therefore accounts only for the effects of inertia in the total pressure line.

$$f_d = \frac{2.95}{\pi} \sqrt{\frac{E_d h_d^2}{(1-n_d^2) \rho_d h_d r_d^4}}$$ \hspace{1cm} \text{Equation 5.1}

$$f_{\text{system}} = 1.11 f_d^{3/2} \sqrt{\frac{\rho_d \pi d^2}{\rho \cdot 4L}} \left( \frac{\rho_d (1-n_d^2)}{E_d} \right)^{1/4}$$ \hspace{1cm} \text{Equation 5.2}

Different diameter Pitot probes were tested to investigate the effects of the inertia of the fluid. Logically, the less fluid to be moved, the better the frequency response should be. Thus the length of tube was minimised for all probes, with the transducer connected as closely as physically possible to the Pitot probe. Also, having a smaller diameter tip gives better spatial resolution in the boundary layer. However, the expression for $f_{\text{system}}$ indicates that a larger diameter tube will have a higher natural frequency, due to the higher pressure force acting on the diaphragm. The model does not account for viscous forces, however, it is expected that a larger diameter tube would minimise the friction losses.

There will be less than 10% distortion in a turbulence signal for frequencies lying below 75% of the natural frequency of the system (Arndt & Ippen 1970). Thus the estimated maximum frequency response of each system will be taken as 75% of the natural frequency.

The thickness of the #24 stainless steel diaphragm was measured using a micrometer at 0.065 mm, giving a natural frequency of 4.5 kHz. The predicted natural frequency and maximum frequency response of each system is given in Table 5.2. The predicted response for a less
sensitive diaphragm (#28) with a (full scale pressure of 5.5 kPa) is also given in Table 5.2 for comparison.

Measuring unsteady velocity fluctuations with a Pitot probe and Validyne pressure transducer is a compromise between diaphragm sensitivity and frequency response. The less sensitive diaphragm should have a better frequency response than the diaphragm used for all mean velocity and turbulence measurements in this study. However, it is less sensitive to pressure changes with a resolution of 2.7 Pa compared to 1.1 Pa.

### 5.1.3 Boundary Layer Parameters

A summary of the main boundary layer parameters obtained with each probe is given in Table 5.3 - Table 5.5. Figure 5.2 compares the skin friction coefficients obtained at each test plate Reynolds number for the three different Pitot probes with an equilibrium turbulent boundary layer with the same $Re_b$.

The local skin friction coefficients obtained using the Preston Tube Method are compared with results obtained using Bradshaw’s Method in Figure 5.2. Bradshaw’s Method is a wall similarity technique which was used for all smooth plate boundary layer profiles. It is described in detail in Section 6.1.4 and Section 6.2.2. The results agree within the experimental uncertainty, with the exception of the 0.7 mm diameter Pitot probe, which gives consistently higher $c_f$ values when the data is analysed using the Preston Tube Method. The data shows reasonable agreement with the equilibrium relation given in Equation 5.3 (Schlichting 1955):

$$c_f = 0.0256 Re_b^{-1/4}$$

*Equation 5.3*
### Table 5.3 Smooth wall boundary layer parameters for 0.7 mm diameter Pitot probe

| \(Re_l\) | \(Re_0\) | \(U\) [m/s] | \(\delta\) [mm] | \(\delta'\) [mm] | \(\theta\) [mm] | \(H\) | \(c_f\) | \(\nu^*\) [m/s] | \(d^*\) [mm] | \(\epsilon\) [m/s] | \(\nu^*\) [m/s] | \(c_f\) |
|---------|---------|-------------|----------------|----------------|----------------|-----|-------|---------------|----------|---------|---------------|-------|-------|
| 1.09E+06 | 5170   | 1.23        | 49.9           | 5.97           | 4.74           | 1.26 | 0.003473 | 0.051         | 32        | 0.71    | 0.048         | 0.003069 |
| 1.24E+06 | 5680   | 1.42        | 48.4           | 5.73           | 4.56           | 1.26 | 0.003306 | 0.058         | 35        | 0.64    | 0.055         | 0.003047 |
| 1.44E+06 | 6380   | 1.64        | 47.6           | 5.35           | 4.31           | 1.25 | 0.003310 | 0.067         | 41        | 0.71    | 0.063         | 0.002980 |
| 1.62E+06 | 6950   | 1.84        | 46.8           | 5.31           | 4.28           | 1.24 | 0.003335 | 0.075         | 46        | 0.80    | 0.070         | 0.002909 |
| 1.70E+06 | 7360   | 1.94        | 46.7           | 5.35           | 4.31           | 1.24 | 0.003199 | 0.078         | 48        | 0.75    | 0.074         | 0.002888 |
| 1.84E+06 | 8200   | 2.03        | 48.0           | 5.53           | 4.44           | 1.24 | 0.003046 | 0.079         | 50        | 0.69    | 0.076         | 0.002826 |

### Table 5.4 Smooth wall boundary layer parameters for 1.0 mm diameter Pitot probe

| \(Re_l\) | \(Re_0\) | \(U\) [m/s] | \(\delta\) [mm] | \(\delta'\) [mm] | \(\theta\) [mm] | \(H\) | \(c_f\) | \(\nu^*\) [m/s] | \(d^*\) [mm] | \(\epsilon\) [m/s] | \(\nu^*\) [m/s] | \(c_f\) |
|---------|---------|-------------|----------------|----------------|----------------|-----|-------|---------------|----------|---------|---------------|-------|-------|
| 1.04E+06 | 4860   | 1.19        | 49.1           | 5.82           | 4.66           | 1.25 | 0.003271 | 0.048         | 42        | 0.83    | 0.047         | 0.003148 |
| 1.21E+06 | 5480   | 1.39        | 48.8           | 5.59           | 4.50           | 1.24 | 0.003128 | 0.055         | 48        | 0.88    | 0.055         | 0.003089 |
| 1.40E+06 | 6450   | 1.60        | 49.9           | 5.70           | 4.59           | 1.24 | 0.003008 | 0.062         | 54        | 0.81    | 0.062         | 0.003026 |
| 1.60E+06 | 7140   | 1.83        | 48.0           | 5.49           | 4.44           | 1.24 | 0.002961 | 0.070         | 62        | 0.75    | 0.071         | 0.003002 |
| 1.69E+06 | 7510   | 1.92        | 48.0           | 5.46           | 4.43           | 1.23 | 0.003021 | 0.075         | 66        | 0.76    | 0.075         | 0.003012 |
| 1.76E+06 | 7360   | 2.00        | 47.4           | 5.13           | 4.18           | 1.23 | 0.002989 | 0.077         | 68        | 0.72    | 0.078         | 0.003036 |

### Table 5.5 Smooth wall boundary layer parameters for 1.0 mm diameter head, hollow stem Pitot probe

| \(Re_l\) | \(Re_0\) | \(U\) [m/s] | \(\delta\) [mm] | \(\delta'\) [mm] | \(\theta\) [mm] | \(H\) | \(c_f\) | \(\nu^*\) [m/s] | \(d^*\) [mm] | \(\epsilon\) [m/s] | \(\nu^*\) [m/s] | \(c_f\) |
|---------|---------|-------------|----------------|----------------|----------------|-----|-------|---------------|----------|---------|---------------|-------|-------|
| 1.05E+06 | 4180   | 1.19        | 43.9           | 4.90           | 3.98           | 1.23 | 0.003140 | 0.047         | 42        | 0.79    | 0.048         | 0.003184 |
| 1.19E+06 | 5340   | 1.37        | 48.2           | 5.55           | 4.47           | 1.24 | 0.003076 | 0.054         | 47        | 0.78    | 0.054         | 0.003089 |
| 1.46E+06 | 6510   | 1.66        | 48.7           | 5.51           | 4.45           | 1.24 | 0.003021 | 0.064         | 57        | 0.81    | 0.064         | 0.002993 |
| 1.58E+06 | 6780   | 1.80        | 49.0           | 5.20           | 4.28           | 1.21 | 0.002967 | 0.069         | 61        | 0.76    | 0.070         | 0.002999 |
| 1.67E+06 | 7100   | 1.91        | 47.3           | 5.20           | 4.23           | 1.23 | 0.002904 | 0.073         | 64        | 0.77    | 0.073         | 0.002921 |
| 1.76E+06 | 7630   | 2.00        | 46.9           | 5.36           | 4.33           | 1.24 | 0.002888 | 0.076         | 67        | 0.74    | 0.076         | 0.002873 |
5.1.4 Mean Velocity Profiles

Boundary layer mean velocity profiles for a range of Reynolds numbers based on the length of the test plate and normalised by the wall shear velocity, are presented in Figure 5.3 - Figure 5.5. The results for each Reynolds number collapse well onto the same curve in the log-law region of the boundary layer. The Log Law was fitted to the boundary layer mean velocity profiles using Equation 2.17, and Spalding’s Law for the buffer region was fitted using Equation 2.13.

Whilst the data for the 0.7 mm Pitot probe collapses well onto the same curve, it is not exhibiting smooth wall behaviour. It was noted above that the 0.7 mm Pitot probe was overestimating the local skin friction coefficient and wall shear stress, which explains the shift of the data on the \( u' \) vs. \( y' \) plot below the smooth wall curve. The same data analysed using Bradshaw’s method collapses onto the smooth wall curve as expected.

The 1.0 mm Pitot probe and the 1.0 mm hollow stem Pitot probe give acceptable results, with the data collapsing onto the smooth wall curve as expected. A comparison of the different Pitot probes is given in Figure 5.6, which clearly demonstrates the overestimation of the wall shear stress and local skin friction coefficient by the 0.7 mm Pitot probe.

Figure 5.2 Comparison of smooth wall skin friction coefficient data for 3 different Pitot probes using the Preston Tube Method (P) (Patel 1965) and Bradshaw's Method (B) (Bradshaw 1959)
Figure 5.3 Smooth wall dimensionless boundary layer profiles from the 0.7 mm Pitot probe at various Re.

Figure 5.4 Smooth wall dimensionless boundary layer profiles from the 1.0 mm Pitot Probe at various Re.
Figure 5.5 Smooth wall dimensionless boundary layer profiles from the 1.0 mm hollow stem Pitot probe at various Reₐ

Figure 5.6 Comparison of smooth wall dimensionless boundary layer profiles at Reₐ of approx 1.7 x 10⁶
5.1.5 Turbulence Profiles

Full data traces for a boundary layer traverse were recorded with the 1.0 mm Hollow Stem Pitot Probe to determine the turbulence profile. The profiles for three different Reynolds numbers are given in Figure 5.7, along with the well-known Klebanoff (1955) data for smooth walls. It is evident from Figure 5.7 that the measurement system is significantly underestimating the magnitude of the turbulence intensity.

Since the mathematical model predicted that the 1.0 mm hollow stem Pitot probe system would have the best frequency response, turbulence traces for the 0.7 mm and 1.0 mm diameter Pitot probe systems were not obtained. In an attempt to obtain an adequate frequency response a 2.0 mm diameter probe with the shortest length possible was used to minimise losses. This probe was connected directly to the pressure transducer by butting up the scanivalve tubing of the probe and pressure transducer connection and joining them with flexible tubing. Due to the minimal length of the probe, measurements were taken against the Perspex bottom of the water tunnel. The exact location of the probe in relation to the wall was unknown, as it was not physically possible to rest the probe against the wall.

Figure 5.7 Smooth wall turbulence intensity profiles for the 1.0 mm hollow stem Pitot probe: (a) \( \text{Re}_l = 1.5 \times 10^6 \); (b) \( \text{Re}_l = 1.6 \times 10^6 \); (c) \( \text{Re}_l = 1.75 \times 10^6 \); and the 2.0 mm short Pitot probe (\( \text{Re}_l = 1.75 \times 10^6 \))
It was not possible to obtain a full boundary layer traverse into the freestream. Thus the boundary layer thickness was estimated, based on results for a smooth plate, to be in the order of 45 mm. The turbulence intensity is plotted in Figure 5.7 and shows no improvement.

The base electrical noise level was minimised as much as possible by employing low-pass filters at the Nyquist frequency (500 Hz). However, the rms of the noise fluctuations was noted to be approximately the same as the magnitude of the turbulence intensity in the freestream, leading to further issues in determining the true turbulence intensity.

5.1.6 Frequency Spectra

A spectral analysis was undertaken for the 2.0 mm short Pitot probe system. The best frequency response was observed with a stiff diaphragm, and is shown in Figure 5.8. The spectral plot indicates a useful frequency response of approximately 10 Hz, where the roll-off starts to occur.

![Figure 5.8 Frequency Spectra for the 2.0 mm short Pitot probe system with a stiff diaphragm](image)

The size of the eddies to be measured is expected to vary from the boundary layer thickness (approximately 50 mm) to the Kolmogorov length scale (White 1991), which is approximately 0.01 mm in this case. For a velocity of 2.0 m/s, the frequency of the large eddies in the freestream should be approximately 40 Hz. The small eddies close to the wall have a much higher frequency (in the order of 10 kHz) and are not captured using this method. Aside from the frequency response issues, it is likely that the diaphragm would not be sensitive enough to capture the high frequency fluctuations.
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5.1.7 Conclusions

The possibility of using Pitot probes closely coupled to Validyne differential pressure transducers to measure unsteady velocity fluctuations has been investigated. A frequency response of up to 10 Hz can be obtained at the sacrifice of diaphragm sensitivity, probe tip diameter, and probe length. Unfortunately this is not adequate to capture all of the energy containing turbulent eddies expected in either the freestream or in the boundary layer. Thus whilst Pitot probes are good for measuring mean velocities, they are not suitable for measuring fluctuating velocity components in this situation, due to the effects of the inertia of the fluid in the probe and connecting tube, and friction losses. Another limitation is thought to be the design of the pressure ports of the Validyne pressure transducers. The fluid enters through a small hole perpendicular to the direction of the incoming fluid thus causing more losses.

The inability of the proposed system to measure fluctuating velocity resulted in the purchase of a LDV system. The theory of operation is described in Section 5.2 and the specifications of the system are given in Section 4.4.5.

5.2 LASER DOPPLER VELOCIMETRY

Laser Doppler Velocimetry is an optical technique for measuring velocities at a point in a flow. It is based on the Doppler phenomenon, where the frequency of a moving light or sound wave moving away from or towards a stationary observer will be shifted from its original value. Albrecht et al. (2003) and Durst et al. (1981) examine the LDV technique in depth, and Candries (2001) and Tachie (2000) review the principles of LDV and the errors associated with LDV use. LDV is compared and contrasted with other measurement techniques in Section 2.3

A basic LDV system consists of a continuous wave laser, transmitting optics, receiving optics, and a signal conditioner and processor, as shown in Figure 5.9. The laser provides a spatially and temporally coherent laser beam. A Bragg cell is used to both split the beam into two coherent beams and to frequency shift one of the beams. If the two laser beams are of the same frequency, a particle passing in either direction through the measurement volume at the same velocity will give the same Doppler shift frequency, which is problematic in situations of flow reversals or where the direction of the flow is unknown. Providing a frequency shift to one of the beams allows the directional ambiguity to be resolved.
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The two parallel beams are then focussed by a lens to intersect in the measurement volume. When two coherent laser beams intersect, they will interfere in the volume of intersection. The interference planes are known as fringes, and the distance, \( d_f \), between them is defined by the wavelength of the laser light, \( \lambda \), and the angle between the two beams, \( \theta \):

\[
d_f = \frac{\lambda}{2\sin(\theta/2)}
\]

Equation 5.4

The measurement volume is defined by the diameter of the beam and the angle between them.

The velocity information is determined from the light scattered by seeding particles carried in the fluid as they move through the measurement volume. The scattered light contains a Doppler shift of frequency, \( f_D \), which is proportional to the velocity as given in Equation 5.5:

\[
f_D = \frac{\nu}{d_f} = \frac{2\sin(\theta/2)}{\lambda} u
\]

Equation 5.5

The scattered light is collected by a receiver lens and focused on a photo-detector. An interference filter passes only the required wavelength to the photo-detector, removing noise from ambient light and other wavelengths. The photo-detector converts the light intensity to an electrical signal, the Doppler burst. The Doppler bursts are filtered and amplified in the signal processor, which determines the Doppler shift frequency for each particle.

The LDV system used in this study is of the backscatter type, where the receiving optics are housed in the same location as the transmitting optics.

An LDV system does not actually measure the velocity of the fluid itself. It is the velocity of particles suspended in the flow that are measured. The seeding particles must be small enough to track the flow accurately, yet large enough to scatter sufficient light for the photo-detector to be able to detect the Doppler shift frequency. In liquid flows naturally occurring particles may be used, or if unsatisfactory results are obtained, the fluid may be ‘seeded’ with another substance such as glass spheres or aluminium powder. In the present study, the flow was seeded with 10 \( \mu \)m hollow glass spheres.

The spatial and temporal resolution of an LDV system depends on the optical layout, the signal detection electronics, the validation scheme, and the particle seeding (Albrecht et al. 2003). Spatial resolution is particularly important in regions where a velocity gradient exists and is
discussed further in Section 5.2.1.4. The temporal resolution is adjusted primarily through the seeding particle concentration. For two-time statistics such as power spectra and correlation functions, it is important to have as high a frequency resolution (data rate) as possible.

5.2.1 Potential Sources of Error

LDVs can provide velocity and turbulence information with very high temporal and spatial resolution, provided that the user is aware of possible sources of error and takes the appropriate measures to correct for errors where required. This section details the different errors that may be encountered when taking measurements with an LDV system and the steps taken to avoid these errors, or where necessary, the corrections applied to minimise them.

5.2.1.1. Non-Orthogonality of Laser Beams

Whilst every care is taken in the set-up of the measurement system, the errors associated with components being not exactly orthogonal needs to be considered in both the $xy$ and $yz$ planes, as defined by Figure 5.10. Karlsson et al. (1993) studied non-orthogonality in detail and found that any rotation in the $xy$ plane (Figure 5.10a) caused inconsistencies in the measured data, particularly in the near-wall region for boundary layers. Corrections can be easily made to the
data using a coordinate transformation, provided that the angle of rotation, $\alpha$, is known as given in Equation 5.6 - Equation 5.8 (where the subscript $m$ refers to the measured value). Schultz (1998) deliberately rotated his laser system by 45 degrees in order to obtain measurements as close as possible to the wall.

$$v = \frac{1}{\cos \alpha} \left( v_m - u_m \sin \alpha \right)$$  \hspace{1cm} \text{Equation 5.6}

$$v' = \frac{1}{\cos \alpha} \sqrt{ v_m^2 - u_m v_m \sin \alpha + u_m^2 \sin^2 \alpha}$$  \hspace{1cm} \text{Equation 5.7}

$$\overline{u' v'} = \frac{1}{\cos \alpha} \left( \overline{u_m v_m} - u_m^2 \sin \alpha \right)$$  \hspace{1cm} \text{Equation 5.8}

To obtain data in the near-wall region using two-dimensional LDV techniques it is usually necessary to tilt the probe towards the wall (Candries 2001; Candries & Altar 2005; Karlsson et al. 1993; Schultz 1998, 2000; Schultz & Swain 1999; Tachie 2000), as shown in Figure 5.10b, where $\beta$ is the angle of rotation in the $yz$ plane. A problem arises from one of the laser beams measuring the wall normal component becoming blocked as the probe volume approaches the wall. Without tilting the probe, the closest that the probe volume can get to the wall is related to the beam half angle and the focal distance. In the current situation, the probe can only measure to 24.7 mm from the wall without tilting.

The near-wall region data can be corrected using the relations given in Equation 5.9 - Equation 5.11 (Karlsson et al. 1993), which were derived assuming $\overline{w}$, $\overline{u'w'}$ and $\overline{u'w'}$ are zero in a two-dimensional flow. Note that $v$ and $\overline{u' v'}$ are only affected through the geometrical factor $1/\cos \beta$; however, $v'$ has spanwise velocity terms present. Equation 5.11 can be solved using near-wall
flat plate boundary layer data, such as given in Karlsson et al. (1993) where $w' = 3.5v'$ for $y^+ = 5$ and $w' = 10.3v'$ for $y^+ = 2$. Inserting these relationships into Equation 5.11 gives $v'/v_m = 0.982$ for $y^+ = 5$ and 0.860 for $y^+ = 2$.

\[
v = \frac{1}{\cos \beta} v_m
\]

Equation 5.9

\[
\overline{u'v'} = \frac{1}{\cos \beta} \overline{u_m'v_m'}
\]

Equation 5.10

\[
\frac{v'}{v_m} = \frac{1}{\cos \beta} \frac{1 - \overline{w'^2} \sin^2 \beta}{\sqrt{\overline{w'^2} \cos^2 \beta + \overline{w'^2} \sin^2 \beta}}
\]

Equation 5.11

Karlsson et al. (1993) obtained two-dimensional measurements in coincidence mode as close as 0.10 mm from a solid wall by slightly tilting the probe with a tilt angle of 1.5°. The influence of tilting the probe was found to be negligible for the streamwise components, and for $v$ and $\overline{u'v'}$. For $v'$, the effects were negligible apart from the near-wall region, $y^+ \leq 5$, where the tilt angle had a substantial influence.

The two-dimensional measurements presented in Chapter 7 were obtained by rotating the probe to an angle of approximately $\beta = 5°$. The correction for $v$ and $\overline{u'v'}$ is negligible, as $1/\cos \beta = 1.004$. The closest measurement to the wall was obtained at approximately $y^+ = 20$, thus any influence on $v'$ should also be negligible.

Tachie (2000) carried out a systematic study of the influence of tilt angle. Tests were completed on a smooth wall turbulent boundary layer for angles in the range $0° \leq \beta \leq 3°$ using single-component LDV. Deviations amongst profiles obtained at different tilt angles were within the measurement uncertainties for the streamwise velocity, turbulence, and higher order moments. Two-dimensional LDV measurements were completed in a turbulent wall jet with tilt angles in the range $0° \leq \beta \leq 5°$. Similarly to the results reported by Karlsson et al. (1993), Tachie found that any deviations were within the measurement uncertainty, with the only exception being $v'$.

A similar test was carried out on a smooth wall turbulent boundary layer in the present study using single-component LDV for tilt angles in the range $0° \leq \beta \leq 2°$. Data for five different tilt angles are presented in Figure 5.11 and Figure 5.12, normalised by the wall shear velocity, $u^*$. The profiles show no dependence on the tilt angle, $\beta$. 


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It was not possible to test the two-dimensional measurements for tilt angle sensitivity due to the refraction of the beams causing the measuring volumes to become non-coincident, which is discussed in detail in Section 5.2.2.

To alleviate problems associated with probe tilting to obtain near-wall measurements, Connelly et al. (2006) redirected one of the vertical beams using a beam displacer on the LDV probe which aligned the beam parallel to the test surface. Other techniques to obtain near-wall measurements include using a mirror to tilt the laser beams (Kaftori et al. 1995) and cutting a narrow slot in the wall to allow the passage of the wall-normal laser beam without the need for tilting the probe (Poggi et al. 2002), however these techniques are not as practical as simply rotating the probe.
Figure 5.11 Mean velocity and turbulence statistics for a smooth wall boundary layer at various angles of tilt: (a) mean velocity; (b) turbulence intensity
Figure 5.12 Turbulence statistics for a smooth wall boundary layer at various angles of tilt: (a) skewness; (b) flatness
5.2.1.2. Velocity Bias

In turbulent flow measurements, biased statistics occur because a larger than average number of particles per unit time are swept through the measuring volume when the velocity is faster than the mean. Similarly, a smaller than average number of particles passes through the measuring volume when the velocity is slower than the mean. Thus a histogram of individual velocity readings is biased towards the faster end of the velocity range (Albrecht et al. 2003; Buchave et al. 1979; Dantec Dynamics 2006; Edwards 1987; McLaughlin & Tiederman 1973; Tachie 2000). The arrival rate of a particle is not independent of the velocity field. If arithmetic averaging is used, the statistics will be biased in favour of the higher velocities.

McLaughlin and Tiederman (1973) suggested a bias correction for one component measurements, however, Buchave et al. (1979) propose that it is more promising in turbulence measurements, particularly of high intensity, to use residence time weighting (also known as transit time weighting):

“If time averages were formed by integrating only during the time measurement (i.e. only during particle residence times) no bias would exist. Thus complete velocity bias correction is achieved (or better, bias is avoided altogether) if in addition to the velocity output a measurement of the residence time is made available at the processor output to be used as a weighting factor in the computation of statistical quantities.”

Transit time weighting should give correct results independent of the particular shape of the volume, including for measurements close to a wall, and is the most reliable estimator even at low data densities. Edwards (1987) recommends using residence time weighting, provided that the particle seeding density is spatially uniform and that the processor used gives an accurate estimate of the residence time.

Thus following Buchave et al. (1979), Tachie (2000), Candries et al. (2003, 2005), and Schultz and Flack (2007) the statistics for all measurements obtained using the LDV system were transit time averaged, where a transit time weighting factor is used to determine the flow statistics:

\[
\eta_i = \frac{t_i}{\sum_{j=0}^{N-1} t_j}
\]

Equation 5.12
where \( t_i \) is the transit time of the \( i^{th} \) particle crossing the measuring volume (Albrecht et al. 2003; Dantec Dynamics 2006).

### 5.2.1.3. Fringe or Angle Bias

Fringe or angle bias arises because processors cannot measure all speeds at all angles, as a set number of fringes must be encountered by a scattered particle passing through the measurement volume before the measurement is validated (Edwards 1987; Schultz & Flack 2005). Edwards (1987) recommends that the effective fringe velocity should be at least twice the maximum Doppler shift to provide uniform angular response.

### 5.2.1.4. Finite Measuring Volume / Velocity Gradient Bias

The measuring volume of an LDV system has a finite size, and whilst measurements are usually referred to as being at a point they are really integrated over the space in the measuring volume. This is particularly important in regions of large velocity gradient, including in the near-wall region of a turbulent boundary layer. Time averaged quantities will show a dependence on the measuring volume size and thus may require a volume correction (Albrecht et al. 2003; Durst et al. 1998; Durst et al. 1995; Edwards 1987; Tachie 2000).

The following expressions indicate the first order difference between the measured mean velocity and the actual mean at the centreline of the measuring volume. For a linear mean velocity gradient, no error occurs since the second derivative is zero. However, in situations with strong spatial velocity gradient, such as in a boundary layer, the linear assumption is invalid and the detection volume (given as diameter, \( d \)) must be decreased (Albrecht et al. 2003). Alternatively, the correction formulae as suggested by Durst et al. (1998; 1995) can be used:

\[
\bar{u}_{i,\text{meas}} = \bar{u}_{i,\text{true}} + \frac{d^2}{32} \left( \frac{d^2 \bar{u}_{i,\text{true}}}{dy^2} \right) + ... \tag{Equation 5.13}
\]

\[
\bar{u}_{i,\text{meas}}^2 = \bar{u}_{i,\text{true}}^2 + \frac{d^2}{16} \left( \frac{d \bar{u}_{i,\text{true}}}{dy} \right)^2 + \frac{d^2}{32} \left( \frac{d^2 \bar{u}_{i,\text{true}}}{dy^2} \right) + ... \tag{Equation 5.14}
\]

where \( d \) denotes the diameter of the measuring volume in the wall normal (\( y \)) direction and \( i \) denotes the \( i^{th} \) velocity component.
Similar expressions can be derived for the higher order moments. Schultz (2000) used the correction scheme proposed by Durst et al. (1998) to correct for velocity gradient bias in his $u'$ measurements. The correction to the mean velocity and other turbulence quantities were found to be quite small and were therefore neglected.

In the present study, the corrections were applied to a smooth wall boundary layer with points in the viscous sublayer. However the corrections were negligible and thus were not applied to subsequent data.

5.2.1.5. Multiple Particles in the Measuring Volume

In flows with a high particle density (and hence high data rate) or in a long measuring volume, the probability of there being more than one particle in the measuring volume and at any one time is high (Johnson & Barlow 1989; Tachie 2000). When there are multiple particles in the measuring volume, Doppler signals on both the streamwise and wall-normal channels may be validated coincidently, but may not come from the same particle. This can result in the Reynolds shear stress being underestimated.

Johnson and Barlow (1989) studied the effect of the spanwise length of the measuring volume on the velocity and stress components using a two-component LDV system and varying the spanwise extent of the measuring volume in the range $7 \leq l' \leq 44$ (where $l' = l u^*/\nu$). Shear stress measurements show a strong dependence on $l'$, with $\bar{uv}$ decreasing with measuring volume length. They recommended that to obtain accurate Reynolds shear stress measurements, the spanwise extent of the measuring volume should be less than 15 viscous units (i.e. $l' < 15$).

This was not possible for the experiments conducted in the present study. The length of the measuring volume was 1.1013 mm for the red pair of beams and 1.205 mm for the near-infrared pair of beams. This gave $l'$ values in the range 40 – 85 for smooth plate measurements and 50 - 120 for rough plate measurements. Thus the Reynolds shear stress may have been underestimated for the two dimensional measurements presented in Chapter 7.

5.2.1.6. Errors Due to Noise

The accuracy of LDV measurements may be affected by electronic noise from the signal processing equipment, the light scattering process, and from light scattered from small impurities which may collect on the sidewall of the water tunnel (Durst et al. 1995; Tachie 2000). Durst et al. (1995) found that cleaning the pipe wall prior to taking measurements reduced the electrical
noise, as did running the processor in total burst mode. Poor grounding of the experimental apparatus can also lead to ground noise (Albrecht et al. 2003), although every effort was made to remove ground loops in the present work. Noise contributions in LDV systems are usually considered to be spectrally white, or in other words the total noise power is distributed evenly over all frequencies (Albrecht et al. 2003).

5.2.2 Commissioning the 2D LDV System

Setting up the LDV system described in Section 4.4.5 to acquire accurate and reliable data in two dimensions simultaneously comprised a major component of this project. The potential errors involved in using such a system have already been discussed. The following sections detail the process of setting up the LDV system, minimising the potential sources of error, and other issues that arose such as low data rate. Some initial data is also presented.

5.2.2.1. An Investigation of Data Rate

The LDV system was originally installed so that the measurement volume was as close as possible to the centreline of the water tunnel. This resulted in a penetration depth from the inside of the Perspex wall to the measurement volume of 221 mm (excluding refraction affects). Early measurements found that the data rate, particularly on the near-infrared channel, was very low even when the probe was rotated 90° so that the near-infrared channel was parallel to the flow and measuring the streamwise velocity component.

Three separate investigations were carried out in an attempt to increase the data rate for both coincident (2D) and non-coincident measurements. These included seeding the flow (Section 5.2.2.1.1), examining the affects of light absorption (Section 5.2.2.1.2), and investigating the influence of ambient light on the data rate (Section 5.2.2.1.3). The findings are summarised in Section 5.2.2.1.4.

5.2.2.1.1. Seeding the Flow

In an attempt to increase the data rate, 10 µm hollow glass spheres were added to the flow as seeding particles. The data rates for various amounts of seeding are given in Table 5.6. A definite improvement in data rate was noted when 1 pinch of seeding particles was added to the flow in both coincidence and non-coincidence mode. Adding a second pinch of seeding particles did not further improve the data rate.
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5.2.2.1.2. Laser Penetration Depth

One point of difference between the LDV system used in the present study and LDV systems typically used in similar applications is the wavelength of the laser light used. The LDV system used in the present study consists of two different wavelengths: red (660 nm) and near infrared (785 nm). Other researchers have typically used blue-green lasers with lower wavelengths (e.g. Candries 2001; Schultz 1998; Tachie 2000). The extinction of the laser light in the particular medium being used (in this case water) is a function of the wavelength. Albrecht et al. (2003) express the extinction as an exponential decrease in intensity:

\[ I = I_o e^{-\gamma x_p} \]  

*Equation 5.15*

where \( \gamma \) is the extinction coefficient and \( x_p \) is the penetration depth. Typical values for the extinction coefficient for different wavelengths of light in pure water are given in Table 5.7.

<table>
<thead>
<tr>
<th>Wavelength [nm]</th>
<th>Extinction Coefficient (( \gamma )) [m(^{-1})]</th>
</tr>
</thead>
<tbody>
<tr>
<td>800</td>
<td>2.293</td>
</tr>
<tr>
<td>632.5</td>
<td>0.2995</td>
</tr>
<tr>
<td>527.5</td>
<td>0.0428</td>
</tr>
<tr>
<td>515</td>
<td>0.0396</td>
</tr>
<tr>
<td>487.5</td>
<td>0.0144</td>
</tr>
</tbody>
</table>

A study was done to investigate the relationship between the data rate and the penetration depth. The probe attachment to the traversing system was modified so that the laser could be moved away from the water tunnel wall (in the z-direction), hence providing the ability to change the penetration depth. The data rates in both coincidence and non-coincidence mode were measured
at a range of different z positions. The results are plotted in Figure 5.13. It is obvious from the results that the relationship between the data rate and the penetration depth is exponential. Thus measurements with acceptable acquisition times could be made by decreasing the penetration depth.

Moving the measurement volume away from the centreline of the water tunnel towards the side wall presents another potential problem: the uniformity of the transverse velocity distribution. Barton (2007) measured the traverse velocity distribution in the freestream and pressure distributions across the test plate as a part of the original calibration of the UTAS Water Tunnel. The transverse pressure distribution was measured using a series of Preston tubes butted up against the test plate, and found to be symmetrical about the plate longitudinal centreline. The flow was found to be evenly distributed across the central 400mm of the working section width, with some edge effects noted (see Figure 4.10). The transverse velocity distribution was measured using a Pitot-static probe, with the results showing the flow to be uniform within ± 2% across the central 500 mm of the working section (see Figure 5.14). Thus any boundary layer velocity profiles must be taken at a penetration depth of ≥ 100 mm.

\[ \text{Figure 5.13 Penetration depth vs. data rate for LDV system in coincidence mode} \]
5.2.2.1.3. Ambient Light Interference

It is possible that ambient light in the working section could increase the signal-to-noise ratio and hence reduce the data acquisition rate. A simple experiment was devised to investigate whether or not this was the case. The data rate was measured at two different flow speeds for two scenarios: the working section open to ambient light; and the working section completely covered with dark material to remove as much ambient light as possible as shown in Figure 5.15. The measurements were taken in the afternoon, when the ambient light in the laboratory is at its highest levels, for a period of either 10,000 samples or 60 seconds, whichever occurred sooner. The average data rate for each scenario is presented in Table 5.8, as well as the positive or negative trend. Covering the working section improves the data rate.

Table 5.8 Data rates (Hz) with and without ambient light at 2 different flow speeds

<table>
<thead>
<tr>
<th>Pump Speed: 300 rpm (U ~ 1.0 m/s)</th>
<th>Pump Speed: 600 rpm (U ~ 2.0 m/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Light</td>
<td>Dark</td>
</tr>
<tr>
<td>-------</td>
<td>------</td>
</tr>
<tr>
<td>2D</td>
<td>467</td>
</tr>
<tr>
<td>LDV 1</td>
<td>2190</td>
</tr>
<tr>
<td>LDV 2</td>
<td>1082</td>
</tr>
</tbody>
</table>
5.2.2.1.4. **Summary of Data Rate Investigations**

Each investigation into possible measures to increase the data rate was successful. Introducing seeding saw improvements of up to 400 Hz. Reducing the penetration depth of the laser beams in the water improved the data rate exponentially. Removing the ambient light from the working section also increased the data rate. However, the data rate for coincidence measurements was significantly lower than the data rate for non-coincident measurements (at times by 90%), leading to the conclusion that the measurement volumes were not coinciding. This is due to refraction effects and is explored in detail in Section 5.2.2.3.

5.2.2.2. **Optimising Tilt Angle for 1D Measurements**

One of the data sets presented in this thesis was measured using the LDV system in one-dimension to obtain data as close to the wall as possible. Very near-wall measurements were not possible in two-dimensions due to the need to tilt the laser, as discussed in Section 5.2.1.1.

The optimal LDV tilt angle for one-dimensional measurements was investigated by taking boundary layer profiles at 1.25 m/s and 2.00 m/s for five different tilt angles in the vertical plane (0.0°, 0.2°, 0.5°, 1.0°, and 2.0°). The tilt angle was measured using a digital inclinometer (Unilevel LS160 S-Digit Mini) which had an accuracy of ± 0.1°. The data was fitted to the smooth wall log law by adding a wall origin error until the data matched Spalding’s equation (Equation 2.13) in the viscous sublayer. The wall shear stress was determined using Bradshaw’s method, as described in Section 6.2.2.

The results for a freestream velocity of 2.00 m/s are shown in Figure 5.16. The results for a freestream velocity of 1.25 m/s were very similar and are not shown for clarity reasons. A tilt
angle of 0.2° had the lowest virtual origin error and lowest \( y^+ \) value for the point closest to the wall, indicating that 0.2° tilt gives data closest to the wall and within the viscous sublayer as shown in Table 5.9 for \( U = 1.25 \text{ m/s} \) and Table 5.10 for \( U = 2.00 \text{ m/s} \). Thus an LDV tilt angle of 0.2° was used for all of the subsequent one-dimensional LDV measurements reported in this thesis.

![Figure 5.16 Smooth wall boundary layer profiles at \( x = 850 \text{ mm} \) and \( U = 2.00 \text{ m/s} \) for different LDV tilt angles](image)

**Table 5.9 Smooth plate boundary layer parameters for \( U = 1.25 \text{ m/s} \) at different LDV tilt angles**

<table>
<thead>
<tr>
<th>Tilt [deg]</th>
<th>( Re_{\theta} )</th>
<th>( \delta ) [mm]</th>
<th>( \delta^* ) [mm]</th>
<th>( \theta ) [mm]</th>
<th>( H )</th>
<th>( u^* ) [m/s]</th>
<th>( C_f )</th>
<th>( \varepsilon ) [mm]</th>
<th>1st pt ( y^+ )</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0</td>
<td>3.52E+03</td>
<td>32.29</td>
<td>4.13</td>
<td>3.22</td>
<td>1.28</td>
<td>0.051</td>
<td>0.003373</td>
<td>0.26</td>
<td>11.68</td>
</tr>
<tr>
<td>0.2</td>
<td>3.67E+03</td>
<td>33.16</td>
<td>4.45</td>
<td>3.36</td>
<td>1.32</td>
<td>0.052</td>
<td>0.003372</td>
<td>0.01</td>
<td>2.69</td>
</tr>
<tr>
<td>0.5</td>
<td>3.62E+03</td>
<td>32.66</td>
<td>4.29</td>
<td>3.28</td>
<td>1.31</td>
<td>0.051</td>
<td>0.003388</td>
<td>0.07</td>
<td>3.18</td>
</tr>
<tr>
<td>1.0</td>
<td>3.62E+03</td>
<td>36.28</td>
<td>4.42</td>
<td>3.33</td>
<td>1.33</td>
<td>0.052</td>
<td>0.003402</td>
<td>0.10</td>
<td>6.72</td>
</tr>
<tr>
<td>2.0</td>
<td>3.69E+03</td>
<td>33.19</td>
<td>4.34</td>
<td>3.36</td>
<td>1.29</td>
<td>0.051</td>
<td>0.003356</td>
<td>0.14</td>
<td>6.31</td>
</tr>
</tbody>
</table>

131
5.2.2.3. Obtaining Coincident Measurements – Refraction Issues

When the UTAS Water Tunnel was constructed, it was not envisaged that it would be used with an optical measurement system. The working section is constructed of clear Perspex, as shown in Figure 5.17, but it is not of high optical quality. The working section has also been affected by thermal distortion and is bowed outwards in the streamwise direction. The alignment of the LDV traverse (x-axis) was checked against the side wall of the water tunnel and found to have up to a 1 mm difference over the 200 mm depth of the tunnel, with similar results in the streamwise direction. The side wall of the tunnel is not flat, and most likely not of uniform thickness.

Two-dimensional measurements were attempted through the Perspex at various tilt angles to obtain near-wall measurements. Results were obtained at a distance of 500 mm downstream from the leading edge of the test plate. The LDV probe was rotated 90° around the z-axis (see Figure 5.10 for definition of coordinate system) to enable the near-infrared pair of beams to

<table>
<thead>
<tr>
<th>Tilt [deg]</th>
<th>Re_θ</th>
<th>δ [mm]</th>
<th>δ* [mm]</th>
<th>θ [mm]</th>
<th>H</th>
<th>u* [m/s]</th>
<th>c_f</th>
<th>ε [mm]</th>
<th>1st pt y^*</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.0</td>
<td>5.53E+03</td>
<td>31.35</td>
<td>4.03</td>
<td>3.17</td>
<td>1.27</td>
<td>0.078</td>
<td>0.003141</td>
<td>0.14</td>
<td>12.10</td>
</tr>
<tr>
<td>0.2</td>
<td>5.63E+03</td>
<td>32.73</td>
<td>4.18</td>
<td>3.24</td>
<td>1.29</td>
<td>0.078</td>
<td>0.003115</td>
<td>0.02</td>
<td>4.69</td>
</tr>
<tr>
<td>0.5</td>
<td>5.74E+03</td>
<td>34.65</td>
<td>4.24</td>
<td>3.27</td>
<td>1.29</td>
<td>0.079</td>
<td>0.003143</td>
<td>0.07</td>
<td>11.85</td>
</tr>
<tr>
<td>1.0</td>
<td>5.57E+03</td>
<td>31.37</td>
<td>4.06</td>
<td>3.18</td>
<td>1.28</td>
<td>0.079</td>
<td>0.003136</td>
<td>0.09</td>
<td>9.72</td>
</tr>
<tr>
<td>2.0</td>
<td>5.66E+03</td>
<td>33.06</td>
<td>4.08</td>
<td>3.21</td>
<td>1.27</td>
<td>0.079</td>
<td>0.003104</td>
<td>0.13</td>
<td>12.51</td>
</tr>
</tbody>
</table>

Figure 5.17 Perspex working section (with rough plate installed)

Table 5.10 Smooth plate boundary layer parameters for U = 2.00 m/s at different LDV tilt angles
measure the streamwise component of the flow. The measuring volume penetration depth, ignoring refraction effects, was approximately 120 mm into the flow.

Results are plotted here for a tilt angle of 5°, although similar results were found for a tilt angle of 2.5°. Measurements taken with LDV orthogonal to the side wall did not allow adequate points to be taken in the boundary layer to assess if the measuring volumes were coincident or not. Reasonable results were obtained separately in the streamwise (Figure 5.18) and wall normal (Figure 5.19) directions, but the Reynolds shear stress, which is only valid if the two measurement volumes are totally coincident, was found to be severely under-estimated, as shown in Figure 5.20. The smooth wall data of Schultz and Flack (2005) is shown for comparison. The anomalies between the measured data and the data of Schultz and Flack (2005) may be due to either Reynolds number variation or to the distance downstream from the leading edge of the test plate. This will be examined in more detail in Chapter 7.

It was concluded from these results that the two measuring volumes were not coinciding. It was also thought that coincidence was not obtained when the LDV probe was orthogonal to the side wall of the water tunnel, based on the differences between the coincidence and non-coincidence data rates.

![Graph](image-url)

*Figure 5.18 Streamwise Reynolds normal stress for $Re_\theta = 2650 – 4520$, compared with data of Schultz and Flack (Schultz & Flack 2005) at $Re_\theta = 9050$*
Chapter 5 - Measuring Turbulence

Figure 5.19 Wall normal Reynolds stress for $Re_\theta = 2650 – 4520$, compared with data of Schultz and Flack (Schultz & Flack 2005) at $Re_\theta = 9050$

Figure 5.20 Reynolds shear stress for $Re_\theta = 2650 – 4520$, compared with data of Schultz and Flack (Schultz & Flack 2005) at $Re_\theta = 9050$
To verify the alignment of the laser beams, a sighting plate was manufactured with a 100 µm aperture to locate the beam crossing point. If the measuring volumes are coincident, all four laser beams should pass through the pinhole. The beam alignment was successfully verified with an air-Perspex-air interface and an air-Perspex-water interface with the LDV completely orthogonal (as much as possible) to the side wall of the water tunnel. As soon as the LDV probe is tilted in the vertical plane, as is required to obtain near-wall measurements, the measuring volumes become non-coincident; however, each pair still aligns satisfactorily.

Durst et al. (1981), Kaftori et al. (1995), Buchave et al. (1979), and Zhang and Eisele (1995, 1998a, 1998b) point out that tilting the probe slightly so as to avoid interference with the wall, or using a round duct rather than a duct with planar walls, causes the laser beams to enter the working section at a non-perpendicular angle to the interface, which can change the optical path of the laser beams. This distorts the measurement volume and fringe interference pattern, and may produce the following effects (Albrecht et al. 2003; Zhang & Eisele 1995, 1998a):

- Two dimensional measurements may become impossible because the two measurement volumes may no longer be coincident;

- The data rate of a single component back-scatter mode system may be significantly reduced due to only a small part of the lens surface being able to efficiently collimate the scattered laser light from the particle passing through the measurement volume, while a great number of lens surface segments with their individual focuses in the flow are blind to the measurement volume; and

- The beam waists may be translated along the beam axis, resulting in beam and fringe pattern distortion and errors in the moments.

The data obtained with the laser on a tilt certainly exhibited the first two effects and possibly the latter as well. Zhang and Eisele (1998b) considered the affects of fringe distortion in the measurement volume caused by improper optical layout and found that it results in a negligible overestimation of the mean velocity and turbulence, expect in flow with very low turbulence intensity. The error can be minimised by keeping the interface surface as thin as possible and by having the same media on both sides. Alternatively, a fluid may be used whose refractive index matches that of the duct (1995; Durst et al. 1981).
Zhang and Eisele (1995) describe the optical phenomenon of astigmatism, which refers to the loss of a unique focusing point of a light bundle after its refraction through a non-perpendicular interface when it will converge at two separate points. They state that the appearance of astigmatism in LDV measurements may decisively influence the measurement capability and accuracy, particularly where the probe has been tilted off-axis and where the light must pass through thick windows, such as in the case of the UTAS Water Tunnel. The spatial displacement between both intersection points depends on many parameters including: the degree of off-axis probe alignment; the beam intersection angle; the colour difference between the two pairs of laser beams; the thickness of the window; and the depth of the beam intersection points in the flow (Zhang & Eisele 1995).

The displacement between both intersection points for a 2D probe for a measurement arrangement, according to Figure 5.21, is given as (Zhang & Eisele 1995):

\[
\Delta S = \frac{\Delta x_m}{\cos \varepsilon_{o2}}
\]

Equation 5.16

where:

\[
\Delta x_m = \frac{1}{\phi_m} (L_1 \psi_1 + L_2 \psi_2)
\]

\[
\phi_m = \tan \varepsilon_{d2} - \tan \varepsilon_{b2}
\]

\[
\psi_1 = \frac{K}{\sqrt{n_1^2 - n_0^2 (1 - \cos^2 \phi \cdot \cos^2 \alpha/2)}} \left(\tan \varepsilon_{d1} - \tan \varepsilon_{b1}\right)
\]

\[
\psi_2 = \frac{K}{\sqrt{n_2^2 - n_0^2 (1 - \cos^2 \phi \cdot \cos^2 \alpha/2)}} \left(\tan \varepsilon_{d2} - \tan \varepsilon_{b2}\right)
\]

\[
K = n_0 \cos \phi \cdot \cos(\alpha/2) \cdot \left[\tan(\phi + \alpha/2) - \tan(\phi - \alpha/2)\right]
\]

A sensitivity analysis was conducted to determine how precise the alignment of the LDV probe needed to be. Figure 5.22 shows that even small misalignments of the probe result in an offset in the measuring volumes, making two-dimensional measurements impossible.
Booij and Tukker (1994) conducted measurements in a curved flume of rectangular cross section with a 4.10 m radius of curvature, a width of 0.50 m, and a water depth of approximately 50 mm.
To allow measurements in three dimensions using one-dimensional and two-dimensional probes, a water filled prism with a small air gap was fitted to the flume, as shown in Figure 5.23. This arrangement compensated for the refraction effects caused by the laser beams passing through several different media and allowed the measurement volume of each beam pair to be aligned. Kaftori et al. (1995) constructed a water cell on the side wall with a tilting window so that the window was always perpendicular to the optical axis of the laser beam and preserving the same pathlength, resulting in a difference in pathlength of the beams of less than 1% of the length of the measuring volume. Any astigmatism remaining is due to the transmission of the laser beams through the window, as the refractions of the laser beams at the external air-window interface are symmetric so that no displacement between intersection points of the laser beams will occur (Zhang & Eisele 1995).

The arrangement shown in Figure 5.23, with the probe in air, is equivalent to a probe in water inside the prism, with changed beam half-angle. The internal refractions of the laser beams in the water (or test medium) cause no additional displacement between the beam intersection points since $\Psi_i = 0$ if the flow water is assumed to be the $i^{th}$ medium (Zhang & Eisele 1995). If a narrow air gap exists between the water-filled prism and the window, the effect of the astigmatism can be compensated for completely ($\Delta x_m = 0$) and coincidence of the measurement volumes can be achieved. The thickness of the air gap can be determined by experiment or by calculation, as given by Zhang and Eisele (1995).

Thus to address the refraction issues and to obtain coincident measurements, a series of tilted prisms were designed and tested, using the existing 30 mm thick Perspex side wall of the water tunnel. Equation 5.16 was used to estimate the magnitude of the astigmatism, and the beam paths
were drawn up using AutoCAD to visualise the situation and to check the alignment of the measuring volumes, using the relationship for refraction of light through an interface given in Equation 5.17 (Snell’s Law). Both prisms were constructed of high quality 10 mm thick Perspex, and were angled at 5° to allow the laser to be tilted towards the wall to obtain near-wall measurements. Prism 1 (Figure 5.24) sealed against the side wall of the water tunnel, and Prism 2 (Figure 5.25) incorporated an adjustable air gap.

\[ n_1 \sin \theta_1 = n_2 \sin \theta_2 \]  

Equation 5.17

Assuming a 300 mm penetration depth and a refraction index of 1.49 for Perspex, the displacement of the measuring volumes for each scenario is given in Table 5.11. The dimensions of the measuring volumes are 0.1 mm diameter and 1.1 mm long for the red pair of beams, and 0.12 mm diameter and 1.2 mm long for the near-infrared pair of beams. The results of the analysis indicate that without a refraction correcting prism, the measuring volumes will be offset by 1.5 mm, which clearly indicates that the measuring volumes are no longer coincident.

It was noted that Prism 1 was distorted when bolted to the side wall of the water tunnel. An adequate seal between the two surfaces could not be obtained. This was due to the warp in the side wall of the tunnel. A graphical analysis of the beam paths indicated that the thickness of each medium that the laser tracks through must be uniform. Due to the warp in the side wall of the tunnel, an even air gap was not attainable with Prism 2. It is also unlikely that the Perspex side wall of the water tunnel is of uniform thickness.
Despite the best efforts of the research team, two-dimensional LDV measurements were not attainable through the existing Perspex side wall of the UTAS Water Tunnel. Thus a 200 mm diameter, 6 mm thick porthole of fused silica optical glass was designed and inserted into the side wall of the water tunnel to allow two-dimensional measurements at 850 mm from the leading edge of the test plate. Care was taken when selecting the material for the optical window that the dispersion was as low as possible to minimise any differences in refractive index for the red and near-infrared laser beams. Fused silica was chosen over BK7, as it has slightly better optical properties.

The porthole was designed to sit flush with the internal surface of the side wall of the water tunnel, to minimise any disruptions to the flow. A tilted glass panel was designed to sit at a 5° angle to allow two-dimensional measurements to be made close to the wall, with the gap between the two pieces of glass to be filled with water (designated Panel A). The possibility of incorporating an air gap was considered (designated Panel B). The displacement of the two

<table>
<thead>
<tr>
<th>Case</th>
<th>Schematic</th>
<th>∆S [ mm]</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>No prism</td>
<td><img src="image" alt="No prism Schematic" /></td>
<td>1.545</td>
<td>• The separation between measuring volumes varies with penetration depth (calculated for 300 mm penetration depth).</td>
</tr>
</tbody>
</table>
| Prism 1       | ![Prism 1 Schematic](image)  | 0.042    | • Displacement of measuring volumes is due to thickness of Perspex side wall only.  
• Probe in air is equivalent to probe in water, but with changed beam half angle. |
| Prism 2       | ![Prism 2 Schematic](image)  | 0.000    | • Air gap of 6.70 mm is required to completely compensate for astigmatism.  
• Displacement of measuring volumes and required air gap are a function of thickness of the Perspex side wall and the backing plate of the prism.  
• Probe in air is equivalent to probe in water, but with changed beam half angle. |
measuring volumes for each scenario with the optical glass are presented in Table 5.12. There was insufficient space to insert Panel B into the water tunnel without making major structural changes to the working section. Thus the Panel A arrangement was built and installed. The expected displacement of 0.007 mm was considered manageable. The optical porthole with the tilted glass panel installed is shown in Figure 5.26.

Dantec Dynamics advised that the data rate in coincidence mode is typically anywhere between 50-70% of the weakest signal in non-coincidence mode, depending on the flow seeding and flow conditions. A data rate 44% of the weakest signal in non-coincidence mode was achieved with the LDV completely orthogonal to the optical porthole (without the tilted window).

*Table 5.12 Displacement of LDV measuring volumes for different scenarios using optical glass porthole with LDV tilted 5°*

<table>
<thead>
<tr>
<th>Case</th>
<th>Schematic</th>
<th>ΔS [ mm]</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>No Panel</td>
<td><img src="image" alt="No Panel Schematic" /></td>
<td>1.052</td>
<td>• The separation between measuring volumes varies with penetration depth (calculated for 300 mm penetration depth).</td>
</tr>
<tr>
<td>Panel A</td>
<td><img src="image" alt="Panel A Schematic" /></td>
<td>0.007</td>
<td>• Displacement of measuring volumes is due to thickness of glass only.</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>• Probe in air is equivalent to probe in water, but with changed beam half angle.</td>
</tr>
<tr>
<td>Panel B</td>
<td><img src="image" alt="Panel B Schematic" /></td>
<td>0.000</td>
<td>• Air gap of 1.60 mm is required to completely compensate for astigmatism.</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>• Displacement of measuring volumes and required air gap are a function of thickness of the glass porthole and the backing plate of the prism.</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>• Probe in air is equivalent to probe in water, but with changed beam half angle.</td>
</tr>
</tbody>
</table>
For the two-dimensional measurements, the tilted panel was installed. The LDV was aligned to the tilted surface within 0.1 mm across approximately 100 mm diameter using a dial gauge attached to the head of the LDV probe. The connection plate between the LDV and the traverse was fitted with fine adjustment screws to allow the precise alignment of the probe in three-dimensions. The actual tilt of the LDV to the horizontal was 5.5°. The difference between the coincident and non-coincident data rates is given in Table 5.13. Thus for the range of speeds used for the boundary layer traverses, the data rate in coincidence mode was 41-52% of the data rate of the weakest signal in non-coincidence mode.

Two-dimensional boundary layer measurements for smooth, rough and biofouled test plates are presented in Chapter 7. The Reynolds shear stress was found to correlate reasonably with other smooth wall data, as shown in Section 7.3.9.

<table>
<thead>
<tr>
<th>Pump Speed [rpm]</th>
<th>Approximate U [m/s]</th>
<th>Non-Coincidence Data Rates [Hz]</th>
<th>Coincident Data Rate [Hz]</th>
<th>Percentage Data Rate of LDV 2</th>
</tr>
</thead>
<tbody>
<tr>
<td>150</td>
<td>0.5</td>
<td>1348</td>
<td>189</td>
<td>99</td>
</tr>
<tr>
<td>300</td>
<td>1.0</td>
<td>2337</td>
<td>438</td>
<td>203</td>
</tr>
<tr>
<td>500</td>
<td>1.7</td>
<td>3151</td>
<td>735</td>
<td>318</td>
</tr>
<tr>
<td>600</td>
<td>2.0</td>
<td>3680</td>
<td>903</td>
<td>368</td>
</tr>
</tbody>
</table>
Chapter 5 - Measuring Turbulence

5.3 CHAPTER SUMMARY

Much of the present study was devoted to the successful measurement of unsteady velocity and the associated 2\textsuperscript{nd} order or higher moments and Reynolds stresses. An unsuccessful attempt was made to measure turbulence using a closely coupled Pitot probe and Validyne pressure transducer. A Laser Doppler Velocimetry system was then employed. The setting up and commissioning process for this system was laborious and many problems were encountered. These have been described in detail.
Chapter 6 begins with a review of methods available to determine the wall shear stress and skin friction coefficient. Some of these methods have been applied to data obtained from the UTAS Water Tunnel. They are critically assessed for their accuracy, reliability, and ability to produce realistic results.

6.1 METHODS TO DETERMINE SKIN FRICTION

The skin friction coefficient, wall shear stress, and friction velocity are vital parameters in boundary layer analysis (Schetz 1993). The local skin friction coefficient is usually determined from a boundary layer velocity profile, and is given by Equation 2.8. The drag coefficient is determined from the drag force measured on a surface, and is given by Equation 2.9.

There are two main groups of methods available to measure the wall shear stress: direct force methods and wall similarity techniques, which include velocity profiles and similarity of flow about obstacles such as Preston tubes (Brown & Joubert 1969; Winter 1977). Brown and Joubert (1969) and Winter (1977) give comprehensive reviews of techniques to measure skin friction. Methods applicable to the current research are reviewed in this section.

6.1.1 Direct Force Methods

Direct force methods involve removing part of the wall and inserting a flush-mounted element that can move freely. A transducer is attached to the floating element to directly measure the drag force (Brown & Joubert 1969), and the output is calibrated against a known force. A schematic of such a floating element is given in Figure 6.1, and a photograph of the floating element installed in the UTAS Water Tunnel is given in Figure 6.2. The output signal may be provided by a linear variable differential transformer or a bridge arrangement of strain gauges, as in the present study.
The shear force on the floating element is the product of the wall shear stress, $\tau_w$, and the surface area of the floating element exposed to the flow. However, there are several secondary forces and effects which also need to be considered. Winter (1977) and Brown and Joubert (1969) provide comprehensive reviews of floating element force balances, and list the following problems which must be considered when designing or using one:

- A transducer that is capable of measuring small forces is required;
- A compromise is required between the need to measure the local properties and the necessity of having an element of sufficient size that the forces on it can be measured accurately;
Chapter 6 - Data Analysis

- Effects of the necessary gaps around the floating element:
  - The gap around the floating element acts as a source of roughness, particularly if there is any misalignment between the floating element and the roof of the channel;
  - The floating element is free to move in the gap, which may cause changes in the flow pattern through the gap;
- Forces arising from pressure gradients;
- Sensitivity of result to misalignment of the element with the surrounding surface;
- Effects of temperature changes; and
- Effects of leaks:
  - The pressure gradient (if one exists) will cause a pressure difference between the fluid in the boundary layer and the fluid in the cavity above the floating element. The resultant flow through the gap, presumably into the cavity on the high pressure side and out of the cavity on the low pressure side, will result in a momentum exchange between the fluid in the cavity and the fluid in the boundary layer. At least part of the force needed to maintain this momentum exchange will be transferred to the floating element.

Perhaps the greatest cause of inaccuracy is the alignment of the face of the element with the surface in which it is mounted. Figure 6.3 demonstrates the forces caused by misaligning the floating element. For an element that protrudes into the flow, the pressure rise caused by the forward-facing step and the pressure drop caused by the rearward-facing step which act on the edges of the element will result in an increased force reading. For a parallel-linkage balance, such as the one used in the present study, the effects of a recessed element are limited to the forces acting on the ends, unlike a pivoted balance where the effects of separation which cause forces acting normal to the plate will contribute to the moment measured (Winter 1977).
The balance used in the current study is of the parallel-linkage type (Figure 6.2) and thus the errors incurred by misalignment of the element are limited to the axial forces. For a protruding element, it is necessary to consider both the forces on the edge of the element that protrudes, and the forces on the edges within the gaps. Winter (1977) states that it is usually assumed that the pressure forces act over half of the thickness of the edges of the element. For a recessed element, the only axial forces occur within the gaps.

The gap size is a critical factor in floating element force balance design. Increasing the gap size around the floating element reduces the effect of misalignment. However, having a large gap may be expected to disturb the flow over the surface (Winter 1977). For flows in pressure gradients, the minimum possible gap and edge thickness are desirable to reduce the flow through the gaps and its effects on the pressure at the edges (Winter 1977).

Brown and Joubert (1969) investigated the secondary forces acting on a floating element due to both the direct effects of a pressure gradient, and the distortion of the boundary layer flow caused by the gap around the floating element. They found that the greatest secondary force for their system was 15% of the wall shear stress force.

Schultz (1998) and Candries (2001) point out that accurate measurements of the skin friction on rough walls using a floating element force balance are difficult. The surface of the floating element should accurately mimic the surrounding surface, which was obviously not the case for the rough and biofouled plate measurements made in the present study.
The floating element force balance used in the current study is described in detail in Chapter 4. The data analysis procedures for the total drag measurements are presented in Section 6.3, along with results from repeatability tests on both smooth and rough plates.

6.1.2 Preston Tube Method

The Preston tube method (Preston 1954) is one of several methods that are based on similar flows about obstacles. The velocity field around any small obstacle immersed completely in the log-law region is determined by the wall variables, provided that the obstruction is small enough to have a negligible effect on the growth of the boundary layer. Thus any pressure difference, \( \Delta P \), is a function of the wall shear stress, \( \tau_w \), the density of the fluid, \( \rho \), the kinematic viscosity of the fluid, \( \nu \), and the length scale associated with the obstruction which is the diameter, \( d \), for a Preston tube (Brown & Joubert 1969).

Preston (1954) expressed the relationship between the Preston tube reading and the skin friction in the following non-dimensional form:

\[
\frac{\tau_w d^2}{4 \rho v^2} = f\left(\frac{\Delta P d^2}{4 \rho v^2}\right) \quad \text{where} \quad \Delta P = \text{difference between static and total pressure measured by a surface Pitot tube} \tag{Equation 6.1}
\]

Patel (1965) carried out extensive calibrations of the Preston tube in both favourable and adverse pressure gradients, and expressed the calibration in terms of the parameters \( x^* \) and \( y^* \), given in Equation 6.2. The calibration curve for the zero pressure gradient case is constructed in three parts: one for each of the linear sublayer, the transition region, and the fully turbulent region, as given in Equation 6.3 (Chue 1975; Winter 1977).

\[
y^* = \log_{10}\left(\frac{\tau_w d^2}{4 \rho v^2}\right) \quad \text{and} \quad x^* = \log_{10}\left(\frac{\Delta P d^2}{4 \rho v^2}\right) \tag{Equation 6.2}
\]

\[
\begin{align*}
y^* &= 0.5x^* + 0.037 \quad \text{for} \quad 0 < y^* < 1.5 \\
y^* &= 0.8287 - 0.1381x^* + 0.1437x^2 - 0.0060x^3 \quad \text{for} \quad 1.5 < y^* < 3.5 \tag{Equation 6.3} \\
x^* &= y^* + 2\log(1.95y^* + 4.10) \quad \text{for} \quad 3.5 < y^* < 5.3
\end{align*}
\]
For adverse and favourable pressure gradients, the pressure gradient was expressed in terms of a pressure gradient parameter \( \Delta p = (\nu u^* / \rho)(dp/dx) \). Patel (1965) suggested the following purely empirical limits in terms of the pressure gradient parameter to determine the wall shear stress within the prescribed accuracy:

**Adverse Pressure Gradients:**
- Maximum error 3\%: \( 0 < \Delta p < 0.01, u^*/\nu \leq 200 \)
- Maximum error 6\%: \( 0 < \Delta p < 0.015, u^*/\nu \leq 250 \)

**Favourable Pressure Gradients:**
- Maximum error 3\%: \( 0 > \Delta p > -0.005, u^*/\nu \leq 200, dp/dx < 0 \)
- Maximum error 6\%: \( 0 > \Delta p > -0.007, u^*/\nu \leq 250, dp/dx < 0 \)

Macintosh and Isaacs (1992) further improved the Preston tube by adding a static tube in the wake of the Preston tube to measure the static pressure, removing the need for a static wall tapping.

### 6.1.3 Momentum Integral Methods

To determine the shear stress using momentum integral methods all that is required is to substitute measured quantities into the momentum integral equation, as given in Equation 6.4 for two-dimensional flow, and solve for \( \tau_w \) (Brown & Joubert 1969). However, it is quite difficult to apply the momentum integral equation to developing boundary layers and boundary layers in pressure gradients (Brown & Joubert 1969; Perry et al. 1969) and thus this method has not been used in the present study.

\[
\frac{\tau_w}{\rho} = \frac{d}{dx} \left( U \delta \right) + U \delta^* \frac{dU}{dx}
\]

*Equation 6.4*

### 6.1.4 Wall Similarity Methods

It is well known that the velocity profiles for turbulent boundary layers have inner layers for which the velocity scale is the wall shear velocity, \( u^* \). The wall shear stress, \( \tau_w \), is given by Equation 2.6 and can be determined by measuring the velocity gradient in the viscous sublayer. However, this is no simple task, as the viscous sublayer is typically very thin and it is difficult to
obtain accurate velocity measurements in this region due to the physical limitations of velocity probe size.

6.1.4.1. Inner Layer Similarity

Clauser (1954) was the first to observe similarity in the inner region of the boundary layer. He developed a method for graphically determining the local skin friction coefficient, $c_f$, on smooth plates based on the observation that similarity exists in the inner region of the boundary layer when scaled with the wall shear velocity, $u^*$. Clauser used the log law (Equation 2.17) and the fact that $\frac{u}{U} = \frac{u^*}{U} \frac{2}{c_f}$ and $\frac{yu^*}{\nu} = y\left(\frac{U}{V}\right)\sqrt{\frac{c_f}{2}}$, to construct a family of $u/U$ vs. $\ln(yU/\nu)$ curves by varying $c_f$ with each line corresponding to a different value of $c_f$. This is known as a Clauser Chart. When experimental data is plotted on a Clauser Chart, the data in the log law region will be parallel to one of the lines on the chart, giving the appropriate value for $c_f$.

Bradshaw (1959) formulated a more convenient form of the Clauser Chart method, which has been used for all smooth surface profiles measured in this study. Winter (1977) describes the method as follows: one suitable reference point on the inner-law curve is taken, such as $yu^*/\nu = 100$ for which $u/u^* = 16.2$ (using $\kappa = 0.41$ and $C = 5.0$). By taking a range of values of $u/u^*$ a curve of $u/U$ versus $yU/\nu$ can be drawn, as illustrated in Figure 6.4. The value of $u/U$ at the intersection of this curve with the measured velocity profile leads to $c_f$, since:

$$
c_f = 2\left(\frac{u^*}{U}\right)\frac{w}{U} = 2\left(\frac{u}{U}\right)\frac{u}{u^*} \left(\frac{u}{u^*}\right)_{ref}
$$

Equation 6.5

The Clauser Chart method for determining $c_f$ is not easily used for rough walls, as the origin for $y$ is unknown, and the roughness causes a shift in the logarithmic profile – thus the $c_f$ value is determined only by the slope of the line and not its position (Perry & Joubert 1963; Perry et al. 1969). However, if the wall shear stress can be determined by another method, the Clauser Chart method may be used fairly accurately to determine both the origin for $y$ and the shift in the logarithmic profile (Perry & Joubert 1963; Perry et al. 1969).
Research on rough plates is more difficult than on smooth plates as the experimental techniques used to find the shear stress on smooth plates, such as the Preston tube method, cannot be applied unless the laws of roughness behaviour are known (Perry & Joubert 1963). The main difficulty in determining the skin friction coefficient for rough or biofouled walls is that the exact wall origin is not known prior to taking measurements, due to the rough nature of the wall (Perry & Joubert 1963; Perry & Li 1990). The question arises as to where to begin the boundary layer profile – in the gaps between roughness elements; at the peaks of the roughness elements; or somewhere in between? To overcome this problem, measurements are usually started from an arbitrary location and adjusted using a wall origin (or virtual origin) error, $\varepsilon$.

Perry and Joubert (1963) developed a simple method to estimate $\varepsilon$. Data is plotted for $w/U$ vs. $\log(y)$. The log-law region will most likely be curved, rather than linear. To force a linear log-law region, the $y$ values are adjusted by $\varepsilon$. If the log-law region curve has a positive second derivative, a positive value of $\varepsilon$ is added to all $y$ data points. If the log-law region curve has a negative second derivative, a negative value of $\varepsilon$ is added to all $y$ data points. This procedure is repeated until a linear log-law region is obtained.
Once the virtual origin has been determined, an adaptation of the Clauser Chart Method may be used to determine the local skin friction coefficient. The method has been successfully applied to fouled surfaces by Lewthwaite et al. (1985), Schultz and Swain (1999), Schultz (2000), and Barton (2007). Given the definition of the wall shear stress (Equation 2.7) and the local skin friction coefficient (Equation 2.8) it follows that the wall friction velocity can also be given by Equation 6.6:

\[ u^* = U \sqrt{\frac{c_f}{2}} \]  

Equation 6.6

The log law can be rewritten in terms of U by substituting the expression for \( u^* \) (Equation 6.6) into Equation 2.17 to give:

\[
\frac{u}{U} = \frac{\sqrt{c_f}}{\kappa} \ln \left( \frac{y U}{\nu} \right) + \frac{\sqrt{c_f}}{\kappa} \ln \left( \frac{c_f}{2} \right) + \sqrt{\frac{c_f}{2}} C
\]

Equation 6.7

When experimentally measured points of \( u/U \) are plotted against \( \ln(yU/\nu) \), the slope of the regression line of best fit is given by differentiation of Equation 6.7 as:

\[
\frac{d \left( \frac{u}{U} \right)}{d \left( \ln \frac{y U}{\nu} \right)} = \frac{\sqrt{c_f}}{\kappa}
\]

Equation 6.8

Thus the local skin friction coefficient, \( c_f \), can be determined from Equation 6.9:

\[
c_f \approx 2\kappa^2 \frac{d \left( \frac{u}{U} \right)}{d \left( \ln \frac{y U}{\nu} \right)}
\]

Equation 6.9

In the present study, the use of Perry and Joubert’s (1963) method to determine the virtual origin combined with the above method to determine the skin friction coefficient, originally given by Lewthwaite et al. (1985), is referred to as the “Log Law Slope Method”. The analysis of boundary layer profiles using the Log Law Slope Method with virtual origin correction was carried out using a MATLAB program, which is described in more detail in Section 6.2.3.
Chapter 6 - Data Analysis

Perry and Joubert (1963) found that the method described above was insensitive, since many combinations of $\varepsilon$ and $c_f$ gave equally good fits to the experimental data. Perry et al. (1969) suggest that the above method is acceptable where the skin friction coefficient is known by another method such as pressure tappings in the roughness elements (if the roughness elements are large enough), or the momentum integral method.

Perry and Li (1990) developed a method to determine the wall origin error and the wall shear velocity simultaneously using points in the inner layer of the boundary layer. They argue that methods using outer layer similarity put too much weight on the properties of the outer part of the layer, and hence rely too heavily on having a universal defect law. Perry and Li gave the following equation for the mean velocity in the logarithmic wall region, with wake parameter, $\Pi = 0.55$ (corresponding to zero streamwise pressure gradient):

$$\frac{u}{U} = 1 + \frac{1}{\kappa} \frac{u^*}{U} \ln \left( \frac{y}{\delta^*} \right) + \frac{1}{\kappa} \frac{u^*}{U} \ln \left( \frac{u^*}{U} \right) + 0.493 \frac{u^*}{U}$$

Equation 6.10

To achieve the result given in Equation 6.10, Perry and Li must have used 0.42 for the von Karman constant, $\kappa$. Perry and Li’s rough wall boundary layer equation for the inner layer is derived as follows, starting from the rough wall velocity defect law given in Equation 2.30:

$$\frac{U - u}{u^*} = -\frac{1}{\kappa} \ln \left( \frac{y}{\delta} \right) + \frac{\Pi}{\kappa} \left[ w(1) - w \left( \frac{y}{\delta} \right) \right]$$

By definition, (Coles 1956), $w(1) = 2$, and $w(y/\delta) = 0$ in the log law region. Thus

$$\frac{U - u}{u^*} = -\frac{1}{\kappa} \ln \left( \frac{y}{\delta} \right) + \frac{2\Pi}{\kappa}$$

Substituting $\delta = \frac{\kappa U}{(1 + \Pi) u^*}$ from Coles (1956) gives:

$$\frac{U - u}{u^*} = -\frac{1}{\kappa} \ln \left( \frac{y}{\delta^*} \right) - \frac{1}{\kappa} \ln \left( \frac{u^*}{U} \right) - \frac{1}{\kappa} \ln \left( \frac{1 + \Pi}{\kappa} \right) + \frac{2\Pi}{\kappa}$$

Rearranging gives
A family of curves similar to a Clauser chart may therefore be obtained by plotting \( \frac{u}{U} \) versus \( \frac{y}{\delta^*} \). The experimental data is first plotted with \( \varepsilon = 0 \). Points below \( \frac{y}{\delta^*} = 1 \) are used and compared with the constant \( \frac{u^*}{U} \) lines. The data is adjusted by adding the virtual origin error, \( \varepsilon \), incrementally until the experimental data fits one of the plotted curves, as shown in Figure 6.5. Perry and Li (1990) found that a ± 10% variation in the value of \( \Pi \) caused only a ± 3% variation in the value for \( u^* \). There is variation in \( \Pi \) in the UTAS Water Tunnel due to the slight flow acceleration caused by the growing boundary layer. \( \Pi \) values at different downstream stations for a smooth plate are given in Table A.9.

In this study, this method is referred to as “Perry and Li’s Method”. The analysis of boundary layer profiles using Perry and Li’s Method was carried out using a MATLAB program, which is described in more detail in Section 6.2.4.

Equation 6.11

\[
\frac{u}{U} = 1 + \frac{1}{\kappa U} u^* \ln \left( \frac{y}{\delta^*} \right) + \frac{1}{\kappa U} u^* \ln \left( \frac{u^*}{U} \right) + \frac{1}{\kappa U} u^* \ln \left( \frac{1 + \Pi}{\kappa} \right) - \frac{u^*}{U} \frac{2\Pi}{\kappa} 
\]

Figure 6.5 Perry and Li’s Method for determining the skin friction over a rough wall (Perry & Li 1990) (\( \varepsilon \) is the virtual origin offset)


6.1.4.2. Outer Layer Similarity

Bandyopadhyay (1987) points out that the Clauser Chart technique (and its variations) is a smooth wall profile matching technique which is applied in the logarithmic region only. There exist other techniques which use the entire outer region of the boundary layer to determine the wall shear stress. These will now be described.

A commonly used form for the velocity defect wake function (Equation 2.30) was proposed by Hama (1954). Hama split Equation 2.30 into two parts. The first part (Equation 6.12) describes the velocity profile in the logarithmic region, and is applicable for \( \frac{y + \varepsilon}{\delta} \leq 0.15 \). Note that \( \delta^*U \) has been used instead of \( \delta \) as proposed by Clauser (1954). The second part describes the velocity profile in the outer region; its velocity distribution can be given by the empirical Equation 6.13. Equation 6.13 connects smoothly with the logarithmic velocity defect law (Equation 6.12) at \( \frac{y}{\delta} = 0.15 \), or \( \frac{y u^*}{\delta^*U} = 0.045 \) (Hama 1954). Hama’s formulation fixes the wake strength, \( \Pi \), and thus only \( u^* \) and \( \varepsilon \) need be determined. Another advantage of this technique is that it can be used for boundary layer profiles over both smooth and rough surfaces.

\[
\frac{U - u}{u^*} = - \left( 5.6 \log \left( \frac{y + \varepsilon}{\delta^*U} + 0.6 \right) \right) \text{ for } \frac{y + \varepsilon}{\delta} \leq 0.15
\]

Equation 6.12

\[
\frac{U - u}{u^*} = 9.6 \left( 1 - \frac{y + \varepsilon}{\delta} \right)^2 \text{ for } \frac{y + \varepsilon}{\delta} > 0.15 \quad \left( \delta = 0.30 \delta^* \frac{U}{u^*} \right)
\]

Equation 6.13

However, it has been observed by Bandyopadhyay (1987), Acharya and Escudier (1985), and Krogstad et al. (1992) that the wall shear velocity, \( u^* \), obtained using Hama’s method (Equation 6.12 and Equation 6.13) is consistently higher than that obtained using other methods such as the momentum integral methods (see Section 6.1.3), a total force balance (see Section 6.1.1), or the total stress method (see Section 6.1.5). Bradshaw (1987) proposed that the elevated friction velocity may be due to the implicitly fixed wake parameter being too small.

A method which allows the wake strength, \( \Pi \), to be optimised has been successfully used by several researchers to determine the wall shear stress (Akinlade et al. 2004; Candries 2001; Granville 1976; Krogstad et al. 1992; Tachie 2000). This method will be referred to as the Modified Hama Method. The wake function given in Equation 6.14 is used, since it satisfies the boundary conditions (zero outer slope and conformity to the boundary conditions of the wake
function) both near the wall and at the edge of the boundary layer (Granville 1976; Krogstad et al. 1992).

\[ w \left( \frac{y}{\delta} \right) = \left( \frac{1}{2\Pi} \right) \left[ (1 + 6\Pi) - (1 - 4\Pi) \left( \frac{y}{\delta} \right) \right] \left( \frac{y}{\delta} \right)^2 \]  

**Equation 6.14**

Krogstad et al. (1992) combined Equation 2.30 and Equation 6.14 to obtain:

\[ \frac{u}{U} = 1 + \frac{u^*}{\kappa U} \left[ \ln \left( \frac{y}{\delta} \right) - (1 + 6\Pi) \left( 1 - \left( \frac{y}{\delta} \right)^2 \right) + (1 + 4\Pi) \left( 1 - \left( \frac{y}{\delta} \right)^3 \right) \right] \]  

**Equation 6.15**

For rough-wall boundary layers, the distance from the wall, \( y \), needs to be replaced with the corrected value, \( (y + \varepsilon) \). Equation 6.15 is fitted to the data to determine \( u^* \), \( \Pi \), and \( \varepsilon \) using least squares regression analysis. Following Figliola and Beasley (2000), Equation 6.15 can be written in least squares form as follows:

\[ D = \sum_{i=1}^{n} \left[ \left( \frac{u}{U} \right) - 1 - \frac{u^*}{\kappa U} \left[ \ln \left( \frac{y + \varepsilon}{\delta} \right) + (1 + 6\Pi) \left( 1 - \left( \frac{y + \varepsilon}{\delta} \right)^2 \right) - (1 + 4\Pi) \left( 1 - \left( \frac{y + \varepsilon}{\delta} \right)^3 \right) \right] \right]^2 \]  

**Equation 6.16**

To minimise \( D \), the partial derivatives of the dependent variables (\( \partial D / \partial u^* \), \( \partial D / \partial \Pi \), \( \partial D / \partial \varepsilon \)) must be equal to zero. Thus a set of equations can be formulated to determine \( u^* \), \( \Pi \), and \( \varepsilon \).

Krogstad et al. (1992) found that the \( u^* \) determined using the modified Hama method where the wake strength is allowed to vary was in much better agreement with the \( u^* \) obtained by extrapolating the Reynolds shear stress to the wall than the \( u^* \) determined using the original Hama method.

### 6.1.5 Total Stress Method

The total stress method assumes a nominally constant shear stress region exists in the inner part of the boundary layer which is equal to the wall shear stress. The total stress is calculated at the plateau of the Reynolds shear stress profile in the overlap region of the boundary layer by summing the viscous and turbulent stress contributions, and can be used for both smooth and
rough surfaces (Schultz & Flack 2007). The local skin friction coefficient, $c_f$, can then be determined using the following expression:

$$c_f = \frac{2}{U^2} \left[ \nu \frac{\partial U}{\partial y} - \overline{u'v'} \right]$$  \hspace{1cm} \text{Equation 6.17}

Other researchers (Candries 2001; Krogstad et al. 1992; Schultz 1998) have simply extrapolated the peak of the measured Reynolds shear stress to the wall. Ligrani and Moffat (1986) found that the Reynolds shear stress in the near-wall region contributed 96-98% of the wall shear stress.

### 6.1.6 Discussion of Methods

Schultz and Flack (2005), amongst others, point out that it is very difficult to accurately determine the skin friction coefficient for rough surfaces. Most of the methods available for smooth surfaces, such as the Preston Tube Method and Bradshaw’s Method, cannot be used for rough surfaces. The Log Law Slope Method assumes that the log law is valid for the particular rough wall boundary layer, and introduces two additional degrees of freedom, $\varepsilon$ and $\Delta u^+$. Methods that rely on outer layer similarity, such as Hama’s Method and the Modified Hama Method, allow for more points in the boundary layer; however, they assume both the existence of the log law and the functional form of the law of the wake for rough wall flows.

The Total Stress Method can also be difficult to use on rough wall boundary layers, as it relies on a plateau in the Reynolds shear stress profile, which is often not clearly defined in the roughness sublayer, and the high measurement uncertainty in the roughness sublayer. The difficulties in using direct force methods are discussed in detail in Section 6.1.1.

A satisfactory method for the analysis of rough wall boundary layers is yet to be devised. Thus several methods of analysis were trialled as part of this study and critically assessed for their accuracy, reliability, and ability to produce realistic results.

### 6.2 Boundary Layer Velocity Profile Analysis

This section describes the various methods used to analyse the boundary layer velocity profiles. A comparison of the data for a smooth and a rough plate using two-dimensional LDV at 850 mm downstream from the leading edge of the test plate is given in Section 6.2.7. The MATLAB code used for each analysis method is given in Appendix C (on compact disc).
Before running the boundary layer analysis programs, raw near-wall velocity histograms at each \( y \) location were checked for the presence of wall noise. Several MATLAB routines were written to allow: identification of the noise band; removal of the wall noise; and re-calculation of the velocity statistics for the filtered data. The velocity statistics for \( y \) locations without wall noise were determined by the Dantec Dynamics software based on the number of samples per measurement point, using transit time averaging as discussed in Section 5.2.1.2.

The uncertainty values quoted in this section have been determined using repeatability tests. Ten replicate boundary layer profiles were taken for each case. In order to determine the 95% confidence interval for a single statistic, the standard error was multiplied by the two-tailed \( t \) value (\( t = 2.262 \)) for 9 degrees of freedom (Coleman & Steele 1995; Figliola & Beasley 2000; Schultz & Swain 1999).

6.2.1 Preston Tube Method

The initial experimental work undertaken in this study was carried out using a Pitot probe and static wall tapping connected to a Validyne pressure transducer for boundary layer velocity profile measurements. This lends itself to using the Preston tube method for determining the wall shear stress. This method is not appropriate for rough or fouled plate measurements, as it requires the Pitot tube to rest flat against a smooth wall and calibration data is only available for that case. All measurements using the Preston tube method were corrected for the effects of a transverse velocity gradient deflecting the streamlines by using an apparent shift in location of the centre of the probe using the method proposed by McKeon \textit{et al.} (2003) and described in Section 4.4.3.

Repeatability tests, as described in Section 6.2, were conducted on a smooth plate at Plug 3 (865 mm downstream from the leading edge of the test plate). The uncertainty in the measured variables was ± 0.3% for \( u^* \); and ± 0.7% for \( c_f \) using a 95% confidence interval, which is slightly better than the ± 3% suggested by Patel (1965).

6.2.2 Bradshaw’s Method

Bradshaw’s Method, described in Section 6.1.4.1, was used to analyse all of the smooth wall data in the present study. A MATLAB routine was written to automate the process for determining \( u^* \), \( \varepsilon \), and \( c_f \). The exact location of the wall is hard to determine using the LDV system and thus a wall origin correction was required even for the smooth plate measurements.
The program is iterative, and incrementally changes the value of the wall origin error, $\varepsilon$, and calculates $u^*$ using Bradshaw’s Method for each new $\varepsilon$ value. In each iteration the data is fitted to Spalding’s equation for the sublayer (Equation 2.13). The fit was limited to points in the region $y^+ < 30$. The rms deviation between Spalding’s equation and the experimental data was calculated for each iteration, and the iteration with the smallest rms value was taken as the best fit. An example plot demonstrating Bradshaw’s Method is given in Figure 6.4.

Repeatability tests, as described in Section 6.2, were conducted on a smooth plate at 850 mm from the leading edge of the test plate at a freestream velocity of 1.25 m/s. Two sets of tests were conducted: the first test consisted of one-dimensional measurements taken at a tilt angle of $\beta = 0.2^\circ$; the second test consisted of two-dimensional measurements taken at a tilt angle of $\beta = 5.5^\circ$ with the LDV aligned precisely to the tilted glass pane in the optical porthole. The uncertainty estimates for a 95% confidence interval are given in Table 6.1. The reason for the uncertainty in $\varepsilon$ being so high for the 1D measurements was that the origin correction applied was very small (0.04 ± 0.04 mm).

### 6.2.3 Log Law Slope Method

The Log Law Slope Method, described in Section 6.1.4.1, has been used by several researchers including Barton (2007), Schultz (1998), and Lewthwaite et al. (1985). To employ this technique successfully, the log law region must be consistently defined. Any points measured in the viscous sublayer must be excluded. Lewthwaite et al. (1985) suggest that the outside edge of the viscous sublayer be defined by Equation 6.18, i.e. $y^+ = 50$. However, as $u^*$ is not initially known, assuming a value of $U/30$ for $u^*$ is suggested, which gives the inner cut-off given by Equation 6.19.

The outer cut-off for the log law region also needs to be defined. Lewthwaite et al. (1985) suggests that the log law region occupies the innermost 10-15% of the boundary layer thickness,
\( \delta \). Schultz (1998) used 12.5% of \( \delta \) for the outer cut-off, and Barton (2007) used 10-15%, depending on the fit of the data.

\[
y = \frac{50\nu}{u} \quad \text{Equation 6.18}
\]

\[
y = 1500 \frac{\nu}{U} \quad \text{Equation 6.19}
\]

An iterative MATLAB routine was written to determine the boundary layer parameters. The first iteration used Equation 6.19 for the inner cut-off, where there were viscous sublayer points present, and 15% \( \delta \) for the outer cut-off. Without a virtual origin correction, the data in the log law region will most likely be curved rather than linear. A 2\textsuperscript{nd}-order least squares polynomial was fitted to the log law data and its second derivative determined. The value of \( \varepsilon \) was changed iteratively to achieve a vanishing second derivative of the polynomial fit to the data in the log law region. Once the virtual origin had been determined, the modified Clauser plot method described in Section 6.1.4.1 was applied to determine the boundary layer parameters using Equation 6.6 - Equation 6.9. For the subsequent iterations, the inner and outer cut-offs were taken as \( y^+ = 30 \) and \( y^+ = 300 \) respectively, using the \( u^* \) value determined in the previous iteration. Convergence was usually achieved after 3 iterations.

The Log Law Slope Method consistently gave unrealistic values for the equivalent sandgrain roughness, \( k_s \), which cast doubt over the values determined for \( u^* \) and \( c_f \). For example, for the rough plate with average sandgrain diameter of 1.5 mm (see Section 3.3) the Log Law Slope Method determined \( k_s \) to be in the order of 10 mm, whereas Perry and Li’s Method gave a value of 1.98 mm for the same boundary layer profile. The Log Law Slope Method also gave inconsistent values for the local skin friction coefficient at different Reynolds numbers, as shown in Section 6.2.7 where the different boundary layer analysis techniques are compared.

The MATLAB code described above was the process that gave the most consistent result. Many different alternative combinations of the cut-offs for the log law region were trialled, with unsatisfactory results. Data was also analysed by using the same \( \varepsilon \) value for different Reynolds numbers and by using Barton’s (2007) data for \( k_s \) for \( \varepsilon \).
Based on these unsatisfactory results, the Log Law Slope Method was not subjected to an uncertainty analysis, or retained for subsequent data analysis for either smooth or rough test plates.

### 6.2.4 Perry & Li’s Method

Perry and Li’s Method, detailed in Section 6.1.4.1, was used for all of the rough and biofouled surfaces tested in the present study. A MATLAB routine was written to automate the process for determining the virtual origin error, $\epsilon$, and wall shear velocity, $u^*$. The program creates a matrix of constant $u/U$ vs. $\ln(y/\delta^*)$ lines using $\kappa = 0.41$ and $\Pi = 0.55$ for $0.03 < u^* < 0.12$ using Equation 6.11. The approximate intersection point of the lines (see Figure 6.6) is found by calculating the standard deviation for each $u/U$ point. The point with a minimum standard deviation is chosen as the common intersection point.

The original experimental data is plotted as $u/U$ vs. $\ln(y/\delta^*)$. A first order polynomial is fitted through selected data points in the logarithmic region ($y/\delta^* < 0.9$). The $u/U$ coordinate for the common intersection point is compared with the experimental data at the computed $\ln(y/\delta^*)$ value and the absolute value of the difference is saved. The program iterates through $\epsilon = 0.0$ mm to $\epsilon = 2.0$ mm to find the virtual origin with the smallest absolute value of the difference. The optimised data is plotted, and the slope of the line is used to calculate $u^*$ using Equation 6.11.

The program iterates the above process five times, with a refined set of five constant $u/U$ vs. $\ln(y/\delta^*)$ lines calculated for $[u^*, 0.02, u^* - 0.01, u^*, u^* + 0.01, u^* + 0.02]$ (m/s) to give a better defined intersection point each time, based on the new $u^*$ value. The final iteration for the data set shown above is given in Figure 6.7. Convergence is usually found after three iterations.

Perry and Li (1990) found that a ±10% variation in the value of $\Pi$ caused only a ±3% variation in the value for $u^*$. This claim was tested by applying a ±10% variation in the value of $\Pi$ to the data for a rough plate at four different flow speeds measured at 850 mm downstream from the leading edge of the test plate. The results support this claim, with a ±4.4% variation in $u^*$ caused by a ±10% variation in the value of $\Pi$. However, the results for $\epsilon$, $c_\delta$, and $k_s$ were not so promising, with ±10%, ±8.7%, ±18.4% variations respectively.
Figure 6.6 First iteration of Perry & Li’s Method for determining $u^*$ and $\varepsilon$ ($u^*$ in m/s, and $\varepsilon$ in mm)

Figure 6.7 Final iteration of Perry & Li’s Method for determining $u^*$ and $\varepsilon$ ($u^*$ in m/s, and $\varepsilon$ in mm)
Repeatability tests, as described in Section 6.2, were conducted on a rough test plate at 850 mm from the leading edge of the test plate at a freestream velocity of 1.25 m/s. The results were analysed using Perry and Li’s Method, with $\Pi = 0.55$. The uncertainties in the measured variables using a 95% confidence interval were $\pm 0.2\%$ for $U$; $\pm 1.2\%$ for $\delta$; $\pm 0.5\%$ for $\theta$; $\pm 0.5\%$ for $\delta^*$; $\pm 3.3\%$ for $k$; $\pm 6.0\%$ for $c$; $\pm 0.6\%$ for $u^*$; $\pm 1.1\%$ for $c_\beta$; $\pm 1.1\%$ for $\Delta u$; $\pm 1.8\%$ for $\sqrt{\overline{u''^*/U}}$; $\pm 3.7\%$ for $\overline{u''^2}/u^2$; $\pm 2.9\%$ for $\overline{v''^2}/u^2$; and $\pm 5.1\%$ for $-u'v'/u^2$.

### 6.2.5 Hama’s Method

The theory behind Hama’s Method was described in Section 6.1.4.2. A MATLAB routine was written for Hama’s Method which used the virtual origin error calculated by fitting Spalding’s equation to the near-wall data, as explained in Section 6.2.2. Hama’s Method collapsed the data for different freestream velocities well, as shown in Figure 6.8. However, the wall shear velocity was significantly underestimated compared to other analysis methods, as shown in Section 6.2.7. Hence the method was not used to analyse any of the data reported in further chapters of this thesis, nor was a repeatability analysis done.
6.2.6 Total Stress Method

The Total Stress Method, detailed in Section 6.1.5, was used for all two-dimensional boundary layer profiles in the present study. Following Candries (2001) and Schultz and Flack (2005, 2007) the total stress was evaluated at the plateau of the Reynolds shear stress profile in the overlap region of the boundary layer to $y/\delta = 0.2$. A MATLAB routine was written to evaluate the viscous component of the total stress by determining the derivative of a curve fitted to $u$ vs. $y$. The maximum value in the region $0.06 < y/\delta < 0.2$ of the Reynolds shear stress profile was taken as the turbulent component of the total stress.

Repeatability tests were conducted for both a smooth and a rough plate to determine the 95% confidence intervals for the wall shear velocity and skin friction coefficient, as described in Section 6.2. The uncertainties for the smooth plate were ± 0.9% and ± 1.7% for $u^*$ and $c_f$ respectively. The uncertainties for the rough plate were ± 1.1% and ± 1.9% for $u^*$ and $c_f$ respectively. The peak on a smooth plate is much better defined than for a rough plate, as illustrated by Figure 6.9.

Figure 6.9 Reynolds shear stress profiles normalised by the freestream velocity: (a) smooth plate; (b) rough plate
6.2.7 Comparison of Methods

The methods detailed in Section 6.2 are now compared by applying them to two-dimensional boundary layer profiles taken on both the smooth (SP Lab) and rough (RP Lab) reference plates at 850 mm downstream of the leading edge of the test plate. As these measurements were taken in two-dimensions, the LDV was rotated approximately 5° towards the test plate as discussed in Section 5.2.2.3, with a nominal measurement volume penetration depth of 150 mm from the sidewall towards the centreline of the test plate. The Preston tube measurements reported here were taken at 865 mm downstream from the leading edge of the test plate at the centreline of the test plate. Measurements were taken at nominal freestream velocities of 1.00, 1.25, 1.75 and 2.00 m/s. These values were chosen to avoid the vibration of the UTAS Water Tunnel noted at a freestream velocity of 1.50 m/s, as well as providing a range of Reynolds numbers.

Table 6.2 Basic boundary layer parameters for a smooth plate at x = 850 mm (* Measurements taken at x = 865 mm using Pitot probe at plate centreline)

<table>
<thead>
<tr>
<th>U [m/s]</th>
<th>Re_l</th>
<th>Re_μ</th>
<th>δ [mm]</th>
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<td>PL</td>
<td>LLS</td>
<td>B</td>
</tr>
<tr>
<td>1.04</td>
<td>9.10E+05</td>
<td>2.71E+03</td>
<td>29.54</td>
<td>3.737</td>
<td>2.969</td>
<td>1.26</td>
<td>0.47</td>
<td>0.62</td>
<td>0.72</td>
</tr>
<tr>
<td>1.27</td>
<td>1.11E+06</td>
<td>3.33E+03</td>
<td>30.25</td>
<td>3.737</td>
<td>2.976</td>
<td>1.26</td>
<td>0.49</td>
<td>0.59</td>
<td>0.71</td>
</tr>
<tr>
<td>1.74</td>
<td>1.51E+06</td>
<td>4.36E+03</td>
<td>30.88</td>
<td>3.577</td>
<td>2.877</td>
<td>1.24</td>
<td>0.61</td>
<td>0.88</td>
<td>0.73</td>
</tr>
<tr>
<td>1.99</td>
<td>1.74E+06</td>
<td>4.98E+03</td>
<td>31.23</td>
<td>3.520</td>
<td>2.845</td>
<td>1.24</td>
<td>0.57</td>
<td>0.86</td>
<td>0.70</td>
</tr>
<tr>
<td>1.19*</td>
<td>1.05E+06</td>
<td>4.18E+03</td>
<td>43.93</td>
<td>4.896</td>
<td>3.976</td>
<td>1.23</td>
<td>-</td>
<td>-</td>
<td>0.79</td>
</tr>
<tr>
<td>2.00*</td>
<td>1.76E+06</td>
<td>7.63E+03</td>
<td>46.85</td>
<td>5.359</td>
<td>4.327</td>
<td>1.24</td>
<td>-</td>
<td>-</td>
<td>0.74</td>
</tr>
</tbody>
</table>

Boundary layer parameters for the smooth plate are presented in Table 6.2 and Table 6.3. A virtual origin error was applied to these profiles due to the rotation of the probe. Figure 6.10 shows the local skin friction coefficient, including error bars where the experimental uncertainty was calculated, at various test plate Reynolds numbers for each analysis method. The notation for each of the methods is as follows: Perry & Li’s Method (PL); Hama’s Method (H); Log Law Slope Method (LLS); Total Stress Method (TS); Bradshaw’s Method (B); Preston Tube Method (PT).

Perry & Li’s Method and the Total Stress Method agree within the experimental uncertainty. Hama’s Method significantly underestimates the local skin friction coefficient. The results for the Log Law Slope Method are inconsistent at different Reynolds numbers. The Preston Tube Method and Bradshaw’s Method display the same trend, but do not agree within the measurement uncertainty.
Table 6.3 Wall friction velocity and local skin friction coefficient for a smooth plate using different analysis methods at $x = 850$ mm (* Measurements taken at $x = 865$ mm using Pitot probe at plate centreline)

<table>
<thead>
<tr>
<th>$U$ [m/s]</th>
<th>Preston Tube Method</th>
<th>Perry &amp; Li’s Method</th>
<th>Hama’s Method</th>
<th>Log Law Slope Method</th>
<th>Total Stress Method</th>
<th>Bradshaw’s Method</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$u^*$ [m/s]</td>
<td>$c_f$ [x10^3]</td>
<td>$u^*$ [m/s]</td>
<td>$c_f$ [x10^3]</td>
<td>$u^*$ [m/s]</td>
<td>$c_f$ [x10^3]</td>
</tr>
<tr>
<td>1.038</td>
<td>-</td>
<td>0.039 2.81</td>
<td>0.034 2.12</td>
<td>0.042 3.26</td>
<td>0.040 2.93</td>
<td>0.044 3.60</td>
</tr>
<tr>
<td>1.274</td>
<td>-</td>
<td>0.047 2.72</td>
<td>0.041 2.11</td>
<td>0.050 3.12</td>
<td>0.047 2.70</td>
<td>0.053 3.42</td>
</tr>
<tr>
<td>1.736</td>
<td>-</td>
<td>0.064 2.69</td>
<td>0.054 1.91</td>
<td>0.077 3.90</td>
<td>0.064 2.70</td>
<td>0.070 3.21</td>
</tr>
<tr>
<td>1.992</td>
<td>-</td>
<td>0.071 2.53</td>
<td>0.061 1.88</td>
<td>0.087 3.85</td>
<td>0.071 2.52</td>
<td>0.079 3.16</td>
</tr>
<tr>
<td>1.192*</td>
<td>0.047</td>
<td>3.14</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>2.000*</td>
<td>0.076</td>
<td>2.88</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

Figure 6.10 Local skin friction coefficient for smooth plate using different analysis methods at $x = 850$ mm
The smooth plate boundary layer profiles are plotted in Figure 6.11 in inner coordinates (i.e. normalised by $u^*$ and $\nu$). For Hama’s Method and the Total Stress Method the virtual origin error, $\varepsilon$, calculated using Bradshaw’s Method has been used to determine $y^+$. Bradshaw’s Method and the Preston Tube Method follow the smooth wall log law given by Equation 2.17. The Log Law Slope Method is again inconsistent. Perry and Li’s Method and the Total Stress Method collapse the data well, but lie above the smooth wall log law. The data for Hama’s Method sits significantly above the smooth plate log law, and above all of the other profiles. To fit a smooth wall log law profile, the intercept would be required to be $14 < C < 15$, instead of the $C = 5$ which is typically used. Tachie et al. (2000) reported a similar result using Hama’s Method for their shallow open channel flow data, and concluded that correlations that implicitly fix the wake strength at $\Pi = 0.55$ were not suitable for the open channel boundary layer being studied.

The smooth wall data is plotted in velocity defect form in Figure 6.12, and the Reynolds streamwise normal stress, wall-normal normal stress, and shear stress are plotted in Figure 6.13, Figure 6.14, and Figure 6.15 respectively, normalised by the wall shear stress. The same trends are observed in these plots.
Figure 6.12 Smooth wall boundary layer profiles in velocity defect form at $x = 850$ mm and $U = 1.25$ m/s, 2.00 m/s for different analysis methods: Perry and Li’s Method (PL); Hama’s Method (H); Log Law Slope Method (LLS); Total Stress Method (TS); Bradshaw’s Method (B); Preston Tube Method (PT).

Figure 6.13 Smooth wall streamwise Reynolds normal stress, normalised by the wall shear stress, at $x = 850$ mm and $U = 1.25$ m/s, 2.00 m/s for different analysis methods: Perry and Li’s Method (PL); Hama’s Method (H); Log Law Slope Method (LLS); Total Stress Method (TS); Bradshaw’s Method (B).
Figure 6.14 Smooth wall wall-normal Reynolds normal stress, normalised by the wall shear stress, at \( x = 850 \) mm and \( U = 1.25 \) m/s, 2.00 m/s for different analysis methods: Perry and Li’s Method (PL); Hama’s Method (H); Log Law Slope Method (LLS); Total Stress Method (TS); Bradshaw’s Method (B)

Figure 6.15 Smooth wall Reynolds shear stress, normalised by the wall shear stress, at \( x = 850 \) mm and \( U = 1.25 \) m/s, 2.00 m/s for different analysis methods: Perry and Li’s Method (PL); Hama’s Method (H); Log Law Slope Method (LLS); Total Stress Method (TS); Bradshaw’s Method (B)
Boundary layer parameters for the rough plate are presented in Table 6.4 and Table 6.5. Note that the Log Law Slope Method did not produce a viable result for the boundary layer profile at a freestream velocity of 1.747 m/s. The $\varepsilon$, $k_s$, $u^*$, and $c_f$ values calculated using the Log Law Slope Method were much higher than those calculated using any of the other methods.

Table 6.4 Basic boundary layer parameters for a rough plate at $x = 850$ mm: Perry and Li’s Method (PL); Log Law Slope Method (LLS)

<table>
<thead>
<tr>
<th>$U$ [m/s]</th>
<th>$Re_l$</th>
<th>$Re_\theta$</th>
<th>$\delta$ [mm]</th>
<th>$\delta^*$ [mm]</th>
<th>$\theta$ [mm]</th>
<th>$H$</th>
<th>$\varepsilon$ [mm] PL</th>
<th>$\varepsilon$ [mm] LLS</th>
</tr>
</thead>
<tbody>
<tr>
<td>1.01</td>
<td>8.84E+05</td>
<td>3.91E+03</td>
<td>33.08</td>
<td>6.559</td>
<td>4.406</td>
<td>1.49</td>
<td>0.36</td>
<td>0.63</td>
</tr>
<tr>
<td>1.26</td>
<td>1.10E+06</td>
<td>4.91E+03</td>
<td>33.67</td>
<td>6.643</td>
<td>4.455</td>
<td>1.49</td>
<td>0.32</td>
<td>1.05</td>
</tr>
<tr>
<td>1.75</td>
<td>1.52E+06</td>
<td>6.74E+03</td>
<td>32.91</td>
<td>6.642</td>
<td>4.417</td>
<td>1.50</td>
<td>0.28</td>
<td>-</td>
</tr>
<tr>
<td>2.01</td>
<td>1.75E+06</td>
<td>7.69E+03</td>
<td>32.80</td>
<td>6.551</td>
<td>4.378</td>
<td>1.50</td>
<td>0.31</td>
<td>1.00</td>
</tr>
</tbody>
</table>

Table 6.5 Wall friction velocity, local skin friction coefficient, and equivalent sandgrain roughness for a rough plate using different analysis methods at $x = 850$ mm

<table>
<thead>
<tr>
<th>$U$ [m/s]</th>
<th>Perry &amp; Li’s Method</th>
<th>Hama’s Method</th>
<th>Log Law Slope Method</th>
<th>Total Stress Method</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$u^*$ $x10^3$ [m/s]</td>
<td>$c_f$ [mm]</td>
<td>$k_s$ [m]</td>
<td>$u^*$ $x10^3$ [m/s]</td>
</tr>
<tr>
<td>1.01</td>
<td>0.056 6.31 1.91</td>
<td>0.051 5.11 1.34</td>
<td>0.068 9.13 4.06</td>
<td>0.053 5.62 1.58</td>
</tr>
<tr>
<td>1.26</td>
<td>0.071 6.25 1.93</td>
<td>0.071 6.35 1.98</td>
<td>0.121 18.44 11.57</td>
<td>0.066 5.43 1.56</td>
</tr>
<tr>
<td>1.75</td>
<td>0.098 6.27 1.99</td>
<td>0.091 5.38 1.59</td>
<td>- - -</td>
<td>0.091 5.48 1.64</td>
</tr>
<tr>
<td>2.01</td>
<td>0.112 6.26 1.98</td>
<td>0.103 5.21 1.51</td>
<td>0.181 16.32 10.20</td>
<td>0.105 5.45 1.63</td>
</tr>
</tbody>
</table>

Figure 6.16 shows the local skin friction coefficient at the various test plate Reynolds numbers tested for each analysis method. The results for the Log Law Slope Method are inconsistent at different Reynolds numbers and are up to 195% higher than the values calculated using Perry & Li’s Method. The differences between the other methods range from 2-19%, and are outside the measurement uncertainty.

The rough plate boundary layer profiles are plotted in Figure 6.17 in inner coordinates. For Hama’s Method and the Total Stress Method, $y^+$ has been calculated using the $\varepsilon$ determined using Perry and Li’s Method. Figure 6.17 clearly demonstrates the difference between the Log Law Slope Method, and the other three methods trialled for rough plates.
Chapter 6 - Data Analysis

Figure 6.16 Local skin friction coefficient for a rough plate using different analysis methods at \( x = 850 \text{ mm} \)

Figure 6.17 Rough wall boundary layer profiles in inner coordinates at \( x = 850 \text{ mm and } U = 1.25 \text{ m/s, 2.00 m/s for different analysis methods: Perry and Li’s Method (PL); Hama’s Method (H); Log Law Slope Method (LLS); Total Stress Method (TS)\)
Figure 6.18 Rough wall boundary layer profiles in velocity defect form at $x = 850$ mm and $U = 1.25$ m/s, 2.00 m/s for different analysis methods: Perry and Li’s Method (PL); Hama’s Method (H); Log Law Slope Method (LLS); Total Stress Method (TS).

Figure 6.19 Rough wall streamwise Reynolds normal stress, normalised by the wall shear stress, at $x = 850$ mm and $U = 1.25$ m/s, 2.00 m/s for different analysis methods: Perry and Li’s Method (PL); Hama’s Method (H); Log Law Slope Method (LLS); Total Stress Method (TS).
Figure 6.20 Rough wall normal Reynolds normal stress, normalised by the wall shear stress, at $x = 850 \text{ mm}$ and $U = 1.25 \text{ m/s}, 2.00 \text{ m/s}$ for different analysis methods: Perry and Li’s Method (PL); Hama’s Method (H); Log Law Slope Method (LLS); Total Stress Method (TS).

Figure 6.21 Rough wall Reynolds shear stress, normalised by the wall shear stress, at $x = 850 \text{ mm}$ and $U = 1.25 \text{ m/s}, 2.00 \text{ m/s}$ for different analysis methods: Perry and Li’s Method (PL); Hama’s Method (H); Log Law Slope Method (LLS); Total Stress Method (TS).
The rough plate boundary layer profiles are plotted in velocity defect form in Figure 6.18. The profiles collapse for all methods in the outer region of the boundary layer, with the exception of the Log Law Slope Method. There is a noticeable difference between the remaining three methods in the inner region of the boundary layer.

The normalised Reynolds streamwise normal stress, wall-normal normal stress, and shear stress for the rough plate are plotted in Figure 6.19, Figure 6.20, and Figure 6.21 respectively. The same trends are observed in these plots.

Based on these comparisons, Hama’s Method and the Log Law Slope Method were not used in any subsequent data analysis. Hama’s Method significantly underestimated the wall friction velocity for the smooth plate, and did not provide consistent results for the rough plate. The Log Law Slope Method did not provide consistent solutions for either the smooth or rough test plates and significantly overestimated most of the boundary layer parameters for the rough plate.

Bradshaw’s Method was used as the primary analysis method for all subsequent smooth wall boundary layer profiles. Perry and Li’s Method was used as the primary data analysis method for all subsequent rough wall boundary layer profiles, including for biofouled surfaces. Where two-dimensional measurements were made, the Total Stress Method was used to check the wall friction velocity and local skin friction coefficient values.

As demonstrated in Figure 6.22, Perry and Li’s Method underestimated the wall friction velocity for the smooth plate, with a difference of up to 12% between Perry and Li’s Method and Bradshaw’s Method. An attempt was made to force Perry and Li’s Method to fit the smooth wall log law, with the results shown in Table 6.6. The $\Pi$ values stated for Bradshaw’s Method in Table 6.6 were calculated at the maximum deviation from the log law line. The first column under Perry and Li’s Method presents results with $\Pi = 0.55$ as Perry and Li (1990) intended. For (2) Perry and Li’s Method was solved using the $\varepsilon$ value calculated from Bradshaw’s Method. This reduced the difference between Bradshaw’s Method and Perry and Li’s Method to a maximum of 8% for $U = 1.038$ m/s and 0.2% for $U = 1.736$ m/s. For (3) the wake strength used in Perry and Li’s Method was varied to give the same $u^*$ value as determined by Bradshaw’s Method. This resulted in $\Pi$ values that were up to 49% higher than those calculated from the Bradshaw’s Method profiles. Finally, in (4), Perry and Li’s Method was evaluated using the same $\Pi$ value as determined in Bradshaw’s Method. This resulted in $u^*$ values that were up to 10% higher than those calculated using Bradshaw’s Method.
Figure 6.22, plotted for $U = 1.736 \text{ m/s}$ and $1.992 \text{ m/s}$, clearly demonstrates that using Perry and Li’s Method as the authors intended is not within the measurement uncertainty of Bradshaw’s Method. Using the $\Pi$ value from Bradshaw’s Method (4) is also outside the measurement uncertainty. However, (2) and (3) provide results that are within the measurement uncertainty.

It should be noted that Perry and Li (1990) devised the method solely for use on rough plates, and they collapsed their smooth wall data using another method. The trials reported here were done in an attempt to provide a common analysis method for smooth and rough surfaces to allow different surfaces to be objectively compared. The fact that Perry and Li’s Method does not give good results for a smooth surface provides support for the notion suggested by Krogstad et al. (1992), that the wake strength, $\Pi$, should be allowed to vary. Nevertheless, Perry and Li’s Method was used to analyse all rough wall boundary layer profiles. Since the same method was used for all rough wall boundary layer profiles, the results may be compared objectively. Comparisons between smooth and rough wall profiles should be viewed with some caution, as different methods have been used to calculate the results.

### Table 6.6 Perry and Li’s Method on a smooth plate

<table>
<thead>
<tr>
<th>$U$ [m/s]</th>
<th>Bradshaw’s Method</th>
<th>Perry and Li’s Method</th>
</tr>
</thead>
<tbody>
<tr>
<td>$u^*$ [m/s]</td>
<td>$\varepsilon$ [mm]</td>
<td>$\Pi$</td>
</tr>
<tr>
<td>$u^*$ [m/s]</td>
<td>$\varepsilon$ [mm]</td>
<td>$\Pi$</td>
</tr>
<tr>
<td>$u^*$ [m/s]</td>
<td>$\varepsilon$ [mm]</td>
<td>$\Pi$</td>
</tr>
<tr>
<td>$u^*$ [m/s]</td>
<td>$\varepsilon$ [mm]</td>
<td>$\Pi$</td>
</tr>
<tr>
<td>1.04</td>
<td>0.044</td>
<td>0.72</td>
</tr>
<tr>
<td>1.27</td>
<td>0.053</td>
<td>0.71</td>
</tr>
<tr>
<td>1.74</td>
<td>0.070</td>
<td>0.73</td>
</tr>
<tr>
<td>1.99</td>
<td>0.079</td>
<td>0.70</td>
</tr>
</tbody>
</table>
6.3 TOTAL DRAG ANALYSIS

Drag measurements were obtained using the floating element force balance described in Section 4.4.4. The following sections detail the theory behind the analysis method (Section 6.3.1), along with an assessment of the virtual origin of the boundary layer over the test plate (Section 6.3.2), and repeatability tests to determine the measurement uncertainty for both smooth and rough test plates (Section 6.3.3).

6.3.1 Drag Analysis Theory

The drag coefficient for a flat plate of width $b$ and length $l$ is defined by (Schlichting 1955):

$$D = C_D \rho lb \frac{U^2}{2}, \quad C_D = \frac{D}{\frac{1}{2} \rho U^2 b l}$$

Equation 6.20

The relationship between drag coefficient and Reynolds number for hydraulically smooth conditions is given by Equation 6.21 (Schlichting 1955).
\[
C_D = 0.074 \text{Re}^{0.37} \quad \text{for} \quad 5 \times 10^4 < \text{Re} < 10^7
\]  

Equation 6.21

Equation 6.21 is based on the assumption that the origin of the turbulent boundary layer is at the leading edge of the plate. This was not the case for the present tests: here a fully turbulent boundary layer was already established on the tunnel wall upstream of the test plate on which drag measurements were obtained. This problem can be addressed by postulating a turbulent boundary layer in zero pressure gradient growing from a virtual origin at distance \(l_1\) upstream from the leading edge of the test plate as shown in Figure 6.23. This is the point at which a continuously turbulent boundary layer would need to have been initiated to achieve the measured thickness of the boundary layer over the test plate.

The relationship between boundary layer thickness and distance from the origin of the boundary layer is given by (Schlichting 1955):

\[
\delta = 0.37/\text{Re}^{0.37} \]

Equation 6.22

The virtual origin of the boundary layer can be determined by varying \(l\) in Equation 6.22 until the boundary layer thickness, \(\delta\), matches a measured boundary layer thickness at a known location. In the present study the virtual origin was estimated from velocity profile measurements on the tunnel wall at \(x = -38\) mm, i.e. slightly upstream of the test plate leading edge (see Section 6.3.2).

The situation is more complex for rough test plates, as would be expected. The relationship between equivalent sandgrain roughness and rough wall skin friction coefficient for the hydraulically rough flow regime is given by Equation 6.23 (Schlichting 1955).
Equation 6.23

\[
C_D = \left( 1.89 + 1.62 \log \frac{l}{k_s} \right)^{-2.5} \quad \text{for } 10^2 < l/k_s < 10^6
\]

Equation 6.23 can be used to determine the equivalent sandgrain roughness for the rough test plates based on the measured drag coefficient (including the correction for the virtual origin of the boundary layer). The flaw in this method is that it assumes that the roughness is consistent for the test plate and the region upstream of the test plate, which is not the case for rough test plates. Thus the equivalent sandgrain roughness values obtained are likely to overestimate the actual roughness of the test plates as the method assumes that the smooth Perspex wall region upstream of the test plate is also rough, which is not the case.

For the smooth plate case only, the theoretical drag on the test plate alone could be obtained using Equation 6.24, where the drag coefficients for the region upstream of the test plate to the virtual origin \((l_1)\) and the region including the test plate and the virtual origin \((l_2)\) are determined using the respective lengths and Equation 6.21.

\[
D_{\text{theor}} = \rho b \frac{U^2}{2} \left( C_{D2} l_2 - C_{D1} l_1 \right)
\]

Equation 6.24

The smooth plate drag coefficients determined in this manner are slightly lower than those with the virtual origin distance included, as shown in Figure 6.24. They also have higher measurement uncertainty.

Unfortunately, this technique cannot be applied to rough plates because the equivalent sandgrain roughness, \(k_s\), must be determined from the relative roughness, \(k_s/l\), based on the total length, \(l\), from the virtual origin to the trailing edge of the test plate, as given in Equation 6.23. Therefore, for consistency, the drag coefficients for both smooth and rough test plates have been determined by including the virtual origin distance.

For both the smooth and rough test plate cases there will be errors in the virtual origin and plate drag estimates due to the effects of suction through the gap at the leading edge of the test plate. The step change in wall roughness at the leading edge of the test plate will reduce the reliability of equivalent sandgrain roughness estimates and further increases the uncertainty of drag estimates for that case. The uncertainty is estimated from repeatability tests, as detailed in Section 6.3.3.
6.3.2 Assessment of Virtual Origin

Schlichting’s equations for the turbulent boundary layer, given in Equation 6.21 and Equation 6.22, are based on the distance from the leading edge of a flat plate. It is assumed that transition from laminar to turbulent flow occurs at the leading edge and that the boundary layer there is of zero thickness (Preston 1957). This does not occur in the present situation for two reasons: (1) the plate is not isolated in the flow as it forms part of the roof of the water tunnel, with a small gap between the upstream Perspex roof and the test plate; and (2) the boundary layer is tripped 600 mm upstream of the leading edge of the test plate using a 3 mm diameter brass welding rod.

With regard to the tripping of the boundary layer, Preston (1957) found that the drag of the trip wire and the transition process both increase the momentum thickness of the boundary layer. He proposes that where the upstream $Re_\theta > 320$, transition will occur very close to the wire. If $Re_\theta < 320$ he suggests that $Re_d (= Ud/\nu) = 600$ to cause transition at the wire, where $d$ is the diameter of the trip wire. In the present study, $Re_d$ ranges from approximately 2600 – 5300. It is not possible to obtain measurements upstream of the trip wire to determine the upstream $Re_\theta$, however, based on $Re_d$ it was assumed that the transition occurs at the trip rod. The virtual origin of the boundary layer, i.e. where the momentum thickness is zero, occurs some distance upstream from the trip wire.
Schultz (1998) found that his smooth plate boundary layers were thicker than predicted by Schlichting’s equation, which was attributed to the tripping of the boundary layer at the leading edge. Schultz determined the apparent origin of the boundary layer to be 386 mm upstream from the leading edge by solving for $x$ based on the measured boundary layer thickness, $\delta$.

To estimate the virtual origin of the boundary layer, mean velocity profiles were taken at several different flow speeds just upstream of the leading edge of the test plate using the LDV system to provide a reference point with a known boundary layer thickness. The profiles were taken at 38 mm upstream from the leading edge of the test plate to avoid the perturbation in the boundary layer caused by the gap between the roof of the working section and the test plate. The data is given in Table 6.7. Equation 6.22 was rearranged and used to estimate the virtual origin ($l_1$) of the boundary layer, which is also given in Table 6.7. The variation of the virtual origin distance with the freestream velocity is given in Figure 6.25. Based on these results, if the boundary layer were not tripped the origin would be approximately 1.2 m upstream of the leading edge of the test plate. This is 0.6 m upstream of the boundary layer trip, and located partway into the contraction. This is illustrated in Figure 6.25.

To verify the use of 1.2 m as the virtual origin distance, the analysis was run on a clean rough plate (RP Lab). Three scenarios were considered: using the average virtual origin distance of 1.2 m; using a varied virtual origin depending on freestream velocity calculated from the line of best fit given in Figure 6.25; and using the boundary layer trip rod as the virtual origin (600 mm upstream of the leading edge). The results are given in Figure 6.26 and Figure 6.27 for the total drag coefficient and the equivalent sandgrain roughness respectively and summarised in Table 6.8. These plots indicate that the drag coefficient and the equivalent sandgrain roughness are both quite sensitive to the virtual origin distance. The parameters were solved using the average $k_s$ for the five highest Reynolds numbers.

### Table 6.7 Boundary layer parameters at x = 38 mm for determination of virtual origin

<table>
<thead>
<tr>
<th>Pump Speed [rpm]</th>
<th>$U$ [m/s]</th>
<th>$Re_\theta$</th>
<th>$\delta$ [mm]</th>
<th>$\delta^*$ [mm]</th>
<th>$\theta$ [mm]</th>
<th>$H$</th>
<th>$u^*$ [m/s]</th>
<th>$c_f$</th>
<th>$\Pi$</th>
<th>$\varepsilon$ [mm]</th>
<th>$l_1$ [m]</th>
</tr>
</thead>
<tbody>
<tr>
<td>200</td>
<td>0.68</td>
<td>1610</td>
<td>26.23</td>
<td>3.40</td>
<td>2.71</td>
<td>1.26</td>
<td>0.031</td>
<td>0.004102</td>
<td>0.18</td>
<td>0.42</td>
<td>1.05</td>
</tr>
<tr>
<td>300</td>
<td>1.01</td>
<td>2290</td>
<td>24.71</td>
<td>3.25</td>
<td>2.59</td>
<td>1.25</td>
<td>0.045</td>
<td>0.003869</td>
<td>0.21</td>
<td>0.41</td>
<td>1.08</td>
</tr>
<tr>
<td>400</td>
<td>1.33</td>
<td>2970</td>
<td>25.68</td>
<td>3.14</td>
<td>2.53</td>
<td>1.24</td>
<td>0.058</td>
<td>0.003724</td>
<td>0.17</td>
<td>0.40</td>
<td>1.21</td>
</tr>
<tr>
<td>500</td>
<td>1.64</td>
<td>3530</td>
<td>24.91</td>
<td>3.04</td>
<td>2.46</td>
<td>1.23</td>
<td>0.070</td>
<td>0.003611</td>
<td>0.17</td>
<td>0.41</td>
<td>1.22</td>
</tr>
<tr>
<td>600</td>
<td>1.93</td>
<td>4180</td>
<td>24.80</td>
<td>3.00</td>
<td>2.47</td>
<td>1.22</td>
<td>0.081</td>
<td>0.003507</td>
<td>0.15</td>
<td>0.42</td>
<td>1.27</td>
</tr>
</tbody>
</table>
The estimated roughness height from the particle size distribution was 1.5 mm. The photogrammetry results for the clean rough plate, presented in Section 7.2, gave $R_t$ values of 1.7 – 2.2 mm. Boundary layer profiles for the clean rough plate at $x = 850$ mm, presented in Chapter 7, indicate $k_s$ values of 2.8 – 3.0 mm.

The relationship between the virtual origin distance and the equivalent sandgrain roughness height was also investigated by varying the virtual origin systematically. The results indicate a linear relationship, with $k_s$ increasing with virtual origin distance, as shown in Figure 6.28. The virtual origin for any type of test plate should exist at the same distance upstream of the leading edge of the test plate, as the upstream surface does not change. Thus, provided that a consistent virtual origin is used, results for different types of test plate can be confidently compared. Based on the roughness values presented here, a varied virtual origin was chosen, based on the freestream velocity as given in Figure 6.25, for all subsequent data analysis.

![Figure 6.25 Influence of streamwise velocity on distance from leading edge to the virtual origin](image)

The estimated roughness height from the particle size distribution was 1.5 mm. The photogrammetry results for the clean rough plate, presented in Section 7.2, gave $R_t$ values of 1.7 – 2.2 mm. Boundary layer profiles for the clean rough plate at $x = 850$ mm, presented in Chapter 7, indicate $k_s$ values of 2.8 – 3.0 mm.

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### Table 6.8 Summary of $k_s$ and $C_D$ for various virtual origins

<table>
<thead>
<tr>
<th>VO = 600 mm (trip rod)</th>
<th>VO = 1200 mm</th>
<th>VO = varied</th>
</tr>
</thead>
<tbody>
<tr>
<td>$k_s$ [mm]</td>
<td>1.68</td>
<td>2.62</td>
</tr>
<tr>
<td>$C_D$ (measured)</td>
<td>0.0086</td>
<td>0.0089</td>
</tr>
</tbody>
</table>
Figure 6.26 Influence of virtual origin on total skin friction coefficient at different test plate Reynolds number

Figure 6.27 Influence of virtual origin on equivalent sandgrain roughness at different test plate Reynolds number
Repeatability tests were undertaken on both smooth and rough test plates to determine the 95% confidence interval for each variable. As with the boundary layer profile uncertainty analyses, the 95% confidence interval for a single statistic was determined by multiplying the standard error with the two-tailed t value for the relevant degree of freedom, depending on how many runs were done (Coleman & Steele 1995; Figliola & Beasley 2000; Schultz & Swain 1999).

Uncertainty results for 10 repeatability tests done on a smooth plate are given in Table 6.9 and Figure 6.29. The tests were done without changing the gap around the test plate. It is clear both from the tabulated results, and from Figure 6.29 that the force balance gives more precise results at higher flow speeds, which was also observed by Barton et al. (2007). Repeatability tests were also conducted where the gap around the test plate was varied for each run. The results were very similar with those given in Table 6.9 and are thus not shown here.

Similarly, ten repeatability tests were conducted for a rough test plate, with the results given in Table 6.10 and Figure 6.30. The mean drag coefficient and 95% confidence intervals for clean smooth and rough test plates are given in Figure 6.31, based on ten repeatability tests.
Table 6.9 Uncertainty of drag variables at various flow speeds for a smooth plate, based on 10 tests

<table>
<thead>
<tr>
<th>Pump Speed [rpm]</th>
<th>Approximate Velocity [m/s]</th>
<th>Mean $C_D$</th>
<th>$\pm % C_D$</th>
<th>Mean Raw Drag Force [N]</th>
</tr>
</thead>
<tbody>
<tr>
<td>150</td>
<td>0.52</td>
<td>0.00517</td>
<td>14.8</td>
<td>0.41</td>
</tr>
<tr>
<td>200</td>
<td>0.69</td>
<td>0.00361</td>
<td>12.7</td>
<td>0.32</td>
</tr>
<tr>
<td>250</td>
<td>0.86</td>
<td>0.00406</td>
<td>12.9</td>
<td>0.75</td>
</tr>
<tr>
<td>300</td>
<td>1.03</td>
<td>0.00393</td>
<td>4.7</td>
<td>1.05</td>
</tr>
<tr>
<td>350</td>
<td>1.20</td>
<td>0.00379</td>
<td>3.3</td>
<td>1.37</td>
</tr>
<tr>
<td>400</td>
<td>1.37</td>
<td>0.00364</td>
<td>2.3</td>
<td>1.66</td>
</tr>
<tr>
<td>450</td>
<td>1.54</td>
<td>0.00373</td>
<td>2.4</td>
<td>2.33</td>
</tr>
<tr>
<td>500</td>
<td>1.69</td>
<td>0.00371</td>
<td>1.7</td>
<td>2.86</td>
</tr>
<tr>
<td>550</td>
<td>1.85</td>
<td>0.00366</td>
<td>1.5</td>
<td>3.41</td>
</tr>
<tr>
<td>600</td>
<td>2.00</td>
<td>0.00364</td>
<td>1.9</td>
<td>4.02</td>
</tr>
<tr>
<td>650</td>
<td>2.08</td>
<td>0.00365</td>
<td>1.3</td>
<td>4.45</td>
</tr>
</tbody>
</table>

Figure 6.29 Ten repeatability tests for drag coefficient on a smooth test plate

Equation 6.21
Table 6.10 Uncertainty of drag variables at various flow speeds for a rough plate, based on 4 tests

<table>
<thead>
<tr>
<th>Pump Speed [rpm]</th>
<th>Approximate Velocity [m/s]</th>
<th>Mean $C_D$</th>
<th>± % $C_D$</th>
<th>Mean Raw Drag Force [N]</th>
</tr>
</thead>
<tbody>
<tr>
<td>150</td>
<td>0.14</td>
<td>0.0098</td>
<td>10.8</td>
<td>0.80</td>
</tr>
<tr>
<td>200</td>
<td>0.59</td>
<td>0.0097</td>
<td>5.7</td>
<td>1.31</td>
</tr>
<tr>
<td>250</td>
<td>0.86</td>
<td>0.0104</td>
<td>2.5</td>
<td>1.78</td>
</tr>
<tr>
<td>300</td>
<td>1.03</td>
<td>0.0096</td>
<td>2.5</td>
<td>2.41</td>
</tr>
<tr>
<td>350</td>
<td>1.20</td>
<td>0.0096</td>
<td>1.8</td>
<td>3.06</td>
</tr>
<tr>
<td>400</td>
<td>1.36</td>
<td>0.0097</td>
<td>0.9</td>
<td>3.47</td>
</tr>
<tr>
<td>450</td>
<td>1.53</td>
<td>0.0098</td>
<td>1.5</td>
<td>4.73</td>
</tr>
<tr>
<td>500</td>
<td>1.68</td>
<td>0.0097</td>
<td>1.2</td>
<td>5.91</td>
</tr>
<tr>
<td>550</td>
<td>1.83</td>
<td>0.0098</td>
<td>1.2</td>
<td>7.31</td>
</tr>
<tr>
<td>600</td>
<td>1.98</td>
<td>0.0098</td>
<td>1.1</td>
<td>8.64</td>
</tr>
<tr>
<td>650</td>
<td>2.06</td>
<td>0.0098</td>
<td>1.0</td>
<td>9.35</td>
</tr>
</tbody>
</table>

Figure 6.30 Ten repeatability tests for drag coefficient on a rough test plate
This chapter has been devoted to the determination of wall friction velocity and the local and total skin friction coefficients. A satisfactory method for the analysis of rough wall boundary layer velocity profiles is yet to be devised. Thus a critical appraisal of the available methods was undertaken, resulting in Bradshaw’s Method being selected to analyse smooth wall boundary layer profiles and Perry and Li’s Method being selected to analyse rough wall boundary layer profiles. The Total Stress Method will be used to confirm results in subsequent chapters.

The measurement of total drag force on the test plates was also discussed, and is not without its shortcomings. However, a consistent approach has been devised for both smooth and rough surfaces that allows the results to be compared.

Uncertainty estimates for 95% confidence intervals have also been presented for all methods that were selected for subsequent data analysis.
Chapter 7 – Measurements on Live Biofilms

7 MEASUREMENTS ON LIVE BIOFILMS

Chapter 7 presents total drag measurements, roughness characterisation using photogrammetry, and one- and two-dimensional boundary layer profiles obtained using the Laser Doppler Velocimeter for both smooth and rough plates in the clean and biofouled condition.

7.1 DESCRIPTION OF TEST PLATES

The data set includes:

- 4 x smooth substrates:
  - Clean laboratory reference plate, SP Lab;
  - Fouled smooth plate incubated for approximately 12 months, SP1 F2;
  - Fouled smooth plate incubated for two weeks, SP2 F3; and
  - Fouled smooth plate incubated for approximately 16 weeks, SP1 F6; and

- 4 x rough substrates:
  - Clean laboratory reference plate, RP Lab;
  - Fouled rough plate incubated for approximately 11 months, RP1 F1;
  - Fouled rough plate incubated for two and a half weeks RP1 F4; and
  - Fouled rough plate incubated for approximately 15 weeks, RP2 F5.

The fouled plates were incubated in Pond No.1 at Tarraleah over different periods of time as detailed in Table 7.1. The deployment locations of Transition 4 and Pond No.1 are described in Section 3.4.2 and Section 3.4.3 respectively. The test plates were transported from Pond No.1 at Tarraleah to the laboratory in Hobart, approximately a 1.5 hour journey, in a container filled with water from the canal. Care was taken to minimise the time between removing the test plate from Tarraleah and installing it in the water tunnel for testing, particularly time where the test plate was exposed to air during the photography.

The water level in Tarraleah No.1 Canal over the deployment period is given in Figure 7.1. There were several periods when the canal was not running full due to system requirements and consequently the test plates would have been exposed. These occurred in March/April 2008 for 17 days; June/July 2008 for 14 days; August 2008 for 4 days; September/October 2008 for 24 days; December 2008 for 7 days; January 2009 for 2 days; February/March 2009 for 29 days for a system outage; and May 2009 for 4 days.
Table 7.1 Large test plate deployment schedule: Pond No.1 (P1); Transition 4 (T4)

<table>
<thead>
<tr>
<th>Date</th>
<th>RP1</th>
<th>RP2</th>
<th>SP1</th>
<th>SP2</th>
</tr>
</thead>
<tbody>
<tr>
<td>07/02/2008</td>
<td>-</td>
<td>-</td>
<td>Deployed at P1</td>
<td>Deployed at T4</td>
</tr>
<tr>
<td>21/02/2008</td>
<td>Deployed at P1</td>
<td>Deployed at T4</td>
<td>P1</td>
<td>T4</td>
</tr>
<tr>
<td>27/01/2009</td>
<td>Removed (F1)</td>
<td>T4</td>
<td>P1</td>
<td>T4</td>
</tr>
<tr>
<td>30/01/2009</td>
<td>Deployed clean at P1</td>
<td>T4</td>
<td>Removed (F2)</td>
<td>Moved to P1 (clean)</td>
</tr>
<tr>
<td>13/02/2009</td>
<td>P1</td>
<td>T4</td>
<td>Deployed clean at P1</td>
<td>Removed (F3)</td>
</tr>
<tr>
<td>17/02/2009</td>
<td>Removed (F4)</td>
<td>Moved to P1 (clean)</td>
<td>P1</td>
<td>-</td>
</tr>
<tr>
<td>01/06/2009</td>
<td>-</td>
<td>Removed (F5)</td>
<td>P1</td>
<td>-</td>
</tr>
<tr>
<td>04/06/2009</td>
<td>-</td>
<td>-</td>
<td>Removed (F6)</td>
<td>-</td>
</tr>
</tbody>
</table>

Figure 7.1 Water levels in Tarraleah No.1 Canal: (1) Deployed clean plates; (2) RP1 F1 and SP1 F2 taken to lab, deployed clean plates; (3) SP2 F3 and RP1 F4 taken to lab, deployed clean plates; (4) RP2 F5 and SP1 F6 taken to lab
7.1.1 RP1 F1

RP1 was installed in the clean condition on 21/02/2008 in Pond No.1. It was removed on 27/01/2009 after approximately 11 months incubation. Three very distinct regions of fouling were apparent on the plate by visual inspection upon removal from Pond No.1, as shown in Figure 7.2. Cleaner, fluffier fouling was present in the bottom 150 mm; this was followed by dark, very gelatinous fouling in the next 150 mm; then dark, thin and clumpy fouling occurred in the top section of the plate (Perkins 2009a). Samples were taken from each region of fouling and identified under a microscope, with the results given in Table 7.2.

Perkins (2009a) identified two likely reasons for the differentiation in fouling, the first being varied water levels in Pond No.1 due to the normal operation of the Tarraleah Power Station. Assuming that the bottom of the plate had been under water for the majority of the time, this would mean that the fouling is older at the bottom of the test plate. The second reason for differentiation in the fouling on the plate could be variation of light attenuation with depth.

Table 7.2 Description of fouling on RP1 F1 27/02/2009

<table>
<thead>
<tr>
<th>Location on Plate</th>
<th>Description of Fouling</th>
</tr>
</thead>
<tbody>
<tr>
<td>Top (remainder)</td>
<td>Majority of the biofilm consisted of live <em>T. flocculosa</em> with groups of living stalking <em>G. tarraleahae</em>, and a large colony of small round <em>Chlorophytes</em>. The biofilm trapped a lot of rubbish material (i.e. leaves, organic matter, dirt, gravel).</td>
</tr>
<tr>
<td>Middle (~ 150 mm)</td>
<td>Few living cells identified. The fouling contains much more rubbish material than the top layer, and many dead <em>T. flocculosa</em> cells. Dead raphid diatoms and <em>Cymbella</em> were also evident. Quite a bit of stalk material, but little to indicate what produced it.</td>
</tr>
<tr>
<td>Bottom (~ 150 mm)</td>
<td>The majority of the fouling consisted of the diatom <em>Cymbella</em>, which is a stalking diatom like <em>G. tarraleahae</em>. Few <em>T. flocculosa</em> cells were present. Some <em>Chlorophytes</em> and other raphid diatoms were present, along with lots of stalk material. This layer was almost clear of rubbish material.</td>
</tr>
</tbody>
</table>
Chapter 7 – Measurements on Live Biofilms

7.1.2 SP1 F2

SP1 was installed in the clean condition on 07/02/2008 in Pond No.1. It was removed on 30/01/2009 after approximately 12 months incubation. SP1 F2 had much thicker fouling present on the bottom of the test plate (Figure 7.5). The lack of fouling on the leading edge (left) of the plate, evident in Figure 7.4, is likely due to the movement of long reeds on the bank of Pond No.1 slightly upstream from the plate installation brackets.

Samples of the biofilm were taken from the top, middle, and bottom of the test plate for species identification under a microscope. The composition of the biofilm was found to be uniform, even though the fouling was of different thicknesses. The biofilm consisted of live *T. flocculosa* and a lot of stalking material, some of which was still occupied by *G. tarraleahae* and *Cymbella*. A significant amount of organic rubbish was present, along with dead diatom cells such as...
Brachysira, G. tarraleahae, and Cymbella. The fouling was observed to be quite similar to the fouling found on the top section of RP1 F1 (Perkins 2009a).

### 7.1.3 SP2 F3

SP2 was installed in the clean condition on 30/01/2009 in Pond No.1. It was removed on 13/02/2009 after 2 weeks incubation. The fouling was observed to consist of a light slime, which covered the entire test plate (see Figure 7.6 and Figure 7.7), consisting mainly of *T. flocculosa* chains held together by bacterial fouling as shown in Figure 7.8 (Perkins 2009a). Stalking material was also present with few *G. tarraleahae* and *Cymbella* cells attached. No streamers were evident.

![Figure 7.6 SP2 F3 immediately after removal from Pond No.1 on 13/02/2009 (flow is left to right)](image1)

![Figure 7.7 Close up of SP2 F3 immediately after removal from Pond No.1 on 13/02/2009](image2)

### 7.1.4 RP1 F4

RP1 was reinstalled in Pond No.1 in the clean condition on 30/01/2009. It was removed on 17/02/09 after 2.5 weeks incubation. The fouling was observed to consist of a light slime, as shown in Figure 7.10, with very fine streamers up to 20 mm long present over the entire test plate, as seen in Figure 7.11. The fouling was heavily dominated by *T. flocculosa* with some bacterial fouling and empty fouling stalks, illustrated in Figure 7.9 (Perkins 2009a).
7.1.5 RP2 F5

RP2 was installed in the clean condition on 17/02/2009 in Pond No.1. It was removed on 01/06/2009 after approximately 15 weeks incubation. It was noted that the fouling was much thicker on the bottom of the test plate. This is most likely due to canal operation.

The dominant fouling species present was *T. flocculosa*, which is shown in Figure 7.14 under x5 magnification. There was also some other organic matter caught in the fouling. Other non-stalking species in the biofilm included *Synedra* and *Cylindrocystis*, which are shown in Figure 7.15. Some stalking diatom species were also noted, including *G. tarraleahae* and *Cymbella* which are also visible in Figure 7.15 (Perkins 2009a).
7.1.6 SP1 F6

SP1 was installed in the clean condition on 13/02/2009 in Pond No.1. It was removed on 04/06/2009 after approximately 16 weeks incubation. The fouling was not evenly distributed across the test plate, with much heavier fouling occurring on the bottom 300 mm, as shown in Figure 7.16, most likely due to canal operation.

The dominant fouling species present was *T. flocculosa*, which is shown in Figure 7.18 under x5 magnification. There was also a significant amount of organic matter caught in the fouling, and a larger presence of stalking diatoms such as *Gomphonema* sp. as shown in Figure 7.19 under x10 magnification (Perkins 2009a).
7.2 ROUGHNESS CHARACTERISATION

The rough and biofouled test plates were photographed to obtain three dimensional models of the surfaces. The photographs were taken as soon as the plates were retrieved from the field, just prior to being placed in the water tunnel for testing.

The process used to obtain the models using photogrammetry is explained in Section 4.6. A sample cross section at $Y = 160$ mm, which is located approximately in the middle of the viewing window, is given in Appendix A1 for each test plate (Figure A.1 - Figure A.8) along with tabulated roughness and statistical information (Table A.1 - Table A.8). Models were obtained at the viewing windows as defined by Figure 4.17. Note that windows A, B and C are
located on the bottom half of the plate, capturing the heavier fouling. The fouling found at the top of the plates was generally thinner, and was captured at windows D and E.

Barton (2007) found the $R_t$ parameter (maximum peak-to-valley height for a cross section) to correlate best with the equivalent sandgrain roughness height determined from water tunnel measurements. The $R_t$ values for each test plate are summarised in Table 7.3. The data indicate that the fouling was more uniform over SP2 F3 and RP1 F4 than RP1 F1 and SP1 F2, which show higher roughness values for the bottom half of the plate. This was expected from observation of the fouling on each test plate, as described in Section 7.1.

RP2 in the clean state generally has higher $R_t$ values than RP2 F5, which may indicate that the biofilm is actually smoothing the surface out. However, the high uncertainty in the fouled plate roughness measurements must be taken into account, as the results are within the measurement uncertainty of $\pm 8.5\%$ (95% confidence interval) for $R_t$ on biofouled surfaces.

### 7.3 BOUNDARY LAYER PROFILES

#### 7.3.1 Measurement and Analysis Procedures

Measurements were undertaken at freestream velocities of 1.00 m/s, 1.25 m/s, 1.75 m/s, and 2.00 m/s at 70 locations in the boundary layer. The process for obtaining the boundary layer profiles is detailed in Section 4.5.2. The smooth plate boundary layer profiles were analysed using Bradshaw’s Method (Section 6.2.2) and the rough plate boundary layer profiles, including biofouled specimens, were analysed using Perry and Li’s Method (Section 6.2.4). The two-dimensional boundary layer profiles were also analysed using the Total Stress Method (Section 6.2.6). The near-wall points were checked for wall noise, which was removed using the

<table>
<thead>
<tr>
<th>Window</th>
<th>RP1 Clean</th>
<th>RP2 Clean</th>
<th>RP1 F1</th>
<th>SP1 F2</th>
<th>SP2 F3</th>
<th>RP1 F4</th>
<th>RP2 F5</th>
<th>SP1 F6</th>
</tr>
</thead>
<tbody>
<tr>
<td>A</td>
<td>1.73</td>
<td>2.59</td>
<td>3.31</td>
<td>2.04</td>
<td>0.17</td>
<td>1.87</td>
<td>2.67</td>
<td>2.25</td>
</tr>
<tr>
<td>B</td>
<td>-</td>
<td>2.63</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>2.34</td>
<td>1.95</td>
</tr>
<tr>
<td>C</td>
<td>2.21</td>
<td>-</td>
<td>3.35</td>
<td>1.62</td>
<td>0.16</td>
<td>1.94</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>D</td>
<td>-</td>
<td>2.36</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>2.29</td>
<td>0.24</td>
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<tr>
<td>E</td>
<td>1.82</td>
<td>2.24</td>
<td>2.10</td>
<td>0.44</td>
<td>0.24</td>
<td>1.79</td>
<td>2.04</td>
<td>0.19</td>
</tr>
</tbody>
</table>

Table 7.3 $R_t$ values [mm] at windows A, B, C, D and E
procedure outlined in Section 6.2. The rationale behind the methods and the processes used to determine the boundary layer parameters are discussed in detail in Chapter 6.

### 7.3.1.1. One-Dimensional Boundary Layer Profiles

One-dimensional boundary layer profiles were obtained for the clean lab test plates and the first four biofouled plates (F1 - F4). Boundary layer profiles were taken at several locations in the streamwise direction for each test plate using the LDV system described in Section 4.4.5 in one-dimension.

### 7.3.1.2. Two-Dimensional Boundary Layer Profiles

Two-dimensional boundary layer profiles were obtained for the clean lab test plates and the final two biofouled plates (F5 and F6). Boundary layer profiles were taken at $x = 850$ mm using the LDV system described in Section 4.4.5 in coincidence mode. The laser was carefully aligned to pass orthogonally through the optical glass porthole described in Section 5.2.2.3 to within 0.1 mm across approximately 100 mm diameter using a dial gauge attached to the head of the LDV probe.

### 7.3.2 Boundary Layer Development

The development of the boundary layer over both a smooth and a rough plate, SP Lab and RP Lab, was investigated by taking profiles at 100 mm, 250 mm, 500 mm, 750 mm and 850 mm downstream from the leading edge of the test plate. Profiles were also taken upstream of the leading edge of the test plate, at $x = -38$ mm, and were presented in Section 6.3.2. Note that the leading edge of the test plate has been given the coordinate of $x = 0$ mm.

Previous studies on rough plate boundary layers (Brzek et al. 2007; Schultz & Flack 2007; Schultz & Swain 1999) have been set up with the test surface in the midplane of the wind or water tunnel being used, or with the test surface immediately downstream of a trip device (Krogstad et al. 1992). In the present study, a trip device is located 600 mm upstream from the leading edge of the test plate. The surface between the trip rod and the test plate is the smooth Perspex roof of the water tunnel. Thus for rough test plates, the flow is encountering a step change in surface roughness from smooth to rough, a situation which was investigated by Antonia and Luxton (1971).
The local skin friction coefficient is plotted against the momentum thickness Reynolds number at various streamwise distances in Figure 7.20 for SP Lab and Figure 7.21 for RP Lab. The local skin friction coefficient collapses for $x = 500$, 750, and 850 mm for SP Lab. Results for RP Lab at $x = 750$ mm and 850 mm almost collapse.

*Figure 7.20 Local skin friction coefficient for SP Lab at various downstream locations*
The velocity defect parameter, \( G \) (Clauser 1954), is plotted against distance downstream from the leading edge in Figure 7.22 for both SP Lab and RP Lab. Both cases plateau at the trailing edge of the test plate, indicating that boundary layer equilibrium has been attained.

The theoretical equilibrium values for \( G \) are given by the Nash equilibrium relation (Nash 1965):

\[
G = 1.6\sqrt{\Pi + 1.81} - 1.7
\]

where \( \Pi \) in this case refers to the pressure gradient parameter and is defined as \( \Pi = \frac{\delta^*}{\tau_w} \frac{dp}{dx} \).

The pressure gradient in the UTAS Water Tunnel is slightly negative, due to the growth of the boundary layer over the test plate and tunnel walls. The velocity defect parameter is plotted against the pressure gradient parameter in Figure 7.23, along the equilibrium values determined from Equation 7.1.
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Figure 7.22 Variation of velocity defect parameter, $G$, with distance downstream from the leading edge of the test plate for SP Lab and RP Lab

Figure 7.23 Variation of velocity defect parameter, $G$, with pressure gradient parameter, $\Pi$
The variation of displacement and momentum thickness, $\delta^*$ and $\theta$, with distance from the leading edge of the test plate for RP Lab is plotted in Figure 7.24. Both $\delta^*$ and $\theta$ increase almost linearly with $x$, which was also observed by Antonia and Luxton (1971). The variation of the shape factor with distance from the leading edge for both SP Lab and RP lab is given in Figure 7.25. Antonia and Luxton (1971) found that the shape factor for their rough wall increased from the smooth wall value and flattened out in the region $25 \text{ (in)} < x < 60 \text{ (in)}$, after which a slow decrease in $H$ was observed. No plateau is observed in Figure 7.25 for RP Lab, although $H$ does decrease at $x = 850 \text{ mm}$ from the value at $x = 750 \text{ mm}$.

Mean velocity defect profiles for SP Lab and RP Lab at varying different distances from the leading edge are plotted in Figure 7.26 and Figure 7.27, respectively. The SP Lab profiles for $x = 500$, 750, and 850 mm collapse, again demonstrating that the smooth plate boundary layer has reached equilibrium by 500 mm downstream from the leading edge of the test plate. The RP Lab profiles for $x = 750 \text{ mm}$ and $x = 850 \text{ mm}$ collapse for the majority of $(y+\epsilon)/\delta$, supporting the notion of self preservation of the boundary layer by the trailing edge of the test plate, and the furthest downstream measurement location of $x = 850 \text{ mm}$.
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Figure 7.25 Variation of shape factor, $H$, with $x$ for SP Lab and RP Lab at different freestream velocities

Figure 7.26 Velocity defect profiles for SP Lab at different distances from the leading edge of the test plate
Streamwise turbulence intensity profiles for SP Lab are given in Figure 7.28 and Figure 7.29. The profiles collapse in both the inner and outer regions of the boundary layer for \( x \geq 500 \) mm. Similarly, profiles for RP Lab are plotted in Figure 7.30 and Figure 7.31. The profiles for \( x = 750 \) mm and 850 mm almost collapse in the outer region of the boundary layer. The logarithmic profile shows that the intensity in the near-wall region decreases with increasing distance downstream, which was also observed by Antonia and Luxton (1971). They found that the profiles in the near-wall region collapse at sufficient distance downstream from the step change from smooth to rough. This does not occur for the RP Lab profiles, as the turbulence intensity for \( x = 850 \) mm is less than that for \( x = 750 \) mm in the near-wall region.

Antonia and Luxton (1971) also considered the wall normal turbulence intensity and the Reynolds shear stress. They found that the \( v' \) and \( u'v' \) components collapsed at similar downstream distances as \( u' \). Such measurements were not able to be taken at multiple streamwise locations in the current study due to the refraction issues discussed in Section 5.2.2.3.

In conclusion, the boundary layer over the smooth plate reaches equilibrium by 500 mm downstream from the leading edge of the test plate. The boundary layer over the rough plate appears to be close to equilibrium by the final measuring station of \( x = 850 \) mm.
Figure 7.28 Streamwise turbulence intensity profiles for SP Lab at different distances from the leading edge of the test plate.

Figure 7.29 Streamwise turbulence intensity profiles in semi-logarithmic form for SP Lab at different distances from the leading edge of the test plate.
Figure 7.30 Streamwise turbulence intensity profiles for RP Lab at different distances from the leading edge of the test plate.

Figure 7.31 Streamwise turbulence intensity profiles in semi-logarithmic form for RP Lab at different distances from the leading edge of the test plate.
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7.3.3 Boundary Layer Parameters

Boundary layer parameters for each test plate are presented in Appendix A2 in Table A.9 - Table A.20. The results presented in each table were analysed using either Bradshaw’s Method or Perry & Li’s Method, depending on the roughness of the surface. The wake strength, $\Pi$, was determined by fitting Equation 7.2 to the data, as suggested by White (1991). Equation 7.2 gave a good fit to the data at the trailing edge of the test plates (i.e. at $x \sim 850$ mm), but was not a good fit near the leading edge of the test plates where the boundary layer was not in equilibrium.

$$\frac{U - u}{u^*} = -\frac{1}{\kappa} \ln \left( \frac{y^* + \varepsilon}{\delta} \right) + \frac{2\Pi}{\kappa} \cos^2 \left( \frac{\pi}{2} \frac{y^* + \varepsilon}{\delta} \right)$$  \hspace{1cm} \text{Equation 7.2}

The roughness function, $\Delta u^*$, was determined by fitting Equation 2.26 to the data using $\kappa = 0.41$ and $C = 5.0$. The uncertainty for the tabulated data is given in Chapter 6.

The setup of the LDV system meant that measurements could only be made on the top 300 mm of each test plate. Thus many of the measurements presented here were taken in regions of light fouling. This is particularly significant for SP1 F6, which had very thick fouling on the bottom half of the plate and very light fouling on the top half, as shown in Figure 7.16. SP2 F3 was also very lightly fouled. Both of these test plates were analysed using both Bradshaw’s Method and Perry and Li’s Method. The reasons for using both methods are discussed in the following sections.

Dimensionless boundary layer mean velocity profiles for each plate are presented in Figure 7.32 at $x = 850$ mm and $U = 2.00$ m/s. As mentioned in Chapter 2, plots of $u/U$ vs. $(y^*+\varepsilon)/\delta$ do not collapse for varying degrees of roughness in turbulent flow. A greater skin friction coefficient correlates to a greater velocity deficit, as was shown in Figure 2.13. Figure 7.32 clearly shows that the plates with a rough substrate have a greater velocity deficit than those with a smooth substrate.

The rough plate profiles (RP Lab, RP1 F1, RP1 F4 and RP2 F5) appear to collapse in Figure 7.32. However, there is more separation between the smooth plate profiles. This is expected for SP1 F2, which exhibited much higher $c_f$ values than the other smooth plates. However, the SP2 F3 profile appears to contradict the local skin friction coefficient values, as it exhibits a higher velocity deficit than SP Lab, but a lower local skin friction coefficient.
Figure 7.32 Dimensionless boundary layer mean velocity profiles at $x = 850$ mm and $U = 2.00$ m/s

Figure 7.33 Comparison of local skin friction coefficients for one-dimensional boundary layer profiles at $x \sim 850$ mm for various $Re_\theta$

The local skin friction coefficients for the one-dimensional boundary layer profiles at $x = 850$ mm are given in Figure 7.33. The fouled plate with a rough substrate that was incubated for 11
months had the highest skin friction coefficient. The fouled plate with a smooth substrate that was incubated for 12 months had a significantly higher skin friction coefficient than the clean plate value.

The local skin friction coefficients for the two dimensional boundary layer profiles at \( x = 850 \text{ mm} \), determined using the primary analysis method (Bradshaw’s Method for the smooth plates or Perry and Li’s Method for the rough plates) and the Total Stress Method, are given in Figure 7.34 and Table 7.4. The local skin friction coefficients for SP Lab and SP1 F6 agree within the measurement uncertainty. At higher Reynolds numbers, the local skin friction coefficient for RP2 F5 is higher than for RP Lab for both analysis methods.

![Figure 7.34 Local skin friction coefficient for two-dimensional boundary layer profiles at \( x = 850 \text{ mm} \): Bradshaw’s Method (B); Perry and Li’s Method (PL); Total Stress Method (TS)](image)

*Figure 7.34 Local skin friction coefficient for two-dimensional boundary layer profiles at \( x = 850 \text{ mm} \): Bradshaw’s Method (B); Perry and Li’s Method (PL); Total Stress Method (TS)*
**7.3.4 Inner Coordinates**

Boundary layer profiles, normalised by $u^*$, are given in Appendix A3 for each of the test plates. For clarity purposes, the boundary layer profiles, normalised by $u^*$, for each test plate are compared separately for the one- and two-dimensional data in Figure 7.35 and Figure 7.36, respectively.

The one-dimensional boundary layer profiles for SP Lab, given in Figure A.9, show the development of the wake component with streamwise location. The values for the wake strength are given in Table A.9 although the fit to Equation 7.2 for measurement locations near the leading edge of the test plate was poor. Several points were located in the viscous sublayer for SP Lab, and the data exhibited a good fit to Spalding’s equation (Equation 2.13). Only a minor virtual origin correction was required to fit the data to the smooth wall log law. Two-dimensional boundary layer profiles for SP Lab are given in Figure A.11 and tabulated in Table A.10. No points were obtained in the viscous sublayer, due to the necessity of the laser to be...
titled to obtain near-wall measurements for the wall normal velocity component. The profiles are a good fit to the smooth wall log law.

The one- and two-dimensional boundary layer profiles for RP Lab are given in Figure A.10 and Figure A.12, respectively. Both figures clearly show the shift in velocity profile associated with increasing wall roughness, with $\Delta u^+$ values up to 10.6. The measured variables for RP Lab are given in Table A.11 and Table A.12 for the one- and two-dimensional measurements, respectively. All of the profiles for RP Lab were in the hydraulically rough flow regime, as indicated by the roughness Reynolds number $Re_k$. The equivalent sandgrain roughness for RP Lab ranged from 1.2 – 3.3 mm. This is the local sandgrain roughness, and is valid for the region local to the boundary layer profile.

RP1 F1 exhibited only slightly rougher behaviour than RP Lab, as shown in Figure 7.35, which was unexpected given the large amount of fouling present. However, due to the orientation of the test plate in the water tunnel, the heavily fouled bottom section of the test plate (see Figure 7.2) was located on the opposite side to the LDV system, meaning that the measuring volume for the LDV was positioned on the region of lighter fouling. The roughness function was consistently higher than RP Lab, as was the sandgrain roughness which ranged from 3.7 – 5.5 mm. The boundary layer profiles for RP1 F1 are given in Figure A.13 and the data is tabulated in Table A.13.

SP1 F2 was also a fairly heavily fouled test plate, as shown in Figure 7.4. The boundary layer parameters and profiles are given in Table A.14 and Figure A.14, respectively. There is a significant shift in the velocity profile compared to the clean smooth plate data, which is clearly shown Figure 7.35. The roughness function, $\Delta u^+$, ranged from 3.8 – 6.8, and the equivalent sandgrain roughness ranged from 0.6 – 0.9 mm. The flow was in the smooth-rough transition regime for all streamwise positions and all Reynolds numbers.

SP2 F3 had a much more uniform spread of fouling than SP1 F2, as shown in Figure 7.6, and was a very light slime film. Analysing the boundary layer profiles for this plate proved to be difficult, as it lay in between the rough and smooth plate methods. When analysed using Bradshaw’s method, the profiles collapsed with the smooth wall log law as shown in Figure A.15, but exhibited a higher wake strength than SP Lab, as shown in Figure 7.35. The skin friction coefficient was less than the smooth plate skin friction coefficient, which may indicate that the biofilm is actually beneficial in terms of drag reduction. Skin friction coefficient results
from Perry and Li’s method were also lower than results for the clean smooth plate. Perry and Li velocity profiles fell in the vicinity of the smooth wall log law, as shown in Figure A.16.

Boundary layer profiles for RP1 F4 are given in Figure A.17. RP1 F4 consisted of a much lighter layer of fouling than RP1 F1 and consequently had a smaller roughness function, $\Delta u^*$, which ranged from 9.1 - 11.7. The equivalent sandgrain roughness was greater than that for the smooth rough plate (RP Lab), and ranged from 3.1 – 4.1 mm.

Boundary layer profiles for RP2 F5 are given in Figure A.18. RP2 F5 exhibited only slightly rougher behaviour than RP Lab, as shown in Figure 7.36, which was unexpected given the large amount of fouling present. This was again due to the orientation of the test plate in the water tunnel.

SP1 F6 (Figure 7.16) had a very heterogeneous distribution of fouling, and the boundary layer profiles were obtained in the region of light fouling. Both Bradshaw’s and Perry and Li’s Methods were applied and the boundary layer profiles for both methods are given in Figure A.19. Data analysed using Bradshaw’s Method shows good collapse with the smooth wall log law. The Perry and Li profiles for $U = 1.00$ m/s and 1.25 m/s collapse with the SP Lab profile, which indicates that the flow is in the hydraulically smooth regime and that the very fine layer of biofouling does not impede the flow. However, the Perry and Li profiles for $U = 1.75$ m/s and 2.00 m/s sit slightly below the SP Lab profile, which suggests that the biofouling may have had a roughening affect at the higher freestream velocities. The data is shown scaled using variables analysed using Bradshaw’s Method in all subsequent figures.
Figure 7.35 Comparison of 1D boundary layer profiles, normalised using $u^*$, at $x = 850$ mm and $U = 2.00$ m/s (for SP2 F3, $B =$ Bradshaw’s Method, $PL =$ Perry and Li’s Method)

Figure 7.36 Comparison of 2D boundary layer profiles, normalised by $u^*$, at $x = 850$ mm and $U = 2.00$ m/s
7.3.5 Outer Coordinates

Boundary layer profiles in velocity defect form are presented in Appendix A4 in Figure A.20 - Figure A.27 for each test plate. Only the profiles obtained with the LDV system configured in one-dimension are shown for SP Lab and RP Lab, as the results were very similar for the two-dimensional profiles. The profiles all show good collapse and are generally a good fit to Equation 7.2 which was used to determine the wake strength, $\Pi$. The only exception was for some of the profiles taken near the leading edge of the plate where the fit was not as good, illustrated in Figure A.21a, Figure A.23a and Figure A.25a.

Profiles analysed using both Bradshaw’s Method and Perry and Li’s Method are presented in Figure A.24 for SP2 F3 at 850 mm downstream from the leading edge of the test plate. The results collapse well for different freestream velocities, when analysed using the same method. However, the two methods do not agree with each other, particularly in the inner region of the boundary layer due to differing $\varepsilon$ and $u^*$ values.

The profiles for all test plates are compared in Figure 7.37 in traditional form, and in semi-logarithmic form in Figure 7.38. The differences between the test plates are not obvious in Figure 7.37; however, the data in semi-logarithmic form clearly shows differences between the smooth and rough plate profiles, particularly in the near-wall region. The profiles which were analysed using Perry and Li’s Method show reasonable collapse, even in the inner region of the boundary layer. However, the three profiles that were analysed using Bradshaw’s Method, SP Lab, SP2 F3 and SP1 F6 do not collapse with the rest of the data; nor do they collapse against each other. This is due to differences in the analysis methods, as the SP2 F3 profiles analysed using Perry and Li’s Method do collapse with other data analysed using Perry and Li’s Method.
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Figure 7.37 Comparison of velocity defect profiles at $x = 850$ mm and $U = 2.00$ m/s

Figure 7.38 Comparison of velocity defect profiles in semi-logarithmic form at $x = 850$ mm and $U = 2.00$ m/s (for SP2 F3, $B =$ Bradshaw’s Method, $PL =$ Perry and Li’s Method)
7.3.6 Roughness Plots

Boundary layer profiles scaled using the equivalent sandgrain roughness, $k_s$, are given in Appendix A5. The plates with a rough substrate were all in the hydraulically rough regime and were fitted to Equation 2.25 using $B_N = 8.5$. SP1 F2 was in the smooth-rough transition regime. At $x = 850$ mm, most of the SP2 F3 profiles were in the smooth regime, when analysed using either methods the one exception was the case for $U = 2.00$ m/s (Perry and Li’s Method) which had a roughness Reynolds number of 6.6 (smooth-rough transition regime).

The profiles are compared at $x = 850$ mm and $U = 2.00$ m/s in Figure 7.39. This plot demonstrates the differences in equivalent sandgrain roughness between each test plate in a different form. Most of the plates with a rough substrate had similar roughness Reynolds numbers, thus they lie closely together in Figure 7.39. RP1 F1 had a much higher $k_s$ value of 5.53 mm and a correspondingly higher roughness Reynolds number; thus the profile is further down Nikuradse’s curve. On the other hand, SP1 F2 had a significantly lower $k_s$ value of 0.78 mm, placing it on the cusp of the smooth-rough transition/hydraulically rough regimes according to the classification given in Table 2.1, and sliding it further up Nikuradse’s curve. SP2 F3 had a very small $k_s$ value of 0.09 mm and was only just in the smooth-rough transition regime; it thus sits the highest on Nikuradse’s curve in Figure 7.39.

![Figure 7.39 Comparison of boundary layer profiles scaled using $k_s$ for hydraulically rough and smooth-rough transition regime test plates](image-url)
Turbulence Intensity

Streamwise and wall-normal turbulence intensity profiles for each plate are presented in Appendix A6 in Figure A.34 - Figure A.43. The smooth plate data of Klebanoff (1955) is shown for comparison in each plot. The streamwise data for SP Lab, given in Figure A.34 and Figure A.35, is slightly elevated above Klebanoff’s data in the region $0 < (y+\varepsilon)/\delta < 0.8$, and departs from Klebanoff’s data for $(y+\varepsilon)/\delta > 0.8$; this is likely due to differences in the freestream turbulence intensity.

The streamwise turbulence intensity profiles, obtained for all test plates, are compared in Figure 7.40 at $x = 850$ mm and $U = 2.00$ m/s. The turbulence intensity profiles for the rough plates collapse in the outer part of the boundary layer. The peak for the fouled rough plate profiles is further into the boundary layer than for the clean rough plate. The turbulence intensity for SP2 F3 and SP1 F6, the lightly fouled smooth plates, were slightly elevated above the turbulence intensity for the SP Lab, indicating that the fine biofilm layer is causing the mean velocity to fluctuate more than for a smooth surface. It should be noted that both Bradshaw’s Method and Perry and Li’s Method produce similar results for the streamwise turbulence intensity profile, as it is scaled using the freestream velocity rather than the wall shear velocity. The turbulence intensity for SP1 F2, the heavily fouled smooth plate, was in between the smooth plate and rough plate turbulence intensities.

Wall-normal turbulence intensity profiles were obtained for the measurements with the LDV system configured in two-dimensions, and are given in Figure 7.41. The results for each substrate type collapse well in the outer region of the boundary layer. The smooth wall wall-normal turbulence intensity data of Klebanoff (1955) is shown for comparison in Figure 7.41. The wall-normal turbulence intensity profiles for SP Lab and SP1 F6 show a departure from Klebanoff’s data, particularly in the region $(y+\varepsilon)/\delta > 0.8$, similar to the streamwise turbulence intensity profiles.
Figure 7.40 Comparison of streamwise turbulence intensity profiles at $x = 850$ mm and $U = 2.00$ m/s

Figure 7.41 Comparison of wall-normal turbulence intensity profiles at $x = 850$ mm and $U = 2.00$ m/s
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7.3.8 Reynolds Normal Stresses

The streamwise Reynolds normal stress for the measurements taken with the LDV system set-up in one-dimension are plotted in Figure 7.42 for \( x = 850 \text{ mm} \) and \( U = 2.00 \text{ m/s} \). The data indicates that the roughness affects only the inner region of the boundary layer for \( (y+\epsilon)/\delta < 0.2 \), with the exception of SP3 F3. The results also show good agreement with the smooth wall data of Schultz and Flack (2005). Regardless of which analysis method is used for SP2 F3, the results do not show good collapse with the results from other test plates.

The Reynolds normal stresses for all two-dimensional measurements at all freestream velocities are given in Appendix A7 in Figure A.44 - Figure A.51, scaled using \( u^* \). All of the test plates exhibit Reynolds number dependence when plotted in inner coordinates, typified by the lack of collapse at different freestream velocities. The effect of increasing the Reynolds number is a shift away from the wall when plotted in inner variables. This was also noted by Brzek et al. (2007). The profiles show reasonable collapse when plotted in outer variables, i.e. \( \overline{u'x'} \) vs. \( (y+\epsilon)/\delta \).

Figure 7.42 Normalised streamwise Reynolds normal stress profiles for all surfaces at \( x = 850 \text{ mm} \) and \( U = 2.00 \text{ m/s} \) (Schultz and Flack at \( Re_\theta = 9050 \), for SP2 F3, B = Bradshaw’s Method, PL = Perry and Li’s Method)
Profiles at similar $Re_\theta$ in inner coordinates are given in Figure 7.43 for the streamwise Reynolds normal stress. The smooth wall one-dimensional data for SP Lab is also given in Figure 7.43 to demonstrate the behaviour of the streamwise Reynolds normal stress on a smooth plate in the near-wall region, as the two-dimensional SP Lab measurements did not reach the near-wall region as already discussed. The lightly fouled smooth plate, SP1 F6, exhibits smooth wall streamwise Reynolds normal stress characteristics.

The high near-wall peak in the smooth wall profiles in Figure 7.43 is not apparent in the rough wall profiles, which is indicative of profiles in the hydraulically rough regime (Ligrani & Moffat 1986). Ligrani and Moffat (1986), Schultz and Flack (2007), and Brzek et al. (2007) note that the smooth wall peak in the streamwise Reynolds normal stress is due to viscous effects, and is associated with streamwise vortical structures. The roughness elements break up the streamwise vortices; as hydraulically rough flow is approached, the frictional drag of the wall is dominated by form drag and viscous effects become negligible, even very near the wall. The streamwise Reynolds normal stress has a lower peak for RP2 F5 than for RP Lab.

Ligrani and Moffat (1986) identified broad flat humps, with maximum values at $250 < y^+ < 400$, in plots of $\overline{u'^2}$ vs. $y^+$ that are characteristic of hydraulically rough flow and regions where the production of longitudinal turbulence energy is important. These humps are evident in the RP Lab and RP2 F5 profiles in Figure 7.43. Ligrani and Moffat suggest that the hump is a result of the ejection-sweep cycle differences between a smooth and a rough surface. In rough wall flows, larger amounts of low speed fluid ($u' < 0, \nu' > 0$) may be pushed further from the wall to collide with high speed fluid ($u' > 0, \nu' < 0$) in larger quantities than for smooth wall flows, resulting in the region of greatest mixing being moved further from the wall and spread over a greater portion of the layer. This is explored in more detail in Section 7.3.10, which presents a quadrant analysis.
Figure 7.43 Normalised streamwise Reynolds normal stress at $x = 850$ mm in inner variables at similar $Re_\theta$

Figure 7.44 Normalised wall-normal Reynolds normal stress at $x = 850$ mm in inner variables at similar $Re_\theta$
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The wall-normal Reynolds normal stress profiles are compared in Figure 7.44 in inner variables. The hydraulically smooth wall profiles, SP Lab and SP1 F6, have a similar shape to those of Brzek et al. (2007). However, the rough wall profiles, RP Lab and RP2 F5, have a near-wall peak that is not present in the smooth wall profiles, nor in the rough wall profiles of Brzek et al. (2007). The position and magnitude of the peak is Reynolds number dependent, as demonstrated in Figure A.47a and Figure 7.44. The wall-normal extent of the measuring volume in wall units ranges from $5 < y^+ < 10$ for RP Lab. Thus the peak cannot be attributed to spatial averaging issues in the near-wall region.

Schultz and Swain (1999) also observed near-wall peaks in their $v'^2$ profiles for biofouled test plates, which they attributed to surface compliance and movements of the algae filaments. However, that explanation is not plausible for the current results, as the peak is present for the non-compliant sandgrain roughness of RP Lab. Antonia and Luxton (1971) found that the wall-normal Reynolds stress was elevated in the near-wall region near a step change from smooth to rough, but attained self-preservation reasonably rapidly (at 35 inches (~890 mm) downstream from the change in their surface roughness). Bandyopadhyay (1987) also found that the boundary layer reached equilibrium after 600 mm for a $k$-type roughness after a step change from smooth to rough. Hinze (1975) points out that the inner region recovers quickly from disturbances since the eddies in the inner region are very small. The outer region, on the other hand, takes much longer to recover from disturbances. The one-dimensional measurements presented in Section 7.3.2 suggested that the boundary layer over RP Lab was close to equilibrium by $x = 850$ mm. Thus it seems that the peak is not caused by lack of equilibrium in the rough plate boundary layers.

Profiles at similar $Re_\theta$ in outer coordinates are given in Figure 7.45, scaled using $u^*$ determined from the primary analysis methods: Bradshaw’s Method for the smooth plate profiles, and Perry and Li’s Method for the rough plate profiles. The profiles collapse reasonably well for $(y+c)/\delta > 0.6$. The profiles do not collapse as well in the region $(y+c)/\delta < 0.6$. This is much more evident in the wall-normal Reynolds stress, plotted in Figure 7.47, where the data for SP Lab sits noticeably below the data for the other test plates.

To determine whether these trends are true differences in Reynolds normal stresses between the test plates, or just anomalies caused by using different analysis methods for the smooth and rough plates, the data was also plotted using $u^*$ determined using the Total Stress Method. The
data is plotted in Figure 7.46 for the streamwise Reynolds normal stress and in Figure 7.48 for the wall-normal Reynolds normal stress.

The data shows much better collapse for the streamwise Reynolds normal stress when scaled using $u^*\overline{u}$ determined by the Total Stress Method. There are differences in the near-wall region of the boundary layer, as discussed above, for the profiles scaled using inner coordinates. The wall-normal Reynolds stress also collapses better when scaled using $u^*\overline{u}$ determined by the Total Stress Method. There are differences between the smooth and the rough plates in the region $0.2 < (y+\varepsilon)/\delta < 0.8$, but they are within the measurement uncertainty. The differences in the near-wall region were discussed above for the data plotted in inner coordinates.

The results from this study are compared with smooth and biofouled results of Schultz and Swain (1999), the smooth and sandgrain roughness results of Tachie et al. (2004), and the smooth wall results of Schultz and Flack (2005) in Figure 7.49 for both the streamwise and wall-normal Reynolds normal stresses. Schultz and Swain (1999) conducted boundary layer measurements on marine biofilms, and the profile shown here was for a low-form slime consisting mainly of EPS (see Chapter 2) and marine diatoms, taken at $x = 1.13$ m and $U = 1.5$ m/s. Tachie et al. (2004) examined open channel boundary layers over smooth, sandgrain and mesh roughnesses. The smooth and sandgrain profiles are shown in Figure 7.49. They found that the sandgrain profile did not deviate significantly from the smooth wall profile. However, significant differences were noted between the mesh profile and the smooth wall profile, similar to the results of Krogstad and Antonia (1999). Schultz and Flack (2005) compared boundary layer profiles for a flat plate covered with uniform spheres, the same surface with the addition of a finer-scale grit roughness, and a smooth plate. Their results collapsed for $y/\delta > 0.1$, outside of the roughness sublayer for both the streamwise and wall-normal Reynolds normal stresses.

It should be noted that the data presented in Figure 7.49 were determined using different analysis methods. Schultz and Swain (1999) used Bradshaw’s Method for the smooth profiles, and the Log Law Slope Method for the fouled surfaces; Tachie et al. (2004) used the Modified Hama Method for the rough wall profiles and the Sublayer Slope Method for the smooth wall profiles; and Schultz and Flack (2005) used the Clauser Chart Method for their smooth wall profiles.

The data shows good collapse for the streamwise Reynolds normal stress, and reasonable collapse for the wall-normal Reynolds normal stress. The trends displayed in the current data certainly correlate with the previous data, with the exception of the near-wall peak in the rough plate wall-normal Reynolds normal stress profiles.
Figure 7.45 Streamwise Reynolds normal stress in outer variables at $x = 850$ mm, scaled using $u^*$ from the primary analysis methods, at similar $Re_\theta$: Bradshaw’s Method (B); Perry and Li’s Method (PL).

Figure 7.46 Streamwise Reynolds normal stress at $x = 850$ mm in outer variables, scaled using $u^*$ from the Total Stress Method, at similar $Re_\theta$.
Figure 7.47 Wall-normal Reynolds normal stress at $x = 850 \text{ mm}$ in outer variables, scaled using $u^*$ from the primary analysis methods, at similar $Re_{\theta}$. Bradshaw’s Method (B); Perry and Li’s Method (PL).

Figure 7.48 Wall-normal Reynolds normal stress in outer variables at $x = 850 \text{ mm}$, scaled using $u^*$ from the Total Stress Method, at similar $Re_{\theta}$. 

*Source*
Figure 7.49 Reynolds normal stresses at $x = 850$ mm for various $Re_θ$: (a) streamwise; (b) wall-normal. SS – Schultz and Swain (1999); TBB – Tachie et al. (2004); SF – Schultz and Flack (2005)
7.3.9 Reynolds Shear Stress

The \( xy \) Reynolds shear stress profiles for the profiles obtained using the LDV in the two-dimensional configuration at \( x = 850 \text{ mm} \) are given in Appendix A8 in Figure A.52 - Figure A.55, scaled using the wall shear velocity determined from Bradshaw’s Method for the hydraulically smooth test plates (SP Lab and SP1 F6) and Perry and Li’s Method for the hydraulically rough test plates (RP Lab and RP2 F5). The results for each test plate at different freestream velocities collapse well in the outer region of the boundary layer, but there is considerable scatter in the near-wall data.

Reynolds shear stress profiles at similar \( Re_\theta \) are given in Figure 7.50, scaled using \( u^* \) determined from the primary analysis methods. The profiles collapse reasonably well for \( (y+\varepsilon)/\delta > 0.35 \), with the exception of the SP Lab profile which sits below the data for the other test plates.

To determine whether these trends are true differences in the Reynolds shear stress between test plates, or anomalies caused by using different analysis methods for the smooth and rough plates, the data was also plotted using \( u^* \) determined from the Total Stress Method, as shown in Figure 7.51. The SP Lab profile collapses better with the data from other test plates when scaled using the Total Stress Method, which indicates that the differences observed in Figure 7.50 are due to the use of different analysis methods.

There are significant differences between the smooth and rough wall profiles in the near-wall region, \( (y+\varepsilon)/\delta < 0.1 \). The rough wall profiles exhibit a high peak that is not present in the smooth wall profiles. The uniform sphere data of Schultz and Flack (2005) exhibited a similar near-wall peak, which is shown in Figure 7.53, as did the rough wall data of Brzek et al. (2007). Schultz and Flack (2005) found that their uniform spheres collapsed well with smooth plate data in the outer region of the boundary layer, and also showed good agreement with Ligrani and Moffat (1986) on uniform spheres and Schultz and Flack (2003) on sandgrain roughness outside the roughness sublayer.

The Reynolds shear stress data from this study are compared with smooth and biofouled results of Schultz and Swain (1999), the smooth and sandgrain roughness results of Tachie et al. (2004), and the smooth and uniform sphere results of Schultz and Flack (2005) in Figure 7.52 for the smooth wall data and in Figure 7.53 for the rough wall data.
Tachie et al. (2004) note that the peak value of the normalised Reynolds shear stress should be approximately 1 for high Reynolds numbers. However, their smooth wall profile has a peak of 0.65, for $Re_\theta = 1900$. They attributed the lower peak to large probe volume extent (Johnson & Barlow 1989), low Reynolds number effects, and secondary flow. In the present study the smooth wall peak value was approximately 0.8 using Bradshaw’s Method with $Re_\theta$ in the range $2710 < Re_\theta < 7690$. The data collapsed reasonably well at different $Re_\theta$ for each test plate. The effect of the probe volume size was discussed in Section 5.2.1.5. Johnson and Barlow (1989) found that the shear stress measurements show a strong dependence on $l^+$, with $-u'v'$ decreasing with increasing measuring volume length. They recommend that to obtain accurate Reynolds shear stress measurements, the spanwise extent of the measuring volume should be less than 15 viscous units (i.e. $l^+ < 15$). In the present study, $40 < l^+ < 85$ for smooth plate measurements and $50 < l^+ < 120$ for rough plate measurements. Thus the Reynolds shear stress is most likely underestimated in the near-wall region.

Both the smooth and rough wall data show the same trends as the data of Schultz and Swain (1999), Tachie et al. (2004), and Schultz and Flack (2005). However, the magnitudes of the normalised shear stress are not the same, due to the probe volume issue mentioned above and the different analysis methods adopted by different authors.
Figure 7.50 Reynolds shear stress at $x = 850$ mm for similar $Re_\theta$ using $u^*\bar{w}$ from the primary analysis methods: Bradshaw’s Method (B); Perry and Li’s Method (PL).

Figure 7.51 Reynolds shear stress at $x = 850$ mm for similar $Re_\theta$ using $u^*\bar{w}$ from the Total Stress Method.
Figure 7.52 Comparison of smooth wall Reynolds shear stress at $x = 850$ mm for various $Re_\theta$: SS – Schultz and Swain (1999); TBB – Tachie et al. (2004); SF – Schultz and Flack (2005)

Figure 7.53 Comparison of rough wall Reynolds shear stress at $x = 850$ mm for various $Re_\theta$: SS – Schultz and Swain (1999); TBB – Tachie et al. (2004); SF – Schultz and Flack (2005)
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7.3.10 Quadrant Analysis

Quadrant analysis, introduced in Section 2.2.5.3, sorts turbulent events into each of the four quadrants of the $u'v'$-plane, providing information regarding the turbulence structure. In particular, it allows the contributions of ejection (Q2) and sweep (Q4) motions to the total Reynolds shear stress to be calculated (Schultz & Flack 2007). This can be particularly useful in determining if there is any difference in turbulence structure between smooth and rough surfaces. The hyperbolic hole size method of Lu and Willmarth (1973) was used to sort the data into quadrants, and was detailed in Section 2.2.5.3.

The contributions from each quadrant for each test plate with $H = 0$ are shown in Figure A.56 - Figure A.59 in Appendix A9, normalised by $u^*$ determined using the Total Stress Method. Setting $H = 0$ captures all of the turbulence information. The results collapse reasonably well at different Reynolds numbers.

The quadrant contributions for each test plate are compared in Figure 7.54 for Q1 and Q2 and in Figure 7.55 for Q3 and Q4 for similar $Re_\theta$ and $H = 0$. The results for all quadrants collapse reasonably well in the outer region of the boundary layer, which was also noted by Schultz and Flack (2005) for their smooth and uniform sphere roughnesses. The Q2 and Q4 contributions for RP Lab and RP2 F5 are significantly higher in the near-wall region, $(y+\varepsilon)/\delta < 0.1$, than the contributions for the smooth walls, which is in agreement with the results of Schultz and Flack (2005).
Figure 7.54  Normalised Reynolds shear stress contributions with $H = 0$ at $x = 850$ mm for all test plates at similar $Re_\theta$ ($u^*$ from Total Stress Method) (a) $Q^1$; (b) $Q^2$
The stronger ejection and sweep events were investigated by setting $H = 2$, which corresponds to Reynolds shear stress producing events stronger than $\overline{u'v'}$. The results for each freestream velocity for each test plate are not shown here. They collapsed in a similar fashion to those with similar $Re_0$ ($u^*$ from Total Stress Method) (a) Q3; (b) Q4.
H = 0, but with more scatter in the results. The results for each test plate are compared in Figure 7.56 for strong Q2 events and in Figure 7.57 for strong Q4 events.

The profiles for all of the test plates show reasonable agreement for the stronger Q2 events, with the exception of the near-wall region, \((y+\varepsilon)/\delta < 0.1\). This is in contrast to the results of Schultz and Flack (2005) and Krogstad et al. (1992), who noted that the Q2 events were enhanced for rough walls in the ranges 0.1 < \(y/\delta< 0.3\) and over most of the boundary layer respectively. They also both noted an upturn in the strong ejection events for smooth walls in the very near-wall region. This is not observed here because two-dimensional measurements were not obtained close enough to the wall.

Similarly to the ejection events, the strong Q4 (sweep) events collapse for the different surfaces, with the exception of the near-wall region, \((y+\varepsilon)/\delta < 0.1\). In the near-wall region, the rough wall profiles are significantly elevated, and the smooth wall profiles show a downturn. The same trends were noted by both Krogstad et al. (1992) and Schultz and Flack (2005).

Lu and Willmarth (1973) used the ratio of Q2 to Q4 events as a measure of the relative importance of the ejection and sweep motions. Q2/Q4 is presented in Figure 7.58 for all turbulence events (H = 0) and in Figure 7.59 for strong turbulence events (H = 2).

For H = 0, the smooth and rough wall profiles compare well in the outer region of the boundary layer. Some disparity is noted in the near-wall region for \((y+\varepsilon)/\delta < 0.1\): here the Q2 events are stronger than the Q4 events for the smooth wall, whereas the sweep events are more dominant for the rough wall. The shape of the curve throughout the boundary layer is in very good agreement with those observed by Krogstad et al. (1992) and Schultz and Flack (2005, 2007).

For H = 2, the profiles are also in agreement for most of the boundary layer, with the exception of the near-wall region. As noted by Schultz and Flack (2007), Q2/Q4 is greater than 2 for the majority of the boundary layer for both the smooth and the rough plates; this highlights the importance of the strong ejection events. In the near-wall region Q2/Q4 increases for the smooth walls; this indicates that near the wall, strong ejection events are more prevalent than strong sweep events. The opposite is seen for the sandgrain roughness profile (RP Lab), with Q2/Q4 decreasing to a value of less than unity in the near-wall region; this indicates that near the rough wall strong sweeps make a larger contribution to the Reynolds shear stress than the strong ejections. Measurements were not made close enough to the wall to allow comment on motions over the rough biofouled surface (RP2 F5) in the near-wall region.
Figure 7.56 Normalised Reynolds shear stress $Q_2$ contributions with $H = 2$ at $x = 850$ mm for all test plates at similar $Re$ ($u^*$ from Total Stress Method)

Figure 7.57 Normalised Reynolds shear stress $Q_4$ contributions with $H = 2$ at $x = 850$ mm for all test plates at similar $Re$ ($u^*$ from Total Stress Method)
Figure 7.58 Ratio of Reynolds shear stress contributions from $Q_2$ and $Q_4$ at similar $Re_0$ with $H = 0$ at $x = 850 \text{ mm}$

Figure 7.59 Ratio of Reynolds shear stress contributions from $Q_2$ and $Q_4$ at similar $Re_0$ with $H = 2$ at $x = 850 \text{ mm}$
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7.4 DRAG MEASUREMENTS

Total drag force measurements were undertaken on all of the test plates. The measurements were analysed using the techniques detailed in Section 6.3 using the virtual origin distance calculated from boundary layer profiles taken just upstream of the test plate.

Results for the smooth plates and rough plates in both the clean and fouled condition are given in Figure 7.60 and Figure 7.61 respectively. The data for each test surface exhibit Reynolds number dependence for $Re_l < 1.2 \times 10^6$. It was also noted in Section 6.3.3 that the measurement uncertainty was much lower at higher Reynolds numbers. Thus the average values quoted in Table 7.5 based on results at the highest five Reynolds numbers measured for each test plate.

RP Lab, the clean rough reference plate, had an average equivalent sandgrain roughness of 2.76 mm, and was in the hydraulically rough flow regime for all test plate Reynolds numbers. The drag coefficient was on average 155% greater than SP Lab at high Reynolds numbers.

SP1 F2 had patches of quite heavy fouling, as shown in Figure 7.4. The average equivalent sandgrain roughness was 0.51 mm, with all flow speeds occurring in the smooth-rough transition regime. This poses problems for the resolution of the total drag coefficient, as the equations used are based on the flow being hydraulically rough. The increase in drag was an average of 68% compared with the clean smooth plate, SP Lab.

SP2 F3 (Figure 7.6) was exposed in the field for 2 weeks and had a light, uniform coverage of fouling. It was analysed using the smooth plate method and the difference in drag coefficient between the fouled and clean condition was small. Analysis using the rough plate method also indicated that the flow was in the hydraulically smooth regime.

SP1 F6 was deployed in Pond No.1 for 16 weeks and had an uneven distribution of fouling, with much heavier fouling occurring on the bottom 300 mm, as shown in Figure 7.16. The flow was in the smooth-rough transition regime for $U \leq 1.35 \text{ m/s}$, and in the hydraulically rough regime for $U > 1.35 \text{ m/s}$. The average $k_s$, calculated for freestream velocities in the hydraulically rough regime, was 1.03 mm. The drag coefficient was 99% higher than that for the clean smooth plate.

RP1 F1 (Figure 7.2) was the most heavily fouled plate in the data set, and the flow was in the hydraulically rough regime for all Reynolds numbers tested. The equivalent sandgrain roughness was 5.58 mm. The drag coefficient was on average 22% higher than RP Lab at high Reynolds numbers and 210% higher than SP Lab.
RP1 F4 (Figure 7.10) was more lightly fouled than RP1 F1, as it only had 2.5 weeks of exposure compared to the 11 months of exposure for RP1 F1. The equivalent sandgrain roughness was 4.34 mm, and the flow was in the hydraulically rough regime for all Reynolds numbers tested. The drag coefficient was on average 13% higher than RP Lab and 189% higher than SP Lab.

RP2 F5 (Figure 7.12) was also much more heavily fouled on the bottom section of the test plate. The flow was in the hydraulically rough regime for all of the freestream velocities tested. The average $k_s$ was 3.98 mm, 44% higher than the $k_s$ value for the clean smooth plate, RP Lab. The total skin friction coefficient was 181% higher than SP Lab, and 10% higher than RP Lab. The difference in drag coefficient between RP1 F4 and RP2 F5 was within the measurement uncertainty.

Table 7.5 Summary of variables for the drag measurements on the 1D data set

<table>
<thead>
<tr>
<th>Plate</th>
<th>Length of Exposure</th>
<th>Average $C_D$</th>
<th>% increase over SP Lab</th>
<th>% increase over RP Lab</th>
<th>Average $k_s$ [mm]</th>
<th>$Re_k$ at max $Re_l$</th>
<th>Flow Regime</th>
</tr>
</thead>
<tbody>
<tr>
<td>SP Lab</td>
<td>-</td>
<td>0.0035</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>3</td>
<td>smooth</td>
</tr>
<tr>
<td>RP Lab</td>
<td>-</td>
<td>0.0089</td>
<td>155</td>
<td>-</td>
<td>2.76</td>
<td>375</td>
<td>rough</td>
</tr>
<tr>
<td>RP1 F1</td>
<td>11 months</td>
<td>0.0108</td>
<td>210</td>
<td>22</td>
<td>5.58</td>
<td>771</td>
<td>rough</td>
</tr>
<tr>
<td>SP1 F2</td>
<td>12 months</td>
<td>0.0059</td>
<td>68</td>
<td>-</td>
<td>0.51</td>
<td>53</td>
<td>smooth-rough transition</td>
</tr>
<tr>
<td>SP2 F3 (1)</td>
<td>2 weeks</td>
<td>0.0038</td>
<td>8</td>
<td>-</td>
<td>-</td>
<td>4</td>
<td>smooth</td>
</tr>
<tr>
<td>SP2 F3 (2)</td>
<td></td>
<td>0.0041</td>
<td>16</td>
<td>-</td>
<td>0.09</td>
<td>7</td>
<td>smooth-rough transition</td>
</tr>
<tr>
<td>RP1 F4</td>
<td>2.5 weeks</td>
<td>0.0101</td>
<td>189</td>
<td>13</td>
<td>4.34</td>
<td>575</td>
<td>rough</td>
</tr>
<tr>
<td>RP2 F5</td>
<td>15 weeks</td>
<td>0.0098</td>
<td>181</td>
<td>10</td>
<td>3.98</td>
<td>561</td>
<td>rough</td>
</tr>
<tr>
<td>SP1 F6</td>
<td>16 weeks</td>
<td>0.0069</td>
<td>99</td>
<td>-</td>
<td>1.03</td>
<td>109</td>
<td>smooth-rough transition - rough</td>
</tr>
</tbody>
</table>

(1) Analysed using the smooth plate method, (2) analysed using the rough plate method
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Figure 7.60 Drag coefficient for clean and biofouled plates with a smooth substrate

Figure 7.61 Drag coefficient for clean and biofouled plates with a rough substrate
### Table 7.6 Summary of one-dimensional measurements

<table>
<thead>
<tr>
<th>Test Plate</th>
<th>Boundary Layer (at x = 850 mm)</th>
<th>Drag</th>
<th>Photogrammetry</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$U$ [m/s]</td>
<td>$Re_U$</td>
<td>$k_f$ [mm]</td>
</tr>
<tr>
<td>SP Lab (1D)</td>
<td>1.25</td>
<td>3480</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>2.00</td>
<td>5550</td>
<td>-</td>
</tr>
<tr>
<td>SP Lab (2D)</td>
<td>1.27</td>
<td>3330</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>1.99</td>
<td>4980</td>
<td>-</td>
</tr>
<tr>
<td>RP Lab (1D)</td>
<td>1.26</td>
<td>5100</td>
<td>3.04</td>
</tr>
<tr>
<td></td>
<td>1.99</td>
<td>7910</td>
<td>2.75</td>
</tr>
<tr>
<td>RP Lab (2D)</td>
<td>1.26</td>
<td>4910</td>
<td>1.93</td>
</tr>
<tr>
<td></td>
<td>2.01</td>
<td>7690</td>
<td>1.98</td>
</tr>
<tr>
<td>RP1 F1</td>
<td>1.25</td>
<td>5040</td>
<td>4.61</td>
</tr>
<tr>
<td></td>
<td>2.00</td>
<td>8390</td>
<td>5.53</td>
</tr>
<tr>
<td>SP1 F2</td>
<td>1.25</td>
<td>4190</td>
<td>0.89</td>
</tr>
<tr>
<td></td>
<td>2.00</td>
<td>6740</td>
<td>0.78</td>
</tr>
<tr>
<td>SP2 F3</td>
<td>1.25</td>
<td>3840</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>1.99</td>
<td>6080</td>
<td>-</td>
</tr>
<tr>
<td>RP1 F4</td>
<td>1.25</td>
<td>5150</td>
<td>3.14</td>
</tr>
<tr>
<td></td>
<td>2.00</td>
<td>8460</td>
<td>3.47</td>
</tr>
<tr>
<td>RP2 F5</td>
<td>1.25</td>
<td>4500</td>
<td>2.66</td>
</tr>
<tr>
<td></td>
<td>2.07</td>
<td>7270</td>
<td>2.59</td>
</tr>
<tr>
<td>SP1 F6</td>
<td>1.26</td>
<td>3500</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>1.99</td>
<td>5470</td>
<td>-</td>
</tr>
</tbody>
</table>

**RESULTS SYNTHESIS**

The main parameters for each test plate are summarised in Table 7.6. Only boundary layer roughness and drag coefficients to be compared between the different measurement techniques.
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The drag measurements are based on the whole of each test plate, whereas the boundary layer profiles results are local to the region where the traverse was taken. This is particularly significant considering the heterogeneity of the fouling over some of the plates: for example, RP1 F1 and SP1 F6 were much more heavily fouled on the bottom half of the plate compared to the top half of the plate. Unfortunately, the water tunnel setup meant that the more heavily fouled sections of each plate, which were always at the bottom of the test plate due to light attenuation or flow depth variations, were not able to be measured using the LDV system. It is recommended that this should be rectified for any future biofouling measurements in the UTAS Water Tunnel.

Selected equivalent sandgrain roughness values for each test plate are summarised in Table 7.7. The maximum peak-to-valley height, $R_t$, over the sample length obtained from the photogrammetry measurements provides the closest correlation with the boundary layer profile and drag values. Window A is located near the trailing edge at the bottom half of the test plate, and hence captured some of the heavier fouling present on each plate. Window E is located in the middle section of the top half of the test plate, and hence captured some of the lighter fouling present on each test plate.

Table 7.7 Equivalent sandgrain roughness values

<table>
<thead>
<tr>
<th>Plate</th>
<th>$k_s$ [mm]</th>
<th>$R_t$ [mm]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Boundary layer profiles at $x = 850$ mm</td>
<td>Drag</td>
</tr>
<tr>
<td></td>
<td>1.00 m/s</td>
<td>1.25 m/s</td>
</tr>
<tr>
<td>RP Lab (1D)</td>
<td>-</td>
<td>3.04</td>
</tr>
<tr>
<td>RP Lab (2D)</td>
<td>1.91</td>
<td>1.93</td>
</tr>
<tr>
<td>RP1 F1</td>
<td>5.18</td>
<td>4.61</td>
</tr>
<tr>
<td>SP1 F2</td>
<td>0.76</td>
<td>0.89</td>
</tr>
<tr>
<td>SP2 F3 (**)</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>RP1 F4</td>
<td>3.31</td>
<td>3.14</td>
</tr>
<tr>
<td>RP2 F5</td>
<td>1.84</td>
<td>2.66</td>
</tr>
<tr>
<td>SP1 F6</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

(*) Analysed using the rough plate methods

The $k_s$ values obtained from the one-dimensional boundary layer profiles at $x = 850$ mm and those from the drag measurements show good agreement for the clean sandgrain roughened test plate, RP Lab; the values from the two-dimensional boundary layer profiles were lower. Both the drag measurements and the boundary layer profiles were in the hydraulically rough flow...
regime. The values of $R_t$ from the photogrammetry are approximately 20% lower than $k_s$. The total drag coefficient is approximately 30% higher than the local skin friction coefficient at $x = 850$ mm.

The results for $k_s$ for the three hydraulically rough fouled test plates, RP1 F1, RP1 F4 and RP2 F5 show reasonable correlation between values from the boundary layer profiles and the total drag measurements.

Based on the non-uniform distribution of fouling on RP1 F1, it was expected that the drag measurement would produce a higher equivalent sandgrain roughness value, as the measurement volume for the LDV system was located in the region of lighter fouling shown in Figure 7.2. This did occur for boundary layer profiles taken at $x = 500$ mm where $k_s$ was 1 mm less for the boundary layer profile; however, it was not the case for boundary layer profiles taken at $x = 850$ mm. The data from both the boundary layer profiles and the total drag measurements, with $k_s \sim 5.5$ mm, was significantly greater than the maximum peak to trough height from the photogrammetry measurements, with $R_t = 3.3$ mm in the more heavily fouled region approximately 40% lower than the water tunnel measurements. The drag measurements showed a 210% increase in $C_D$ compared to a clean smooth plate, and a 22% increase compared to a clean rough plate. The boundary layer profile results showed a 180% increase in $c_f$ compared to a clean smooth plate, and a 26% increase compared to a clean rough plate.

RP1 F4 was incubated for approximately 2.5 weeks and had a more uniform coverage than RP1 F1, with the roughness values from the photogrammetry comparable to the top half of the fouling present on RP1 F1, as shown by the observations given in Section 7.1 and the $R_t$ values given in Table 7.7. The other notable difference in the biofilm for RP1 F4 was the presence of very fine streamers up to 20 mm long. The $R_t$ values were approximately 56% lower than the $k_s$ value from the drag measurements, and 46% lower than the boundary layer profile measurements at $x = 850$ mm and $U = 2.00$ m/s. The drag measurements showed a 189% increase in $C_D$ compared to a clean smooth plate, and a 13% increase compared to a clean rough plate. The boundary layer profile results showed a 135% increase in $c_f$ compared to a clean smooth plate, and a 6% increase compared to a clean rough plate.

The boundary layer profile $k_s$ values for RP2 F5 varied quite significantly with freestream velocity, and the variation did not appear to be a function of increasing or decreasing flow speed. The average value was 3.02 mm, which correlates reasonably well with the 3.98 mm determined from the total drag measurements. It was observed that the fouling was heavier on the bottom
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half of the test plate; this was picked up by the photogrammetry measurements, with the $R_t$ values for windows A and B being higher than those for windows D and E. The drag measurements showed a 181% increase over the smooth plate measurements and a 10% increase over the RP Lab measurements. The boundary layer profiles at $x = 850$ mm showed a 12% increase in $c_f$ at $U = 2.00$ m/s over RP Lab, and a 122% increase over SP Lab.

SP1 F2 had a heterogeneous distribution of fouling, as shown in Figure 7.4. The heaviest region of fouling occurred in the bottom half of the trailing edge of the test plate, which was captured by the photogrammetry at Window A, with an $R_t$ value of 2.0 mm, whereas the top section of the test plate had an $R_t$ value of only 0.4 mm (Window E). The boundary layer profiles gave $k_s$ values of 0.8 mm, compared to 0.5 mm from the drag measurements. The drag measurements showed a 68% increase in $C_D$ compared to a clean smooth plate and the boundary layer profile results showed a 65% increase in $c_f$.

SP2 F3 was incubated for just 2 weeks, and had the lightest and most uniform cover of biofouling. The drag measurements and boundary layer profiles were both difficult to analyse, but both put the flow within the smooth regime or very close to the cusp of the smooth-rough transition regime. This is a very interesting result, as it means that the establishing biofilm has little effect on the boundary layer in terms of drag production. The drag measurements showed a small increase in $C_D$ compared to a clean smooth plate. However, the boundary layer profile results showed a 10% decrease in $c_f$ when using the smooth plate methods. It would be beneficial to obtain more data on establishing biofilms to determine if they do in fact reduce the skin friction coefficient during the early stages of development.

The fouling on SP1 F6 was non-uniform, and obviously affected by low water levels in Pond No.1 during its deployment. The heavy fouling on the bottom of the test plate was not able to be measured using the LDV system, as already discussed. The boundary layer profiles were taken over regions of very light fouling and the measurements were found to be in the hydraulically smooth regime. The total drag measurements were in the smooth-rough transition regime for $U \leq 1.35$ m/s and in the hydraulically rough regime for $U > 1.35$ m/s. The equivalent sandgrain roughness was determined to be 1.03 mm for the flow speeds in the hydraulically rough regime. The photogrammetry results clearly show the differences between the two regions of fouling on the test plate. The fouling on the bottom of the plate had $R_t$ values of ~ 2 mm, whereas the light fouling on the top of the plate had $R_t$ values of ~ 0.2 mm. The average $R_t$ for the test plate was 1.15 mm, which correlates well with the $k_s$ from the drag measurements. The total drag
coefficient was 99% higher than for a smooth clean plate. The local skin friction coefficient was found to be similar to the SP Lab values at similar \( Re_\theta \), as was shown in Figure 7.34.

The results indicate that the motion of the biofilm is important and that the effective roughness of the biofilm is greater than the actual physical roughness. This has also been observed by others including Brett (1980), Callow (1993), and Schultz and Swain (1999).

Two mechanisms for energy dissipation were observed during the water tunnel measurements (Andrewartha et al. 2008b):

1. The algae filaments, such as on RP1 F4 (Figure 7.11), were observed to flutter in three dimensions under flow conditions. It is thought that this movement removes more momentum from the flow than low-form gelatinous biofilms (Schultz & Swain 1999); and

2. The low-form gelatinous biofilms were also observed to vibrate under flow conditions. The dense mat structure of the biofilm significantly impedes the flow in the near-wall region as the water is forced through the biofilm mat.

### 7.6 CHAPTER SUMMARY

This chapter has presented detailed measurements of the flow physics over eight different surfaces, including one- and two-dimensional boundary layer profiles, total drag measurements and roughness characterisation using photogrammetry. It was found that the presence of biofouling significantly increases both the local and total drag coefficients, as well as the equivalent sandgrain roughness. The effect of the biofouling is greater than would be expected from the physical roughness due to the compliant nature of biofilms.

Differences were noted in the near-wall region of the boundary layer for both the streamwise and wall-normal Reynolds normal stresses, however, the data in the outer region collapsed within the experimental uncertainty when scaled using a consistent method for the smooth and the rough test plates. Significant differences between the smooth and the rough plates were also noted in the near-wall region for the Reynolds shear stress. A quadrant analysis was undertaken to determine the relative importance of the different motions in the boundary layer; this found differences in the near-wall region only.

The data is assessed for its adherence to Townsend’s Wall Similarity Hypothesis in Chapter 9.
Chapter 8 – Measurements on an Artificial Filamentous Biofilm

8 MEASUREMENTS ON AN ARTIFICIAL FILAMENTOUS BIOFILM

Chapter 8 examines the flow structure in the immediate vicinity of an artificial filamentous biofilm, which was developed to mimic the filamentous algae streamers observed in hydropower canals. The work presented in this chapter was presented at the 3rd International Association of Hydraulic Engineering and Research (IAHR) International Symposium on Hydraulic Structures (Andrewartha et al. 2008a).

The detrimental effect of biofilms on drag is well established, and was reviewed in detail in Chapter 2. The biofilms studied in the previous chapter were low-form gelatinous biofilms, consisting of EPS, diatoms, and debris which becomes trapped in the biofilm mat. However, filamentous algae streamers up to 200 mm long have also been observed in Tarraleah No.1 Canal (Section 2.1.3), and it is expected that such streamers would cause an increase in skin friction drag above what would be expected for a low-form gelatinous biofilm.

This chapter presents boundary layer mean velocity and turbulence intensity profiles on both a smooth reference plate and a plate artificially fouled with streamers.

8.1 EXPERIMENTAL PROCEDURE

All experimental studies were completed in the UTAS Water Tunnel, which was described in detail in Chapter 4. One-dimensional instantaneous velocity samples were obtained using the LDV system at 62 locations in the boundary layer, with 10,000 samples collected per position.

Filamentous algae streamers have been identified as causing additional drag over low-form gelatinous biofilms, and are therefore of interest. An artificially fouled test plate (designated AP) was developed by gluing single-strand wool streamers to a smooth painted test plate on a 100 mm grid to study the effects of filamentous algae streamers in a controlled environment. Figure 8.1 and Figure 8.2 illustrate the similarities between an actual filamentous biofilm grown in the field, and the artificial streamers constructed in the laboratory. It is acknowledged that further work is needed to properly replicate the natural streamers, particularly in matching the material properties of the natural and artificial streamers. However, the purpose of using wool for the artificial streamers was to simply replicate the movement of the natural streamers under flow conditions, which was achieved.
Measurements were conducted with different streamer lengths (180 mm, 160 mm, and 120 mm) on the centreline of the test plate (Figure 8.3) to investigate the relationship between streamer length and local skin friction coefficient. Measurements were also conducted immediately downstream of an 80 mm long streamer to investigate the flow structure in the vicinity of a streamer. The streamer was located approximately half way down the plate and had 4 x 80 mm long streamers upstream of it, in series (Figure 8.3).

Streamer ‘snapping’ was an issue for the first set of measurements on 120 – 180 mm long streamers. Streamer ‘snapping’ refers to the streamers physically blocking one or both of the laser beams and effectively preventing the crossing of the two beams at the measurement volume. The streamers did not interfere in the measurement volume itself; rather they blocked the beams and prevented the formation of the measurement volume. This led to longer data collection times to achieve the required number of samples. For the second set of measurements with a streamer length of 80 mm, interfering streamers were removed from the LDV side of the test plate to resolve the issue. The measuring volume was located immediately downstream of a streamer so that no ‘snapping’ would occur.

Results are compared to those for the clean smooth plate, SP Lab. Extensive results are given for SP Lab in Chapter 7. The SP Lab measurements were obtained with the LDV tilted at 0.2° towards the wall to obtain measurements in the near-wall region, as discussed in Section 5.2.2.2. The data for AP was taken prior to the SP Lab measurements, before the tilt angle investigation was completed; thus the LDV probe was aligned orthogonally to the side wall of the water tunnel, with a 0.0° tilt angle towards the wall. Bradshaw’s Method, as presented in 6, was used to determine to determine \( \nu^* \) and \( c_f \).
Chapter 8 – Measurements on an Artificial Filamentous Biofilm

8.2 RESULTS AND DISCUSSION

The LDV system was first set up so that the measurement volume was situated on the centreline of the test plate, directly between two rows of streamers on the artificially fouled plate as shown by the line (1) in Figure 8.3. Measurements were taken with different streamer lengths (nominally 180 mm, 160 mm and 120 mm) at a distance of 495 mm from the leading edge of the test plate.

The mean velocity and turbulence intensity profiles showed no variation from a smooth plate profile, regardless of streamer length. It is thought that the streamers affect the boundary layer only in their immediate vicinity, with a low wake spreading angle, and the measurement volume was located outside of the area of influence.

The flow structure behind an 80 mm long streamer was investigated by making adjustments to the mount for the LDV probe so that the measurement plane was in-line with a row of streamers as shown by point (2) in Figure 8.3, and the measurement volume was positioned directly downstream from a streamer. The full range of boundary layer parameters for the smooth plate are given in Table A.9. Boundary layer parameters for the artificially fouled plate and the smooth plate at $x = 500$ mm are given Table 8.1. The local skin friction coefficient, $c_f$, was similar to the values obtained for the smooth plate; however, the boundary layer thicknesses and shape factor values were all higher for the artificially fouled plate.
Boundary layer mean velocity profiles for AP, scaled using inner parameters, are given in Figure 8.4 and compared with the profile for SP Lab at $U = 2.00$ m/s. The profiles for the artificially fouled plate collapse with the smooth wall log law. The wake strength for the AP profiles is stronger than the SP profiles, which was also observed for SP2 F3 and SP1 F6.

Dimensionless velocity profiles are given in Figure 8.5 for AP and are compared with the profile for SP Lab at $U = 2.00$ m/s. The AP profiles exhibit a Reynolds number dependence, as the lower freestream velocity profiles of 0.50 m/s and 1.00 m/s do not collapse well with profiles at 1.50 m/s and 2.00 m/s. Reynolds number dependence aside, the profiles at $U = 1.50$ m/s and 2.00 m/s exhibit a slight velocity deficit when compared to the smooth wall profile.

Velocity defect profiles are given in semi-logarithmic form in Figure 8.6. The Reynolds number dependence for the low freestream velocity profiles was also evident in these plots and the data has been removed for clarity purposes. The fit to Equation 7.2, used to determine the wake strength, is also shown. It was found in earlier Chapters that Equation 7.2 fitted well to profiles taken at the furthest profiling stations downstream from the leading edge, but not as well to profiles near the leading edge. The profiles presented here were taken at approximately the midpoint of the test plate, $x = 500$ mm, and thus do not collapse perfectly with Equation 7.2. The AP and SP Lab profiles collapse in the near-wall region and in the outer region of the boundary layer. However, the deficit is higher for the AP profiles in the region $0.01 < (y+\varepsilon)/\delta < 0.3$. 

\begin{table}[h]
\centering
\begin{tabular}{|c|c|c|c|c|c|c|c|c|c|}
\hline
Test Plate & $U$ [m/s] & $Re_\theta$ & $\delta$ [mm] & $\delta^*$ [mm] & $\theta$ [mm] & $H$ & $c_f$ & $\Pi\varepsilon$ [m/s] & $\Pi$ & $\varepsilon$ [mm] \\
\hline
AP & 0.50 & 1552 & 30.49 & 5.05 & 3.56 & 1.42 & 0.00363 & 0.021 & 0.59 & 0.16 \\
AP & 1.00 & 3157 & 33.91 & 4.82 & 3.60 & 1.34 & 0.00329 & 0.041 & 0.45 & 0.12 \\
AP & 1.50 & 4559 & 35.83 & 4.45 & 3.42 & 1.30 & 0.00319 & 0.060 & 0.32 & 0.10 \\
AP & 2.01 & 6087 & 37.12 & 4.36 & 3.43 & 1.27 & 0.00309 & 0.079 & 0.25 & 0.12 \\
SP & 1.00 & 2572 & 28.03 & 3.80 & 2.92 & 1.30 & 0.00370 & 0.043 & 0.24 & 0.09 \\
SP & 1.25 & 3146 & 29.00 & 3.70 & 2.87 & 1.29 & 0.00352 & 0.052 & 0.24 & 0.05 \\
SP & 1.75 & 4409 & 28.91 & 3.61 & 2.85 & 1.27 & 0.00330 & 0.071 & 0.24 & 0.05 \\
SP & 2.00 & 5059 & 32.31 & 3.64 & 2.89 & 1.26 & 0.00322 & 0.080 & 0.18 & 0.09 \\
\hline
\end{tabular}
\caption{Boundary layer parameters for the artificially fouled plate (AP), directly behind a streamer, compared with results for a smooth painted plate (SP) both at $x \sim 500$mm}
\end{table}
Chapter 8 – Measurements on an Artificial Filamentous Biofilm

Figure 8.4 Boundary layer mean velocity profiles in inner coordinates for AP and SP Lab

Figure 8.5 Dimensionless velocity profiles for AP and SP Lab
Streamwise turbulence intensity profiles for AP are given in Figure 8.7, and compared with the SP Lab profile at \( U = 2.00 \) m/s. Profiles for SP Lab are presented in Figure A.34 at the same location. The turbulence intensity is higher for the artificially fouled plate in the region \( 0.08 < \left( \frac{y+\varepsilon}{\delta} \right) < 0.4 \) (3 mm < \( y \) < 15 mm). This corresponds to the observed maximum extent of outward movement of the streamers. The turbulence caused by the streamer would be expected to thicken the boundary layer, which is observed in the results given in Table 8.1.

Streamwise Reynolds normal stress profiles are presented in Figure 8.8 in semi-logarithmic form and in Figure 8.9 in outer coordinates. Both plots clearly show an increase in the streamwise Reynolds normal stress for the AP profiles when compared to the SP Lab profile. Similarly to the turbulence intensity profile, the region of increased stress is \( 0.08 < \left( \frac{y+\varepsilon}{\delta} \right) < 0.4 \) (3 mm < \( y \) < 15 mm) in outer coordinates, and approximately 150 < \( y' \) < 900 in inner coordinates.

Schultz (2000) conducted turbulent boundary layer measurements on surfaces covered with a filamentous marine alga that had filaments up to 71 mm in length. Modest increases in the streamwise Reynolds normal stress were observed over a significant region of the boundary layer, which supports the results of the present study. More significant increases were noted in the wall-normal Reynolds normal stress and the Reynolds shear stress, extending to > 2.5 times the maximum extent of outward movement of the filaments.
Chapter 8 – Measurements on an Artificial Filamentous Biofilm

Figure 8.7 Streamwise turbulence intensity profiles for AP and SP Lab

Figure 8.8 Semi-logarithmic normalised streamwise Reynolds normal stress profiles for AP and SP Lab
8.3 CHAPTER SUMMARY

Biofilms, particularly filamentous algae streamers, are known to cause significant skin friction drag. The current work has identified that streamers affect the boundary layer only in their immediate vicinity. The streamwise turbulence intensity and streamwise Reynolds normal stress were elevated for the artificially fouled test plate in the region \(0.08 < \left(\frac{y+\varepsilon}{\delta}\right) < 0.4\) (3 mm < \(y\) < 15 mm). This corresponds to the observed maximum extent of outward movement of the streamers. The boundary layer on the artificially fouled plate was also found to have a stronger wake component. No significant differences in local skin friction coefficient were observed between the smooth plate and the results from directly behind the streamer.

Future work is recommended to determine the downstream and lateral extents of elevated turbulence production from the streamer motion. It is also recommended that two-dimensional measurements be undertaken to determine the effects on wall-normal components and on the Reynolds shear stress.
Chapter 9 - Discussion

9 DISCUSSION

This thesis has presented a detailed study of the effects of freshwater biofouling on turbulent boundary layers, with application to hydropower canals. The major objectives of this study were:

- to investigate the structure of the turbulent boundary layer over smooth, rough, and biofouled surfaces to advance the understanding of the mechanisms for drag production by biofilms; and
- to determine whether or not the wall similarity hypothesis can be extended to include compliant biological surfaces.

The results of this thesis have been discussed in detail in each chapter. The purpose of this discussion chapter is to bring together the major findings and present them in light of the objectives.

9.1 GENERAL COMMENTS ON BIOFILMS

This study was concerned primarily with the freshwater low-form gelatinous diatom species *Gomphonema tarraleahae* and *Tabellaria flocculosa*. The physical characteristics of the biofilms were described in Chapters 2 and 7.

The effect of *T. flocculosa* and *G. tarraleahae* was to increase the skin friction coefficient above the clean surface value for both smooth and sandgrain roughened test plates. This validates the results of the field trial undertaken in Tarraleah No.1 Canal, which found an increase in canal capacity of 9.9% through the removal of as much of the biofilm mat as possible from the surface using physical scrubbing (as described in Sections 2.1.4.2 and Appendix B).

Due to operational factors outside of the University’s control, the test plates were not always completely immersed in the flow in Tarraleah No.1 Canal and Pond No.1, resulting in non-uniform fouling on some of the test plates, shown in Figure 9.1. Where the biofilm coverage was heterogeneous, the drag measurements showed greater increases in skin friction coefficient. The water tunnel setup meant that the more heavily fouled sections of each plate, which were always at the bottom of the test plate due to light attenuation or flow depth variations, were not able to be measured using the LDV system. It is recommended that this should be rectified for any future biofouling measurements in the UTAS Water Tunnel.
The largest percentage increase in skin friction coefficient was for the smooth plate exposed to fouling over a 16 week period (SP1 F6), which had a 99% increase in $C_D$ over the clean smooth plate value. SP1 F2, which was exposed for 12 months, had a 68% increase in $C_D$ and a 65% increase in $c_f$ at $x = 850$ mm. The increases in skin friction coefficient over the clean plate values were significantly higher for the smooth plates than the rough plates. The maximum increase for a plate with a rough substrate was a 26% increase in $c_f$ at $x = 850$ mm and a 22% increase in $C_D$ for RP1 F1 which was incubated for 11 months. Similar results were obtained by Barton et al. (2007) who noted the serious economic consequences if appropriate low-friction and anti-fouling paints are not used in hydraulic conduits such as those found in hydropower systems.

Two points of difference between this study and previous studies on live biofilms are: firstly the length of exposure to the fouling, and secondly the method of growing the biofilms under flow conditions in the field. Schultz (1998) grew the marine fouling algae, *Enteromorpha*, in grow-
out tanks under static conditions with specimens taken with 6, 14 and 17 days exposure. Barton (2007) exposed test plates in Pond No.1 at Tarraleah over approximately four month periods. The biofilms in the present study ranged from 2 weeks to 12 months exposure, all grown under flow conditions at Tarraleah.

The total drag characteristics of each test plate are summarised in Table 9.1, highlighting the length of incubation for each test plate. Photographs of each plate are collated in Figure 9.1. Significantly higher drag coefficients and equivalent sandgrain roughnesses were observed on the test plates with a sandgrain substrate. The biofilms were also observed to be much thicker on these plates. This indicates that the substrate roughness is an important parameter in terms of biofilm growth.

Table 9.1 Development of biofilm over different incubation periods (\(C_D\) and \(k_s\) from drag measurements)

<table>
<thead>
<tr>
<th>Length of Incubation</th>
<th>Smooth Plate</th>
<th>Rough Plate</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Test Plate</td>
<td>Flow Regime</td>
</tr>
<tr>
<td>Clean</td>
<td>SP Lab</td>
<td>Smooth</td>
</tr>
<tr>
<td>~2 weeks</td>
<td>SP2 F3</td>
<td>Smooth</td>
</tr>
<tr>
<td>~16 weeks</td>
<td>SP1 F6</td>
<td>Smooth-rough transition - Rough</td>
</tr>
<tr>
<td>~12 months</td>
<td>SP1 F2</td>
<td>Smooth-rough transition</td>
</tr>
</tbody>
</table>

The biofilm established very quickly on the large plates in Pond No.1. A uniform biofilm residue, consisting primarily of \(T. flocculosa\), was present on SP2 F3 after only two weeks of exposure. The water tunnel measurements revealed that the flow was operating in the hydraulically smooth regime for the range of freestream velocities tested. Similar results were obtained for the top half of SP1 F6, which was also covered with a light uniform slime film.

A light slime, with very fine streamers, established on RP1 F4 after two weeks exposure at Pond No.1. The drag coefficients for the rough plate with 2 weeks exposure and 16 weeks exposure were the same within the experimental uncertainty, although RP2 F5 (16 weeks exposure) had visually more biofilm present.
The biofilms took much longer to develop on the small test plates that were installed at Bridge No.9 and Transition 4 (see Section 3.2). The most likely cause is the variation in flow rate between the slower flowing Pond No.1 and the two sites located in the faster flowing canal. It was noted in the field trial that the biofilm established on the rough plates first, which was also observed for the large test plates. These results are corroborated by those of Peterson (2007), who noted that algae colonise less effectively in fast current than in slow; and in fast flow, colonisation rates are higher on rough surfaces than on smooth ones. The rough surfaces provide interruptions to the flow and refuge from the wall shear stress.

Examination of the total drag coefficient for both the smooth and rough test plates over time revealed that the biofouling does not build significantly with time and may have a maximum thickness before the outer layers are sloughed off by the shear stress.

It was observed during the small test plate field trial, presented in Section 3.2, that *G. tarralleahae* grew longer stalks on the Sikagard 680s surface than the Jotamastic 87 surface. Both of these surfaces were smooth painted surfaces and would be in the hydraulically smooth regime when in the clean condition. A highly stalked biofilm will cause greater resistance to the flow, and hence cause a lower flow carrying capacity than a biofilm with less stalks. It is not known why more stalks were produced on one surface than the other, but may be due to the surface free energy or application method (e.g. sprayed or rolled). This warrants further investigation.

RP1 F1 and SP1 F2 were installed in the same location at the same time for approximately the same period, and thus experienced the same flow, light, and nutrient conditions. However, the fouling found on the two plates was quite different in species composition. The variables were the roughness and colour of the test plates. Peterson (2007) noted that colonisation rates are higher on rough surfaces than on smooth ones as surface irregularities provide interruptions to the flow and refuge from the shear force. The impact of substrate colour was investigated by Swain *et al.* (2006) who found that two biofouling species had significantly higher settlement on black surfaces than white surfaces. This warrants further investigation.

### 9.2 PHYSICAL CHARACTERISATION OF BIOFILMS

The biofouled surfaces investigated in this study were mapped using digital photogrammetry to obtain three-dimensional roughness information. Barton (2007) found that the maximum peak-to-valley height, $R_t$, provided the closest correlation with the equivalent sandgrain roughness, $k_s$, obtained from the water tunnel measurements.
Chapter 9 - Discussion

It is clear from the photogrammetry measurements, and from the photographs shown in Figure 9.1, that the biofouling obtained on the 1.0 m by 0.6 m (large) test plates was highly variable. The case is even more extreme for the Tarraleah canal system as a whole, with regions of light and shading, flow transitions, occasional grazing events by black fly larvae and significant variations in fouling growth with season. A higher growth rate is experienced in the warmer summer months, and the fouling has been observed to prefer the more shaded sections of the canal to those exposed to direct sunlight. As such, a single drag coefficient or roughness factor for the canal is not practical or expected.

The following trends were observed from the photogrammetry data:

- Plate sections with a heavy fouling coverage and a sandgrain substrate had higher $R_t$ and mean height values than the corresponding clean plate. They also had a higher variance of the mean height, which indicates a higher measurement uncertainty, and generally had a negative skewness. The peak count was less than for a clean sandgrain plate, suggesting that the biofouling smooths the surface out somewhat;

- Plate sections with a light fouling coverage and a sandgrain substrate had lower $R_t$ and mean height values than the corresponding clean sandgrain plate. The lower $R_t$ values suggest that the fouling is smoothing the surface out. The variance of the mean height was comparable to the clean plate values, and the skewness was positive. The peak count was more than for the heavily fouled sections, but less than for the corresponding clean plate;

- Plates with a heavy fouling coverage and a smooth substrate had higher mean height values than the same plates with a light fouling coverage. The peak counts were similar to the heavily fouled sandgrain plates. The variance of the mean height and $R_t$ values were comparable to the clean sandgrain plate values; and

- Plates with a light fouling coverage and a smooth substrate had very low mean height and variance values. The peak count was much higher than for a clean rough plate, or for a heavily fouled plate. The $R_t$ values were much lower than the clean sandgrain surface values.

The results for the lightly fouled sections of the sandgrain surfaces indicate that the biofilm can make the surface physically smoother than the underlying roughness. This phenomenon was also
observed by Barton (2007). However, where the fouling was particularly heavy it had the opposite effect and made the surface physically rougher than the underlying sandgrains.

The equivalent sandgrain roughness height for the biofouled surfaces was observed to be significantly higher than the roughness obtained from the photogrammetry measurements, with the exception of the lightly fouled smooth plates, as shown in Table 9.2. The roughness information obtained from the photogrammetry measurements only describes the surface under still conditions as the photographs were taken with the biofilms in air, rather than submerged and under flow conditions as they are in their natural environment. Schultz and Swain (1999) suggest that other parameters such as the ratio of the wall shear stress to the shear modulus of the biofilm are also likely to be important. Vibration, shearing behaviour and flow through the biofilm are also important parameters and mechanisms for energy dissipation, and are the subject of ongoing research.

It was noted in Chapter 2 that the effective roughness caused by biofilm growth is considerably larger than the absolute thickness of the biofilm layer and is much more complex than the widely used Nikuradse sand grain roughness. Biofilms are very unlike the typical solid engineering roughnesses. Their compliant nature allows them to move under flow conditions.

Two mechanisms for energy dissipation were observed during the water tunnel measurements:

1. The algae filaments were observed to flutter in three dimensions under flow conditions. This movement removes more momentum from the flow than low-form gelatinous biofilms (Picologlou et al. 1980; Schultz & Swain 1999); and

2. The low-form gelatinous biofilms were also observed to vibrate under flow conditions, thus dissipating energy within the vibrating biofilm mat. The dense mat structure of the biofilm significantly impedes the flow in the near-wall region as the water is forced through the biofilm mat.

The average physical roughness height, $k$, for the sandgrain surfaces was determined using a particle size distribution to be approximately 1.5 mm. The ratio $k_s/k$ ranged from 1.3 – 1.8. Similarly, $R_t$ for the rough plate was approximately 2.2 mm, giving a ratio $k_s/R_t$ around 1.3.

It is much harder to define a $k_s/R_t$ ratio for a biofouled surface. The biofilms tested in this study were largely non-uniform in terms of both their distribution over the test plates and the thickness
Chapter 9 - Discussion

of the biofilm. Table 9.2 summaries the \( k_s/R_t \) ratios for all test plates, using the \( k_s \) values from the total drag measurements and \( R_t \) values from the top and bottom of the test plate respectively.

Schultz and Flack (2005) determined \( k_s/k \sim 1 \) for their uniform spheres, which is comparable to the \( k_s/R_t \sim 1.2 \) for the sandgrain roughness in the present study. In contrast, Krogstad et al. (1992) had \( k_s/k \sim 3 \) for their mesh roughness, and Krogstad and Antonia had \( k_s/k \sim 6 \) for their transverse bar roughness.

Table 9.2 \( k_s/R_t \) ratios for all test plates, based on total drag measurements

<table>
<thead>
<tr>
<th></th>
<th>RP Lab</th>
<th>RP1 F1</th>
<th>SP1 F2</th>
<th>SP2 F3</th>
<th>RP1 F4</th>
<th>RP2 F5</th>
<th>SP1 F6</th>
</tr>
</thead>
<tbody>
<tr>
<td>( k_s/R_t )</td>
<td>1.2</td>
<td>2.7</td>
<td>1.2</td>
<td>0.4</td>
<td>2.4</td>
<td>1.8</td>
<td>4.8</td>
</tr>
<tr>
<td>( k_s/R_t )</td>
<td>1.2</td>
<td>1.7</td>
<td>0.3</td>
<td>0.6</td>
<td>2.2</td>
<td>1.6</td>
<td>0.5</td>
</tr>
</tbody>
</table>

9.3 RELATING ROUGHNESS TO DRAG

The objective of any roughness study is to relate the physical characteristics of a surface to the drag it produces. It was noted in Chapter 2 that rough surfaces cause a shift in the velocity profile when plotted in inner coordinates. This shift, known as the roughness function, is a measure of the drag penalty of a surface.

The roughness functions for the rough and biofouled surfaces are shown in Figure 9.2 plotted against the roughness Reynolds number based on the equivalent sandgrain roughness. This figure demonstrates that the rough fouled surfaces had both higher roughness functions and roughness Reynolds numbers than the clean rough surface. The equivalent sandgrain roughness was calculated using Nikuradse’s sandgrain roughness function; thus the results plotted in Figure 9.2 collapse onto that curve.

However, data which relates the roughness function to a physical roughness characteristic of the surface are much more useful. The roughness function is plotted against the roughness Reynolds number based on the maximum peak-to-valley height from the photogrammetry measurements in Figure 9.3 (i.e. \( k' = R_t u^*/\nu \)). The \( R_t \) values used were those measured on the top half of the test plate, nearest to where the boundary layer profiles were taken, not the average values for each test plate, due to the highly heterogeneous nature of the biofilms on some of the test plates. The roughness function relationship of Lewkowicz and Das (1985) is shown for comparison, along with the marine biofilm data of Schultz and Swain (1999) and Schultz (2000).
Figure 9.2 Roughness functions vs. roughness Reynolds numbers based on $k_s$ at $x = 850$ mm

Figure 9.3 Roughness functions vs. roughness Reynolds numbers based on $R_l$
Lewkowicz and Das (1985) found that the roughness function for their model algae, consisting of uniformly distributed nylon tufts attached to a rough plate, collapsed well to Equation 9.1 for $40 \leq k^+ \leq 400$. Schultz (2000) found that the roughness functions for surfaces covered with filamentous marine algae (up to 71 mm in length) behaved like $k$-type roughnesses; however, data was only obtained for very similar $u^*$ values. Conversely, Schultz and Swain (1999) found that plots of $\Delta u^+$ vs. $k^+$ did not show a good collapse to the Nikuradse sandgrain roughness function (Equation 2.28), using either the mean biofilm thickness or the rms biofilm thickness for marine biofilms dominated by low-form gelatinous slimes.

$$\Delta u^+ = \frac{0.89}{\kappa} \ln(k^+) + 1.055$$  \hspace{1cm} Equation 9.1

The data for the sandgrain surface, RP Lab, correlates well with the Nikuradse sandgrain roughness function (Equation 2.28), as expected. The fouled plate data also exhibits a $k$-type roughness function, and is much less scattered than the data of Schultz and Swain (1999) and Schultz (2000). RP2 F5, which was exposed for approximately 15 weeks, shows a closer correlation to the Nikuradse sandgrain roughness function than the other fouled test plates.

The fouled plate measurements, with the exception of the data for RP2 F5, are fitted to a logarithmic curve in Figure 9.4. Writing the relationship in similar form to Lewkowicz and Das (1985) gives:

$$\Delta u^+ = \frac{1.185}{\kappa} \ln(k^+) - 3.932$$  \hspace{1cm} Equation 9.2

The existence of a relationship between a physical roughness height of the biofilm and the roughness function is unexpected. The photogrammetry measurements do not take into account the vibration of the biofilms under flow conditions, and were thus not expected to scale well with the roughness function. The roughness function presented in Equation 9.2 is only valid for the low-form gelatinous freshwater biofilms measured in this study.
Figure 9.4 Relationship between roughness Reynolds number and roughness function for measurements on freshwater diatoms

Figure 9.5 Relationship between roughness maximum peak-to-valley height, $R_t$, and total drag coefficient, $C_D$, for measurements on freshwater diatoms
Similar results were found when the average maximum peak to valley height was compared to the drag coefficient, as shown in Figure 9.5. In this case, the average $R_t$ value for each test plate was used, as the drag coefficient represents the drag on the entire test surface, rather than a localised area as in the boundary layer measurements. The scatter in the data is expected, given that biofilms are living and of highly variable roughness. The results indicate that the drag coefficient is a roughly linear function of the average maximum peak-to-valley height of the biofilm over a sample length of 90 mm, given by Equation 9.3.

$$C_D = 0.0030R_t + 0.0032 \text{ where } R_t \text{ is in } [\text{mm}]$$  

Equation 9.3

### 9.4 ADHERENCE TO WALL SIMILARITY AND THE EFFECT OF BIOFILMS ON BOUNDARY LAYER STRUCTURE

Townsend’s Wall Similarity Hypothesis was introduced in Chapter 2, including summaries of recent studies which both support and contest wall similarity. The majority of the recent studies have considered regular idealised roughness patterns, such as sandpaper, woven meshes, or closely packed spheres, and were concerned with solid roughness types. The surfaces investigated in this study were compliant freshwater biofilms that vibrated under flow conditions and were physically very different to the roughnesses used in previous studies.

The wall similarity hypothesis postulates that the structure of the boundary layer outside the roughness sublayer is not affected by surface condition at sufficiently high Reynolds numbers, with the underlying assumption that the roughness height is small compared to thickness of the boundary layer. This implies that the effects of surface roughness are confined to the immediate vicinity of the roughness elements in the sublayer (usually assumed to extend up to $5k$); and that the turbulence structure over a significant portion of the boundary layer should be independent of the surface characteristics of the wall, and will thus collapse for all types of surfaces when suitably scaled.

It has been suggested that wall similarity exists provided $\delta/k \geq 40$ (Jimenez 2004) or $\delta/k_s \geq 40$ (Flack et al. 2005). The boundary layer profile results for each test plate at $x = 850$ mm are summarised in Table 9.3, in similar format to the summary tables (Table 2.3 and Table 2.4) for studies that support and contest the wall similarity hypothesis. The $R_t$ values given are representative of the surface in the region where the boundary layer profiles were taken.
Chapter 9 - Discussion

The scale separation in the present boundary layer profiles is not within the limits suggested above for any of the rough test plates, whether in the clean or fouled condition. However, $\delta/k \geq 40$ and $\delta/k_s \geq 40$ were satisfied for all of the smooth fouled plates. This indicates that wall similarity may not hold for the rough test plates, but should hold for the smooth test plates.

Following Schultz and Flack (2005), if the extent of the roughness sublayer is taken to be $5k_s$, then the roughness sublayer corresponds to $(y+\epsilon)/\delta < 0.30$ for the sandgrain rough plate, and up to $(y+\epsilon)/\delta < 0.71$ for the biofouled plates, which is well into the outer region of the boundary layer. The average $5k/\delta$ and $5k_s/\delta$ values for all of the test plates are given in Table 9.3. These figures are similar to the mesh roughness of Krogstad et al. (1992) which had $y/\delta < 0.33$, and to $y/\delta < 0.66$ for the transverse bar roughness of Krogstad and Antonia (1999). Their data did not support the concept of wall similarity. In contrast, Schultz and Flack (2005) had $y/\delta < 0.16$ and Ligrani and Moffat (1986) had $y/\delta \sim 0.1$ for uniform sphere roughnesses.

The use of different analysis methods was found to impact on the collapse of the data, particularly for the velocity defect plots. The rough wall data, analysed using Perry and Li’s Method, collapsed well for various degrees of fouling, and for the clean sandgrain surface in both the inner and outer regions of the boundary layer. The smooth wall profiles did not collapse completely with the rough wall profiles, even when analysed using Perry and Li’s Method.
Table 9.3 Scale separation for rough and biofouled measurements at $U = 2.00$ m/s and $x = 850$ mm

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<th>$U$ [m/s]</th>
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<th>$\delta$ [mm]</th>
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The studies presented in Section 2.2.6.1 that contested the wall similarity hypothesis generally found that the wake parameter, $\Pi$, differed for varying degrees of roughness. The wake values in the present study for the rough and fouled test plates determined using Perry and Li’s Method were fairly similar, and consistently higher than those for the smooth plates, which were determined using Bradshaw’s Method.

However, the collapse of the fouled plate results with the clean rough plate results supports the wall similarity hypothesis, and indicates that the presence of the fouling at the wall does not alter the mean velocity structure in the outer region of the boundary layer.

Streamwise turbulence intensity profiles for the clean rough and fouled rough plates showed reasonable collapse in the outer region of the boundary layer, for $(y+\epsilon)/\delta > 0.2$. The peak in the turbulence intensity was further into the boundary layer for the fouled plates than for the rough plate, indicating that fouling does affect the structure of the boundary layer in the inner region. The streamwise turbulence intensity profiles did not collapse for the clean smooth plate and the fouled smooth plates. SP1 F2: the more heavily fouled smooth plate exhibited a peak similar to that observed for the fouled rough plates. SP2 F3 and SP1 F6 behaved similarly to the smooth clean plate, but with slightly elevated turbulence intensity profiles over much of the boundary layer; this indicates that the fine biofilm layer is causing the mean velocity to fluctuate more than for a smooth surface. The turbulence intensity profile for the artificial biofilm, presented in Chapter 8, was elevated in the region $0.08 < (y+\epsilon)/\delta < 0.4$ (3 mm < y < 15 mm), which corresponds to the observed maximum extent of outward movement of the streamers.

The wall-normal turbulence intensity profiles for the clean and fouled rough plates collapsed well in the outer region of boundary layer, for $(y+\epsilon)/\delta > 0.1$. There was a noticeable peak in the near-wall region for the hydraulically rough profiles which was not evident in the smooth plate profiles. The peak for the fouled plate, RP2 F5, was slightly further into the boundary layer than the peak for the clean rough plate, RP Lab. Like the streamwise turbulence intensity profiles, the wall-normal turbulence intensity profile for the lightly fouled smooth plate, SP1 F6, was slightly elevated above the smooth plate profile over much of the boundary layer.

The Reynolds normal and shear stress profiles are discussed in detail in Section 7.3. It was found that the data collapsed in the outer region, $(y+\epsilon)/\delta > 0.1$, of the boundary layer when scaled using $\tau$ determined from the Total Stress Method. This supports the wall similarity hypothesis, even though the scale separation between the boundary layer thickness and the physical roughness height is small. The structure of the boundary layer in the near-wall region was found
to be different for the smooth and rough surfaces, particularly for the wall-normal Reynolds normal stress and the Reynolds shear stress, for which there was a significant peak in the rough wall data that was not present in the smooth wall profiles. The streamwise Reynolds normal stress profiles for the artificial biofilm, presented in Chapter 8, are significantly elevated above smooth wall data in the region \(0.08 < (y+\epsilon)/\delta < 0.4\) (3 mm < \(y < 15\) mm) in outer coordinates, and approximately 150 < \(y^+\) < 900 in inner coordinates.

The use of different analysis methods, i.e. Bradshaw’s Method for the smooth plates and Perry and Li’s Method for the rough plates, does not satisfactorily collapse the data. The determination of the boundary layer friction parameters is difficult for rough surfaces. Most of the wall shear stress determination methods that are used for smooth walls are either not feasible or have increased uncertainty for rough walls (Schultz & Flack 2005). A method which can be used for both smooth and rough surfaces with reasonable measurement uncertainty is needed to allow objective comparison of the structure of smooth and rough wall turbulent boundary layers.

A quadrant analysis was undertaken in Chapter 7 to further investigate individual contributions to the Reynolds shear stress. Good agreement was observed in the outer region of the boundary layer for the smooth and rough plate profiles for \(H = 0\). The Q2 and Q4 contributions for RP Lab and RP2 F5 are significantly higher in the near-wall region, \((y+\epsilon)/\delta < 0.1\), than the contributions for the smooth plates, which agrees with the results of Schultz and Flack (2005).

The stronger turbulence events were examined with \(H = 2\). The profiles for all of the test plates exhibited good agreement for the stronger Q2 (ejection) and Q4 (sweep) events, with the exception of the near-wall region, \((y+\epsilon)/\delta < 0.1\). The trends observed in the near-wall region were very similar to those observed by Krogstad et al. (1992) and Schultz and Flack (2005). The ratio of the Q2 to Q4 events for both \(H = 0\) and \(H = 2\) also showed good agreement in the outer region of the boundary layer.

Assessment of the test plates using the criteria given by Jimenez (2004), \(\delta/k \geq 40\), or Flack et al. (2005), \(\delta/k_s \geq 40\), did not provide an accurate indication of whether or not wall similarity would hold for freshwater biofilms. The majority of the surfaces reported here would not have been expected to exhibit wall similarity given these limits. However, the present results do show support for the wall similarity hypothesis for flows over low-form freshwater biofilms. The presence of biofilms at the wall only modified the flow in the near-wall region of the boundary layer. The turbulence structure of the outer region of the boundary layer was found to be similar to that for smooth wall profiles.
Schultz and Flack (2007) suggest that the most significant changes to the outer flow occur with two dimensional roughnesses, such as the transverse rods of Krogstad and Antonia (1999). Three dimensional surfaces such as uniform spheres (Schultz & Flack 2005) or sandpaper (Flack et al. 2005) tend to affect the boundary layer structure only in the near-wall region. The ratio $k_s/R_t$ (see Table 9.2) is suggested as an indicator of extent of influence of surface roughness, where closely packed three-dimensional roughnesses have $k_s/k \sim 1$. In contrast, Krogstad et al. (1992) had $k_s/R_t \sim 3$ for their mesh roughness, and Krogstad and Antonia (1999) had $k_s/R_t \sim 6$ for their transverse bar roughness. In the present study, the results lie in between the typical three-dimensional roughness values, and those for the mesh roughness of Krogstad et al. (1992).

The results for the filamentous artificial biofilm indicate that wall similarity may be absent in such cases, as the motions of the filaments affect the turbulence structure in the outer region of the boundary layer. Further work is recommended to determine the downstream and lateral extents of elevated turbulence production from the streamer motion, and the effects on the wall-normal fluctuation components and Reynolds shear stress.
10 CONCLUSIONS AND RECOMMENDATIONS

The purpose of this study was to quantify the effects of the freshwater biofilms *Gomphonema tarralleahae* and *Tabellaria flocculosa*, commonly found in the hydropower canals in the Tarraleah Power Scheme in Tasmania, Australia, on the structure of turbulent boundary layers and to advance the understanding of the mechanisms for drag production by biofilms. The applicability of the wall similarity hypothesis for flows over biofilms was also assessed.

This chapter presents the conclusions of the present study. Recommendations for future work are also given.

10.1 CONCLUSIONS

10.1.1 Field Study

The field study presented in Chapter 3 examined the ramifications of different upgrade options on biofilm growth, and involved installing a series of small test plates in the canal and studying the growth of the biofilm over the summer period. The major conclusions from this study were:

- In terms of biofilm retardation the best option is to reline and paint the canal; and
- Painting over the existing deteriorated concrete surface without relining is preferable to taking no other action in terms of biofilm retardation. However, over time the biofilm does establish and the current regime of draining and scrubbing the canal approximately annually would need to continue in order to maintain an acceptable flow rate in Tarraleah No.1 Canal.

10.1.2 Turbulence Measurement

The measurement of three-dimensional unsteady velocity fluctuations and Reynolds stresses is vital in advancing the understanding of the interaction of freshwater biofilms with flowing water. The possibility of using Pitot probes closely coupled to Validyne differential pressure transducers to measure unsteady velocity fluctuations was investigated in Chapter 5.
Unfortunately, this measurement system could only achieve a frequency response of up to 10 Hz, at the sacrifice of diaphragm sensitivity, probe tip diameter, and probe length. This was not adequate to capture all of the energy containing turbulent eddies expected in either the freestream or in the boundary layer, and the turbulence intensity was significantly underestimated. Thus whilst Pitot probes are good for measuring time mean velocities, they are not suitable for measuring fluctuating streamwise velocity in this situation. A two-dimensional Laser Doppler Velocimetry system was subsequently purchased and used to obtain the required turbulence information.

10.1.3 Data Analysis

The determination of the wall shear stress is particularly difficult for rough walls. Several methods of analysis were investigated in Chapter 6 and critically assessed for their accuracy, reliability and ability to produce realistic results. The major conclusions were:

- The Log Law Slope Method did not provide consistent solutions for either smooth or rough test plates and significantly overestimated most of the boundary layer parameters for the rough plate. It was thus not selected for subsequent data analysis;
- Hama’s Method significantly underestimated the wall friction velocity for the smooth plate and did not provide consistent results for the rough plate. It was thus not selected for subsequent data analysis;
- Bradshaw’s Method provided consistent results with low measurement uncertainty, and was thus used as the primary analysis method for all smooth wall boundary layer profiles;
- Perry and Li’s Method also provided consistent results with low measurement uncertainty for rough surfaces, and was used as the primary analysis method for all rough wall boundary layer profiles;
- The Total Stress Method was found to underestimate the wall shear velocity. However, where two-dimensional boundary layer measurements were made, it was used to check the results from Bradshaw’s and Perry and Li’s methods; and
- A consistent approach was devised for the analysis of the total drag measurements from the floating element force balance that allowed the result for smooth, rough, and biofouled surfaces to be compared.
Chapter 10 - Conclusions and Recommendations

The use of different boundary layer analysis methods, i.e. Bradshaw’s Method for the smooth plates and Perry and Li’s Method for the rough plates, did not satisfactorily collapse the data. A method which can be used for both smooth and rough surfaces with reasonable measurement uncertainty is needed to allow objective comparison of the structure of smooth and rough wall turbulent boundary layers.

10.1.4 The Effects of Biofilms on Skin Friction

Six different biofouled surfaces were investigated during the present study. The biofilms were grown under flow conditions in Pond No.1 in the Tarraleah Power Scheme, over varying incubation times from two weeks to 12 months. The following conclusions are made in regard to the effects of biofilms on skin friction:

- Smooth and rough test plates covered with the freshwater diatoms *T. flocculosa* and *G. tarraleahae* had increased drag coefficients and local skin friction coefficients above the clean surface values;

- Test plates exposed for 12 months exhibited large increases in drag coefficient, $C_D$. A fouled rough test plate ($R_t = 2.73$ mm) had a 22% increase over the clean rough plate value, and a 210% increase from a smooth plate value. A fouled smooth test plate ($R_t = 1.03$ mm) had a 68% increase in drag coefficient. Similar increases in local skin friction coefficient, $c_f$, were observed;

- Test plates exposed for 2 weeks also had increases in $C_D$. A fouled rough test plate ($R_t = 1.87$ mm) had 13% increase over the clean rough plate value, and a 189% increase from a smooth plate value. Similar increases in local skin friction coefficient were observed. A fouled smooth test plate ($R_t = 0.20$ mm) exhibited an 8% increase in drag coefficient. However, the boundary layer profile results revealed a decrease in local skin friction coefficient, indicating that an establishing biofilm on a smooth surface may in fact improve the drag characteristics, which is worthy of further investigation;

- Higher relative increases in both total drag coefficient and local skin friction coefficient were measured for smooth fouled plates than for rough fouled plates, which has implications for canal operation and the selection of appropriate protective surface coatings;

- Significantly higher drag coefficients and equivalent sandgrain roughnesses were experienced on the test plates with a sandgrain substrate. The biofilms were also
observed to be much thicker on the rough plates. This indicates that the substrate is an important parameter in terms of biofilm growth;

- The biofilms established within two weeks of exposure, and were observed to establish on the rough substrates before the smooth substrates;

- Results for lightly fouled sections on rough test plates indicated that the biofilm may smooth the substrate. However, they still produce increased skin friction coefficients; and

- Biofilms are very unlike the typical solid engineering roughnesses. Their compliant nature allows them to move under flow conditions. Two mechanisms for energy dissipation were observed during the water tunnel measurements: (1) algae filaments were observed to flutter in three dimensions under flow conditions; and (2) the low-form gelatinous biofilms were observed to vibrate under flow conditions. Forcing of water through the biofilm mat may have contributed to this behaviour.

### 10.1.5 Relating Roughness to Drag

The objective of any roughness study is to relate the physical characteristics of a surface to the drag it produces. With regard to biofilm roughness effects, the following conclusions are made:

- The freshwater low-form gelatinous biofilms displayed a $k$-type roughness function;

- The roughness function, $\Delta u^+$, can be related to the local maximum peak-to-valley height over the sample length, $R_n$, using: 
  \[
  \Delta u^+ = \frac{1.185}{k} \ln \left( \frac{R_u^*}{v} \right) - 3.932; 
  \]

- The drag coefficient, $C_D$, can be related to the average maximum peak-to-valley height over the sample length, $R_t$, using: 
  \[
  C_D = 0.0030R_t + 0.0032 \text{ where } R_t \text{ is in } [\text{mm}]; \text{ and}
  \]

- The existence of these relationships was unexpected as the photogrammetry measurements did not take into account the vibration of the biofilm under flow conditions; the roughness function and drag coefficient were therefore not expected to scale well with the roughness values observed under static conditions.
10.1.6 Wall Similarity and the Structure of the Turbulent Boundary Layer

An extensive review of the studies investigating the wall similarity hypothesis for turbulent boundary layers was given in Chapter 2. Previous studies have considered only solid, regular, idealised roughness patterns, whereas the present study considers compliant freshwater biofilms. The following conclusions were drawn, based on the results presented in Chapters 7 and 8:

- Assessment of the boundary layer profiles over the test plates using the criteria given by Jimenez (2004), $\delta/k \geq 40$, or Flack et al. (2005), $\delta/k_s \geq 40$, did not provide an accurate indication of whether or not wall similarity would hold for freshwater biofilms;
- The present results show support for the wall similarity hypothesis for flows over low-form freshwater biofilms;
- The presence of biofilms on the wall only modified the flow in the near-wall region of the boundary layer;
- A significant near-wall peak was observed in the wall-normal Reynolds normal stress and Reynolds shear stress profiles for rough walls. These peaks were not present in the smooth wall profiles;
- The turbulence structure of the outer region of the boundary layer was found to be similar to that for smooth wall profiles. The Reynolds normal and shear stress profiles and quadrant analyses all showed good agreement for different test plate surfaces in the outer region of the boundary layer; and
- The results for the filamentous artificial biofilm indicate that turbulent flow over filamentous biofilms may not exhibit wall similarity, as the motions of the filaments affect the turbulence structure in the outer region of the boundary layer. These observations were made directly downstream of a filament.
10.2  RECOMMENDATIONS

10.2.1 Growth of Biofilms

It is recommended that an investigation be undertaken which examines the stalking behaviour of freshwater diatoms, and whether or not they prefer certain smooth surface coatings, as *G. tarraleahae* grew longer stalks on one surface coating than on another.

Two test plates which were installed in the same location for approximately the same amount of time, and thus experienced the same flow, light, and nutrient conditions, had considerably different biofilm composition. This may be due to the colour and/or the roughness of the surface, and warrants further investigation.

The flow over a smooth test plate with a very light, uniform cover of biofouling was in the hydraulically smooth regime for both the boundary layer and drag measurements, and exhibited a lower local skin friction coefficient than that for a clean smooth surface. This is a very interesting result, as it means that the establishing biofilm has little effect on the boundary layer in terms of drag production. It would be beneficial to obtain more data on establishing biofilms to determine if they do in fact reduce the skin friction coefficient during the early stages of development.

10.2.2 UTAS Water Tunnel and Photogrammetry Process

The following recommendations are made regarding the UTAS Water Tunnel and the photogrammetry process:

- The LDV setup should be adjusted to allow measurements to be taken on the bottom half of the test plates, as the more heavily fouled sections were consistently inaccessible by the LDV measuring volume in the present study;

- The working section should be replaced with better quality materials that minimise thermal distortions and have superior optical characteristics to improve LDV measurements;

- The incorporation of high speed video should be investigated, particularly if it could be synchronised with instantaneous velocity measurements. This would allow the movements of the filamentous biofilms to be correlated with turbulence production; and
• Underwater photogrammetry methods should be developed to characterise biofilms in their natural, submerged environment, and possibly under flow conditions.

10.2.3 Data Analysis

Despite the numerous investigations of rough wall turbulent boundary layers over the last forty years, a satisfactory method for determining the wall shear stress is yet to be devised. A method to determine the shear stress which can be used for both smooth and rough walls with reasonable measurement uncertainty is needed to allow objective comparison of the structure of smooth and rough wall turbulent boundary layers.

10.2.4 Filamentous Biofilms

It is recommended that further work be done on filamentous biofilms to determine the downstream and lateral extents of elevated turbulence production from the motion of the filaments. Two-dimensional boundary layer measurements would also be very beneficial, as they would allow more accurate assessment of the applicability of the wall similarity hypothesis for turbulent flow over filamentous biofilms.