Drag Reduction via Manipulation of Large-scale Coherent Structures in a High Reynolds-number Turbulent Boundary Layer

by

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This dissertation is dedicated to my parents and grandparents...
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Abstract

The large-scale coherent structures in the outer region of a high-Reynolds-number turbulent boundary layer (TBL) have been shown to be highly energetic with an influence that extends down to the wall and thus directly affects the skin friction. This thesis is concerned with a drag reduction strategy that involves targeting these large-scale motions. The notion of high-Reynolds-number TBL refers to the case where an outer site in the spectrogram of the streamwise velocity fluctuations emerges, which is a manifestation of the energy of the large-scale structures. The largest length of the manipulated large-scale motions in this study measures $10\delta$ in the streamwise direction (where $\delta$ is the boundary layer thickness).

The current experimental study investigates a feedforward control scheme in which the large-scale motions and very large-scale motions of a zero-pressure-gradient TBL at an approximate friction Reynolds number of 14 400 were manipulated selectively. An array of nine wall-shear stress sensors—0.07$\delta$ apart in the spanwise direction—was utilized to measure the wall-shear stress fluctuations. The wall signature of the large-scale structures was resolved in real-time from the fluctuating signal of each individual wall-shear stress sensor. At 1.6$\delta$ downstream of the sensing point, an array of nine rectangular wall-normal jets was designated, each aligned in the streamwise direction with a corresponding wall-shear stress sensor, forming nine sensor-actuator pairs. On/off wall-normal jet airflows through the rectangular planes provided the actuation, and the penetration height reached the upper-bound of the log-region.

Large-scale structures possess bilateral characteristics; their instantaneous streamwise velocity are either higher or lower than the mean streamwise velocity at each wall-normal height. These high- and low-speed regions are accompanied by respective down- and up-ward wall-normal velocity components. In a conditional sense, this results in a manifestation of counter-rotating roll modes in the spanwise–wall-normal plane. Therefore, the wall-normal jet actuators were programmed to be synchronized with either the high- or low-speed regions. As the wall-normal jet actuation was synchronized with the high-speed events, it was implicitly synchronized with the downwash sections of the counter-rotating roll modes. This led to an opposition mechanism of the control scheme (opposing control scheme), and the intensity of the high- and low-speed events was reduced. A maximum reduction of 3.2% in the mean wall-shear stress was measured at 1.6$\delta$ downstream of the actuators. The opposite occurred when the wall-normal jet actuation was synchronized with the low-speed events. For this
type of manipulation, the actuation was implicitly synchronized with the up-wash sections of the counter-rotating roll modes. This led to a reinforcing control mechanism (reinforcing control scheme) with regard to the intensity of the roll modes. The energetic high- and low-speed events were also enhanced. However, a maximum mean wall-shear stress reduction of 1.2% was measured at $1.6\delta$ downstream of the actuators. It can be concluded that the detrimental top-down influence of the more energetic large-scale structures was overpowered by the beneficial influence of the streamwise momentum deficit downstream from the actuators. The streamwise momentum deficit is the inevitable by-product of the introduction of wall-normal jet into cross flow.

A third control scheme investigated actuation that was synchronized with neither the high- nor the low-speed regions. Hence, there was no underlying control logic, and the results served as a baseline case. A 2.4% maximum reduction in the mean wall-shear stress was measured at $1.6\delta$ downstream of the actuators. No behavioral change in the energy of the large-scale structures was observed. Thus, the entire amount of the associated skin friction reduction can be attributed to the generated streamwise momentum deficit downstream of the actuators.

In summary, the experimental results support the conjecture of this thesis. That is, the energetic large-scale structures in the outer-region of a high-Reynolds-number TBL can, indeed, be decreased, and this results in their reduced top-down influence on the wall. Reduction in the large-scale component of the energy of the wall-shear stress fluctuations together with the mean wall-shear stress reduction is observed. If wall-normal jet airflows are used as the large-scale forcing, they ought to be synchronized with the high-speed events in order to obtain the above-mentioned results.
Declaration of Authorship

This is to certify that:

- The thesis comprises only my original work towards the PhD except where indicated in the Preface,
- Due acknowledgment has been made in the text to all other material used,
- The thesis is fewer than 100,000 words in length, exclusive of tables, maps, bibliographies and appendices.

Signed: 

Date: 

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Preface

The work presented in this thesis is my own. The only exceptions are the figures within the Introduction, which have been reproduced from previous works. The authors of those works have been appropriately cited.
Acknowledgments

It has been a great honor for me that I had the opportunity of working under the supervision of Professor Ivan Marusic, Professor Nicholas Hutchins and Dr. Woutijn J. Baars. I express my utmost gratitude to each one of my advisors sincerely, and this can never be enough. Their invaluable insight into fluid mechanics and their unceasing willingness to share them with me have both helped and guided me immensely during the course of my PhD journey.

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Publications


The above-mentioned paper was awarded the Tony Perry Prize for an outstanding research-paper presentation by a student at the 20th Australian Fluid Mechanics Conference, 2016.

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<td>Constant Temperature Anemometer</td>
</tr>
<tr>
<td>DR</td>
<td>Drag Reduction</td>
</tr>
<tr>
<td>FIK</td>
<td>Fukagata Iwamoto Kasagi</td>
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<tr>
<td>HRNBLWT</td>
<td>High Reynolds Number Boundary Layer Wind Tunnel</td>
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<tr>
<td>LDA</td>
<td>Laser Doppler Anemometry</td>
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<td>LEBU</td>
<td>Large Eddy Break Up</td>
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<td>LSM</td>
<td>Large Scale Motion</td>
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<td>MDR</td>
<td>Maximum Drag Reduction</td>
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<td>MEMS</td>
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<td>TET</td>
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<td>TTL</td>
<td>Transistor Transistor Logical</td>
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<td>VITA</td>
<td>Variable Interval Time Averaging</td>
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<td>VLSM</td>
<td>Very Large Scale Motion</td>
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Nomenclature

$(x' - y' - z')$  Local coordinate system associated with the actuators

+  Viscous scaled parameters based on uncontrolled inner-variables

$-\bar{uu}_w \frac{\partial \bar{U}}{\partial z}$  Production of turbulence kinetic energy

$\Delta t$  Controller sampling rate

$\Delta x$  Streamwise separation between the sensors and actuators

$\Delta x_{GF}$  Streamwise Length of the Gaussian filter

$\Delta y$  Spanwise spacing of the sensor-actuator pairs

$\Delta y_{GF}$  Spanwise width of a two-dimensional Gaussian filter

$\delta$  Boundary layer thickness

$\gamma_{uu_r}^2$  Coherence magnitude between streamwise and friction velocity fluctuations

$\kappa$  Von Kármán constant of the log law

$\kappa_x$  Wavenumber of the wall forcing

$\lambda$  Wavelength of transpiration

$\lambda_x$  Wavelength of the streamwise velocity fluctuations

$\lambda_{xF}$  Cut-off wavelength of the Gaussian filter

$\nu$  Kinematic viscosity

$\omega$  Temporal frequency of the wall forcing
\[ \bar{\tau}_p \] Mean polymer stress

\[ \bar{\tau}_w \] Mean wall-shear stress

\[ \bar{u}^2 \] Streamwise velocity variance

\[ \bar{uw} \] Reynolds shear stress

\[ \bar{u}_{lam} \] Mean bulk flow velocity corresponding to laminar flow

\[ \phi_{uu,r} \] Complex-valued cross-spectrum density between streamwise and friction velocity fluctuations

\[ \phi_{ur,ur} \] Power spectra density of the friction velocity fluctuations

\[ \phi_{uu} \] Power spectra density of the streamwise velocity fluctuations

\[ a_{1-9} \] Demarcation of the actuators

\[ s'_{1-9} \] Demarcation of the downstream hot-film sensors

\[ s_{1-9} \] Demarcation of the upstream hot-film sensors

\[ \sigma_i(e) \] Standard deviation of the hot-film voltage signal

\[ \tau \] Time coordinate of the conditional velocity

\[ \tau_e \] Single controller time step

\[ \tau_c \] Extra output temporal delay introduced by the controller

\[ \tau_f \] Time delay corresponding to half of the Gaussian width

\[ \tau_i \] Temporal lead of the actuation due to forward inclination of the large-scale structures

\[ \tau_m \] Time delay accumulates from the mechanical activation of the jet flow

\[ \tau_w \] Wall-shear stress fluctuations

\[ \tau^+_L \] Auto-correlation time-lag normalized with uncontrolled inner-variables

\[ \theta \] Momentum thickness
\( \tilde{u} \) Mean streamwise velocity conditioned on zero-crossings of the footprint of the large-scale fluctuations at the wall

\( A \) Amplitude of the wall oscillation

\( a \) Amplitude of transpiration

\( B \) Intercept constant of the log law

\( c \) Wave speed of transpiration

\( C(y_i, t) \) Binary field (serving as the TTL signals controlling the valves)

\( C_f \) Wall-shear stress coefficient

\( C_q \) Transpiration coefficient (\( \equiv V_j/U_\infty \))

\( d \) Sensing element diameter of the hot-wire

\( d_a \) Downstream distance relative to the actuators

\( e(y_i, t) \) Hot-film voltage signal

\( e_L(y_i, t) \) Large-scale component of the hot-film voltage signal

\( f_c \) Controller sampling frequency

\( k_{s}^+ \) Wall-normal step associated with glue-on sensors

\( k_x \) Wavenumber of the streamwise velocity fluctuations

\( l_F \) Streamwise length of the floating element sensor

\( l_m \) Streamwise length of the modular insert

\( l_{hf} \) Sensing element width of a hot-film

\( l_{hw} \) Sensing element length of the hot-wire

\( l_j \) Streamwise length of the jet slit

\( R_{u_u \tau} \) Auto-correlation coefficient of \( u_\tau \)
Nomenclature

$R_{uu}$  Auto-correlation coefficient of $u$

$Re_\tau$  Friction Reynolds number

$Re_\theta$  Reynolds number based on the momentum thickness

$T$  Temperature of the wind tunnel facility

$T^+$  Period of spanwise wall oscillation

$T^+_u$  Integral time-scale of $u_\tau$ normalized with *uncontrolled* inner-variables

$T^+_u$  Integral time-scale of $u$ normalized with *uncontrolled* inner-variables

$U$  Mean streamwise velocity

$u$  Streamwise velocity fluctuations

$U_c$  Convection velocity of the large-scale structures

$U_\infty$  Free-stream velocity

$U_\tau$  Mean friction velocity

$u_\tau$  Friction velocity fluctuations

$v$  Spanwise velocity fluctuations

$v_w$  Spanwise component of the wall forcing

$w$  Wall-normal velocity fluctuations

$w_F$  Spanwise width of the floating element sensor

$W_j$  Mean wall-normal velocity from the jet exit plane in still air

$w_m$  Spanwise width of the modular insert

$w_j$  Spanwise width of the jet slit

$x$  Streamwise coordinate

$x_1-x_9$  Streamwise centers of 9 modular inserts
Nomenclature

$x_F$ Streamwise coordinate of the center of the floating element

$y$ Spanwise coordinate

$y_1 - y_9$ Spanwise locations of 9 sensor-actuator pairs

$z$ Wall-normal coordinate

$z_L^+$ Wall-normal height of the outer-peak in the pre-multiplied energy spectrogram

$z_p^+$ Penetration height of jet airflow into cross flow
Chapter 1

Introduction

Turbulent flows are the predominant flows both in nature and engineering applications. The presence of a solid boundary or wall along the direction of a uniform viscous fluid flow introduces a new set of characteristic length and time scales into the shear free flow. Shear emerges both in the small and large scales and plays a vital role in the dynamics of the resultant flow. Consequently, the flow is no longer considered as shear-free, and at a sufficiently high Reynolds number it is known as a turbulent wall-bounded flow. These boundary layers lead to skin friction drag. In this region, the wall-normal gradient of the majority of the flow variables are the largest due to the no-slip boundary condition. As the boundary layer transforms from laminar to turbulent, the associated skin-friction drag increases. Turbulent skin-friction drag constitutes approximately 50%, 90% and 100% of the total drag on airliners, submarines and pipelines, respectively (Gad-el Hak, 1994). The constituent components of the total drag of a transport aircraft, each of which with its corresponding drag reduction potential (Schrauf et al., 2006) is illustrated in Figure 1.1. The annual global CO₂ emission from civil aviation has been estimated to rise up to 400 million tonnes in 2030 (Horton, 2006). A drag reduction of 1% for an aircraft at cruise conditions would reduce fuel consumption by approximately 0.75%. This would translate to nine million tonnes reduction in CO₂ emission per annum (Leszhiner et al., 2011). Therefore, skin-friction drag reduction has both economical and environmental importance.
From now on, the usage of the terms “boundary layer” and “drag” refer to “turbulent boundary layer” and “skin-friction drag”, unless otherwise mentioned.

Throughout this dissertation, $x$, $y$ and $z$ denote the streamwise, spanwise and wall-normal directions of the flow, respectively. Hence, the corresponding fluctuating velocity components are, represented by $u$, $v$ and $w$. The superscript ‘+$’ denotes normalization by the viscous scaling of the uncontrolled turbulent boundary layer and capitalized notation and over-bars indicate time-averaged values: for instance, $z^+ = z U_{\tau_U} / \nu$, where $\nu$ is the kinematic viscosity and $U_{\tau_U} = (\tau_{w\tau} / \rho)^{1/2}$ is the uncontrolled mean friction velocity—the subscript $U$ refers to the uncontrolled case. Tildes denote conditionally averaged quantities. $\phi_{uu}$ and $k_x$ are the power spectral density and the wavenumber of the streamwise fluctuating velocity, respectively.

Turbulent boundary layers comprise two major regions in the wall-normal direction: The near-wall region ($0 \leq z^+ \leq 30$) and the outer region ($0.02 \leq z/\delta \leq 1$), both of which are populated by coherent structures (Adrian et al., 2000; Kim and Moin, 1979; Kline et al., 1967; Robinson, 1991b; Smits and Marusic, 2013; Townsend, 1976; Wark

**Figure 1.1:** Drag breakdown of transport aircraft in cruise. Reproduced from Schrauf et al. (2006).
and Nagib, 1990). The near-wall region, in turn, comprises a viscous sub-layer and buffer layer, and the outer region is made up of the logarithmic and wake regions. The constituent structures of the near-wall region scale in viscous units: 1. The friction velocity, \( U_\tau \equiv \sqrt{\tau_w/\rho} \), where \( \tau_w \) is the mean wall-shear stress and \( \rho \) is the fluid density; and 2. The viscous length-scale, \( \nu/U_\tau \), where \( \nu \) is the kinematic viscosity of the fluid. In the outer region the appropriate length scale is the boundary layer thickness, \( \delta \).

Considerable effort has been devoted to reducing skin-friction drag over the past few decades by interrupting or interacting with the coherent structures, either passively or actively, as reviewed by a range of scholars (Coustols and Savill, 1992; Gad-el Hak, 1996; Gad-el Hak and Tsai, 2006; Lumley and Blossey, 1998; Moin and Bewley, 1994). The majority of the flow control studies with the aim of skin-friction reduction have attempted to manipulate structures within the near-wall region (e.g. Gad-el Hak, 2000; Karniadakis and Choi, 2003; Kasagi et al., 2009b; Moin and Bewley, 1994; Rathnasingham and Breuer, 2003). Therefore, most of these studies have tailored their control parameters in viscous-scaled units.

The characteristic Reynolds number of a wall-bounded flow is the friction Reynolds number, formulated as \( Re_\tau \equiv \delta^+ \equiv \delta/(\nu/U_\tau) \), and defined as the ratio between the outer and viscous length-scales. This implies that as the Reynolds number increases, the scale separation between the largest and smallest length-scales in the flow widens. At high, practical values of \( Re_\tau \), the physical thickness of the near-wall region and hence the size of the structures populating that region become smaller than their counterparts at low-Reynolds-number values (Gad-el Hak and Bandyopadhyay, 1994; Head and Bandyopadhyay, 1981; Robinson, 1991b). Due to the viscous length- and time-scales of turbulent boundary layers (TBLs) in engineering applications (\( Re_\tau \sim 10^3–10^6 \), Figure 1.2), which are of the order of \( [O(\mu m)] \) and \( [O(\mu s)] \), respectively, one needs to deal with micro-electro-mechanical systems (MEMS) for controlling the near-wall region. Both the development and operation of these sensors become demanding. At the same time, it has been shown that the performance of the control schemes, which are devised to solely manipulate the near-wall region, deteriorates as the \( Re_\tau \) increases (e.g. Chang et al., 2002; Gatti and Quadrio, 2013; Hurst et al., 2014; Iwamoto et al., 2002; Canton et al., 2016b).
Typical Reynolds numbers in boundary layer applications. $Re_L$ and $Re_\theta$ denote the Reynolds numbers based on the streamwise characteristic length and momentum thickness, respectively. $Re_\tau$ denotes the friction Reynolds number. Reproduced from Deck et al. (2014).

On the other hand, it has been discovered that the energy associated with the large-scale motions (LSMs) and very large-scale motions (VLSMs or superstructures) populating the log-region of a high-Reynolds-number turbulent boundary layer is comparable to that of the small-scale structures in the near-wall region (Abe et al., 2004; Hutchins and Marusic, 2007a,b; Hutchins et al., 2011; Marusic et al., 2010b). The characteristic length scales of LSMs and VLSMs are of the order of $O(2\delta-3\delta)$ and $O(10\delta)$, respectively (reviewed by Adrian 2007 and Marusic et al. 2010c). The criterion under which a high-Reynolds-number TBL is discerned from its low-Reynolds-number counterpart is chosen to be the emergence of an outer site in the spectrogram of the streamwise velocity fluctuations which requires to have Reynolds numbers of $Re_\tau \geq 2000$ (Hutchins and Marusic, 2007a). LSMs have been simplistically categorized as large elongated regions ($O(\delta)$) of streamwise momentum deficit with a forward leaning
inclination to the horizontal (low-speed event), flanked on either spanwise side by regions of streamwise momentum surplus (high-speed event, e.g. Adrian et al., 2000). These regions are accompanied by large-scale counter-rotating roll modes, with the respective up- and down-wash sections embodied within the low- and high-speed events (Dennis and Nickels, 2011a; Hutchins and Marusic, 2007b; Hutchins et al., 2012).

The large-scale structures in the log-region have an influence on both the near-wall region and the wall. The near-wall region influence is manifested in the amplitude modulation of the near-wall small-scale fluctuations (Hutchins and Marusic, 2007a; Marusic et al., 2010b) and the wall influence is manifested in the direct superimposition of the large-scale skin-friction fluctuations (Abe et al., 2004). This opens up an alternative approach to targeting the near-wall small-scale motions, and that is the use of a large-scale forcing scheme. It is hypothesized that the fluctuating energy associated to the energetic large-scale structures in the log-region of a high-Reynolds-number TBL can be diminished. Hence, the associated top-down influence would diminish. As a result, the large-scale components of the energy of the wall-shear stress fluctuations would also be reduced, which has the potential to result in the reduction of the mean wall-shear stress.

Two major types of large-scale manipulations are investigated in this dissertation: 1. desynchronized (i.e. predetermined) wall-normal jet actuation—Figure 1.3(a), and 2. synchronized (i.e. selective) wall-normal jet actuation—Figures 1.3(b) and (c). The synchronized scheme, in turn, can be divided into two sub-types of manipulations: 1. high-speed event manipulation (opposing control scheme)—Figure 1.3(b), and 2. low-speed event manipulation (reinforcing control scheme)—Figure 1.3(c).
Figure 1.3: Schematic representation of the desynchronized, opposing and reinforcing control schemes (a–c), with the respective predicted influence on the quadrants of the instantaneous streamwise–wall-normal ($u$–$w$) plane (d–f). For the opposing control scheme, the distribution of the lower part of the quadrants of the $u$–$w$ plane is predicted to contract towards the origin, solid arrow in (e), together with the contraction of the upper part of the quadrants of the $u$–$w$ plane, dashed arrow in (e). The former contraction is due to the direct influence of the wall-normal jet airflow on the down-wash sections of the counter-rotating roll modes and the latter contraction is due to the diminished counter-rotating roll modes. For the reinforcing control scheme, the distribution of the upper part of the quadrants of the $u$–$w$ plane is predicted to stretch from the origin, solid arrow in (f), together with the stretch of the upper part of the respective manipulated quadrants, solid arrow in (f). For the desynchronized control scheme, the quadrants $u$–$w$ plane exhibits the same distribution as those of the unmanipulated ones, (d), since any influence due to the manipulated high-speed events is counteracted by the opposite influence of the manipulated low-speed events.
The effects of the three above-mentioned types of manipulations on a high-Reynolds-number turbulent boundary layer at $Re_\tau \approx 14 400$ are investigated thoroughly. For this, the streamwise velocity fluctuations throughout the boundary layer, and the friction velocity fluctuations were measured using hot-wire anemometry. The illustrations in Figures 1.3(a–c) are only meant to depict the primary differences of the control schemes in terms of the control logic underlying each scheme.

Due to the random nature of the large-scale structure manipulation for the desynchronized control scheme, it is predicted that the respective quadrants of the instantaneous streamwise–wall-normal (i.e. $u$–$w$) plane would exhibit the same distribution as those of the unmanipulated ones—illustrated in Figure 1.3(d). Any influence due to the manipulated high-speed events are counteracted by the opposite influence of the manipulated low-speed events. For the opposing control scheme, the wall-normal jet airflow is believed to oppose the down-wash sections of the naturally occurring counter-rotating roll-modes. Hence, the distribution of the lower part of the quadrants of the instantaneous $u$–$w$ plane is predicted to contract towards the origin—illustrated in Figure 1.3(e) with solid arrow. The upper part of these quadrants is also predicted to be contracted, because of the diminished counter-rotating roll modes—illustrated in Figure 1.3(e) with dashed arrow. On the contrary, the opposite is predicted to occur for the reinforcing control scheme. As the wall-normal jet airflow reinforces the upwash sections of the counter-rotating roll-modes, the distribution of the upper part of the quadrants of the instantaneous $u$–$w$ plane is predicted to stretch from the origin—illustrated in Figure 1.3(f) with solid arrow. Likewise, the lower part is also predicted to exhibit the same behavioral change as that of the upper part of its respective manipulated quadrants due to the enhanced counter-rotating roll modes—illustrated in Figure 1.3(f) with dashed arrow. In summary, the solid and dashed arrows in Figures 1.3(d–f) can be considered to be the respective primary and secondary influences of each one of the manipulation schemes on the quadrants of the instantaneous $u$–$w$ planes.

Indeed, the feasibility of a large-scale flow control strategy in a turbulent boundary layer was shown by Schoppa and Hussain (1998) in a DNS study. They manipulated the streamwise vortices in the near-wall region of a turbulent channel flow at $Re_\tau \approx 180$
via superimposition of either large-scale streamwise counter-rotating vortices or colliding spanwise wall jets. As such, they reported a respective 20% or 50% reduction in the skin-friction drag. The drag reduction mechanism of such a scheme was the attenuation of the turbulence energy associated with the streamwise vortices in the near-wall region of a low-Reynolds-number turbulent boundary layer. Although the term “large-scale” is ubiquitous in both this study and the study by Schoppa and Hussain (1998), the drag reduction mechanism that is sought after here is not analogous to that pursued by Schoppa and Hussain (1998). They used large-scale forcing to manipulate the near-wall region of a low-Reynolds-number TBL at $Re_\tau \approx 180$ in a predetermined fashion. However, in this research large-scale forcing was utilized in order to selectively interact with the energetic large-scale structures in the log-region of a high-Reynolds-number TBL at $Re_\tau \approx 14400$. Unlike high-Reynolds-number TBLs, the large-scale structures in low-Reynolds-number TBLs are not energetic. Indeed, no distinct scale-separation between the energetic coherent structures exists at a low-Reynolds-number TBL. This can be evidenced in the unimodal shape of the associated spectrogram.

The order of scale of the large-scale forcing by Schoppa and Hussain (1998) differs to that that was used here. In other words, with regard to the associated order of scale, the usage of the term large-scale have different connotations between this study and that of Schoppa and Hussain (1998). The scale of the large-scale manipulation by Schoppa and Hussain (1998) was relatively larger than that of the structures that were diminished via their scheme—streamwise vortices in the near-wall region of a turbulent channel flow at $Re_\tau \approx 180$. However, the scale of the large-scale manipulation in this study is comparable to that of the targeted structures—energetic large-scale structures in a zero-pressure-gradient TBL at $Re_\tau \approx 14400$. Recently it has been shown that the reported drag reduction in Schoppa and Hussain (1998) was associated with a transient nature of the flow due to the low values of $Re_\tau$, at which the turbulence is marginally sustainable (Canton et al., 2016b). Moreover, Canton et al. (2016b) concluded that inducing large-scale vortices for drag reduction becomes ineffective at their highest Reynolds number ($Re_\tau \approx 550$).

The global objective of this experimental investigation can be divided into two sub-objectives: 1. diminishing the energetic large-scale structures in the log-region of a
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high-Reynolds-number turbulent boundary layer, 2. reduction of the mean wall-shear stress associated with the energetic large-scale structures. The experiments were conducted in the High-Reynolds-Number Boundary Layer Wind Tunnel (HRNBLWT) at the University of Melbourne. The large-scale forcing was employed via wall-normal jet airflow and the structure detection was carried out at the wall, upstream of the actuation point. Hot-wire anemometry methods were used in this study in order to measure the flow and wall variables of both the unmanipulated and manipulated TBLs. More specifically, a single hot-wire sensor was used for boundary layer surveys, and an array of hot-film sensors was used for wall shear stress measurements.

1.1 Thesis Outline

Prior to any attempt regarding active flow control of the turbulent boundary layer, it is essential to have a reasonably appreciable understanding of the coherent turbulence structures populating the turbulent boundary layer. Therefore, Chapter 2 focuses on the characteristics of the coherent structures in the turbulent boundary layer both in the near-wall and outer regions. Then turbulent boundary layer control strategies are reviewed. Depending on the mechanism of drag reduction, the flow control strategies are divided into two main classifications: 1. Attenuation of the near-wall turbulence (2.2.1), and 2. Attenuation of the outer region turbulence (2.2.2). The logic behind the implemented control schemes are presented and they are placed into one of the above-mentioned mainstream categories of skin-friction drag reduction.

Chapter 3 describes the wind tunnel facility in which the experiments were taken place. A detailed description of the characteristics of the sensors, actuators and the real-time controller is provided. This is followed by elaborating the nuances of the implemented control scheme. Finally, the measurement techniques with which the streamwise and friction velocity measurements were conducted are described. For both of these measurements, hot-wire anemometry was utilized. A novel approach to calibrate the hot-film sensors in order to compensate for the temperature drift is presented.
Chapter 4 presents both the results and interpretation of the experimental data acquired during the numerous measuring campaigns. Prior to that, in an effort to gain an insight into the accuracy of the large-scale structure manipulation, the coherence magnitude between large-scale component of the upstream friction velocity fluctuations with the downstream streamwise velocity fluctuations throughout the canonical turbulent boundary layer is quantified. All three control schemes (i.e. opposing, reinforcing and desynchronized control schemes) are investigated. Their successes in terms of fulfilling the desired main objectives of this study—weakening the large-scale structures in the outer region and alleviating the skin-friction drag associated with the energetic large-scale structures—are critically assessed. For such, quantitative analyses of both the streamwise velocity fluctuations (i.e. $u$) throughout the boundary layer and the friction velocity fluctuations (i.e. $u_\tau$) are conducted. These analyses include observations of the behavioral change of the mean, variance, conditional averages, energy spectra and integral time-scale of both $u$ and $u_\tau$ fluctuations. Furthermore, the behavioral change of the modulation coefficient of the large-scale component of the velocity fluctuations and the filtered envelope of the small-scale component of the velocity fluctuations throughout the boundary layer is examined.

The thesis is finalized in Chapter 5, which concludes that the results presented in Chapter 4 support the hypothesis postulated in the current Chapter. The control scheme which is evidenced to be most beneficial among the other investigated schemes in terms of skin-friction drag reduction (i.e. a maximum of 3.2% reduction in the mean wall-shear stress) is believed to have the potential to be modified in an effort to enhance the respective efficacy. Finally, the future perspective of this project is presented within two separate paradigms. The first paradigm requires no changes in hardware and solely attempts to modify the control software (i.e. implemented algorithm) and the second proposed paradigm deals with altering the control set-up.
Chapter 2

Review of the Relevant Literature

2.1 Coherent Structures in a Turbulent Boundary layer

Turbulent boundary layers (TBLs) were once considered as phenomena with random nature. As such, the study of these flows was limited to the observations of the profiles of the mean and often statistical quantities. Over the past 50 years or so, it has been shown that TBLs comprise coherent structures in both the near-wall and outer regions, as reviewed by, Kline et al. (1967); Townsend (1976); Robinson (1990, 1991b,a); Alfonsi (2006) and Marusic and Adrian (2010), among others. Historically, the turbulent structures have witnessed four distinct eras (Robinson, 1991b): 1. the discovery era (1932-1957); 2. the flow visualization era (1958-1971); 3. the conditional sampling era (1972-1983); and 4. the computer simulation era (1984 -present). Although the term “coherent structures” is widely used across literature, no consensus of the concept exists among the fluid community. Here, the definition of Robinson (1991a) is adopted:

*A coherent motion is defined as a three-dimensional region of the flow over which at least one fundamental flow variable (velocity component, density, temperature, etc.) exhibits significant correlation with itself or with another variable over a range of space and/or time that is significantly larger than the smallest local scales of the flow.*
One set of such coherent structures can be considered as the vortices populating the turbulent boundary layer. Due to their importance in characterizing the kinematics and dynamics of the turbulent boundary layers, Acarlar and Smith (1987a,b) defined these vortices as the “building blocks” of wall-bounded flows. Depending on the Reynolds number of the flow, they can be ideally characterized as either “horseshoe” or “hairpin” vortices. At low-Reynolds-number TBLs, they resemble mainly horseshoes (Theodorsen, 1952). As the Reynolds number increases, the resemblance transforms from horseshoes to hairpins (Head and Bandyopadhyay, 1981)—illustrated in Figure 2.1. Both the horseshoe and hairpin vortices possess the same topology; they are forward inclined, with their heads lifted away from the wall and pointed downstream and their legs close to the wall and pointed upstream—shown in Figure 2.2.

![Figure 2.1: Transformation of a horseshoe- into a hairpin-vortex as the Reynolds number increases in a turbulent boundary layer. Reproduced from Robinson (1991a).](image)

Based on the observations of the characteristics of a single hairpin vortex at high Reynolds number boundary layers, mathematical models have been proposed, in which the kinematics of an ideal hairpin vortex are mimicked. The most successful of such is the “attached eddy hypothesis” by Townsend (1976) which is further investigated by Perry and Chong (1982); Perry et al. (1986); Perry and Marusic (1995); Marusic and Perry (1995); Nickels et al. (2005, 2007); Hwang (2015); Woodcock and Marusic (2015) and Marusic (2001).
2.1.1 Structures in the Near-wall Region

The coherent structures populating the near-wall region of a turbulent boundary layer can be categorized as either the quasi-streamwise vortices or the velocity field associated with these vortices. The former are believed to be the legs of the hairpin vortices and exhibit a strong correlation with the higher skin-friction values on the wall (Kravchenko et al., 1993). The fact that these vortices are not necessarily symmetric plays an important role in the dynamics of the near-wall structures and is the cause of strong shear in the spanwise direction. The latter are the streamwise elongated low- and high-speed streaks in the viscous sub-layer of a turbulent boundary layer. These coherent structures were visualized for the first time using hydrogen bubbles in the pioneering work of Kline et al. (1967) and later simulated by Kim and Moin (1979) in an LES simulation of a channel flow.

Kline et al. (1967) associated the production and transport of turbulence within the boundary layer to the gradual lift-up, then sudden oscillation, bursting and ejection of the low-speed streaks. They showed that turbulence in a turbulent boundary layer is sustained due to the break-up process of the lifted vortex element which straddle
a low-speed streak—depicted in Figure 2.3. This process generates propagating turbulent spots, under which new wall-layer streaks are spawned at the wall, which, in turn, undergo the same breakup process as those that generated them. Kline et al. (1967) quantified the spanwise spacing of the low-speed streaks to be approximately 100 wall units. Furthermore, Smith and Metzler (1983) investigated the spanwise streak spacing as a function of Reynolds number. They realized that such spacing is invariant with Reynolds number and its probability distribution exhibits a similar shape for different Reynolds numbers, with a maximum distribution at a spanwise wavelength which is well scaled with inner variables.

Kim et al. (1971) claimed that the spanwise spacing among the streaks is due to the time scale balance of the streak formation, breakdown and vortex regeneration. For smaller spanwise spacing, the time scale of the three above-mentioned phenomena become mismatched and the regeneration cycle at that scale is broken. The sustainable process of turbulence production in the turbulent boundary layer was shown to possess both linear and non-linear mechanisms (Hamilton et al., 1995). A feedback control strategy was introduced by Kim and Bewley (2007) and Kim (2011) to stabilize the energetic unstable eigenmode of the linearized system. Moreover, Min et al. (2006) showed numerically that by controlling the linear mechanism of the turbulence production, the associated skin-friction drag of a turbulent channel flow can be reduced to a sub-laminar value, albeit at low Reynolds number.
2.1.2 Structures in the Outer Region

Experimental results by Adrian et al. (2000); Tomkins and Adrian (2003) showed that streamwise-aligned packets of hairpin vortices reside in the outer layer of a turbulent boundary layer—shown in Figures 2.4 and 2.5 in a streamwise–wall-normal plane and a streamwise–spanwise plane, respectively. The packet formation of the hairpin vortices is attributed to the auto-generation mechanism that naturally spawns multiple, streamwise-organized hairpins (Adrian, 2007) beneath the lifted vortex elements which undergo the breakup process. The fact that the hairpin vortices tend to appear in packets, rather than individually, was also observed by both Zhou et al. (1999) and Jodai and Elsinga (2016).

Figure 2.4: Realization of hairpin-vortex heads from an instantaneous streamwise–wall-normal PIV snapshot. Reproduced from Adrian et al. (2000)
Due to the spanwise vorticity of the heads of the hairpin vortices—Figure 2.6—large regions ($O(\delta)$) of streamwise momentum deficit (with respect to the observer moving in the flow direction with a mean convection velocity) are generated underneath these packets. For the conservation of mass and momentum to be preserved, these regions of momentum deficit are flanked on the lateral sides by regions of streamwise momentum surplus—Figure 2.7. The former and latter regions are named, hereafter, as low- and high-speed events, respectively and are referred to as large-scale motions (LSMs).
At the initial stage of the evolution of the LSMs, they grow linearly in both the spanwise and wall-normal directions. As they convect downstream, these structures undergo a combination of linear and non-linear growths (Tomkins and Adrian, 2003) until they span the entire boundary layer. The non-linear growth can be attributed to the merging of the hairpin vortices together (e.g. Tomkins and Adrian, 2003; Wark and Nagib, 1990).

Figure 2.6: Signature of the hairpin vortex in the streamwise–wall-normal plane. The momentum transfer by the hairpin-vortex head and the generation of the low-speed zone is depicted. Reproduced from Adrian et al. (2000).

Figure 2.7: Schematic of a large-scale low-speed event flanked by two large-scale high-speed events, accompanied by the counter-rotating roll modes illustrated as black circular arrows. The preferential arrangement of the small-scale fluctuations around the large-scale events is also illustrated in the schematic. Reproduced from Marusic et al. (2010b).
Accompanying these large-scale events are counter-rotating roll-modes, with the respective up- and down-wash sections embodied within the low- and high-speed events as illustrated in Figure 2.7 with black circular arrows. The existence of these roll modes has also been observed in numerous numerical and experimental studies, for instance by Jiménez and del Alamo (2004); Toh and Itano (2005); Del Álamo et al. (2006); Hutchins and Marusic (2007b); Marusic and Hutchins (2008); Chung and McKeeon (2010); Dennis and Nickels (2011a) and Dennis and Nickels (2011b), and even by Hutchins et al. (2012) in the atmospheric boundary layers with $Re_t \sim \mathcal{O}(10^6)$. LSMs tend to align in the streamwise direction and form very large-scale motions (VLSMs) or superstructures as postulated by Kim and Adrian (1999)—shown in Figure 2.8—and reviewed by Marusic and Adrian (2010). In summary it can be noted the LSMs are generated by growing and merging, whereas the genesis mechanism of the VLSMs are due to merging (Lee et al., 2014). An interesting feature of these large-scale events is that they meander in the spanwise direction (Hutchins and Marusic, 2007a), the length of which can occasionally extend beyond $20\delta$ in the streamwise direction in the log-region (Hutchins et al., 2004; Hutchins and Marusic, 2007a).

The influence of the LSMs in the outer region extend down to the wall and are manifested in the large-scale components of both the energy of the near-wall structures (Jiménez et al., 2004; Marusic et al., 2010a) and the energy of the wall-shear stress fluctuations as direct superimpositions (Abe et al., 2004). As the Reynolds number increases, the range of the length- and velocity-scales in a turbulent boundary layer
widens: a distinct scale separation starts to evolve between the near-wall small-scale and the outer region large-scale coherent structures, as reviewed in Marusic and Adrian (2010). At high Reynolds numbers the large-scale structures in the outer region of a turbulent boundary layer become highly energetic (Hutchins and Marusic, 2007a,b; Marusic et al., 2010a). Hutchins and Marusic (2007a) claimed that Reynolds numbers of $Re_{\tau} \geq 2{,}000$ is required for the emergence of an outer site in the spectrogram of the streamwise velocity fluctuations. The large-scale energetic structures in the log-region, together with the small-scale structures in the near-wall region become the dominant contributor to the production of the turbulent kinetic energy (Marusic et al., 2010a; Marusic and Adrian, 2010).

Deck et al. (2014) quantified the influence of the large-scale structures in the outer-region of a high-Reynolds-number turbulent boundary layer on the mean wall-shear stress. They conducted their analysis within the range of $1{,}000 \leq Re_{\tau} \leq 4{,}500$ by using Fukagata-Iwamoto-Kasagi (FIK) identity, which was derived by Fukagata et al. (2002) and named after the initials of the founders. This equation is based on Navier–Stokes equations and relates the wall-shear stress to the Reynolds shear stress in a boundary layer flow. FIK identity for zero-pressure-gradient boundary layer (Kasagi and Fukagata, 2006) reads:

$$C_f = \frac{4(1 - \delta_d)}{Re_\delta} + 4 \int_0^1 (1 - z)(-u'u')dz - 2 \int_0^1 (1 - z)^2(\frac{\partial\overline{\mu'\phi'}}{\partial z} + \frac{\partial\overline{\mu'\phi'}}{\partial z})dz,$$  \hspace{1cm} (2.1)

in which the variables are normalized by free-stream velocity and 99% boundary layer thickness. $\delta_d$ is the dimensionless displacement thickness. $x$ and $z$ denote the streamwise and wall-normal directions, respectively.

Deck et al. (2014) concluded that at high Reynolds numbers, the wake region is the main contributor to the mean skin-friction. However, Renard and Deck (2016) showed that the generation of the turbulence-induced excess friction in dominated by the log-region. Instead of using the FIK identity which is based on a momentum budget, they decomposed theoretically the mean skin friction generation into physical phenomena across the boundary layer based on a mean streamwise kinetic-energy budget. The seemingly contradicting conclusions of the two above-mentioned studies puts us in a
state of ambivalence as whether the log-region or the wake region contributes mainly to the mean skin-friction at high Reynolds-number TBLs.

In addition to the above-mentioned superimposition effect, the large-scale components of the velocity fluctuations exhibit amplitude modulation on the filtered envelope of the small-scale component of the velocity fluctuations in the near-wall region (Hutchins and Marusic, 2007a,b; Marusic et al., 2010b). Within that region the small-scale fluctuations are flourished and languished beneath the high- and low-speed events, respectively. Recently, Baars et al. (2015, 2017a) interpreted that this modulation effect is not limited to the near-wall region, however it accommodates the entire boundary layer. At approximately the geometric center of the log-region the respective behavioral influence of the modulation of the high- and low-speed events on the small-scale fluctuations was shown by Mathis et al. (2009) to be interchanged with each other. That is, beyond the geometric center of the log-region, contrary to the modulation effect in the near-wall region, the small-scale fluctuations are languished and flourished within the high- and low-speed events, respectively. This implies that there is a preferential arrangement of the small-scale structures around the high- and low-speed events (Baars et al., 2015, 2017a). In other words, close to the wall the small-scale fluctuations are more prevalent beneath the high-speed events and in the wake region they become prevalent above the low-speed events. Based on this observation, a predictive model was proposed by Marusic et al. (2010b), in which near-wall streamwise turbulence can be predicted given only the large-scale streamwise velocity fluctuations in the outer boundary layer region. Talluru et al. (2014a) realized that not only the streamwise component, but also the spanwise and wall-normal components of the small-scale velocity fluctuations are modulated by the large-scale component of the $u$ fluctuations.
2.2 Turbulent Boundary Layer Control

Here, the turbulent boundary layer control is considered from an engineering perspective, as reviewed in Coustols and Savill (1992); Moin and Bewley (1994); Gad-el Hak (1996); Lumley and Blossey (1998) and Gad-el Hak and Tsai (2006). Historically, Gad-el Hak (1996) divided the flow control into five distinct eras: 1. the empirical era (prior to 1900); 2. the scientific era (1900-1940); 3. the World War II era (1940-1970); 4. the energy crisis era (1970-1990); and 5. the 1990s and beyond.

With regard to the utilized control device, a control strategy can be either passive, requiring no auxiliary energy during the process of the control, or active, requiring energy expenditure (Gad-el Hak, 1994). An active control strategy, in turn, can be classified into open-loop, coordinated or closed-loop control schemes (MacMynowski and Williams, 2000)—illustrated in Figure 2.9. Furthermore, considering the size of the utilized control components or the size of the coherent structures which are the primary target of the manipulation, a control strategy can be regarded as either a small- or large-scale control scheme.

Here, pertaining to the “drag reduction mechanism” of the control strategies, they are classified into two main categories: 1. Attenuation of the near-wall turbulence; and 2. Attenuation of the outer-region (i.e. log- and wake-region) turbulence. This taxonomy assists to the construction of the hypothesis of this study, which is: “Is it possible to obtain skin friction drag reduction with an opposition flow control scheme which

![Figure 2.9: Options for an active control system: (a) open-loop (predetermined) control, (b) coordinated (feedforward), and (c) closed-loop control. Reproduced from MacMynowski and Williams (2000)](image-url)
acts upon the large-scale coherent structures in a high-Reynolds-number turbulent boundary layer?” Due to the dominant influence of the large-scale structures in the logarithmic region of a high-Reynolds-number TBL onto the near-wall region, the author predicts that this hypothesis is within the realm of possibility. Therefore, the relevant literature of the boundary layer control strategies are reviewed in the following two sections (i.e. §2.2.1 and §2.2.2) based on the above-mentioned classification.

2.2.1 Control via Attenuation of the Near-wall Turbulence

A significant number of control schemes for the purpose of skin-friction reduction have sought to manipulate the near-wall region (e.g. Blackwelder, 1989). Most of these strategies share a common drag reduction mechanism: modifying the counter-rotating longitudinal vortices in the near-wall region so that the fluctuating energy associated to these vortices is reduced. This leads to weaker burst events and reduction in the turbulence production (Choi, 2001). The remainder of this section reviews control strategies that bear the above-mentioned drag reduction mechanism.

2.2.1.1 Passive Control Schemes

Riblets Throughout the history, engineers have always sought to adopt the geometry and characteristics of the ingenious evolutionary solutions which are evident in nature to their favor. Such attempts are called biomemetics (Bhushan, 2009). One such successful application of biomemetics relates to riblets inspired by the pattern of shark skin—Figure 2.10. Sharks have evolved dermal denticles (known as riblets) on their scales aligned in the direction of the fluid flow which gives them the ability to swim quickly. Recently, it has been discovered that shark-skin possesses hydrophobicity due to a thin covering of mucus which aids drag reduction (Dean and Bhushan, 2010). In contrast, Pu et al. (2016) showed that the hydrophobic properties of shark-skin is due to the nano-structured protuberances which exist on the concave groove surface of each denticle. They argue that the elasticity of shark skin also contributes to drag reduction.
There are three major mechanisms shown to be responsible for the drag reduction on riblet surfaces: First, riblets impede the translation of the streamwise vortices, which causes a reduction in vortex ejection and outer-layer turbulence. Second, riblets lift the vortices off the surface and reduce the amount of surface area exposed to the high-velocity flow. By modifying the velocity distribution, riblets facilitate a net reduction in shear stress at the surface (Dean and Bhushan, 2010; Martin and Bhushan, 2014). Third, the spanwise movement of the longitudinal vortices are restricted, which according to Choi (1989) is the prime mechanism for the drag reduction by riblets, and leads to “premature” burst of the low-speed streaks. This mechanism was also reported by Choi (1990); Chu and Karniadakis (1993) and Goldstein et al. (1995). As such, the intensity of the bursts decreases, which lead to higher burst frequency and less spanwise correlation of the near-wall structures (Choi, 1990). Consistent with the third mechanism, Walsh and Lindemann (1984) found that the performance of the V-groove riblet surfaces is dependent on the height and spacing of the riblets scaled by inner-variables.

In addition to the primary influence of the riblets on the flow structures, secondary
vortices are generated within the riblet valleys which do not impose any detrimental influence in terms of skin-friction, since they are weak and short-lived (Tullis and Pollard, 1994). Rather, they act as a barrier between the near-wall sweep events and the effective wall position. Choi et al. (1993) showed in a DNS study that skin-friction drag can be increased by a specific spacing of riblets. For such a riblet configuration, the tip and middle of the riblets are exposed to the near-wall high-speed streaks. The riblet spacing for a drag-decreasing configuration is smaller than that for the drag-increasing configuration so that only the tip of the riblets are exposed to the high-speed streaks—shown in Figure 2.11. It has been shown that, in a mean sense, there is a downward and upward shift of the log-region of the mean velocity profile—identified as $\Delta U^+$—for the drag-increasing and -decreasing configurations, respectively. These findings were also supported by Lee and Lee (2001) from experimental investigations.

![Drag-increasing and drag-reducing riblet configurations](image)

**Figure 2.11:** Drag-increasing (a) and drag-reducing (b) riblet configurations. Reproduced from Choi et al. (1993).

In an experimental attempt to optimize the cross-sectional geometry of V-groove riblets, Walsh (1983) identified that the peak curvature decreases and the valley curvature increases the riblet drag reduction performance. They reasoned that the near-wall low-speed streaks are better constrained by riblets with the shape of a valley curvature. Bechert et al. (1997) showed experimentally that conventional riblets are able to reduce drag by up to 5%. Using blade riblets, with optimized groove depth and slit width, they managed to improve the drag reduction by up to 9.9%, which is claimed
to be the maximum amount of drag reduction that one can possibility achieve via riblets. *Pollard (1998)* argued that the inefficient interaction of the riblets with the quasi-streamwise vortices is the reason of the existence of an upper limit of achievable drag reduction via the riblets.

![Figure 2.12](image-url)

*Figure 2.12:* (a) Characteristic structure of a bird feather. Reproduced from *Chen et al. (2013)*, (b) Schematic drawing of converging-diverging (herringbone) riblets. Reproduced from *Kevin et al. (2017)*.

So far, scenarios where the orientation of the riblets are along the flow direction have been discussed in the current section. However, some examples of the preferential orientation of the riblet grooves have also been observed in nature. For instance, small areas of the fast-swimming sharks with converging or diverging riblets (*Koeltzsch et al., 2002*) or bird feathers (*Chen et al., 2014, 2013*)—Figure 2.12(a). In order to study flow over such types of riblets, herringbone riblets have been used, as in *Koeltzsch et al. (2002); Chen et al. (2014); Nugroho et al. (2013)*. Herringbone riblets consist of parallel columns of riblets, with all the riblets in one column sloping one way and all the riblets in the next column sloping in the opposite direction—shown in Figure 2.12(b). Over the converging section the time-averaged streamwise velocity decreases, which is accompanied by an increase of the velocity fluctuations. The opposite occurs over the diverging section (*Koeltzsch et al., 2002; Nugroho et al., 2013*). Furthermore, *Nugroho et al. (2013)* and *Kevin et al. (2017)* showed that time-averaged large-scale counter-rotating roll modes are induced by the herringbone riblets onto the turbulent boundary.
layer, with a down- and up-wash section over the diverging and converging section, respectively.

2.2.1.2 Active Control Schemes

Uniform Blowing/Suction  The previous section reviewed the usage of riblets as a means of drag reduction, which is considered a passive control strategy. For the remainder of §2.2.1, active flow control strategies for the purpose of drag reduction are reviewed, however still within the context of near-wall turbulent manipulation. Suppression of turbulence via brute force is always possible, but energy consuming. The main challenge facing these kinds of strategies is to achieve a desired effect with minimum energy expenditure (Gad-el Hak, 1996). As a reminder, an active flow control scheme requires a constant source of energy, for example electrical or mechanical energy—for the control components to operate. This source of energy can be either external—as illustrated in Figure 2.13(a)—or internal—as illustrated in Figures 2.13(b) and (c) (MacMynowski and Williams, 2000). The illustration in Figure 2.13 is only meant to demonstrate the difference in the source of consumed energy during the process of active flow control. In other words, it is not meant to show the concept of the separation control. The majority of the active control schemes, especially those that have been developed in laboratory experiments, consume an external source of energy because they have been presented as proof-of-concept experiments. However, for practical applications, it is desired to have a self-contained control scheme during the operation of the vehicle; for instance suction at the leading edge of an airfoil wing for laminar/turbulent transition delay and using the intake-air for blowing at the trailing edge of the wing for drag reduction. This concept was demonstrated by Noguchi et al. (2016). They showed 44.1% reduction in global skin-friction coefficient by a combination of suction for transition delay and blowing for drag reduction in the turbulent boundary layer. Ninety percent of the accomplished drag reduction was contributed by the beneficial effect of the transition delay. The rest was contributed by turbulent friction drag reduction via blowing.
In a DNS of a fully developed turbulent channel flow, Sumitani and Kasagi (1995) showed that uniform wall injection and suction tend to increase and decrease the near-wall turbulent activity and Reynolds stresses, respectively. The effects of uniform blowing and suction on the dynamics of the near-wall streamwise vortices in a turbulent boundary layer were also investigated by Park and Choi (1999). In the case of blowing, the near-wall streamwise vortices are lifted up from the wall, thus the effect of the viscous diffusivity of the wall becomes weaker on the lifted vortices and they become stronger. This results in an increase of both the turbulence intensities and the skin friction downstream the blowing location. The opposite occurs for uniform suction, resulting in a decrease of turbulence intensities and skin-friction downstream of the slot—Figure 2.14.

In terms of turbulence structures, wall-blowing lifts the streamwise vortices away from the wall in the downstream near-field of the location of blowing. Therefore, they become less affected by the viscous diffusivity of the wall and have the leeway to gain more turbulent activity. Consequently, as these vortices fall back to the near-wall region in the downstream far-field of the location of the blowing, they carry more Reynolds stress and become more detrimental in terms of their effects on the wall (i.e. more skin-friction compared to the unmanipulated case). The opposite occurs for uniform suction (Park and Choi, 1999). Kametani and Fukagata (2011) and Kametani et al. (2015) also observed the above-mentioned phenomena in a low-Reynolds-number
DNS and moderate-Reynolds-number LES of a spatially developed turbulent boundary layer, respectively. Furthermore, suction increases the near-wall anisotropy of the turbulent boundary layer (Schlatter and Örlü, 2011), whereas blowing decreases that quantity (Chung et al., 2002; Schlatter and Örlü, 2011).

![Three-dimensional view of near-wall vortices: (a) Uniform blowing, (b) uniform suction. Reproduced from Park and Choi (1999).](image)

Using FIK identity (Equation 2.1), Kametani and Fukagata (2011) revealed that the mean convection term, in a spatially developing boundary layer, works as a determining factor for drag reduction. This can explain the observed reduction in drag by uniform blowing, even though the turbulence away from the wall increases. In other words, the beneficial contribution from the mean convection term to the drag reduction overpowers the detrimental contribution from the increase in turbulent activity.

**Periodic Blowing/Suction**  Periodicity in blowing/suction can be accomplished either spatially or temporally. Spacial periodicity can be generated by sinusoidal or quasi-sinusoidal transpiration along the streamwise direction and temporal periodicity can be generated by sinusoidal or quasi-sinusoidal fluid injection through a slot. Quadrio et al. (2007) studied the influence of a streamwise sinusoidal transpiration with
a steady, uniform distribution in the spanwise direction. They found that the wavelength of the transpiration plays a more important role in the drag variation than the transpiration intensity. For long and short wavelengths, with a threshold of $\lambda^+ = 350$, the skin-friction drag was shown to increase and decrease, respectively. With a correct combination of the amplitude and wavelength of the transpiration, drag reduction of up to 13% can be achieved. Mamori et al. (2014) relaminarized a turbulent channel flow with traveling waves induced by blowing and suction. They found that with the following combination of wave amplitude ($a$), wavelength ($\lambda$) and wave speed ($c$) relaminarization occurs: $a/\bar{u}_{lam} > 0.1$, $200 < \lambda^+ < 500$, and $c/\bar{u}_{lam} > 1.5$, where $\bar{u}_{lam}$ is the mean bulk flow velocity corresponding to laminar flow.

In an innovative experimental study, Tardu and Doche (2009) investigated the effects of a dissymetric localized time periodical blowing on the near wall turbulence and drag. Instead of a sinusoidal blowing, they improvised a periodical blowing in which the acceleration and the deceleration phases were rapid and slow, respectively. This was to prevent the roll-up of the vorticity layer induced by the blowing. They claimed a 50% skin-friction drag reduction.

**Selective Blowing/Suction** Coherent structures in turbulent boundary layers emerge on a random basis. Therefore, feedforward control generally works better than predetermined control in terms of the gain, since feedforward control strives to determine the best control input by sensing the flow state at each time instance (Gad-el Hak, 2000; Kasagi et al., 2009a). However, a predicament facing the feedforward control of TBL is the lack of ability to predict the accurate state of the turbulent structures, especially with wall sensors (Wilkinson, 1990). Rathnasingham and Breuer (2003) showed that the near-wall large-scale structures can be predicted by a linear predictive large-scale filter, which is based on the statistical correlation (i.e. coherence magnitude) between the signals of the sensor and those at the control point. Using this filter, they managed to counteract the near-wall large-scale structures with synthetic jets and showed a 30% and 7% reduction in the streamwise velocity fluctuation and the mean wall shear stress, respectively.
Gad-el Hak (2000) proposed for the first time selective injection and suction below the near-wall high- and low-speed events, respectively. Since the velocity profile under the high-speed events are fuller than the mean velocity profile, he speculated that by injecting fluid under these events the fullness of the velocity profile reduces. In reverse, suction beneath the low-speed events is believed to reduce the inflectional nature of the instantaneous velocity profile. Gad-el Hak and Blackwelder (1989) demonstrated the feasibility of controlling the bursting events in a turbulent boundary layer by selective suction from a slot on the wall. In a towing tank experiment, they generated artificial burst-like events via impulsive or continuous suction through two holes of 0.4 mm in diameter, separated in the spanwise direction by a distance of 10 mm (≈ 100 wall units). Using a variable interval time averaging (VITA) detection algorithm and selective suction from a downstream streamwise slot, they managed to eliminate these artificially-generated events.

Instead of selective blowing and suction beneath the near-wall streaks, in a DNS study Choi et al. (1994) implemented selective wall-based forcing, in the wall-normal or in the spanwise direction in order to counteract the near-wall streamwise vortices—Figure 2.15—known as opposition control scheme. They showed that the near-wall turbulence was attenuated and 20–30% skin-friction drag reduction was achieved. The attributed mechanism of the drag reduction in Choi et al. (1994) was two-fold: 1. within a short time after the control was applied, drag was reduced mainly by deterring the sweep motion without modifying the primary streamwise vortices above the wall, 2. the lifting of the spanwise vorticity near the wall was prevented which resulted in less instability and hence suppression of a source of new streamwise vortices. The optimum location for detecting the instantaneous wall-normal velocity fluctuations was determined by Choi et al. (1994) to be at a wall-normal height of 10 wall units.

Hammond et al. (1998) studied the mechanism behind the drag reduction of the opposition control scheme and attributed the effectiveness of such optimum wall-normal location to the establishment of a “virtual” wall at a location between the detection plane and the actual wall. Fukagata and Kasagi (2003) implemented the opposition control scheme on a limited length of a streamwise direction of a turbulent pipe flow.
Review of the Relevant Literature

and determined that the control influence still remained up to 11–14 times the pipe radius downstream of the position at which the control scheme finished. However, rapid recovery of the skin friction was observed immediately downstream of the controlled region. This contradicting behavior of the skin-friction coefficient recovery suggests that different length scales are responsible for the recovery process: one is short and limited near the wall \( z^+ < 40 \), the other is long and covered the entire height from the wall to the pipe axis. Chung and Talha (2011) conducted a parametric study on the control parameters of the scheme proposed by Choi et al. (1994) and found that a threshold in the wall blowing and suction strength exists, beyond which the control scheme is not effective.

Lee (2015) modified the opposition control in terms of the sensor location. By choosing a sensor location at a wall-normal height of 20 wall units and at 26.2 wall units upstream of the actuation point, they showed that the performance of the control scheme increased and a maximum drag reduction of 29% could be achieved. Xia et al. (2015) implemented the opposition control scheme on a spatially developing turbulent boundary layer in a DNS study and determined a maximum drag reduction of about 22% at the equilibrium state of the controlled flow, which was accompanied by a reduction of the boundary layer thickness.

**Figure 2.15:** Schematic diagram of the wall-based forcing of the near-wall streamwise vortices. Reproduced from Choi et al. (1994).
In an experimental study, Rebbeck and Choi (2006) implemented a real-time opposition control of near-wall turbulence, in which they selectively target only the sweep events with a single synthetic jet and showed that the down-wash strength of the sweep events were diminished. Due to the usage of wall-normal jet for counteracting the sweep events, the control scheme by Rebbeck and Choi (2006) can be considered as a combination of in-phase u-velocity and out-of-phase v-velocity control—illustrated in Figure 2.16. They argued that such combination is more effective than the out-of-phase v-control scheme (i.e. opposition control scheme) proposed by Choi et al. (1994), in which only the wall-normal velocity component of the sweep events was canceled out, and the other velocity components—streamwise and spanwise—were left unaffected. The significance of the study by Rebbeck and Choi (2006) is that the opposition control scheme was implemented experimentally for the first time. However, they did not measure the amount of drag reduction that can be yielded via such a scheme. Furthermore, they used a protruding sensor from the wall in order to detect the sweep events, which influenced the downstream region before the control was implemented.

Figure 2.16: Schematic diagram of the combination of in-phase u-velocity and out-of-phase v-velocity control. Reproduced from Rebbeck and Choi (2006).

In a similar study, Kang et al. (2008) implemented an opposition control scheme to counteract the up-wash motion of synthetic single hairpin vortices in a laminar flow via a nozzle which was placed outside the boundary layer. Their investigation suggests that a reduction in wall skin-friction may be possible by a direct intervention of hairpin
structures if the speed of the control jet can correctly match the up-wash velocity of the hairpins. The study by Kang et al. (2008) provides insight into the concept of the opposition control with time- and length-scales that match those of the outer-layer. However, because of the existence of the nozzle in the boundary layer as an actuator, it is not of practical value.

Large-scale Streamwise Vortical Flow   One promising boundary-layer-control strategy is the superimposition of large-scale streamwise vortical flows on the turbulent boundary layer. The implication of the word “large-scale” for this type of control strategy refers to the relative scale of the superimposed vortical flow with respect to the scale of the near-wall streaks, and not necessarily the scale of the structures in the boundary layer that are acted upon. The underlying mechanism of such control scheme was demonstrated for the first time in the seminal work of Schoppa and Hussain (1998). In a direct numerical simulation of a turbulent channel flow at $Re_\tau \approx 180$, Schoppa and Hussain (1998) showed that the longitudinal vortices near the wall were weakened due to the forcing-induced suppression of an underlying streak instability mechanism. They demonstrated that, as the large-scale vortical flow fuses the longitudinal vortices with each other, the corresponding swirling strength of the resultant vortex becomes weaker. Consequently, the near-wall streaks are also attenuated and meander less, resulting in less instability—shown in Figure 2.17. Schoppa and Hussain (1998) claimed that a drag reduction of up to 50% can be achieved via this control scheme. Furthermore, they showed that the strength of the superimposed vortical flow should be large enough in order to be able to move the near-wall streaks. However, its strength ought to be not too strong so that undesirable extra drag would be generated. Recently, Canton et al. (2016a) conducted a thorough investigation on the characteristics and the optimal parameters of the control scheme proposed by Schoppa and Hussain (1998). They showed that the reported drag reduction in Schoppa and Hussain (1998) is associated with a transient nature of the flow due to the low values of $Re_\tau$ at which the turbulence is marginally sustainable. Moreover, they concluded that inducing vortical flow for drag reduction becomes ineffective at high Reynolds numbers. More argument regarding the influence of the Reynolds number on the control
The findings of Schoppa and Hussain (1998) was confirmed by Iuso et al. (2002) in an experimental study of a fully developed turbulent channel flow. They utilized vortex generator jets distributed along the wall in the spanwise direction, in order to generate large-scale streamwise vortical structures and reported local mean skin-friction reduction as high as 30%. Moreover, Iuso et al. (2002) showed that such flow manipulation caused an increase in longitudinal coherence of the wall organized motions, accompanied by an increase of their mean lateral spacing. As such, the frequency of burst events was shown to be reduced. From a practical point of view, vortical flow can be generated via synthetic jets as in Gomit et al. (2016); Lorkowski et al. (1997); Lee and Goldstein (2001) and Lee and Goldstein (2002); or via flush mounted surface actuators as in Jacobson and Reynolds (1998); Jeon and Blackwelder (2000) and Kim et al. (2003); or via plasma actuators as in Wicks et al. (2015).

**Selective Out-of-plane Wall Deformation**  In a DNS of a turbulent channel flow, Kang and Choi (2000) implemented wall-normal deformation in order to emulate the wall forcing of the opposition control scheme by Choi et al. (1994). They demonstrated that with the active wall motions, turbulence intensities and near-wall streamwise vortices were significantly weakened and 13–17% drag reductions could be obtained. In their simulations, they employed a minimal spacing between the actuators. This

![Figure 2.17: Distribution of the instantaneous streamwise velocity associated with the near-wall low-speed streaks without (a) and with (b) an imposed large-scale control flow. Reproduced from Schoppa and Hussain (1998).](image-url)
led to a non-discrete distribution of the actuators which could ideally cover the entire wall surface. In another DNS study by Endo et al. (2000), 10% drag reduction was achieved through selective manipulation of the streamwise vortices via discrete wall deformation as the actuation.

Implementation of the opposition control scheme of Choi et al. (1994) requires small-scale wall-based forcing, which can be accomplished either by micro blowing/suction or by small-scale wall deformation. The former requires micro channels, of which the manufacturing procedure is quite cumbersome. The latter requires micro-electro-mechanical systems (MEMS), which is still laborious to manufacture and operate. However, recent advancement in MEMS has opened up a potential aspect to the usage of these devices as flow control appliances (Gad-el Hak, 2001; Kasagi et al., 2009b), and this renders the implementation of the opposition control scheme a plausibility. This is demonstrated by Yoshino et al. (2008), who conducted feedback control experiments in a fully-developed turbulent air channel flow at $Re_r = 300$. In their study, arrayed micro hot-film sensors with a spanwise spacing of 1mm were utilized for measuring the streamwise shear stress fluctuations, and arrayed magnetic actuators of 2.4mm in spanwise width were used for introducing wall deformation as the control input. As such, a 6% drag reduction was measured.

Using two flush mounted piezo-ceramic actuators with dimensions of length×width×thickness $= 21.5 \times 2.0 \times 0.33$mm and 1mm spanwise separation, Qiao et al. (2017) managed to demonstrate the possibility of reducing the skin-friction drag via three different control strategies: feedforward, feedback and a combination of both feedforward and feedback control schemes, with respective drag reductions of 24%, 20% and 28%. In Qiao et al. (2017), similar to this thesis, the actuators operated on an intermittent basis. However, their actuators manipulated the near-wall high-speed streaks, whereas the wall-normal jet actuators in this study manipulate the high-speed LSMs in a high-Reynolds number TBL.

**Predetermined In-plane Wall Oscillation** Oscillation of a flat plate in a stationary viscous fluid, with the oscillation direction parallel to the plate, generates a Stokes
boundary layer. The amplitude and frequency of the wall-oscillation together with the viscosity of the fluid determine the amplitude and frequency of the oscillating fluid particles as well as the Stokes boundary layer thickness. Likewise, a spanwise oscillation of the wall in a wall-bounded flow, generates a spanwise Stokes boundary layer in a time-averaged sense. The interaction of the Stokes boundary layer with a turbulent boundary layer can lead to diminishing of the spanwise coherence of the near-wall streak, which results in a decrease of the number and intensity of the burst events. Hence, the near-wall turbulence intensity is decreased (Jung et al., 1992).

Jung et al. (1992) investigated the response of a turbulent channel flow to spanwise oscillation of both the top and bottom wall of the channel—shown in Figure 2.18(a)—by direct numerical simulations. They carried out their simulations with periods of oscillation ranging from $T^* = 25$ to 500 and determined that the oscillations at $T^* = 100$ produced the most effective suppression of turbulence. Choi et al. (1998) confirmed the results of Jung et al. (1992) in an experimental investigation. When the wall oscillation was optimized with a non-dimensional wall speed, skin-friction reductions of as much as 45% were observed within five boundary layer thickness downstream of the start of the wall oscillation. The logarithmic velocity profile was shifted upwards and the turbulence intensities were reduced. Furthermore, Choi and Graham (1998) managed to reduce the wall-shear stress of a turbulent pipe flow as much as 25% via oscillating a section of a pipe in a circumferential direction. They attributed the mechanism of drag reduction to the active manipulation of the near-wall turbulence structures by circular wall oscillation. Choi and Clayton (2001) attributed the mechanism of drag reduction with spanwise wall oscillation to the spanwise vorticity generated by the periodic Stokes layer over the oscillating wall, which affects the boundary-layer profile by reducing the mean velocity gradient within the viscous sublayer. Choi (2002) showed that the stretching of the quasi-streamwise longitudinal vortices are hampered by spanwise wall oscillation which leads to a weaker near-wall burst activity, and eventually leading to skin-friction drag reduction. Conversely, Duggleby et al. (2007) argued that the primary influence of a spanwise wall oscillation is the lifting of the turbulent structures away from the wall by the Stokes flow which
Flow

\[ V = V_m \sin\left(\frac{2\pi}{T} t\right) \]

\[ V = A \sin(k x) \]

\[ v_w = A \sin(k_x x - \omega t) \]

Flow

\[ L_x \]

\[ L_y \]

\[ L_z = 2h \]

Figure 2.18: Illustration of spanwise oscillation of a channel wall, (a); stationary distribution of in-plane spanwise velocity alternating in streamwise direction, (b); and Streamwise-traveling waves of in-plane spanwise velocity, (c). (a), (b) and (c) reproduced from Ricco and Quadrio (2008), Viotti et al. (2009) and Quadrio et al. (2009), respectively.
causes the reduction in the time and duration of Reynolds stress generating events, resulting in drag reduction.

Based on the success of the spanwise wall oscillation for drag reduction, Viotti et al. (2009) proposed a stationary distribution of the in-plane spanwise velocity that alternates in streamwise direction, as illustrated in Figure 2.18(b). This type of wall oscillation can be considered as the spatial counterpart of the former type of manipulation. By considering the convection velocity of the to-be-manipulated structures as the link between these two types of manipulations, one can transform the time scale of the spanwise wall-oscillation to the length scale of the stationary in-plane sinusoidal spanwise velocity distribution. Viotti et al. (2009) showed that the optimal period of the oscillating wall corresponds to the optimal wavelength of the stationary in-plane spanwise wall oscillation. This warrants again the analogy between these two types of manipulation. Furthermore, they demonstrated that in terms of the absolute value of drag reduction the latter yielded better results than the former.

Quadrio et al. (2009) proposed a more general in-plane wall oscillation—illustrated in Figure 2.18(c)—which is formulated as:

\[
v_w = A \sin (\kappa x - \omega t),
\]  

(2.2)

where, \(v_w\) is the spanwise component of the wall forcing, \(\kappa\) and \(\omega\) are the wavenumber and temporal frequency of the forcing, respectively. \(A\) is the amplitude of the oscillation. This type of oscillation includes as particular cases both of the above-mentioned techniques of wall oscillation: 1. The oscillating wall technique (a traveling wave with infinite phase speed), and 2. The steady distribution of spanwise velocity (a wave with zero phase speed). They conducted a parametric study of the wavenumber (i.e. \(\kappa_x\)) and frequency (i.e. \(\omega\)) of oscillation and presented a diagram illustrating different flow behaviors in terms of drag variation as a function of these two variables—depicted in Figure 2.19.

The overall feature of Figure 2.19 is as follows: forward-traveling waves (i.e. positive \(\omega\)) with small phase speed produce a large reduction in drag (red regions in Figure 2.19) that can relaminarize the flow at low values of Reynolds number. If this type of
manipulation was to be adopted for controlling the turbulent boundary layer at the flow conditions of this study (i.e. $Re_x \approx 14,400$), the physical time- and length-scale of the manipulation would have been in the order of 10 nano seconds and 10 micrometers, respectively. Manufacturing and operation of sensors and actuators with the above-mentioned time- and length-scales are both demanding and expensive. Drag increase is observed when the waves travel at a speed comparable with that of the convecting near-wall turbulence structures (blue regions in Figure 2.19). Backward-traveling waves (i.e. negative $w$) lead to drag reduction at any speed. From a practical point of view, plasma actuators can generate periodic spanwise cross flows without any moving parts, the interaction of which with the boundary layer can resemble those of the mechanical spanwise wall-oscillation.

![Figure 2.19](image.png)

**Figure 2.19**: Map of friction drag reduction in the $\omega-K_x$ plane, associated with the forcing represented in Equation 2.2. Red and blue contours represent drag reduction and increase, respectively. Reproduced from Quadrio et al. (2009).

Using surface plasma to create a spanwise oscillatory force near the wall, Jukes and
Choi (2006) managed to generate a large streamwise velocity deficit in the lower region of the boundary layer ($6 < z^+ < 110$) with a maximum reduction of mean velocity by 40% at $z^+ = 30$. Based on a reduction of the near-wall slope of the velocity profile, they concluded that a friction velocity reduction by 22% was achieved. Furthermore, they suspected that the observed streamwise velocity deficit was due to the induced co-rotating vortices in the inner region of the boundary layer. Whalley and Choi (2014) configured the plasma actuator arrays to produce either unidirectional or bidirectional spanwise traveling waves. For unidirectional spanwise traveling waves, starting vortices were created in sequence, which moved as a single vortex engulfing the neighboring vortices from the previous phases. Whalley and Choi (2010) showed that these starting vortices were more effective in terms of interaction with turbulent boundary layer. On the other hand, the bidirectional traveling waves generated counter-rotating vortices which carried the low-speed fluid in the near-wall region of the turbulent boundary layer and reduced the mean velocity in the buffer and lower log-region (Choi et al., 2011).

The control strategies reviewed so far have been shown to yield promising results in laboratory environments. However, because of the limitations surrounding the fabrication of the control components in the order of scale [$O(\mu m)$] of the near-wall structures in TBLs with $Re_\tau \sim 10^4 – 10^6$ (Figure 1.2), their full-scale implementation has not yet been realized. For instance, the application of riblets in airline industry are hampered by issues such as (Goldhammer, 2009): 1. adhesive robustness over operational life; 2. appearance relative to standard paint; and 3. time required to install, maintain, remove and re-apply. Furthermore, blowing and suction for manipulating the near-wall structures are inhibited by its demand for micro-porous walls, subsurface plumbing and a mass-transport system (Leschziner et al., 2011).

### 2.2.2 Control via Attenuation of the Outer Region Turbulence

The flow control strategies reviewed in the previous section (§2.2.1) have sought to gain skin-friction drag reduction via either interrupting or interacting with the sequence of events in the near-wall region. However, whether wall-bounded turbulence
is driven by processes in the near-wall region and diffuses outwards, or it is driven by the ambient shear from the outer region, is still a matter of contention. Regarding the latter cause of turbulence, certain control strategies have tried to manipulate the outer region for the purpose of drag reduction, such as large eddy breakup devices (LEBUs)—reviewed in 2.2.2.1—and polymer additives—reviewed in 2.2.2.2.

2.2.2.1 Passive Control Schemes

**Large Eddy Breakup Devices**  
*Anders and Watson (1985)* defined LEBU as follows: 
*A LEBU is a device to break up and modify the large outer scales of a turbulent boundary layer for the purposes of turbulence control and drag reduction.* With parallel-plates as LEBUs, *Corke (1981)* showed that the intermittent excursions of potential fluid into the boundary layer is suppressed and the streamwise growth rate of the boundary layer is decreased. As such, this phenomenon was also shown to be accompanied by a reduction of the burst frequency. However, *Tardu and Binder (1991)* reviewed that the burst frequency—even with different definition and detection criteria—is not altered by the usage of LEBUs.

*Nagib (1982)* claimed that 20% local wall-shear stress reduction and 22% net drag reduction can be obtained with an optimal configuration of two single plates (i.e. tandem plate configuration) residing at a wall-normal height of 0.8δ in the boundary layer with a streamwise spacing of 8δ. These dimensions correspond temporally to the average life span of the large-scale eddies that are to be suppressed. The remarkable feature of LEBUs is that their influence appears to persist for quite a large downstream distance (at least 70 boundary layer thickness). Instead of flat plates, *Anders and Watson (1985)* used airfoil-shaped LEBUs and showed that they perform as well as the flat plate LEBUs. Furthermore, in terms of the stiffness they were demonstrated to be 1000 times stiffer than the flat plate LEBUs, which yields them as potential devices to withstand a real flight environment.

*Savill and Mumford (1988)* depicted a tentative drag reduction mechanism of tandem LEBU devices—shown in Figure 2.20—which is best explained in their own words: 
*“When the vortex street formed behind the first plate impinged on the second it split so*
that the top set of vortices passed over the second plate and the lower set below it. Their presence appeared to amplify the instability at the trailing edge, producing a larger-scale second street. This lay embedded in the first so that there were again four sets of vortices arranged vertically as with the stacked and staggered cases, but now a second set of more intense counter-rotating or uplifting vortices lay directly above the lowest set of such structures. This appeared to be the most advantageous arrangement because the first set were displaced towards the wall by the second stronger set and so reached it sooner. Then when they decayed the latter took over their role and it was at that point that the maximum drag reduction had been detected with this configuration.”

Whether the usage of LEBUs can lead to any net drag reduction is still a matter of contention among the fluid research community. In a towing tank experiment, Sahlin et al. (1988) investigated the possibility of obtaining net drag reduction by using LEBUs with both single and tandem configurations. Disappointingly they did not observe any net drag reduction with either of the configurations. The lack of existence of any net drag reduction was also reported by both Corke (1981) and Mah et al. (1992) in separate experimental studies. In an LES of a spatially developing TBL, Chin et al. (2015) placed a single flat-plate LEBU at a wall-normal height of $0.8\delta$, in which they did not observe any net drag reduction either. In order to be able to comment conclusively with regard to the plausibility of achieving any net drag reduction via LEBUs, further investigation is required.
2.2.2.2 Active Control Schemes

**Polymer Additives**  Boundary layer flow control for the purpose of drag reduction by polymer additives is one of the oldest control strategies—first discovered by Toms (1948). It is remarkable to note that polymer drag reduction has been by far the most effective control strategy—drag reduction by up to 80% has been observed, as reviewed by Gad-el Hak (1996) and Bushnell and Hefner (1990). Despite that, the current understanding of the entire drag reduction mechanism behind such a method is yet incomplete and requires further study (White and Mungal, 2008). Such a mechanism can be reviewed with regard to the influence of each of the following parameters to the qualitative and/or quantitative changes of polymer drag reduction mechanism: 1. The topology of the polymer particles; 2. The elasticity of the polymer particles; 3. The concentration of the polymer solution; and 4. The mixing methodology; among others. The common statistical and structural features that a polymer solution can impose upon the turbulent boundary layer is highlighted in the following, regardless of the direct influence of each of the above-mentioned factors. The addition of polymer to a wall-bounded flow has two major influences on the boundary layer in a mean sense: 1. The dynamics of polymer affect the scales of the boundary layer up to the outer scale and the distinction between inner and outer scaling is lost—the “near-wall” regime may no longer be near the wall (Graham, 2004), and 2. The momentum flux from the bulk to the wall is reduced, which according to Procaccia et al. (2008) is the main mechanism of polymer drag reduction.

In an experimental study, Koskie and Tiederman (1991) showed that the sheer presence of the advecting polymer particles in the direction of the flow in a turbulent boundary layer may add extra shear stress to the wall. However, depending on the concentration of the polymer solution, the beneficial influence of the violation of the integrity of the coherent structures in a polymer-modified turbulent boundary layer can overpower the detrimental influence of the polymer-particle imposed drag. Furthermore, they showed that the slope of the logarithmic region of the velocity profile of a polymer drag-reduced boundary layer is increased. Sureshkumar et al. (1997) conducted a DNS on a turbulent channel flow with a dilute polymer solution. They observed that the
streamwise vorticity fluctuation in the near-wall region are decreased and the spanwise spacing between the streamwise low-speed streaks in the buffer layer is increased. Both of the above-mentioned phenomena imply that the turbulence-generating events are inhibited in the near-wall region, which is known to be beneficial for skin-friction drag reduction.

Warholic et al. (1999) implemented a thorough experimental investigation on the mechanism of drag reduction of polymer solutions in a fully-developed channel flow. They showed that the polymer drag reduction mechanism is different for low drag-reduction cases than it is for high drag-reduction cases. At low drag-reduction, the viscous sublayer is thickened which is also accompanied by an upward movement of the logarithmic region of the velocity profile. However, the viscous sub-layer experiences a surplus in the intensity of the streamwise velocity fluctuations, which is due to the added mean polymer stresses, $\tau_p$. On the other hand, for a high drag-reduction case, the modified velocity profile does not show any logarithmic behavior. By increasing the polymer concentration, the mean velocity profiles of TBLs approach an asymptotic mean velocity profile (Virk, 1975), which corresponds to a condition of maximum drag reduction (MDR) with polymer injection. The same observations as those of Warholic et al. (1999)’s was also made by Ptasinski et al. (2001) in an experimental study of a turbulent pipe flow and by Ptasinski et al. (2003) in a DNS of a channel flow. Furthermore, Warholic et al. (2001) conducted particle image velocimetry (PIV) measurement in a turbulent channel flow with drag-reducing polymers. The main feature that they highlighted from the influence of the polymer was the fact that the wall-normal velocity fluctuations and the Reynolds shear stress have been dramatically decreased, which can be a manifestation of the decrease of ejection events. Consequently, the turbulence created by the wall is not propagated to the higher wall-normal heights.

Dubief et al. (2004) showed that the mechanism of polymer drag reduction is a self-sustained drag reducing phenomenon, akin to the auto-regeneration cycle of wall turbulence by Jiménez and Pinelli (1999). In other words, polymers dampen the near-wall vortices but also enhance the streamwise kinetic energy in the near-wall streaks (Kim et al., 2008). This implies that the dynamics of vortex generation in a TBL with polymers exhibit strong resemblance with those of the TBL without polymer.
The majority of the studies pertaining to polymer drag reduction in a boundary layer, that have been reviewed so far, involve wall-bounded internal flows. However, Fontaine et al. (1992) investigated the influence of injecting polymer through a narrow inclined slot into the near-wall region of a zero-pressure-gradient TBL. The immediate effects of polymer injection are a deceleration of the flow near the wall, together with a dramatic decrease of both the r.m.s levels of the wall-normal velocity fluctuation and the Reynolds shear stress values. Furthermore, they concluded that the bursting process is suppressed by the injection of polymer solution which might indicate that the turbulent transport of momentum in the direction normal to the wall is suppressed. With a similar set-up, White et al. (2004) also studied the drag reduction mechanism in a zero-pressure-gradient TBL by injection of a concentrated polymer solution through a spanwise wall-normal slot along the entire test section. From the wall-parallel PIV measurements downstream of the injection location, an increase in the spanwise separation of the low-speed velocity streaks and a reduction in the strength and numbers of the near-wall vortices was reported.

### 2.3 High-Reynolds-number Boundary Layer Control

The performance of the majority of the control strategies for the purpose of skin-friction drag reduction has been evaluated either numerically or experimentally in laboratory environments. However, the range of the friction Reynolds number of the majority of the engineering applications—see Figure 1.2—is beyond that of the computational and laboratory experiments. So far, the maximum achievable friction Reynolds numbers for a DNS of a zero-pressure-gradient TBL and channel flow are 2025 (Sillero et al., 2011) and 5200 (Lee and Moser, 2015), respectively. Only recently, experimental facilities have yielded high-precision measurements of near-wall velocity at friction Reynolds number of the order of $10^5$ (Smits and Marusic, 2013). It is known that the performance of a control scheme varies with respect to the Reynolds number. Therefore, prior to any practical implementation, the success of the control strategies should be assessed at practical values of Reynolds number. Alternatively, the performance of the control algorithms can be investigated as a function of Reynolds number within the
range of achievable Reynolds numbers in computation and laboratory experiments, and its behavioral change can be extrapolated to higher Reynolds number values in order to accommodate the practical Reynolds number values.

Generally, the performance of control schemes which are solely based on manipulating the near-wall region deteriorates with increasing $Re_\tau$. In a DNS of a channel flow, Gatti and Quadrio (2013) investigated the Reynolds-number dependency of the performance of the “streamwise-traveling waves of spanwise wall velocity” control scheme within a Reynolds number range of $Re_\tau = 200–1000$. They realized that drag reduction degrades with $Re_\tau$ and the optimal control parameters of the manipulation—the wavelength and frequency of the wall forcing—is dependent on $Re_\tau$. The same findings as those of Gatti and Quadrio (2013)’s was also observed by Hurst et al. (2014) in a DNS of a channel flow in $Re_\tau = 200–1600$.

Furthermore, the effectiveness of the opposition control scheme proposed by Choi et al. (1994) has also been shown to deteriorate at higher Reynolds numbers (e.g. Iwamoto et al. (2002) in a DNS of a channel flow in $Re_\tau = 110–300$ and Chang et al. (2002) in an LES of a channel flow in $Re_\tau = 80–720$). In order to modify the opposition control scheme, so that its beneficial effect on the skin-friction also accommodates high Reynolds numbers, Pamiès et al. (2007) eliminated the suction part of the control and demonstrated that the performance of the control scheme increases with a blowing-only opposition control.

The observations that the majority of the control schemes would lose their performance as the Reynolds number increases was reconciled by Hunt and Morrison (2000). They argued that a “bottom-up” process (i.e. the inner part of the boundary layer drives the outer part) is dominant at low Reynolds numbers, while a “top-down” process (i.e. the outer part drives the inner part) plays a dominant role at high Reynolds numbers. In other words, the dynamics of the boundary-layer flow is mostly governed by the near-wall phenomena at low Reynolds numbers, whereas at high Reynolds numbers it is affected by both the near-wall and outer region (Gad-el Hak, 2001).

de Giovannetti et al. (2016) determined that the large-scale motions and very large-scale motions are responsible for 20–30% of the total skin-friction, however their complete
removal only yielded 5–8% of skin-friction reduction. This finding can also be considered to be consistent with the results of Fukagata et al. (2010), in which they selectively dampened the large-scale fluctuations (the spanwise wavelength larger than 300 wall units) in a turbulent channel flow at $Re_\tau = 640$ and observed that the friction drag reduction is much less than that expected from the absence of large-scale fluctuations. They justified this observation to the contribution of small-scale fluctuations to the friction drag, which was found to be drastically increased due to the reduction of pressure fluctuation and destruction of Reynolds shear stress.

Furthermore, de Giovanetti et al. (2016) showed that the attached eddies in the log-region of a high Reynolds-number turbulent boundary layer generate the largest amount of skin friction, and with increasing the Reynolds number the contribution of these motions to turbulent skin-friction also increases, whereas the near-wall motions lose their importance. These observations are in congruent with those by Renard and Deck (2016).

Iwamoto et al. (2005) demonstrated that large drag reduction can be attained at high Reynolds numbers, if by some virtual means, the turbulence fluctuations adjacent to the wall are completely damped. They demonstrated the potential of obtaining 35% skin-friction reduction at $Re_\tau = 10^5$ via a theoretical analysis. Using the FIK identity (Equation 2.1), Kasagi and Fukagata (2006) also concluded that considerable drag reduction can be attained even at high Reynolds numbers by merely suppressing the turbulence near the wall, without any direct manipulation of large-scale structures arising away from the wall.

### 2.3.1 Present Control Strategy

The majority of the studies on skin-friction drag reduction—reviewed in Section 2.2—have investigated control strategies for TBLs at low Reynolds numbers (i.e. $Re_\tau \sim O(10^2)$). Although the friction Reynolds numbers of the TBLs in most of the engineering applications are predominantly high and in the order of $Re_\tau \sim O(10^3–10^6)$, no research, to the author’s knowledge, has been conducted to explore a realistic control strategy for
a high-Reynolds-number TBL. As the Reynolds number increases, the large-scale coherent structures in the outer-layer of a TBL become more energetic, and hence their influence on the dynamics of the near-wall region becomes prevalent—as reviewed earlier in 2.1.2. Therefore, the objective of the present experimental study is to investigate whether a selective manipulation of the LSMs and VLSMs in a high-Reynolds-number (i.e. $Re \tau \sim O(10^4)$) zero-pressure-gradient TBL can generate skin-friction reduction. To target the large-scale structures, a large-scale blowing-only control scheme was employed. The usage of the term “large-scale” has two implications; it refers to both the structures that are manipulated and the size of the forcing which is experimentally induced on the flow.

The experiments were conducted in the high-Reynolds-number boundary layer facility at the University of Melbourne with an embedded control architecture and will be described in full detail in the next Chapter (§3). The hardware includes surface-embedded actuators, which can inject a wall-normal jet flow into the TBL so that the jets in cross-flow penetrate into the log-region. Since the actuators were active only when the high-speed events were predicted to convect above them, a control scheme within an opposition framework was nominally explored. In order to detect these events, their wall signatures were sensed upstream of the actuation point.

The wall-normal injection of air, which lacks any initial streamwise momentum as it passes through the jet exit plane, into a zone with a naturally larger instantaneous streamwise velocity than the mean streamwise velocity (i.e. high-speed events) renders an in-phase streamwise-velocity control. Furthermore, since the high-speed events are accompanied by the down-wash sections of the naturally occurring counter-rotating roll modes, the introduction of wall-normal jet airflow into the high-speed events results in an out-of-phase wall-normal-velocity control. The opposition framework of the control scheme comes from the realization of the latter control mechanism. How the control affects the downstream flow in terms of the flow and wall variables are investigated in §4.

Two other types of manipulations are also investigated. First, contrary to the above-mentioned control scheme, the actuators were active when a low-speed event was
predicted to convect above them. Secondly, the actuators were operated on a random basis. Therefore, the results of the former and latter types of manipulations serve as a counter-evidence and a base-line case for those of the high-speed event manipulation case, respectively.

In summary, from a drag reduction mechanism perspective, the investigated control scheme can be placed into the main category “Control via Attenuating the Outer Region Turbulence” (§2.2.2), and from a control mechanism point of view, it can be placed into the sub-category “Selective Blowing/Suction” (§2.2.1.2). In an earlier experimental study, Marusic et al. (2014) demonstrated the feasibility of generating a low-speed region within the existing high-speed events using a single wall-normal jet actuator that consisted of a rectangular jet exit plane with an aspect ratio of 25 in the streamwise direction. They carried out their experiments in a high-Reynold-number TBL at $Re_s = 14,000$, which is of the same order of magnitude as that investigated in this thesis. The implemented control scheme in Marusic et al. (2014) operated based on a “predetermined intermittent blowing” control mechanism in which the jet actuator had on- and off-duty periods of approximately $\Delta t_{\text{on-duty}}/(\delta/U_c) = 4$ and $\Delta t_{\text{off-duty}}/(\delta/U_c) = 11$, respectively (where $\delta$ is the boundary layer thickness and $U_c$ is the convection velocity of the large-scale structures in the TBL). Two spanwise arrays of skin-friction sensors were utilized in Marusic et al. (2014): one array for detecting the footprints of the large-scale structures located at $1\delta$ upstream of the jet actuator and the other array for monitoring the footprints of the manipulated large-scale structures, located at $1\delta$ downstream of the actuator. They performed conditional averages of the manipulated streamwise velocity ($u$) fluctuations based on both the on-duty periods of the actuation and the large-scale high-speed events that were predicted to convect over the actuator, and compared them with the unmanipulated conditional events. Furthermore, they claimed that these conditional averages would resemble those generated using the manipulated $u$ fluctuations downstream of a selective control scheme that only targets the predicted high-speed events in real-time. Here, the number of the actuators are extended to nine and are distributed in the spanwise direction with a specific spanwise spacing so that the entire spanwise width of the actuator-array would accommodate twice the characteristic width of the large-scale structures—the details of the
actuators are explained in §3.2. In other words, the control scheme has been extended from a two-dimensional predetermined control mechanism (streamwise–wall-normal plane) to a three-dimensional selective control mechanism (streamwise–spanwise–wall-normal).
Chapter 3

Experimental Set-up

3.1 Facility and Conditions

Experiments were conducted in the High-Reynolds-Number Boundary Layer Wind Tunnel (HRNBLWT) at the University of Melbourne (Nickels et al., 2005; Baars et al., 2016b). The maximum free-stream velocity of the tunnel is approximately 45 m/s with a turbulence level of less than 0.05% at the test section inlet in the free-stream flow. HRNBLWT is an open-loop blowing wind tunnel with two 90° corners, each with 32 streamline vanes. After the second corner, air flows through a honeycomb straightener and six fine mesh screens. Prior to entering the 27 m-long test section of the tunnel, the air enters a six to one contraction section, whose outlet cross section is nominally $2 \times 1$ m. The boundary layer is tripped by a coarse grid P40 grit sand paper immediately after the contraction in order to guarantee the onset of a turbulent boundary layer at the beginning of the test section. The relatively long test section in the streamwise direction ensures the formation of a high-Reynolds-number boundary layer over the wind tunnel floor, while high spatial and temporal resolutions are obtained with the existing instrumentation under moderate free-stream velocities. The ceiling of the tunnel is constructed of 22 adjustable bleeding plates, the angle and bleeding gaps of which can be adjusted. With a zero-pressure gradient configuration, the pressure coefficient is constant to within ±0.87% (Marusic et al., 2015). An isometric sketch of the
wind tunnel, together with an open-view of the test section, are shown in Figures 3.1(a) and (b).

Real-time control of the turbulent boundary layer (TBL) was performed at a streamwise location that nominally coincided with a floating element drag balance (permanently embedded within the wind tunnel surface). This large-scale floating element drag balance measures $l_F = 3.00\text{m}$ and $w_F = 1.00\text{m}$ in the streamwise and spanwise directions, respectively, and is nominally centered at $x = x_F = 21.00\text{m}$ and $y = 0$. The modular design of the flow-exposed surface of the drag balance allowed for an implementation of the control hardware. Directly measured wall drag data at the local Reynolds numbers (Baars et al., 2016b) provided the nominal experimental conditions and assisted in the calibration of hot-film sensors (discussed in §3.4.2). All components of the control set-up were implemented on the floating element. Its flow-exposed surface of $l_F \times w_F$ is gray-shaded in Figure 3.1(c). Nine removable panels, each measuring $l_m = 0.30\text{m}$ in $x$ and $w_m = 0.70\text{m}$ in $y$, form a modular construction. Locations $x_1 = 19.80\text{m}$ to $x_9 = 22.20\text{m}$ indicate the streamwise centers of all the insert plates. The characteristics of the sensors and actuators embedded on these panels are discussed in §3.2.1 and §3.2.2, respectively.

As mentioned earlier in §1, the streamwise, spanwise and wall-normal directions of the flow is denoted by coordinates $x$, $y$ and $z$, respectively. Therefore, $u$, $v$ and $w$ represent the fluctuating velocity components in the respective $x$-, $y$- and $z$-directions. The origin of the defined coordinate system is determined to coincide with the test section inlet, on the wall and at the spanwise center of the tunnel.
$x_L = 19.50\text{m}$, leading-edge of the floating element
$x_F = 21.00\text{m}$, streamwise center
$x_T = 22.50\text{m}$, trailing-edge
$x_i (i=1, 9)$: streamwise centers of 9 modular inserts

$l_F = 3.00\text{m}$, length of the floating element
$w_F = 1.00\text{m}$, width of the floating element
$w_T = 1.90\text{m}$, width of the test section
$l_m = 0.30\text{m}$, length of the modular insert
$w_m = 0.70\text{m}$, width of the modular insert

**Figure 3.1:** (a) Schematic of the boundary layer facility at the University of Melbourne. (b) An open view (side walls and ceiling removed) of the test section, together with the six to one contraction. (c) The floating element assembly with its corresponding dimensions (Baars et al., 2016b). Reproduced from Abbassi et al. (2017).
For all the measurement campaigns, the free-stream velocity was set to be nominally $U_\infty = 20\text{m/s}$. This resulted in a spatially-developing turbulent boundary layer along the wind tunnel floor. The boundary layer thickness was measured to be $\delta = 0.360\text{m}$ at $x = 21.00\text{m}$, which was determined by fitting a composite velocity profile of Chauhan et al. (2009) to the mean velocity profile, with log-law constants $\kappa = 0.384$ and $B = 4.17$. The corresponding friction velocity is calculated to be $U_{\tau_U} = 0.649\text{m/s}$, resulting in $Re_{\tau} \approx 14\,400$ and $Re_\theta = U_\infty \theta/\nu \approx 44\,040$. At the same flow conditions, the floating element drag balance measured a friction velocity of $U_{\tau_U} = 0.649\text{m/s}$, which agreed to within $1.2\%$ with that determined via the composite profile fit. For inner-normalizing the variables throughout this dissertation, the latter friction velocity value (i.e. $U_{\tau_U} = 0.649\text{m/s}$), together with the measured kinematic viscosity are used. A summary of both the nominal flow conditions and the properties of the TBL is provided in Table 3.1.

<table>
<thead>
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<th>Conditions for the <em>uncontrolled</em> TBL flow</th>
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<td>$x$ (m)</td>
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<td>-------</td>
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</table>

Table 3.1: Flow conditions.

### 3.2 Control Architecture

Any real-time flow control system comprises two major components: a hardware component and a software component. The hardware component, by itself, contains three sub-components: sensors (§3.2.1), actuators (§3.2.2) and the controller (§3.2.3). The software component is the utilized control algorithm—discussed in §3.3—implemented via the controller in order for the sensors and actuators to communicate with each other. Such communication is unilateral for a *feedforward* real-time control scheme—from the upstream sensors to the downstream actuators.
3.2.1 Sensing: Turbulent Boundary Layer Footprint

Monitoring the passage of the high- and low-speed events at one streamwise location can be achieved via a spanwise rake of hot-wires (e.g. Hutchins and Marusic, 2007a). However, this alters the flow for the purpose of control which is not desirable. Previous studies have shown that a strong coherence between the large-scale fluctuations in the log-region and those in the inner-region of the flow exists (Johansson and Alfredsson, 1982; Abe et al., 2004; Marusic et al., 2010b; Baars et al., 2016a, among others). Therefore, surface-mounted hot-film sensors are employed to measure the wall-shear stress fluctuations as surrogates for the streamwise velocity fluctuations in the log-region. For such, a spanwise array of nine Dantec skin-friction sensors (model 55R47) was glued on one panel insert, located at $x_1 = 19.80$ m (Figure 3.2). The signals from these sensors were fed into a controller—defined in §3.2.2—in real-time. The prescribed control algorithm for the controller singled out the large-scale components of the wall-shear stress fluctuations (described in §3.3)—believed to be the footprints of the large-scale motions (LSMs) in the log-region.

A spanwise spacing of the sensors was adopted from Hutchins et al. (2011) and measured $\Delta y = 26.0$ mm ($\Delta y^+ \approx 1042$ or $\Delta y/\delta = 0.072$). The sensor-array was placed in the spanwise center of the panel insert; note that nine actuators (§3.2.2) were located in-line, downstream of the nine sensors, forming nine sensor–actuator pairs. Hutchins et al. (2011) determined that two sensors with such spanwise spacing, accommodate approximately one-fifth of the width of the detected footprint of the large-scale structures at the same flow conditions as those investigated in this study (i.e. $Re_+ \approx 14400$), which was inferred from the two-point correlation of the measured wall-shear stress fluctuations. Therefore, nine hot-films with the same spanwise spacing accommodate approximately twice the width of the detected footprint of the structures. Each hot-film has a sensing element of $0.1 \times 0.9$ mm (in $x$ and $y$ directions, respectively), which equates to an active spanwise sensor length of $l_{sf}^+ = 37$ (see inset-Figure 3.2b). Wall-normal steps associated with these glue-on sensors are generally less than $3.5\nu/U_\tau$ (Hutchins et al., 2011) and can therefore be considered as hydraulically smooth ($k_s^+ \lesssim 4$,
following Nikuradse, 1965). A nine-channel (AA Lab Systems AN-1003) constant-temperature anemometer (CTA) with an overheat ratio of 1.05 was used to operate these sensors.

Aside from the sensor-array at $x = 19.80m$, a duplicate array (with a separate nine-channel CTA) was used to survey the controlled flow (§3.4.2). The position of this sensor-array can manually be varied from $x = x_4$ to $x = x_9$, which is shown to be positioned at $x = x_5$ in Figure 3.2. Positions and relevant dimensions of the nine sensors forming the arrays at both $x_1$ and $x_5$ (s$_1$–s$_9$ for $x_1$ and s’$_1$–s’$_9$ for $x_5$) are summarized in Table 3.2. Wall-shear stress fluctuations are only resolved up to a cut-off frequency of roughly 80Hz due to sensor limitations (Hutchins et al., 2011). For the purpose of the large-scale manipulation this bandwidth is sufficient enough (a large-scale filter is discussed in §3.3.1), since $f = 80$Hz equates to a spatial wavelength of $\lambda_x \approx U_c/f \approx 0.48\delta$.

<table>
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</table>

Table 3.2: Positions and dimensions of the nine sensor–actuator pairs used for flow control. Flow conditions for the experimental campaign, at a friction Reynolds number of $Re_{\tau} \approx 14\,400$, are indicated in Table 3.1.
Figure 3.2: (a) Top-view of the floating element assembly with the real-time control hardware implemented in the wind tunnel surface. (b) Magnified top-view of the sensors $s_1$ and $s_2$. (c) Magnified top-view of the actuators $a_1$ and $a_2$. (d) Cross-sectional view A–A, indicated in sub-figure (c). Reproduced from Abbassi et al. (2017).
3.2.2 Actuation: Wall-normal Jets

The actuation of the implemented feedforward control scheme took place downstream of the sensing location via nine rectangular exit slits, each of which aligned in the streamwise direction with the upstream sensors (i.e. $s_1$–$s_9$), forming nine sensor-actuator pairs. Each jet exit slit measures $l_j \times w_j = 50.0 \times 2.0$ mm. These dimensions were adopted from an earlier study by Talluru (2014). To prevent the jet slits from acting as roughness elements, so that the near-wall region of the canonical TBL would not be disturbed, Talluru (2014) chose a relatively large aspect ratio for the jet exit planes in the streamwise direction, namely 25:1. Since each actuator was paired with each of the upstream wall-shear sensors, their spanwise spacing follows that of the sensors. Their corresponding dimensions in viscous and outer units measure $l_j^+ \times w_j^+ \approx 2004 \times 80$ and $l_j/\delta \times w_j/\delta \approx 0.139 \times 0.006$, respectively. The viscous units indicated in Table 3.1. are used for inner-normalization. The spanwise array of the actuators was actually embedded in a panel which was located at 0.6 m ($= 1.6\delta$, which is twice the width of the modular insert plate) downstream of the sensor-array—inset-Figure 3.2(c). This renders them to have an absolute streamwise location of $x_3 = 20.40$ m. Given the following factors, such streamwise spacing between the sensor- and actuator-array is the minimum streamwise distance between the sensing and the actuation locations: 1. The flow conditions under study (i.e. $Re_\tau \approx 14400$); 2. The control parameters (i.e. the implemented $1.5\delta$-long Gaussian filter on the sensor-array signals—§3.3.1); and 3. The utilized control devices (i.e. the controller—§3.2.3—and the mechanical-spring-return solenoid valves—Figure 3.3.) Figure 3.2(d) shows a cross-section of two adjacent actuators, namely $a_1$ and $a_2$.

Further details of the actuators are illustrated in Figure 3.3, together with the pneumatic circuit diagram of the compressed air preparation unit. Each actuator consists of a jet cavity (item 8 in Figure 3.3a) which is connected to a nominally-closed single Festo MPAL-IV solenoid valve (item 6 in Figure 3.3a) via an 80cm-long hose (item 7 in Figure 3.3a). The solenoid valves have a mechanical spring return mechanism and were operated via either real-time (for opposing and reinforcing control schemes) or
off-line (for desynchronized control scheme) transistor transistor logical (TTL) control signals (§ 3.3) that triggered 24V relay switches.

The actuator cavities were 3D printed using RGD720 as the raw material, which resembles standard plastic in its final processed stage. A local coordinate system \((x' - y' - z')\) is associated to each individual actuator as shown in Figure 3.3(a), referred to as the "jet coordinate system". The compressed air preparation unit for supplying the airflow through the actuators consists of the following: 1. one compressor with a nominal operating capacity of 10bar (item 1 in Figure 3.3a), 2. one reservoir with a storage volume of 1.5m\(^3\) (item 2 in Figure 3.3a), 3. two fully automatic filters to remove contamination, rust and condensate from the compressed air (items 3 and 4 in Figure 3.3a) and 4. one precision pressure regulator to adjust the flow rate out of the jet slits, which is set at approximately 0.8 bar (item 5 in Figure 3.3a).
Each jet cavity accommodates a cylindrical polyethylene porous silencer, through which compressed air is supplied—Figure 3.3(b). The porous silencer ensures that the jet exit velocity profile, in the absence of any TBL flow, has a mean velocity of $W_j \approx 12.9 \text{m/s}$ ($W_j/U_\infty \approx 0.64$ and $W_j^+ = 20.2$) and is uniform along both the longitudinal (i.e. $x'$) and the lateral (i.e. $y'$) directions of the jet exit planes—Figure 3.4. Note that $W_j$ corresponds to continuous operation of five actuators and the value of that was chosen so that the jets in cross-flow penetrated within the log-region (see §4.3).

A mechanical latency for the actuators is determined by sending a square wave TTL signal with a 20ms duration of high voltage value and simultaneously recording the evolution of the jet exit velocity in still air. The measurement was conducted using a hot-wire of 5$\mu$m-diameter which was positioned at the centroid of the jet exit plane,
Figure 3.4: Wall-normal jet airflow velocity, along the $x'$-axis (a) and $y'$-axis (b) of the jet coordinate system in still air. The velocity measurements have been conducted via a Pitot tube with an inner diameter of 0.5mm, while five of the actuators out of nine are in constant blowing mode. The top-hat exit velocity profile in both cases are evident.

namely the origin of the jet coordinate system (i.e $(x', y', z') = (0, 0, 0)$). The hot-wire was aligned in the longitudinal direction of the jet slit. The evolution of the jet exit velocity accompanied by a single 20ms square-wave driving signal is shown in Figure 3.5. As Figure 3.5 suggests, the velocity profile has an initial transient section which is slightly higher than the mean blowing velocity and a rippling section at the tail of the profile, which is believed to be due to the dynamics of the mechanical spring of the solenoid valve that is used for the return mechanism of the valve. However, the mean value of the velocity profile after the initial transient section and before the falling edge of the profile is considered to be the exit velocity of the actuators, which is determined to be 12.93 m/s. The latency of the actuator measures 14.3ms, as inferred from the time span from the rising edges of the commanding signal and the velocity signal—the rising edge of the velocity signal is defined to be where the velocity exceeds beyond $0.6U_{\infty}$. 
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3.2.3 Real-time Controller and Sampling

A Speedgoat performance real-time target machine with an Intel core 2 Duo 3.33GHz processor, with dedicated 4096MB DDR2 memory, was used as hardware for the execution of the active control strategy (§3.3), implemented via MathWorks Simulink Real-Time in an xPC target environment. A 16-bit A/D conversion via an IO106 Speedgoat module allowed for simultaneous sampling of all 22 sensors (generally two arrays of nine hot-films each, a hot-wire, ambient pressure, temperature and free-stream dynamic pressure). The controller sampling rate was set at $\Delta t = 0.25\text{ms}$, corresponding to a sampling frequency of $f_c = 4\,000\text{Hz}$. The implementation of the control logic on the target machine resulted in a task execution time (TET) of less than 0.20ms (the time it takes to accomplish one iteration through the control diagram). Real-time control was feasible since the controller sampling rate was 25% larger than the TET. Hot-film signals were acquired at the controller frequency $f_c$, although the signal of a hot-wire (§3.4.1) was simultaneously acquired at a faster sampling rate. Output commands to the 24V relays, for switching the solenoid valves, were sent via an IO205 Speedgoat module.
3.3 Control Logic and Implementation

In this section the implemented control logic are described, consisting of three general steps (Figure 3.6a): 1. Large-scale filtering; 2. Control scheme implementation; and 3. Timing of the actuation to be in-sync with the convecting flow. It is worth noting that the control logic is the same for each sensor–actuator pair, $s_i$–$a_i$ at spanwise location $y_i$.

3.3.1 Large-scale Filter

Time segments of the raw voltage signals of sensors $s_1$–$s_9$ are visualized in Figure 3.6(b) and denoted as $e(y_i, t)$; note that the amplitude of these zero-mean fluctuations at each of the nine discrete locations $y_i$ are normalized by their respective standard deviations $\sigma_i(e)$. In order to retrieve the large-scale fluctuations, $e_L(y_i, t)$, a Gaussian filter was convoluted with each signal in real-time. The Gaussian filter of six standard deviations in length spanned $1.5f_c\delta/U_c \approx 166$ samples in terms of the controller sampling rate (here a slightly larger $\delta$ of 0.369m was assumed).

The Gaussian filter is simplistically characterized as a low-pass filter with a cut-off wavelength $\lambda_{xF} \approx 1.9\delta$, defined as the scale below which the magnitude response drops below 0.707—Figure 3.7(a). Taylor’s hypothesis was used to transform frequency to wavelength. Note that $\lambda_{xF}$ is larger than the $0.48\delta$ small-wavelength limitation of the sensors (§3.2.1), hence, the signals $e_L(y_i, t)$ adequately reflect the footprints of the LSMs. One may wonder whether a filter with a sharp cut-off frequency can be used instead? With such a filter, the large-scale wavelengths can be distinctly separated from the small-scale wavelengths. However, the impulse response of such a filter would be infinite and this renders them impractical for real-time applications.
Aside from the large-scale Gaussian filter, a running mean filter compensated the drift of the hot-film signals; this running mean of 10s (40 000 samples) was subtracted from the signals in real-time. Furthermore, the commencement of the implementation of the control scheme accounted for an initialization time of the filters. A time segment of the large-scale filtered voltage signals, corresponding to the raw signals shown in Figure 3.6(b), is shown in Figure 3.6(c). Since the Gaussian filter is applied on the hot-film signals in real-time, its impulse response needs to be shifted to the right for half of the filter length (i.e. 83 sample points) to obtain the causal equivalent of the filter, as illustrated in Figure 3.7(b). Considering the convection velocity of the footprints of the large-scale coherent structures—\(U_c\)—the temporal equivalent of the above-mentioned
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shift measures $\tau_f \approx 20.8\text{ms}$, which was introduced as a time delay between the sensing and actuation processes. For visual illustration, this time delay ($\tau_f$) is accounted for in Figure 3.6(c) so that the raw and filtered fields align with each other in time.

![Figure 3.7: (a) Magnitude response of a $\Delta x_{GF} = 1.5\delta$-long Gaussian filter as a function of the streamwise wavelength. Dotted vertical line shows the position of the cut-off wavelength ($\lambda_{x,F}$), which by definition is the wavelength below which the magnitude response of the filter falls below 0.707. (b) 166-point Gaussian-filter impulse response coefficients (blue) and its causal equivalent (red), obtained by shifting the impulse response to the right by half of the Gaussian filter. For both of the impulse responses every third coefficient is shown for visual clarity.](image)

3.3.2 Control Scheme

Three control schemes were investigated for manipulating the LSMs. Nominally, an opposition control framework requires actuation of the jets at instances of high-speed events. This control scheme is referred to as an opposing scheme and a so-called binary field is formed from $e_L(y_i, t)$, following:

$$C(y_i, t) = \begin{cases} 1, & e_L(y_i, t) > 0 \\ 0, & e_L(y_i, t) < 0 \end{cases}$$

(3.1)

Here, $C(y_i, t)$ is the binary field (serving as the TTL signals controlling the valves, §3.2.3), which is either one- or zero-valued. An illustration of the contour of $C(y_i, t)$ is shown in Figure 3.6(d), which corresponds to the large-scale filtered field $e_L(y_i, t)$
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in Figure 3.6(c). Since the on/off control was based on the sign of the large-scale fluctuations, the need for calibrating the sensor-array for the real-time control is spared. During the process of real-time control, the actuators were on-duty for half of the operation time and for the other half they were off-duty. The PDF distribution of the on-duty cycles are almost identical to that of the off-duty cycles—shown in Figure 3.8.

\[
\Delta (t_Uc)/\delta
\]

\[
0 \quad 0.1 \quad 0.2 \quad 0.3 \quad 0.4 \quad 0.5 \quad 0.6 \quad 0.7 \quad 0.8
\]

\[
10^{-1} \quad 10^0 \quad 10^1
\]

\(PDF\)

\(\Delta (t_Uc)/\delta\)

\(On\text{-}duty\text{ }Periods\)

\(Off\text{-}duty\text{ }Periods\)

\[\text{Figure 3.8: PDF distributions of the on- and off-duty cycles of the actuators based on the TTL control signals developed for 13 100 boundary layer turn over times.}\]

In addition to the opposing control scheme, two other control strategies were implemented: 1. A base line case in which the same amount of jet fluid is injected into the TBL, but not in-sync with large-scale high-speed events; this case is referred to as desynchronized control. For such a scheme, a set of predetermined binary control signals that had been recorded during the operation of the opposing control scheme triggered the actuator-array. As such, both the amount and rate of the wall-normal jet airflow introduced into the TBL were identical for both the opposing and reinforcing control schemes. 2. The other control scheme is dubbed reinforcing and from a control logic point of view it is the contrary of the opposing control scheme. That is, the jet actuators are on-duty for the duration that the low-speed events are predicted to convect over them. The results of such manipulation can serve as counter-evidences for the observed results of the opposing scheme. A summary of the control schemes is provided in Table 3.3.
3.3.3 Timing of Actuation

The implementation of the feedforward active control strategy imposes time delays, which inherently requires a finite distance between the sensor- and actuator-arrays. A time delay accumulates from the mechanical activation of the jet flow ($\tau_m \approx 14.3\text{ms}$, §3.2.2), real-time large-scale filtering ($\tau_f \approx 20.8\text{ms}$, §3.3.1) and a single controller time step ($\tau_c = 0.25\text{ms}$). For the streamwise separation between sensing and actuation ($\Delta x = 0.60\text{m}$) the convection velocity of the large-scale footprint is well-approximated by $U_c$ (recall that $U_c$ is the mean streamwise velocity in the log-region at $z_L$, see §3.1). Because the LSMs in the log-region are aimed to be manipulated, while measuring their imprint at the wall, their characteristic forward leaning structure needs to be accounted for. Typically, forward inclination angles of approximately $25^\circ$ was reported by Hutchins et al. (2012) in both zero-pressure-gradient turbulent boundary layer at $Re_\tau \approx 1\,100$ (PIV data of Hambleton et al. (2006)) and atmospheric surface-layer flow at very high Reynolds numbers, $Re_\tau \sim \mathcal{O}(10^6)$—Figure 3.9.
The temporal precedence of the LSMs in the log-region, relative to their footprint, was empirically determined as the zero-shift of the peak in the temporal two-point correlation between hot-film $s_5$ and a hot-wire in the log-region. The hot-wire was nominally positioned directly above the hot-film at a wall-normal height corresponding to the geometric center of the log-region (Marusic et al., 2013), $z^+ \approx 900$. As such, a temporal precedence by $\tau_i \approx 8.0$ms is calculated. Actuators were synchronized so that the jet exit velocity reached full-flowing conditions when the LSMs at $z^+ \approx 900$ were estimated to appear at the streamwise location of the centroids of the jet exit slits. For this to be realized, an extra output delay ($\tau_e$) was implemented in the controller, which equals:

$$\tau_e = \frac{\Delta x}{U_c} - \tau_i - (\tau_m + \tau_f + \tau_c) \approx -0.2\text{ms},$$

where, $\Delta x/U_c$ is the convection time of the footprints of the LSMs. $\tau_i$ is the temporal precedence due to the structural inclination; $\tau_m$, $\tau_f$ and $\tau_c$ are the time delays due to the mechanical activation of the jet flow, real-time large-scale filtering and a single controller time step. The negative sign of $\tau_e$ in the above-mentioned equation indicates that the actuation is late for 0.2ms. Since only a positive time delay can physically be implemented in real-time, no extra delay was imposed ($\tau_e = 0$ in practice). A 0.2ms timing inaccuracy is insignificant in terms of the large-scale control (0.2ms corresponds to a scale less than $0.01\delta$ and is less than one time step of the controller). To

**Figure 3.9:** Iso-contours of $R_{uu}$ in the streamwise/wall-normal plane for zero-pressure-gradient turbulent boundary layer at $Re_\tau \approx 1100$ and atmospheric surface-layer flow at very high Reynolds numbers, $Re_\tau \sim O(10^6)$. The dashed straight lines in both figures (a) and (b) indicate an inclination angle of 25°. Reproduced from Hutchins et al. (2012).
investigate the effectiveness of the opposing control scheme as a function of $\tau_c$, control experiments were performed with a deliberately imposed delay ($\tau_c > 0$). As such, it was found that this effectiveness deteriorates as $\tau_c$ increases, which implies that the implemented timing for $\tau_c$ was optimum for the current hardware configuration. The timing of the actuation is illustrated in Figure 3.10.

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**Figure 3.10:** Illustration of the convection time of the LSMs from the streamwise positions of the sensors to those of the actuators ($\Delta x$). The total latency of the system between the sensing time and the actuation time is also illustrated, which is accumulated from $\tau_i$, $\tau_m$, $\tau_f$ and $\tau_c$. For the magnitudes and meanings of each of these individual latencies please refer to either the text or the figure.

Although the correct timing of the active control is guaranteed, the separation distance between the sensor- and actuator-arrays results in an inaccuracy related to the inapplicability of Taylor’s hypothesis. That is, the first-principal implementation of the control assumes that the footprints of the LSMs is frozen when convecting from the sensors to actuators. Baars et al. (2014) determined (with a second sensor-array at the location of the current actuator-array) that the on/off control is 68.3% accurate in terms of timing. Hence, both the opposing and reinforcing control schemes are subject to this inaccuracy.
### 3.4 The Measurement Techniques

The effects of the control schemes on both the streamwise velocity and the wall-shear stress fluctuations were examined via hot-wire anemometry. For the streamwise velocity measurements, a single hot-wire sensor was mounted on a traversing mechanism located at the centerline of the tunnel. Thus, the nominal spanwise location of the hot-wire was $y = 0$. This traversing mechanism provides two fully-automated linear degrees of freedom in the streamwise and wall-normal directions for the hot-wire. The movements in these two directions are controlled via a servo motor in each direction. The transformation from circular to linear motion is performed via a ball-screw rod and the accuracy of the movement is controlled via a linear encoder which provides a resolution of 0.5 µm in the wall-normal direction. More details of the traversing mechanism can be found in Kulandaivelu (2011). For the wall-shear stress measurement, an array of nine hot-film sensors—similar to those serving as controller input—were utilized. Due to the fact that these nine hot-films were permanently mounted on a modular panel insert (Figure 3.2), wall-shear stress measurements could only be conducted at $x = x_4, x_5, x_6, x_7, x_8$ and $x_9$. The spanwise spacing of these sensors is the same as that of the control-sensor-array—$\Delta y = 26.0$ mm (nal $\approx 1042$ or $\Delta y/\delta \approx 0.072$). With such configuration, each downstream measuring hot-film sensor was aligned with each sensor-actuator pair in the streamwise direction.

**3.4.1 Traversing Hot-wire Probe**

The hot-wire is made of a $d = 2.5$ µm-diameter platinum wire mounted on a Dantec boundary layer probe (55P15) with offset prongs. The wire has an exposed sensing element of $l_{hw} = 0.5$ mm, which provided a length-to-diameter ratio of $l_{hw}/d = 200$ (recommended by Ligrani and Bradshaw, 1987). At the experimental conditions under study, the viscous-scaled spanwise wire length measured $l_{hw}^+ \approx 20$. The hot-wire was operated with the Melbourne University Constant Temperature Anemometer (MUCTA) of in-house design with a sampling rate of $\Delta t^+ \approx 1.32$. As such, the absence
of any temporal attenuation was guaranteed by Hutchins et al. (2009). The cold resistance of the wire was measured directly via a multimeter and was determined to be 8.2Ω. To avoid oxidization of the wire element the overheat ratio was chosen as 1.72. As such, the absence of any temporal attenuation was guaranteed (Hutchins et al., 2009). Signals were acquired for a duration of approximately $20 \times 300 \delta/U_\infty$ at each hot-wire position. A relatively large value of turnover times are required to attain converged statistics in HRNBLWT, after Hutchins et al. (2009). The hot-wire characteristics are summarized in Table 3.4.

The boundary layer survey, the results of which are discussed in §4.3, was performed within the physical range of $0.3 < z < 525\, \text{mm}$, with 40 logarithmically spaced wall-normal heights. The hot-wire signal was sent through an anti-aliasing filter before it was sampled. The sampling process for the hot-wire was actually performed in a sample and hold fashion by the target machine, simultaneously with the real-time signal processing and was then transferred to the host PC for data storage and analysis.

The traversing probe was calibrated in situ against the streamwise velocity measured with a Pitot-static tube located in free-stream at 525mm above the wall. The calibration procedure for the hot-wire comprised pre- and post-calibration exercises. Each calibration exercise consisted of 16 measuring points ranging from a free-stream velocity of 0m/s to 24m/s. A fifth order polynomial curve is best fitted through each set of calibration data points, in a least-squares sense. Linear interpolation in time between the pre- and post-calibration curves is implemented to compensate for any

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Table 3.4: Sensor specifications.

3.4.1.1 Calibration

The traversing probe was calibrated in situ against the streamwise velocity measured with a Pitot-static tube located in free-stream at 525mm above the wall. The calibration procedure for the hot-wire comprised pre- and post-calibration exercises. Each calibration exercise consisted of 16 measuring points ranging from a free-stream velocity of 0m/s to 24m/s. A fifth order polynomial curve is best fitted through each set of calibration data points, in a least-squares sense. Linear interpolation in time between the pre- and post-calibration curves is implemented to compensate for any
probable temperature drift of the hot-wire sensor during the measurements. The calibration sampling length was chosen to be 40s, which provides a 0.32% uncertainty in the r.m.s. value with a 95% confidence level.

### 3.4.2 Downstream Sensor Array

Akin to the control-sensor-array (i.e. $s_1$–$s_9$), the downstream measuring-sensor-array (i.e. $s'_1$–$s'_9$) was also operated with a nine-channel (AA Lab Systems AN-1003) CTA. Two sets of measurements were conducted via these sensors:

1. wall-shear stress measurements of the uncontrolled TBL, simultaneously with the streamwise velocity measurements via the hot-wire sensor, in order to quantify the coherence magnitude (i.e. $\gamma_{uu,\tau}^2$) between these two quantities in a canonical TBL (§4.1), and

2. wall-shear stress measurements of the controlled TBL, separately from the streamwise velocity measurements via the hot-wire sensor, in order to evaluate the effect of the control schemes on the wall-shear stress (§4.4).

For both of the above-mentioned sets of measurements, the hot-film sensors were sampled at the controller sampling rate, $\Delta t^+ \approx 6.62$. However, the overheat ratios of these hot-film sensors were set to 1.05 and 1.50 for the former and latter sets of measurements, respectively. The hot-film characteristics are summarized in Table 3.4.

### 3.4.2.1 Calibration

The hot-film sensors were calibrated in situ against the unit Reynolds number (i.e. $U_\infty/\nu$). $U_\infty$ is measured using a Pitot-static tube located in free-stream at 0.525m above the wall and the kinematic viscosity calculations were conducted using the Sutherland law approximation (Sutherland, 1893), of which the corresponding atmospheric conditions were monitored via a calibrated thermocouple and an electric barometer. Using previously acquired wall-shear stress data via a floating element sensor (Baars et al., 2016b),
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\( U_\infty / \nu \) could be converted to a unit friction Reynolds number \( U_\tau / \nu \), and thus a friction velocity \( U_\tau \).

However, since the hot-films were attached to a modular panel insert (made of Aluminum), heat of the sensors could conduct to the underlying surface. Consequently, temperature variations in the laboratory directly influence the output voltages of the sensors. In an effort to account for such “inevitable” data contamination and depending on whether or not the hot-film signal was simultaneously recorded with the hot-wire signal, one of the following two calibration techniques was pursued:

1. the first technique was adopted from Talluru et al. (2014b) and will be applied to the former measurement set of §3.4.2. They proposed a calibration exercise which, apart from the standard pre- and post-calibration processes, requires regular intervals of single point recalibration measurements. As such, the changes of the temperature of the substrate is empirically taken into account. Finite-order polynomial curves are fitted through the calibration data points in a least square sense—here a fifth order polynomial is used. As suggested by Talluru et al. (2014b) a correction coefficient \( R_i = (E_i - E_{pre}) / U_\tau (E_{post} - E_{pre}) / U_\tau \) is calculated from the recalibration data point together with the pre- and post-calibration curves and a modified calibration curve is generated as follows:

\[
E_{int} / U_\tau = R_i (E_{post} / U_\tau - E_{pre} / U_\tau) + E_{pre} / U_\tau. \tag{3.3}
\]

Figures 3.11(a–d) illustrates how the numerator and denominator of the correction coefficient is determined. Depending on the relative position of the recalibration data point with respect to the pre- and post-calibration ranges two different possibilities might occur:

(a) the single point recalibration falls outside the pre- and post-calibration ranges. As such, extrapolation is implemented to calculate \( R_i \)—Figure 3.11(a), and

(b) the single point recalibration falls between the pre- and post-calibration ranges. As such, interpolation is implemented to calculate \( R_i \)—Figure 3.11(b).
Since the hot-film sensors are only capable of resolving the large-scale fluctuations accurately, prior to converting the hot-film voltage fluctuations into the friction velocity fluctuations, they are convoluted in time with a $1.5\delta$ Gaussian filter.

![Figure 3.11](image_url)

**Figure 3.11:** (a) and (b) An example of the pre-calibration ($\triangle$) and post-calibration (□) data points of the hot-film voltage against the friction velocity with their respective fifth order polynomial curve fits. The single data point (●) of the $i$th re-calibration exercise together with the corrected calibration curve (dashed line) is also shown. (c) and (d) magnified views; illustrating how each term of the correction ratio $(R_i = (E_i - E_{pre})/U_\tau)/(E_{post} - E_{pre})/U_\tau)$ is calculated.

2. The second technique required continuous logging of the temperature in the wind tunnel facility, $T$, during the course of both the calibration and experiment exercises. As such, the calibration data consist of the mean friction velocity ($U_{\tau u}$), mean voltage ($E$) and mean temperature ($T$). Figure 3.12 shows the calibration data points for hot-film sensor $s_5$ in a $T - E_{hf} - U_\tau$ domain at the nominal free-stream velocities of 17m/s, 18m/s, 19m/s, 20m/s and 21m/s. At each velocity setting the signals were recorded for 180s—the mean of which is referred to as
a single realization—resulting in a 0.11% uncertainty in the mean value with a 95% confidence level. For each nominal free-stream velocity, 20 realizations were repeated.

![Calibration data of one hot-film (sensor s'_f), consisting of the mean friction velocity (U'_f), mean voltage (E) and mean temperature (T) for five different unit friction Reynolds numbers, corresponding to the nominal free-stream velocities U'_f = 17m/s, 18m/s, 19m/s 20m/s and 21m/s. Twenty individual measurements were conducted for each respective free-stream velocity.](image)

It was observed that the calibration data obeys the following functional form:

\[ U'_f = aT + bE + c \]

where the coefficients were determined from a linear least squares fitting \((a = 0.122, b = 0.312 \text{ and } c = -1.871)\); the corresponding planar surface is shown with the mesh in figure 3.12). Having obtained how \(U'_f\) depends on the voltage of the hot-film sensor and temperature, the modified friction velocity during a case of active control could be inferred from the recordings of the respective mean voltage and temperature. Aside from the calibration
recordings, the three control schemes (Table 3.3) were also operated for 180s long realizations and repeated 20 times to check for consistency/repeatability and converged statistics of the changes in skin-friction drag (§4.4). In practice, the calibration recordings and the control experiments were carried out in an alternating sense. First, the calibration recordings were obtained at nominal free-stream velocities of 17m/s, 18m/s, 19m/s and 20m/s (four realizations). Secondly, the three control schemes were recorded at the nominal velocity setting of 20m/s (three realizations). Finally, the calibration recording was obtained at 21m/s (one realization). This sequence of eight realizations was then repeated 20 times. Alternating between the calibration and the measurement exercises is straightforward with the current set-up. It requires the control scheme being either on or off, which can be easily accomplished via the loaded data-acquisition program on the target machine, without altering the hardware components.
Chapter 4

Experimental Results and Discussion

4.1 Coherence Magnitude between $u_\tau$ and $u$ for a Canonical Turbulent Boundary Layer

As mentioned earlier in §3.2, selective wall-normal jet airflows were utilized as the manipulation scheme. Therefore, in the immediate vicinity of the actuators, the direct actuation of the scheme was merely upon the boundary layer. However, the transistor transistor logical (TTL) control signals upon which the actuators operated, had been generated from the large-scale wall-shear stress fluctuations. The existing wall-normal separation between the to-be-manipulated velocity fluctuations throughout the boundary layer and the wall-shear stress fluctuations which drive the actuators causes uncertainty in the accuracy of the outer-region structure manipulations. Furthermore, as discussed in §3.3.3, due to the finite latency of the control scheme, it is required to have the actuators at $1.6\delta$ downstream of the sensors, which further compromises the accuracy of the manipulation. This is investigated in the current section by evaluating the coherence magnitude between the $u_\tau$ fluctuations and the simultaneous $u$ fluctuations throughout the turbulent boundary layer at $1.6\delta$ downstream of the $u_\tau$ measurements.
Prior to that however, the coherence magnitude between the $u_\tau$ fluctuations and the simultaneous $u$ fluctuations throughout the turbulent boundary layer above the $u_\tau$ measurements are evaluated in order to obtain the accuracy of manipulation for a hypothetical control scheme with zero time delay. To that end, a boundary layer measurement with a traversing hot-wire sensor at $x = x_1 = 19.80m$ & $y = 0$, together with a single hot-film sensor at the same streamwise and spanwise locations as that of the hot-wire, were carried out—Figure 4.1. Similar to the measurements in which the control scheme was implemented, here the free-stream velocity was also set to be nominally $20m/s$ (equivalent to $Re_\tau \approx 14\,400$). The overheat ratios for the hot-wire and the hot-film sensors were chosen to be 1.72 and 1.05, respectively. The choice of the relatively low overheat ratio for the hot-film sensor is believed to minimize the mutual influence of these two sensors during the course of the boundary layer measurement, especially for those data points during which the hot-wire was in the proximity of the hot-film.

**Figure 4.1**: Schematic of the set-up for simultaneous measurements of the friction velocity fluctuations ($u_\tau(x)$) and streamwise velocity fluctuations throughout the turbulent boundary layer above the $u_\tau$-measurements ($u(x)$).
The coherence magnitude between the measured streamwise and friction velocity fluctuations is presented in terms of the linear coherence spectrum, denoted as $\gamma_{uu_r}^2$, and formulated as:

$$\gamma_{uu_r}^2 = \frac{|\phi_{uu}|^2}{\phi_{uu} \phi_{u u_r}}, \quad 0 < \gamma_{uu_r}^2 < 1 \quad (4.1)$$

where, $\phi_{uu_r}$ is the complex-valued cross-spectrum, and $\phi_{uu}$ and $\phi_{u u_r}$ are the power spectra densities of the streamwise and friction velocity fluctuations, respectively. By physical description, the coherence magnitude between any two signals is a coefficient—bounded by 0 and 1—which indicates the level of correlation between the constituent respective spectral components of the two composing signals. For instance, if the $\gamma^2$-value of two signals is 1 at a certain wavelength, it implies that the amplitude of oscillation per unit standard deviation of these two signals at that particular wavelength are identical with each other. Here, the two signals are the streamwise velocity fluctuations (i.e. $u$) at each wall-normal height throughout the turbulent boundary layer and the simultaneous friction velocity fluctuations (i.e. $u_r$). More details with regard to the coherence magnitude can be found in Baars et al. (2017b).

The iso-contours of the calculated coherence magnitude are plotted in Figure 4.2 as a function of the wall-normal height and the streamwise wavelength (colored contour lines). Additionally three levels, namely 1.1, 1.2 and 1.3, of the pre-multiplied energy spectrogram (i.e. $k_x \phi_{uu}/u_r^2$) of the uncontrolled turbulent boundary layer (TBL)—black contour lines—are also plotted in Figure 4.2. The iso-contours of $\gamma_{uu_r}^2$ possess a distinct peak—demarcated with a red cross (+)—at the proximity of the outer peak of the energy spectrogram—demarcated with a black cross (+). The lack of overlap of these two outer-peaks might indicate that the most energetic structures in the log-region of the TBL under the investigated flow conditions ($Re_x \approx 14 400$) are not the most influential ones on the friction velocity fluctuations.

Akin to the pre-multiplied energy spectrogram of $u$ fluctuations, the contour plots of the coherence magnitude of $u$ and $u_r$ fluctuations is also predicted to possess an inner-peak in the proximity of the inner-peak of the spectrogram—demarcated as a blue cross (+) in Figure 4.2. In other words, the influence of the energetic near-wall cycle structures on the wall should also be manifested in the coherence magnitude of
Experimental Results and Discussion

the \( u \) and \( u_r \) fluctuations. It might be deduced that since the hot-film sensors are not capable of resolving the high frequency fluctuations accurately, the inner-peak of the coherence magnitude is nullified.

Recently, Baars et al. (2017b) presented the coherence magnitude between velocity fluctuations derived from a stationary hot-wire at \( z^+ = 4.3 \) and a traversing hot-wire throughout the boundary layer. Because of the proximity of their stationary sensor to the wall (i.e. \( z^+ = 4.3 \)) it can be predicted that any two-input/one-output analysis from their data would exhibit strong resemblance with those presented here, where a glued-on hot-film sensor on the wall was utilized as the stationary sensor—given that the temporal resolution of the hot-film would be similar to that of the hot-wire. Baars et al. (2017b) observed that the iso-contours of the coherence magnitude generated from their data does not possess any inner-peak either. They attributed this to the fact that the measurement data acquired via hot-wire anemometry is temporal data. Taylor’s hypothesis together with the local mean streamwise velocity at each wall-normal height were used to convert temporal information to spatial information. Since the small-scale coherent structures in the near-wall region have a large spread of convection velocities the usage of a single convection velocity in the Taylor’s hypothesis at each wall-normal height distorts the spatial realization of the boundary layer in the near-wall region. Therefore, the coherence of the small-scale structures within that region appears to be annihilated.
Experimental Results and Discussion

Figure 4.2: Iso-contours (colored contour lines) of the coherence magnitude for the streamwise velocity fluctuations relative to the large-scale friction velocity fluctuations at the same streamwise location as that of the velocity measurements. Contour levels vary from 0.05 to 0.75 with increments of 0.05. The peak of the coherence magnitude is shown by a red cross (+). Underlying are three contour levels, namely 1.1, 1.2 and 1.3 (black contour lines), of the pre-multiplied energy spectrum of the streamwise velocity fluctuations in a canonical flow. The corresponding inner- and outer-peaks of the spectrogram are demarcated by blue (+) and black (+) crosses, respectively.

Because the actuators are 1.6δ downstream of the sensors, the coherence magnitude shown in Figure 4.2 cannot be representative of the implemented sensor-actuator configuration. In order to quantify the coherence magnitude between the \( u_\tau \) fluctuations at the position of the sensors and the \( u \) fluctuations throughout the boundary layer at the position of the actuators, simultaneous measurements using a hot-film sensor at \( x = x_1 = 19.80 \text{m} \) and \( y = 0 \) and the traversing hot-wire sensor at \( x = x_1 + 1.6\delta \) & \( y = 0 \) were conducted at a nominal free-stream velocity of 20m/s (equivalent to \( Re_\tau \approx 14400 \))—Figure 4.3.
Figure 4.3: Schematic of the set-up for simultaneous measurements of the friction velocity fluctuations ($u_\tau(x)$) and streamwise velocity fluctuations throughout the turbulent boundary layer at 1.6$\delta$ downstream of the $u_\tau$-measurements ($u(x + 1.6\delta)$).

Figure 4.4 shows the iso-contours of the coherence magnitude between $u$ and $u_\tau$ for such measurements (colored contour lines) together with three levels, namely 1.1, 1.2 and 1.3, of the pre-multiplied energy spectrogram of the uncontrolled TBL (black contour lines). According to Figure 4.4 it becomes clear that due to the streamwise spacing between the hot-film and hot-wire sensors, the associated $\gamma_{uu_\tau}^2$ is compromised with respect to that in Figure 4.2. Meaning that the iso-contours of $\gamma_{uu_\tau}^2$ occupy less of the $z^+ - \lambda^+_z$ domain and the maximum level of correlation is also lowered. This can be considered as a systematic error of the experimental set-up, since the streamwise spacing between the sensors and the actuators is an inevitable necessity. On the other hand, given the spatial resolution of the wall-shear stress sensors and the current streamwise spacing between the detection sensors and the actuators (i.e. 1.6$\delta$), such analysis can also be regarded as an assessment of the accuracy of the manipulation via the current control scheme. In other words, those coherent structures that demonstrate...
higher values of the coherence magnitude throughout the boundary layer would be manipulated more accurately than those that exhibit lower values of the coherence magnitude. Similar analysis was also conducted by Rathnasingham and Breuer (1997), and was identified as “system identification”. However, they only evaluated the coherence magnitude between a wall-shear stress sensor and a downstream hot-wire sensor at a single wall-normal height of 10 wall units and 300 wall units downstream of the detection sensor, since their control scheme was designed to manipulate only the near-wall region.

![Iso-contours (colored contour lines) of the coherence magnitude for the streamwise velocity fluctuations relative to the large-scale friction velocity fluctuations at \(1.6\delta\) upstream of the velocity measurements, contour levels vary from 0.05 to 0.4 with increments of 0.05. Underlying are three contour levels, namely 1.1, 1.2 and 1.3, of the pre-multiplied energy spectrum of the streamwise velocity fluctuations—black contour lines. The horizontal dashed line marks the cut-off wavelength corresponding to the large-scale filter.](image)

The cut-off wavelength of the large-scale filter—described earlier in §3.3.1—is also illustrated in Figure 4.4 as a horizontal dashed line. It is observed that this dashed line is tangent to where the coherence magnitude contour is tapered at the lower part, which implies that the bandpass of the impulse response of the large-scale filter would cover the entire coherence region. This, on the other hand, can be considered as justifiable evidence for the choice of the large-scale filter length—\(1.5\delta\) in the streamwise direction.
The effectiveness of the opposing control scheme at the logarithmic region with respect to the length of the implemented real-time Gaussian filter on the hot-film signals is investigated in §4.5. It is evidenced, via using real-time Gaussian filters with lengths smaller than \(1.5\delta\), that the current length of the implemented Gaussian filter (i.e. \(1.5\delta\)) is, indeed, the sub-optimal choice for the current streamwise distance between the hot-film sensors and the actuators.

4.2 Streamwise Evolution of the Manipulated Flow in the Logarithmic Region

As mentioned earlier in §2, the primary objective of the manipulation is to diminish the energetic large-scale structures in the log-region of a high-Reynolds-number TBL, in an attempt to create less top-down influence on the wall. This is hypothesized to hold the potential to reduce skin-friction drag. Therefore, prior to investigating the effects of the manipulation schemes on the wall-shear stress, it is essential to evaluate the effect of such schemes on the flow variables throughout the boundary layer and especially within the log-region. To this end, hot-wire measurements were conducted at a single wall-normal height which falls within the log-region, corresponding to that of the outer-peak in the pre-multiplied energy spectrogram—\(z^+ = 3.9\sqrt{Re_\tau}\), after Mathis et al. (2009). This measures \(z^+ = z_L^+ = 477\) at the experimental conditions under study—\(Re_\tau \approx 14 400\). The streamwise locations of these hot-wire measurements were chosen to be \(d_a/\delta = 0.8, 1.6, 2.4, 3.3, 4.1\) and \(4.9\), which coincided with the streamwise centers of the six panel inserts downstream of the actuator-array. From the above-mentioned measurements, the effects of the manipulations on: 1. The mean streamwise velocity; 2. The energy of the streamwise velocity fluctuations; and 3. The streamwise coherence of the coherent structures are investigated in §4.2.1, §4.2.2 and §4.2.3, respectively. Next, boundary layer measurements are addressed in §4.3.
4.2.1 Effect on the Mean Streamwise Velocity

The percentage variations of the mean streamwise velocity in the log-region of the manipulated TBL with respect to that of the uncontrolled TBL, that is \((U_M - U_U)/U_U(\%)\), is plotted in Figure 4.5. Here, \(U_M\) and \(U_U\) are the manipulated and uncontrolled mean streamwise velocities, respectively. According to Figure 4.5, the mean streamwise velocity for all the manipulation cases has been reduced in the log-region along all the downstream measurement locations. This indicates that the wall-normal jet airflow, regardless of the type of manipulation, generated a low-momentum region downstream of the actuators. Since, they did not possess any initial streamwise momentum as they passed through the jet exit planes, the “streamwise” momentum of the canonical TBL were violated for all the manipulation schemes, namely diminished.

Even though the amount of the introduced wall-normal jet airflow into the TBL is, on average, the same for the three control schemes, it is interesting to note that at each measurement location in Figure 4.5, the percentage variations of \(U\) differ among different control schemes. Such differences can be attributed to the difference in the violation of the “wall-normal” momentum balance of the TBL among different schemes. Since there was no prescribed synchronization between the actuation and the incoming large-scale structures for the desynchronized control scheme, in a mean sense there

![Figure 4.5](image.png)

**Figure 4.5**: Percentage variation of the manipulated mean streamwise velocity at \(z_L^* = 3.9 \sqrt{Re_x} \approx 477\) & \(y = 0\) relative to the uncontrolled flow as a function of the streamwise distance from the actuators (i.e. \(d_a\)). \(M\) and \(U\) in the subscripts stand for the manipulated and the uncontrolled cases, respectively.
would be no behavioral change in the level of fluctuating energy of these structures. Therefore, the entire amount of the $U$ reduction for the desynchronized control scheme can be attributed to the streamwise momentum hindrance of the canonical TBL.

However, for the opposing control and the reinforcing control schemes, it is believed that in addition to the violation of the streamwise momentum balance of the TBL as that observed for the desynchronized one, the change of the wall-normal momentum balance is also manifested in the reduction of the respective mean streamwise velocity. In other words, as the down-wash sections of the existing counter-rotating roll modes are counteracted in the opposing control scheme, it is predicted that the intensity of these acted-upon roll modes diminish which contributes to the reduction in $U$ for the opposing control scheme with respect to that for the desynchronized control scheme. For the reinforcing control scheme however, as the wall-normal jet air airflows act upon the up-wash sections of the existing roll modes, it is predicted that the intensity of these roll modes are enhanced, which result in compromising the reduction in $U$, with respect to the desynchronized control scheme—explicit evidence for both of the above-mentioned arguments are presented in §4.3.3 and Figure 4.10 where the conditional large-scale coherent structures are investigated.

Considering Figure 4.5, the maximum percentage reduction of $U$ in the log-region for each manipulation case is observed at the first measurement location, after which it relaxes as one moves further downstream of the actuators. The slope of the recovery appears to be approximately the same for the three control schemes, which can indicate that the rate of the streamwise recovery of the manipulated turbulent boundary layer downstream of the actuators depends only on the flow conditions, regardless of the initial state of manipulation.
4.2.2 Effect on the Energy of the Streamwise Velocity Fluctuations

The percentage variations of the streamwise velocity variance at $z^+ \approx 477$—corresponds to the wall-normal height of the outer-peak in the energy spectrogram—due to the manipulation schemes, with respect to that of the uncontrolled scheme are plotted in Figure 4.6(a) as a function of the downstream distance from the actuators. The respective pre-multiplied energy spectra of the $u$ fluctuations for the desynchronized, opposing and reinforcing control schemes at $d_a/\delta = 0.8, 1.6, 2.4, 3.3, 4.1$ and $4.9$ are plotted in Figures 4.6(b–d), together with that of the uncontrolled $u$ fluctuations at $d_a = 0.8\delta$. Note that, temporal data were transformed to spatial representatives by using Taylor’s hypothesis, where the local mean velocity is taken as the convection velocity. Baars et al. (2014) showed that the mean streamwise velocity at $z^+ = 3.9\sqrt{Re_\tau}$ is $U_c^+(x_F, z^+_L) \approx 21.7$, which can be considered to be an approximate convection velocity for the large-scale structures residing at that wall-normal height.

According to Figure 4.6(a), for the desynchronized control scheme, apart from an increase by 6% in $\overline{u^2}$ at the first measurement location (i.e. $d_a = 0.8\delta$), no considerable energy change of the streamwise velocity fluctuation is observed downstream of the actuators. However, for the opposing and reinforcing control schemes, a significant decrease and increase of $\overline{u^2}$ can be observed, respectively, with the corresponding variations of around $-13\%$ and $22\%$ for the first measurement location. Examining the associated energy spectra in Figure 4.6(b–d), reveals that for the desynchronized control scheme, there is no considerable energy change across the scales. A slight increase in the moderate-scale energy with respect to that for the uncontrolled scheme can be observed at the first measurement location. This increase is also observed for both the opposing control and the reinforcing control schemes at the first measurement location. Since this appeared to be a common feature for the three control schemes, it might be attributed to either or both of the following factors: 1. The energy contribution by the lifted small-scale structures which used to reside in the near-wall region; and 2. The energy contribution by the turbulence activity in the shear layer generated between the wall-normal jet airflow and the cross flow.
Figures 4.6(c–d) reveal that the large-scale energy is significantly attenuated and enhanced for the opposing control and the reinforcing control schemes, respectively. This implies that the opposing control scheme generates desirable behavioral change of the large-scale energy (i.e. large-scale energy reduction). Furthermore, the reinforcing control scheme demonstrates detrimental behavioral change of the large-scale energy. Therefore, it can be deduced that the reason why the desynchronized control scheme does not show any change of the large-scale energy is because of the fact that for such a scheme nearly half of the wall-normal jet airflows act upon the high-speed events and the other half of those act upon the low-speed events. As such, the beneficial effects of manipulating the high-speed events are canceled out by the detrimental effects of the low-speed-event manipulation. Hence, there is almost no net effect in terms of the variation of the fluctuating energy across the scales. Therefore, it can be confidently concluded that among all three schemes, only the opposing control scheme meets the primary objective of the manipulation (i.e. large-scale energy reduction). Furthermore, according to Figures 4.6(b–d), it is observed that unlike the immediate recovery of the moderate-scale energy from the slight increase for all the manipulation schemes, the large-scale energy change for the opposing control and the reinforcing control schemes persists even until 5δ downstream of the actuators.
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Figure 4.6: (a) Percentage variation of the variance of the \textit{manipulated} streamwise velocity fluctuations at $z^*_L = 3.9\sqrt{Re} \approx 477$ & $y = 0$ relative to the \textit{uncontrolled} variance as a function of the streamwise distance from the actuators. $M$ and $U$ in the subscripts stand for the manipulated and the uncontrolled cases, respectively. (b–d) Pre-multiplied energy spectra of the manipulated streamwise velocity fluctuations, corresponding to the variance-curve in (a), together with that of the uncontrolled streamwise velocity fluctuations at $d_a = 0.8\delta$. The vertical dashed lines mark the cut-off wavelength corresponding to the large-scale filter. For all figures, the uncontrolled $U_r$, as determined in §3.1, was used for inner-normalization.
According to Figure 4.6(a), the percentage variation of the streamwise velocity variance for the opposing scheme demonstrates a minimum at the second measurement location by approximately $-16\%$, beyond which it relaxes towards $0\%$ by moving downstream of the actuators. Since the addition of the moderate-scale energy seems to dissipate faster than the recovery-effect of the suppression of the large-scale energy, the observed minimum in the variance profile of the opposing control scheme in Figure 4.6(a) can be explained. In other words, at the first measurement location for the opposing control scheme, the competing effects of the increase of $u^2$—contributed by the moderate-scale energy increase—and the reduction of $u^2$—contributed by the large-scale energy decrease—leads to less net reduction of the percentage variance of $u^2$ than that at the second measurement location. Hence, the minimum in the corresponding percentage variation of $u^2$ is manifested at the second measurement location.

### 4.2.3 Effect on the Auto-correlation of the Streamwise Velocity Fluctuations

The streamwise coherence of the structures within the boundary layer can be evaluated via calculating the integral time-scale of the $u$ fluctuations, defined as:

$$T_{uu}^+ = \int_0^\infty R_{uu} d\tau_L^+$$

where, $R_{uu}$ is the auto-correlation coefficient of the streamwise velocity fluctuations and $\tau_L^+$ is the time-lag associated with calculating the correlation coefficient normalized with the uncontrolled inner-variables. Here, the upper limit of the integration is chosen to be $2000\nu/U_{\tau}^2$. Beyond that the auto-correlation coefficient for the uncontrolled case drops below 0.007 which is believed to be a reasonable approximation of the asymptotic limit. The percentage variation of $T_{uu}^+$, at the log-region downstream of the actuators are plotted in Figure 4.7(a) for all three types of manipulation schemes. The corresponding auto-correlation coefficients as a function of the time-lag are plotted in
Figures 4.7(b–d) for the desynchronized, opposing and reinforcing control schemes, respectively, together with that of the uncontrolled TBL. For clarity the magnified views of each $R_{uu}$ curves are also shown in the insets of Figures 4.7(b–d).

According to Figure 4.7(a), it becomes clear that the integral time-scale for the desynchronized control scheme along the streamwise distance has been altered negligibly. Figure 4.7(b) reveals that the corresponding $R_{uu}$ for neither the small nor the large values—considered here as those of the order of $10\nu/U^2_\tau$ and $100\nu/U^2_\tau$, respectively—of the time-lag were modified. Therefore, it can be concluded that for such a scheme both the small- and large-scale structures exhibit the same level of the auto-correlation as that of the uncontrolled structures.

According to Figure 4.7(a), the integral time-scale along the downstream distance from the actuators has been decreased and increased significantly for the opposing control and the reinforcing control schemes, respectively. The corresponding percentage changes of $T_{uu}^<$ for the former and latter schemes approximate $-37\%$ and $23\%$ at the first measurement location. These variations fade away by moving downstream from the actuators. Exploring the corresponding $R_{uu}$ as a function of the time-lag in Figures 4.7(c–d), shows that the change in the integral time-scale is mainly contributed by the change of $R_{uu}$ for large values of the time-lag. This indicates that the opposing control scheme weakens the auto-correlation of the large-scale structures in the log-region, whereas the reinforcing control scheme strengthens such correlation.

The behavioral change of the auto-correlation coefficient as a function of the time-lag for the three control schemes shown in Figures 4.7(b–d) might suggest the following statement from a qualitative point of view: The large-scale coherent structures become more and less prevalent for the reinforcing and opposing control schemes, respectively, compared to those for the uncontrolled case. It is interesting to note that the former and latter phenomena are accompanied by the large-scale energy increase and decrease, respectively—as inferred from the results of the previous section (i.e. §4.2.2). For the desynchronized control scheme, the large-scale coherent structures do not tend to be prevalent for any particular range of the streamwise wavelength, and this is accompanied by the lack of change of the strength of these structures.
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Figure 4.7: (a) Percentage variation of the integral time-scale of the manipulated streamwise velocity fluctuations at $z_L^+ = 3.9\sqrt{Re}\tau \approx 477$ & $y = 0$ relative to the uncontrolled integral time-scale as a function of the streamwise distance from the actuators. (b–d) Auto-correlation coefficient of the manipulated streamwise velocity fluctuations, $R_{uM,uM}$, corresponding to the integral time-scale in (a) as a function of the time-lag, $\tau_\ell^+$, together with that of the uncontrolled streamwise velocity fluctuations, $R_{uU,uU}$, at $d_a = 0.8\delta$. The uncontrolled inner-scales have been used for normalization. $M$ and $U$ in the subscripts stand for the manipulated and the uncontrolled cases, respectively. The uncontrolled $U_\tau$, as determined in §3.1, was used for inner-normalization.
4.3 Manipulation Effect throughout the Boundary layer

Boundary layer measurements were conducted for the uncontrolled and the manipulated TBLs at $d_a = 1.6\delta$, where the maximum reduction of the streamwise velocity variance has been observed for the opposing control scheme at the log-region—Figure 4.6(a). The details regarding how the boundary layer survey was conducted can be found in §3.4.1. The manipulation influence on the mean streamwise velocity and the turbulence intensity, together with the corresponding energy spectra are addressed in §4.3.1 and 4.3.2, respectively. The effects on the conditional large-scale structures are investigated in §4.3.3. Furthermore, the manipulation effects on the modulation coefficient of the large-scale component of the velocity fluctuations and the filtered envelope of the small-scale component of the velocity fluctuations throughout the boundary layer are explored in §4.3.4.

4.3.1 Effect on the Mean Velocity Profile

The mean streamwise velocity profile for the uncontrolled and the manipulated TBL is plotted in Figure 4.8(a). In order to highlight the absolute differences between the un-manipulated and manipulated TBLs the uncontrolled inner-variables have been used for normalization. The vertical dashed lines in Figure 4.8(a) mark the lower- and upper-bound of the log-region, after Marusic et al. (2013), namely $3Re_\tau^{1/2}$ and $0.15Re_\tau$. As seen in Figure 4.8(a), in a mean sense, the wall-normal jet actuators generate a low-speed region from the wall, up to the penetration height (denoted as $z_p^+$) of the jet airflow into the turbulent boundary layer, which exceeds the upper limit of the log-region. This implies that at the current experimental conditions and with the chosen jet exit velocity ($V_j/U_\infty \approx 0.64$), direct manipulation of the large-scale structures in the log-region is ensured. The respective penetration heights are marked with dash-dotted lines in Figure 4.8, which are determined to be the wall-normal heights where the mean streamwise velocity of the manipulated boundary layer reaches 99.9% of that of the uncontrolled one.
Experimental Results and Discussion

Figure 4.8: (a) Mean velocity profile of the uncontrolled and the manipulated TBLs. (b) Percentage variation of the manipulated mean velocity with respect to that for the uncontrolled flow, throughout the boundary layer. Dashed lines mark the lower- and upper-bound of the log-region. Dash dotted lines mark the respective penetration heights of the actuator-flows for each of the manipulated cases as specified on the graph. The measurements have been conducted at \( d_a/\delta \approx 1.6 \) downstream of the actuation at \( y = 0 \). For both figures, the uncontrolled inner-variables have been used for normalization.

In order to highlight the change of the mean streamwise velocity due to manipulation schemes, the percentage variation of the manipulated \( U \) at each wall-normal height with respect to the uncontrolled \( U \) is plotted in Figure 4.8(b). Apart from the first two closest measurement points to the wall, the percentage reduction of the mean streamwise velocity for the opposing control scheme at each wall-normal height is the largest among all the schemes. This is followed by that for the desynchronized control scheme and the reinforcing control scheme, respectively. This sequence has also been observed earlier in §4.2.1, for the mean streamwise velocity in the log-region along the downstream distance from the actuators.

As already mentioned in §4.2.1, it can be concluded again from Figure 4.8 that the wall-normal jet actuation generally reduces the streamwise momentum of the TBL. Moreover, as the wall-normal jet airflows act selectively upon only one type of the predicted large-scale structures—high-speed events for the opposing control scheme and low-speed events for the reinforcing control scheme—in addition to the primary influence of reducing the streamwise momentum, a secondary influence also exists. This secondary influence might explain the observed offset between the percentage
variation of $U$ for the opposing and the reinforcing control schemes with respect to that for the desynchronized control scheme. As the wall-normal jet airflows counteract the down-wash sections of the existing counter-rotating roll modes in the opposing control scheme, the strength of these roll modes is reduced, and this contributes to the mean streamwise velocity reduction. The opposite occurs for the reinforcing control scheme. That is, the wall-normal jet airflow enhanced the strength of the counter-rotating roll modes as they act upon the low-speed events. Consequently, the reduction in the mean streamwise velocity is compromised with respect to that of the desynchronized control scheme.

The behavioral change of the first two measurement points in Figure 4.8(b) is not in agreement with that of the rest of the boundary layer data points. The author acknowledges that there is not yet have enough evidence to make a conclusion about such an observation. However, it can be speculated for now that it might be caused due to the proximity of the hot-wire to the wall, which might have contaminated the hot-wire signal via the heat transfer between the sensing element and the aluminum substrate. In order to investigate this possibility, further hot-wire measurements may be conducted with a substrate which is heat resistant (e.g. a wooden substrate). Alternatively, other means of velocity fluctuation measurement can be utilized which are not sensitive to the temperature change of the substrate, for instance laser doppler anemometry (LDA) or particle image velocimetry (PIV) measurements.

Due to the reduction of the mean streamwise velocity throughout the majority of the boundary layer, the streamwise momentum of the TBL is also reduced, which entails an increase in the momentum thickness. However, such increase does not necessarily suggest increase in the skin-friction coefficient. The von Kármán integral relation for zero-pressure-gradient flat plate with wall transpiration reads: $C_f = 2(\frac{d\theta}{dx}) - 2C_q$, after Gad-el Hak (1994), where $C_f$ is the local skin-friction coefficient, $\theta$ is the momentum thickness and $C_q$ is the transpiration coefficient ($\equiv \frac{V_j}{U_\infty}$), in which $V_j$ is the wall-normal velocity of the fluid injected through the surface. Since $C_q > 0$, according to the von Kármán integral relation, the possibility of skin-friction drag reduction still exists for all three schemes.
The mass flow rate through the jet exit planes in the wall-normal direction for all nine jets is approximated as:

\[ \dot{m}_{\text{jets}} = \frac{(\rho_j W_j A_j)}{2} = 0.0121 \text{kg/s} \]  

(4.3)

where, \( \rho_j \) is the density of the compressed air at the jet exit plane. Since the precession pressure regulator upstream of the jet cavities was set at 0.8 bar (Section 3.2.2), from ideal gas law \( \rho_j \) reads \( (p_{\text{atm}} + 0.8 \text{bar})/(R_{\text{air}} T) \). \( W_j \) is the jet exit velocity at the jet exit plane (Figure 3.4), and \( A_j \) is the summation of the areas of nine jet exit planes (i.e. \( 9(w_j \times l_j) \), Figure 3.2). \( w_j = 2.0 \text{mm} \) and \( l_j = 50.0 \text{mm} \) are the spanwise width and streamwise length of each jet slits, respectively. The division by 2 in the above-mentioned formula is because, on average, approximately half of the nine jets are on-duty at each instant in time.

The average streamwise mass flow rate of the TBL up to the boundary layer thickness over the spanwise width of the jet array measures:

\[ \dot{m}_{\text{TBL}} = \int_0^\delta \rho U_U(z) \times (8\Delta y + w_j) \times dz = 2.383 \text{kg/s} \]  

(4.4)

where, \( \rho \) is the density of the air throughout the boundary layer. \( U_U(z) \) is the mean streamwise velocity of the unmanipulated TBL as a function of wall-normal height. \( \Delta y \) is the spanwise spacing of the actuators. Comparing the two above-mentioned mass flow rates with each other, one can easily deduce that the former mass flow rate (i.e. \( \dot{m}_{\text{jets}} \)) is only 0.51% of the latter one (i.e. \( \dot{m}_{\text{TBL}} \)). Therefore, with such a negligible mass flow rate ratio, it seems to be highly unlikely that any major boundary layer separation would occur that reaches to the downstream far-field (i.e. distances of the order of \( \delta \)) of the actuators. This can also be evidenced from Figure 4.8(a), which does not show any deflection points in the mean velocity profiles of the manipulated TBLs at 1.6\( \delta \) downstream of the actuators.

However, since the jet exit velocity is relatively high (approximately 20 wall units), one might predict the existence of a local crossflow boundary-layer separation in the downstream near-field of the actuators. These types of flow separation have been coined as...
“separation events” by Fric and Roshko (1989). They suggested that the strong adverse pressure gradient zone generated at the immediate vicinity of the rear side of the jet slits leads to such phenomena. The author acknowledges that no near-field measurements have been conducted during the measurement campaigns of this study. So, there not yet enough data to conclusively confirm whether the turbulent boundary layer underwent any local flow separation.

4.3.2 Effect on the Turbulence Intensity and the Energy Spectra

The turbulence intensity profiles for the uncontrolled and the manipulated TBLs at \( d_a = 1.6\delta \) are plotted in Figure 4.9(a). The corresponding pre-multiplied energy spectra as a function of the streamwise wavelength throughout the boundary layer—known as spectrogram—are plotted in Figures 4.9(b) and 4.9(c–e), for the uncontrolled and the manipulated TBLs, respectively. In order to highlight the difference, Figures 4.9(c–e) are subtracted from Figure 4.9(b) and the resultants are plotted in Figures 4.9(f–h). The horizontal dash lines in Figures 4.9(f–h) mark the cut-off wavelength of the implemented large-scale filter on the upstream hot-films. The respective penetration heights are also marked in Figures 4.9(f–h) via vertical dot-dashed lines. As such, the wall-normal–wavelength domain \((z^+ - \lambda_z^+)\) are divided into four separate regions, demarcated with I–IV.

The spectrogram of a high-Reynolds-number turbulent boundary layer exhibits a bimodal behavior which is manifested as two energetic peaks in the spectrogram domain—shown in Figure 4.9(b): (i) an inner-peak at \( z^+ \approx 12 \) \& \( \lambda_x^+ \approx 1000 \) attributed to the near-wall cycle of streaks and quasi-streamwise vortices—demarcated by a white cross—and (ii) an energetic outer-peak located nominally at \( z_L^+ = 3.9\sqrt{Re_{\tau}} \) \& \( \lambda_x \approx 3\delta \), attributed to large scale motions (LSMs) and superstructures in the log-region (Hutchins and Marusic, 2007a)—demarcated by a black cross. It is worth mentioning again that the primary objective of the current study is to diminish the associated energy of the large-scale structures residing in the log region of a high-Reynolds number TBL. That is, to decrease the energy level of the above-mentioned outer-peak region of the spectrogram.
Figure 4.9: (a) Turbulence intensity profiles of the uncontrolled and the manipulated TBLs. Dashed lines mark the lower- and upper-bound of the log-region. (b) and (c–e) Contours of the pre-multiplied energy spectra of the streamwise velocity fluctuations for the uncontrolled, $k_z \phi_{uuu}/U_{\tau_1}^2$, and manipulated schemes, $k_z \phi_{uuu}/U_{\tau_1}^2$, respectively. (f–h) Absolute difference of the energy spectrograms presented in sub-figures (c–e) and (b). Dash dotted lines mark the respective penetration heights of the actuator-flows for each of the manipulated cases. The horizontal dashed line marks the cut-off wavelength corresponding to the large-scale filter. The measurements have been conducted at $d_a/\delta \approx 1.6$ downstream of the actuation at $y = 0$. For all figures, the uncontrolled inner-variables were used for normalization.
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The majority of the turbulence intensity profile for the desynchronized control scheme—shown by gray square marks in Figure 4.9(a)—has been unaltered with respect to that for the uncontrolled scheme—shown by black solid circles in the same figure. An excess of the turbulence intensity can be observed in the log-region, which can be attributed to the introduction of the turbulence activity by the upper part of the wall-normal jet airflow into the turbulent boundary layer. The inflicted energy excess, shown by red iso-contours in Figure 4.9(f), does not demonstrate a significant regional overlap with the outer-peak region. This supports the speculation that the excess amount of energy is, indeed, due to the introduction of the wall-normal jet airflow into the TBL, rather than any displacement of the energy of the spectrogram domain.

According to Figure 4.9(a), the opposing control scheme demonstrates reduction in the turbulence intensity from the first point of the boundary layer measurement (i.e. $z^+ \approx 8.6$) until a wall-normal height of $z/\delta \approx 0.08$. Above that, there is an increase in the turbulence intensity, which relaxes towards those values of the canonical case by approaching the upper-bound of the log-region. Despite such an energy increase, the majority of the log-region experiences reduction in the turbulence activity. Since the turbulence activity in the log-region is mainly contributed by energetic large-scale structures, it can be predicted that such energy reduction is due to the alleviation of the energy of those structures. Inspecting the respective spectrogram in Figure 4.9(d) supports the prediction of the large-scale energy decrease in the log-region, as inferred from the obvious elimination of the outer-peak region. Moreover, it is clear that the region of the diminished large-scale energy (negative-valued regions in Figure 4.9(g)) is largely restricted to wavelengths $\lambda_x > \lambda_{xF}$, demonstrating that the controller is successfully targeting the specified large-scale zones. The red solid dot in Figure 4.9(g) demarcates the highest reduction in spectral energy which is calculated to be approximately $-33\%$. It is believed that the control parameters still hold the potential to be modified so that more energy reduction would be obtained in the spectrogram domain. The large-scale energy has also been reduced significantly in the near-wall cycle for the opposing control scheme, as seen in Figure 4.9(g). Hence, the attenuation of the inner-peak of the turbulence intensity profile for the opposing control scheme in Figure 4.9(a)
is mainly attributed to the weakening of the large-scale structures in the near-wall region, together with the slight diminish of the energetic small-scale streamwise vortices in that region—as inferred from the slight blue iso-contours in Figure 4.9(g). Akin to the desynchronized control scheme, for the opposing scheme, an energy increase at the proximity of the upper-bound of the log-region can be observed in Figure 4.9(g). This, again, can be attributed to the inevitable consequence of the introduction of the wall-normal jet airflow into the TBL. The observed surplus of the turbulence intensity within a wall-normal range of $z/\delta = 0.05–0.2$ for the opposing control scheme in Figure 4.9(a) can also be explained due to such energy increase.

The reinforcing control scheme demonstrates increase in the turbulence intensity, shown by blue inverse triangular symbols in Figure 4.9(a), from the second point of the boundary layer measurement until a wall-normal height of $z = 0.2\delta$. Examining its associated spectrogram in Figure 4.9(h), it becomes clear that the outer-peak energy is significantly magnified. The majority of the energy increase occurs in the regions above the cut-off wavelength of the implemented filter, with a maximum increase in the spectral energy by as much as 43%—demarcated by black solid circle in Figure 4.9(h). The observed energy increase at the top-right corner of region I in Figure 4.9(h) can again be attributed to the increase of the turbulence activity due to the shear layer between the upper-part of the wall-normal jet airflow and the boundary layer flow.

The mechanisms responsible for all the above-mentioned energy changes can be summarized as follows: As the wall-normal jet actuators act upon the predicted high-speed events (i.e. the opposing control scheme) their accompanying counter-rotating roll modes are counteracted, since the down-wash sections of these roll modes are embodies within high-speed events. The weaker roll modes result in both weaker high- and low-speed events, and this is manifested in the attenuation of the outer-peak region—less large-scale fluctuating energy—of the spectrogram. The opposite happens as the wall-normal jet actuators act upon the predicted low-speed events (i.e. the reinforcing control scheme) as inferred from the energy increase in the outer-peak region shown in Figure 4.9(h). Furthermore, the lack of any considerable change in the outer-region energy of the desynchronized-manipulated TBL observed in Figure 4.9(c) is due to the fact that approximately half of the manipulation is upon the high-speed events and
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the other half is upon the low-speed events. As such, the beneficial effect of the high-speed-event manipulation for the outer-region energy is canceled out by the detrimental effect of the low-speed-event manipulation for that region. More evidence of the mechanism behind the manipulation schemes is presented in §4.3.3. Since the purpose of the current study is to reduce the mean wall-shear stress via diminishing the energetic large-scale structures, the reinforcing control scheme is clearly not suitable for such purpose. However, it might be used to increase mixing, in which high turbulence activity within the boundary layer is desired.

Apart from the difference in the primary influence of the actuation on the outer-region energy for different types of the manipulation schemes, all the schemes seem to share a common secondary influence of the wall-normal jet airflow injection. The left section of region I in Figures 4.9(f–h) experiences a slight reduction in the associated energy as inferred from the blue color of the iso-contours within that region. This might be attributed to the following fact: Regardless of the timing between the incoming large-scale structures and the wall-normal jet actuation, the small-scale structures residing in the near-wall region are lifted to regions beyond the near-wall region via the wall-normal jet airflow. Because the turbulence intensity of the outer region is less than that of the near-wall region, they enter into regions of less turbulence activity. Consequently, their associated fluctuating energy become attenuated. Therefore, as they fall back to the same wall-normal heights where they used to reside, they demonstrate less fluctuating energy than before. The reason why such behavior is claimed to be a secondary influence is because the temporal and spatial characteristics of the actuators are much larger than those corresponding to the near-wall structures, and yet these structures are modified.

4.3.3 Effect on the Conditional Large-scale Structures

Conditional averages of the large-scale structure were constructed from the full boundary layer profile measured at $d_a/\delta = 1.6$. For each wall-normal position of the hot-wire, the fluctuating streamwise velocity (i.e. $u(z, t)$) is conditioned on the zero-crossings of the large-scale component—filtered with a $1.5\delta$-long Gaussian filter in the streamwise
direction—of the voltage signal of the spanwise centered upstream hot-film (i.e. sensor $s_5$). The resultant conditional averages would have been the same if the detection was based on the calibrated hot-film signals. Zero-crossings with a positive temporal gradient are selected and the conditional averaging is performed according to:

$$
\tilde{u}^+(z, \tau) = \left\{ u(z, t) \left| \left( e_L(y_5, t - \tau) = 0 \& \frac{\partial e_L(y_5, t - \tau)}{\partial t} > 0 \right) \right. \right\},
$$

(4.5)

where, $\tau$ is the time coordinate of the conditional velocity and the angular brackets denote an ensemble average over multiple conditional points. With the current flow conditions (i.e. $U_\infty = 20$m/s) and the chosen sampling time of the hot-wire measurements (i.e. 360s), on average 4 000 ensembles are used per each point of the boundary layer survey. The tilde sign in $\tilde{u}^+(z, \tau)$ denotes the conditional fluctuation. $e_L$ is the large-scale voltage signal of sensor $s_5$, located at $y_5$ & $z = 0$. An iso-contour map of $\tilde{u}^+(z, \tau)$ for the uncontrolled and the manipulated flows are shown in Figures 4.10(a) and (b–d), respectively. Additionally the respective conditional averages of the manipulated flows are subtracted from that of the uncontrolled flow and plotted in Figures 4.10(e–g).

The local mean streamwise velocity at $z^+ \approx 477$ for each respective case in Figures 4.10(a–d) is used as the convection velocity for the conditional averages throughout the boundary layer. The convection velocities of the coherent structures in a TBL are scale-dependent (Del Álamo and Jiménez, 2009; Monty and Chong, 2009). However, since these conditional averages are based on the filtered hot-film signals, they only reflect the large-scale structures. Therefore, the usage of a constant convection velocity is unlikely to substantially alter the principal results and conclusions that are based on the observations of the conditional large-scale structures, as noted by Hutchins et al. (2011).

A forward inclined low-speed event followed by a forward-inclined high-speed event is visible in Figure 4.10(a). If the condition would have been based on the zero-crossings with a negative temporal gradient, the high-speed event was preceded by the low-speed event in the conditional plot. However, it does not affect the interpretation of
the results. Hutchins et al. (2011) conducted similar analysis, in which they conditioned the streamwise velocity fluctuation based on all the negative instances of the upstream hot-film. As such, only the low-speed event would be generated from the conditioned signals. The use of the zero-crossing for the conditional averages here, has the advantage of reconstructing both the high- and low-speed events in an ensemble sense within one single contour plot. Relative symmetry in the shape and level of intensity of the high- and low-speed events is seen in Figure 4.10(a), which is expected for a canonical case.

As mentioned in §4.3.2, the spectrogram of a high-Reynolds-number TBL demonstrates a bimodal behavior. Similarly, it can be predicted that the conditional averages of such a TBL should also demonstrate a bimodal behavior. In other words, in addition to the large-scale high- and low-speed events seen in Figure 4.10(a), the near-wall streaks associated with the energetic streamwise vortices in the near-wall region should also have been reconstructed via the ensemble average process. However, Figure 4.10(a) does not possess any bimodal shape, and this can most likely be attributed to the two following factors: 1. The streamwise spacing between the hot-film and the hot-wire is too large for the near-wall streaks to maintain their streamwise coherence; and 2. The hot-film sensors are not able to accurately resolve the high-frequency fluctuations associated with the near-wall streaks. Furthermore, the conditional signals are filtered with a large-scale Gaussian filter in the streamwise direction. This results in canceling out all the small-scale components that are attributed to the near-wall streaks—those which are below the cut-off frequency of the implemented Gaussian filter. Nevertheless, since the focus of this study is to manipulate the large-scale high- and low-speed events, the lack of appearance of the near-wall streaks in the conditional contour plots does not affect the upcoming interpretation of the results.

For the desynchronized control scheme, the large-scale high- and low-speed events maintain their original shape as well as their relative intensity in the manipulated environment—as shown in Figure 4.10(b). The lack of the behavioral change of the relative amplitudes of the high- and low-speed events can be revealed in Figure 4.10(e), in which no distinguishable pattern is observed.
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For the opposing control scheme, it is clear from Figure 4.10(c) that the intensity of both the high- and low-speed events are attenuated. For such a scheme, the actuation is only upon the predicted high-speed events. Therefore, the change in the relative amplitude of the high-speed events and the low-speed events can be considered as the primary and the secondary influence of the manipulation on the conditional events, respectively. As the wall-normal actuators act upon the high-speed events, the down-wash sections of the existing counter-rotating roll modes are counteracted,
since the high-speed events are accompanied by the down-wash sections of these roll modes in a mean sense. Consequently, the intensity of the manipulated roll modes are reduced and this results in diminishing not only the high-speed events, but also diminishing the low-speed events. In other words, in the opposing-controlled environment the high- and low-speed events have become slower and faster, respectively, as inferred from the respective blue and red iso-contours in Figure 4.10(f).

According to Figure 4.10(f), the leading and trailing edges (downstream and upstream interfaces) of the large-scale high-speed events are affected more than the middle sections of these events; as inferred from the intensity of the blue contours in Figure 4.10(f). This is speculated to be due to the transient nature of the manipulation at the beginning and end of the duration of the wall-normal jet airflow, which might lead to the generation of vortical structures with up-wash sections in the middle. The interaction of these vortical structures with the existing counter-rotating roll modes accompanying the leading and trailing edges of the high-speed events, together with the interaction of the wall-normal jet airflow with the down-wash sections of these roll modes result in a predominant opposition mechanism of the manipulation. This, in turn, is manifested in larger hindrance of the streamwise momentum of the high-speed events at their corresponding leading and trailing edges than that at the middle of these events.

The opposite of what has been observed for the opposing control scheme in Figures 4.10(c) and (f) can be observed for the reinforcing control scheme in Figure 4.10(d) and (g). As the wall-normal actuators act upon the low-speed events, the existing counter-rotating roll modes are intensified, since the up-wash sections of these roll modes are accompanied by the low-speed events. The enhanced roll modes result in enhanced high- and low-speed events with respect to those of the uncontrolled TBL. These observations of the variation of the conditional large-scale structures is in line with those of the energy spectra in Section 4.3.2.
4.3.4 **Effect on the Modulation Coefficient**

It was demonstrated earlier in §4.3.2 that within the trajectory of the upper-part of the wall-normal jet airflow into the TBL the level of the small- and moderate-scale turbulence activity was increased, which was inferred from the increase of the iso-contour levels of the spectrogram of the $u$ fluctuations. This excess of turbulence activity can further be evaluated from a temporal point of view by investigating the modulation coefficient of the large-scale component of the $u$ fluctuations and the filtered envelope of the small-scale component of the $u$ fluctuations throughout TBL. Additionally, such analysis can provide us extra evidence of the synchronization of the wall-normal jet airflow with the large-scale high- and low-speed events for the opposing and reinforcing control schemes, respectively.

In a canonical TBL, the small-scale structures have a preferential arrangement around the large-scale high- and low-speed events, which can be explained by the hairpin packet model of Adrian et al. (2000). According to this model, hairpin vortices are aligned in the streamwise direction and they straddle the low-speed events in a packet formation. Therefore, the small-scale turbulence activity appears to be enhanced on the back side of the low-speed events, which is attributed to the presence of the heads of the hairpin vortices. Since the low-speed events are flanked by the high-speed events, the hairpin vortex legs are located underneath the high-speed events. Such preferential arrangement was quantified in a high-Reynolds-number TBL by Mathis et al. (2009), in which they calculated the amplitude modulation coefficient of the large-scale component of the velocity fluctuations ($u_L^+$) and the filtered envelope of the small-scale component of the velocity fluctuations ($E_L(u_S^+)$) throughout the boundary layer, denoted by $R$ and formulated as:

$$R(\tau, z) = \frac{\mathcal{F}^{-1}(G_{u_L^+E_L(u_S^+)})}{\sqrt{u_L^{+2}E_L(u_S^+)^2}}$$  \hspace{1cm} (4.6)$$

where $\mathcal{F}^{-1}$ is the inverse Fourier transform of cross-spectrum $G_{u_L^+E_L(u_S^+)} = \mathcal{F}(u_L^+)(\mathcal{F}(E_L(u_S^+)))^*$. $\mathcal{F}$ is the Fourier transform and $*$ is the complex conjugate. $\sqrt{u^2}$ denotes the root mean square value of the signal $u$. Mathis et al. (2009) showed that the sign of $R$ changes
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from positive to negative approximately at the geometric center of the log-region (i.e. $3.9(Re_\tau)^{1/2}$) as one moves further away from the wall. Recently, Baars et al. (2015) showed that the zero-crossing of $R$ is not due to the lack of the modulation at that particular wall-normal height. Indeed, it is due to the fact that the small-scale structures are modulated by the high-speed events as much as they are modulated by the low-speed events. Hence, the net value of the modulation coefficient appears to be zero. Because modulation of the small-scale structures by the high- and low-speed events have a positive and negative contribution to the value of $R$, respectively. Baars et al. (2015) concluded this based on observing the modulation coefficient as a function of the time-lag of the correlation throughout the boundary layer.

Iso-contours of the modulation coefficient throughout the boundary layer are plotted as a function of the time-lag of the correlation for both the uncontrolled and the manipulated flows in Figures 4.11(a) and 4.11(b–d), respectively. Details of how the correlation coefficients are calculated can be found in Baars et al. (2015). The fact that the red contours in Figure 4.11(a) mainly occupy the near-wall region implies that within that region, the small-scale structures are modulated by the high-speed events in a canonical TBL. In the outer-region however, the small-scale structures are modulated by the low-speed events, as inferred from the dominant presence of the blue contours within that region.

For clarity, the absolute difference between Figures 4.11(b–d) and 4.11(a) are plotted in Figures 4.11(e–g) in order to investigate the influence of the manipulation on the modulation coefficient. For the desynchronized scheme, the excess of the turbulence activity which is superimposed by the upper-part of the jet airflow into the turbulent boundary layer—shown in Figure 4.9(a)—is manifested in two extrema in Figure 4.11(e):

1. A peak value of $\Delta R_{max} = 0.10$ in the proximity of the lower-bound of the log-region at $z^+ = 593$; and
2. A trough value of $\Delta R_{min} = -0.07$ in the proximity of the upper-bound of the log-region at $z^+ = 2219$. These two extrema have the same orders of magnitude, which can be attributed to the fact that the actuators, on average, introduce approximately the same turbulence activity into the high-speed events as they do into the low-speed events for the desynchronized scheme.
For the opposing scheme, as shown in Figure 4.11(f), the iso-contours of $\Delta R$ show a relatively predominant peak value of $\Delta R_{\text{max}} = 0.21$ at $z^+ = 716$, together with a relatively insignificant trough value of $\Delta R_{\text{min}} = -0.05$ in the proximity of the upper-bound of the log-region at $z^+ = 2678$. The fact that $\Delta R_{\text{max}}$ is one order of magnitude larger than the absolute value of $\Delta R_{\text{min}}$ can be regarded as evidence that the excess of the turbulence activity introduced by the wall-normal jet airflows are embodied within the high-speed events, and this is because the wall-normal jet actuators are introduced into the predicted high-speed events. On the other hand, Figure 4.11(g) reveals that for the reinforcing scheme, the low-speed events accommodate the extra turbulence activity introduced by the wall-normal jet airflows. This is inferred from the relatively predominant trough value of $\Delta R_{\text{min}} = -0.1$ in the proximity of the upper-bound of the log-region at $z^+ = 2219$, compared to a peak value of $\Delta R_{\text{max}} = 0.05$ at $z^+ = 336$. In other words, for the reinforcing scheme contrary to the opposing control scheme, the absolute value of $\Delta R_{\text{min}}$ is one order of magnitude higher than $\Delta R_{\text{max}}$. 
Figure 4.11: Iso-correlation map of the amplitude modulation coefficient, $R(\tau, z^+)$, for the uncontrolled flow, (a); desynchronized-, (b); opposing-, (c); and reinforcing-controlled, (d), flows. The contours correspond to the level range $-0.5$–$0.5$, with the increments of 0.1, excluding the iso-contour of the level 0. (e–g) Absolute difference of the amplitude modulation coefficient in sub-figures (b–d) and (a). Contour levels vary from $-0.2$–$0.2$ with increments of 0.01. The horizontal dashed lines mark the lower- and upper-bounds of the log-region.
4.4 Streamwise and Spanwise Evolution of the Manipulated Flow on the Wall

In order to evaluate the variations in the wall-shear stress fluctuations due to the manipulation schemes, hot-wire measurements with sufficient spatial- and temporal-resolution in the viscous sub-layer of the TBL can be conducted. Due to the relatively high friction Reynolds number in this study (i.e. \( Re_\tau \approx 14400 \)), the physical thickness of the viscous sub-layer of the investigated TBL is in the order of 10 micro meters, which renders the hot-wire measurements in that layer demanding. Conversely, due to the proximity of the hot-wire sensor to the wall, the likelihood of the hot-wire signals becoming contaminated by the heat transfer between the measuring section of the wire and the substrate is quite high. Additionally, the possible error in determining the accurate wall-normal location of the wire within that region can also be added to the above-mentioned factors. Furthermore, the manipulated mean velocity profile cannot be fitted to the composite profile of Chauhan et al. (2009) or any other profile which is based on the premise of the existence of a log-region in the mean velocity profile. This is because the log-region is violated by the manipulation, the evidence of which was shown earlier in Figure 4.8(b). Thus, hot-film sensors were used to evaluate the change in the wall-shear stress instead. The dimensional and dynamical properties of the utilized hot-films (i.e. model 55R47) as well as the implemented calibration process are described in §3.4.2. The manipulation effects on: 1. The mean wall-shear stress; 2. The energy of the friction velocity fluctuations; and 3. The auto-correlation of the friction velocity fluctuations, are examined in §4.4.1, §4.4.2 and §4.4.3, respectively.

4.4.1 Effect on the Mean Wall-shear Stress

Figures 4.12(a–c) show iso-contours of the percentage variation of the mean wall-shear stress for the desynchronized, opposing and reinforcing control schemes, respectively. The black solid squares illustrate the position of the hot-film sensors. A surface with fifth order polynomial in both the streamwise and the spanwise directions was used to
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(Figure 4.12: (a–c) Iso-contours of the percentage variation of the mean wall-shear stress, downstream of the actuators for the desynchronized, opposing and reinforcing control schemes, respectively, relative to the uncontrolled case. The black solid squares illustrate the positions of the hot-film sensors. A fifth order polynomial surface in both the streamwise and the spanwise direction was fitted in a least square sense for each case. $M$ and $U$ in the subscripts stand for the manipulated and the uncontrolled cases, respectively. The black contours correspond to the respective level ranges of (-2.35)–(-1.30), (-3.20)–(-1.80) and (-1.15)–(-0.60), with respective increments of 0.15, 0.20 and 0.07. (d) The percentage variation of the mean wall-shear stress at $d_a = 1.6\delta$, $\Delta y = 0$ demonstrated as vertical bars, together with respective error bars.}

The measurements were conducted successively by placing an array of nine hot-film sensors at each respective streamwise location, one at a time. This was easily accomplished by repositioning the modular insert on which the array of the hot-film sensors were glued. The modularity design of the set-up is fit the data in a least square sense.
shown in Figure 3.2(a).

It becomes clear from Figures 4.12(a–c) that wall-shear stress has been reduced for all three manipulation cases. The amount of wall-shear stress reduction for the *opposing* control scheme is the highest among all the control cases, and this is followed by that for the *desynchronized* and the *reinforcing* control schemes, respectively. For all the control schemes the maximum reduction can be observed at the second streamwise location of the measurement (i.e. \( d_a = 1.6\delta \)). This implied that at the current flow conditions and with the chosen control parameters, the wall-normal jet airflows introduce their maximum influence—in terms of the mean wall-shear stress variation—at \( 1.6\delta \) downstream of the actuators. With the current measurement-grid resolution the percentage variation of \( \tau_w \) measured at \( d_a = 1.6\delta \) & \( y = 0 \) demonstrates the maximum relative reduction at each respective contour plot in Figures 4.12(a–c). The reduction in \( \tau_w \) fades away as one moves from the centerline of the control zone towards the spanwise lateral sides.

For the purpose of comparison, the amounts of the maximum wall-shear stress reduction, occurring at \( d_a = 1.6\delta \) & \( y = 0 \), are illustrated via vertical bars in Figure 4.12(d), together with their respective error bars. Each error bar corresponds to the 95% confidence interval of twenty realizations. Each realization is derived from the difference of the mean value of a 180s-long hot-film signal—calibrated according to the procedure described in §3.4.2—for each respective control case with respect to that of the uncontrolled one. As shown earlier in §4.3, except for the reduction in the mean streamwise velocity, the desynchronized control scheme does not alter the energy of the large-scale structures in a mean sense. Therefore, the entire amount of the drag reduction obtained from the desynchronized control scheme—the maximum of which illustrated by the gray bar in Figure 4.12(d)—can be attributed to the low momentum region generated at the control zone via the wall-normal jet airflow. As such, it might be induced that as long as the effective wall position is not changed, any type of the manipulation that results in reduction of the mean streamwise velocity would also yield reduction in the mean wall-shear stress.
A maximum skin-friction reduction of 3.2% is observed for the \textit{opposing} control scheme at $d_a = 1.6\delta \& y = 0$, which is greater than that for the \textit{desynchronized} control scheme. Due to the top-down influence of the energetic large-scale structures in a high-Reynolds-number TBL (e.g. Abe et al., 2004; Marusic et al., 2010b), it is believed that this excess amount of drag reduction is contributed by less energetic large-scale structures in the regions above the near-wall region. The evidence of the energy reduction of the large-scale structures in the outer region was shown earlier in §4.3.2 and §4.3.3.

Accordingly, the mechanism of the drag reduction of the control scheme can be explained as a combination of the beneficial effects of both: 1. The streamwise momentum deficit generated via the wall-normal jet actuation, and 2. The energy decrease of the large-scale structures which results in a weaker influence on the wall. This is one of the most important and positive outcomes arising from the results. It ensures that the hypothesis set in the introduction (§1) is, indeed, valid. That is, less energetic large-scale structures in the log-region of a high-Reynolds-number TBL result in less top-down influence, and this is shown to be beneficial for skin-friction drag reduction. The dual mechanism responsible for the observed mean wall-shear stress reduction was also reported by Rebbeck and Choi (2006), in which they conducted a real-time opposition control of the near-wall high-speed streaks with a single synthetic wall-normal jet. They defined the former and latter drag reduction mechanisms as ‘in-phase u-velocity’ and ‘out-of-phase v-velocity’ control, respectively.

For the \textit{reinforcing} control case, although the large-scale fluctuations in the outer region were intensified (shown in §4.3.2 and §4.3.3), a maximum skin-friction reduction of 1.2% is still obtained at $d_a = 1.6\delta \& y = 0$. This amount is half of that observed for the desynchronized case at the same location. It can be concluded that half of the skin-friction reduction that could have been obtained via the \textit{desynchronized} control scheme is nullified here by the detrimental contribution of the energy increase of the large-scale structures in the log-region. The observation that even though the large-scale structures in regions above the near-wall region became more energetic for the reinforcing scheme, and yet this scheme results in drag reduction is interesting. This might indicate that the detrimental influence of the energy increase of the large-scale structures on the wall was not only succumbed but also overpowered by the beneficial
Influence of the low momentum region generated downstream of the wall-normal jet actuators and the net effect leads to drag reduction.

In summary, the drag reduction mechanisms for the investigated control schemes are two-fold: 1. The reduction of the streamwise momentum of the TBL due to injection of the wall-normal jet. This is manifested in the mean streamwise velocity profiles shown in Figure 4.8 for all the three control schemes, and 2. The alteration of the fluctuating energy of the large-scale structures residing in the log-region of the TBL, which has a top-down influence and hence direct effect on the wall-shear stress value. Since both of the above-mentioned mechanisms are caused by the wall-normal jet injection into the TBL, there is an associated time response for the flow from the jet to reach the outer-layer of the manipulated TBL. Furthermore, there is an additional time delay for the modified large-scale influence to affect the wall-shear stress. Therefore, the maximum effect of the jet injection on the TBL, and hence on the wall, is at a location further downstream of the actuators (i.e. $d_a = 1.6\delta$, shown in Figures 4.12a–c).

For all three types of manipulations, the amount of the introduced wall-normal jet airflow into the turbulent boundary layer was the same. The only difference in terms of the manipulation algorithm was the timing between the actuation and the predicted incoming large-scale structures. To the knowledge of the author, this is the first time that a large-scale control scheme has been implemented experimentally and its success has been shown evidently. Among the three control schemes, the *opposing* case is worthy of further scrutiny as it holds the potential to produce even greater drag reduction. Additionally, one may combine the large-scale *opposing* control mechanism (for regions above the near-wall region) with those mechanisms that attempt to manipulate the near-wall region directly. In other words, combining micro sensors and actuators with the macro sensors and actuators.

Comparing the 3.2% skin-friction reduction of the *opposing* control scheme with the 6% drag reduction that Yoshino et al. (2008) measured experimentally in a channel flow with a friction Reynolds number of 300, in which they used micro sensors and actuators, it can be concluded that, in terms of its efficacy, the implemented control scheme in this investigation is relatively successful. The reason for this is because the
friction Reynolds number in the current study (i.e. 14400) is approximately fifty times larger than that of Yoshino et al. (2008), while the drag reduction that Yoshino et al. (2008) reported is only two times larger than that measured in this study (i.e. 3.2%). The amount of the drag reduction obtained here can be further appreciated by considering the fact that the larger the Reynolds number becomes, the more intricate the boundary layer control gets. This is due to the growth of the range of the energetic turbulent scales and the decrease of the physical distance of the near-wall region. Furthermore, the dimensions of the sensors and actuators used here are not in the order of micrometers, as opposed to those used by Yoshino et al. (2008), which adds another merit to the control scheme implemented here.

### 4.4.2 Effect on the Energy of the Friction Velocity Fluctuations

Figures 4.13(a–c) show the iso-contours of the percentage changes in the variance of the friction velocity fluctuations for the desynchronized, opposing and reinforcing control schemes, respectively. The black solid squares in Figures 4.13(a–c) illustrate the position of the hot-film sensors that were used for conducting the measurements. Similar to Figures 4.12(a–c), here a surface with fifth order polynomial in both the streamwise and the spanwise directions has also been used to fit the data in a least square sense. According to Figure 4.13(a), it is revealed that except for a minor reduction, the desynchronized scheme does not modify the variance of $u_\tau$ fluctuations significantly. However, according to Figures 4.13(b–c), the opposing and the reinforcing control schemes decreases and increases the variance of the $u_\tau$ fluctuations, respectively. Akin to Figures 4.12(b–c), the maximum influence has also been observed at $d_a = 1.6\delta$ & $y = 0$ for both the opposing and the reinforcing control schemes in Figures 4.13(b–c).
Experimental Results and Discussion

Figure 4.13: (a–c) Iso-contours of the percentage variation of the variance of the friction velocity fluctuations, downstream of the actuators for the desynchronized, opposing and reinforcing control schemes, respectively, relative to the uncontrolled case. The black solid squares illustrate the positions of the hot-film sensors. A fifth order polynomial surface in both the streamwise and the spanwise direction was fitted in a least square sense for each case. $M$ and $U$ in the subscripts stand for the manipulated and the uncontrolled cases, respectively. The black contours correspond to the respective level ranges of $(−1.35)−(−0.30)$, $(−4.60)−(−0.80)$ and $(−0.30)−(3.45)$, with respective increments of 0.13, 0.47 and 0.47. (d) Pre-multiplied energy spectra of the manipulated $u_τ$ fluctuations at $d_a = 1.6δ$ & $Δy = 0$, together with that of the uncontrolled $u_τ$ fluctuations. The vertical dashed line marks the cut-off wavelength corresponding to the large-scale filter.
The pre-multiplied energy spectra of the respective friction velocity fluctuations at $d_a = 1.6\delta$ & $y = 0$ for the three control schemes are plotted in Figure 4.13(d), together with that of the uncontrolled case. Due to the temporal resolution of the hot-film sensors—a sampling frequency of 4000Hz was chosen to sample the hot-film signals, as described in §3.4.2—the hot-film signals do not possess any spectral information below a wavelength of approximately $300\nu/U_{\tau}$. Since solely the relative change of the large-scale fluctuations are sought after here, the lack of high temporal-resolution of the hot-film sensors does not influence the interpretation of the results. It becomes clear from Figure 4.13(d) that the control schemes only modify the large-scale energy of the friction velocity fluctuations; those beyond the cut-off wavelength of the implemented large-scale filter—illustrated by the vertical dashed line in Figure 4.13. The opposing control scheme diminishes the large-scale energy, whereas the reinforcing control scheme enhances that. For the desynchronized scheme the large-scale energy distribution almost preserves its original shape. Such observations are in line with the observed energy change of the large-scale structures at the log-region—shown in Figure 4.6—of the manipulated TBL. That is, as the energy of the large-scale structures diminish and enhance for the respective opposing and the reinforcing control schemes, their associated footprints—large-scale $u_+$ fluctuations—also experience respective reduction and enhancement of the energy. This observation might support the “top-down influence” phenomenon of large-scale structures in a high-Reynolds-number TBL.
4.4.3 Effect on the Auto-correlation of the Friction Velocity Fluctuations

The percentage variation of the integral time-scale of the friction velocity fluctuations downstream of the actuators along the centerline of the actuator array are plotted in Figure 4.14(a), for the three types of manipulation. The details of how the integral time-scale is calculated was earlier described in §4.2.3. The upper limit of the integration is chosen to be $2000 \frac{\nu}{U_f^2 \tau U}$, beyond which the auto-correlation coefficient of the uncontrolled case drops below 0.003 which is believed to be a reasonable approximation of the asymptotic limit. According to Figure 4.14(a), the integral time-scale of the desynchronized, opposing and the reinforcing control schemes are unaltered, decreased and increased, respectively. At the first measurement location, the respective percentage reduction and increase of $R_{u,\tau}$ measure approximately $-14\%$ and $17\%$, for the opposing and the reinforcing control schemes. Akin to both Figures 4.6(a) and 4.7(a), these significant variations abate by moving further downstream from the actuators (i.e. as $d_a/\delta$ increases).

The respective auto-correlation coefficients as a function of the time-lag of the correlation are plotted in Figures 4.14(b–d), together with that of the uncontrolled TBL at $d_a = 0.8\delta$. For clarity, the magnified views of each $R_{uu}$ curve are also shown in the insets of Figures 4.14(b–d). According to Figure 4.14(b), the auto-correlation coefficient of the large-scale friction velocity fluctuations are altered negligibly for the desynchronized control scheme. For the opposing and reinforcing control schemes, the corresponding auto-correlation coefficient of the large-scale friction velocity fluctuations decreases and increases, respectively, with a converging behavior towards the uncontrolled scheme by moving further downstream from the actuators. These behavioral changes of the auto-correlation of the $u,\tau$ fluctuations for different control schemes resemble those of the $u$ fluctuations at the log-region (discussed earlier in §4.2.3). In other words, the relative changes of the former reflect those of the latter.
Figure 4.14: (a) Percentage variation of the integral time-scale of the manipulated friction velocity fluctuations, relative to that of the uncontrolled case as a function of the streamwise distance from the actuators. (b–d) Auto-correlation coefficient of the manipulated friction velocity fluctuations corresponding to the integral time-scale in (a) as a function of the time-lag, $\tau_L$, together with that of the uncontrolled streamwise velocity fluctuations at $d_a = 0.8\delta$. The uncontrolled inner-scales have been used for normalization.
4.5 Sub-optimization of the Opposing Control Scheme in terms of the Large-scale Filter Size

As described in §3.3.1, in order to single out the large-scale-structure footprints a Gaussian filter of the size of $1.5\delta$ in the streamwise direction is implemented on the hot-film signals. On the other hand, in §3.3.3 the minimum streamwise spacing between the sensors and the actuators, in order to be able to implement the control logic—described in §3.3—to the TBL at the current flow conditions (i.e. $Re_\tau \approx 14400$), is determined to be $1.6\delta$. In this section, the effect of reducing the size of the large-scale filter on the efficacy of the opposing scheme are investigated, while the streamwise spacing between the sensors and the actuators as well as the jet exit velocity were kept the same as those for the previous measurements—a streamwise spacing of $0.6\text{m}$ and a jet exit velocity of $12.95\text{m/s}$. The timing of the actuation was tailored such that the actuations were synchronized with the respective detected large-scale structure.

![Figure 4.15: Three pre-multiplied energy spectra of the opposing-manipulated streamwise velocity fluctuations, $k_x\phi_{uu}/U_{\tau_0}^2$, at $d_0 = 1.6\delta$, $y = 0$, $z_L^+ \approx 477$, with a respective large-scale filter size of $\Delta x_{GF} = 1.5\delta$, $0.75\delta$ and $0.25\delta$, together with that of the uncontrolled streamwise velocity fluctuations, $k_x\phi_{uu}/U_{\tau_0}^2$.](image-url)
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The energy reduction of the large-scale structures at $d_a = 1.6\delta$ and $y = 0$ and $z_L^+ \approx 477$ was chosen to be the criterion by which the efficacy of the implemented opposing control scheme is evaluated. The justifications of the choice of the above-mentioned wall-normal height and downstream streamwise distance to the actuators is due to the following: the former corresponds to the wall-normal height of the outer-peak in the spectrogram of the canonical TBL for current flow conditions, and the latter corresponds to the downstream streamwise distance to the actuators at which the maximum reduction of the variance of the streamwise velocity fluctuations occurs. Two sets of hot-wire measurements were conducted in which the large-scale filter was reduced to half (i.e. $0.75\delta$) and one-sixth (i.e. $0.25\delta$) of the initial size, respectively. The pre-multiplied energy spectrogram of these $u$ fluctuations were compared in Figure 4.15 with their counterparts for the uncontrolled and the opposing control scheme in which a $1.5\delta$-long large-scale filter was used. According to Figure 4.15, all the control schemes showed a slight increase of moderate-scale energy. This can be attributed to the lift of the structures from the lower wall-normal heights, which appears to be a common feature, regardless of the size of the Gaussian filter, for all three of the control schemes and with almost the same influence. Beyond that, the energy of the large-scale structures decreased for all three control schemes. However, it is apparent that for the opposing control scheme in which a $1.5\delta$-long large-scale filter was used, a maximum reduction of the large-scale energy was yielded among others.

Such results could be predicted, since as it was concluded in §4.1, the bandpass of the impulse response of a $1.5\delta$-long Gaussian filter would cover the entire non-zero coherence region of the $u_\tau$ fluctuations at the position of the sensors and the $u$ fluctuations throughout the boundary layer at the position of the actuators—shown in Figure 4.4. This implied that, the structures which are larger than $1.5\delta$ in the streamwise direction retain their coherence within the streamwise distance between the sensors and the actuators, which is nominally the same value as the length of the large-scale filter, namely $1.6\delta$. Therefore, with the current configuration of the experimental set-up, if the large-scale Gaussian filter is chosen to be $1.5\delta$ long, the likelihood that the predicted structures which reach at the streamwise coordinate of the actuators would represent the actual structures is larger than that if the large-scale Gaussian filter was chosen to be
a value smaller than $1.5\delta$. In other words, by choosing a Gaussian filter of $1.5\delta$ long, it can be ensured that the majority of the manipulation by the wall-normal actuators would target the desired actual structures. However, as the large-scale Gaussian filter is chosen to be smaller than $1.5\delta$ long, the corresponding bandpass of its impulse response would also cover those regions in the coherence magnitude (i.e. $\gamma_{uu}^2$) domain which correspond to the structures that are not coherent within the $1.6\delta$ streamwise spacing between the sensors and the actuators. Consequently, the actuators would also manipulate those structures which have lost their coherence as they reach at the position of the actuators. As such, the beneficial results of the opposing control scheme, in which a $1.5\delta$-long Gaussian filter has been chosen, would be compromised, as evidenced in Figure 4.15. Therefore, it can be concluded that the current length of the streamwise Gaussian filter (i.e. $1.5\delta$) is, indeed, a sub-optimal filter length for the implemented opposing control scheme, given the current experimental set-up and the flow conditions under investigation in this study.

### 4.6 An Estimation of the Efficiency of the Opposing Control Scheme

One decisive factor that determines whether an active control scheme is worth employing in practical applications is the efficiency of that scheme. The absolute energy saving must exceed the energy expenditure (i.e. penalty) that is required for the operation of the control components, and this is only possible under the condition that the control scheme generates a net drag reduction. In order to be able to argue conclusively about the quantity of net drag variation for the implemented control schemes, it is proposed to conduct measurements with a floating element sensor while the entire control set-up together with all the associated accessories are embedded within and driven by the floating element during the course of the measurement. As such, the net drag variation, which is a synergistic outcome of the changes of both the pressure and skin-friction drags, could be determined. Furthermore, the energy expenditure needs
to be quantified, which comprises the following: 1. The kinetic energy of the introduced jet airflow from the jet exit planes; 2. The electrical energy required to operate the sensors and drive the actuators; and 3. The electrical energy required for real-time signal processing.

The author acknowledges that at this stage there is not yet enough data to evaluate the accurate efficiency value of the implemented control schemes. However, to obtain a crude estimation of the efficiency of the opposing control scheme—provides the maximum wall-shear stress reduction among the other control schemes (§4.4.1)—from the available data, the following relationship for efficiency ($\eta$) presented in terms of its percentage variation was devised and evaluated:

$$
\eta(\%) = \frac{U_\infty \Delta F_{Drag}}{P_{KE}/2} \times 100
$$

(4.7)

where, $U_\infty$ is the free-stream velocity, $\Delta F_{Drag}$ is the difference in the skin-friction drag over a $\Delta d_a \times \Delta y = 4.1\delta \times 0.56\delta$ surface downstream of the actuators due to the opposing control scheme—Figure 4.12(b)—defined as $\Delta F_{Drag} = \int_{-0.285}^{+0.285} \int_{0.88}^{4.95} \tau_{wO} - \tau_{wU} \, dd_a \, dy$. These limits of integration were determined from the range of the contour plot in Figure 4.12(b). Yet, according to Figure 4.12(b), it can be inferred that neither the streamwise nor the spanwise ranges of influence of the opposing control scheme are restricted to these limits. Consequently, the to-be-calculated value of $\Delta F_{Drag}$ is underestimated, which results in an underestimated value of $\eta$. However, this compromise does not diminish the value and importance of the following analysis. As mentioned above, such analysis only provides us an approximation of the order of magnitude of the efficiency and the exact value. $P_{KE}$ is the rate of the introduced kinetic energy into the TBL via wall-normal jet airflow when all the actuators are in constant blowing mode, and defined as $P_{KE} = 1/2(\rho_j W_j A_j) W_j^2$. The division by 2 in the denominator of Equation 4.7 counts for the fact that on average half of the actuators are on-duty at each instant in time. $\rho_j$ is the density of the compressed air at the jet exit planes. This is calculated using ideal gas law for air, where the total pressure was measured via a Pitot tube with an inner diameter of 0.5mm placed vertically at the centroid of the jet exit planes. $W_j$ is the wall-normal jet airflow velocity in still air at the jet exit planes—Figure 3.4. $A_j$
is the summation of the areas of nine jet exit planes, that is \( 9(w_j \times l_j) \). Substituting all the above-mentioned parameters into Equation 4.7 results in an efficiency of approximately 16\% for the opposing control scheme. To ensure the industrial application of any active flow control scheme, the required condition is that its associated efficiency exceeds beyond 100\%. Since the estimated efficiency of the opposing control scheme is two orders of magnitude smaller than 100\%, its full-scale implementation, with the implemented control components and parameters in this dissertation, is not yet warranted. Thus, further research towards optimizing the large-scale opposing control scheme in order to enhance its efficiency and ensure its application in the future is required.
Chapter 5

Conclusions and Future Work

5.1 Conclusion

The energetic large-scale coherent structures in the log-region of a high-Reynolds-number turbulent boundary layer (TBL) at $Re_{\tau} \approx 14400$ were selectively manipulated via a large-scale active flow control scheme. It was shown that diminishing the energy associated with these structures can lead to skin-friction drag reduction. The actuation was provided by a spanwise array of nine wall-normal jets—with a spanwise spacing of 26mm ($\approx 1060\nu/U_{\tau}u \approx 0.08\delta$)—which operated in an intermittent on/off fashion. In order to directly interact with the coherent large-scale structures throughout the entire log-region and preferably not beyond that (since they are only energetic within the log-region), the exit velocity of the jet airflow was adjusted so that the penetration height of the jet in cross-flow exceeds just slightly beyond the upper-bound of the log-region. The penetration height was determined to be the wall-normal height at which the controlled mean streamwise velocity reaches 99.9% of the uncontrolled case.

The spanwise actuator-array was triggered with a real-time controller, which was programmed to detect the wall-signature of the energetic large-scale motions in the log-region. The wall-shear stress fluctuations were measured via a spanwise hot-film sensor-array at $1.6\delta$ upstream of the actuation location. The hot-film voltage signals are composed of both the small- and the large-scale components. In order to single
out only the large-scale components from these signals, a 1.5δ-long Gaussian filter (six standard deviations of the Gaussian distribution equates to 1.5δ) was implemented on each hot-film signal in real-time. The resultant filtered signals were converted into transistor transistor logical (TTL) control signals, whose high- and low-values represent the footprint of the large-scale high- and low-speed events, respectively. Depending on whether the high-speed events or the low-speed events are the target of the manipulation, the actuators can be programmed to be active for the duration of which the TTL control signals possess either high- or low-values.

The total latency of the actuation is determined to be 35.35ms. This is defined to be the interval between the instant that the wall-shear stress signals were sampled by the hot-film sensors and the instant that the wall-normal jet airflow reached the jet exit planes of the actuators. This total latency was an aggregate of the following individual latencies: 1. A time delay which accumulated from the mechanical activation of the jet flow (τm ≈ 14.3ms, §3.2.2); 2. Real-time large-scale filtering (τf ≈ 20.8ms, §3.3.1); and 3. A single controller time step (τc = 0.25ms). The aggregate latency is 8ms less than the convection time (i.e. (1.6δ)/Uc = 43.35ms) for the large-scale component of the wall-shear stress fluctuations from the sensing to the actuation point, and this is both desired and required because the high- and low-speed events are forward-inclined.

As the on-periods of the actuators were synchronized with the predicted high-speed events in the TBL flow (i.e. the opposing control scheme), the relative amplitudes of the high-speed events were attenuated, as well as those of the low-speed events. The underlying mechanism of the manipulation is believed to follow an opposition framework. That is, the down-wash sections of the existing counter-rotating roll modes which accompany the high-speed events were counteracted. This resulted in diminishing the intensity of the acted-upon roll modes, which in turn, resulted in diminishing the variance of the streamwise velocity fluctuations in the log-region. At 1.6δ downstream of the actuation point, the highest reduction in spectral energy—as much as 30%—was found for wavelengths larger than 5δ in the log-region. This energy reduction is anticipated to have the potential to increase even further by implementing a more effective opposing control scheme. Furthermore, a maximum skin-friction reduction of 3.2% was measured at 1.6δ downstream of the actuators. Comparing this
amount of reduction with the 6% drag reduction that Yoshino et al. (2008) measured experimentally in a channel flow with a friction Reynolds number of 300, suggests that the implemented opposing control scheme is, indeed, a successful control algorithm in regards to skin-friction drag reduction. The reason is that the friction Reynolds number in the current study (i.e. \( Re_\tau \approx 14400 \)) is approximately 50 times higher than that of Yoshino et al. (2008), yet only one seventh of that of a Boeing 747 under typical cruising conditions (i.e. \( Re_\tau \approx 10^5 \), Kasagi et al., 2009b). Furthermore, Yoshino et al. (2008) used sensors and actuators with inner-scale dimensions, whereas both the dimensional and the dynamical properties of the control components used here were in the order of outer-scales. As a result, the intricacies associated with the production and operation of the micro control elements were disposed of. To the author’s knowledge, this is the first time that a large-scale active control scheme in a high-Reynolds-number TBL has been implemented experimentally, and has been shown to be beneficial for skin-friction drag reduction.

To investigate whether the above-mentioned results are, indeed, due to the synchronization of the wall-normal actuation with the high-speed events or it is solely due to the injection of the wall-normal jet airflow into the TBL, another control scheme was examined, where no synchronization between the actuation and any of the incoming large-scale structures were prescribed. Hence, this control scheme was identified as desynchronized control scheme. In order for the results of the desynchronized control scheme to be considered as the base-line case for those of the synchronized control schemes—including the opposing control scheme—it is essential for the associated TTL control signals to possess the same PDF distribution of the duty cycles of the on-periods (Figure 3.8) as those for the synchronized ones. Therefore, they were restored on a random basis from the TTL control signals which were generated during the operation of the opposing control scheme.

As such, approximately half of the actuation would be upon the high-speed events and the other half would be upon the low-speed events. The beneficial influence of one type of manipulation was canceled out by the detrimental influence of the other type and no behavioral change in the energy of the large-scale structures was observed for the desynchronized control scheme. However, a 2.4% skin-friction reduction was obtained
for such a scheme. Comparing this amount of drag reduction with that obtained via the *opposing* control scheme, it can be rationalized that approximately three-quarters of the 3.2% skin-friction reduction via the *opposing* control scheme was attributed to the streamwise momentum deficit that was generated downstream of the injected jet fluid by the actuators. In other words, only a quarter of the entire amount of drag reduction via the *opposing* control scheme is contributed by the beneficial top-down influence of the diminished large-scale structures in the outer region. This supports the hypothesis which was premised in the introduction. That is, it is feasible to tame the large scale structures in a high-Reynolds-number turbulent boundary in a selective manner so that the outcome would result in skin-friction drag reduction.

In an effort to study the response of the TBL to external large-scale perturbations even further, a *reinforcing* control scheme was implemented, in which the wall-normal jet actuation was synchronized with the low-speed events. The manipulated large-scale structures for such a scheme (i.e. low-speed events) are the inverse of those for the *opposing* control scheme (i.e. high-speed events). Therefore, the results of the *reinforcing* control scheme can be considered as counter-evidence for the results of the *opposing* control scheme. An increase in the energy of the large-scale structures in regions above the near-wall region was observed for the *reinforcing* control scheme. Despite that, a maximum skin-friction drag reduction of 1.2% was measured at 1.6δ downstream of the actuators. It can, therefore, be speculated that the measured drag reduction for such a scheme is an amalgamate influence of the following two mechanisms: 1. The beneficial influence on the wall due to the low-momentum region generated downstream of the actuators induced by the wall-normal jet airflow; and 2. The detrimental top-down influence of the more energetic large-scale structures in the regions above the near-wall region; where the former factor overpowers the latter one. Hence, the net influence in terms of the skin-friction drag results in reduction.

From the measurements of the streamwise velocity fluctuations downstream of the actuators at a single wall-normal height which corresponds to the that of the geometric center of the log-region a common feature was observed for all the above-mentioned control schemes: At 0.8δ downstream of the actuators, the evidence of the presence of additional small-scale energy was observed in the log-region, which is most likely
attributed to: 1. The lift of the small-scale coherent structures from the near-wall region to the outer-region; and 2. The presence of a shear layer between the top part of the jet airflow and the TBL. Since the evidence of the excess of the small-scale energy in the log-region is completely abated at $1.6\delta$ downstream of the actuators, it can be concluded that the lifted small-scale coherent structures descended quickly to the near-wall region, rendering them to have a relatively high recovery rate. Furthermore, the above-mentioned shear layer was, perhaps, raised further away from the wall and entered to higher wall-normal height. It is interesting to note that the evidence for the energy alteration of the large-scale structures were observed even at $5\delta$ downstream of the actuators. It can be deduced that, compared to the small-scale structures, the large-scale structures are quite resilient to the streamwise recovery effect of the canonical TBL. This might be attributed to the size of the large-scale structures in the streamwise direction with respect to that of the small-scale structures—the large-scale structures scale with boundary layer thickness (i.e. $\delta$) whereas the small-scale structures scale with viscous length-scale (i.e. $\nu/\bar{U}_c$).
5.2 Future Work

The beneficial results of the *opposing* control scheme in terms of both the large-scale energy reduction—discussed in §4.2 and §4.3—and the wall-shear stress reduction—discussed in §4.4—in a high-Reynolds-number TBL entices us to scrutinize such a scheme. Particle image velocimetry (PIV) measurements at the spanwise–wall-normal plane of the *opposing*-controlled TBL can be conducted to quantify the diminish of the counter-rotating roll modes due to the control scheme. Moreover, both the streamwise and spanwise evolutions of the manipulated large-scale structures can be investigated in more of a detail via PIV measurements in the streamwise–wall-normal and streamwise–spanwise planes, respectively.

To obtain a more effective and/or efficient *opposing* control scheme, an optimization approach needs to be undertaken. To that end, an appropriate cost function needs to be defined and a systematic investigation upon the software and/or hardware components is required in order to minimize the defined cost function. However, because of the multi-functionality of the subject matter it is a significantly intricate task to optimize all parameters at the same time. Nevertheless, towards sub-optimizing the *opposing* control scheme, the *efficacy* of the control scheme can be initially tackled. To that attempt, some modifications are proposed in the current section, which are categorized with regard to altering either the *software* (i.e. keeping the control set-up as it is) or the *hardware* components of the control scheme. At the end of this section, both the need and importance of investigating the *Reynolds-number dependency* of the results are highlighted.

Modification of the Software Component

**Intermittent Injection during the Predicted High-speed Events** For the *opposing* control scheme, as mentioned in §4.3.3, the leading and trailing edges (downstream and upstream interfaces) of the large-scale high-speed events are decelerated more than the middle sections; as inferred from the contour plot of the difference between
the respective conditional large-scale structures for the uncontrolled and opposing-controlled cases—Figure 4.10(f). One might argue that the upstream interfaces of these conditional large-scale structures are masked due to the ensemble-averaging with a constant window size, hence any observation regarding the behavioral change of that region might not be realistic. Recently, Baars et al. (2017a) constructed the conditional large-scale structures using variable window sizes, each of which represents the actual size of each of the predicted individual structures in the streamwise direction.

Despite the differences in the overall shape of the large-scale structures, by using the former and latter methods, the leading edge of the conditional structures exhibit a strong resemblance to each other, which warrants the validity of any conclusion driven by observing the behavioral change of the leading edge of these structures via the conventional conditional average method. That is, the larger deceleration of the high-speed events at the leading edge of these structures relative to the middle section is indeed realistic. This can be attributed to the stronger mitigation of the associated counter-rotating roll modes at the beginning of the actuation, which, in turn, is attributed to the speculation that the transient nature of the jet into the cross flow at the beginning of the actuation generates vortical structures with an up-wash section in the middle. The interaction of these generated vortical structures with the existing counter-rotating roll modes accompanying the high-speed events renders the opposition mechanism of the manipulation a predominant mechanism for the beginning of the actuation. Therefore, it is proposed to implement intermittent blowing instead of continuous blowing for the whole duration of the large-structure detection in order for the opposition mechanism of the control to have a long lasting effect. Di Cicca (2010) showed that the opposition control of sweep events by using multiple injections whose number is proportional to the duration of the event allows the sweep event to be controlled over its whole duration.

Two-dimensional Filtering The success of any selective control scheme in a TBL is highly dependent on, among others, the ability of that scheme to predict the coherent structures that are the target of manipulation as accurately as possible in real-time. In
the implemented control scheme in this study, the footprints of the large-scale structures were predicted by filtering the signal of each individual skin-friction sensor with a low-pass $1.5\delta$-long Gaussian filter in the streamwise direction. However, since the characteristic widths of the coherent structures in TBLs are finite in the spanwise direction, the realistic realization of the footprints of the large-scale structures might not have been resembled via the above-mentioned filtering procedure—a one-dimensional filtering in the streamwise direction. This could have been exacerbated by low levels of signal-to-noise ratios of the hot-film signals, which could have led to inaccurate and/or spurious detection of the large-scale structures. Therefore, it is proposed to implement a two-dimensional Gaussian filter (illustrated in Figure 5.1) on the signals of the upstream hot-film sensor-array, instead of filtering the signal of each individual hot-film sensor with a single streamwise Gaussian filter.

In the spectral domain, a two-dimensional Gaussian filter translates to low-pass filters in both the streamwise and spanwise directions. As the width of the two-dimensional Gaussian filter in the spanwise direction ($\Delta y_{GF}$) gets wider, the spanwise cut-off wavelength of the filter becomes larger. Depending on the width of the Gaussian filter in the spanwise direction, the number of the contributing hot-film sensors to the generation of each row of the TTL control field can be adjusted—more hot-film sensors

![Figure 5.1: Illustration of a two-dimensional Gaussian filter with a streamwise filter length ($\Delta x_{GF}$) of 1.5$\delta$ and a spanwise filter length ($\Delta y_{GF}$) of 0.6$\delta$.](image-url)
are accommodated by wider Gaussian filter in the spanwise direction. In other words, apart from the upstream hot-film sensor that is aligned with the downstream actuator, the neighboring hot-film sensors would also contribute to the development of the TTL control signal that would be sent to each downstream actuator. Consequently, the associated TTL control signals accommodate less footprints of the small- to moderate-scale structures compared to when a streamwise Gaussian filter was implemented on the hot-film sensors, and this can be beneficial from a large-scale control point of view.

Variable Interval Time Averaging Technique for Structure Detection Turbulence in a turbulent boundary layer is sustainable due to the instability of low-speed streaks (Kline et al., 1967). That is, as the low-speed streaks are lifted away from the wall by the head of the hairpin vortices, they demonstrate a sinusoidal oscillation in the streamwise direction, which eventually leads to the “break-up” of these events. As the low-speed streaks are broken-up, they are replaced by the down-rush of the high-momentum fluid. This generates a local increase in the fluctuation level (variance) of the wall-shear stress. Due to self-similarity of the structures evolving from the wall, a similar phenomenon also occurs in a large-scale sense, in which the large-scale high- and low-speed events now play the role of the high- and low-speed streaks. As the low-speed events are replaced by the high-speed events, the fluctuation level of the local wall-shear stress increases. As such, the variable interval time averaging (VITA) technique proposed by Blackwelder and Kaplan (1976) can be implemented on the variance of the wall-shear stress fluctuations in real-time in order to detect those high-speed events, whose leading-edge-associated local variance either exceeds beyond a certain threshold or falls within a certain range.

To be able to implement VITA analysis on the hot-film signals used in this research, these sensors must have the capability of resolving the high-frequency components of the wall-shear stress fluctuations, because VITA analysis relies on the comparison of the local variance of a section of the signal with the global variance of that signal. Since these hot-film sensors are not good for resolving the high-frequency fluctuations accurately, the VITA analysis is not feasible with the current data set. Therefore, in order to realize such an analysis, wall-shear stress sensors with high enough spatial
Conclusions and Future Work

and temporal resolutions need to be utilized. However, the lack of resolution of the small-scale fluctuations by these sensors does not affect or devalue the results and observations, because the main objective and focus of this research is to interact with the large-scale structures in the log-region of a high-Reynolds-number TBL. The footprints of these structures were resolved using the hot-film sensors—as noted earlier in Section 3.2.1.

Prediction Transfer Function for Structure Detection  As mentioned earlier in 3.3.3, real-time hot-film signals were utilized together with the Taylor’s hypothesis to predict the passage of the LMSs over the actuators. This implies that the footprints of the large-scale structures were assumed to be frozen for a streamwise distance of $1.6\delta$—the streamwise separation between the hot-film sensors and the actuators. Such assumption would result in uncertainty in the accuracy of the structure manipulation, since they may experience formation, evolution and decay processes as they convect downstream. Furthermore, they interact with each other from the sensing location to the actuation location. Qiao et al. (2017) showed that the linear behavior of the large-scale structures in the near-wall region of a TBL can be well-predicted downstream of the detection position. This forward prediction was accomplished via modeling a transfer function which related two streamwise-separated velocity signals in the near-wall region of a canonical TBL. The algorithm described in Qiao et al. (2017) for the generation of the predictive model can be implemented between a hot-film signal at the sensing location and a streamwise velocity signal at the streamwise location of the actuators and at a wall-normal height corresponding to the geometric center of the log-region—where the large-scale structures are most energetic. Based on the generated single-input-single-output (SISO) transfer function model derived using the above-mentioned two signals a more predictable opposing control scheme may be developed and the efficacy of the control scheme may be improved.
Modification of the Hardware Component

**Suction beneath the Low-speed Events**  Suction beneath the low-speed events can be incorporated in the *opposing* control scheme in order to augment the opposition drag-reduction mechanism of the control scheme. As suction is employed upon the low-speed events, the up-wash sections of the counter-rotating roll modes are hindered. As a result, it is speculated that the instability level of the low-speed events are abated. The beneficial influence of that, in terms of drag reduction, is speculated to be added to the already-existing drag-reduction mechanism of the *opposing* control scheme; namely the opposition mechanism upon the down-wash section of the counter-rotating roll modes and the reduction of the streamwise momentum of the boundary layer purely due to wall-normal jet injection. Furthermore, suction also annihilates the small-scale turbulent activity in the near-wall region. A control scheme in which both blowing and suction are employed as the control mechanisms holds the potential of being developed into a sustainable control scheme. Since the population of the low-speed events is approximately the same as that of the high-speed events in a TBL, the same amount of the air that was sucked from the surface during the suction periods can be stored and utilized for the blowing periods. This bestows the proposed control scheme an applicational significance.

![Figure 5.2: Schematic representation of the *opposing* control scheme, implemented upon: (a) only the up-wash sections of the counter-rotating roll modes; (b) both the up- and down-wash sections of the counter-rotating roll modes.](image-url)
Combination of Passive and Active Control  The emergence of coherent structures in turbulent boundary layers takes place in a random fashion, both temporally and spatially. Therefore, in order to be able to selectively manipulate them, they need to be detected by upstream sensors—either wall-based or flow-based—in real-time. The random nature of the appearance of the counter-rotating roll modes can be overridden by a passive or active forcing scheme. One such passive scheme is converging-diverging riblets. As reviewed in §2.2.1.1, converging-diverging riblets can generate artificial counter-rotating roll modes, with the up- and down-wash sections above the converging and diverging sections, respectively—illustrated in Figure 5.3. These types of riblets are proposed to be used in combination with the opposing control scheme in the following manner: As the artificially generated counter-rotating roll modes are spatially confined in a time-average sense above the converging-diverging riblets, their appearance does not occur stochastically any more. It can be predicted where the up- and down-wash sections of these roll modes appear in order for the opposing control scheme (both suction and blowing) to act upon them—illustrated in Figure 5.3. The benefit is that the usage of any sensors for structure detection is spared, but the selective manipulation is still in place.

\[ \Lambda: \text{spanwise wavelength of the converging-diverging (C–D) regions} \]
\[ \beta: \text{yaw angle of riblets} \]
\[ h: \text{riblet height} \]
\[ s: \text{riblet spacing} \]

Figure 5.3: Schematic illustration of a conceptual opposing control scheme in the form of both blowing (red arrow) and suction (blue arrows), in combination with a passive control scheme of converging-diverging (C–D) riblets. Black circular arrows illustrate the artificial counter-rotating roll modes generated in the boundary layer due to the presence of C–D riblets. Reproduced from Kevin et al. (2017).
Conclusions and Future Work

Expansion of the Control Wall With the current spanwise spacing and width of the jet exit planes described in §3.2.2, only 8.6% of the wetted surface area was under control. Figure 5.4 illustrates how the wetted surface area is calculated, which is defined as the ratio of the area of the blue sections (i.e. the area of the jet exit planes) to the area of both the red and blue sections. Therefore, it is proposed to nullify the spanwise spacing between the actuators so that the entire wetted surface area would be under control. As a result, it is speculated that the beneficial variations of the flow and wall variables due to the opposing control scheme would be greater than any observed changes in §4.

![Diagram of control wall expansion](image_url)

$x_i(=1–9)$: streamwise centers of 9 modular inserts of size $l_m \times w_m = 0.30 \times 0.70$m
$y_i(=1–9)$: spanwise locations of 9 sensor-actuator pairs ($y_5 = 0$)

- $l_{hf} = 0.9$m, width of the sensing element
- $w_j = 2.0$m, spanwise width of the jet slit
- $l_j = 50.0$m, streamwise length of the jet slit
- $\Delta y = |y_{i+1} - y_i| = 26.0$m, spanwise spacing of the sensor-actuator pairs

Figure 5.4: (a) Top-view of the floating element assembly with the real-time control hardware implemented in the wind tunnel surface. (b) Magnified top-view of the sensors $s_1$ and $s_2$. (c) Magnified top-view of the actuators $a_1$ and $a_2$. For more details regarding this figure please refer to Figure 3.2.
Reynolds-number Dependency

The experiments in this study were conducted at one friction Reynolds number, namely $Re_\tau \approx 14,400$. Among all the implemented schemes, the opposing control scheme shows the maximum reduction in wall-shear stress, as discussed in §4. Therefore, it holds the potential for real-world application. One example is the control of the turbulent boundary layer over the fuselage of an aircraft. However, prior to that, the success of the opposing control scheme needs to be examined as a function of Reynolds number. As the boundary layer over the fuselage of an aircraft evolves, the associated friction Reynolds number increases and this requires us to control the turbulent boundary layer within a range of Reynolds numbers at a single cruising velocity of the aircraft. Therefore, it is of vital importance to investigate the performance of the control scheme as a function of Reynolds number prior to any real-world applications.


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