Experimental Investigation of an Actively Controlled Three-Dimensional Turret Wake

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Hemispherical turrets are bluff bodies commonly used to house optical systems on airborne platforms. These bluff bodies develop complex, three-dimensional flow fields that introduce high mean and fluctuating loads to the turret as well as the airframe support structure which reduce the performance of both the optical systems and the aircraft. An experimental investigation of the wake of a three-dimensional, non-conformal turret was performed in a low-speed wind tunnel at Syracuse University to develop a better understanding of the fundamental flow physics associated with the turret wake. The flow field was studied at a diameter based Reynolds number of 550,000 using stereoscopic particle image velocimetry and dynamic pressure measurements both with and without active flow control. Pressure measurements were simultaneously sampled with the PIV measurements and taken on the surrounding boundary layer plate and at several locations on the turret geometry. Active flow control of the turret wake was performed around the leading edge of the turret aperture using dynamic suction in steady open-loop, unsteady open-loop, and simple closed-loop configurations. Analysis of the uncontrolled wake provided insight into the complex three-dimensional wake when evaluated spatially using PIV measurements and temporally using spectral analysis of the pressure measurements. Steady open-loop suction was found to significantly alter the spatial and temporal nature of the turret wake despite the control being applied locally to the aperture region of the turret. Unsteady open-loop and simple closed-loop control were found to provide similar levels of control to the steady open-loop forcing with a 45% reduction in the control input as calculated using the jet momentum coefficient. The data set collected provides unique information regarding the development of the baseline three-dimensional wake and the wake with three different active flow control configurations. These data can be used to help guide future studies, both experimental and computational, of similar geometries and to provide insight for developing active control systems for complex, three-dimensional flows.
EXPERIMENTAL INVESTIGATION OF AN ACTIVELY CONTROLLED THREE-DIMENSIONAL TURRET WAKE

by

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Chapter 1

Introduction

Hemispherical turrets are three-dimensional bluff bodies that are commonly used to house radar and optical systems on airborne platforms. As bluff bodies, these geometries develop complex, three-dimensional flow fields that introduce high mean and fluctuating loads to the turret and the airframe reducing the performance of both the optical equipment and the aircraft. Turrets were initially introduced and have remained a geometry of choice as a result of the wide field of view that can be achieved for target tracking. Optical turrets, for example, can include a number of systems that require moving the turret aperture including cameras for imaging, forward looking infrared (FLIR) systems, and directed energy systems. When located on the bottom of an aircraft, the aperture can track targets in almost any direction below the aircraft with minimal changes to the geometric shape. Although improvements to the bluff body aerodynamics have been made by installing passive flow control devices such as fairings, generally these devices reduce the field of view for the optical systems and are discounted as viable solutions to the aerodynamic problem. The current research looks to use active flow control devices to manipulate the aperture and wake flow fields of a hemispherical turret. Active flow control devices do not impede the optical field of view yet have the potential to reduce the turbulence intensities near the turret
optics and improve the aerodynamic characteristics of the turret.

Active flow control has become a topic of considerable interest over the past several decades with most research focusing on two-dimensional test configurations such as cylinders (See, for example, Gregory et al. (2008), Arad et al. (2008), or Bhattacharya and Ahmed (2010)) and airfoils (See, for example, Seifert et al. (1993), Amitay et al. (2001), Pinier et al. (2007), or Shea and Smith (2009)). Control of more complex three-dimensional flow fields has only more recently become a topic of interest for test configurations such as delta and swept wings (See, for example, Kondor et al. (2005), Farnsworth et al. (2007), or Sefcovic and Smith (2010)) and ground based vehicles which will be discussed Section 1.3.1. Active flow control techniques have also been applied to turret geometries in the past and these studies will be discussed in more detail in Section 1.2.2.

1.1 Finite Cylinders

Finite cylinders are geometries where three-dimensional effects and the flow interactions at the cylinder-wall interface are important components of the flow field. Although finite cylinder studies typically include aspect ratios that are significantly higher than that of airborne turret geometries, the turret geometry can still be considered as a finite cylinder with a specific cap geometry. When considering finite cylinders, there are three main flow structures of interest. These are the horseshoe vortex at the cylinder-wall interface, Von Kármán vortex shedding from the cylindrical base, and cap vortices that develop from the free surface. Definitions of the basic geometric parameters as well as a schematic of a general finite cylinder flow field can be seen in Figure 1.1 where $U_\infty$ is the freestream velocity, $H$ is the total height, and $D$ is the cylinder diameter.
In what follows, aspect ratio will be represented using the symbol $\mathcal{R}$ and is defined as:

$$\mathcal{R} \equiv \frac{H}{D}$$

(1.1)

### 1.1.1 Horse Shoe Vortex

A horseshoe or necklace vortex forms at the base of a cylinder or airfoil when mounted normal to a surface. These structures are commonly seen in turbomachinery and atmospheric boundary layer geometries such as wind turbines or buildings. For most two-dimensional studies investigating high aspect ratio cylinders, the cylinder-wall interaction can be neglected, but for low aspect cylinders, either bounded or finite, the horseshoe vortex becomes more important. The horseshoe vortex develops as the incoming boundary layer and a portion of the freestream flow separate and wrap around the base of the cylinder. Research by Eckerle and Langston (1987) studied the formation of a horseshoe vortex around a bounded cylinder in cross-flow.
study was performed at a diameter based Reynolds number, \( Re_D \), of \( 5.5 \times 10^5 \) with an incoming boundary layer thickness, \( \delta \), of 9.9% of the turret diameter where Reynolds number is defined in the traditional manner (Eqn. 1.2).

\[
Re_D \equiv \frac{\rho U_\infty D}{\mu} \tag{1.2}
\]

Here, \( \rho \) is the density and \( \mu \) is the dynamic viscosity. The work by Eckerle and Langston showed that the horseshoe vortex became fully developed at an angle of approximately 15° from the leading edge of the cylinder and started to weaken and move away from the cylinder at a radial location perpendicular to the leading edge (i.e. 90°). This study by Eckerle and Langston did not include analysis of the flow field beyond the 90° location.

Other work studying the horseshoe vortex has suggested that the structure is actually a system of up to four vortices with the vortex traditionally referred to as the horseshoe vortex being the dominant structure (Baker 1980). Recent work has only been able to show the existence of three of the four vortex structures (Pattenden et al. 2005; Krajnovi´c 2011). The paper by Pattenden et al. provides a nice review of experimental studies performed prior to 2005 for finite cylinders where free end effects were considered. Computational work, such as that by Morgan and Visbal (2008), help visualize the horseshoe vortex as it relates to a low \( A_R \) turret geometry as well as the interaction of the vortex with the wake (Fig. 1.2).

### 1.1.2 Von Kármán Vortex Shedding

The wake region downstream of the turret is perhaps the most significant feature of the turret flow field. Independent Von Kármán vortex streets can develop as a result of the flow separation from the cylindrical base as seen in Figure 1.1; however, the vortices that develop from the cap tend to dominate the wake for low \( A_R \) turrets as
seen in Figure 1.2. Previous work has shown that there is a critical height at which the cylinder will experience Von Kármán vortex shedding relatively independent of the counter-rotating vortex pair that develops from the cap geometry thus giving a value to help define low $\mathcal{R}$ turrets. Work by Kawamura et al. (1984) showed that for a finite cylinder in cross-flow with a flat cap, the critical $\mathcal{R}$ was on the order of 6-8 depending upon the thickness of the incoming boundary layer. For the current research, $\mathcal{R}$ will be 1.17 and thus the flow field from the cap will be dominant in the wake development. The details of Von Kármán vortex shedding will not be discussed here, but is is important to note that the Strouhal number (Eqn. 1.3) for the dominant shedding frequency, $f$, of a two-dimensional, circular cylinder at $Re_D$ of $3.2 \times 10^4$ is approximately 0.21.

$$St_D \equiv \frac{fD}{U_\infty} \quad (1.3)$$

Using cross-wires, Kawamura et al. (1984) showed that the dominant Strouhal number for a finite cylinder decreases as $\mathcal{R}$ decreases and is dependent upon the location
of the measurement height above the plate. Very near the plate, the Strouhal number was found to be as low as 0.15. Work at high Reynolds numbers with two-dimensional turrets has shown the Strouhal number to shift from 0.21 for ReD of 1.4 × 10^5 to 0.27 for ReD of 3.6 × 10^6 (Lo et al. 2005).

1.1.3 Free End Geometry Effects

Several end cap geometries have been considered for finite cylinders in cross-flow including flat, beveled, and hemispherical geometries. Park and Lee (2004) presented data for various cap geometries studied at ReD of 2.0 × 10^4 using flow visualization, PIV, and hotwire measurements. These data were acquired at various locations in the flow field to show the effects of the cap geometry on the flow field as a whole for a finite cylinder with A of 6. For the current work, only the hemispherical cap geometry will be considered. With respect to turrets, there are two main hemispherical geometries that have been studied: a conformal turret with a true hemispherical shape (Fig. 1.3(a)), and a non-conformal turret with a flat aperture used to house optics (Fig. 1.3(b)). It has been shown both experimentally (Vukasinovic et al. 2008) and computationally (Morgan and Visbal 2008) that for a conformal turret at low

![Figure 1.3: Comparison of hemispherical turret cap geometries.](image-url)
Mach numbers, the flow separates from the turret along a well defined line. This line forms the shape of a horseshoe that wraps over the top of the turret at an angle relative to the boundary layer plate of approximately 120° at the centerline. This separation can become asymmetric as the Mach number increases (Vukasinovic et al. 2008).

For a non-conformal turret, separation is highly influenced by the location of the aperture as the sharp corners can induce flow separations at different angles than are seen for a conformal turret. From work performed by Wallace et al. at $Re_D$ of $5 \times 10^5$, it was seen that the flow stayed attached over the aperture region for static aperture angles up to $\beta = 115^\circ$. Beyond this angle, the flow was found separate at the leading edge of the aperture. The experiments by Wallace et al. did not find if there was a limit of $\beta$ at which the flow would again separate upstream of the aperture. This work did show, however, that the angle at which separation occurred was influenced by dynamically pitching the turret aperture. As $\beta$ increased from an attached flow state, the flow would remain attached for several degrees past 115°. Similarly, as $\beta$ decreased from a separated flow state, the flow would remain separated several degrees past 115° (Wallace et al. 2010; Wallace et al. 2012). This effect is known as hysteresis and is commonly seen in dynamic stall testing of pitching airfoils.

\section{1.2 Turrets}

This section will present work focused specifically on turrets. The devices that follow will all be low $AR$ geometries ($\leq 2$) with either conformal or non-conformal hemispherical cap geometries. The work presented here includes aerodynamic and aero-optic studies where a majority of the active flow control work on these geometries has been performed in the aero-optics field.
1.2.1 Aerodynamic Studies

Static Loading

Testing has been performed on turrets to evaluate the aerodynamic loads that exist as a result of the pressure distributions over the turret. Research by Snyder et al. (2000) was performed on a turret with an undersized cylindrical housing (i.e. the diameter of the cylindrical base was smaller than the diameter of the hemispherical cap) supporting a conformal, hemispherical cap. The focus of this work was to examine the aerodynamic loads on the turret with various splitter plate and fairing geometries at $Re_D$ ranging from $3 \times 10^5$ to $9 \times 10^5$. Similar work by Sluder et al. (2008) was performed to examine the aerodynamic loads for different $AR$ while keeping the diameter fixed. The body of work by Sluder et al. was performed over a range of Reynolds numbers up to $Re_D = 1.23 \times 10^6$ and a portion of this work also examined the use of splitter plates and fairings in the wake of the turret. Both of these studies were performed to obtain an understanding of the mean loading on the turret geometry and time resolved measurements were not presented.

In both the work by Snyder et al. and Sluder et al., the drag force was reduced to a drag coefficient using the frontal area of the turret. Snyder et al. showed that the drag coefficient on the turret asymptotically decreased to a value of approximately 0.57 as the velocity increased. Sluder et al. showed that the drag coefficient increased asymptotically to a value of approximately 0.5 which suggests that the differences in the test geometries was not negligible. A unique component of the work by Sluder et al. was an investigation of the lift force on the turret. The lift force was found to be quite significant for the conformal turret geometry. When the lift force was reduced to a lift coefficient using the frontal area, the lift coefficient decreased from 1.5 for $AR = 0.5$ (i.e. no cylindrical base) to 0.7 for $AR = 1.5$. Perhaps more appropriately, when lift was reduced using the planform (i.e. the circular footprint), Sluder et al.
found that the lift coefficient increased asymptotically with increasing $AR$ to a value of approximately 1.25.

Snyder et al. studied an array of thin splitter plates and three-dimensional fairings placed in the turret wake. With the basic splitter plate, reductions in the drag coefficient as large as 7% were observed, and reductions in the drag as large as 55% were seen with the three-dimensional fairing. For similar devices placed in the wake, Sluder et al. saw reductions of approximately 10% and 25% for the splitter plate and fairings, respectively. Sluder et al. also showed reduction in the lift coefficient although the effects were not as large as for the drag coefficient. In general, these passive devices have the effect of reducing the strength of vortices in the wake of the turret responsible for pressure drag thus improving the aerodynamic performance of the bluff body, but do impede the field of view for the turret.

**Oil Flow Visualization**

One common method used to gain an understanding of the flow field that develops in the wake of the turret is surface flow visualization. This technique involves coating the surface of the turret and wall geometry with oil and running the wind tunnel at the prescribed operating conditions for a period of time long enough to allow flow structures to develop in the oil. Surface flow visualization studies show the structure of the horseshoe vortex that wraps around the turret and that, in a mean sense, there is a closed recirculation region that develops in the wake of the turret. This has been shown for a variety of turret geometries as well as for different wall geometries (see, for example, Tutkun et al. (2007), Vukasinovic et al. (2009), or Reynolds et al. (2012)). As expected, the size of the recirculation region is dependent upon the height of the turret and the diameter-based Reynolds number. Experimental (Leder et al. 2003) and computational work (see, for example, Morgan and Visbal (2008)) have shown
that this closed recirculation region is formed by a symmetric pair of counter-rotating vortices (Fig. 1.2) that develop as a result of flow separation from the cap of low $AR$ turrets.

**Laser Doppler Anemometry**

Leder et al. (2003) have performed a three-dimensional assessment of a turret wake using a three-component laser doppler anemometry (LDA) system. These studies investigated a conformal turret with $AR$ of 2 at $Re_D = 2.0 \times 10^5$. For these studies, data were acquired at approximately 20000 locations in the wake of the turret at a rate of approximately 100 Hz. Results were presented for the mean velocities, turbulence intensities in the wake, and vorticity. Isosurfaces of the zero velocity region were shown to be contained within the width of the turret in the cross-stream direction parallel to the boundary layer plate but extended 2.4 diameters in the streamwise direction. Plots of the turbulent kinetic energy showed that the highest concentrations of energy existed two diameters downstream, and high levels of turbulent kinetic energy were seen to extend well beyond the length of the closed recirculation and even beyond the measurement range of 3.5 diameters downstream of the turret.

Although it is possible to use an LDA system to acquire time resolved data that can be used to gain an understanding of the temporal nature of a flow field, Leder et al. did not present their data in this manner. Due to the relatively low sampling rate (100 Hz), a significant portion of the spectral range would not have been investigated as the dominant frequencies in the wake were most likely on the order of several hundred Hertz. To use the LDA data as a time resolved measurement for these test conditions, the sampling rate would most likely needed to be increased by as much as two orders of magnitude.
Pressure

Limited work has been performed to characterize the temporal nature of the turret wake as most research has focused on obtaining information regarding time-averaged statistics or frequency spectra of the shear layer that develops in the aero-optics region as a result of separation from the cap geometry. Work performed by Gordeyev et al. (2006) investigated the wake frequency content of a 0.305 m diameter, conformal turret at various Mach numbers, $M$, ranging from 0.30 to 0.45. The pressure sensors in the study were located on the support wall a distance of one radius downstream of the trailing edge and one radius from the centerline of the turret. For the cases studied, it was seen that a single peak frequency existed at $St_D = 0.35$ independent of the Mach number. As discussed previously, $St_D$ can be as high as 0.27 for two-dimensional cylinders at higher Reynolds numbers comparable to the work by Gordeyev et al. The peak in Strouhal number for the Gordeyev research was attributed to vortex shedding in the wake of the turret, but is notably higher than $St_D$ for a two-dimensional cylinder indicating that Kármán vortices were not the dominant feature of the turret wake. In the same body of work, Gordeyev et al. took optical measurements through the shear layer that developed between the separated flow and the freestream using a Malley probe. These data showed that the shear layer above the turret had frequency content on the order of 2 kHz which corresponds to $St_D \approx 6$. As would be expected, the frequency of vortex shedding in the wake and the frequency of the shear layer instabilities differ significantly. In this case, by an order of magnitude.

1.2.2 Aero-Optic Studies

The aero-optics problem arises in optical based turret systems operating on an aircraft capable of traveling at speeds where compressibility effects are present. The highly turbulent flow field that develops in the turret wake and over the turret aperture for
some look angles has the ability to create density fluctuations as a result of compressibility effects. These small density fluctuations change the local index of refraction and act to distort the optical signal to and from the optical system degrading the performance of the device. It is important to note that these effects can be seen over a wide range of Mach numbers starting as low as 0.3. The following sections discuss only the aerodynamic work that has been performed to study this problem, but it important to note that adaptive optics have also been utilized in this field (see, for example, Jumper and Fitzgerald (2001), Nightingale et al. (2008), or Rennie et al. (2010)).

Passive Flow Control

Passive flow control techniques, similar in concept to the splitter plates and fairings applied to improve the bluff body characteristics of the turret, are devices that geometrically modify the turret in an attempt to improve the aero-optic characteristics of the turret flow field. Work by Gordeyev et al. (2010) has investigated placing pins of varying diameter, spacing, and height upstream of a two-dimensional, cylindrical turret with a rear facing, flat window. Studies were performed at Mach numbers ranging from 0.4 to 0.5 using static pressure to obtain velocity information and a Malley probe system to measure aero-optic distortions. These studies showed that, with the proper pin configuration, the optical path distortion root mean square values \( \langle \text{OPD}_{\text{RMS}} \rangle \) could be reduced by 15-20% for a range of rear facing look angles. If the pins were not properly configured, however, the passive control devices had the ability to increase the \( \text{OPD}_{\text{RMS}} \) values. Large Eddy Simulation (LES) performed for similar test conditions verified the sensitivity to the pin size and placement and provided a more detailed understanding of the physical control mechanisms associated with the pins (Morgan and Visbal 2010).
Geometric modifications have also been studied on three-dimensional turrets. Two tests of interest involve complex modifications to a non-conformal turret geometry including adding ribs, fins, chevrons, and pins of varying sizes (Haynes et al. 2012; Reynolds et al. 2012). The studies performed by Haynes et al. (2012) were performed at $Re_D = 1.4 \times 10^5$ with turrets of $AR$ less than 1.0. Similarly, the studies performed by Reynolds et al. (2012) were performed at $Re_D = 1.93 \times 10^5$ with turrets with $AR$ of 0.85. For rear facing apertures, Haynes et al. showed that ridges wrapped around the turret provided the best flow improvement over the turret aperture. Similar results were reported by Reynolds et al., but pins were shown to be even more effective when considering pressure fluctuations at the center of the turret aperture. Reynolds et al. also studied side facing aperture angles and again concluded that properly sized pins were the most effective at reducing pressure fluctuations at the aperture. It is important to note that the sizing of the most effective pins was different for the rear facing and side facing aperture settings which helps to illustrate one of the difficulties of performing passive control on dynamic geometries. Considering the practical application of the passive control techniques from both Haynes et al. and Reynolds et al., most of the passive control devices studied would severely limit the ability to move the turret aperture through the wide field of view desired in real-world applications. Having the ability to make real-time adjustments to the passive control devices (such as changing the length or retracting completely) would be necessary.

Numerical studies performed by Crahan et al. (2012) have shown the possibility of performing passive flow control in a more global sense by applying fences, referred to as “virtual ducts”, to both the turret and the airframe surrounding the turret as seen in Figure 1.4. With the application of the virtual duct to only the conformal turret, the critical Mach number was increased from 0.55 to 0.74. Here, the critical Mach number is defined by the freestream flow conditions (i.e. the aircraft cruise velocity)
at which supersonic flow will occur over the turret. With the application of the virtual duct around the turret on the aircraft, the critical Mach number was increased beyond 0.8. These configurations were also seen to delay the natural separation on the turret by almost 20°. Similar to the studies by Haynes et al. and Reynolds et al., these ducts were optimized for a particular set of conditions and may not provide similar improvements for a wide range of look angles. The concern also exists that the outer fences may further reduce the field of view for the turret although the proposed geometries have the potential to be less intrusive than the splitter plates and fairings discussed previously in the work by Snyder et al. and Sluder et al.

Figure 1.4: “Virtual duct” concept proposed by Crahan et al. (2012).

**Active Flow Control**

Active flow control techniques differ from passive flow control techniques in that geometric modifications are not made to the geometry of interest. To perform the control, either mass or momentum is imparted to the flow using blowing, suction, or a combination of both. Active flow control studies have used several techniques to gain control authority over the aperture region of an optical turret including synthetic jet actuators, dynamic suction, and hybrid methods employing a combination of active and passive control techniques. Synthetic jets were one of the first experimentally
successful active control techniques to be used on the hemispherical turret geometry
to gain control authority over the aero-optics flow field. In tests by Andino et al.
(2008) performed at a Mach number of 0.3, synthetic jet actuators were shown to
reduce the root mean square of the pressure signal on the surface of the aperture
by up to 11% using open-loop flow control techniques. Tests with the same experi-
mental configuration using closed-loop control achieved pressure $RMS$ reductions up
to 18% (Wallace et al. 2008). In the closed-loop work by Wallace et al., feedback
sensing was performed using surface pressure measurements acquired on or around
the turret aperture. Similar work by Gordeyev et al. (2009) using synthetic jet actu-
ators at $M = 0.3$ has shown that open-loop control had the ability to reduce $OPD_{RMS}$
above the aperture by up to 34%. The optical path distortions were measured using
a Malley probe that uses two parallel laser beams to measure optical variations along
a path. The work by Gordeyev et al. demonstrated that synthetic jets were effective
up to $M = 0.6$ with decreasing effectiveness as the Mach number increased. This
reduction in performance at higher velocities is a common characteristic of synthetic
jet actuators.

Research has recently been performed investigating the effects of dynamically
pitching the cap geometry of a non-conformal turret to simulate target tracking for
an airborne system. In these studies, the time-dependent pitch angle of the aper-
ture ($\beta$) was prescribed such that $\beta$ increased and decreased in a sinusoidal fashion
throughout testing. For the dynamic pitching tests, active flow control was performed
using dynamic suction actuators arranged around the aperture of a non-conformal
turret. Wallace et al. performed initial experiments in a low-speed wind tunnel at
$M = 0.1$ where the effects of compressibility could not be studied (Wallace et al.
2010; Wallace et al. 2012). Preliminary tests of the dynamic suction actuators on a
static turret were performed to ensure the effectiveness of the control system. Fig-

15
Figure 1.5 directly compares a baseline and open-loop control test. The contours indicate reduced, streamwise velocity and streamlines are drawn along lines of constant velocity magnitude. In Figure 1.5(a), the separated region above the aperture can be seen clearly as a closed recirculating flow in the mean. With open-loop suction, this recirculation region no longer exists over the aperture (Fig. 1.5(b)) indicating that the dynamic suction actuators do in fact have control authority over this flow field.

In dynamic pitching tests, Wallace et al. saw up to 57% reductions in the streamwise velocity fluctuations ($u_{RMS}$) above the aperture. This work also showed that including any information about the flow state (i.e. any level of feedback control) into the control input signal had the effect of increasing the efficiency of the active control system while still achieving comparable reduction in $u_{RMS}$ above the aperture (Wallace et al. 2010; Wallace et al. 2012). Similar tests at a Mach number of 0.3 showed significant reduction in the $OPD_{RMS}$ for varying levels of open-loop control (Wallace et al. 2011).

Morgan and Visbal have performed computational studies of active flow control techniques as applied to both conformal (Morgan and Visbal 2009) and non-conformal turrets (Morgan and Visbal 2011). In both studies, a hybrid RANS/LES model was
used to investigate the turret flow field. The study of the conformal turret was performed at $Re_D = 2.4 \times 10^6$ with a free stream Mach number of 0.4 and turret $AR$ of 1.75. This study investigated three different control configurations including oscillatory blowing/suction through a slot, steady suction through a slot, and low velocity suction through a porous surface. Both configurations with suction were shown to delay separation on the geometry while the oscillatory blowing/suction did not have a significant effect. The study on the non-conformal turret was performed at $Re_D = 1.98 \times 10^6$ with a free stream Mach number of 0.35 and turret $AR$ of 1.67. Using steady suction from a slot around the turret aperture as an active control input, the studies showed that the active control was able to shift the separation point from the leading edge of the aperture to the trailing edge of the aperture similar to the experimental results seen in Figure 1.5.

**Hybrid Control**

Vukasinovic et al. (2011) have developed a hybrid control system that consists of an array of synthetic jet actuators on the hemispherical cap and a leading edge splitter or partition plate located parallel to the floor of the tunnel at the top cylindrical base. It was shown that, with a properly sized splitter plate, the angle $\beta$ at which separation occurred on the non-conformal turret was delayed by more than $10^\circ$. Surface flow visualization also showed that the length of the recirculating wake was reduced by more than 45%. Reductions in the $OPD_{RMS}$ of up to 33% were seen with the application of the partition plate, specifically at high values of $\beta$ (Gordeyev et al. 2010). The application of synthetic jets further improved the response of the flow to the passive control device. It is important to note that the passive control device considerably alters the basic flow that typically develops on the turret. As a result of the partition plate being parallel to the support wall, an additional horseshoe vortex
is created on the surface of the splitter plate that wraps around the hemispherical cap. This vortex has the effect of relocating the stagnation point of the freestream flow on the cap geometry and will most likely alter how the flow around the cylindrical base interacts with the flow over the hemispherical cap. Thus, the passive device is capable of significantly altering the flow field around the turret, but it is difficult to isolate the features of the altered flow field that are responsible for improving the flow characteristics related to the aero-optics.

1.3 Related Geometries

The intent of the current study was to investigate the wake of the turret as a three-dimensional bluff body. The fundamental physics associated with the turret flow field will be similar to the physics associated with geometries such as road vehicles, aircraft, buildings, and biological flows. As such, the results from the current studies can be used to help understand these related flow fields as well. A brief overview of the research in some of these fields is presented in the following sections.

1.3.1 Road Vehicles

Road vehicles here are discussed in reference mainly to the most common vehicles such as passenger cars and tractor-trailers used for hauling freight as opposed to more advanced sports cars. Typical road vehicles are generally complex three-dimensional geometries with the design focus on operational requirements as opposed to the aerodynamic shapes. Perhaps the most obvious example of this is the tractor-trailer with a simple box shape designed to meet specific size constraints. Several studies have been performed studying the fundamental physics of the wake flow of these large trucks as well as to investigate passive and active control techniques that could be
applied to reduce aerodynamic drag (see, for example, McCallen et al. (2004), Seifert et al. (2008), or Bellman et al. (2009)).

Passenger cars have also been studied fairly extensively with a significant portion of the work being performed on a generic vehicle shape known as the Ahmed body. The Ahmed body is a fairly simple shape that allows researchers to easily characterize and describe the test model geometry and make comparisons to similar research. Some examples of the fundamental aerodynamic studies and flow control studies can be seen in the work by Wassen and Thiele (2008), Carnarius et al. (2009), or Aubrun et al. (2010). Although studies of the Ahmed body are common, research has also been performed on more applied geometries as seen in the work by Kozaka et al. (2004), Özdemir and Özdemir (2004), and Heft et al. (2011).

1.3.2 Aircraft Structures

Structural and operational requirements of cargo aircraft often result in geometries that are not optimized for the wide range of aerodynamic conditions they are expected to operate in. One such aircraft is the United States Air Force C-130 cargo airplane that has been in service since 1959 and provides tactical airlift support. The C-130 can operate in a range of conditions including operating from dirt and ice runways in extreme conditions and is often required to operate in short take off and landing (STOL) conditions (Air Force Mobility Command 2009). The rear cargo door on the C-130 is known to be a region that induces large drag penalties on the aircraft and several studies have focused on improving the flow field in this region (See, for example, Karmondy and Yechout (2008), Wooten and Yechout (2008), or Bergeron et al. (2009)). Comparing the rear cargo door area of a C-130 to a non-conformal turret, similarities can be seen in the complex three-dimensional geometry that develops a separated flow region from a geometrically induced separation line.
1.3.3 Biological Flows

Turret like geometries also exist naturally in biological flows. One such example can be seen by studying Mussels when partially buried in sediment at the bottom of a river or stream. Studies have been performed on mussels to investigate attachment strength by performing lift and drag measurements (Witman and Suchanek 1984) as well as to gain a better understanding of the flow field that develops around the mussels as can be seen in the work by Miyawaki et al. (2009). The work by Miyawaki et al. included both numerical and experimental investigations of an array of mussels attached to a ground plane.

1.4 Motivation

As seen in the previous sections, an extensive amount of work has been performed studying finite cylinders and turret geometries. Throughout these studies, however, there is a notable absence of time dependent studies focused on the aerodynamic characteristics of the turret wake. Similarly, work performed on the aero-optic problem has investigated the effects of various active flow control techniques on the temporal and spatial characteristics of the aperture flow field, but have not studied the effects of these devices on the turret flow field as a whole with the exception of oil flow visualization and the limited computational studies. The goal of the current work is to bring both spatial and time resolved measurements into the wake of the turret to gain a better understanding of the flow field structure and the temporal characteristics with and without active flow control. It is the belief of the author that having more detailed understanding of the wake flow field and the effects of the active control systems will allow for the development of more effective and efficient active control systems whether focusing on the aerodynamic problems associated with the
bluff body or the aero-optics problem near the aperture. There is also the possibility of applying the concepts developed here to similar geometries such as aircraft structures or road based vehicles in the future. Developing a full understanding of these complex three-dimensional flow fields will require joint experimental, computational, and theoretical efforts, and the current data set has been acquired with this in mind. The data presented in this document have been acquired and analyzed to serve as a guide not only for future experimental efforts, but also to serve as a validation test bed and starting point for computational studies.

The current work is a continuation of the efforts of Syracuse University graduates Marlyn Andino and Ryan Wallace who studied the three-dimensional turret geometry with a focus on the aero-optics problem. As such, the geometry and active flow control systems have been modeled after the systems tested previously at Syracuse University with modifications made to help study the wake. This includes the introduction of an instrumented boundary layer plate, modifications to the turret attachment points, and a larger distribution of pressure taps on the turret itself. It is important to note that the active flow control system investigated in the current work was designed specifically to perform flow control in the aero-optic region of a dynamically pitching turret. The control effects of this specific active control system will be studied in a more global sense by evaluating the effects of the control in the wake using both spatial data from PIV and time resolved data from surface pressure measurements. To perform active flow control with an objective of controlling the mean and fluctuating aerodynamic loads, a different control system would most likely need to be developed with active control devices placed at different locations on the turret geometry.
Chapter 2

Theoretical Background

This chapter provides a brief overview of the theoretical topics and techniques used throughout this document. These topics will not be covered in detail here, but comprehensive discussions can be found in the references listed throughout.

2.1 Equations of Motion

2.1.1 Navier-Stokes Equations

The Navier-Stokes equations are a system of non-linear, partial differential equations that define the flow of Newtonian fluids. The equations of interest are the conservation of mass equation (Eqn. 2.1), simplified here for constant density fluids, and the conservation of momentum equations (Eqn. 2.2). There are a limited number of exact solutions to these equations, and only recently have computers become powerful enough to perform direct numerical simulations of practical problems for a well defined set of boundary conditions (Kundu and Cohen 2004; Muson et al. 2002).

\[
\frac{\partial u_i}{\partial x_i} = 0 \quad (2.1)
\]
\[
\frac{\partial u_j}{\partial t} + u_i \frac{\partial u_j}{\partial x_i} = -\frac{1}{\rho} \frac{\partial p}{\partial x_i} + \nu \frac{\partial^2 u_j}{\partial x_i^2} + f_j \tag{2.2}
\]

### 2.1.2 Reynolds Averaged Navier-Stokes (RANS) Equations

The complexity of turbulent flows makes it difficult to apply the Navier-Stokes equations directly. Because it is not possible to describe a turbulent flow at all points in space and time, a statistical representation of the Navier-Stokes equations has been developed to analyze turbulent flow fields. Using the decomposition methods outlined by Reynolds, the statistical equations known as the Reynolds Averaged Navier-Stokes equations can be derived. Reynolds decomposition is used to separate the total velocity and pressure into time averaged and fluctuating components as seen in Equations 2.3 and 2.4 where \( u \) and \( p \) are the total terms, \( U \) and \( P \) are the time averaged terms, and \( u' \) and \( p' \) are the fluctuating terms.

\[
u_i = U_i + u'_i \tag{2.3}
\]

\[
p = P + p' \tag{2.4}
\]

The details will not be presented here, but substituting the decomposed velocity and pressure terms into the mass and momentum equations and performing a temporal average results in the RANS equations seen in Equation 2.5.

\[
U_i \frac{\partial U_j}{\partial x_i} = \frac{1}{\rho} \frac{\partial}{\partial x_i} \left[ -P \delta_{ij} - \rho u'_i u'_j + \mu \left( \frac{\partial U_i}{\partial x_j} + \frac{\partial U_j}{\partial x_i} \right) \right] \tag{2.5}
\]

Here, \( \delta_{ij} \) is the Kronecker delta and the symmetric tensor \( u'_i u'_j \) is known as the Reynolds stress tensor which adds an additional six unknowns to the system of equations. As such, there are more unknowns than equations in the system and the equations cannot be solved directly. This is known as the closure problem of turbu-
lence (Pope 2000; Tennekes and Lumley 1972).

2.2 Turbulent Kinetic Energy (TKE) Equation

To understand how turbulent energy is produced, transported, and dissipated, the turbulent kinetic energy equation can be derived from the RANS equations and the mean kinetic energy equation. The quantity of interest is the turbulent kinetic energy defined in Equation 2.6 and governed by Equation 2.7.

\[
k \equiv \frac{u'_i u'_i}{2} \quad (2.6)
\]

\[
U_j \frac{\partial k}{\partial x_j} = -\frac{\partial}{\partial x_j} \left[ \frac{\rho u'_i u'_j}{2} - \nu \frac{\partial k}{\partial x_j} \right] - \frac{u'_i u'_j}{2} \frac{\partial U_i}{\partial x_j} - \nu \frac{\partial u'_i}{\partial x_j} \frac{\partial u'_i}{\partial x_j} \quad (2.7)
\]

In Equation 2.7, term 1 is the turbulent kinetic energy convection, term 2 is the transport, 3 is the production, and 4 is the dissipation. From an experimental standpoint, it is difficult to measure the pressure for the entire flow field and to acquire data that can be used to evaluate the derivatives for the dissipation term. Despite these difficulties, energy balances can still be performed using experimental data to gain better insight into how the flow field develops from a turbulence perspective.

2.3 Statistical Analysis of Turbulent Flows

As discussed previously, it is not possible to describe a turbulent flow at all points in space and time. With this in mind, a statistical approach to analyzing and presenting data will be used in what follows. The two types of statistical analysis that will be used are single point and multi-point formulations. Single point statistics are determined by analyzing a single point in space over time and multi-point statistics
are calculated by comparing multiple points over time to gain an understanding of the spatial relationships throughout the flow field.

### 2.3.1 Single Point Statistics

Single point statistics are perhaps the easiest to calculate and interpret. Basic single point statistics such as the mean (Eqn. 2.8) and fluctuating root mean square (RMS) defined in Equation 2.9 are commonly used to analyze PIV snapshots that are independent in time such as the data that are presented in the following chapters. Although $u_i$ is defined at all points in space (i.e. $u_i(x_i, t)$ for $l = 1, 2, 3$), only the planar or two-dimensional definition will be used for PIV measurements (i.e. $u_i(x_i, t)$ for $l = 1, 2$ or 1,3).

\[
U_i(x_i) = \frac{1}{N} \sum_{q=1}^{N} u_i(x_i, t_q) \quad (2.8)
\]

\[
\sigma_i(x_i) = \sqrt{\frac{1}{N + 1} \sum_{q=1}^{N} (u'_i(x_i, t_q))^2} \quad (2.9)
\]

Similarly, the time averaged Reynolds stresses can be calculated from the fluctuating velocities as seen in Equation 2.10

\[
\overline{u'_i u'_j}(x_i) = \frac{1}{N} \sum_{q=1}^{N} (u'_i(x_i, t_q) u'_j(x_i, t_q)) \quad (2.10)
\]

It is also possible to examine the individual terms of the TKE equation as single point statistics. The terms of interest for the current study are convection (Eqn. 2.11), production (Eqn. 2.12), turbulent transport (Eqn. 2.13), viscous transport (Eqn. 2.14), and dissipation (Eqn. 2.15).

\[
C = U_j \frac{\partial k}{\partial x_j} \quad (2.11)
\]
\[ P = -u'_i u'_j \frac{\partial U_i}{\partial x_j} \]  
\[ T_k = -\frac{\partial}{\partial x_j} \left[ \frac{u'_i u'_i u'_j}{2} \right] \]  
\[ T_\nu = -\frac{\partial}{\partial x_j} \left[ -\nu \frac{\partial k}{\partial x_j} \right] \]  
\[ \varepsilon = -\nu \frac{\partial u'_i}{\partial x_j} \frac{\partial u'_i}{\partial x_j} \]  

It is important to note that limitations in the PIV measurements will not allow for derivatives to be calculated in all directions and therefore it will not be possible to account for the TKE terms in their entirety. Investigating the spatial dependence and relative magnitude of the components that can be calculated will still provide insight into the nature of the turbulent flow field and how it develops.

### 2.3.2 Multi-Point Statistics

The multi-point statistics of interest for the PIV measurements are the spatial cross-correlation function and the integral length scales. The time averaged, two-point cross-correlation is calculated as seen in Equation 2.16 (Cole and Glauser 1998; Pope 2000). The two-point cross-correlation is defined in three dimensions, but will be used in a two-dimensional form in the current studies as the PIV data are planar measurements.

\[ R_{ij} (x_i, x'_i, x_m, x'_m) = \frac{u'_i (x_i, x_m)}{u'_j (x'_i, x'_m)} \]  

Here, \( x_i \) and \( x_m \) are fixed reference point locations and \( x'_i \) and \( x'_m \) are the locations of the shifted point used for the cross-correlation where \( l=1 \) and \( m=2 \) or 3 depending on the field of view. \( u'_i \) and \( u'_j \) are components of the fluctuating velocity where \( i \) and \( j \) are not necessarily equal. From the cross-correlation function, the integral length scale can be found by integrating the normalized cross-correlation as seen in
Equation 2.17, here again reduced to two-dimensions as appropriate for the planar PIV measurements where $l=1$ and $m=2$ or 3 depending on the field of view.

$$L_{ij}(x_l, x_m) = \sqrt{\frac{1}{R_{ii}(x_l, 0, x_m, 0)} \int \int R_{ij}(x_l, x'_l, x_m, x'_m) \, dx'_l dx'_m}$$ (2.17)

It is also possible to look at the individual contributions to the integral length scale by integrating the cross-correlation function along a path away from the fixed reference point as seen in Equation 2.18 where $n=l$ or $m$ depending on the field of view.

$$L_{ij}(x_l, x_m) = \frac{1}{R_{ii}(x_l, 0, x_m, 0)} \int_0^\infty R_{ij}(x_l, x'_l, x_m, x'_m) \, dx'_n$$ (2.18)

In a three-dimensional wake, turbulence is not homogeneous and therefore the integral length scale will be path dependent. For the current document, length scales will only be calculated along the coordinate directions and the path will be indicated as a superscript. For example, the integral length scale calculated parallel to the $x$-axis for the cross-correlation of the $u'$ velocity component will be indicated as $L_{uu}^x$. The integral length scale can be used to gain insight into the size of the largest coherent structures in a flow field. As an example, the integral length scales in pipe flow are generally on the order of the pipe radius.

### 2.4 Spectral Analysis

Spectral analysis is a technique that is used extensively throughout this document to evaluate temporal data. From spectral analysis, one can determine dominate frequencies in the flow field corresponding to organized structures, time scales associated with various structures, and convective time scales between multiple points. The auto-spectral density functions and cross-spectral density functions will be calculated...
using Fourier methods as outlined in the text by Bendat and Piersol (1993) where the Fourier transform and block averaged cross-spectral density function are defined in Equations 2.19 and 2.20, respectively.

\[
\hat{p}_{mj}(f,T) = \int_{0}^{T} p'_{mj}(t)e^{-i2\pi ft} dt
\]

(2.19)

\[
S_{jk}(f) = \lim_{T \to \infty} \frac{1}{T} \hat{p}_{mj}^*(f,T)\hat{p}_{mk}(f,T)
\]

(2.20)

Here, \( T \) is the total sampling time for block \( m \), \( p'_{mj} \) is the time series of the pressure signal for block \( m \) and sensor \( j \), \( \hat{p}_{mj} \) is the Fourier transform of the pressure signal for block \( m \) of sensor \( j \), \( t \) is time, and \( S_{jk} \) is the cross-spectral density function of sensor \( j \) with sensor \( k \). Note that (*) indicates the complex conjugate, the overline indicates the block average, and the auto-spectral density function is returned for \( j = k \).

### 2.4.1 Multi-Point Cross-Correlations

The cross-correlation function (Eqn. 2.21) is taken as the inverse Fourier transform of the block averaged cross-spectral density function and is normalized using the definition of the correlation coefficient seen in Equation 2.22.

\[
R_{jk}(\tau) = \int_{-\infty}^{\infty} S_{jk}(f)e^{i2\pi f \tau} df
\]

(2.21)

\[
\rho_{jk}(\tau) = \frac{R_{jk}(\tau)}{\sqrt{(R_{jj}(0))(R_{kk}(0))}}
\]

(2.22)

Here, \( R_{jk} \) is the cross-correlation function between sensors \( j \) and \( k \), \( \tau \) is a time delay, and \( \rho_{jk} \) is the normalized correlation coefficient.

Similar to the integral length scales discussed previously, it is possible to calculate an integral time scale from the temporal cross-correlation (Eqn. 2.23). The integral time scale provides insight into the length of time that a temporal signal is correlated.
with itself. As an example, two flow field measurements taken at least two integral
time scales apart are considered to be independent (Tennekes and Lumley 1972).

\[ \mathcal{J}_j \equiv \int_0^\infty \rho_j(\tau) d\tau \quad (2.23) \]
Chapter 3

Experimental Setup

This chapter describes the experimental setup and the data acquisition systems used at Syracuse University to investigate the wake of a three-dimensional turret. The main components of the physical test setup were the boundary layer plate, turret, and the vacuum system used to develop the suction for the active control slots. Several experimental techniques were used to characterize the experiment and to evaluate the turret flow field including hotwire anemometry, particle image velocimetry (PIV), and dynamic pressure measurements.

3.1 Low Speed Wind Tunnel

The experimental investigation was conducted in a low-speed wind tunnel facility at Syracuse University. The wind tunnel was a closed return facility with a contraction ratio of 6.25:1 and had a variable frequency drive system that was used to operate the 40 hp motor mounted in the fan housing. The test section had square cross-section of 0.61 m and length of 2.44 m. The tunnel was primarily designed for flow visualization and probe based measurements such that optical access was obtainable from all four sides of the test section. A channel running the length of the test section was inte-
grated into the top of the test section to allow for access with measurement probes which limited optical access from the top of the tunnel. An inline heat exchanger was housed in the settling chamber of the tunnel which allowed for continuous operation of the facility at velocities ranging from approximately 2 m/s to 60 m/s.

3.2 Test Model Configuration

The real-world application of turret geometries made it necessary to consider and develop more specific inflow conditions than would be required for most external flow field testing performed in a wind tunnel facility. As a surface mounted geometry, experimental investigation of the turret geometry required the development of an appropriate surface flow field in addition to the uniform freestream flow field. On an aircraft, the additional complexity of the fuselage curvature must be considered in relation the turret geometry, but for the current tests a flat boundary layer plate geometry was used as a simplified model. The coordinate system that will be used throughout this document is defined in Figure 3.1. This coordinate system was selected relative to the rear of the turret to help simplify discussions of PIV data acquired in the wake. As a result of this coordinate system selection, measurements upstream of the turret will be referenced as negative $x/D$ locations where the front of the turret is located at $x/D = -1.0$.

Figure 3.1: Definition of coordinate system.
3.2.1 Boundary layer Plate

A boundary layer plate was used to develop a uniform surface flow field for the turret geometry. The boundary layer plate was designed to span the width of the wind tunnel test section with 0.003 m tolerances on either side that were sealed using an adhesive foam weather striping. The plate had a total width of 0.603 m, length of 1.863 m, and a thickness of 0.032 m. The plate was attached to the floor of the wind tunnel using eight NACA-0012 piers distributed along the length of the plate, each with height of 0.076 m and chord length of 0.070 m. The boundary layer plate was designed using interchangeable aluminum and acrylic plates as seen in Figure 3.2 to allow for three turret placements. Based on the axis of the turret cylinder relative to the leading edge of the boundary layer plate, the three possible turret positions were $2D$, $6D$, and $10D$. For the current tests, the turret was located at the $6D$ location. The individual plates were attached to a ladder like structural system (Fig. 3.2(b)) that was manufactured from 6061-T6 aluminum and allowed for spacing between the upper and lower plates. This space was used to conceal the turret attachment points and to protect the dynamic pressure sensors and the associated wiring. The assembled plate was mounted at the rear of the test section such that trailing edge of the plate was immediately upstream of the wind tunnel diffuser.
The plate was designed to be instrumented with an array of surface pressure sensors that could be relocated to investigate various regions of the turret flow field. The pressure taps for the pressure sensors were arranged in a C-grid fashion around the turret that followed the circular contour of the turret upstream of the geometry and blended to a cartesian grid in the downstream of the turret as seen in Figure 3.3(a). For simplicity during the manufacturing process, all of the proposed pressure taps
were machined into the plates but were covered with an adhesive sheet when not in use during testing. The surface of the boundary layer plate was then coated in a flat black paint to help reduce reflections from the PIV laser. The matt paint that was used was Lefranc & Bourgeois Flashe vinyl based paint that provided excellent coverage and a good surface finish after drying. Despite having the option to move the pressure sensors, a standard array of pressure sensors were used throughout the course of testing. This array can be seen in Figure 3.3(b) where the red dots indicate the pressure sensor locations. Note that the linear arrays in the wake of the turret are located at $x/D$ equal to 0.5 and 1.0. The radial arrays around the front of the turret are located at a distance of 0.125 and 0.25 diameters from the surface of the turret cylinder.

![Diagram of pressure sensor arrays](image)

(a) Total pressure tap array

(b) Standard pressure sensor array indicated in red

Figure 3.3: Top view of boundary layer plate with C-grid pressure tap arrangement.
An aluminum elliptical leading edge was fabricated to help reduce flow separation over the leading edge of the plate (see Fig. 3.4). The ellipse had an aspect ratio of 0.54 and was faded into the flat plates downstream of the leading edge using tangent curves in SolidWorks. To initiate a uniform starting point for the turbulent boundary layer, the boundary layer was tripped using a “zig-zag turbulator” tape with thickness of 0.5 mm and width of 0.012 m. The trip device was located at $x/D = -6.0$.

![SolidWorks sketch of the boundary layer plate elliptical leading edge.](image)

**3.2.2 Turret**

The turret test article was a non-conformal turret designed using SolidWorks and manufactured using stereolithography (SLA). InterPro manufactured the test article using the Watershed XC 11122 photopolymer. The SLA manufacturing technique allowed the turret to be designed with a complex internal structure and integrated passages for the active flow control system and pressure taps. The hemispherical cap geometry was mounted on a cylindrical base with diameter of 0.152 m and height of 0.101 m. This geometry corresponded to a turret $AR$ of 1.17 and $Re_D$ of $5.5 \times 10^5$. The aperture located on the hemispherical cap had diameter of 0.070 m and could be set at varying pitch angles using the internal support system. For the work presented in what follows, the aperture was set to a fixed pitch angle, $\beta$, of $120^\circ$ with respect to
the incoming flow. Table 3.1 provides a summary of the most significant variables for the current test configuration. These values have been used throughout the document to reduce data to non-dimensional forms.

<table>
<thead>
<tr>
<th>Freestream Velocity ((U_\infty))</th>
<th>53 m/s</th>
</tr>
</thead>
<tbody>
<tr>
<td>Turret Diameter ((D))</td>
<td>0.1524 m</td>
</tr>
<tr>
<td>Turret Height ((H))</td>
<td>0.1778 m</td>
</tr>
<tr>
<td>Aperture Pitch Angle ((\beta))</td>
<td>120°</td>
</tr>
<tr>
<td>Reynolds Number ((Re_D))</td>
<td>(5.5 \times 10^5)</td>
</tr>
<tr>
<td>Aspect Ratio ((AR))</td>
<td>1.17</td>
</tr>
</tbody>
</table>

Pressure taps were distributed around the turret cylinder, cap, and aperture. Similar to the boundary layer plate, a limited number of the pressure taps were used during any given experiment and the remainder were sealed. The typical placement of sensors relative to the aperture can be seen in Figure 3.5(c). Two additional pressure sensors were located at \(y/H = 0.236\) on opposing sides of turret cylinder at 83° relative to the incoming flow. This radial location is slightly upstream of the last set of sensors in the radial array on the boundary layer plate (90°).

The aperture was surrounded by 16 independent suction slots arranged in two concentric rings as seen in Figure 3.5(a). The slots had a width of 0.5 mm and an arc length of 0.025 m. Each row of actuators contained 8 independently addressable slots to allow for a variety of forcing configurations depending upon the location of the turret aperture. For the current tests with a fixed, rear facing aperture, only the seven leading slots were used for active flow control (see Fig. 3.5(b)) and the remaining slots were sealed. Although the slots were independently addressable, the seven slots in use were operated using the same driving signals and back pressure with the goal of maintaining consistent forcing across the array of suction slots. Two Parker fail-closed solenoid valves with 1/4 inch NPT female connections (model number 04F20C3110A3F4C80) were used to generate the unsteady suction. Back pressure
for the suction system was generated using two Welch Duo-Seal 1405 vacuum pumps, each with a maximum flow rate of 0.091 m$^3$/min. The pumps were rated to an ultimate pressure of $1 \times 10^{-4}$ Torr and were used to evacuate a 0.176 m$^3$ reservoir which was used to help maintain a constant back pressure during extended test runs.

Blockage is known to affect aerodynamic measurements in wind tunnels (Rae and Pope 1984) and this was a concern with the current test configuration. Total blockage was calculated to be 16.1% of the test section area for the entire test setup including the boundary layer plate, turret, boundary layer plate piers, and the cylindrical shield.
used for protecting the wiring leaving the turret. The turret was the only item that caused blockage in the area of investigation above the boundary layer plate and the turret blockage was calculated to be 8.0% of the reduced test section area.

### 3.2.3 Suction Slot Characterization

The suction slots were characterized using the hotwire system discussed in Section 3.3.2. The focus of this study was to compare the output of the seven active suction slots and to develop a better understanding of the time dependent behavior of the suction slots, both for extended steady suction and for cyclic control at varying duty cycles. Duty cycle percentages will be used extensively in this document and the definition used is provided in Equation 3.1 where $t_{\text{valve}}$ is the time of a total cycle $(1/f_{\text{valve}})$ and $t_1$ and $t_2$ are not necessarily equal. Note that for DC = 0% there will be no control signal (baseline) and for DC = 100% the control signal will always be on (steady open-loop control).

$$DC \ [\%] = \left( \frac{t_1}{t_{\text{valve}}} \right) \times 100 \tag{3.1}$$

Figure 3.6: Graphical representation of square wave variables for duty cycle definition.
In what follows, the suction velocities will be normalized using the jet momentum coefficient, $C_\mu$. The purpose of the jet momentum coefficient is to normalize the jet momentum to that of the freestream momentum using appropriate densities, velocities, and dimensions. There are several definitions of the jet momentum coefficient used in the literature, but for the current document $C_\mu$ will be defined as seen in Equation 3.2 where $A_J$ is the area of the suction slot and $A_o$ is the planform area of the turret (i.e. the area of a circle with diameter $D$).

$$C_\mu \equiv \frac{\rho_J U_J^2 A_J}{\rho_\infty U_\infty^2 A_o} \quad (3.2)$$

**Slot comparison**

The individual suction slots were characterized to develop a better understanding of the suction distribution around the leading edge of the turret aperture. For this study, data were acquired at a rate of 20 kHz and low-pass filtered at 9.07 kHz. The value of the low-pass filter was automatically set by the PXI-4472 DAQ card at 45.35% of the sampling rate. Five independent data sets were acquired, each 25 seconds in length with the suction slots operating for 24 seconds. The statistics presented here are for the final 23 seconds of acquisition after the suction velocity had stabilized. Figure 3.7 provides a definition of the slot numbering system and Table 3.2 compares the calculated performance statistics. The combined $C_\mu$ for all of the slots was $3.4 \times 10^{-4}$.

**Velocity profiles**

A more detailed analysis of actuator slot 4 was performed to gain a better understanding of the time dependent nature of the actuation as well as to determine the affect of duty cycle modulation. Figure 3.8 shows the change in average $C_\mu$ that
resulted from changing the duty cycle of the driving signal to the valves. These data were averaged from 23 seconds of data acquired using the same sampling conditions discussed previously. From Figure 3.8, one finds that $C_\mu$ has a nearly linear variation with duty cycle for range of 20% to 80%. Driving the valves outside of this range was found to cause the valves to stutter such that a uniform signal could not be achieved for longer sampling times. For cases where closed-loop control was performed, the controller was constrained to this duty cycle limit.

In order to acquire PIV and pressure data in an efficient manner, it was necessary to sample for relatively long periods of time. To acquire the desired 50 PIV snapshots per block at a sampling rate of 4.0 Hz, the suction slots were operated for 21 seconds.

---

Table 3.2: Comparison of suction slot performance.

<table>
<thead>
<tr>
<th>Slot</th>
<th>Mean Velocity [m/s]</th>
<th>Fluctuating RMS [m/s]</th>
<th>$C_\mu$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>11.2</td>
<td>0.53</td>
<td>$3.1 \times 10^{-5}$</td>
</tr>
<tr>
<td>2</td>
<td>14.4</td>
<td>0.68</td>
<td>$5.1 \times 10^{-5}$</td>
</tr>
<tr>
<td>3</td>
<td>12.4</td>
<td>0.51</td>
<td>$3.8 \times 10^{-5}$</td>
</tr>
<tr>
<td>4</td>
<td>16.8</td>
<td>0.75</td>
<td>$6.9 \times 10^{-5}$</td>
</tr>
<tr>
<td>5</td>
<td>16.0</td>
<td>0.65</td>
<td>$6.3 \times 10^{-5}$</td>
</tr>
<tr>
<td>6</td>
<td>11.6</td>
<td>0.47</td>
<td>$3.3 \times 10^{-5}$</td>
</tr>
<tr>
<td>7</td>
<td>14.9</td>
<td>0.60</td>
<td>$5.4 \times 10^{-5}$</td>
</tr>
</tbody>
</table>
to allow the flow field to stabilize for several seconds before acquiring PIV data. Figure 3.9 shows a typical time trace of $C_\mu$ as a function of time, here for a 23 second run. As expected, $C_\mu$ decreases with time as the back pressure in the tank increases due to the limited flow rate of the vacuum pumps. This decrease was found to be approximately 25% of the maximum velocity over the 23 second run.

Finally, the impulse response of the system was tested by operating the valves at 0.25 Hz with a 50% duty cycle. Figure 3.10(a) shows a time series of $C_\mu$ for opening the valves and Figure 3.10(b) shows a time series of $C_\mu$ for closing the valves. In Figure 3.10, the time axes have been adjusted to highlight the event of interest. Here it can be seen that it took approximately 0.7 seconds to reach peak suction after opening the valve although the suction velocity reached 90% of the maximum in less than 0.02 seconds. Similarly, it took approximately 0.03 seconds for the system to settle after closing the valves. Note that $C_\mu$ will not reach an absolute value of zero.
as a result of the hotwire system calibration and response at very low velocities. In Figure 3.10(b), one finds oscillatory behavior as the valves cycle off. One possible explanation for these oscillations is an effective pressure resonance in the system that results from the valves closing rapidly which is commonly referred to as hammering. There is also the possibility of rectification errors in the hotwire measurements as the velocity oscillates near zero velocity.

### 3.3 Measurement and Data Acquisition Systems

#### 3.3.1 National Instruments PXI System

A National Instruments, Real-Time PXI system (Fig. 3.11) was used for acquisition of pressure and hotwire data, generation of the driving signals for the valves and PIV
trigger signals, and for performing closed-loop control. A PXI-1052 chassis housed a Real-Time, PXI-8196 controller with LabVIEW v8.0. For the hotwire measurements, a single PXI-4472 card was used with 8 independent channels and 24-bit resolution. AC measurements were acquired using four SCXI-1531 accelerometer cards, each with 8 independent channels. The analog to digital conversion for the SCXI cards was performed using a PXI-6259 M-Series card with 16-bit resolution and up to 1.25 Megasamples per second for a single channel. Both the PXI-4472 and SCXI-1531 cards were designed to provide software configurable IEPE conditioning which was used for the PCB pressure transducers. Analog outputs were generated using a PXI-6733 card in conjunction with a TB-2705 connector block. The PXI-6733 had 8 analog outputs and 16-bit resolution.

The Real-Time PXI system was operated remotely using a Windows XP PC with LabVIEW v8.0 and Measurement & Automation Explorer v4.0.2.3002. LabVIEW virtual instruments (VIs) were created on the PC based system and then transferred to the Real-Time system prior to acquisition using a Gigabit ethernet connection. The use of the independent Real-Time system allowed for more precise data I/O
timing as the system was not constrained by the Windows operating system timing requirements. Data were stored in ASCII formatted text files that were saved on the PXI system during data acquisition and later transferred to the PC system for post-processing and long-term storage.

### 3.3.2 Hot-Wire Anemometry

A Dantec 56C01 constant temperature anemometry (CTA) system with a Dantec 56C16 bridge was used to acquire hot-wire data that were used to characterize the suction slots and boundary layer profile. The output voltage from the CTA bridge was sampled using the National Instruments PXI system discussed previously. Calibration was performed by placing the hot-wire adjacent to a pitot tube in the wind tunnel test section and running the tunnel through a range of velocities. The hot-wire output voltage was calibrated against the tunnel velocity measured with the pitot tube and a 4th order polynomial curve fit was used to determine the calibration coefficients. Curve fitting was performed using intrinsic functions in Microsoft Excel. The polynomial used for calibration can be seen in Equation 3.3 and a typical curve fit result from Excel can be seen in Figure 3.12.
\[ u = aV^4 + bV^3 + cV^2 + dV + e \] (3.3)

Figure 3.12: Example of hot-wire curve fit from Excel.

### 3.3.3 Dynamic Pressure Transducers

PCB Piezotronics dynamics pressure sensors were used to acquire dynamic pressure on the turret and boundary layer plate. The 28 103B01 ICP pressure sensors were mounted using adhesive rings provided by PCB. The sensors had a useful frequency range of approximately 5 Hz to 13 kHz and a sensitivity of 217.5 mV/kPa. The SCXI cards in the PXI system discussed previously provided the required excitation voltage as well as signal conditioning. Pressure data were acquired at 11 kHz (limited by the capabilities of the PXI system) and low-pass filtered at 5 kHz to prevent aliasing.
As a test for homogeneity in the test section and for continuity testing of the pressure sensors, an array of pressure sensors were aligned in two rows across the boundary layer plate without the turret in place. The sensor placements can be seen in Figure 3.13 with the rows located at $x/D$ of 0.50 and 0.75. Data were acquired at 23 kHz with a 10 kHz anti-aliasing filter and are presented here as block averaged, power spectral density functions taken from 1000 blocks. The mathematical background for these calculations can be found in Section 2.4 of this document. Studying the spectra for the pressure array (Fig. 3.14), there are differences between the individual pressure sensors, but all of the sensors spanning the tunnel have similar spectral characteristics. The spectra presented here do not distinguish between the individual sensors, but it was found that the variations between the pressure sensors could not be attributed to the sensor location in the tunnel. In other words, the boundary layer appeared to be relatively homogenous across the span of the tunnel.

![Figure 3.13: Pressure sensor array for preliminary testing without turret.](image-url)
Three different PIV setups were used throughout the course of testing with three different objectives for each setup. The basic equipment used in all three setups were the cameras, frame-grabber cards, laser, seeding system and traverses. The cameras (up to four depending upon the setup) were 1.3 Megapixel (1024×1280), 12-bit HiSense PIV/PLIV cameras configured to use Nikon Nikkor lenses. The HiSense cameras were capable of firing at up to 4.3 Hz, but were only used to acquire data at 4.0 Hz for the current set of tests. National Instrument PCI-1424 frame grabber cards were used to interface with the cameras. A New Wave Gemini Nd:YAG laser with maximum output of 200 mJ/pulse and peak firing rate of 15 Hz was used for particle illumination. Particles were generated using a Dantec Dynamics Laskin nozzle with a working fluid of olive oil. The traverse system used was a standard Dantec light weight traverse which was setup in a 2D configuration to allow for movement of the PIV system in the $x$-$y$ plane. When both PIV and pressure data were acquired, the systems were synchronized in time by outputting the PIV trigger signal from the
laser Q-Switch to an SCXI channel on the PXI system. This signal allowed for precise
determination of the PIV acquisition timing with respect to the pressure acquisition
timing.

The PIV system was calibrated using standard Dantec calibration plates. For
two-component calibrations, a single level target was used with solid dots defining
the grid and dimension of 200 × 200 mm. Stereoscopic calibrations were carried out
using a 270 × 190 mm multi-level target with the two levels being offset by 4 mm.
The multi-level target used translucent dots to define the grid that could be back
lit to improve contrast. For both calibration techniques, an imaging model fit was
performed using a direct linear transform method. During processing of the vector
fields, this calibration was applied for image dewarping as well as to define a common
reference origin when multiple cameras were used. For two-component calibrations,
measurement of a scale factor was also required which was achieved by taking an
image of a ruler and mapping the physical dimensions to pixel space.

PIV snapshots were analyzed using the typical single-image/dual-frame cross-
correlation technique. Adaptive correlations were used with decreasing interrogation
areas starting at 128 × 128 pixel windows for the initial pass and reducing to 32 × 32
pixel for the final pass. A total of five passes were made with 50% overlap which
resulted in vector fields of approximately 80 × 63 vectors with some variations for
the different configurations. Interrogation window offset was performed using central
differences and the interrogation areas near the image map boundaries were free to
extend outside of the active image map. Local validation of the vectors was performed
using an iterative, moving average of 3 × 3 vectors with an acceptance factor of 0.1.
Due to background noise in the images, specifically in the stereoscopic data, image
preprocessing was also required for some data sets. To increase the signal to noise
ratio, a mean intensity field was calculated for the data set in question and subtracted
from each individual snapshot to reduce background noise. Adaptive correlations were then performed on these preprocessed images.

Two-Component Setup for Inflow Condition Investigation

Two-component measurements were made in the $x$-$y$ plane upstream of the turret to help characterize the boundary layer. For these measurements, two cameras were configured with 60mm Micro-Nikkor lenses (maximum aperture of f/2.8) such that the two inspection regions overlapped slightly. With this configuration, the total inspection region was nearly double that of a single camera configuration. The system was operated using the Dantec software Dynamic Studio v3.20.89 on a Windows XP PC. National Instruments NI-MAX v4.7 was installed to interface with the PCI cards and timing was handled with a National Instruments PCI-6602 linked with a Dantec 80N77 timing box.

Two-Component Setup for Turret Center-Plane Investigation

PIV measurements were taken along the center plane of the turret in the $x$-$y$ plane as seen in Figure 3.15. This setup used an older Dantec system configured with a FlowMap Hub running Windows NT 4.0 and the FlowMap software v4.17. To interface with the FlowMap Hub, a Windows XP PC running FlowManager v4.71 was configured via network connection to operate as a host system. Two cameras were configured with 28mm Nikkor lenses (maximum aperture of f/2.8) and were placed in a vertically stacked arrangement (Fig. 3.16(b)) to acquire a larger measurement window. Note that the camera on the bottom in Figure 3.16(b) is set at a slight upward angle to minimize laser light reflections from the boundary layer plate. The laser and cameras were attached to a 1D traverse (seen atop the test section in Figure 3.16(a)) such that the measurement plane could be shifted along the $x$-axis to
increase the total inspection region. In Figure 3.15, the red and blue regions indicate the two respective cameras view fields and the darker regions indicate regions of overlap. Flow fields for this set of measurements were stitched together during post-processing to create a single window.

Figure 3.15: Two-component PIV measurement window along turret center-plane.

Stereoscopic Setup for Turret Wake and Inflow Investigations

Stereoscopic PIV flow field measurements were made in the wake of the turret parallel to the boundary layer plate in the $x$-$z$ plane as seen in Figure 3.17 where flow is from...
left to right. For this configuration, the four cameras were arranged in pairs (Fig. 3.18) with 28mm lenses to obtain two overlapping stereoscopic measurement windows. As the measurement plane of interest was not parallel to the camera lenses, the use of Scheimpflug camera mounts was required. The single Dynamic Studio computer system discussed previously was incapable of holding four PCI frame grabber cards therefore requiring a secondary computer system to be configured as an “Acquisition Agent”. The secondary system was built using Dynamic Studio and the host system was configured to recognize and operate the two cameras connected to the secondary system over a network connection. With this configuration, images from the four cameras could be acquired simultaneously using the single New Wave laser.

![Diagram](image.png)

(a) Location in x-y plane  
(b) Location in x-z plane

Figure 3.17: Example stereoscopic PIV measurement plane.
3.4 Uncertainty Analysis

As discussed in Chapter 2, mean statistics will be used extensively throughout this document and having an understanding of the error associated with these measurements is important. The four measurement configurations of interest are the hotwire characterization of the actuators, the hotwire characterization of the boundary layer, the pressure measurements, and the PIV measurements. Each of these measurements have been averaged from a finite number of samples, and the standard error of the mean is given in Equation 3.4 (Taylor 1982).

\[
\sigma_y = \frac{\sigma_x}{\sqrt{N}}
\]  

(3.4)

Using this definition, the standard error of the mean will vary for measurements where the standard deviation, \(\sigma_x\), is spatially dependent such as in the hotwire and
PIV measurements. For the values presented in Table 3.3, the typical standard deviation values were selected as the freestream value for the boundary layer and PIV measurements. Note that the standard error was calculated for the block averaged values in the case of the pressure spectra and actuator characterization.

<table>
<thead>
<tr>
<th></th>
<th>Samples (N)</th>
<th>$1/\sqrt{N}$</th>
<th>Typical $\sigma_x$</th>
<th>Typical $\sigma_\pi$</th>
</tr>
</thead>
<tbody>
<tr>
<td>HW - Actuators</td>
<td>5</td>
<td>0.45</td>
<td>0.06 m/s</td>
<td>$\pm 2.8 \times 10^{-2}$ m/s</td>
</tr>
<tr>
<td>HW - Boundary Layer</td>
<td>10000</td>
<td>0.010</td>
<td>0.10 m/s</td>
<td>$\pm 1.0 \times 10^{-3}$ m/s</td>
</tr>
<tr>
<td>Pressure Spectra</td>
<td>5200</td>
<td>0.014</td>
<td>0.02 Hz/PSI$^2$</td>
<td>$\pm 2.8 \times 10^{-4}$ Hz/PSI$^2$</td>
</tr>
<tr>
<td>PIV</td>
<td>500</td>
<td>0.045</td>
<td>1.12 m/s</td>
<td>$\pm 5.0 \times 10^{-2}$ m/s</td>
</tr>
</tbody>
</table>

Errors in the instantaneous PIV snapshots were also considered and a detailed discussion of those errors is presented in Appendix A. In general, the error for an instantaneous velocity vector was found to be on the order of 5% although this value is dependent upon several factors that can vary significantly throughout the flow field.

### 3.5 Test Matrix

The following section outlines the test matrix for the current experiments. Each of the following sections has a table that indicates an array of PIV interrogation windows and control configurations. Although these tables are intended to provide a guide to the PIV data that were acquired, it is important to note that corresponding pressure data were simultaneously sampled for each of the cases with the exception of the inflow conditions. Hotwire data are not included in this test matrix, but were used to characterize the suction slots and the boundary layer profile.
3.5.1 Inflow Conditions

Inflow conditions were acquired to check for homogeneity in the upstream flow field as well as to investigate any influence the control had on the upstream flow field. These data can be used not only to help characterize the current experiment, but also for helping establish boundary conditions for computational investigations. Two different PIV configurations, discussed in Section 3.3.4, were used for the inflow characterization and are presented here for two-component center-plane data (Table 3.4) and stereoscopic data parallel to the boundary layer plate (Table 3.5).

Table 3.4: Center-plane PIV inflow measurement test matrix.

<table>
<thead>
<tr>
<th>No Control</th>
<th>x/D=5.00</th>
<th>x/D=4.31</th>
<th>x/D=3.67</th>
</tr>
</thead>
<tbody>
<tr>
<td>y/H=0.14</td>
<td>x</td>
<td>x</td>
<td>x</td>
</tr>
</tbody>
</table>

Table 3.5: Stereoscopic PIV inflow measurement test matrix.

<table>
<thead>
<tr>
<th>No Control</th>
<th>Steady Suction</th>
</tr>
</thead>
<tbody>
<tr>
<td>y/H=0.08</td>
<td>x/D=5.00</td>
</tr>
<tr>
<td>y/H=0.17</td>
<td>x/D=4.28</td>
</tr>
<tr>
<td>y/H=0.28</td>
<td>x/D=3.67</td>
</tr>
<tr>
<td>y/H=0.39</td>
<td>x/D=3.67</td>
</tr>
<tr>
<td>y/H=0.51</td>
<td>x/D=3.67</td>
</tr>
</tbody>
</table>

3.5.2 Center Plane PIV Measurements

PIV measurements were made along the center plane of the turret in the $x$-$y$ plane as discussed in Section 3.3.4. Baseline and steady suction data were acquired for three overlapping windows along the $x$-axis. The test matrix is seen below.

Table 3.6: Center-plane PIV measurement test matrix.

<table>
<thead>
<tr>
<th>No Control</th>
<th>Steady Suction</th>
</tr>
</thead>
<tbody>
<tr>
<td>x/D=0.31</td>
<td>x/D=1.13</td>
</tr>
<tr>
<td>y/H=0.46</td>
<td>x</td>
</tr>
<tr>
<td>y/H=1.17</td>
<td>x</td>
</tr>
</tbody>
</table>
3.5.3 Stereoscopic PIV Wake Measurements

The final set of experiments performed were stereoscopic PIV measurements in the turret wake in the $x$-$z$ plane. Four different control configurations were investigated including baseline (no control), steady open-loop control (DC=100%), unsteady open-loop control (DC=60%), and simple closed-loop control (SCLC). For each configuration, an array of planes along both the $x$ and $y$ axes were investigated as presented in Table 3.7 below.

<table>
<thead>
<tr>
<th>y/H</th>
<th>x/D=0.60</th>
<th>x/D=1.32</th>
<th>x/D=0.60</th>
<th>x/D=1.32</th>
<th>x/D=0.60</th>
<th>x/D=1.32</th>
<th>x/D=0.60</th>
<th>x/D=1.32</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.14</td>
<td>x</td>
<td>x</td>
<td>x</td>
<td>x</td>
<td>x</td>
<td>x</td>
<td>x</td>
<td>x</td>
</tr>
<tr>
<td>0.17</td>
<td>x</td>
<td>x</td>
<td>x</td>
<td>x</td>
<td>x</td>
<td>x</td>
<td>x</td>
<td>x</td>
</tr>
<tr>
<td>0.20</td>
<td>x</td>
<td>x</td>
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Table 3.7: Stereoscopic PIV wake measurement test matrix.
Chapter 4

Experimental Results

This chapter presents the experimental results acquired using the hotwire system, PIV system, and surface pressure transducers. Throughout this chapter, the PIV data are presented as spatial contour plots which are useful for making qualitative assessments of the wake flow field. Appendix C presents profiles taken from the various contour plots and provides a more quantitative description of the turret wake. The data presented in Appendix C include profiles of $U$, $V$, $W$, and $k$ at various spatial locations and multiple heights above the boundary layer plate for the baseline and three control cases.

4.1 Boundary Layer Characterization

The boundary layer profile was evaluated at $x/D = -1.0$ without the turret in place using the single wire hotwire probe discussed previously. The mean boundary layer profile is seen in Figure 4.1. As expected, the boundary layer profile has the typical characteristics of a turbulent boundary layer with high gradients near the wall. Defining the boundary layer thickness as the location where $U/U_\infty = 0.99$, the boundary layer was found to have height, $\delta_{99}/H$, of 0.10.
Figure 4.2 shows the profile of the turbulence intensity ($u' u'$) through the boundary layer. As is typical for turbulent boundary layers, turbulence intensities are highest slightly above the wall with the lowest turbulence levels in the freestream flow. The fluctuating RMS value in the freestream is approximately 0.3 m/s.

PIV measurements were also made upstream of the turret to evaluate the boundary layer evolution and to check for homogeneity in the freestream flow at $x/D = -5.0$. These measurements included two-component measurements of the boundary layer and stereoscopic measurements made at multiple heights parallel to the boundary layer plate. The PIV results are presented in detail in Appendix B.

Figure 4.1: Boundary layer profile at $x/D = -1.0$ without turret in place.
4.2 Wake Flow Field - Baseline

Measurements in the turret wake were taken without the suction system activated to establish an understanding of the baseline flow field. In what follows, the tests in which no control is applied will be referred to as either the baseline or no control cases.

4.2.1 PIV Measurements

A general overview of the baseline wake structure can be seen in Figure 4.3. In Figure 4.3, contours are of the mean $U$ velocity component and streamlines of $U$ and $V$ velocity were calculated using Tecplot 360. The freestream flow is from left to right, and the shaded grey region indicates the approximate location of the turret geometry.
in the measurement window. The separation region that naturally develops from the turret aperture at this pitch angle ($\beta = 120^\circ$) can be seen clearly. This separation over the turret aperture has been the focus of the aero-optic studies as discussed previously. Due to similarities in the experimental setup between the current studies and the work performed by Wallace et al. (Wallace et al. 2010; Wallace et al. 2012), this figure can be compared directly to Figure 1.5(a) in the aperture region. With the extended field of view, one can now see the extent of the separated flow and wake deficit region. At $x/D = 1.5$, the $U$ component of velocity has recovered to approximately 85% of the freestream flow velocity below $y/D = 1.0$ with the exception of near the wall where the flow did not recover as quickly. Note that the $y$-axis has been normalized using the diameter of the turret such that the axes would scale properly for this figure, but will typically be normalized using the turret height.

Figure 4.3: Mean contours of $U$ velocity component along center plane with no control.

Figures 4.4–4.11 show the ensemble averaged results of the stereoscopic PIV measurements for eight planes parallel to the boundary layer plate. 500 snapshots were
used for the ensemble averages at each measurement location. All three components of velocity were measured and are presented here as independent contour plots, each normalized by the freestream velocity of 53 m/s. In the stereoscopic results, the freestream flow is from top to bottom, and the two PIV interrogation windows have been stitched together at $z/D = 0.0$ to build a single image. The scales of the axes have been normalized by dimensions related to the turret, either the turret diameter in the $x$-$z$ plane or the turret height along the $y$-axis. Note that the velocity contours are different for each of the velocity components but are consistent across the different measurement planes so that direct comparisons can be made. Recalling the coordinate system defined previously, the edges of the turret cylinder are located at $z/D = \pm 0.5$ and a dotted line has been plotted to indicate the center plane corresponding to the data presented in Figure 4.3. There are regions of the flow field where data were not able to be calculated correctly as a result of the overlap regions of the camera inspection regions used for the stereoscopic reconstruction. One such region can be seen in the lower right hand corner of Figure 4.4(a) where there is a discontinuity in the contours. This region has been included such that the rest of the window could be extended, but the results in this region are non-physical.

Studying the streamwise velocity contours at $y/H = 0.169$ (Fig. 4.4(a)), the three-dimensional nature of the wake flow can be seen clearly. Strong shear layers are seen to develop from the turret cylinder at $z/D = \pm 0.5$ which define the boundary of the turret wake region. Two lobes developed in the near wake region that extend beyond the field of view with a region of increased velocity along the center plane that appears to separate the two lobes and can be seen more clearly further downstream of the turret. There is a slight asymmetry in the flow field with a larger negative velocity being seen in the left recirculating region at this height above the plate. Additionally, it appears as though the center plane calculated from the PIV calibration may be
slightly shifted relative to the physical geometry \((z/D \approx 0.03)\). These details can be seen more clearly in Appendix C where the wake profiles are presented.

Two deficit regions are seen along \(z/D = \pm 1.0\) that can be attributed to the horseshoe vortex being shed into the wake. Consistent with the results of Eckerle and Langston (1987) who showed that the horseshoe vortex separates from the geometry at 90° relative to the inflow, the horseshoe vortex measured here has spread a notable distance away from the edge of the cylinder and does not immediately mix with the shear layer that develops from the cylindrical base. As the vortex expands, it begins to mix with the turret shear layer near the edge of the measurement window. The window of inspection does not extend far enough into the wake to determine if these two structures merge completely.

Studying Figures 4.4(b) and 4.4(c), very little structure can be seen as a result of the scaling of the contours. In order to make direct comparisons with the planes located farther above the boundary layer plate, the contours were held constant such that very little structure is seen close to the plate. In Figure 4.4(b), a very weak downwash velocity can be seen along the center plane which is consistent with Figure 4.3. In Figure 4.4(c), it is difficult to define flow structures in the wake region of the turret, but small velocity contours associated with the horseshoe vortex can be seen at \(z/D = \pm 1.0\). As expected, the mean \(W\) velocities associated with the horseshoe vortex are of opposite sign on opposing sides of the center plane.

As the field of view is shifted away from the boundary layer plate, the region of negative \(U\) velocity is seen to decrease in size. In general, the wake deficit is seen to recover more rapidly in the higher planes which is consistent with the results seen in Figure 4.3. Additionally, the wake region is seen to expand away from the center line in the lower planes, but collapses in the higher planes which can be seen clearly by comparing Figures 4.4(a) and 4.11(a). It is also important to note that the horseshoe
vortex structures that could be seen at $y/H = 0.169$ are not present in the higher planes.

Studying the $V$ component of velocity for the baseline case, there is very little downwash in the planes studied. In planes where a downwash structures are seen (such as in Figure 4.8(b)), the negative velocities are contained in a fairly small strip along the center plane that extends beyond the field of inspection. For the $W$ component of velocity, as the inspection plane is shifted away from the boundary layer plate, two independent regions of strong cross-stream velocity are seen. These regions have opposite sign on opposing sides of the center plane and are, for the most part, symmetric. Throughout all of the planes where the strong $W$ velocities are seen, the regions of negative velocity (blue) are typically smaller in size but higher in velocity magnitude, again indicating a slight asymmetry in the wake flow field.
Figure 4.4: Mean velocity contours at $y/H = 0.169$ with no control.
Figure 4.5: Mean velocity contours at $y/H = 0.225$ with no control.
Figure 4.6: Mean velocity contours at $y/H = 0.281$ with no control.
Figure 4.7: Mean velocity contours at $y/H = 0.337$ with no control.
Figure 4.8: Mean velocity contours at $y/H = 0.394$ with no control.
Figure 4.9: Mean velocity contours at $y/H = 0.450$ with no control.
Figure 4.10: Mean velocity contours at $y/H = 0.506$ with no control.
Figure 4.11: Mean velocity contours at $y/H = 0.562$ with no control.
Figures 4.12–4.17 show Reynolds stress contours at several planes above the turret where Figures 4.12–4.14 are the Reynolds normal stresses and Figures 4.15–4.17 are the Reynolds shear stresses. For these figures, the contour levels have been matched for either the normal or shear stresses such that the normal stress figures have the same contours and the shear stress figures have the same contours. Considering the \( \overline{u'u'} \) normal stress in Figure 4.12, it can be seen that the regions of highest intensity are in the shear layers where the freestream and wake flow interact. This trend is consistent for each of the planes presented. As the interrogation plane is moved away from the boundary layer plate, the magnitude of the \( \overline{u'u'} \) normal stress decreases and at the highest plane (Fig. 4.12(c)), the region where \( \overline{u'u'} \) is strongest is contained to approximately 0.6 diameters downstream of the turret. As this Reynolds stress component provides some insight into the shear layer interaction between the freestream and wake flow fields, these contour plots can be used to help determine the size and location of the wake relative to the turret.

Figure 4.13 shows contours of the \( \overline{v'v'} \) Reynolds normal stress. From Figure 4.13, it can be seen that this component of the normal stress is contained within the bounds of the wake as determined by the \( \overline{u'u'} \) Reynolds stress. In the freestream flow, turbulent fluctuations along the \( y \)-axis should be very small. This is reflected in Figure 4.13 as regions of \( \overline{v'v'} \) nearly equal to zero. In the wake region where there are strong velocity fluctuations in all three components of velocity, the levels of \( \overline{v'v'} \) are much higher. Compared to the \( \overline{u'u'} \) Reynolds stress, the \( \overline{v'v'} \) component is much weaker in the shear layers. Along the center plane where the mean \( V \) velocity component was seen to be the largest, the \( \overline{v'v'} \) Reynolds stress component is also seen to have the greatest magnitude. It is interesting to note that \( \overline{u'u'} \) and \( \overline{v'v'} \) have similar intensities levels such that one component is not dominant. This again provides insight into the highly three-dimensional nature of the wake flow field.

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The final normal stress component to be considered is the $w'w'$ component seen in Figure 4.14. Compared to the $u'u'$ and $v'v'$ components, $w'w'$ is consistently higher in magnitude throughout the wake and has well defined structure in both the shear layer and the wake regions. Similar to the $u'u'$ component, the $w'w'$ normal stress is seen to exist in a large, expanding region near the boundary layer plate. As the interrogation plane is shifted away from the boundary layer plate, the region of strong $w'w'$ is seen to decrease in size, but the relative intensity remains constant. At the highest plane (Fig. 4.14(c)), $w'w'$ is the dominant Reynolds stress component.

The Reynolds shear stresses are seen in Figures 4.15–4.17. The contours for these three figures are consistent such that comparisons of magnitude can be made. The first thing to note is that, unlike the normal stress components, the shear stress contours have been extended to include both positive and negative levels as the definition of the shear stress components does not necessarily result in positive values. Second, the maximum contour range for the normal stresses was set to 0.25 where the maximum contour levels for the shear stresses was seen to be contained between $\pm 0.05$ indicating that the normal stress components are larger in magnitude. Studying Figures 4.15–4.17, the Reynolds shear stresses are seen to be quite small with structures existing only in the shear layer for $u'v'$ and weak structures existing along the center plane for the $v'w'$ component of shear stress. Conversely, very strong structures are seen to exist in the shear layers for the $u'w'$ component with structures of opposite sign on opposing sides of the center plane.

Comparing the Reynolds normal stress and shear stress results, one finds that the shear stress indicate regions of overlap for the normal stress components. Physically, this provides insight into the three dimensional nature of the turbulence. In regions with strong shear stress, the turbulent fluctuations exist in both components of velocity as opposed to regions where the shear stresses are small which indicates that
there is very little fluctuation in one or both velocity components. Using the $u'w'$ shear stress as an example, although high levels of turbulent fluctuations were seen throughout the wake in $w$ component of velocity, the only regions where fluctuations were seen in both the $u$ and $w$ velocity components were the shear layers. As such, high levels of shear stress are seen in the shear layers, but not in the wake region where the $u$ fluctuations were small.

Finally, the turbulent kinetic energy is calculated as the linear superposition of the Reynolds normal stresses as defined in Equation 2.6 and is plotted as contours in Figure 4.18. As the combination of the normal stress components, the values of $k$ are seen to be almost zero in the freestream flow as expected. In the wake region, the levels of $k$ are fairly uniform across the wake with very little differentiation between the shear layers and actual wake region. Similar to previous results, as the interrogation window is moved away from the boundary layer plate, the turbulence intensity levels are seen to be contained in a much smaller region. One flow structure that is notably absent in the turbulent intensity levels is the horseshoe vortex that could be seen in the $U$ mean velocity near the boundary layer plate (Fig. 4.4(a)). With the proper scaling of the TKE contour plots, the horseshoe vortex structure can be seen, but the values of $k$ in the horseshoe vortex region are an order of magnitude smaller than those in the shear layer between the wake and freestream flow.
Figure 4.12: Contours of normalized $u'u'$ with no control.
Figure 4.13: Contours of normalized $\overline{v'v'}$ with no control.
Figure 4.14: Contours of normalized $w'w'$ with no control.
Figure 4.15: Contours of normalized $\overline{u'v'}$ with no control.
Figure 4.16: Contours of normalized $\overline{u'w'}$ with no control.
Figure 4.17: Contours of normalized $\overline{v'w'}$ with no control.
Figure 4.18: Turbulent kinetic energy contours with no control.
The time averaged, two-point, cross-correlation coefficients, $R_{ij}$, are presented as contour plots in Figures 4.19–4.22 where the fixed reference point used for the two-point correlation is indicated as the crossing point of the two dashed lines. Note that the correlations have not been normalized and will therefore have units of $(\text{m/s})^2$. As discussed in Section 2.3.2, the cross-correlation value can be integrated along a path to calculate the integral length scale of the turbulence ($L_{ij}$). The length scales presented in the following sections are integrated in the positive direction of either $x$ or $z$. To account for the regions of anti-correlation, the integrations were performed using the absolute value of $R_{ij}$ as the anti-correlation still indicates coherent structures in the flow field.

Considering $R_{uu}$ in the shear layer at $(z/D, x/D) = (0.46, 0.30)$ (Fig. 4.19), the two-point correlations are seen to be strongest near the boundary layer plate where the turbulent fluctuations were strongest. The region of strong correlation is seen to extend further along the $x$-axis when compared to the $z$-axis indicating that $L_{uu}^x$ will be larger than $L_{uu}^z$ which is consistent with the length scales presented in Table 4.1. As the interrogation plane is shifted farther away from the boundary layer plate, the region of strong correlation decreases in size and is not seen in the highest plane as the reference point is outside of the turret wake. At the highest plane, the correlation has decreased significantly, but the local $R_{ii}$ value at the reference point acts to normalize the length scale calculations. As such the $L_{uu}^z$ is seen to have the highest value in the highest of the three planes studied where a strong correlation was not seen. Comparing the $L_{uu}^x$ values, the length scale is seen to be largest in the lowest plane and decreases in the higher planes as expected. It is important to note that the correlation of $R_{uu}$ extends beyond the field of view and therefore the calculated value of $L_{uu}^x$ may underestimate the actual length scale.

Comparing $R_{uu}$ to $R_{vv}$ for the $(0.46, 0.30)$ reference point, it can be seen that the
correlations are much weaker for $R_{vv}$. This result is not unexpected when considering the relative strength of the turbulent fluctuations of the $v$ component of velocity as seen previously. Comparing the length scale values presented in Table 4.1, the values of $L_{xvv}$ and $L_{zvv}$ in the lowest plane are nearly the same. At the highest plane, $L_{xvv}$ and $L_{zvv}$ are similar to the values of $L_{xuu}$ and $L_{zuu}$.

Finally, considering the $R_{ww}$ two-point correlation (Fig. 4.21), the correlation magnitude is seen to be comparable to that of the $R_{uu}$ correlations, but the correlation region does have a different structure. Similar to the $R_{uu}$ correlations, the magnitude of the $R_{ww}$ correlation is seen to decrease as the plane of interest is shifted away from the boundary layer plate. At the highest plane, there is almost no correlation as a result of the reference point being outside of the wake. Studying the structure of the correlation at the plane closest to the boundary layer plate (Fig. 4.21(a)), it can be seen that the positive correlation region extends a greater distance along the $z$-axis than along the $x$-axis. Using the absolute value to calculate the length scale, however, will cause $L_{xww}$ to be larger than $L_{zww}$ which is consistent with the values presented in Table 4.1. The region of anti-correlation downstream of the initial correlation was not seen in the previous correlations and this structure indicates that there is an oscillatory structure in the flow field. Considering the location of the reference point (bounded in the shear layer), the oscillatory behavior of $R_{ww}$ indicates that the shear layer is, in effect, flapping back and forth along the $z$-axis. The $R_{uu}$ and $R_{vv}$ correlations were seen to be positive at all points indicating that no oscillations existed in the $x$ and $y$ directions. Therefore, it can be inferred that the oscillation of
the shear layer is constrained to the $z$ direction. It is important to note that, in the lowest plane (Fig. 4.21(a)), the correlation extends beyond the extent of the window such that the calculated value of $L_{uw}^x$ may underestimate the actual length scale.

Two-point correlation coefficients were also calculated closer to the center of the wake at $(z/D, x/D) = (0.12, 0.30)$ and are seen in Figure 4.22. The three correlation coefficients presented are seen to be more consistent in magnitude at this location, but the $R_{vv}$ and $R_{ww}$ correlations do have larger magnitude than $R_{uu}$ which is consistent with the turbulence intensity levels seen previously. Due to the strong fluctuations in the $v$ velocity component, the $R_{vv}$ correlations are seen to be much stronger near the center plane than in the shear layer. There is an anti-correlation across the center plane for the $R_{vv}$ coefficient, again indicating that there is an oscillatory wake structure in this region, but the extent of these oscillations is not clear as a result of the interrogation window limits. Comparing the length scales presented in Table 4.2, $L_{uu}^z$ and $L_{vv}^x$ are of comparable size as are $L_{uu}^x$ and $L_{vv}^z$. For the plane presented, $L_{uw}^z$ and $L_{ww}^z$ are the largest length scales with $L_{uw}^x$ being the larger of the two.

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Figure 4.19: $R_{uu}$ contours with no control at $(z/D, x/D) = (0.46, 0.30)$.  

(a) $y/H = 0.169$

(b) $y/H = 0.337$

(c) $y/H = 0.506$
Figure 4.20: $R_{vv}$ contours with no control at $(z/D, x/D) = (0.46, 0.30)$. 

(a) $y/H = 0.169$

(b) $y/H = 0.337$

(c) $y/H = 0.506$
Figure 4.21: $R_{\omega\omega}$ contours with no control at $(z/D, x/D) = (0.46, 0.30)$. 

(a) $y/H = 0.169$

(b) $y/H = 0.337$

(c) $y/H = 0.506$
Figure 4.22: Baseline correlation contours at \((z/D, x/D, y/H) = (0.12, 0.30, 0.34)\) in the wake region.
4.2.2 Pressure Measurements

Pressure data were acquired simultaneously with the PIV data and are presented here as auto-spectral density functions (ASDF) calculated using the methods described in Section 2.4. The following data were block averaged from 5200 independent blocks and are presented at three streamwise locations which are located at $x/D$ equal to -1.25, -0.50, and 1.00. In the figures that follow where spectra are plotted for multiple sensors, the spectra are color coded and displayed in the order seen in Figure 4.23 with the bottom sensor being plotted on the left.

The auto-spectral density function was calculated for the pressure sensor upstream of the turret ($x/D = -1.25$) located along the plane of symmetry (Fig. 4.24). The ASDF shows the behavior of a typical turbulent spectra with high energy content in the lowest frequencies and a roll-off in energy at the highest frequencies. There are no dominant frequencies seen to exist, and the ASDF generally behaves similar to that of spectra seen in homogeneous turbulence.

Four pressure sensors located at $90^\circ$ relative to the inflow as seen in Figure 4.23(b) were evaluated and the corresponding auto-spectral density functions are seen in Figure 4.25. Compared to the ASDF at the front of the turret, the spectral behavior is seen to change significantly. The smooth roll-off that was seen in upstream sensor (Fig. 4.24) is not seen at this location where a more drastic decrease in energy is seen starting at $St_D = 0.20$. The low-frequency peak that is seen is corresponds to $St_D = 0.03$ and exists at the same frequency for each of the sensors. This peak was seen in all of the sensors in Figure 3.14 when the turret was not in place indicating that this frequency is not unique to the turret flow field. Comparing the auto-spectral density functions at the lowest frequencies, the sensors at $x/D = -0.50$ are seen to have values that are nearly an order of magnitude smaller than that of the upstream sensor at $x/D = -1.25$.
Figure 4.23: Pressure sensor reference locations.
Figure 4.24: Auto-spectral density function at $x/D = -1.25$ with no control.

Figure 4.25: Auto-spectral density functions at $x/D = -0.50$ with no control.
Studying the auto-spectral density function plots for the baseline case at $x/D = 1.00$ (Fig. 4.26), one finds that there are peak frequencies that exist in spectra, but these peaks are not seen across the entire wake. Considering the results presented previously for the velocity correlations, the peak frequencies that do exist are likely the result of the oscillatory behavior in the shear layers at the interface of the freestream and wake flow field. As discussed previously, these results are consistent with the work of Kawamura et al. (1984) where no Von Kármán vortex structures were seen for finite cylinders with $AR$ less than 6. The peak frequencies that do exist correspond to a Strouhal number of approximately 0.35 which is consistent with the work by Gordeyev et al. (2006). The peak at $St_D = 0.35$ was not seen at $x/D = -0.50$ indicating that the oscillations did not exist upstream of the wake separation point.

Figure 4.26: Auto-spectral density functions at $x/D = 1.00$ with no control.

Cross-correlation functions, $\rho_{ij}$, were calculated using the same sensor array at $x/D = 1.00$ discussed above. Here, the reference sensor for the cross-correlation
was the sensor in black on the left side of the array and all of the cross-correlations have been normalized using the $\rho_{ij}(0)$ value of the auto-correlation. Note that, by definition, the maximum correlation for the reference sensor is equal to 1.0. As is typical for a turbulent flow field (Fig. 4.27), an oscillation around the zero value for the auto-correlation exists for the reference sensor. As the sensor used for the cross-correlation is shifted across the wake, the magnitude of the correlation function is seen to decrease dramatically with there being effectively no correlation with the sensor opposite of the center plane. It is interesting to note that the three sensors in the middle do indicate some levels of correlation, but a consistent trend is not seen as two are initially anti-correlated and the other initially has a positive correlation. Had Von Kármán vortex shedding existed in this flow field, stronger correlations across the center plane would be seen.

Figure 4.27: Cross-correlation functions at $x/D = 1.00$ with no control.

Cross-correlation functions were also calculated in the streamwise direction to de-
velop an understanding of how the coherent structures in the flow field were convected downstream. The sensors used for the streamwise correlation are at an \( x/D \) location of 0.50 at \( z/D \) of 0.50 and 1.00 as seen in Figure 4.28. Studying the streamwise cross-

![Figure 4.28: Sensor placement for streamwise cross-correlation function at \( x/D = 0.50 \).](image)

correlation for the baseline case (Fig. 4.29), an initial anti-correlation is seen between the sensors with a recovering maximum positive correlation at a time delay, \( \tau \), of 3.6 milliseconds. This peak correlation corresponds to a convection velocity of 21.4 m/s. Normalizing by the freestream velocity of 53 m/s, this value is reduces to 0.40 which is consistent with the mean streamwise velocity, \( U \), seen in the PIV data in the shear layer region where the two sensors of interest were located. The behavior and strength of the streamwise cross-correlation indicates that there are strong coherent structures convecting through the shear layers for the baseline case.

### 4.3 Wake Flow Field - Open-Loop: DC=100%

To develop an understanding of the active flow control limitations, the suction system was allowed to operate at peak suction levels without cycling of the valves (i.e. steady suction). As such, the results that are presented below are related strictly to the controlled flow field and will not have a time dependent active control input as with the unsteady open-loop and simple closed-loop control configurations where the valves
were cycled at 15 Hz. The steady suction control input is a form of open-loop control, here with a fixed duty cycle of 100% as defined in Section 3.2.3. The average combined $C_\mu$ for the seven suction slots for this control input was found to be $3.4 \times 10^{-4}$ with an average suction slot velocity of 13.9 m/s.

4.3.1 PIV Measurements

Figure 4.30 compares the center plane PIV data for the baseline and DC=100% configurations. Note that Figure 4.30(a) is the same as Figure 4.3 and has been included here to help make comparisons. The most obvious change with the application of the suction is the elimination of the separation region over the turret aperture. In effect, the separation point has been shifted from the leading edge of the aperture to the trailing edge of the aperture. A wake deficit is still seen to extend downstream of the turret, but is more locally contained near the boundary layer plate with the flow.
recovering to freestream conditions more rapidly at higher $y/D$ values. The large scale effects of the control over the aperture are consistent with the work by Wallace et al. (2010). Note that in Figure 4.30 the $y$-axis has again been scaled by the turret diameter as opposed to the turret height.

Studying the stereoscopic data in the wake of the turret, differences between the baseline and DC=100% cases are also apparent. For the $U$ component of velocity at $y/H = 0.169$ (Fig. 4.31), the turret wake is seen to expand more rapidly with the application of control. Additionally, the two lobes that develop in the wake of the turret are more well defined as the flow appears to recover to the freestream velocity more rapidly along the center plane. The horseshoe vortex structures are also seen in the lowest plane, but start to merge into the wake shear layers closer to the turret as a result of the increased spreading of the wake.

As the interrogation plane is shifted away from the boundary layer plate (Figs. 4.32 & 4.33), the difference in spreading rate becomes more apparent. At $y/H = 0.337$, the wake extends beyond $z/D = \pm 0.9$ in the DC=100% case compared to being contained between $z/D = \pm 0.8$ for the baseline case. The region of negative velocity is also seen to be reduced in the DC=100% case such that the flow appears to be recovering to the freestream conditions more rapidly. At the highest plane presented here (Fig. 4.33), both flow fields are seen to have a collapsing wake structure, but the wake in the DC=100% case is contained closer to the turret.
Figure 4.30: Comparison of mean contours of $U$ velocity component along the center plane for the baseline and DC=100% test cases.
Figure 4.31: Mean contours of $U$ velocity component at $y/H = 0.169$. 

(a) No Control

(b) DC=100%
Figure 4.32: Mean contours of $U$ velocity component at $y/H = 0.337$.
Figure 4.33: Mean contours of $U$ velocity component at $y/H = 0.506$. 

(a) No Control

(b) DC=100%
Significant differences between the baseline and DC=100% cases are seen throughout all of the planes when considering the $V$ component of velocity (Figs. 4.34–4.36). In each plane, the magnitude of the $V$ velocity component is seen to be significantly higher with the application of steady suction. In both the baseline and DC=100% cases, the regions of downwash (negative velocity) are seen to be contained in a fairly small strip along the center plane of the turret. This helps to provide insight into why the Von Kármán vortex shedding does not develop as the downwash from the cap acts, in a mean sense, as a fluidic splitter. As the interrogation plane is shifted away from the boundary layer plate, the downwash velocity is seen to collapse toward the turret geometry such that the downwash velocity is contained in a much smaller region. The general increase in the downwash velocity also helps to explain why the wake is seen to expand more rapidly in the lower planes. As the additional mass and energy from the cap flow is directed towards the boundary layer plate, the wake region must expand more rapidly in the lower planes to help balance this influx.

Comparing the contours of the $W$ velocity component for the baseline and DC=100% cases (Figs. 4.37–4.39), similarities in the flow structure are again present but the effects of the control can be seen clearly. In the planes farther away from the boundary layer plate, the two independent regions of cross-stream velocity are again seen, but the magnitude of the cross-stream velocity has increased. Additionally, the regions of cross-stream velocity have shifted closer to the turret geometry. As a result, the local velocity magnitude in the near wake region has increased most likely resulting in a corresponding decrease in the static pressure. Direct measurements of the static pressure in the wake were not made to confirm this.
Figure 4.34: Mean contours of $V$ velocity component at $y/H = 0.169$. 

(a) No Control

(b) DC=100%

Figure 4.34: Mean contours of $V$ velocity component at $y/H = 0.169$. 

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Figure 4.35: Mean contours of $V$ velocity component at $y/H = 0.337$.
Figure 4.36: Mean contours of $V$ velocity component at $y/H = 0.506$. 

(a) No Control 

(b) DC=100%

Figure 4.36: Mean contours of $V$ velocity component at $y/H = 0.506$. 

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Figure 4.37: Mean contours of $W$ velocity component at $y/H = 0.169$. 

(a) No Control

(b) DC=100%

Figure 4.37: Mean contours of $W$ velocity component at $y/H = 0.169$. 

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Figure 4.38: Mean contours of $W$ velocity component at $y/H = 0.337$. 

(a) No Control

(b) DC=100%
Figure 4.39: Mean contours of $W$ velocity component at $y/H = 0.506$. 

(a) No Control

(b) DC=100%

Figure 4.39: Mean contours of $W$ velocity component at $y/H = 0.506$. 

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Turbulent kinetic energy comparisons between the baseline and DC=100% flow fields are seen in Figures 4.40–4.42. Perhaps the most noteworthy result in these figures is that the relative magnitude of $k$ has neither increased nor decreased any significant amount with the application of control. At the lowest plane presented, the turbulent kinetic energy is seen to cover a much larger area as a result of the more rapid expansion of the wake. As the interrogation plane is shifted away from the boundary layer, a bifurcation of the turbulent kinetic energy is seen to develop. At the highest plane presented here, the turbulent kinetic energy is split into two independent lobes that are contained close to the turret geometry as opposed to a single structure that extends a significant distance into the wake as was seen in the baseline case.
Figure 4.40: Contours of turbulent kinetic energy at $y/H = 0.169$. 

(a) No Control

(b) DC=100%
Figure 4.41: Contours of turbulent kinetic energy at $y/H = 0.337$. 

(a) No Control

(b) DC=100%
Figure 4.42: Contours of turbulent kinetic energy at $y/H = 0.506$. 
Finally, comparisons of the two-point correlation functions at two different $z/D$ locations are presented in Figures 4.43 and 4.44. In general, very little change is seen in the structure of the correlations between the baseline and DC=100% cases. However, there is a general increase in the strength and spatial extent of the correlations which indicates that the integral length scales have increased with the application of control. Considering the calculated length scales in the shear layer (Table 4.3), all of the length scales, with the exception of $L_{ww}^x$, have increased with the application of control.

In the shear layer (Fig. 4.43), the $R_{uu}$ correlation has become the dominant correlation when compared to the $R_{vv}$ and $R_{ww}$ correlations. Conversely, the $R_{ww}$ correlation near the center plane seen in Figure 4.44(f) appears to have become dominant towards the center of the wake. In the baseline case, the $R_{vv}$ correlation was seen to have higher magnitude than the $R_{ww}$ correlation, but with the application of control, the magnitude of the $R_{ww}$ correlation has dramatically increased where the $R_{vv}$ correlation has remained relatively constant. Considering the length scales in the shear layer $L_{uu}^z$ and $L_{ww}^z$ have again increased fairly significantly with the application of control. The remaining four length scales have all decreased slightly.

<table>
<thead>
<tr>
<th>Shear Layer</th>
<th>$L_{uu}^x/D$</th>
<th>$L_{uu}^z/D$</th>
<th>$L_{vv}^x/D$</th>
<th>$L_{vv}^z/D$</th>
<th>$L_{ww}^x/D$</th>
<th>$L_{ww}^z/D$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Wake Region</td>
<td>0.18</td>
<td>0.24</td>
<td>0.14</td>
<td>0.07</td>
<td>0.20</td>
<td>0.21</td>
</tr>
</tbody>
</table>

Table 4.3: Length scales for DC=100% configuration at $y/H = 0.337$. 
Figure 4.43: Comparison of baseline and DC=100% correlation contours at \((z/D, x/D, y/H) = (0.46, 0.30, 0.34)\) in the shear layer.
Figure 4.44: Comparison of baseline and DC=100% correlation contours at \((z/D, x/D, y/H) = (0.12, 0.30, 0.34)\) in the wake region.
4.3.2 Pressure Measurements

Figure 4.45 compares the auto-spectral density functions for the upstream pressure sensor located at \( x/D = -1.25 \). In Figure 4.45, the baseline case is plotted in red and the DC=100\% case is plotted in black. Despite the significant changes in the wake of the turret, the time dependent behavior upstream of the turret is effectively the same for both the baseline and steady open-loop control cases.

![Auto-spectral density function comparison at \( x/D = -1.25 \).](image)

Figure 4.45: Auto-spectral density function comparison at \( x/D = -1.25 \).

Significant differences are seen in at \( x/D = -0.50 \) location when comparing the steady open-loop control auto-spectral density functions (Fig. 4.46) to the baseline results at the same location (Fig. 4.25). A strong peak frequency is now seen in each of the four plots that corresponds to \( St_D \) of 0.10. The sharp roll-off is again seen to start at \( St_D \) of 0.20, but the energy throughout the steady open-loop control auto-spectra density functions are seen to be higher and those of the baseline case.
The auto-spectral density functions at $x/D = 1.00$ for the DC=100% control case are plotted in Figure 4.47 for the pressure array seen in Figure 4.23(c). The most notable change in the pressure spectra between the baseline and DC=100% case is that peak frequencies are now seen across the span of the wake. However, the peak frequency that was seen in the baseline case ($St_D = 0.35$) is different from the first peak frequency seen in the red and green sensors which corresponds to a Strouhal number of 0.10. The peak seen at the center of the wake (i.e. the blue sensor) corresponds to a Strouhal number of 0.25. The turbulence intensities at this point appear to be fairly low such that the flow structure responsible this peak frequency at the center of the wake is not clear at this time.

The cross-correlation functions were again calculated for the sensors at $x/D = 1.0$ using the black sensor on the left as the reference (Fig. 4.48). Comparing the DC=100% case to the baseline case, the results are not significantly different. The
initial oscillations in the auto-correlation of the reference sensor do not fully cross the zero value which, when performing the integration to determine an integral time scale, will result in a smaller value. Shifting across the wake, the cross-correlation with the sensor closest to the reference sensor has significantly decreased in magnitude with the application of the control. Small correlation levels are seen in the remaining sensors with no apparent trend emerging across the wake. It is interesting to note that the correlations with the sensors opposite of the center plane have increased slightly, but the magnitude of the correlations are still too small to be considered significant.

Figure 4.49 compares the streamwise cross-correlation function for the baseline (red) and steady open-loop control cases (black). In general, the magnitude of the correlation has decreased indicating that the convecting structures are not as organized. The DC=100% case is seen to have a peak correlation at nearly the same time delay indicating a similar convection velocity for the two cases compared here.
Figure 4.48: Cross-correlation functions at $x/D = 1.00$ with DC=100%.

Figure 4.49: Cross-correlation function in streamwise direction at $x/D = 0.50$ with DC=100%.
4.4 Wake Flow Field - Open-Loop: DC=60%

The test cases with the simple closed-loop control system were the next set of experiments to be performed, but the unsteady open-loop control cases will be presented first. To determine the duty cycle to be used for the unsteady open-loop control, the average duty cycle from the simple closed-loop controller was calculated. The mean duty cycle was found to be 60% and this value was used for the unsteady open-loop control cases as a fixed, cyclic input. Recalling the definition of the duty cycle seen in Section 3.2.3, a value of 60% indicates that the valves were open 60% of the time and closed for 40% which resulted in a mean $C_{\mu}$ of $3.74 \times 10^{-5}$ per slot. This is a 45% reduction in the mean $C_{\mu}$ compared to the DC=100% value of $6.91 \times 10^{-5}$ per slot. The maximum driving frequency of the valves over a wide range of duty cycle values was found to be 16 Hz. In order to prevent phase locking the PIV data (acquired at 4 Hz) to the active control input, the duty cycle frequency was reduced to 15 Hz and a random start trigger was generated in LabVIEW and used to initiate the PIV acquisition at random points in the 15 Hz control cycle. In the contour plots that follow, both the baseline and DC=100% flow fields will be presented for reference. These figures are identical to the figures presented in the previous sections, but are shown here to help simplify comparisons.

4.4.1 PIV Measurements

Figures 4.50–4.52 show contour plots of the $U$ component of velocity at various heights above the boundary layer plate. In these figures, the DC=60% flow fields are seen to more closely resemble the results of the DC=100% case. As expected, the effects of the control are slightly reduced as a result of the smaller average, active control input, but the differences between the DC=100% and DC=60% cases are fairly small. Perhaps the most significant difference between the steady and unsteady open-loop
cases is seen in the highest plane (Fig. 4.52(b)) where the wake deficit for the DC=60% case extends slightly further downstream of the turret than the wake deficit for the DC=100% case.

Studying the contour plots for the $V$ and $W$ components of velocity, the differences between the DC=100% and DC=60% case are even smaller. For the $V$ component (Fig. 4.53), the only difference is a slight decrease in the downwash velocity. Similarly for the $W$ component (Figs. 4.54 & 4.55), the flow field structure for the DC=60% case is nearly identical to that of the DC=100% case with only slight decreases in the velocity magnitudes.

Similar trends are seen in the turbulent kinetic energy plots with the exception of Figure 4.56(b) which shows very high levels of $k$ near the turret geometry when compared to the baseline and DC=100% cases at the same height. Considering the DC=100% (Fig. 4.56(c)) and simple closed-loop control (Fig. 4.74(b)) cases where this increased level of $k$ is not seen, these results are probably erroneous and a result of measurement noise in the PIV data. For the planes farther away from the boundary layer plate (Figs. 4.57 & 4.58), the turbulent kinetic energy was seen to behave similar to that of the mean flow where the DC=60% most closely resembled the DC=100% case with slightly less effect. Again, the magnitude of the turbulent kinetic energy remains relatively constant for each of the control cases.
Figure 4.50: Mean contours of $U$ velocity component at $y/H = 0.169$. 
Figure 4.51: Mean contours of $U$ velocity component at $y/H = 0.337$. 

(a) No Control

(b) DC=60%

(c) DC=100%
Figure 4.52: Mean contours of $U$ velocity component at $y/H = 0.506$. 

(a) No Control

(b) DC=60%

(c) DC=100%
Figure 4.53: Mean contours of \( V \) velocity component at \( y/H = 0.337 \).
Figure 4.54: Mean contours of $W$ velocity component at $y/H = 0.337$. 

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Figure 4.55: Mean contours of $W$ velocity component at $y/H = 0.506$. 

(a) No Control

(b) DC=60%

(c) DC=100%
Figure 4.56: Contours of turbulent kinetic energy at $y/H = 0.169$. 

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Figure 4.57: Contours of turbulent kinetic energy at $y/H = 0.337$. 

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Figure 4.58: Contours of turbulent kinetic energy at $y/H = 0.506$. 
Finally, two-point cross-correlations were performed for the DC=60% case, and again, the results were very similar to that of the DC=100% case as seen in Figures 4.59 and 4.60. In general, the magnitude and spatial extent of the correlations in the wake were seen to increase with the application of control. Similar to the DC=100% case, a significant increase in the $R_{ww}$ correlation was seen at the $(z/D, x/D, y/H) = (0.12, 0.30, 0.34)$ location near the center plane. Considering the length scales for the unsteady open-loop control case (Table 4.4), the calculated values are seen to be very similar to those of the steady open-loop control case.

Table 4.4: Length scales for DC=60% configuration at $y/H = 0.337$.

<table>
<thead>
<tr>
<th></th>
<th>$L^x_{uu}/D$</th>
<th>$L^z_{uu}/D$</th>
<th>$L^x_{vv}/D$</th>
<th>$L^z_{vv}/D$</th>
<th>$L^x_{ww}/D$</th>
<th>$L^z_{ww}/D$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Shear Layer</td>
<td>0.24</td>
<td>0.09</td>
<td>0.19</td>
<td>0.14</td>
<td>0.16</td>
<td>0.13</td>
</tr>
<tr>
<td>Wake Region</td>
<td>0.18</td>
<td>0.20</td>
<td>0.15</td>
<td>0.08</td>
<td>0.20</td>
<td>0.21</td>
</tr>
</tbody>
</table>
Figure 4.59: Comparison of baseline and DC=60% correlation contours at \((z/D, x/D, y/H) = (0.46, 0.30, 0.34)\) in the shear layer.
Figure 4.60: Comparison of baseline and DC=60% correlation contours at \((z/D, x/D, y/H) = (0.12, 0.30, 0.34)\) in the wake region.
4.4.2 Pressure Measurements

The auto-spectral density function for the unsteady open-loop control case at the upstream sensor \((x/D = -1.25)\) is seen in Figure 4.61. Again, the baseline case is plotted in red, and the DC=60% case is plotted in black. Unlike the steady open-loop control case, a slight decrease in the energy of the DC=60% ASDF is seen at the lower frequencies. Consistent with the baseline and DC=100% cases, no peak frequency exists and a smooth roll-off is seen at the higher frequencies. It is also important to note that the valve driving frequency of 15 Hz is not seen at this pressure location.

![Figure 4.61: Auto-spectral density function at \(x/D = -1.25\) with DC=60%.](image)

The auto-spectral density functions for the unsteady open-loop control case at \(x/D = -0.50\) are seen in Figure 4.62. Consistent with the results presented for the PIV data, the ASDF of the DC=60% case are seen to exhibit the same behavior as the DC=100% case although to not the same extent. Considering the peak seen at
\( St_d = 0.10 \), the peak is not as well defined, and the energy increase that was seen between the baseline and DC=100% is not as significant for the DC=60% test case. Unlike the upstream sensor, a peak is seen at the valve driving frequency of 15 Hz, but the peak is not as dominant as in the downstream auto-spectral density functions (Fig. 4.63).

![Auto-spectral density functions at \( x/D = -0.50 \) with DC=60%](image)

**Figure 4.62:** Auto-spectral density functions at \( x/D = -0.50 \) with DC=60%.

The auto-spectral density calculations for the \( x/D = 1.0 \) location (Fig. 4.63) again show similar trends to that of the DC=100% auto-spectral density functions (Fig. 4.47). One feature that is consistent across all of the pressure transducers is a sharp peak frequency at 15 Hz which corresponds to the driving frequency of the valves. Several of the spectra also show the first harmonic at 30 Hz which is common in the spectra of square waves. As these peaks were not seen in the upstream sensor, it can be concluded that the active control system is imparting a 15 Hz structure into the flow field. The peak frequencies resulting from the larger structures in the flow
field, such as those in the shear layers, show spectral content more consistent with that of the DC=100% case as opposed to the baseline case.

![Auto-spectral density functions at $x/D = 1.0$ with DC=60%](image)

Figure 4.63: Auto-spectral density functions at $x/D = 1.0$ with DC=60%.

Similarly, the cross-correlation coefficients show trends that are similar to the DC=100% case (Fig. 4.64). A relatively strong correlation is seen in the sensor closest to the reference sensor, but this correlation is not as strong as in the baseline case. Shifting across the turret wake, it is again difficult to find consistent spatial trends in the correlation coefficients similar to both of the previous cases.

Figure 4.65 compares the streamwise cross-correlation coefficient for the baseline (red) and unsteady open-loop control cases (black). Similar trends in the controlled cross-correlation function are seen when compared to the DC=100% case as the magnitude of the correlation has generally decreased with the application of control. Although not presented here, a peak is not seen at $\tau = 0.067$ seconds which would correspond to the 15 Hz driving frequency of the valves.
Figure 4.64: Cross-correlation functions at $x/D = 1.0$ with DC=60%.

Figure 4.65: Cross-correlation function in streamwise direction at $x/D = 0.50$ with DC=60%.
4.5 Wake Flow Field - Simple Closed-Loop Control

A simple, feedback, closed-loop control algorithm was developed using a single pressure sensor located on the center of the turret aperture. This controller was designed to study the effects of unsteady forcing and to determine if the active control system could be made more effective by including some knowledge of the flow state. The sensor that was selected on the turret aperture was used due to the placement relative to the aero-optics problem as well as for the high observability of the flow field response to the active control at this location. A block diagram of the control loop can be seen in Figure 4.66 where the “sliding average” block was included in the control algorithm to effectively slow the control response. The control algorithm loop operated at the sampling rate of the pressure sensors which was 11 kHz, but the signal to the valves was only updated at the valve cycling rate of 15 Hz. As such, the time dependent duty cycle setting for the valves was calculated based on a short time average of the measured pressure signal.

![Block diagram of simple closed-loop control system.](image)

Due to limitations of the valves, the duty cycle was limited to a range of 20% to 80% but the duty cycle was allowed to modulate freely within that range. Operating outside of this range resulted in inconsistent response from the valves which was undesirable. Figure 4.67 shows example time traces of the duty cycle and corresponding valve driving signals for the unsteady open-loop and simple closed-loop control configurations. Note the discrete time steps of duty cycle in the simple closed-loop
control cases (Fig. 4.67(b)) are the result of the sliding average.

Figure 4.67: Example time traces of duty cycle and valve driving signals for DC=60% and simple closed-loop control (SCLC) test runs.

4.5.1 PIV Measurements

The results from the stereoscopic PIV measurements for the simple closed-loop control case are nearly identical to those from the DC=60% case. For the $U$ component of velocity (Figs. 4.68–4.70), the closed-loop control was seen to modify the flow field
such that it closely resembled that of the DC=100% case. Note that, once again, the baseline and DC=100% contour plots seen below are identical to those in the previous sections and are presented here to help make comparisons. At the highest plane presented here (Fig. 4.70), the differences between the baseline, DC=100%, and closed-loop control cases are perhaps the most clear. The wake deficit for the closed-loop case is seen to extend farther in the wake than in the DC=100% case but does not extend as far as the wake in the baseline case. These differences help provide evidence that the closed-loop controller did not have as large of an effect on the wake flow field as applying DC=100%, but recall that the average $C_\mu$ was reduced by 45% when comparing the closed-loop and DC=100% cases. As such, similar changes to the flow field were achieved for approximately half of the control input which is a positive result when considering efficiency.

Comparing the $V$ and $W$ components of velocity (Figs. 4.71–4.73), the same trends are seen where the closed-loop control system more closely resembles that of the DC=100% case with a slight loss in effectiveness. One figure of interest is Figure 4.72(b) where a strong asymmetry is seen in the wake relative to the center plane. A similar asymmetry is seen in the DC=100% case although the variations across the center plane are not as pronounced. The cause of this asymmetry is not clear although imperfections from the SLA manufacturing process are one possible explanation.
Figure 4.68: Mean contours of $U$ velocity component at $y/H = 0.169$. 

(a) No Control

(b) Simple Closed-Loop Control

(c) DC=100%
Figure 4.69: Mean contours of $U$ velocity component at $y/H = 0.337$. 

(a) No Control

(b) Simple Closed-Loop Control

(c) DC=100%
Figure 4.70: Mean contours of $U$ velocity component at $y/H = 0.506$. 

(a) No Control

(b) Simple Closed-Loop Control

(c) DC=100%
Figure 4.71: Mean contours of $V$ velocity component at $y/H = 0.337$. 
Figure 4.72: Mean contours of $W$ velocity component at $y/H = 0.337$. 

(a) No Control

(b) Simple Closed-Loop Control

(c) DC=100%
Figure 4.73: Mean contours of $W$ velocity component at $y/H = 0.506$. 

(a) No Control

(b) Simple Closed-Loop Control

(c) DC=100%
Turbulent kinetic energy contour plots are seen in Figures 4.74–4.76. Similar to the mean flow fields, the turbulent kinetic energy for the closed-loop control case shows strong similarities to that of the DC=100% case. It is important to note that the high TKE intensities that were seen in the DC=60% case at $y/H = 0.169$ (Fig. 4.56(b)) are not seen here. Despite the unsteady forcing, the TKE intensity levels for the simple closed-loop control case are comparable to both the baseline and DC=100% cases with the general distribution of the turbulent kinetic energy more closely resembling that of the DC=100% case.

Two-point cross-correlations were performed for the simple closed-loop control case and are plotted in Figures 4.77 and 4.78 along with the baseline correlations. Similar to the DC=100% and DC=60% cases, the correlations have generally increased in magnitude and spatial extent with the most significant increase being seen in the $R_{ww}$ component at the $(z/D, x/D, y/H) = (0.12, 0.30, 0.34)$ location near the center of the wake (Fig. 4.78(f)). It is interesting to note that, in Figure 4.78(d), the $R_{vv}$ correlations downstream of the reference point have increased in magnitude when compared to the similar results for the DC=100% and DC=60% cases for $(z/D, x/D, y/H) = (0.12, 0.30, 0.34)$. The downstream correlations are still fairly weak compared the correlations nearer to the reference point however. Considering the length scales for the simple closed-loop control case (Table 4.5), the calculated values are seen to be very similar to those of both the steady and unsteady open-loop control cases.

| Table 4.5: Length scales for SCLC configuration at $y/H = 0.337$. |
|-----------------|-----------------|-----------------|-----------------|-----------------|-----------------|-----------------|-----------------|
|                 | $L_{uu}/D$      | $L_{uu}/D$      | $L_{vv}/D$      | $L_{ww}/D$      | $L_{ww}/D$      | $L_{ww}/D$      | $L_{ww}/D$      |
| Shear Layer     | 0.24            | 0.10            | 0.16            | 0.14            | 0.16            | 0.12            |                 |
| Wake Region     | 0.17            | 0.20            | 0.17            | 0.07            | 0.21            | 0.22            |                 |

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Figure 4.74: Contours of turbulent kinetic energy at $y/H = 0.169$. 

(a) No Control 

(b) Simple Closed-Loop Control 

(c) DC=100%

Figure 4.74: Contours of turbulent kinetic energy at $y/H = 0.169$. 

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Figure 4.75: Contours of turbulent kinetic energy at $y/H = 0.337$. 

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(a) No Control

(b) Simple Closed-Loop Control

(c) DC=100%

Figure 4.76: Contours of turbulent kinetic energy at $y/H = 0.506$. 
Figure 4.77: Comparison of baseline and closed-loop control correlation contours at \((z/D, x/D, y/H) = (0.46, 0.30, 0.34)\) in the shear layer.
Figure 4.78: Comparison of baseline and closed-loop control correlation contours at 

\((z/D, x/D, y/H) = (0.12, 0.30, 0.34)\) in the wake region.
4.5.2 Pressure Measurements

The auto-spectral density function for the simple closed-loop control (SCLC) case at $x/D = -1.25$ is seen in Figure 4.79. The baseline case is plotted in red, and the SCLC case is plotted in black. Similar to the DC=60% case, there is a slight decrease in energy for the SCLC case at the lower frequencies although the decrease is not as significant as was seen in the unsteady open-loop control case. No peak associated with the driving frequency of the valves is seen in the upstream spectra.

Figure 4.79: Auto-spectral density function at $x/D = -1.25$ with simple closed-loop control.

Studying the auto-spectral density functions for the SCLC case at $x/D = -0.50$ (Fig. 4.80), the results are nearly identical to the DC=60% case. A peak frequency at $St_D = 0.10$ is again seen in all of the sensors, but the peak is not as strong as in the steady open-loop control case. Small peaks are seen to exist at the driving frequency of the valves, but this is not a dominant feature in the spectra and the peak is not as well defined as for the DC=60% configuration.
Auto-spectral density functions for the sensors located at $x/D = 1.0$ are seen in Figure 4.81. Similar to the previous figures, the results here are nearly identical to those seen for the DC=60\% data (Fig. 4.63). The valve driving frequency again appears in all of the sensors as a well defined peak at 15 Hz. Similar to the DC=100\% case, an apparent shear layer frequency is seen to exist at a frequency corresponding to a Strouhal number of 0.10 in the red sensors. A peak frequency is again seen along the center plane (i.e. the blue sensor) corresponding to a Strouhal number of 0.25.

The cross-correlation coefficients for the simple closed-loop control case are presented in Figure 4.82. Similar to the previous data sets, the black sensor on the left was used as the reference sensor. The results seen for the simple closed-loop control are, for all practical purposes, identical to those of the DC=60\% cases with only slightly variations in the magnitudes of the correlations. Differences between the unsteady open-loop and closed-loop cross-correlations can only be seen when the two
Figure 4.81: Auto-spectral density functions at $x/D = 1.00$ with simple closed-loop control.

are compared directly on the same plot and even then the differences are very small. 

Figure 4.83 compares the streamwise cross-correlation functions for the baseline (red) and simple closed-loop control (black) cases. The correlation function for the SCLC case again shows similar characteristics to the two control cases presented previously with a general decrease in the correlation magnitude and a peak positive correlation at the same time delay as the baseline case. Comparing the SCLC and DC=60% cases, the correlation magnitude for the SCLC case is slightly larger, but the general characteristics of the correlation are the same.
Figure 4.82: Cross-correlation functions at $x/D = 1.0$ with simple closed-loop control.

Figure 4.83: Cross-correlation function in streamwise direction at $x/D = 0.50$ with simple closed-loop control.
4.6 TKE Budget

This appendix discusses the balance of the turbulent kinetic energy equation that was initially presented in Chapter 2. The TKE equation was seen previously as Equation 2.7, but is presented here for reference as Equation 4.1 where the turbulent kinetic energy is defined in Equation 4.2. The following sections discuss the turbulent convection (Term 1, Section 4.6.1), production (Term 3, Section 4.6.2), turbulent transport (Term 2b, Section 4.6.3), viscous transport (Term 2c, Section 4.6.4), dissipation (Term 4, Section 4.6.5), and finally presents an estimate of the dissipation found from balancing the production, transport, and convection (Section 4.6.6). Recall that experimentally measuring the pressure at all points in the flow field is very difficult and therefore the pressure transport (Term 2a) will not be discussed. Similar techniques for analyzing experimental data acquired in turbulent flow fields can be seen in the literature such as in the work by Cole and Glauser (1998).

\[
U_j \frac{\partial k}{\partial x_j} = -\frac{\partial}{\partial x_j} \left[ \frac{\rho u'_i \delta_{ij}}{2a} + \frac{u'_i u'_j}{2b} - \nu \frac{\partial k}{\partial x_j} \right] - \frac{u'_i u'_j}{2c} \frac{\partial U_i}{\partial x_j} - \nu \frac{\partial u'_i}{\partial x_i} \frac{\partial u'_i}{\partial x_j} \tag{4.1}
\]

\[
k \equiv \frac{u'_i u'_i}{2} \tag{4.2}
\]

In the figures throughout this appendix, the various terms are presented in non-dimensional form and contours have been normalized (except where noted) such that direct comparisons can be made between the different terms. For the figures presented in the following sections, 2000 snapshots were used for the data analysis. Throughout the calculations presented in the following sections, derivatives with respect to \(y\) are not included as a result of working with the planar PIV data. Due to the highly three-dimensional nature of the turret wake, strong gradients in \(y\) do exist and the
corresponding terms would most likely provide significant contributions to the various terms presented. The terms that could be calculated from the planar data still provide insight into the various TKE terms.

4.6.1 Convection

The convection term of the turbulent kinetic energy equation provides insight into the rate of change of $k$. The tensor form of the convection term is seen in Equation 4.3, the expanded form in Equation 4.4, and the terms appropriate for the current data set are seen in Equation 4.5. In Equation 4.5, the term that required differentiation in $y$ has been excluded as discussed previously.

\begin{align*}
C &= U_j \frac{\partial k}{\partial x_j} \\
&= U \frac{\partial k}{\partial x} + V \frac{\partial k}{\partial y} + W \frac{\partial k}{\partial z} \\
&\approx U \frac{\partial k}{\partial x} + W \frac{\partial k}{\partial z}
\end{align*}  

(4.3) 

(4.4) 

(4.5)

Figures 4.84–4.86 show contours of the convection term at three different heights above the boundary layer plate for the baseline case, the steady open-loop control case (DC=100%), and the simple closed-loop control case (SCLC). As expected, performing the spatial derivatives results in fairly noisy results where small disturbances that were not seen in the mean flow contours become clear in the convection plots. One example of this are the dots of high levels of convection along $x/D$ of 0.5 and 1.0. These points are the result of laser reflections from the pressure taps in the boundary layer plate affecting the instantaneous velocity measurements. Despite the increased noise, the large scale structures in the flow field are still clear.

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In the lowest plane (Fig. 4.84), the highest levels of convection are seen in the shear layers. Similarly, at the higher planes, increased levels of convection are seen in the shear layers, but regions of negative convection are also seen near the center of the wake (Figs. 4.85 & 4.86). With the application of control, the increased magnitude of convection in the center of the wake is more apparent. This is the most clear in Figures 4.86(b) and 4.86(c) where there was almost no convection in the baseline case (Fig. 4.86(a)) at this same plane. In the lower two planes investigated (Figs. 4.84 & 4.85), the convection magnitude is seen to increase in the shear layers with the application of control, especially in regions of highest shear near the turret geometry.
Figure 4.84: Mean contours of turbulent convection at $y/H = 0.169$. 

(a) No Control

(b) DC=100%

(c) SCLC
Figure 4.85: Mean contours of turbulent convection at $y/H = 0.337$. 

(a) No Control

(b) DC=100%

(c) SCLC
Figure 4.86: Mean contours of turbulent convection at $y/H = 0.506$. 

(a) No Control

(b) DC=100%

(c) SCLC
4.6.2 Production

The turbulent production term provides insight into how energy is exchanged between the mean flow and the turbulent fluctuations. As indicated by the name, this is how turbulent energy is generated throughout the flow field. Equation 4.6 shows the definition of the production term in tensor form, Equation 4.7 shows the expanded equation, and Equation 4.8 shows the approximation of the full production equation used for the current calculations.

\[
P = -u'_i u'_j \frac{\partial U_i}{\partial x_j} \quad (4.6)
\]

\[
= - \left[ \overline{u'w'} \left( \frac{\partial U}{\partial x} \right) + \overline{u'v'} \left( \frac{\partial U}{\partial y} \right) + \overline{u'w'} \left( \frac{\partial U}{\partial z} \right) \right]
\]

\[
- \left[ \overline{w'w'} \left( \frac{\partial V}{\partial x} \right) + \overline{v'v'} \left( \frac{\partial V}{\partial y} \right) + \overline{v'w'} \left( \frac{\partial V}{\partial z} \right) \right]
\]

\[
- \left[ \overline{u'w'} \left( \frac{\partial W}{\partial x} \right) + \overline{v'w'} \left( \frac{\partial W}{\partial y} \right) + \overline{w'w'} \left( \frac{\partial W}{\partial z} \right) \right] \quad (4.7)
\]

\[
\approx - \left[ \overline{w'u'} \left( \frac{\partial U}{\partial x} \right) + \overline{w'v'} \left( \frac{\partial V}{\partial y} \right) + \overline{w'w'} \left( \frac{\partial W}{\partial z} \right) \right]
\]

\[
- \left[ \overline{u'w'} \left( \frac{\partial U}{\partial z} \right) + \overline{v'w'} \left( \frac{\partial V}{\partial z} \right) \right] \quad (4.8)
\]

Studying Figure 4.87, the highest level of production in the lowest plane was seen to exist in the shear layers. As the production term contains spatial derivatives of the mean flow variables, low levels of production in the center of the wake are expected in the lowest plane where significant gradients were only seen in the shear layers. The dots of increased production magnitude seen at \( x/D \) of 0.5 and 1.0 are again noise resulting from laser reflections off of the pressure taps in the boundary layer plate. As the interrogation plane is shifted away from the boundary layer plate (Figs. 4.88
& 4.89), more production is seen to exist in the center of the wake, especially for the 
cases with control applied where the mean velocities in the center of the wake were 
seen to increase.

In all of the cases presented here, there is an asymmetry in the production contours 
with the shear layer production on the left of the center plane extending a greater 
distance into the wake. Conversely, along the center plane in the wake flow field, the 
right side of the wake is seen to contribute more strongly to the turbulent production. 
This is perhaps the most clear in the baseline case (Figs. 4.87(a) & 4.88(a)) where 
the shear layer production is greater in the shear layer on the left side of the center 
plane (+z/D) in the lowest plane, but the wake production is seen to be greater on 
the right side of the center plane (−z/D) in the interrogation planes higher above the 
boundary layer plate.
Figure 4.87: Mean contours of turbulent production at $y/H = 0.169$. 

(a) No Control

(b) DC=100%

(c) SCLC
Figure 4.88: Mean contours of turbulent productions at $y/H = 0.337$. 

(a) No Control

(b) DC=100%

(c) SCLC
Figure 4.89: Mean contours of turbulent production at $y/H = 0.506$. 

(a) No Control

(b) DC=100%

(c) SCLC
4.6.3 Turbulent Transport

The turbulent transport term is used to describe how turbulent energy is transferred throughout the flow as a result of the turbulent fluctuations. Equation 4.9 is the tensor formulation of the turbulent transport term, Equation 4.10 is the full expansion, and Equation 4.11 is the reduced turbulent transport appropriate for the current data set. As was seen in the previous sections, the derivatives with respect to $y$ are not included in Equation 4.11.

\[
\tau_k = -\frac{\partial}{\partial x_j} \left[ \frac{u' v' w'}{2} \right] \quad (4.9)
\]

\[
= -\frac{1}{2} \left[ \left( \frac{\partial u' v' w'}{\partial x} \right) + \left( \frac{\partial u' v' v'}{\partial y} \right) + \left( \frac{\partial u' w' w'}{\partial z} \right) \right] \\
- \frac{1}{2} \left[ \left( \frac{\partial u' v' w'}{\partial x} \right) + \left( \frac{\partial v' v' v'}{\partial y} \right) + \left( \frac{\partial v' v' w'}{\partial z} \right) \right] \\
- \frac{1}{2} \left[ \left( \frac{\partial u' v' w'}{\partial x} \right) + \left( \frac{\partial v' v' w'}{\partial y} \right) + \left( \frac{\partial w' w' w'}{\partial z} \right) \right] \quad (4.10)
\]

\[
\approx -\frac{1}{2} \left[ \left( \frac{\partial u' v' w'}{\partial x} \right) + \left( \frac{\partial v' v' w'}{\partial z} \right) \right] \\
- \frac{1}{2} \left[ \left( \frac{\partial u' v' w'}{\partial x} \right) + \left( \frac{\partial w' w' w'}{\partial z} \right) \right] \quad (4.11)
\]

Figures 4.90–4.92 show contours of the turbulent transport. As this term is the derivative of the triple product of the fluctuations, it is more susceptible to measurement noise. Structures in the flow field are still evident, however. Studying the turbulent transport in the lowest plane studied (Fig. 4.90), there are strong concentrations of turbulent transport in the shear layers. Recalling Figure 4.16 where strong fluctuations were seen in the shear layers for the $u'w'$ Reynolds shear stress, these results are consistent with the Reynolds stress results where most of the energy
was seen in the shear layers. Comparing the baseline case (Fig. 4.90(a)) to the controlled cases (Figs. 4.90(b) & 4.90(c)), the magnitude of the turbulent transport has increased towards the center of the wake. The magnitude of the turbulent transport is not as large in the higher planes although the baseline case does exhibit relatively large turbulent transport levels along the center plane at the highest interrogation plane studied (Fig. 4.92(a)). Similar results are not seen in the controlled cases where only small levels of turbulent transport are seen close to the turret geometry.
Figure 4.90: Mean contours of turbulent transport at $y/H = 0.169$. 
Figure 4.91: Mean contours of turbulent transport at $y/H = 0.337$. 

(a) No Control

(b) DC=100%

(c) SCLC
Figure 4.92: Mean contours of turbulent transport at $y/H = 0.506$. 

(a) No Control

(b) DC=100%

(c) SCLC
4.6.4 Viscous Transport

The viscous transport term again describes how turbulent energy is transferred throughout the flow, but now as a result of viscous stresses as opposed to turbulent fluctuations. Equation 4.12 shows the viscous transport term in tensor notation, Equation 4.13 shows the full expansion, and Equation 4.14 shows the terms that could be calculated from the current data set.

\[
\tau_v = -\frac{\partial}{\partial x_j} \left[ -\nu \frac{\partial k}{\partial x_j} \right] \quad (4.12)
\]

\[
= \nu \left[ \left( \frac{\partial^2 u' u'}{\partial x^2} \right) + \left( \frac{\partial^2 u' u'}{\partial y^2} \right) + \left( \frac{\partial^2 u' u'}{\partial z^2} \right) \right] \\
+ \nu \left[ \left( \frac{\partial^2 v' v'}{\partial x^2} \right) + \left( \frac{\partial^2 v' v'}{\partial y^2} \right) + \left( \frac{\partial^2 v' v'}{\partial z^2} \right) \right] \\
+ \nu \left[ \left( \frac{\partial^2 w' w'}{\partial x^2} \right) + \left( \frac{\partial^2 w' w'}{\partial y^2} \right) + \left( \frac{\partial^2 w' w'}{\partial z^2} \right) \right] \quad (4.13)
\]

\[
\approx \nu \left[ \left( \frac{\partial^2 u' u'}{\partial x^2} \right) + \left( \frac{\partial^2 u' u'}{\partial z^2} \right) + \left( \frac{\partial^2 v' v'}{\partial x^2} \right) + \left( \frac{\partial^2 v' v'}{\partial z^2} \right) \right] \\
+ \nu \left[ \left( \frac{\partial^2 w' w'}{\partial x^2} \right) + \left( \frac{\partial^2 w' w'}{\partial z^2} \right) \right] \quad (4.14)
\]

Performing a scaling analysis on the TKE equation, the viscous transport can be shown to be \( \mathcal{O} \sim (u^3/l)Re_i^{-1} \) where \( l \) is the turbulent length scale and \( Re_i \) is the turbulent Reynolds number which is estimated using values appropriate to the largest scales of the turbulence as opposed to values appropriate to the bulk flow. For the current data set, the length scale was estimated using the two-point cross-correlations in the center of the wake as a guide to be \( D/4 \) and the turbulent velocity in the wake was estimated from the RMS velocities in the wake to be \( U_\infty/2 \). The resulting turbulent Reynolds number was found to be \( 6.9 \times 10^4 \). Each of the other terms in
the TKE equation are $O \sim (u^3/l)$ which indicates that the viscous transport will be negligible compared to the other terms in the TKE balance (Tennekes and Lumley 1972).

Figure 4.93 shows the viscous transport at $y/H=0.337$ for the baseline case. Figure 4.93(a) has contours that have been normalized to match the figures shown previously and, as expected, there are no structures that can be seen which confirms that the viscous transport term does not significantly contribute to the transport of turbulent energy. Figure 4.93(b) shows contours that have been scaled to highlight the structures in a typical viscous transport plot. Studying this figure, one finds that there are no real structures apparent in the viscous transport other than to say most of the energy is located in the wake region. Although plots are not shown for the steady open-loop and simple closed-loop control configurations, similar results were seen for each of the control cases at the three planes studied.
Figure 4.93: Mean contours of viscous transport at $y/H = 0.337$ with no control.
4.6.5 Dissipation

The dissipation term describes how turbulent energy is reduced in the TKE budget. Equation 4.15 describes the turbulent dissipation in tensor form, Equation 4.16 is the expanded form, and Equation 4.17 shows the terms that could be calculated from the current data set.

\[ \varepsilon = -\nu \frac{\partial u_i'}{\partial x_j} \frac{\partial u_i'}{\partial x_j} \]  
\[ = -\nu \left[ \left( \frac{\partial u'}{\partial x} \right)^2 + \left( \frac{\partial u'}{\partial y} \right)^2 + \left( \frac{\partial u'}{\partial z} \right)^2 \right] \]  
\[ -\nu \left[ \left( \frac{\partial v'}{\partial x} \right)^2 + \left( \frac{\partial v'}{\partial y} \right)^2 + \left( \frac{\partial v'}{\partial z} \right)^2 \right] \]  
\[ -\nu \left[ \left( \frac{\partial w'}{\partial x} \right)^2 + \left( \frac{\partial w'}{\partial y} \right)^2 + \left( \frac{\partial w'}{\partial z} \right)^2 \right] \]  
\[ \approx -\nu \left[ \left( \frac{\partial u'}{\partial x} \right)^2 + \left( \frac{\partial u'}{\partial z} \right)^2 + \left( \frac{\partial v'}{\partial x} \right)^2 + \left( \frac{\partial v'}{\partial z} \right)^2 \right] \]  
\[ -\nu \left( \frac{\partial w'}{\partial x} \right)^2 + \left( \frac{\partial w'}{\partial z} \right)^2 \]  
(4.17)

In turbulent flow fields, dissipation is known to act at the smallest scales of turbulence. These scales are known as the Komolgorov scales and the Komolgorov length scale \( \eta \) can be estimated from the turbulent Reynolds number and turbulent length scale as seen in Equation 4.18.

\[ \eta = \frac{l}{Re_i^{3/4}} \]  
(4.18)

Using the values discussed previously, the Komolgorov length scale for the wake flow was estimated to be \( 9 \times 10^{-6} \) m which indicates that the stereoscopic PIV grid resolu-
tion of approximately $3 \times 10^{-3}$ m will be insufficient to resolve the scales of turbulent dissipation properly. As such, the derivatives in Equations 4.15–4.17 will act to smooth the gradients of the fluctuating velocity and therefore underestimate the turbulent dissipation. Calculating the dissipation using the definition in Equation 4.17 can still provide insight into general behavior of the turbulent dissipation and to draw comparisons between the various control configurations.

An additional approximation of the dissipation that can be used is found in the text by Hinze (1975). For isotropic turbulence (independent of coordinate rotations or reflections), the spatial derivatives are not unique such that the total dissipation can be calculated from a reduced number of derivatives. The isotropic estimation of dissipation is seen in Equation 4.19.

$$
\varepsilon_{\text{ISO}} = -6 \nu \left[ \left( \frac{\partial u'}{\partial x} \right)^2 + \left( \frac{\partial u'}{\partial z} \right)^2 + \left( \frac{\partial u'}{\partial z} \right) \left( \frac{\partial w'}{\partial x} \right) \right]
$$

(4.19)

As seen in the two-point cross-correlations, the turbulence in the wake flow field is neither isotropic nor homogeneous, but this isotropic approximation can still be used to help provide insight into the dissipation levels without being able to perform differentiation in $y$.

Figure 4.94 shows contours of the dissipation calculated using the definition found in Equation 4.17 and Figures 4.95–4.97 show contours calculated using the isotropic assumption. Note that the contours in Figures 4.94–4.97 are not normalized to the same contour levels as the previous figures in this appendix. Comparing Figures 4.94 and 4.95 which plot contours of the two different calculation methods at the same interrogation plane, one finds that the basic structure of the dissipation field is consistent between the two calculations but the isotropic approximation increases the estimate of the dissipation magnitude. As expected, the levels of dissipation are significantly underestimated using either technique.
Throughout all of the dissipation figures, the dissipation is generally seen to be increased in the wake region with the highest dissipation magnitude existing near the turret. In the lowest interrogation plane, the application of control is seen to expand the wake deficit region and the dissipation is seen to exhibit the same characteristics (Figs. 4.95(b) & 4.95(c)). In the higher planes where the wake region was seen to decrease in size with the application of control, the region of increased dissipation also decreases in size (Figs. 4.97(b) & 4.97(c))
Figure 4.94: Mean contours of turbulent dissipation at $y/H = 0.169$. 

(a) No Control

(b) DC=100%

(c) SCLC
Figure 4.95: Mean contours of isotropic dissipation at $y/H = 0.169$. 

(a) No Control

(b) DC=100%

(c) SCLC
Figure 4.96: Mean contours of isotropic dissipation at $y/H = 0.337$. 

(a) No Control

(b) DC=100%

(c) SCLC
Figure 4.97: Mean contours of isotropic dissipation at $y/H = 0.506$. 
4.6.6 Dissipation from TKE Balance

As seen throughout this section (Section 4.6), the dissipation and pressure transport are the only terms that could not be calculated properly from the current data set. Using the remaining terms in the TKE equation, it is possible to estimate these two terms by balancing the TKE equation. For the sake of this balance, a dissipation estimate term that combines the pressure transport and dissipation terms will be used as defined in Equation 4.20.

\[
\varepsilon_P \equiv -\frac{\partial}{\partial x_j} \left( \frac{p' u'_i \delta_{ij}}{\rho} \right) - \nu \frac{\partial u'_i}{\partial x_j} \frac{\partial u'_j}{\partial x_j} \tag{4.20}
\]

Rearranging the TKE equation to solve for \( \varepsilon_P \) results in Equation 4.21 where the terms on the right hand side of the equation have been calculated and presented in the previous sections.

\[
\varepsilon_P = C - (P + T_k + T_\nu) \tag{4.21}
\]

Figures 4.98–4.100 show contours of \( \varepsilon_P \) calculated by performing the TKE balance where the contour levels are again normalized to levels used in Figures 4.84–4.92. In the lowest interrogation plane (Fig. 4.98), the estimated dissipation levels are seen to be fairly low for all of control configurations. At \( y/H = 0.337 \) (Fig. 4.99), the levels of dissipation are seen to increase significantly, especially in the two cases with control applied. The largest \( \varepsilon_P \) magnitudes are seen near the center of the wake, but slightly elevated levels are also seen in the shear layers. In the highest interrogation plane investigated (Fig. 4.100), increased levels of \( \varepsilon_P \) are again seen in the center of the wake, but very little structure is seen in the shear layers. It is important to note that the estimation of \( \varepsilon_P \) is of the same order of magnitude as the convection, production, and turbulent transport terms presented previously as would be expected.
Figure 4.98: Mean contours of the dissipation balance estimate at $y/H = 0.169$. 

(a) No Control

(b) DC=100%

(c) SCLC
Figure 4.99: Mean contours of the dissipation balance estimate at $y/H = 0.337$. 

(a) No Control

(b) DC=100%

(c) SCLC
Figure 4.100: Mean contours of the dissipation balance estimate at $y/H = 0.506$. 
4.7 Summary

The results presented in the previous sections have shown the complexity of the three-dimensional wake that develops behind a non-conformal turret. Mean flow fields of the $U$, $V$, and $W$ components of velocity were shown at several planar locations above the boundary layer plate. These results showed the large wake deficit that existed throughout the wake, but also showed that the wake deficit region collapsed as the interrogation plane was shifted towards the top of the turret. Turbulence intensity levels were presented as normal and shear stress components of the Reynolds stress terms, and it was seen that a significant level of turbulent energy existed in the shear layers that developed from the cylindrical base. Strong regions of turbulence were also seen towards the center of the wake such that the combined turbulent kinetic energy was fairly evenly distributed throughout the wake and shear layers.

With the application of steady open-loop control, the upper region of the wake was seen to collapse, and the downwash velocity in the center of the wake was seen to increase significantly. With this increased downwash velocity, the regions of the wake closer to the boundary layer plate were seen to expand more rapidly. Despite the significant changes in the mean flow field, the turbulent kinetic energy distributions were seen to remain relatively constant with the only significant changes being seen in the highest planes investigated. In general, the two-point, spatial cross-correlations were seen to increase in magnitude and spatial extent with the application of control, and from this it was shown that several of the integral length scales in the flow had increased. Additionally, the pressure spectra acquired in the wake were seen to change dramatically with the application of steady open-loop control. Peak frequencies were seen to exist in the shear layer at $St_\theta$ of approximately 0.35 in the baseline case and 0.10 for the actively controlled case. Peak frequencies were also seen in the steady open-loop control pressure spectra across the center of the wake which were
not present in the baseline case.

Unsteady open-loop control and simple closed-loop control were also tested. The driving duty cycle for the unsteady open-loop control case was selected as the average duty cycle of the simple closed-loop control case such that the average control input to the flow was the same. Studying the mean flow field, the turbulent kinetic energy, two-point correlations, and pressure spectra for both of the unsteady control cases showed that, in a large scale sense, the unsteady control inputs had the nearly the same effect as the steady open-loop control input. Comparing the average jet momentum coefficients, the unsteady open-loop control and simple closed-loop control were found to achieve similar results to that of the steady open-loop control for approximately 45% of the control cost.

As discussed in the Section 1.4, the control system used throughout these studies was developed for performing active flow control of the aero-optics region on a pitching turret. As such, the time scales of the active control system driven at 15 Hz were able to match the time scales of the pitching turret, but were significantly lower than the time scales associated with the natural flow structures. Despite this, the simple closed-loop control was able to arrive at a mean duty cycle that was seen to be nearly as effective as the steady suction case. Past work in active flow control field has shown that control systems can be much more effective if the control system is able to impart fluctuations corresponding to the time scales of the flow field (Greenblatt and Wygnanski 2000). The limited frequency response of the valves used for the current studies did not allow for testing of this hypothesis in the turret wake flow field.
Chapter 5

Concluding Remarks

Experimental measurements were performed in a low-speed wind tunnel at Syracuse University to study the wake of a three-dimensional turret with and without active flow control. The suction based active flow control system was designed to match that of a system used for controlling aero-optical distortions, but the effects of the control system were studied in the near-field wake as opposed to the aperture area. Particle image velocimetry and dynamic surface pressure measurements were used to characterize the flow for baseline, steady open-loop suction (DC=100%), unsteady open-loop suction (DC=60%), and simple closed-loop control (SCLC) configurations.

5.1 Conclusions

The work presented here showed the merit of using dynamic suction to actively control the wake of a three-dimensional turret geometry. For all of the control cases tested, the suction based system was able to significantly alter not only the spatial structure of the wake as was seen in the PIV measurements, but was also able to alter the temporal nature of the wake as was seen by studying the spectral characteristics of the pressure measurements.
Studying the effects of the suction, it was seen that wake became more compact and the velocity magnitudes near the turret geometry increased (specifically the $V$ and $W$ components). From this, it can be inferred the local static pressure in this region has most likely decreased. When the static pressure in the wake region of a bluff body is decreased, the drag on the body will increase. Therefore, a drag penalty is likely associated with the aperture based flow control, but additional studies would need to be performed to verify this. As the control system was designed for the aero-optics problem, an associated drag penalty is not a negative result but rather one that should be considered in future studies of control systems on this geometry. It is possible to estimate the drag by integrating the wake profiles seen in Appendix C and this is commonly done in the wake of two-dimensional geometries such as cylinders and airfoils. Due to the highly three-dimensional nature of the turret wake and considering the wake deficit extends beyond the field of view in several of the test cases, it was determined that the uncertainty of drag estimates resulting from such a calculation using the current data would be too high to be useful. As such, it is the recommendation of the author that the drag be measured directly using a load balance or be calculated from static pressure measurements.

Studying the cross-correlations of the pressure measurements, the correlations across the wake were seen to decrease in magnitude. Studying the two-point spatial cross-correlations, it was seen that streamwise cross-correlation ($R_{uu}$) became the dominant correlation in the shear layer where the strongest oscillations were seen in the pressure data. From these results, it can be inferred that the cross-stream flow structures have become less organized with the application of control such that fluctuations in the aerodynamic loading may be reduced which is advantageous. Additional studies of the time dependent, static pressure distributions or direct measurements of the time dependent loading would need to be performed to confirm this conclusion.
The simple closed-loop control system developed for these studies was shown to have merit. Using the basic proportional feedback control algorithm, an efficient unsteady forcing was found that was able to achieve nearly the same control effect as the steady open-loop suction (DC=100%) with approximately 45% of the control input as evaluated using the jet momentum coefficient. One could argue that the closed-loop control system would not be required for performing flow control on a static turret as the unsteady open-loop suction (DC=60%) was shown to be equally effective. Although this argument is valid, previous work has shown that using closed-loop control systems on dynamic turrets was more efficient than using open-loop control (Wallace et al. 2010; Thirunavukkarasu et al. 2012; Wallace et al. 2012). As the application of most optical turrets requires pitching and yawing of the aperture region, closed-loop, feedback control is highly appropriate for this geometry. More advanced controllers, including dynamic estimators and advanced measurement based estimators, have also been shown to be effective at higher Reynolds numbers (i.e. $Re_D = 2.0 \times 10^6$). An overview of these high Reynolds number studies can be seen in Appendix D as well as in the work by Wallace et al. (2011).

To perform efficient closed-loop control on a static geometry, it has been shown that imparting periodic fluctuations related to the time scales of the flow field is more effective than applying steady blowing or suction. The paper by Greenblatt and Wygnanski (2000) provides a nice review of active flow control methods using periodic excitation. Considering the spectral data presented in the previous chapter, an efficient flow control system would need to be able to operate on the order of several hundred Hertz to achieve time scales related to the peak frequencies seen in the wake of the flow field. The valves that were selected for the current work were selected for the high flow rates that were obtainable. At the time this work was performed, off-the-shelf high speed valves were not available with large enough flow rates to perform
active control at the prescribed test conditions and the maximum cycling rate of 16 Hz was not fast enough to perform control at the speeds necessary for efficient closed-loop control. If valve technology continues to advance in the future such that high flow rate, high cycle rate valves do become available, performing closed-loop, active flow control using dynamic blowing or suction will become more practical.

Throughout this document, the efficiency of the active control system was assumed to increase with reduced levels of suction, but the overall efficiency was never quantified as a result of not measuring the aerodynamic loading or aero-optic distortions directly. An additional consideration is the difficulty in defining an appropriate figure of merit for the active control system. One can imagine that an appropriate figure of merit will differ depending on the control objective which will be related to either the aero-optic distortions, the aerodynamic loading, or some combination of the two. In a real-world application, the figure of merit will also need to take into consideration the complexity of the system, the weight of the controllers and valves, and the power required to operate the vacuum pumps and control system as these will all increase cost and reduce the performance of the aircraft. These penalties will also depend on the size of the turret and the flight characteristics of the aircraft that the turret is attached to. One can imagine that the system limitations for a reconnaissance aircraft such as the MQ-1 Predator will be much different than the limitations for a large airframe such as the C-130.

As discussed previously, the control system that was used for the current set of test was designed to perform flow control focused on improving the aero-optic characteristics of the aperture flow field and may have increased the mean drag load on the turret. To use active flow control techniques to control the aerodynamic loading on the turret, the active control devices would most likely need to be relocated to control different regions of the flow. More specifically, using the active flow control devices
on the cylindrical base may help to reduce the size of the wake region and provide an associated reduction in the drag. Additionally, including active control devices on the cylindrical base would allow for direct control of the shear layers that bound the wake and could possibly be used to help reduce the fluctuating loads. Effective control of the fluctuating loads would most likely require the use periodic excitation that could be guided by a closed-loop feedback controller using a combination of pressure sensors in the wake and on the turret geometry. The current work was not intended to be a parametric study of actuator placement and control inputs, but instead should serve as a guide for future studies interested in controller optimization and for studies looking to achieve different control objectives.

5.2 Recommendations for Future Work

Although the current work has provided an extensive database with which to analyze and study the wake of the turret, there are still aspects that could be studied in more detail. With respect to acquiring additional velocity measurements, it would be beneficial to acquire data for more unsteady open-loop control cases and to begin studying the effects dynamically pitching and yawing the aperture. Additionally, time dependent measurements would provide useful insight into how the time dependent pressure measurements relate to the different structures in the flow field above the boundary layer plate. Time dependent velocity measurements would be particularly useful for designing active control systems that operate at the time scales of the flow field. As discussed previously, dynamic load measurements or time resolved static pressure measurements on the turret surface could provide invaluable information depending on the intent of the active control system. Although these types of measurements could be made experimentally, performing well designed computational studies could provide a significant amount of information regarding not only the time dependent
behavior of the flow field, but also details regarding the three-dimensional nature of the wake.

Without time dependent velocity measurements, it may still be possible to develop an understanding of the time dependent velocity field using linear stochastic estimation (LSE) techniques. Using the time resolved pressure measurements that were simultaneously sampled with the PIV measurements, a time depended reconstruction of the wake could be developed by estimating the velocity field from the pressure readings. This method has been shown to be effective in past work where time dependent flow fields were estimated from reduced order models developed from PIV data using proper orthogonal decomposition (POD). Performing the LSE of the velocity data from the surface pressure data will require a strong correlation between the two data sets and this correlation would need to be evaluated prior to attempting the reconstruction.

An additional advantage of developing the reduced order model of the flow field is for performing closed-loop, feedback control. Using the LSE/POD technique, it is possible to develop a time dependent estimation of the velocity field for use as a feedback input for a control system. The merit of this technique has been shown previously on airfoils and the pitching turret (see, for example, Pinier et al. (2007) or Wallace et al. (2011)). The possible contributions to a control system from pressure sensors in the wake have yet to be studied, but the data presented throughout this document have shown that the wake pressure sensors have good observability of the wake flow characteristics. As the turret geometry is a surface based geometry, it is a unique geometry in that wake based measurements can be made in real-world applications by locating surface mounted pressure transducers on the aircraft downstream of the turret. In contrast, making measurements in the wake of an aircraft or road based vehicle is not practical. Using wake based pressure sensors as part of
the linear stochastic estimation of the important flow structures related to either the aerodynamic loading or the aero-optics may help to improve the effectiveness and efficiency of closed-loop, feedback control systems designed for the turret geometry.

This work was performed to help provide insight into the flow physics of the turret wake both with and without active control and with various steady and unsteady control inputs. It was the intent of the author to develop and present a database that would help guide future studies performed to advance the knowledge of three-dimensional bluff bodies not only experimentally, but computationally as well. The wake of the turret was shown to be complex and highly three-dimensional in nature from both a spatial and temporal standpoint. Performing efficient and effective flow control on this flow field in real-world applications will require a solid working knowledge of not only the fundamental flow field, but also the optimal methods for performing the control. As such, this is a geometry that lends itself well to combined experimental and computational efforts.
Appendix A

PIV Uncertainty

Measurement error in the instantaneous PIV vector fields must be given proper consideration as the image based measurements are prone to several forms of error that can reduce measurement quality. The errors in PIV data result from estimating the particle displacement between a given image pair and determining the time difference between the two snapshots. The resolution of timing boards used in current low-speed PIV systems provides sufficiently high precision to render the timing error negligible compared to the displacement errors. Conversely, the uncertainty in the displacement calculation has more than one source, and each of these sources is discussed below. As a guide, Chapter 5.5 of Raffel et al. (1998) has been followed.

The sources of displacement error that will be considered result from the particle image diameter ($\delta x_{PI-DIA}$), particle image displacement ($\delta x_{PI-DIS}$), camera resolution ($\delta x_{RES}$), background noise ($\delta x_{NOISE}$), and gradients across the interrogation window ($\delta x_{GRAD}$). For the current studies, $32 \times 32$ pixel interrogation windows were used to calculate the instantaneous vector fields, and the particle image diameter was approximately 3 pixels on the CCD sensors for the stereoscopic studies. The typical particle displacement between snapshots in the freestream was set to be 8 pixels, but using adaptive window shifting, the measured particle shift will always be less than
1/2 pixel (Westerweel et al. 1997). Using these values, the RMS-uncertainties of
the displacement estimation were found from Raffel et al. (1998) to be 0.03 for the
particle image diameter, 0.04 for the particle image displacement, 0.03 for the camera
noise, and 0.03 for the background noise.

The largest contribution to displacement error comes from gradients in the flow
field. The interrogating windows have finite size and as such, it is possible for velocity
gradients to exist across the interrogation windows resulting in an increased RMS-
uncertainty. In regions of high shear, such as between the freestream and wake flow
downstream of the turret cylinder, the mean velocity gradients were found to be as
high as 6 m/s between two adjacent velocity vectors. As a result, the gradient was
calculated to be 0.085 pixels/pixel which corresponds to an uncertainty of 0.41 pixel.
Clearly, this is the dominant source of uncertainty in the displacement calculation
for regions of high shear. In regions with low gradients, $\delta x_{\text{GRAD}}$ can be as low as
0.05 (Raffel et al. 1998).

The contributions to the displacement uncertainty are used to calculate the total
displacement uncertainty as seen in Equation A.1. Using this formulation, the total
displacement uncertainty will be as high as 0.42 pixels. Using the standard pixel
displacement for the current tests of 8 pixels, this results in an error of 5.3%. As
discussed previously, this error value will depend on the local gradients in the flow
field as well as the actual pixel displacement at a particular location in the flow field.

$$
\delta x = \sqrt{(\delta x_{\text{PI-DIA}})^2 + (\delta x_{\text{PI-DIS}})^2 + (\delta x_{\text{RES}})^2 + (\delta x_{\text{NOISE}})^2 + (\delta x_{\text{GRAD}})^2}
$$

(A.1)

An additional uncertainty exists in determining the spatial location of the target
plate during calibration which will result in uncertainty in the vector locations relative
to the physical geometry. The typical calibration uncertainty reported by the Dantec
software was approximately 0.4 pixels which corresponds to a spatial uncertainty of
0.07 mm. An additional uncertainly exists in the placement of the calibration plate for acquisition of the calibration images. For the current studies, this error is on the order of ±2 mm is therefore the more significant contribution in the spatial uncertainty.
Appendix B

Inflow Conditions

PIV measurements were made upstream of the turret to evaluate the evolution of the boundary layer and to check for homogeneity in the freestream flow. These measurements included two-component center plane measurements in the boundary layer centered around \(x/D = -5.0\) and stereoscopic measurements parallel to the boundary layer plate at several heights, also centered at \(x/D = -5.0\).

B.1 Two-Component PIV Measurements

Two-component PIV measurements were made upstream of the turret using the setup described in Section 3.3.4 to develop an understanding of the boundary layer thickness and evolution. Two of the main difficulties in making boundary layer measurements with a PIV system are eliminating laser reflections from the boundary layer plate and obtaining sufficient resolution to fully evaluate the boundary layer. In the current studies, every attempt was made to maximize the resolution near the boundary layer plate and the final grid spacing was increased to approximately 0.85 mm in both the \(x\) and \(y\) directions. At \(x/D = -5.0\), the boundary layer had a thickness of approximately 7 mm (\(\delta_{99}/H = .04\)) which meant that there were less than 10 grid
points across the boundary layer making a detailed evaluation of the boundary layer difficult. Despite these difficulties, the current PIV measurements still provide useful information regarding the boundary layer development. Figure B.1 shows contours of the $U$ component of velocity where the mean flow is from left to right and the front of the turret is located at $x/D = -1.0$. As such, the center of the interrogation window is four diameters upstream of the turret. Using the grid lines for reference, the boundary layer thickness can be seen to increase gradually over the field of view. Recalling Figure 4.1, the boundary layer thickness ultimately grew to $\delta_{99}/H$ of 0.10 (17.8 mm) at $x/D = -1.0$.

Figure B.1: Contours of mean $U$ velocity along center plane of the inflow.

**B.2 Stereoscopic PIV Measurements**

Stereoscopic PIV measurements were made at several heights above the boundary layer plate using the setup described in Section 3.3.4. Figure B.2 shows contours of the $U$ mean velocity contour normalized by the freestream velocity (53 m/s) at two different heights above the boundary layer plate. Note that in the stereoscopic results, the freestream flow is from top to bottom. Here, the two PIV interrogation
windows have been stitched together at $z/D = 0.0$ to build a single image. The scales of the axes have been normalized by dimensions related to the turret, either turret diameter in the $x$-$z$ plane or turret height along the $y$-axis. Comparing the flow fields of the two planes, one finds that there is little spatial variation which indicates that the flow is statistically homogeneous in all three directions as desired.

Figure B.2: Contours of mean $U$ velocity upstream of the turret.

Figure B.3 shows contours of the turbulent kinetic energy, $k$, as defined in Equation 2.6. Again, the whole window is comprised of two independent windows stitched
together at $z/D = 0.0$ which is more apparent in these figures. For a majority of the flow field in both planes, the turbulent kinetic energy is nearly zero as would be expected. The region of high turbulent kinetic energy along the center plane of the right interrogation window is most likely the result of measurement errors in the PIV data as opposed to physical turbulent fluctuations. These errors can be caused by small misalignments in the Scheimpflug mounts, slightly defocused cameras, or imperfections in the optical walls of the wind tunnel. It is important to note, however, that the contours of kinetic energy presented here are an order of magnitude lower than those seen in the wake of the turret.

Spatial two-point cross-correlations of the stereoscopic measurements were made using the techniques outlined in Section 2.3.2. Figures B.4 and B.5 show the two-point cross-correlation values ($R_{uu}$, $R_{vv}$, and $R_{ww}$) as contour plots at two different heights above the boundary layer plate. These values have not been normalized and therefore have units of $(m/s)^2$. Figure B.4 shows that the correlations are limited to a very small spatial region relative to the PIV interrogation window. As such, Figure B.5 shows an enlarged view of the same correlations using the same contour levels. To help with comparisons, a circle of diameter 0.076 turret diameters has been drawn on each subfigure. Studying these figures, one finds that there is little difference in the spatial extent of the different correlations, but variations in the correlation strength are seen to exist with $R_{ww}$ having the smallest correlation values. Additionally, the correlations are seen to be of similar size in both the $x$ and $z$ directions for each of the correlations. From these measurements, it can be concluded that the turbulence in the freestream flow field is statistically homogeneous and isotropic as desired. Using the first zero crossing of the cross-correlation as an approximation of $L_{ij}$, the integral length scales in the freestream can be estimated to be 0.04 turret diameters or 6 mm.
Figure B.3: Contours of mean turbulent kinetic energy upstream of the turret.

(a) $y/H = 0.169$

(b) $y/H = 0.394$
Figure B.4: Comparison of freestream two-point, cross-correlation contours at \((z/D, x/D) = (0.56, -5.11)\).
Figure B.5: Enlarged two-point, cross-correlation contours at \((z/D, x/D) = (0.56, -5.11)\).
Appendix C

Line Plots

This chapter contains profiles from the PIV data that were presented earlier in the document. Velocity and TKE profiles were taken from the PIV contour fields at $x/D$ locations of 0.25, 0.50, 0.75, and 1.00 for the interrogation planes at $y/H$ of 0.167, 0.337, and 0.506. In the figures that follow, the profiles for the four streamwise locations are plotted on the same figure for each interrogation plane considered.

C.1 No Control
Figure C.1: Traces of $U$ velocity at $y/H = 0.167$ with no control.

Figure C.2: Traces of $U$ velocity at $y/H = 0.337$ with no control.
Figure C.3: Traces of $U$ velocity at $y/H = 0.506$ with no control.

Figure C.4: Traces of $V$ velocity at $y/H = 0.167$ with no control.
Figure C.5: Traces of $V$ velocity at $y/H = 0.337$ with no control.

Figure C.6: Traces of $V$ velocity at $y/H = 0.506$ with no control.
Figure C.7: Traces of $W$ velocity at $y/H = 0.167$ with no control.

Figure C.8: Traces of $W$ velocity at $y/H = 0.337$ with no control.
Figure C.9: Traces of $W$ velocity at $y/H = 0.506$ with no control.

Figure C.10: Traces of $k$ velocity at $y/H = 0.167$ with no control.
Figure C.11: Traces of $k$ velocity at $y/H = 0.337$ with no control.

Figure C.12: Traces of $k$ velocity at $y/H = 0.506$ with no control.


C.2 Steady Open-Loop Control

Figure C.13: Traces of $U$ velocity at $y/H = 0.167$ with DC=100%.
Figure C.14: Traces of $U$ velocity at $y/H = 0.337$ with DC=100%.

Figure C.15: Traces of $U$ velocity at $y/H = 0.506$ with DC=100%.
Figure C.16: Traces of $V$ velocity at $y/H = 0.167$ with DC=100%.

Figure C.17: Traces of $V$ velocity at $y/H = 0.337$ with DC=100%.
Figure C.18: Traces of $V$ velocity at $y/H = 0.506$ with DC=100%.

Figure C.19: Traces of $W$ velocity at $y/H = 0.167$ with DC=100%.
Figure C.20: Traces of $W$ velocity at $y/H = 0.337$ with DC=100%.

Figure C.21: Traces of $W$ velocity at $y/H = 0.506$ with DC=100%.
Figure C.22: Traces of $k$ velocity at $y/H = 0.167$ with DC=100%.

Figure C.23: Traces of $k$ velocity at $y/H = 0.337$ with DC=100%.
Figure C.24: Traces of $k$ velocity at $y/H = 0.506$ with DC=100\%.
C.3 Unsteady Open-Loop Control

Figure C.25: Traces of $U$ velocity at $y/H = 0.167$ with DC=60%.
Figure C.26: Traces of $U$ velocity at $y/H = 0.337$ with DC=60%.

Figure C.27: Traces of $U$ velocity at $y/H = 0.506$ with DC=60%.
Figure C.28: Traces of $V$ velocity at $y/H = 0.167$ with DC=60%.

Figure C.29: Traces of $V$ velocity at $y/H = 0.337$ with DC=60%.
Figure C.30: Traces of $V$ velocity at $y/H = 0.506$ with DC=60%.

Figure C.31: Traces of $W$ velocity at $y/H = 0.167$ with DC=60%.
Figure C.32: Traces of $W$ velocity at $y/H = 0.337$ with DC=60%.

Figure C.33: Traces of $W$ velocity at $y/H = 0.506$ with DC=60%.
Figure C.34: Traces of $k$ velocity at $y/H = 0.167$ with DC=60%.

Figure C.35: Traces of $k$ velocity at $y/H = 0.337$ with DC=60%.
Figure C.36: Traces of $k$ velocity at $y/H = 0.506$ with DC=60%.
C.4 Simple Closed-Loop Control

Figure C.37: Traces of $U$ velocity at $y/H = 0.167$ with SCLC.
Figure C.38: Traces of $U$ velocity at $y/H = 0.337$ with SCLC.

Figure C.39: Traces of $U$ velocity at $y/H = 0.506$ with SCLC.
Figure C.40: Traces of $V$ velocity at $y/H = 0.167$ with SCLC.

Figure C.41: Traces of $V$ velocity at $y/H = 0.337$ with SCLC.
Figure C.42: Traces of $V$ velocity at $y/H = 0.506$ with SCLC.

Figure C.43: Traces of $W$ velocity at $y/H = 0.167$ with SCLC.
Figure C.44: Traces of $W$ velocity at $y/H = 0.337$ with SCLC.

Figure C.45: Traces of $W$ velocity at $y/H = 0.506$ with SCLC.
Figure C.46: Traces of $k$ velocity at $y/H = 0.167$ with SCLC.

Figure C.47: Traces of $k$ velocity at $y/H = 0.337$ with SCLC.
Figure C.48: Traces of $k$ velocity at $y/H = 0.506$ with SCLC.
Appendix D

Large Scale Testing: SARL

Measurements

During the summers of 2010 and 2011, the dynamic suction based, active flow control system was tested on a large scale turret at the Subsonic Aerodynamic Research Laboratory at Wright-Patterson Air Force Base. The dynamic suction system was tested using various open and closed-loop control algorithms at $Re_o$ of $2.0 \times 10^6$ and a Mach number of 0.3 with positive results. The work performed in 2010 are presented in the AIAA paper by Wallace et al. (2011) and the document that follows provides preliminary results from the 2011 tests where the suction system had been improved following the 2010 testing. The document that follows is an abstract that was submitted and accepted by AIAA for the for the 6th AIAA Flow Control Conference to be held in June of 2012. A formal paper will be submitted to AIAA for the conference but at the time this document was published, the final draft of the work performed at SARL had not been approved by the Air Force for distribution.
Active Flow Control of a Pitching Turret Flow Field Using Closed-Loop Feedback Control

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Experimental testing of an active flow control system on a three-dimensional, non-conformal turret has been performed at a diameter based Reynolds number of $2 \times 10^6$. Active flow control was achieved using dynamic suction and various open and closed-loop feedback control algorithms in an effort to determine the most effective and efficient control scheme for reducing aero-optical distortions in the vicinity of the turret aperture. An array of measurement techniques including dynamic surface pressure, PIV and Malley probe measurements have been used to better characterize and understand the flow field in order to evaluate the active control system. From this research, it has been shown that dynamic suction has the ability to manipulate the separated flow above the turret aperture and can significantly alter the characteristics of the flow field.

Nomenclature

\begin{align*}
A_j &= \text{Suction slot area} \\
A_o &= \text{Frontal area of turret} \\
C_\mu &= \text{Jet momentum coefficient} \\
D &= \text{Turret diameter} \\
DC &= \text{Duty cycle} \\
f_p &= \text{Turret pitch frequency in Hertz} \\
P &= \text{Fourier transform of pressure signal} \\
p &= \text{Time dependent pressure signal} \\
Re_p &= \text{Diameter based Reynolds number} \\
Re/m &= \text{Reynolds number per meter} \\
S_{j,k} &= \text{Cross-spectral density function} \\
T &= \text{Block sampling time} \\
t &= \text{Time} \\
U &= \text{Streamwise velocity} \\
U_j &= \text{Suction jet velocity} \\
U_\infty &= \text{Freestream velocity}
\end{align*}

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θ = Turret pitch angle
ρ_J = Density of suction jet flow
ρ_∞ = Freestream density

I. Introduction

Turrets are a bluff body geometry commonly used for optical systems on airborne platforms. As a bluff body, the turret geometry can induce large mean and fluctuating loads on an aircraft degrading the performance of the aircraft and the optical system. When compressible effects are present in the flow field, the density fluctuations develop in the highly turbulent flow field and the resulting optical distortions can further degrade the performance of the optical system. This problem is commonly referred to as the aero-optics problem.

Various active flow control techniques have been investigated in the past with the goal of reducing aero-optical distortions. Synthetic jet actuators have been shown to reduce optical path distortion RMS values by up to 34% at Mach 0.3\textsuperscript{1} as well as reduce surface pressure fluctuations by 26% at similar test conditions.\textsuperscript{2} Dynamic suction applied to the aero-optics problem on a dynamically pitching turret was shown to reduce the fluctuating RMS velocities in the vicinity of the aperture by up to 48% at Mach 0.1.\textsuperscript{3} Hybrid control (a combination of active and passive control) on the turret flow field has also been investigated and was shown to increase the separation angle by more than 10° and was capable of reducing optical aberrations by as much as 40%.\textsuperscript{4, 5}

The objective of the current study was to develop and test closed-loop feedback control algorithms used to drive an active flow control system designed to mitigate aero-optic distortions while pitching the turret. To this end, experimental testing was performed in the Subsonic Aerodynamics Research Laboratory (SARL) at Wright-Patterson Air Force Base (WPAFB) to test an suction based active control system and to develop variety of open and closed-loop feedback control algorithms to determine the most efficient method of reducing aero-optic distortions at Mach 0.3.

II. Experimental Setup

Experimental testing was performed in the open return SARL facility which had a test section of length 4.57 m and octangular cross section of 3.05 × 2.13 m. The SARL facility was designed to allow for optical access from three sides of the test section making flow visualization, particle image velocimetry (PIV), and Malley probe measurements possible. The tunnel was operated at Mach 0.3 which corresponds to a Reynolds number per meter (\(Re/m\)) of 6.7 × 10\textsuperscript{6}. One important detail to note is that the blade pass frequency for the SARL facility operating at Mach 0.3 was calculated to be 153 Hz.

A. Turret Test Article

The turret test article was a non-conformal turret capable of dynamic pitch and yaw, but for the current testing was only pitched such that the turret was symmetric across the center plane aligned with the freestream flow. The hemispherical cap geometry was mounted on a cylindrical base with diameter of 0.3048 m and height of 0.0508 m. This geometry corresponded to a turret aspect ratio of 0.67 and a diameter based Reynolds number, \(Re_D\), of 2.0 × 10\textsuperscript{6}. The aperture had diameter of 0.127 m and was surrounded by two concentric rings of suction slots as seen in Figure 2. The turret had interchangeable apertures that allowed for the placement of a returning mirror for Malley probe measurements (see Fig. 2(a)) or a flat aperture with additional pressure taps for PIV testing (Fig. 2(b)). The turret geometry was mounted on a splitter plate to help generate a consistent inflow for the surface mounted geometry.

Using LabVIEW, the turret was pitched in a sinusoidal fashion at a rate of 0.1 Hz over a range of pitch angles, \(θ\), from 93° to 103° as defined in Figure 3. The pitch angle for a given cycle can be found (in degrees) using Equation 1.

\[
θ(t) = 98 - 5 \cos (2\pi f_p t)
\]  

(1)

Here, \(t\) is time in seconds and \(f_p\) is the pitch frequency in Hertz.
Figure 1. Internal view of test section looking upstream into tunnel inlet with turret model in place.

(a) Malley probe configuration.  
(b) PIV configuration.

Figure 2. Comparison of turret apertures for PIV and Malley probe measurements.

Figure 3. Definition of turret pitch angle.
1. **Dynamic Suction System**

Active flow control of the turret flow field was achieved using dynamic suction. A vacuum pump was used to evacuate a reservoir which was used to help maintain constant back pressure across an array of valves. Four independently addressable valves were each connected to a bank of five suction slots arranged around the turret aperture and driven using a 7 Hz square wave. To characterize the output of the suction slots, a Dantec hotwire anemometry system was used to measure the suction velocity very near to the slot. Figure 4 shows a plot of the mean jet momentum coefficient per slot as a function of duty cycle where a duty cycle of 0% indicates that the valve is fully closed and 100% is fully open. The jet momentum coefficient is defined as

\[
C_\mu \equiv \frac{\rho_J U_J^2 A_J}{\rho_\infty U_\infty^2 A_o} \tag{2}
\]

where \(U_J\) is the mean suction velocity, \(\rho_J\) is the density of the jet flow, \(A_J\) is the suction slot area, \(U_\infty\) is the freestream velocity, \(\rho_\infty\) is the density of the freestream flow, and \(A_o\) is the frontal area of the turret. From Figure 4, one finds that the output response from the suction slots was effectively linear over a range of duty cycles from 25 to 40%.

![Figure 4. Jet momentum coefficient as a function of duty cycle.](image)

B. **Test Equipment**

1. **PIV System**

A LaVision PIV system was used to acquire ensemble averaged, two-component velocity field measurements in the vicinity of the turret aperture. The PIV measurement system consisted of two 14-bit Cooke Corporation pco.1600 cameras, a Litron Nano L PIV Nd:Yag laser with a 200 mJ/pulse output, a ViCount 5000 series smoke generator, and was operated using the LaVision software package DaVis 8. Data were acquired for both the static and pitching turret at 10 Hz using matched test configurations with overlapping stationary cameras configured to increase the sampling window size. Ensembles of 1100 image pairs were acquired for the static test cases. For test cases with the turret pitching, 110 image pairs phase aligned to the turret pitching were acquired throughout a pitch cycle to build ensembles of 55 pitch cycles. The PIV measurements were simultaneously sampled with the dynamic surface pressure.

2. **Dynamic Pressure Sensors**

An array of 103B01 PCB Piezotronics dynamic pressure sensors were used to measure surface pressure at various locations on and around the turret aperture. The pressure transducers had a sensitivity of 217.5
mV/kPa, resolution of 0.14 Pa, and a measurement frequency range of 5 Hz to 13 kHz. The excitation voltage for the transducers was supplied by the National Instruments PXI system used for data acquisition. In addition to supplying the excitation voltage, the PXI system had built in low-pass filters which were used for anti-alias filtering. The auto-spectral density function was calculated using the spectral methods outlined in the text by Bendat and Piersol where the Fourier transform and cross-spectral density function are defined in Equations 3 and 4, respectively.

$$P_{m,j}(f, T) = \int_0^T p_{m,j}(t)e^{-i2\pi ft} dt$$

$$S_{j,k}(f) = \lim_{T \to \infty} \frac{1}{T} P_{m,j}^* (f, T) P_{m,k}(f, T)$$

Here, $T$ is the total sampling time for block $m$, $p_{m,j}$ is the time series of the pressure signal for block $m$ and sensor $j$, $P_{m,j}$ is the Fourier series of the pressure signal for block $m$ of sensor $j$, and $S_{j,k}$ is the cross-spectral density function of sensor $j$ with $k$. Note that (*) indicates the complex conjugate, the overline indicates the block average, and the auto-spectral density function is returned for $j = k$.

### III. Results

#### A. PIV

1. **Static Turret**

PIV data were acquired for the static turret at $\theta = 103^\circ$ for several open loop control cases including a duty cycle of 0% (also referred to as the baseline case) and 100% (steady suction). The following data are ensemble averaged over 1100 snapshot pairs and contours are of the local streamwise velocity, $U$, normalized by the freestream velocity. The figures have been stitched together from two independent camera windows, and the grey outline indicates the approximate location of the turret and aperture. In the figures that follow, the contour levels for each of the subfigures have been matched for comparison.

Studying Figure 5, one finds that the increased levels of suction increase the extent of the flow reattachment over the turret aperture. In the baseline case (Fig. 5(a)), the flow over the turret aperture was separated resulting in a large recirculating flow above the aperture. With the application of the steady suction, the large separation region is eliminated and the flow remains attached over the aperture. It is important to note that there were difficulties in seeding the flow field such that dropout caused the mean velocities near the turret upstream of the aperture to be reduced from what would be expected. Similarly, near the trailing edge of the aperture, there is another region where dropout caused the calculated velocities to be lower than expected. This dropout region is seen as a small triangular region of green ($U/U_\infty \approx 0.65$).

2. **Dynamic Turret**

PIV data were acquired for the pitching turret and are presented here as time traces of the integrated $u_{RMS}$. For this, the phase aligned data were averaged to obtain a mean flow field for a single pitch cycle. The $u_{RMS}$ for the mean flow field was then averaged spatially at each point in time to obtain a time trace of the $u_{RMS}$ for a mean pitching cycle. The region for the spacial averaging can be seen in Figure 6.

Figure 7 compares the spatially averaged RMS values for the baseline, 30% duty cycle, 40% duty cycle, and steady suction cases. Starting with the baseline case (Fig. 5(a)), the flow over the turret aperture was separated resulting in a large recirculating flow above the aperture. With the application of the steady suction, the large separation region is eliminated and the flow remains attached over the aperture. It is important to note that there were difficulties in seeding the flow field such that dropout caused the mean velocities near the turret upstream of the aperture to be reduced from what would be expected. Similarly, near the trailing edge of the aperture, there is another region where dropout caused the calculated velocities to be lower than expected. This dropout region is seen as a small triangular region of green ($U/U_\infty \approx 0.65$).
Figure 5. Mean velocity profiles for various control cases with $\theta = 103^\circ$. 
Figure 6. Interrogation window for spatial average of the fluctuating RMS.

(a) Baseline
(b) DC = 30%
(c) DC = 40%
(d) Steady Suction

Figure 7. Spatially averaged fluctuating RMS values for an average pitching cycle with various levels of open loop control.

(c) DC = 40%
(d) Steady Suction
B. Dynamic Pressure

1. Static Turret

Figure 8 shows the ASDF for three sensors on the turret aperture. The important thing to note about the spectra of these three sensors is that they vary significantly depending on the location on the turret aperture. This indicates that, despite being in a separated flow region that appears to be uniform (recall Fig. 5(a)), these sensors are being subjected to different flow phenomena. This information starts to give some insight into the complexities of this three-dimensional flow field, even when one considers only the aperture region. There are several spikes in the data, but recalling the blade pass frequency of 153 Hz for the SARL facility, one will find that these spikes correspond to the blade pass frequencies and the associated harmonics.

![Figure 8. Baseline ASDF of three pressure sensors on the turret aperture aligned with the freestream flow.](image)

Similar figures have been generated to compare the ASDF for varying levels of open loop control and are seen below. Note that the red curve corresponds to the baseline data seen in Figure 8 and that the scale of the ordinate has been adjusted from the previous figure. From Figure 9, it is clear that the active flow control has a significant effect on the surface pressure and that varying the levels of control changes the surface pressure behavior. With the application of cyclic control (i.e. the 30% and 40% duty cycle cases), a series of sharp peaks in the spectra develop similar to the Fourier transform of a square wave. These peaks are not seen in the steady cases (i.e. the baseline and steady suction cases). It is also interesting to note the general shift in energy in for each case. The baseline case generally has high energy in the low frequency range and rolls off at higher frequencies while the effect of the active control has been to distribute the energy more uniformly across the frequency band studied.

IV. Concluding Remarks

Experimental testing has been performed at the Subsonic Aerodynamic Research Laboratories at Wright-Patterson Air Force Base with the objective of developing and testing an active flow control system to be used for mitigating aero-optical distortions above the aperture of a three-dimensional, non-conformal turret. The research presented here has provided some insight into the complexities of the turret flow field and shown the potential merits of using dynamic suction to actively control the flow field in the vicinity of the aperture. With the increased complexity of pitching the turret, more intelligent controllers will be required to effectively control the flow field in an efficient manner and as such, several closed-loop feedback control algorithms have been developed to drive the active control system. The final paper will present PIV, dynamic surface pressure, and Malley probe measurements used to evaluate the ability of the closed-loop feedback control algorithms to reduce aero-optic distortions.
Figure 9. ASDF of a pressure sensor located at the center of the turret aperture for various open-loop control levels.

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Perform experimental fluid dynamics research towards completion of dissertation.
Research utilizes test facilities at Syracuse University and Wright-Patterson AFB
Subsonic Aerodynamic Research Laboratory (SARL) to develop and evaluate
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Research performed at the SARL facilities at Wright-Patterson AFB provided
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