Aerodynamic Flow Control of a Moving Axisymmetric Bluff Body

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The present experiments focus on active fluidic control of the aerodynamic forces and moments of an axisymmetric bluff body platform in time-periodic sinusoidal pitch oscillations at reduced frequencies $0 < k < 0.259$. The platform is wire-mounted on a six degree of freedom traverse where each of the eight support wires is individually controlled by a servo motor with an integrated in-line load cell for feedback control of the platform's motion. The aerodynamic forces and moments on the platform are manipulated by controlled interactions of an azimuthal array of synthetic jet actuators on its aft segment with the local cross flow to induce partial (azimuthally-segmented) flow attachment that is coupled with vectoring of its near-wake. The actuation-induced forces and moments can either increase or diminish the corresponding pitch-induced baseline aerodynamic forces and moments. These actuation effects are exploited for open-loop control to suppress or augment the pitch-induced moment, and effect robust (in excess of 50%) control authority over a broad range of oscillation frequencies (up to reduced frequency of 0.259) that are suitable for trajectory stabilization and steering in free-flight.

**Nomenclature**

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
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<tbody>
<tr>
<td>$A_j$</td>
<td>actuator orifice cross-sectional area</td>
</tr>
<tr>
<td>$c$</td>
<td>body chord length</td>
</tr>
<tr>
<td>$C$</td>
<td>transformation matrix in controller</td>
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<tr>
<td>$C_D$</td>
<td>coefficient of drag</td>
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<tr>
<td>$C_P$</td>
<td>coefficient of pressure</td>
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<tr>
<td>$C_P$</td>
<td>jet momentum coefficient</td>
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<tr>
<td>$D$</td>
<td>axisymmetric body diameter</td>
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<tr>
<td>$D_s$</td>
<td>support wire diameter</td>
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<tr>
<td>$f$</td>
<td>body pitching frequency</td>
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<tr>
<td>$f_{cycle}$</td>
<td>synthetic jet frequency</td>
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<tr>
<td>$F_A$</td>
<td>total aerodynamic force</td>
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<td>$F_D$</td>
<td>aerodynamic drag force</td>
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<td>$F_G$</td>
<td>gravitational force</td>
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<td>$F_L$</td>
<td>aerodynamic lift force</td>
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<td>$F_I$</td>
<td>total inertial force</td>
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<tr>
<td>$F_W$</td>
<td>aerodynamic wire drag force</td>
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<td>$h_s$</td>
<td>body backward-facing step height</td>
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<td>$k$</td>
<td>model reduced frequency</td>
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<td>$m_n$</td>
<td>angle coordinate of $n^{th}$ wire</td>
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<td>$M$</td>
<td>controller motor forces and displacements</td>
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<tr>
<td>$M_{BOT}$</td>
<td>bottom jet modulation command</td>
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<tr>
<td>$M_{TOP}$</td>
<td>top jet modulation command</td>
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<tr>
<td>$M_p$</td>
<td>aerodynamic pitching moment</td>
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<tr>
<td>$R_e$</td>
<td>Coanda surface radius</td>
</tr>
<tr>
<td>$R_e$</td>
<td>Reynolds number</td>
</tr>
<tr>
<td>$S_{act}$</td>
<td>actuation Strouhal number</td>
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<tr>
<td>$T_n$</td>
<td>Tension measured in $n^{th}$ wire</td>
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<td>$U_j$</td>
<td>maximum jet expulsion velocity</td>
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<td>$U_0$</td>
<td>free stream velocity</td>
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<td>wire direction matrix</td>
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<td>$x_m$</td>
<td>center of body wire mounts</td>
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<td>actuator induced drag force</td>
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<td>$\Delta F_L$</td>
<td>actuator induced lift force</td>
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<tr>
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<td>bottom actuator induced lift force</td>
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<tr>
<td>$\Delta F_{L,TOP}$</td>
<td>top actuator induced lift force</td>
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<tr>
<td>$\Delta M_p$</td>
<td>actuator induced pitching moment</td>
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<td>$\rho$</td>
<td>air density</td>
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<td>$\zeta$</td>
<td>planar vorticity</td>
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<tr>
<td>$\tau$</td>
<td>body oscillation period</td>
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<tr>
<td>$\tau_{conv}$</td>
<td>body convective time scale</td>
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<tr>
<td>$\tau_{cycle}$</td>
<td>synthetic jet period</td>
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I. Technical Background

The present work focuses on the manipulation of separated flows over external aerodynamic platforms with the objective of exploiting the aerodynamic forces and moments that result from partial or full flow attachment for platform steering and stabilization. The separating shear layer over stalled 2- and 3-D aerodynamic surfaces is typically dominated by a strong coupling to the instability of the near wake (e.g., Wu et al.\textsuperscript{1}). One type of separation control strategy uses coupling to the near-band receptivity of the separating flow at unstable Strouhal numbers of the near wake ($St_{uf} \sim O(1)$, e.g., Hsiao et al.\textsuperscript{2}, Neuberger and Weyganski\textsuperscript{3}, Chang et al.\textsuperscript{4}, Williams et al.\textsuperscript{5}, and Seifert et al.\textsuperscript{6}). Another strategy decouples global flow instabilities from fluidic modification of the “apparent” aerodynamic shape with actuation frequencies that are at least an order of magnitude higher than the flow frequency (e.g., Erk\textsuperscript{7}, Smith et al.\textsuperscript{8}, Amitay et al.\textsuperscript{9}, Honohan et al.\textsuperscript{10}, Glezer et al.\textsuperscript{11}).

Active control of flow separation and thereby of global aerodynamic performance can be significantly enhanced by hybrid, active and passive control that exploits the interaction of the actuation jets with adjacent solid surfaces or the Coanda effect that has been investigated extensively since the 1940s (e.g., Newman\textsuperscript{12}). The Coanda effect has been the basis of circulation control over lifting surfaces in numerous aerodynamic systems (e.g., Englar\textsuperscript{13}). A passive feature that can be used is a sharp leading edge to induce separation of an axisymmetric body that was controlled by jet injection further downstream\textsuperscript{14}. Hybrid flow control was also demonstrated by Nagib et al.\textsuperscript{15} who combined a short backward facing step with a jet to control local separation. This approach was also utilized for controlling internal flows, for example, by Lo et al.\textsuperscript{16} who controlled separation in adverse pressure gradients in a diffuser.

Because the Coanda effect is associated with the attachment of an inherently separated flow to a solid surface, this flow configuration presents a unique opportunity to create net aerodynamic forces on various bluff bodies through controlled activation. Freund and Mungal\textsuperscript{17} reduced the aerodynamic drag of axisymmetric bodies by up to 30% using induced attachment at the aft corner of the body by steady, circumferentially-uniform blowing over Coanda surfaces. Rinehart et al.\textsuperscript{18,19} demonstrated generation of a asymmetric force on an aerodynamic platform using the interaction of a single synthetic jet with an integrated axisymmetric azimuthal Coanda tail surface along a backward facing step. In a related investigation, McMichael et al.\textsuperscript{20} exploited this flow control approach to the separated base flow of an axisymmetric 40 mm spin stabilized projectile to effect aerodynamic steering forces and moments that were sufficient to control the trajectory of the projectile in flight. Corke et al.\textsuperscript{21} reported alteration of the drag and side forces on an axisymmetric body using tangential plasma actuation placed upstream of a Coanda surface. Abramson et al.\textsuperscript{22,23} extended the Coanda actuation methodology to effect prescribed (asymmetric) side forces by using four individually-controlled azimuthally distributed synthetic jets within the rearward facing step of the tail and demonstrated that the induced forces can be used to effect steering during flight and trajectory stabilization. Finally, Lambert et al.\textsuperscript{24,25} showed that unstable motion of a free-moving axisymmetric model can be significantly suppressed or enhanced with appropriate timing and modulation of the actuation, and this could lead to significant directional control authority for free flight aerodynamic bodies.

Control of the aerodynamic forces on axisymmetric airborne platforms builds on numerous earlier investigations of the uncontrolled baseline flow and its natural instabilities. The basic motions of spinning projectiles, including natural nutation and precession linear and nonlinear instabilities, induced by Magnus, damping, and normal forces and moments are discussed in detail in the classical work of Nicolaides\textsuperscript{26}. The instabilities of symmetric projectiles in the presence and absence of spin were discussed in detail by Murphy\textsuperscript{27}. While spin-stabilized projectiles are gyroscopically stable to axisymmetric moment instability, they are susceptible to roll resonance\textsuperscript{28}, and spin-yaw lock in\textsuperscript{29}, which add complicated non-linear effects to the projectile dynamics that are in general hard to correct for. In recent years considerable attention has been devoted to the development active control approaches for both fin- and spin-stabilized projectiles, including aerodynamic forces induced by a piezoelectric-articulated nose section\textsuperscript{30}, synthetic jet actuation on a spinning projectile\textsuperscript{31}, and the swerve response of finned and spin-stabilized projectiles to generic control forces\textsuperscript{32,33}.

An inherent problem with any experimental aerodynamic study of a nominally ‘free’ body is related to its mounting into a test section. Ideally, the model support should not cause aerodynamic interference (e.g., magnetic-force supports\textsuperscript{34}), but the most predominant supports involve sting mounts that are directly in the body’s wake. An alternative support, aimed at minimizing the wake-support interference, was utilized by Abramson et al.\textsuperscript{22,23} and Lambert et al.\textsuperscript{31}, where a model is supported by thin wires. The current work takes advantage of a thin wire mounting setup in order to create a novel traverse, and the goal of the present investigation is to assess the effect of hybrid flow control on a dynamic axisymmetric body.

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The present investigation is conducted in an open-return wind tunnel at Georgia Tech having a test section that measures 91 cm on the side with a test section speed of up to $U_0 = 40$ m/s. The model diameter is 90 mm ($Re_D$ up to $2.4 \times 10^5$), and is shown in Figure 1a and b from the side and back, respectively. The aerodynamic control forces (and associated moments) on the projectile model are generated using an azimuthal array of four independently-driven synthetic jet actuators that are equally distributed along the perimeter of the tail section, with primary importance placed on the two actuators within the plane of the model pitching motion. Each jet is embedded into the surface with an area $A_j = 0.38 \times 34.3$ mm$^2$ rearward facing orifice such that it is issued over a Coanda surface formed by an azimuthal segment of an axisymmetric constant radius of $R_e = 12.7$ mm. The jet actuation leads to the partial attachment of the outer flow along the Coanda surface resulting in a reaction force by the turning of the outer flow into the wake region. The axisymmetric model is built using both stereo-lithographed and aluminum components and measures $D = 90$ mm in diameter and $c = 165$ mm in length. Eight mounting wires are fixed into the center aluminum piece, and the rest of the model containing the synthetic jet actuators is fastened together to the center with an aluminum spine that extends through the model. The rear component with the Coanda surface has the cut-in grooves along the orifice edges that guide the jet flow and prevent its spanwise diffusion. The adjoining backward-facing step to the circumference of the body is $h_s = 1.5$ mm in height. The step height was chosen such that it is shallow enough to enable local flow attachment when the control jet is activated, but deep enough to prevent attachment of the free stream flow in the absence of the jet actuation.

The experimental axisymmetric model along with the traverse support are shown in Figure 2. The $D_s = 0.96$ mm support steel wire is selected to be thin enough to reduce the drag coefficient and decouple its vortex shedding from the model, while thick enough to minimize translational and rotational vibrations. Each wire that extends from the model is fastened to a servo motor, with an in-line load cell, and each motor is fastened to an external spring for pretension. The electrical connection for the actuators is enabled by means of the wires woven along the back four support wires and through the tunnel walls, while the support wires provide the electrical ground. The servo motors are controlled through analog outputs from a Quanser Q8 boards that are controlled through QuaRC hardware-in-loop commands in a Simulink controller, and the actuation is controlled through external amplifiers controlled by a secondary Q8 board. The servo motors are chosen such that they can induce a displacement ranges on the order of 50 mm and 10$^6$ in each direction, and provide high frequency motion of up to $f = 50$ Hz with smaller displacement. These motions were chosen from time scales and deflection scales on the order of previously measured aerodynamic instabilities (20 ms, 10$^6$) that were found in a single degree of freedom model by Lambert et al. The FUTEK in-line load cells were chosen to resolve forces on the order of the expected synthetic jet actuation forces of 0.6N. The load cells were calibrated over the entire range through attaching individual weights, and the calibration was verified by placing static weights on the model and resolving the known lift force. The springs were chosen to give a pretension of 50 N, initializing each wire to half of its allowed tension range.

The final component to the wire support system is an external six degree of freedom Vicon motion analysis system. A representation of this system is shown in Figure 3. The system is comprised of six MX-T408 cameras, each with a 12.5 mm lens, capable of capturing 4 megapixels of information with a response rate of 515 frames per second at full

![Figure 1. Side (a) and upstream (b) views of the centered wind tunnel model with four hybrid fluidic actuators marked in green.](image1)

![Figure 2. Six degree of freedom traversing mechanism utilizing eight support wires, each connected to an in-line load cell, a servo-actuator, and a pre-tensioned spring.](image2)
resolution. The cameras record IR reflections off of thin markers (minimum of six) placed on the aerodynamic platform and uses these markers to calculate the displacement of the model in six degrees of freedom. An external MX-Giganet core unit synchronizes the cameras and transfers the data to a separate computer that then interprets the camera input and broadcasts a data stream of the model’s motion through UDP protocol at 500 Hz.

III. Six Degree of Freedom Controller Design

The fundamental description of the system is as follows: There is an over-constrained model to be controlled in six output degrees of freedom \((x, y, z, \alpha_x, \alpha_y, \alpha_z)\), to which there are eight input motor angles \((m_1, ..., m_8)\). We define \(x\) as streamwise, \(y\) as side-stream, \(z\) as cross-stream, \(\alpha_x\) as roll, \(\alpha_y\) as pitch and \(\alpha_z\) as yaw. The model has an inertial force and moment \((F_I)\), an aerodynamic force and moment \((F_A)\), and a gravitational force and moment \((F_G)\) acting on it. In addition, each mounting cable has a tension \((T_1, ..., T_8)\) that is dependent on the forces on the body, as well as the motor angles. Each motor shaft has a certain amount of inertia and damping, and each wire is assumed to have an aerodynamic force that is entirely drag, with negligible inertial force or friction drag. The mounting locations of each of the centers of the motor pulleys are known, along with the corresponding pulley radii. The mounting locations of each of the wires in the body are also known relative to the center of the model mounts \((x_m = 0.54c\text{ from the nose})\). The wires are not assumed incompressible, but each one is assumed to compress only as a spring with known elastic modulus and cross-sectional area.

In order to build a functional controller, there needs to be stable real-time solutions to two problems: the first is a calculation of the model orientation \((x, y, z, \alpha_x, \alpha_y, \alpha_z, \text{ and all components of } F_A)\) given the motor orientation \((m_1, ..., m_8, T_1, ..., T_8)\), where all values at the previous time step and all geometric variables are allowed to be used. To fully define the six degree of freedom coordinate system, a yaw first, pitch second, and roll last rotational convention is adopted. In order to accurately model the traverse motion, the first function that is required is the direction of each mounting wire given the motor positions, model position, and wire tensions from the frame of the aerodynamic model shown in Eq. 1:

\[
\begin{bmatrix}
W_{x,n} \\
W_{y,n} \\
W_{z,n}
\end{bmatrix} = \text{function}\left(x, y, z, \alpha_x, \alpha_y, \alpha_z, m_3, ..., m_8, T_1, ..., T_8, \text{and Mechanical Constants}\right) = \overline{W_{3xn}} \quad (Eq. 1)
\]

The second function that is required is a relation between the wire tensions and the force on the model. This is done with a force and moment balance with the inertial, gravitational, and aerodynamic forces and moments with the wire tension, as shown in Figure 4. The gravitational force and inertial force are entirely dependent on the orientation of the model, and independent of the orientation of the wires. This allows Eq. 1 to be reorganized as shown below in Eq. 2:

\[
\vec{\dot{x}} = \begin{bmatrix}
x \\
y \\
z \\
\alpha_x \\
\alpha_y \\
\alpha_z \\
F_A
\end{bmatrix} = \vec{c}_{12x16} * \begin{bmatrix}
m_1 \\
... \\
m_8 \\
T_1 \\
... \\
T_8
\end{bmatrix}, \text{ where } \vec{c} = \text{function} (\overline{W}, \text{Mechanical constants, and time}) \quad (Eq. 2)
\]

The formulation of variables and constants as shown in Eq. 2 is powerful because it separates forces and displacements of the model \((\vec{x})\), with the forces and displacements on the motors \((\vec{M})\). The transformation matrix

![Figure 3. Front and top views of the motion analysis system.](image1.png)

![Figure 4. Free body diagram of the aerodynamic platform using aerodynamic (blue), tension (red), gravitational (cyan) and inertial (green) forces and moments.](image2.png)
between these two quantities ($\mathbf{C}$) depends on the orientation of both $\mathbf{M}$ and $\mathbf{R}$ and therefore must be reevaluated at each time step, where the current controller linearizes this calculation to optimize runtime and allow the controller to operate at 1 kHz. The inverse calculation from the model coordinates to the motor coordinates is also quickly computed with a least norm matrix inverse solution.

A depiction of the controller is shown as a block diagram in Figure 5. The commanded position is specified to the controller as a six-vector, and it is compared with the measured position estimate determined from the load cells and motor encoders. This comparison is used to calculate an eight-vector of desired motor positions, and the motor command is then generated from the desired motor positions using PID control. The motors then effect the model dynamics, which is then interpreted by the encoders and load cells, thus closing an 'inner loop' control. The load cell and encoder readings, as well as the estimated aerodynamic forces and model positions are recorded for data analysis. Independent synthetic jet commands can be defined by the user trigger relative to the desired motion, and the effect that the jets have on the aerodynamic forces and model motions are measured through the encoders and load cells as well. In addition, the motion analysis system is set up and calibrated for a much more accurate measurement of the model motion response, and is incorporated into the controller as an 'outer loop' which is used to modify the motor commands to minimize the error between the desired motion and the recorded motion in the camera system, which is done with a secondary PID control.

The system dynamic performance is characterized in terms of displacement range and frequency of sinusoidal commands and is depicted in Figure 6. The displacement amplitude is 50 mm plunge ($x$, $y$, and $z$), $8^\circ \alpha_x$, $9^\circ \alpha_y$, and $15^\circ \alpha_z$ at $f = 0.5$ Hz, and decreases to 5 mm ($x$, $y$, and $z$), $6^\circ \alpha_x$, $6^\circ \alpha_y$, and $3^\circ \alpha_z$ at $f = 20$ Hz. The range of motion decreases with frequency of motion because the inertial force grows proportional to the acceleration of the system, and increases the required torque of the motors at higher frequencies. This larger torque requires the tension in the mounting wires to increase with the same motion range, and because the load cells are limited to 110 N of tension, the range of the motion of the system has to decrease. The variation of the range of the model with the frequency of the motion is shown in Figure 6a for translational motion, and Figure 6b for rotational motions. The choice of the wire mounting location does not affect the translation range, but does affect the angular range, and these locations are chosen to maximize the range of pitch while still having significant authority in roll and yaw.

IV. Aerodynamic Response of the Centered Body

Before examining the model dynamics, a study was conducted to assess the synthetic jet control authority on generation of aerodynamic forces and moments on the axisymmetric body held stationary at the central position. This effect has been studied previously by McMichael et al.\textsuperscript{20}, Abramson et al.\textsuperscript{22,23}, and Lambert et al.\textsuperscript{24}, with a computational model, fixed model with reaction forces, and dynamically-enabled model with reaction forces, respectively.

Initially, a study of the drag of the model is conducted on the body held at center. The drag force was measured over a range of wind tunnel speeds as $F_D$. The expected wire drag is calculated treating the wires as cylinders with flow over them of the projected wind speed across the cylinder $F_W$. The value of the extracted platform drag ($F_D - F_W$) is plotted against the dynamic pressure multiplied by the platform cross-sectional area to resolve a
coefficient of drag \((C_D)\) of 0.238, with good agreement with the expected \(C_D\) of 0.25 from Hoerner\(^3\), as shown in Figure 7. The rest of the current investigation is conducted at the fixed free stream speed of \(U_0 = 40\:\text{m/s}\).

Next, the effects of the hybrid actuators are investigated with the jet momentum coefficient set to \(C_{\mu} = (U^2 \cdot A_j) / (U^2 \cdot \pi D^2) = 3 \cdot 10^{-3}\), and the actuation cycle frequency set to \(f_{\text{cycle}} = 1.4\:\text{kHz}\) \((t_{\text{cycle}} = 0.7\:\text{ms})\). When the synthetic jets are inactive, the model experiences separation off of each of the backward facing steps on the aft end of the model, and the jet actuation leads to the partial attachment of the outer flow along the Coanda surface resulting in a reaction force by the turning of the outer flow into the wake region. This vectored wake induces a reaction force and moment on the model, which is shown in Figure 8. The moment on the body throughout this work is measured relative to mounting center of wires on the body at \(x_m\). The response of a single actuator measured by the load cells is shown in Figure 8a, b, and c, showing the actuator induced forces, \(\Delta F_D = 0.03\text{N}\) and \(\Delta F_L = 0.6\text{ N}\), and the actuator induced moment, \(\Delta M_p = 0.008\:\text{Nm}\), which is in good agreement with previous studies by Abramson et al.\(^2,3\), and Lambert et al.\(^5\). When both jets are activated, the effect of the jets in \(\Delta F_L\) and \(\Delta M_p\) is canceled out (Figure 8d and e), while the actuator induced drag is approximately doubled to \(\Delta F_D = 0.06\text{N}\) (Figure 8f).

To understand the actuation-induced wake development while the axisymmetric model is centered, different actuation schemes are applied and the resulting flow fields are investigated with planar PIV. Figure 9 shows the ensemble-averaged flow field (based on 300 individual flow realizations) in terms of the mean velocity field vectors overlaid on a raster plot of the mean vorticity, \(\zeta\), at the central vertical plane of the model. Figure 9a shows the model wake without flow control, having expected symmetry about the body's axis. Although the flow field is axisymmetric (in a time-averaged sense), the upper and lower shear layers have a different sense of \(\zeta\) due to the Cartesian coordinate system utilized in the measurement plane for calculating \(\zeta\). Figure 9b shows the model wake with the top jet actuated, deflecting the wake downwards across the centerline and inducing a large asymmetry while decreasing the extent and magnitude of the region of \(\zeta\) near the jet. This wake deflection causes a reaction on the model with positive \(\Delta F_L\) and negative \(\Delta M_p\), as shown in Figure 8b. Figure 9c, showing both jets activated, causing the extent and magnitude of the region of \(\zeta\) near each jet to decrease, and vectors the wake on

\[
\begin{align*}
(C_D) & = 0.239 \\
\frac{\Delta F_D}{\Delta F_L} & = 1
\end{align*}
\]

\[
\begin{align*}
\Delta M_p & = 10^{-3} \\
\Delta F_D & = 0.06\:\text{N}
\end{align*}
\]
both side closer to the center, closing the wake. Because this flow field is again symmetric, there is no \( \Delta F_L \) or \( \Delta M_p \) on the model, and the \( \Delta F_D \) seen in Figure 8f can be attributed to the change in shape and magnitude of the wake velocity outside of the controlled perimeter.

V. Aerodynamic Response of the Moving Bluff Body

Once the centered model response has been assessed, the main investigation focuses on commanding a dynamic response from the body. The remainder of the present work focuses on sinusoidal pitch commands for the model motion. Figure 10 shows the measurement of the lift induced on the model commanded for sinusoidal pitching at \( f = 1 \) Hz (a reduced frequency of \( k = \pi cf/U_0 = 0.013 \)), and an amplitude of 3\(^\circ\). The force and moment traces presented here are the average of 100 seconds of data for noise suppression. The recorded lift force variation with pitching angle \( (F_L \) vs. \( \alpha_p \)) is plotted in Figure 10a, and the pitching moment trace \( (M_p \) vs. \( \alpha_p \) is shown in 10c. It is notable that both \( F_L \) and \( M_p \) increase with increasing \( \alpha_p \). In addition, a secondary traverse command is implemented with the same motion of the model with the top jet continuously actuated, where lift force induced solely by the actuation is plotted with pitching angle \( (\Delta F_L \) vs. \( \alpha_p \)) in Figure 10b, and the actuator induced pitching moment trace \( (\Delta M_p \) vs. \( \alpha_p \) is shown in 10d. Activation of the top actuator in Figures 10b and d vectors the wake downwards toward the center, causing a reaction force on the model with positive \( \Delta F_L \) and negative \( \Delta M_p \), where the magnitude of the \( \Delta F_L \) and \( \Delta M_p \) depends on \( \alpha_p \). Because \( F_L \) and \( M_p \) have the same sense and \( \Delta F_L \) and \( \Delta M_p \) have the opposite sense, actuation chosen to decrease \( F_L \) throughout the pitching cycle consequently increases \( M_p \). It is observed that the maximum magnitude of induced \( \Delta F_L = 0.9N \) and \( \Delta M_p = 0.01Nm \) by the top actuator occurs when the model is pitched down to \( \alpha_p = -1^\circ \), which is attributed to the geometry of the backward facing step, and the respective flow field on the model’s aft end. The present range of body orientations had been investigated prior by Lambert\(^{24}\) with the model held at a static angle of attack. In that work, \( F_L, \Delta F_L \) and \( M_p \) were recorded, and they are plotted in Figure 10a, c, and d in solid symbols. There is a small deviation in the \( \Delta F_L \) and \( \Delta M_p \) past the maximum values at \( \alpha_p < -1^\circ \), but the agreement is very strong everywhere outside of this region. In addition, the center of pressure \( (C_p) \) of the centered model is known to be about \( x/c = 0.24 \) upstream of \( x_m \), and the expected moment on the model \( (F_L, C_p) \) is plotted in Figure 10c in black. This measured moment has more hysteresis than the expected moment due to variations in the \( C_p \) over the cycle.

The primary emphasis of the current work is to assess the control authority of hybrid actuators on the present model when it is dynamically pitching. An important aspect of flow control implementation in dynamic configurations is the frequency response of both the hybrid actuators and the baseline flow. The onset of actuation induces \( \Delta F_L \) and \( \Delta M_p \) response that acts like a step function with a rise time of about 20 ms (50 Hz), as shown in Figure 8, with the magnitude of \( \Delta F_L \) and \( \Delta M_p \) on \( \alpha_p \) as well as its rate, \( \dot{\alpha_p} \). The frequency response of the baseline flow is analyzed using representative samples of \( f = 1, 5, 10, \) and \( 20Hz \) \( (k = 0.013,0.065,0.130, \) and \( 0.259 \), respectively) that span a range from pseudo-steady to quasi-unsteady frequencies, shown in Figure 11. The emphasis of this investigation is on using actuation for steering and stabilization of an airborne model in pitch motion, and therefore the rest of this work places emphasis on the lift force trace, \( F_L \) vs. \( \alpha_p \) and pitch moment trace, \( M_p \) vs. \( \alpha_p \). Furthermore, as the pitch motion is detected as representative for the flow control assessment, only two control jets are utilized in the remaining sections of the paper, namely the top and bottom jets. Figure 11a and e show \( F_L \) vs. \( \alpha_p \) and \( M_p \) vs. \( \alpha_p \), respectively, of the model pitching at \( k = 0.013 \), as the 'low' frequency representative, which was shown to be in agreement with previous static data in Figure 10a and e. Figure 11b and e show the
Figure 11. Motion induced lift (a–d) and moment (e–h), development with pitch angle, $\alpha$, at reduced frequencies $k = 0.013$ (a,e), 0.065 (b,f), 0.130 (c,g), and 0.259 (d,h).
Figure 12. Raster plots of the phase-averaged vorticity field with overlaid phase-averaged velocity vectors for \( \alpha_y = \pm 3^\circ \) sinusoidal pitch at reduced frequencies of \( k = 0.013 \) (a-f), and 0.259 (g-l), at \( t/\tau = 0 \) (a,g), 0.083 (b,h), 0.167 (c,i), 0.25 (d,j), 0.333(e,k), and 0.417(f,l).

VI. Aerodynamic Force and Moment Control using Continuous Actuation

After the aerodynamic response from the body’s baseline flow is assessed, the flow control actuation schemes are designed. To further understand the control authority of the hybrid actuation throughout a pitching cycle, the effect of continuous actuation by either the top or both the top and the bottom jets is examined over a range of \( k \). The continuous actuation by a single (top) jet and dual (top and bottom) jets is shown in red and blue, respectively, in Figure 13 with the baseline response shown as a dotted line for reference. Figures 13a and e show the response at \( k = 0.013 \) with significant \( \Delta F_L \) and \( \Delta M_P \) by one jet throughout the cycle, with a maximum effect at \( \alpha_y = -1^\circ \), in agreement with Figure 10a and c. When both jets are activated, the result is a \( F_L \) vs. \( \alpha_y \) and \( M_P \) vs. \( \alpha_y \) that is similar to the baseline case, showing that both jets impose equivalent but opposing effects and hence effectively cancel out the induced effect of each other. Figures 13b and f show the response at \( k = 0.065 \), where there are several noticeable differences from the lowest \( k = 0.013 \), where the \( F_L \) vs. \( \alpha_y \) and \( M_P \) vs. \( \alpha_y \) path of both the single top jet and two jets has changed. It is noteworthy that although the \( F_L \) vs. \( \alpha_y \) and \( M_P \) vs. \( \alpha_y \) paths have changed from the
lower frequency, the continuous actuation traces remain similar to the baseline paths, with the top jet path deflected to a larger $F_L$ and lower $M_P$ and the both jets path centered about the baseline paths. This trend continues in $k=0.130$ (shown in Figures 13c and g) and $k=0.259$ (shown in Figures 13d and h) with the actuated paths remaining similar to the non-actuated paths, and the activation of one jet causing a deflection of the path, with the largest variation occurring in the $F_L$ vs. $\alpha_y$ path at $k=0.130$ (Figure 13c). It is also noteworthy that at the frequencies $k=0.065$ and 0.130 the actuation effect changes to an induced $\Delta F_L = 0.6N$ and $\Delta M_P = -0.02Nm$ approximately independent of $\alpha_y$ (Figures 13b,c,f and g) rather than an effect of $\Delta F_L = 0.9N$ and $\Delta M_P = -0.01Nm$ at a maximum $\alpha_y=3^\circ$ in the 'low' frequency of $k=0.013$ (Figures 13a and e). This change is attributed to the model moving fast enough that the flow doesn't have enough time to fully respond to the 'small' geometry of the backward facing step, and there is no longer a dependence between the pitching angle and this local geometry. In the 'high' frequency case $k=0.230$ again starts to vary with $\alpha_y$, having a maximum effect of $\Delta F_L = 0.5N$ and $\Delta M_P = -0.015Nm$ at $\alpha_y=3^\circ$ (Figure 13 d and h). The emergence of this maximum at $\alpha_y=3^\circ$ at $k=0.259$ is attributed to the model moving fast enough such that the flow cannot fully respond to the model dynamic cycle, and therefore some parts of the pitching cycle become more favorable for actuation than other parts due to the altered shear layer dynamics. It is notable that the hysteresis in both the baseline and actuated responses in Figure 13 increase the pitching cycle with oscillation frequency past $k=0.065$, which is due to the flow development (function of $\tau_{comb}$) taking up a larger percentage of the cycle, $\tau$, but the nature of the flow development (hence path of $F_L$ and $M_P$) is similar for the baseline and actuated cases with a fixed frequency. Although the actuation effects differ for different frequencies, it is important to note that this continuous actuation has a sizable effect across all the pitch frequencies tested, causing an average deflection on the order of $\Delta F_L \approx 0.6N$ and $\Delta M_P \approx -0.01Nm$ with one jet, and effectively cancelling this deflection with both jets, and preserving similar $F_L$ vs. $\alpha_y$ and $M_P$ vs. $\alpha_y$ paths with respect to the baseline flow in all cases.

To understand the similarity of the actuation-induced wake development with frequency while the axisymmetric model is pitching with an amplitude of $3^\circ$, the baseline, top jet, and both jets continuous actuation flow fields are investigated with planar time-averaged PIV. Figure 14 shows the time-averaged flow field (based on 500 individual flow realizations) in terms of the mean velocity field vectors overlaid on a raster plot of $\zeta_c$ at the central vertical.

![Figure 13. Lift force (a-d) and pitching moment (e-h) for open-loop continuous activation of one (red) and both (blue) jets at $C_p = 3 \cdot 10^{-3}$ and $k = 0.013$ (a,e), 0.065 (b,f), 0.130 (c,g), and 0.259 (d,h). The non-actuated cases are shown in dashed (black) lines.](image-url)
plane of the model. Figures 14 a-c show the model wake with $k = 0.013$, and Figures 14 d-f show the model wake with $k = 0.259$. Figures 14a and d show the model wake without flow control, having expected symmetry about the body's axis of symmetry, and remaining similar to Figure 9a when the model was held stationary, with an added diffusion of the shear layer originating from the dynamic motion of the model. The similarity between Figure 14a and Figure 14d suggest that the shear layer deflection magnitude is similar regardless of pitching frequency, with the major difference being the phase lag of the wake response, which is observed in Figure 12. Figure 14b shows the model wake with the top jet actuated, deflecting the wake downwards across the centerline and inducing a large asymmetry while decreasing the extent and magnitude of the region of $\zeta$ near the jet, analogous to Figure 9b. Figure 14c shows both jets activated, causing the extent and magnitude of the region of $\zeta$ near each jet to decrease, and vectors the wake on both side closer to the center, thereby closing the wake, analogous to Figure 9c. The similarity between Figures 14b and e with Figure 9b, and Figures 14c and f with Figure 9c, suggest that the shear layer deflection with jet actuation is also similar regardless of pitching frequency, which also suggests $\Delta F_L$ vs. $\alpha_\gamma$ and $\Delta M_P$ vs. $\alpha_\zeta$ is comparable to $F_L$ vs. $\alpha_\gamma$ and $M_P$ vs. $\alpha_\zeta$.

VII. Aerodynamic Force and Moment Control using Modulated Actuation

Results shown in Figures 13 and 14 clearly demonstrates that the hybrid actuation approach has a significant control authority throughout the full range of the pitching dynamics $0 < k < 0.259$. The main objective of the present study is to apply such a control approach to control the aerodynamic forces ($F_L$ vs. $\alpha_\gamma$ and $M_P$ vs. $\alpha_\zeta$ paths) on the body undergoing dynamic pitch. It should be noted that the decrease of $M_P$ growth with $\alpha_\zeta$ can be used to accelerate steering. This is a coupled system where the baseline $F_L$ and $M_P$ are both increasing with $\alpha_\gamma$, yet the $\Delta F_L$ and $\Delta M_P$ by the actuators are in opposite senses, therefore increasing growth of $M_P$ vs. $\alpha_\zeta$ (for accelerated steering) simultaneously reduces growth of $F_L$ vs. $\alpha_\gamma$, and likewise decreasing growth of $M_P$ vs. $\alpha_\zeta$ (for stabilization) simultaneously increases growth of $F_L$ vs. $\alpha_\gamma$. As it is assessed from Figure 13, the control authority $\Delta F_L$ relative to the baseline path of $F_L$ was larger than $\Delta M_P$ relative to the baseline path of $M_P$, and therefore it is chosen to focus the flow control on augmentation of the $F_L$ vs. $\alpha_\gamma$ path as a case that can be used for stabilization, and cancellation of the $F_L$ vs. $\alpha_\zeta$ path as a case that can be used for accelerated steering. As it was found previously by Lambert et al., the $\Delta F_L$ and $\Delta M_P$ induced by an equivalent synthetic jet varied linearly with $C_\mu < 3 \cdot 10^{-3}$, and such a relationship is utilized in the current control scheme. Modulation commands, $M_{TOP}$ and $M_{BOT}$, are tailored for each jet to each of the baseline force responses of Figure 11a-d with a jet momentum of $C_\mu = 3 \cdot 10^{-3}$ corresponding to 100% modulation, which is commensurate with
Figure 15. Synthetic jet modulation schemes for force augmentation (a-d), and estimated actuator induced force (e-h) with top jet in blue and bottom jet in red for $k = 0.013$ (a,e), 0.065 (b,f), 0.130 (c,g), and 0.259 (d,h). The non-actuated force response is shown in dashed black. The modulation schemes for force cancellation are $180^\circ$ out of phase.

Figure 16. Lift force (a-d) and pitching moment (e-h) for aerodynamic force cancellation (blue) and augmentation (red) with $k = 0.013$ (a,e), 0.065 (b,f), 0.130 (c,g), and 0.259 (d,h). Baseline responses are shown in dashed black.

$a \Delta F_L \sim 0.6N$, based on Figure 13. Figure 15a-d shows the resultant jet modulation signals with varying $k = 0.013$ (Figure 15a), 0.065 (Figure 15b), 0.130 (Figure 15c) and 0.259 (Figure 15d), where the only free parameter of variation used was the phase of the modulation signals. Here the top jet is shown in blue and the bottom jet is shown in red for force augmentation, although the modulation command for force cancellation can be generated by running the jets in the opposite fashion with the top jet in red and the bottom jet in blue. The corresponding
predicted augmented $\Delta F_L$ using a maximum $\Delta F_L \sim 0.6N$ at 100% jet modulation for each jet, $\Delta F_{L, \text{TOP}}$ and $\Delta F_{L, \text{BOT}}$, are shown in Figure 15e-f for these same values of $k$. The phase of these modulation commands was chosen such that the predicted augmented $F_L$ is most similar to the baseline $F_L$, leading to the chosen modulation command phase lags of $5^\circ$ ($k = 0.013$, Figure 15a and e), $15^\circ$ ($k = 0.065$, Figure 15b and f), $25^\circ$ ($k = 0.130$, Figure 15c and g), and $40^\circ$ ($k = 0.259$, Figure 15d and h), relative to $\alpha$.

The results of the augmentation control schemes, as described in Figure 15, and the corresponding out of phase cancellation schemes, are shown in Figure 16 in red and blue, respectively, with the baseline response overlaid in a dashed line. The response of $F_L$ vs. $\alpha$, is shown in Figures 16a-d, and the response of $M_p$ vs. $\alpha$, is shown in Figures 16e-h with varying $k$. At the lowest $k = 0.013$ (Figure 16a), the maximum $F_L$ is cancelled to 0.45 N or augmented to 1.3 N from its baseline value of 0.9 N, leading to a 50% decrease or a 45% increase in the force peaks. The respective case of $M_p$ vs. $\alpha$, in Figure 16e shows a 30% increase which can be used for model steering authority and a 65% decrease in the moment peaks which can be used for model stabilization authority. For $k = 0.065$ (Figure 16b and f), the $F_L$ augmentation case leads to a 55% increase in $F_L$ with a 50% decrease in $M_p$ and the $F_L$ cancellation case leads to a $F_L$ decrease of 65% and an $M_p$ increase of 33%. Overall, the control authority on the $F_L$ vs. $\alpha$, increased and $M_p$ vs. $\alpha$, decreased from $k = 0.013$ to 0.065. At $k = 0.130$ (Figure 16c and g), the control authority on $F_L$ decreases slightly from $k = 0.065$, with a 50% increase and a 60% decrease, and the control authority on $M_p$ becomes the respective 20% decrease and a 55% increase. The resulting $F_L$ vs. $\alpha$, cancellation and augmentation effects remain significant at $k = 0.259$ (Figure 16d and h), with induced changes on $F_L$ of a 105% increase and a 50% decrease, with the respective changes on $M_p$ of a 20% decrease and a 40% increase. For the two lowest $k$ (Figure 16a,b,e,and f) the cancelled and augmented paths show a hysteresis comparable to the baseline flow, but for $k > 0.065$ (Figure 16c,d,g, and h), the cancellation of $F_L$ vs. $\alpha$, decreases the force path hysteresis (decreases the lift response time relative to the cycle), and the augmentation of the $F_L$ vs. $\alpha$, increases the force path hysteresis (increases the lift response time relative to the cycle). It is noteworthy that the hysteresis in $M_p$ vs. $\alpha$, is of the opposite sense to the force, and therefore increasing the hysteresis of the $F_L$ vs. $\alpha$, decreases the hysteresis of the $M_p$ vs. $\alpha$, path, and vice versa.

The 'low' frequency oscillation flow control cases are analyzed in detail with the phase-averaged flow fields measured by PIV. Figure 17 shows six phase-averaged flow fields (based on 300 individual phase-referenced realizations) in terms of the velocity field vectors overlaid on a raster plot of $\zeta$ at the central vertical plane of the model for $k = 0.013$ ($\tau = 1$s) with the $F_L$ vs. $\alpha$, cancellation (Figure 17a-f), and augmentation (Figure 17g-l). Similar to the baseline cycle, both the cancellation and augmentation actuation flow fields are symmetric across the pitching cycle, and therefore the phases chosen for brevity are representative for the first half cycle, similar to Figure 12: $t/\tau = 0$ (Figure 17a and g), 0.083(Figure 17b and h), 0.167(Figure 17c and i), 0.25(Figure 17d and j), 0.333(Figure 17e and k), and 0.417(Figure 17f and l). Based on Figure 15, the actuation chosen for this value of $k$ lags $\alpha$, in phase by $5^\circ$. In Figure 17a, $\alpha$, has reached $0^\circ$ and the top jet is weakly actuated. As the model pitches up to $3^\circ$ in Figure 17b-d, the bottom actuation is activated and increases in strength, causing the domain of counterclockwise $\zeta$ to increase, and the actuation then decreases from $17d$-f, causing the domain of counterclockwise $\zeta$ to decay. The presence of actuation in Figure 17a-f causes the location of zero $\zeta$ to remain closer to the centerline and causes the velocity field to remain more symmetric about the centerline compared to the baseline flow (compare Figure 17a-f with Figures 12a-f), commensurate with the smaller peaks observed in the $F_L$ vs. $\alpha$, path in Figure 16a. For $F_L$ vs. $\alpha$, augmentation (Figure 17g-l), the opposite modulation command to the actuators is sent. Initially in Figure 17g, the bottom actuator is active, and between the next phase shown in Figure 17h the bottom actuator is terminated and the top actuator is activated. The top actuator's strength increases from Figure 17h-j and then decreases from Figure 17j-l. The presence of actuation in Figure 17g-l causes the location of zero $\zeta$ to deflect farther from the centerline and causes the velocity field to remain less symmetric around the centerline compared to the baseline flow (compare Figure 17g-l with Figures 12a-f), commensurate with the enhanced peaks observed in the $F_L$ vs. $\alpha$, path in Figure 16a.

To further understand the time varying 'low' frequency wake dynamics of both the controlled and uncontrolled flows, the time development of streamwise velocity, $u_z$, cross-stream velocity, $u_x$, and the planar vorticity, $\zeta$, in the wake behind the model is assessed. These quantities are characterized at a representative fixed downstream distance of $x/R = 1$ behind the model aft end for $0 < t/\tau < 1$. Figure 18a shows the development of the $u_z$ component of the wake velocity when there is no actuation present, where $u_z$ responds to sinusoidal pitch cycle. The $u_z$ component of the wake velocity is shown in Figure 18d, where $u_z$ is mostly downward for $t/\tau < 0.5$, corresponding to the wake
Figure 17. Raster plots of the phase-averaged vorticity field with overlaid phase-averaged velocity vectors for $\alpha_y = \pm 3^\circ$ sinusoidal pitch at reduced frequency $k = 0.013$ using force cancellation (a-f), and augmentation (g-l) at $t/\tau = 0$ (a,g), 0.083 (b,h), 0.167 (c,i), 0.25 (d,j), 0.333(e,k), and 0.417(f,l).

Following the model and being vectored downward when $\alpha_y > 0$, and mostly positive for $t/\tau > 0.5$, which corresponds to the wake being vectored by the model upward when $\alpha_y < 0$. Figure 18g shows the development of $\zeta$ for the baseline flow, which also, as expected, follows a sinusoidal path similar to $u_x$. Upon $F_L$ vs. $\alpha_y$ cancellation actuation, the wake's variation in $u_x$ decreases significantly throughout the cycle from the baseline, as shown in Figure 18b, and conversely, the wake's variation in $u_x$ increases significantly with the $F_L$ vs. $\alpha_y$ augmentation actuation, as shown in Figure 18c. There is a different effect in the vertical direction, where $F_L$ vs. $\alpha_y$ cancellation (Figure 18e) causes the magnitude of the $u_z$ to decrease significantly across the entire region throughout all times, and the $F_L$ vs. $\alpha_y$ augmentation (Figure 18f) enhances the $u_z$ development seen in Figure 18d. The response of $\zeta$ to $F_L$ vs. $\alpha_y$ cancellation is shown in Figure 18h, with a growth in the area of the shear layer upon actuation, with the bottom actuator present for $t/\tau < 0.5$ and the top actuator present for the other half of the cycle. There is a notable decrease in the deviation of the zero level of $\zeta$ with time that was observed in Figure 17a-f, which is commensurate with the decreased force measured in Figure 16a. The $\zeta$ response to $F_L$ vs. $\alpha_y$ augmentation instead involves a strong
Figure 18. Time development of streamwise (a-c) and cross-stream (d-f) velocity and vorticity (g-i) at a streamwise location of $x/R = 1$ from the model aft end for $\alpha_y = \pm 3^\circ$ sinusoidal pitch at reduced frequency of $k = 0.013$, without actuation (a,d,g), and for the force cancellation (b,e,h) and augmentation (c,f,i).

Vectoring and spreading of the shear layer and an increase in the $\xi$ found in the wake, which increases the deviation of the zero level of $\xi$ in the wake, commensurate with the augmentation of force measured in Figure 16a.

The 'high' frequency, $k = 0.259$, oscillation flow control cases are also analyzed in detail with the phase averaged flow field measured by PIV, and represented in the same fashion as Figure 17, over half of the pitching cycle. Based on Figure 15, the actuation chosen for this value of $k$ lags $\alpha_y$ in phase by $45^\circ$. In Figure 19a, $\alpha_y$ has reached $0^\circ$ and the top jet is moderately actuated. As the model pitches up through Figures 19a-b ($0 < t/\tau < 0.083$), the top actuation is decreased, and transitions to the bottom jet, with increasing intensity in Figure19c-e ($0.167 < t/\tau < 0.333$). Between Figures 19e-f ($0.333 < t/\tau < 0.417$), the bottom actuation begins to decrease. It should be noted that although the modulation at this 'high' frequency lags the modulation at the 'low' frequency by $40^\circ$, the flow observed through Figures 19a-f remains more symmetric then the baseline cases shown in Figure 12g to Figure 12l. This is owing to the aerodynamic force and moment response on the model also inherently lagging the model motion with a similar delay. For 'high' frequency $F_L$ vs. $\alpha_y$ augmentation (Figure 19g-l), the opposite modulation command to the actuators is sent as in Figure 19a-f. Initially, the model shear layer is deflected upward as it is lagging the model position, and the bottom jet is moderately actuated in Figure 19g. As the model pitches up through Figures 19g-i ($0 < t/\tau < 0.167$), the bottom actuation is terminated and transitions to increasing top actuation
in Figures 19c-e (0.167 < \tau < 0.333). In the time instances shown in Figures 19e-f (0.333 < \tau < 0.417), the modulation of the top actuation is reduced. Comparing Figures 19g-l and Figures 12g-l show the increased time of transition of the wake \( \zeta \) is commensurate with the growth of hysteresis in Figure 16d, as well as the increased extent of the shear layer is commensurate with the growth of the \( F_L \) peaks during actuation.

A detailed investigation analogous to the ‘low’ frequency pitch oscillations is conducted at the highest \( k \) to gain a better understanding of the ‘high’ frequency wake dynamics of the \( F_L \) vs. \( \alpha_y \) cancellation and augmentation, and their similarities and differences to the ‘low’ frequency case. To do this, the time development of \( u_x, u_z, \) and \( \zeta \) are presented for the ‘high’ frequency \( k = 0.259 \), showing the same comparisons as for the ‘low’ \( k = 0.013 \) (Figure 18). Figure 20a shows the development of \( u_x \) when there is no actuation present, where \( u_x \) responds as a sinusoid with the same \(-45^\circ\) phase lag behind \( \alpha_y \). Figure 20d shows \( u_x \) development in the baseline flow, with a different structure than what was observed in the lower frequency case in Figure 18d. Here, \( u_x \) is mostly downward when \( u_z \) is deflected downward, and \( u_z \) is mostly upward when \( u_z \) is deflected upward, showing the same \(-45^\circ\) phase lag from
Figure 20. Time development of streamwise (a-c) and cross-stream (d-f) velocity and vorticity (g-i) at a streamwise location of x/R = 1 from the model aft end for $\alpha_y = \pm 3^\circ$ sinusoidal pitch at reduced frequency of $k = 0.259$, without actuation (a,d,g), and for the force cancellation (b,e,h) and augmentation (c,f,i).

$\alpha_y$, observed in $u_x$. It is also notable that the magnitude of the baseline $u_x$ is significantly smaller at this higher frequency (compare 18d and 20d). The baseline $\zeta$ development over the pitching cycle is shown in Figure 20c, which follows $\alpha_y$ with $\sim 45^\circ$ phase lag in agreement with the streamwise and vertical velocity components. The structure of the $\zeta$ agrees well with the structure observed in the 'low' k pitching. Upon $F_L$ vs. $\alpha_y$ cancellation actuation, the wake's variation in $u_z$ decreases significantly throughout the cycle from the baseline, as shown in Figure 20b, and conversely, the wake's variation in $u_x$ increases significantly with the $F_L$ vs. $\alpha_y$ augmentation actuation, as shown in Figure 20c, in the same fashion as $k = 0.013$, with the exception that $u_z$ now lags the model motion (compare Figures 20a-c with Figures 18a-c). At this 'high' frequency oscillation, there is a smaller effect in the vertical direction, compared to low frequencies, where $F_L$ vs. $\alpha_y$ cancellation (Figure 20e) does not change the magnitude of $u_z$, but instead slightly delays its development in time. The $F_L$ vs. $\alpha_y$ augmentation (Figure 20f) dominates the baseline $u_z$ development seen in Figure 20d, and causes an increase in magnitude of $u_z$. The response of $\zeta$ to $F_L$ vs. $\alpha_y$ cancellation is shown in Figure 20h, which manipulates the sense of $\zeta$ such that the region of zero $\zeta$ remains close to the centerline. Comparing Figure 20h to Figure 18h, the suppression of the vertical range of zero $\zeta$ is fundamentally similar, but the shear layer spreading is reduced. Although the structure is somewhat different, as the vertical range of zero $\zeta$ is still suppressed, the corresponding $F_L$ vs. $\alpha_y$ on the body is also successfully reduced.
The $\zeta$ response to $F_L$ vs. $\alpha$ augmentation (Figure 20i) instead involves an increase in the $\zeta$ in the wake, which increases the deviation of the zero level of $\zeta$ in the wake, commensurate with the augmentation of force measured in Figure 16d. The two major differences between Figure 20i and Figure 18i are the phase lag of the response of the wake, and the reduced spreading of the shear layer.

VIII. Conclusions

The present experimental work is focused on fluidic flow control applications to a moving aerodynamic platform, and coupling to and altering of the resulting aerodynamic forces and moments. The flow control concepts are demonstrated on an axisymmetric model having a prescribed sinusoidal pitch oscillations at a range of reduced frequencies $0 < k < 0.259$. In contrast to the prior work by Lambert et al.\textsuperscript{25}, where the flow control was applied to either suppress or augment a body motion which was free to oscillate under the aerodynamic moment in a single degree of freedom system, this study focuses on control of the motion-induced aerodynamic forces and moments while the body’s dynamic path is prescribed and preserved. For that purpose, a novel six degree of freedom traversing system is designed and implemented.

The wind-tunnel model is suspended by eight wires with custom servo actuators utilized to control the end points and tensions in each wire. This mechanism is designed to move all the wires in patterns that can cause rotation in three independent axes: roll ($\pm 8^\circ$), pitch ($\pm 15^\circ$), and yaw ($\pm 9^\circ$), as well as the respective independent translations: streamwise, cross-stream, and vertical, all within a range of $\pm 50$ mm. Each wire has an incorporated load cell that resolves the tension, and thereby captures the aerodynamic forces and moments on the model. Motion is executed by an in-house real-time PID controller that provides signals to the servo actuators as commanded. The executed motion is recorded by an external six degree of freedom Vicon camera system (515 fps), which output is fed into the controller as a feedback signal to maximize the accuracy of motion. The real-time control system utilizes two Quanser data acquisition boards.

The traverse-driven model motion is tested in multiple degrees of freedom, and it could generate desired complex trajectories, comprised of the combined translational and rotational motions. These trajectories could be realized with minimal error compared to the commanded motion, being executed in a PID control loop having feedback from the motion analysis system. In the present work, the flow control capabilities are demonstrated on the model undergoing sinusoidal pitch oscillations. The flow control is effected by either continuous or modulated activation of two synthetic jets that emanate from narrow, azimuthally-segmented slots on the model's aft end, opposite to each other at its top and bottom surface. This main active flow control component is assisted by passive modifications of the surface geometry that involve a short backward-facing step and trailing Coanda surface, and this tandem utilization of active and passive flow control is often coined as ‘hybrid’ flow control. The flow control actuation effects by the synthetic jets are assessed relative to the lift force and pitch moment induced on the model when it is held stationary at center, as well as when it is commanded to pitch dynamically.

As the pitch-induced aerodynamic force and moment on the body are of the same sense and the pitch-induced moment acts to further increase the pitch angle, this baseline moment is essentially ‘unstable’. In contrast, the actuation by either top or the bottom jet induces a normal force and moment of opposite signs. Hence, depending on which jet is activated, flow control is utilized to either suppress the aerodynamic moment (and augment the force) or augment the moment (and suppress the force). These actuation realizations are then implemented into an open-loop flow control scheme with adjustable phase offset of actuation relative to the model’s period of oscillation. The resulting suppressed/enhanced pitch moment/lift force (and vice versa) flow control schemes are executed during the sinusoidal pitch motion, indicated a robust flow control effect over the full range of the pitch frequencies ($0 < k < 0.259$). Typical alteration of the aerodynamic force and moment was approximately 50% of the uncontrolled levels. It is argued that these flow control schemes can be utilized for accelerated steering or stabilization of an equivalent airborne body.

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References


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