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as of 06-Mar-2020

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Major Goals: The goal of the 3-year experimental research program was to elucidate the unsteady flow mechanisms of aerodynamic control of the reciprocal coupling between a free-moving axisymmetric bluff body and its wake instabilities using fluidic actuation at the moving flow boundary. Building on earlier findings at Georgia Tech which had demonstrated that fluidic actuation can affect the global aerodynamic forces and moments on a stationary body, the present research hypothesizes that active alteration of the wake of a moving body can enable prescribed modification of the time-dependent aerodynamic loads (forces and moments), and, consequently, can lead to active control of its motion and stability. The focus of the planned investigations was to explore the effects of the actuation on the coupling between the embedding flow and the platform in the absence and presence of direct feedback between the flow the platform's motions in 'rigid' and 'free' motions, respectively. The planned investigations would assess the effects of both open- and closed-loop actuation strategies for enhancement or suppression of the inherent flow coupling on the evolution and stability of the near wake and the induced unsteady aerodynamic loads on the body.

Accomplishments: Please see uploaded file.

Training Opportunities: One PhD Student, Dr. Thomas Lambert.

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Lambert, T. J., Vukasinovic, B., and Glezer, A. Aerodynamic Flow Control of Axisymmetric Bluff Body by Coupled Wake Interactions, *AIAA J.* 56, pp. 2992-3007, 2018.
Lambert, T.J., Vukasinovic, B., and Glezer, A., A Six Degrees of Freedom Dynamic Wire-Driven Traverse, *Aerospace* 3:11, 2016.
Lambert, T.J., Vukasinovic, B., and Glezer, A., Aerodynamic Flow Control of Wake Dynamics Coupled to a Moving Bluff Body, *AIAA Paper* 2016-4081.
Lambert, T.J., Vukasinovic, B., and Glezer, A., Coupled Aerodynamic Control of the Turbulent Wake of a Moving Bluff Body, *Proc. ETMM11, ERCOFTAC Symposium*, 2016.
Lambert, T.J., Vukasinovic, B., and Glezer, A., Aerodynamic Control of Coupled Body-Wake Interactions, *AIAA Paper* 2016-0053.
Lambert, T.J., Vukasinovic, B., and Glezer, A., Active decoupling of the axisymmetric body wake response to a pitching motion, *J. Fluids Struct.* 59, pp. 129-145, 2015.
Lambert, T.J., Vukasinovic, B., and Glezer, A., Aerodynamic Flow Control of an Axisymmetric Body in Prescribed Maneuvers, *55th IACAS Conference Proceedings*, 2015.
Lambert, T.J., Vukasinovic, B., and Glezer, A., Unsteady Aerodynamic Loads Effected by Flow Control on a Moving Axisymmetric Bluff Body, *AIAA Paper* 2015-0827.

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Participant: Bojan Vukasinovic

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Article Title: A Six Degrees of Freedom Dynamic Wire-Driven Traverse

Authors: Thomas Lambert, Bojan Vukasinovic, Ari Glezer

Keywords: model support; 6-DOF; wind tunnel testing; aerodynamics; wakes

Abstract: A novel support mechanism for a wind tunnel model is designed, built, and demonstrated on an aerodynamic platform undergoing dynamic maneuvers, tested with periodic motions up to 20 Hz. The platform is supported by a 6-DOF (six degrees of freedom) traverse that utilizes eight thin wires, each mounted to a servo motor with an in-line load cell to accurately monitor or control the platform motion and force responses. The system is designed such that simultaneous control of the servo motors effects motion within ± 50 mm translations, $\pm 15^\circ$ pitch, $\pm 9^\circ$ yaw, and $\pm 8^\circ$ roll at lower frequencies. The traverse tracks a desired trajectory and resolves the induced forces on the platform at 1 kHz. The effected motion of the platform is measured at 0.6 kHz with a motion capture system, which utilizes six near-infrared (NIR) cameras for full spatial and temporal resolution of the platform motion, which is used for feedback control. The traverse allows different platform model geometries to be tested, an

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Authors: Thomas J. Lambert, Bojan Vukasinovic, Ari Glezer

Keywords: flow control, bluff body, wake

Abstract: The stability and steering of nonspinning airborne bodies of revolution have traditionally relied on stationary or moving fins that affect controlled interactions with the oncoming flow to modify the inherent aerodynamic loads. An alternative approach for manipulation of these loads in the absence of external control surfaces can be realized by exploiting the reciprocal coupling between a moving bluff body and its near wake by either breaking or restoring the natural azimuthal near-wake symmetry using segmented fluidic actuation. The present paper reviews fluidic actuation of the near-wake dynamics by using an azimuthally segmented four-quadrant array of aft-facing synthetic jet modules around the tail end of an axisymmetric bluff-body model. Each actuator controls a segment of the aft separated vorticity layer using a hybrid configuration with a passive Coanda surface. The actuation allows for controlled suppression or enhancement of near-wake asymmetries and consequently of the assoc

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Aerodynamic Control of Coupled Body-Wake Flow Instabilities on a Free-Moving Platform

Final Project Report

April 29, 2015 - September 30, 2019

submitted by

Ari Glezer

**Woodruff School of Mechanical Engineering
Georgia Institute of Technology**

Program Overview

This ARO research program focused on modification of the coupled instability mechanisms between the flow over a moving axisymmetric bluff body and its near wake by using controlled flow perturbations on the moving flow boundary. This approach enabled prescribed modification of the time-dependent aerodynamic loads on the moving body, and, consequently active control of its motion and stability. Manipulation of the coupling between the flow over the body and the evolution and stability of the near-wake and consequently of the global aerodynamic loads was realized using fluidic actuation at the flow boundary in two limits namely, rigid motion in which the actuation-induced changes do not affect the motion of the platform, and “free flight” which enables feedback between the induced changes and the motion of the platform.

The wind tunnel investigations utilized a moving axisymmetric wire-mounted axisymmetric bluff body integrated with individually-addressable synthetic jet actuators. The model was driven in prescribed canonical time-dependent motion using a programmable 6-DOF (x/y/z-translation & pitch/yaw/roll) 8-wire traverse developed under an ARO DURIP Program. The interactions between the actuation and the cross flow and their coupling to the stability of the near wake was investigated using particle image velocimetry (PIV), a motion analysis system, and time-resolved aerodynamic forces and moments.

Following this overview, brief technical background (Section I) and description of the model and experimental setup (Section II), and the report includes three sections that summarize the results of the present technical work. Section III describes fluidic control of the wake instabilities when the model is rigidly driven in prescribed coupled pitch-yaw rotation. Section IV focuses on control authority of fluidic actuation when the model is free to respond to induced aerodynamic loads in three degrees of freedom (pitch, yaw, and roll). Finally, Section V describes preliminary investigations of flow control effectiveness on the forebody vortices of an axisymmetric body at high incidence and the associated aerodynamic loads.

I. Technical Background

Numerous investigations have utilized active fluidic control to manipulate the local and global instabilities of the near wake and thereby effect significant changes in the flow-induced aerodynamic loads. Significant alterations of bluff body wakes have been achieved by passive control. An example that is particularly relevant to the present investigations is flow attachment along rounded aft solid surfaces, exploiting the Coanda effect that has been investigated extensively since the 1960s (e.g., Newman, 1961). Bypass or suppression of shed vortices using passive elements such as strakes, shrouds, fairings, and plates are discussed in the review articles by Zdravkovich (1981) and by Every, King, and Weaver (1982). Other examples of the utilization of passive devices for affecting flows over bluff bodies include fins for altering the magnus characteristics of a projectile (e.g., Platou, 1965), a secondary off surface control cylinder for suppressing the locally unstable region in wakes (Strykowski and Sreenivasan, 1990), dimples for drag reduction and transition control (Bearman and Harvey, 1993), etc., as reviewed in a recent article on bluff body flow control by Choi, Jeon, and Kim (2008).

Considerable research effort involving active flow control has been devoted to controlling the wake of 2D bluff bodies to alter vortex shedding or the location of separation, or for drag reduction. An early example of ‘hybrid’, or combined passive and active control on a stationary 2D bluff body was demonstrated by Nagib, Reisenthel, and Koga (1985) who combined a small-scale (2-6 cm) backward facing step with an active flap to control local separation. Hsiao, Liu, and Shyu (1990) demonstrated a reduction in drag and increase in lift on a 2D circular cylinder using internal acoustic actuation through a slot placed near the separation point at an excitation frequency that was near the vortex shedding frequency. Active separation control strategies have implemented actuators that either coupled to the narrow-band receptivity of the near wake instability (e.g., unsteady bleed on a cylinder by Williams et al., 1991), or using a decoupled approach with an actuation frequency that is at least an order of magnitude higher (e.g., synthetic jets on a cylinder by Honohan, Amitay, and Glezer, 2000). Amitay et al. (1997) demonstrated that the latter approach effects the global flow instabilities by fluidic modification of the “apparent” aerodynamic shape of a 2-D circular cylinder using a synthetic jet. The utility of this approach was demonstrated in a related later investigation by Amitay et al. (2001) who reported 100% increase in lift, and 45% decrease in drag on an elliptic airfoil.

Flow control has also been demonstrated on 3-D bluff bodies. Freund and Mungal (1994) achieved up to 30% decrease in the aerodynamic drag of an axisymmetric body by induced attachment at the aft end using steady, circumferential blowing over a Coanda surface. Another example of hybrid flow control was demonstrated by Sigurdson (1995) who used a sharp leading edge on a streamwise circular cylinder to induce local separation that was controlled by jet injection through an azimuthal downstream slot. Englar (2000) demonstrated control of separation by jet injection on the aft surface of bluff body models for road vehicles, achieving drag reduction of about 35% and thereby increasing their fuel economy. In an investigation that is particularly relevant to the present study, Rinehart, McMichael, and Glezer (2003) utilized synthetic jet actuation on the aft end of a body of revolution (having a similar geometry to the model used in the present work), and were able to effect a time-varying controlled normal force. A similar effect was reported by Corke et al. (2008) who used tangential plasma actuation placed upstream of a Coanda surface on a similar model to achieve a 50° turning angle of the external flow and 30% reduction in drag. Abramson, Vukasinovic, and Glezer (2011) used a scale model of the present geometry instrumented with an array of four azimuthal synthetic jet actuators to induce a side force

coefficient of 0.08 within seven jet actuation cycles. In a later investigation Abramson, Vukasinovic, and Glezer (2012) showed that amplitude modulation of the jets' actuation waveforms could lead to controlled, asymmetric aerodynamic loads.

In recent years, there have been several investigations of active manipulation on moving platforms where active control approaches are clearly attractive because spatial and temporal actuation can be adjusted to varying flow conditions. Vibration control of a 2-D flat plate was investigated by Munshi, Modi, and Yokomizo (1999) who used imbedded rotating cylinders to move the surface where the boundary layer was formed with variable speed to reduce its galloping instability. An active approach was also used to control the pitching moment on a 2-D moving airfoil (Brzozowski et al., 2008), where actuation by a spanwise array of synthetic jets was used to regulate vorticity flux in the boundary layer for disturbance rejection. In a recent investigation, Chen et al. (2013) demonstrated suppression of transverse oscillations of a spring-supported 2-D cylinder with high mass ratio (1,500) at Reynolds numbers up to about 150,000 using discretized suction. There have also been numerous examples of active flow control on moving 3-D bluff bodies. In a study that is particularly relevant to the present work, McMichael et al. (2004) exploited a synthetic jet flow control approach to an axisymmetric 40 mm spin stabilized projectile to effect aerodynamic steering forces and moments that were sufficient to steer the projectile by about 30° in a flight test when it was launched at about 80 m/s. This actuation approach was also used in the numerical investigations of Sahu (2006) who computed the aerodynamic loads and the unsteady asymmetric flow field around the model. Krakovich, Eshbal, and van Hout (2013) presented a feasibility study on application of external acoustic source for mitigation of vortex induced vibrations of the two tethered spheres with different mass ratios, at relatively low Reynolds numbers ($Re_D < 3,000$). These authors demonstrated that acoustic excitation at a varying excitation frequency can either amplify or suppress vortex induced vibrations, and speculated that these effects were induced by the synchronization (or lack thereof) of vortex shedding from the two bodies. Goyta, Mueller-Vahl, and Greenblatt (2013) demonstrated that plasma excitation of the flow off the leading edge of a tethered cube at Reynolds numbers up to about $Re_D \approx 50,000$ could manipulate the separation bubble for surface deflections away from or towards the free stream.

Earlier ARO-supported work at Georgia tech demonstrated that fluidic actuation effected by momentary vorticity concentrations through the interactions of surface-integrated synthetic jet actuators with the cross flow can affect the global aerodynamic forces and moments on a stationary body (Abramson, Vukasinovic, and Glezer, 2011, 2012), and can lead to active alteration of the wake of a rigidly moving body in both prescribed 1-DOF (Lambert, Vukasinovic, and Glezer, 2012, 2014, 2015a, 2015b) and 2-DOF (Lambert, Vukasinovic, and Glezer, 2016a) trajectories. These investigations also demonstrated that open- and closed-loop control strategies of near-wake can lead to prescribed modification of the time-dependent aerodynamic loads on 'free'-moving bodies in yaw (Lambert, Vukasinovic, and Glezer, 2013) and in yaw, pitch, and roll (Lambert, Vukasinovic, and Glezer, 2016b) and can thereby regulate the body's motion and stability. A recent review paper by Lambert, Vukasinovic, and Glezer (2018) summarizes different aspects of these flow control strategies.

II. Experimental Setup and the Model

The present investigations were conducted in an open-return wind tunnel at Georgia Tech in a square test section measuring 91cm on the side with a free stream velocity of $U_0 = 40$ m/s. The axisymmetric wind tunnel model used in §§III and IV (Figure II.1a) is assembled using stereo-lithographed and aluminum components ($D = 90$ mm,

$L = 165$ mm, $Re_D = 2.3 \cdot 10^5$). The mid-section and nose are hollow and connected to the tail assembly by a central shaft. An azimuthal array of four independently-driven synthetic jet actuators (labeled the top, left, bottom, and right actuators, as shown in Figure II.1b) are equally distributed along the perimeter of the tail section and are used to effect aerodynamic control forces and associated moments. Each jet is issued in the streamwise direction with the orifice placed at the bottom of a backward-facing step to the circumference extending into a Coanda surface. The step height, $h_s = 1.5$ mm, is selected to be 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. There is a streamwise cut-in recess downstream of the orifice edge which guides the jet flow and prevents its spanwise diffusion. The actuator geometry is chosen to have a Coanda radius of $R_C = 12.7$ mm and an orifice height of $h_J = 0.38$ mm ($A_J = 13.03$ mm²), which are based on the parameter studies of Rinehart (2011). The jet actuation leads to the partial attachment of the outer flow along the surface resulting in a reaction force by the turning of the outer flow into the wake region. In order for all synthetic jets to induce comparable aerodynamic forces on the axisymmetric body, they are calibrated over a wide range of actuation frequencies, f_A , and applied powers P_A . The actuation frequency is scaled by the hydraulic diameter ($\sim 2h_J$) yielding $f_A^* = 2f_A h_J / U_0$, and the actuation power is scaled similar to the force coefficients $P_A^* = 24P_A / (\pi \rho U_0^3 D^2)$.

The axisymmetric bluff body is supported in the wind tunnel by the custom-made 6-DOF traverse that was previously developed under an ARO DURIP Program. The two configurations used in §§III and IV are shown schematically in Figures II.2a and b, respectively. In the first configuration (Figure II.2a), the axisymmetric model is directly supported by the eight traverse wires, such that the mounting enables the programmed model motion in full six DOF. In the second configuration (Figure II.2b), the axisymmetric model is constrained to freely move in 3-DOF (pitch, yaw, and

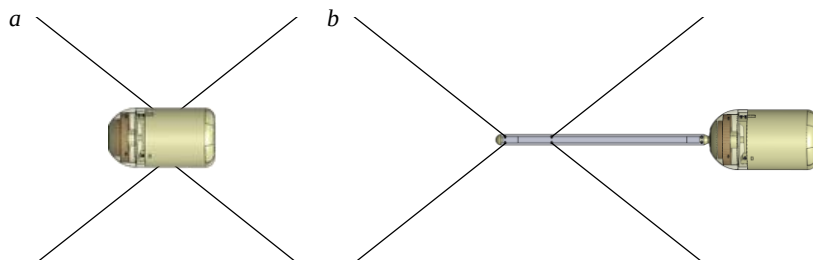


Figure II.2 Side views of the axisymmetric model supported directly by a 6-DOF wire traverse (a) and indirectly by an upstream sting (b).

roll), using a short upstream sting attachment to the 6-DOF wire traverse, i.e., the model is only indirectly supported by the traversing mechanism. The model internal design is modified to allow coupling of its nose section to a stationary gimbal that enables the 3-DOF rotations in response

to aerodynamic pitch, yaw, and roll moments. The stationary segment of the model is attached to an external upstream sting that couples to the model through a central bearing, which is positioned at $x_o = 0.08c$ upstream from the center of pressure. As the sting is supported by the 6-DOF traverse, it is possible to execute its independent motion, besides holding it still, such that the sting motion can be considered as a disturbance for the axisymmetric model. A *VICON* MX Motion Capture System (Lambert et al. 2016) is used to measure the spatially- and temporally-resolved motion in all six degrees of freedom of both the axisymmetric model and its supporting sting. This system utilizes six near infrared cameras that record the light reflected off the small reflective markers affixed to the model and the sting surfaces. In addition to the tracking characterization, the velocity in the wake is measured at $1D$ behind the aft end of the model using a low-speed stereo PIV system for the directly supported model and a high speed ($f = 500$ Hz) stereo particle image velocimetry, which characterizes the wake dynamics at $0.25D$ behind the 3-DOF model.

III. Control of the Near Wake in Prescribed Motion

This section describes investigations of the effects of Lissajous rotations (e.g., coupled pitch and yaw) of the axisymmetric model through a range of ‘quasi-steady’ to ‘highly unsteady’ aerodynamic motion that is inspired by the ‘natural’, coupled pitch-yaw precessions free flight models. The wake dynamics are investigated using stereo PIV that yields simultaneous measurements of the streamwise and cross-stream velocity components in the wake and are characterized using POD modes. The near wake of the static model is described in §III.1.1, and is followed by investigations of the wake in when the model is moving in prescribed Lissajous rotations that are compared to the effects of actuation on the stationary model (§III.1.2). The changes in the wake structure induced by actuation are discussed in § III.1.3, and in § III.1.4 open loop actuation on the rotating model is used for enhancement or suppression of the wake response.

III.1 Static Model: Near-Wake Structure

The near wake behind the stationary model at $Re_D = 1.8 \cdot 10^5$ is measured in the interrogation region highlighted in green in Figure III.1. The result is a 3-D velocity flow field with the streamwise velocity, U_x , in the x direction, and the cross-stream velocities, U_y and U_z , in the y and z directions, respectively. The interrogation region is chosen to be at a fixed streamwise location of $1D$ downstream of the aft end of the model, spanning $1.3D$ in y and $1.3D$ in z , ($y = \pm 0.65D$, and $z = \pm 0.65D$, centered about the model). The resulting stream surface of the flow is shown in Figure III.1, with a 64×64 grid of time averaged velocity vectors, $\bar{\vec{U}}$, over 600 instantaneous measurements. This surface is calculated by taking the location at which each $\bar{\vec{U}}$ was measured and adding on a distance of $\bar{\vec{U}} \cdot t_{ref}$, where $t_{ref} = 2$ ms. This surface is also colored by the streamwise velocity component, U_x , which is the dominant component of the velocity field, and ranges from $0.3U_0$ in the center of the wake deficit behind the model to nominally U_0 in the free stream, with slight reduction in the wake of the wires. For reference, the global coordinate system (x, y, z) used in this chapter is shown in red.

An in-depth analysis of the wake velocity flow field for the stationary model is shown in Figure III.2. The equivalent averaged streamwise velocity component, $\bar{U}_x$, to Figure III.1 is shown in Figure III.2a, in a planar interrogation region, and in addition the projections of the wires are depicted. This baseline flow is not completely axisymmetric due to the geometry of aft end of the model, where the backward-facing steps that house the inactive actuators (see Figure II.1b) locally displace flow from the Coanda surface, which leads to an extension of the wake deficit region in the vertical and

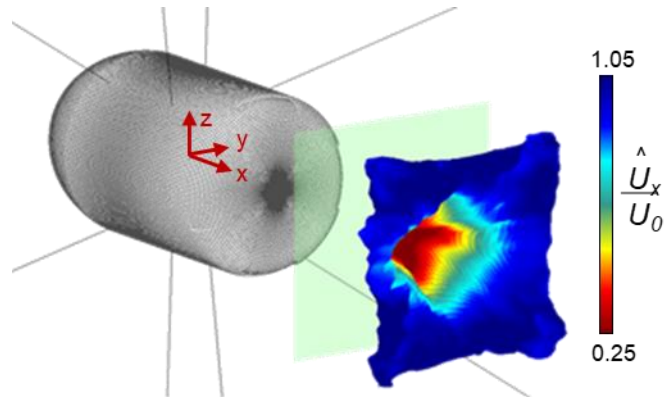


Figure III.1. SPIV on a stationary model with $Re_D = 1.8 \cdot 10^5$ shown to scale with the global coordinate system in **red**, the interrogation region in **green**, and a stream-surface created by the average velocity field, $\bar{\vec{U}} \cdot t_{ref}$, and contoured by the average streamwise velocity, $\bar{U}_x$, where $t_{ref} = 2$ ms.

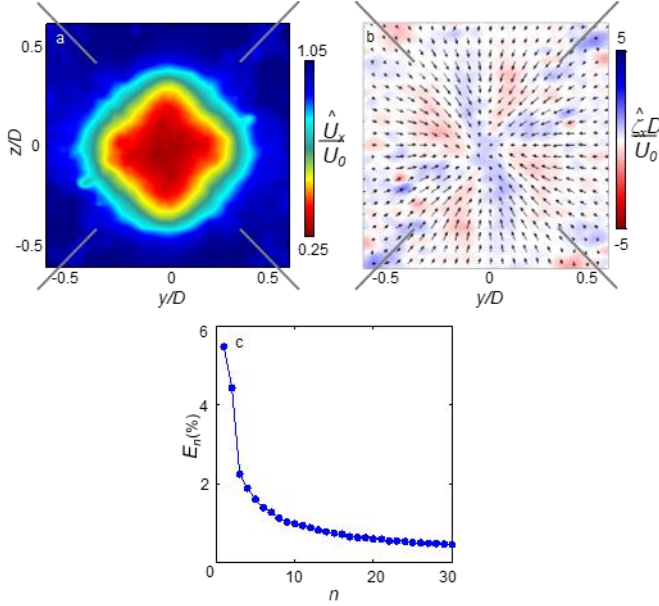


Figure III.2. Contour plots of the averaged base flow streamwise velocity, $\hat{U}_x$ (a) and streamwise vorticity, $\hat{\zeta}_x$ (b), along with the energy contributions for the first 30 modes of a POD analysis on 600 instantaneous velocity vectors (c) for $Re_D = 1.8 \cdot 10^5$ with the cross-stream location of the mounting wires shown in gray.

vorticity due to the wires in the corners of the interrogation region. To further investigate the structure of the wake, instantaneous velocity vectors that are used to calculate the average fields in Figures III.2a and b are also used to extract the Proper Orthogonal Decomposition (POD) modes of the wake. The POD mode analysis is applied on the stationary model by extracting the first 30 modes from 600 snapshots of the velocity field, and the energy contribution of the first 30 modes

horizontal directions. The commensurate cross-stream velocity components ($\hat{U}_y$ and $\hat{U}_z$) are shown in Figure III.2b as a quiver plot alongside with a contour of the averaged streamwise vorticity, $\hat{\zeta}_x$, calculated from these velocity components. The cross-stream velocity magnitude is significantly smaller than the streamwise velocity, with maximum values of $\sim 0.2U_0$ at the location of the shear layer, and approaching zero in both the wake center and in the free stream. Although this flow is nominally converging to center, there is a faint vortex structure present, which is again contributed to the geometry of the aft end of the model: the vortex structure is essentially a weak eight-lobe structure due to slowing of the radial velocity downstream of the inactive actuators. There are also slight contributions to the streamwise

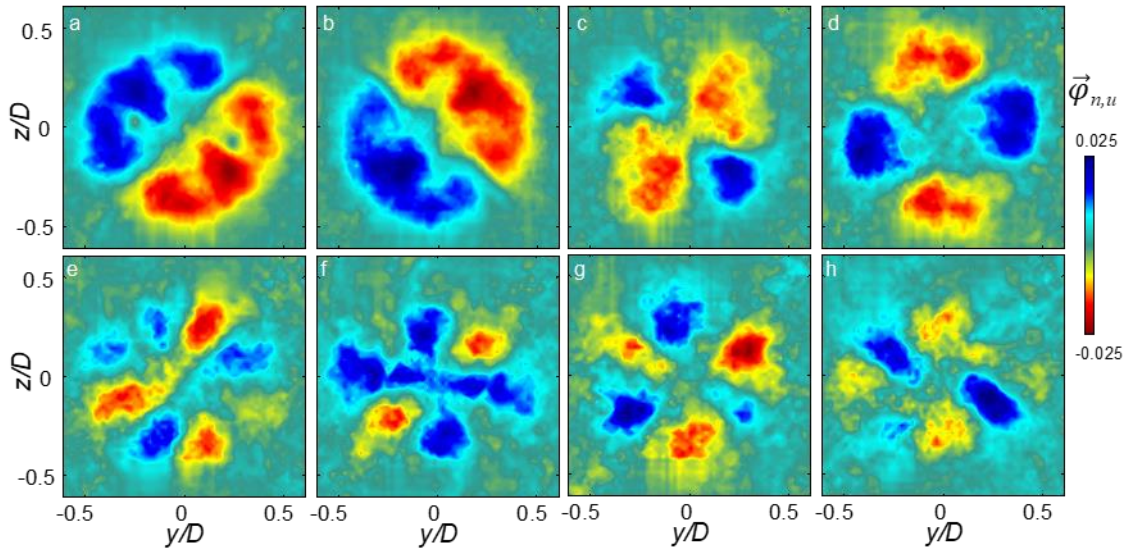


Figure III.3. Contour plots of the streamwise velocity POD modes, $\vec{\varphi}_{n,u}$, with mode number $n = 1$ (a), 2 (b), 3 (c), 4 (d), 5 (e), 6 (f), 7 (g), and 8 (h) for the base flow at $Re_D = 1.8 \cdot 10^5$.

is shown in Figure III.2c. Based on the energy distribution, it appears the first two modes are more prominent than the rest, having a higher than 4% energy contribution, while energy fractions become reduced to less than 1% by the ninth mode.

The POD modes of the subset of $\vec{\varphi}_n$ that correspond to the streamwise velocity, $\vec{\varphi}_{n,u}$ are shown in Figure III.3 with $n = 1-8$ shown in Figures III.3a-h, respectively. The first two modes shown in Figures III.3a and b are a rotated pair that represent a two-fold symmetry of vortex shedding from the model top left to or from the bottom right (Figure III.3a), and the bottom left to or from the top right (Figure III.3b). This wake structure does not seem to line up with the mounting wires, which make a 53° angle from the top in this frame of view, and instead seem to line up with 45° , which is the azimuthal center between two of the jet actuators. This suggests that the primary shedding of the model (and therefore the primary change in the wake deficit) is biased away from the backward facing steps when the jets are inactive and the model is stationary (see Figure II.1b and compare to the shape of Figures III.3a and b). Another interesting feature about this dataset is that the effects of the wires are not as prominent as they were in Figure III.2, which suggests that the effect of the wire is mostly invariant with time (relative to the wake time scales). The second and third modes (Figures III.3c and d) again form a rotated pair which now has a four-fold symmetry and this pattern continues into Figure III.3e which has an apparent eight-fold symmetry. The pairing of POD modes stops at the fifth mode, where the sixth mode in Figure III.3f significantly differs from the fifth mode in Figure III.3e, where it is no longer a rotated pattern of the previous mode, and it is also the first mode to have a predominant sign (i.e., the velocity change in the center is much greater than the two regions of opposite sense around it). The seventh mode in Figure III.3g is the first mode that shows a six-fold symmetry in the velocity fluctuations. The final eighth mode in Figure III.3h also shows six regions of alternating sign, but is dominated mostly by two neighboring regions of the same sense in the bottom right and top left.

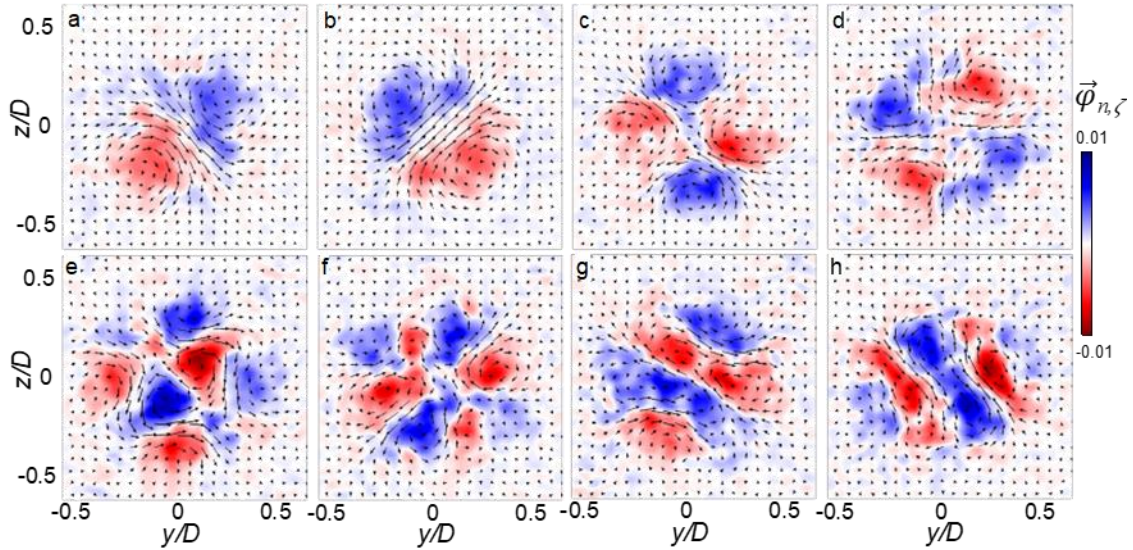


Figure III.4. Quiver plots of the cross-stream velocities' POD modes, $\vec{\varphi}_{n,v}$ and $\vec{\varphi}_{n,w}$, with mode number $n = 1$ (a), 2 (b), 3 (c), 4 (d), 5 (e), 6 (f), 7 (g), and 8 (h) colored by their streamwise vorticity, $\vec{\varphi}_{n,\zeta}$, for the base flow at $Re_D = 1.8 \cdot 10^5$.

The respective subset of $\vec{\varphi}_n$ that correspond to the cross-stream velocities, $\vec{\varphi}_{n,v}$ and $\vec{\varphi}_{n,w}$, are plotted as a quiver plot, and colored by their respective normalized vorticity, $\vec{\varphi}_{n,\zeta}$, in Figure III.4

with the first eight modes, similar to Figure III.3. The same pairing of the first and second mode seen in the streamwise velocity in Figures III.3a and b is observed in the cross-stream velocities in Figures III.4a and b, each with a predominant counter-rotating vortex pair in the top left to/from bottom right and top right to/from bottom left, respectively. The order of these pair modes could change depending on the instantaneous images (i.e., due to the left-right symmetry of this model there is no reason to believe one side should always have dominant energy). The next mode pair in the third and fourth mode in Figures III.4c and d shows two counter-rotating streamwise vortex pairs in agreement with Figure III.3c and d. Another interesting feature that appears in the streamwise vorticity of these modes is that the maximum value occurs at locations where the sign of the streamwise velocity mode changes (compare Figures III.4a-d to Figures III.3a-d). The fifth and sixth modes in Figures III.4e and f, respectively, introduce more vortex structures with some regions not clearly defined, and the seventh and eighth modes in Figures III.4g and h, respectively, introduce a new feature of a streamwise vortex that crosses the centerline of the model which was not seen in lower modes. The last four modes also appear to have a paired structure in the cross-stream velocity modes, while Figures III.4e and f appear similar in structure to each other, as do Figures III.4g and h. However, these same cases show deviations in their respective streamwise velocity modes in Figures III.3e and f and Figures III.3g and h, showing that different modes could have a similar structure in one velocity component and not in other components. It is shown in Figure III.3 and III.4 that as the mode number n increases, the structure of the POD mode starts to lose organization, and therefore for all the following POD datasets shown in the rest of this chapter, only the first four modes are analyzed for brevity.

III.2 Lissajous Rotation- and Actuation- Induced Aerodynamic Loads

The remainder of this investigation focuses on three scenarios: a dynamic bluff body, a stationary bluff body with fluidic actuation, and a dynamic bluff body with fluidic actuation. The induced dynamics are chosen to be 1:1 Lissajous rotation (combined pitch, α_y , and yaw, α_z), where pitch lags the yaw by 90° phase, and both have an amplitude of 3° . This type of motion is representative of the expected natural unstable motion of an equivalent airborne body, in the absence of roll. The frequency of this motion is varied during this study, and a representative ‘quasi-steady’ frequency of $k = 0.017$ ($\tau_{yz} = 1\text{s}$ or $180\tau_{\text{conv}}$) is investigated first and shown in Figure III.5. This executed motion is shown in Figure III.5a, with pitch in cyan and yaw in blue. The corresponding induced

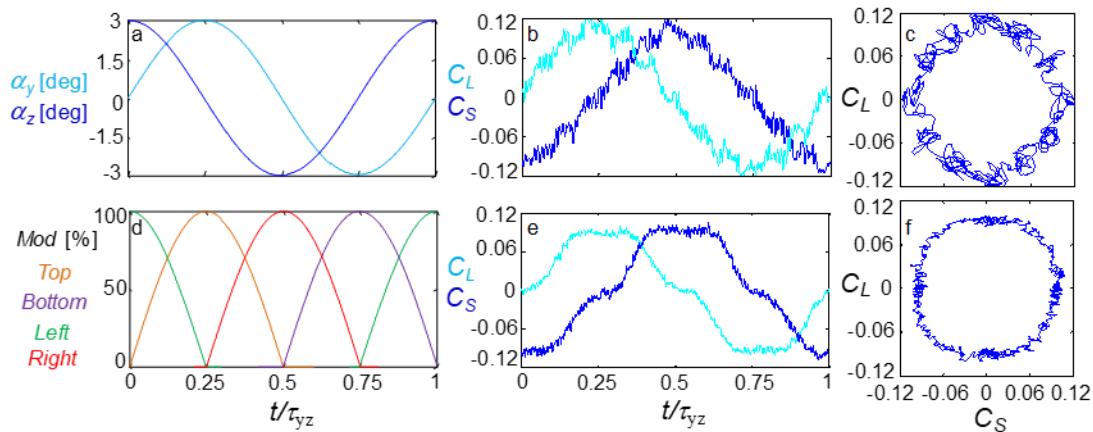


Figure III.5. Side and lift force coefficients (b,c) induced on a model undergoing 1:1 Lissajous rotation with a phase difference of 90° (a), and the corresponding forces (e,f) on a stationary model by the actuation algorithm (d).

lift and side force coefficients (C_L and C_S , respectively) on the model are shown in Figure III.5b, as an average of 50 instantaneous measurements, and the force coefficients are derived from the measured forces by $C = 8F/(\rho\pi U^2 D^2)$. There is a presence of two higher frequencies in both C_L and C_S , one that corresponds to the wake shedding frequency of 83Hz (or $k = 1.434$, which corresponds to $St_D = fD/U_o = 0.234$, and another that corresponds to a lower frequency at ~ 12 Hz ($k = 0.207$), which is attributed to an instability frequency of the 1:1 Lissajous motion on this model without spin. These two force coefficients are plotted against each other in Figure III.5c, where they trace out a circle with radius of 0.11, in addition to the presence of the two instability frequencies. Next, the representative fluidic actuation is chosen to induce forces on a *stationary* model to mimic the flow-induced forces on a *moving* model. To achieve this, the synthetic jet actuation waveform amplitude is chosen to provide a jet momentum coefficient, $C_{\mu} = 0.004$. Independent amplitude modulations of this signal are created and sent to all four jets, which allows control of C_{μ} in the range of 0 to 0.004. This same methodology was used before for two jets for the same model in earlier work, and now is effectively extended to all four jets. Based on this prior data, the desired modulation waveform is chosen to be an overlapping sinusoidal modulation to all four jets, and is shown in Figure III.5d. The corresponding C_L and C_S that are induced by this actuation are shown in Figure III.5e, which indicates good agreement in magnitude with the motion-induced C_L and C_S in Figure III.5b, with the exception that the lower frequency instability ($k = 0.207$) attributed to the spinning motion is no longer detected, even though the shedding instability frequency ($k = 1.434$) is still present. These two induced force coefficients are plotted against each other in Figure III.5f, which shows a less circular trace than in Figure III.5c, which is due to a faster rise time of the actuation effect in Figure III.5e compared to the motion effect in Figure III.5b. Nevertheless, the fact that the actuation-induced forces are of the same order as the motion-induced forces in Figure III.5 suggests that active fluidic control has sufficient authority to significantly control the motion-induced forces for the preselected 1:1 Lissajous rotation.

The frequency of the two patterns shown in Figure III.5 is varied, while keeping the motion amplitude and fluidic modulation amplitude fixed. The resulting variations in the C_L and C_S traces

shown in Figures III.5c and f for these increased frequencies are shown in Figure III.6 (the resulting fluidic induced forces are depicted in green, and the motion-induced forces in blue). Figure III.6a shows $k = 0.017$, which corresponds to the cases in Figure III.5c and f plotted with each other. As k is increased to 0.086 (Figure III.6b), the motion-induced forces start to increase while the actuation-induced forces remain roughly invariant. All the plots in Figure III.6 are plotted in a 1 second time trace ($180\tau_{\text{conv}}$ where $\tau_{\text{conv}} = c/U_o$) that is the result of 50 averages to visualize the

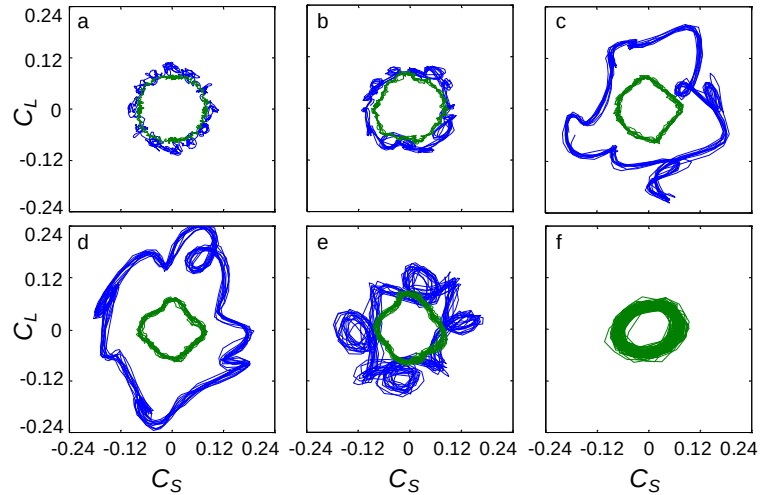


Figure III.6. Frequency variation of C_L and C_S for 1:1 Lissajous rotation (**blue**) and actuation on a stationary model (**green**) with effective reduced frequencies at $k = 0.017$ (a), 0.086 (b), 0.172 (c), 0.207 (d), and 0.259 (e), and 1.434 (f), acquired over $180\tau_{\text{conv}}$. Rotation-induced C_L and C_S are not acquired for $k = 1.434$.

variation of the force coefficients on the same time scale. Near the motion-resonance detected before in Figure III.5b ($k \sim 0.207$), there is a growth in the motion-induced C_L and C_S shown in blue to $\sim 200\%$ and $\sim 250\%$ of the $k = 0.017$ values at $k = 0.172$ (Figure III.6c) and 0.207 (Figure III.6d). However, the corresponding actuation-induced force coefficient magnitude shown in green in Figure III.6c and d remains roughly invariant, with slight changes in its shape to have prominent maxima at the location in the paths where either $C_S = 0$ or $C_L = 0$. When the frequency is increased past the motion-resonance frequency to $k = 0.259$ (Figure III.6e), the motion-induced lift force begins to decrease in magnitude to values similar to its lower frequencies (compare the blue line in Figure III.6e with Figures III.6a and b). The lack of frequency variation in the actuation-induced forces continues at $k = 0.259$, shown in Figure III.6e in green (compare Figure III.6e and Figure III.6d). The absence of the frequency variation of the actuation-induced force coefficients in the range of $k = 0.017$ to 0.259 is commensurate with the lack of the $k \sim 0.207$ resonance in Figure III.5e, as opposed to the motion-induced response, which has a natural frequency present within the range of the frequency variation in Figures III.6a-e (see Figure III.5b). To test this hypothesis, the actuation-induced forces are modulated at the shedding frequency of $k = 1.434$ (83 Hz), shown in Figure III.6f in green. This frequency is out of the range of the current wire-traverse control authority and therefore the motion-induced forces for this frequency are omitted from Figure III.6f. Two features become prominent at this frequency: the response becomes biased in the side force coefficient, and the variation of the recorded actuation-induced force coefficients is much larger than they were at any lower frequency (compare Figure III.6f with Figures III.6a-e). This effect is also similar to the result found in the far wake pitch modulations on a stationary model in Section IV, where setting the modulation frequency to the shedding frequency extended the vertical extent of the wake deficit. This approach of implementing spin modulated fluidic actuation on a stationary model at its shedding frequency is investigated further through its wake response in §V.

Next, the study of the control authority of the fluid actuation on a moving body is tested. Based on the results shown in Figure III.6, there should be a significant impact on C_L and C_S if the fluidic actuation scheme (shown in green) is run in phase or out of phase with the Lissajous 1:1 rotation that induced the aerodynamic loads (shown in blue). In addition, it is also important to check the possible deflection of trajectory (x , y , z , α_x , α_y , and α_z), as well as the changes in the four other coefficients (drag force, C_D , and roll, C_R , pitch, C_M , and yaw, C_Y , moments), where the moment coefficients are based on the measured moments as $C = 8M/(\rho\pi U^2 D^2 L)$. Initially, the combination of the fluidic actuation shown in Figure III.5d and Lissajous rotation in Figure III.5a is shown at the representative steady frequency of $k = 0.017$ in Figure III.7 ($\tau_{yz} = 1\text{ s}$ or $180\tau_{\text{conv}}$). When the actuation and motion are in phase (referred to as the force augmentation case), the corresponding data are shown in red, and when the actuation is run 180° out of phase (force suppression), the data are shown in green, while the unactuated motion response is shown in blue for reference. The nominally zero translational components of the center of the wire mounts on the model are shown on a scale of $\pm 2\text{ mm}$ with streamwise displacement (x), and cross-stream displacements (y and z). There are slight variations in y on the order of 0.2 mm and z on the order of 0.4 mm , but for all translations there is almost no variation in the motion when actuation is applied, implying that the forces induced in Figures III.7g-i are all aerodynamic and not related to inertia effects. The commanded angles of 3° α_y and α_z , 90° out of phase are shown in Figures III.7e and f, which again have little variation due to actuation, also implying that the moment responses in Figures III.7j-l are entirely aerodynamic. There is a slight variation of the roll angle from the commanded value of 0 in Figure III.7d, but it is around 0.3° and is invariant with actuation and therefore is considered

negligible. The commanded force suppression (green) and force augmentation (red) are shown with their C_S and C_L in Figures III.7h and i, respectively, indicate significant control authority over these two coefficients with a suppression of 60% or an augmentation of 35%, respectively. This control negligibly varies the drag seen in Figure III.7g, where it is initially a value of 0.24 (close to the expected value of 0.25, cf., Hoerner, 1965), and the actuation does not significantly increase or decrease C_D (within the resolution of this time trace of about 0.02). Likewise, the roll moment, shown in Figure III.7j appears to be roughly invariant (with small periodic fluctuations attributed to fluctuations in the roll angle, compare Figure III.7j and Figure III.7d), and almost no change is induced due to the actuation. Similar to this model undergoing a pure pitching motion with no yaw, the force suppression actuation appears to augment the aerodynamic moments C_Y and C_M by about 50%, and the force augmentation actuation appears to suppress the aerodynamic moments by about 30%, as seen in Figures III.7k and l. This suggests that this actuation could also be applied for control of the aerodynamic moments instead of the aerodynamic forces, if desired. In addition, the motion-resonance frequency is more clearly depicted in the moment traces in Figures III.7k and l than in the force traces (compare with Figures III.7h and i).

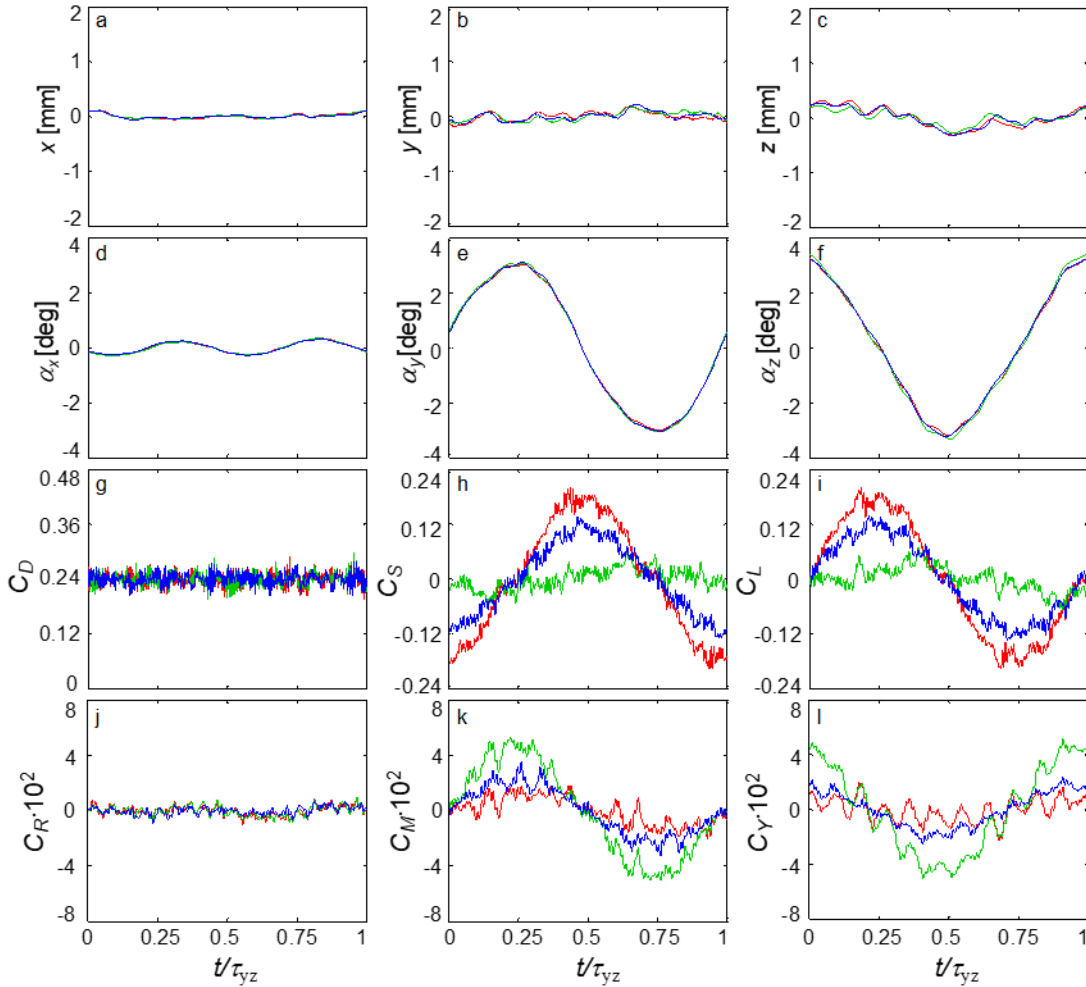


Figure III.7. Time traces of translational (a–c) and rotational (d–f) components of the model motion, as well as the aerodynamic force (g–i), and moment (j–l) coefficients at $k = 0.017$, without (blue) and with actuation for the flow control schemes for the force suppression (green) and augmentation (red) at $Re_D = 1.8 \cdot 10^5$.

A frequency variation of the force suppression (green) and augmentation (red) control schemes, shown in Figure III.8, is implemented (similar to that of Figure III.6), where the side with lift force variation is shown in Figures III.8a,c,e,g, and i and the yaw with pitch moment variation is shown in Figures III.8b,d,f,h, and j. Again, the Lissajous rotation without actuation is shown in blue,

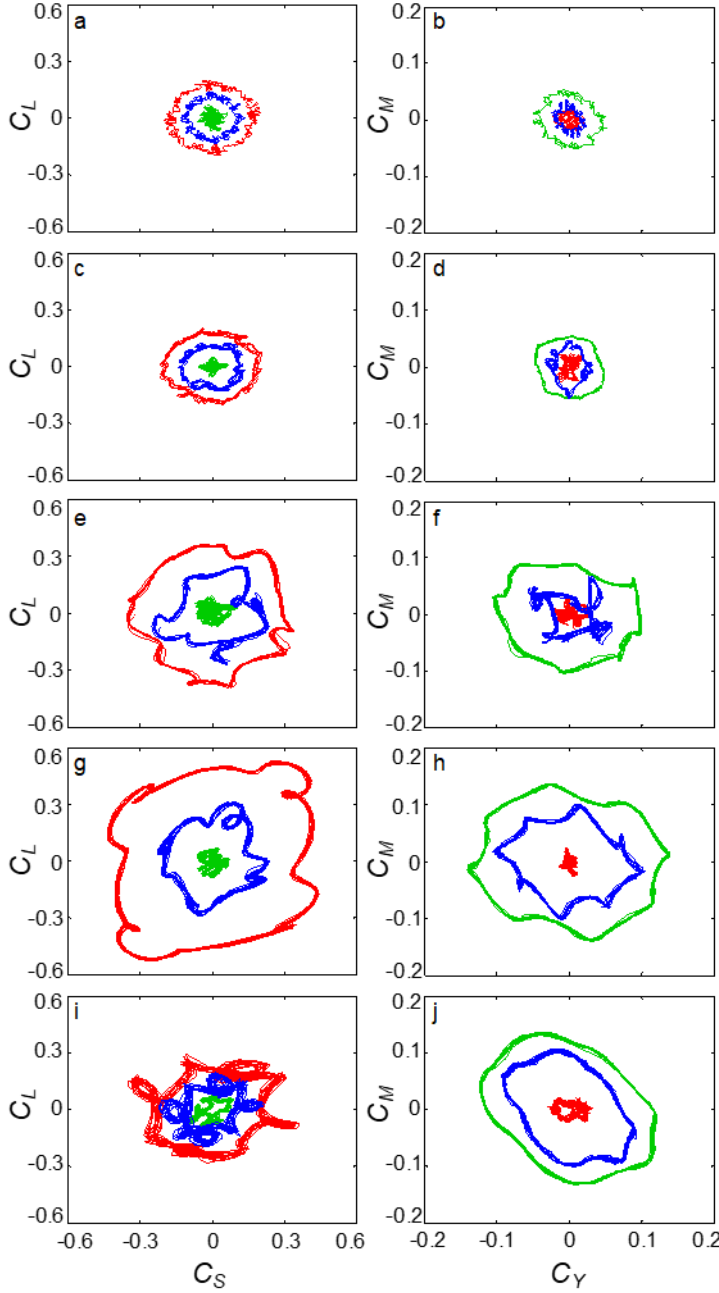


Figure III.8. Phase plots of lift and side force coefficients (left column) and pitch and yaw moment coefficients (right column) on the model at $k = 0.017$ (a), 0.086 (b), 0.172 (c), 0.207 (d), and 0.259 (e) without (blue) and with the flow control schemes for the force suppression (green) and augmentation (red) at $Re_D = 1.8 \cdot 10^5$.

where the force coefficient traces in Figures III.8a,c,e,g and i in blue are the same as the blue traces in Figures 6.6a-e. For $k > 0.017$, the command given to the synthetic jet modulation waveform is modified to include a phase lag relative to the motion, such that the suppression effect is maximized. The respective amplification at that frequency is then chosen as the same command as suppression, 180° out of phase. The lowest frequency shown in Figures III.8a and b shows the same result as in Figure III.7, with approximately a 60% suppression and 35% augmentation in the forces, respectively, as well as a 30% augmentation and 50% suppression in the moments, respectively. This trend continues with little change in the actuation effect, and small changes in the baseline structure as the reduced frequency is increased to $k = 0.086$ (Figures III.8c and d). When the motion is increased to near its spin instability (k increases to 0.207 , see Figures III.8e-h), the effect of the actuation increases from its initially invariant level that was seen in Figure III.6. This shows

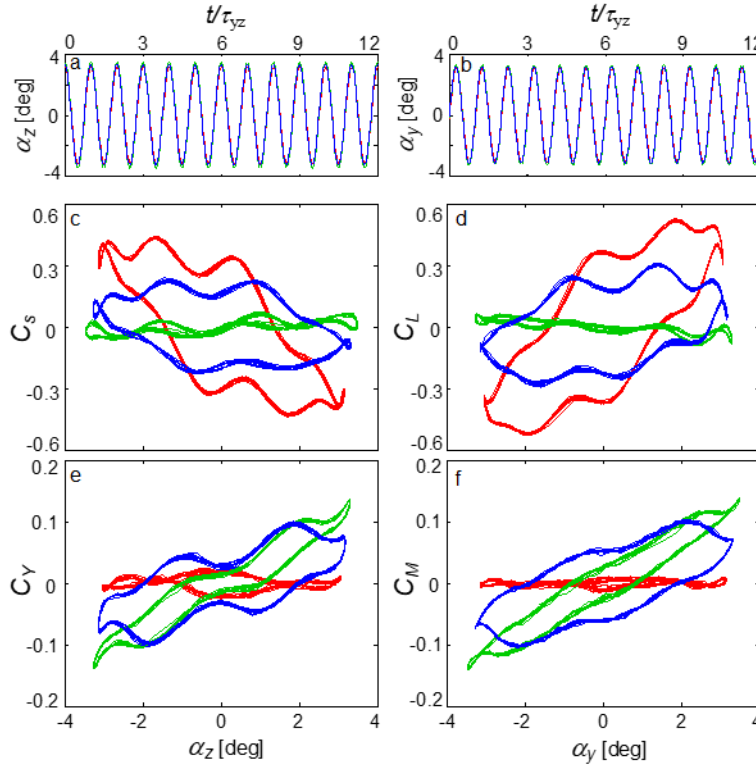


Figure III.9. Angular motion (a,b), as well as angular variation in induced force (c-d), and moment (e-f) coefficients for $k = 0.207$ without (blue) and with the flow control schemes for the force suppression (green) and augmentation (red) at $Re_D = 1.8 \cdot 10^5$.

approximately eight times faster than the motion in Figures III.8g and h. As the reduced frequency is increased above the spin resonance, the force induced by both the flow and the actuation decrease, while the moments induced by the flow and actuation appear to decrease at a slower rate, with the induced moments (Figure III.8j) remaining a larger fraction of their steady moments (Figure III.8b), when compared to the ratio of the induced forces (Figure III.8i) to their representative steady values (Figure III.8a).

Upon analyzing the frequency effect on the forces and actuation outcomes, the resonance case of $k = 0.207$ ($\tau_{yz} = 0.083s$ or $15 \tau_{conv}$) is selected as the representative ‘highly unsteady’ frequency for the remainder of this chapter. Although Figure III.8 gives a detailed description of the forces and moments, and their changes with actuation, it does not include any information about the model attitude. To depict this further the force coefficients and moment coefficients are plotted with their respective angle of attack in Figure III.9. Similar to Figures III.6 and III.8, the dataset is plotted over a 1 second measurement ($12 \tau_{yz}$ or $180 \tau_{conv}$), and this motion is plotted in Figures III.9a and b with time. Again, like the steady frequency in Figures III.7e and f, there is almost no variation in the motion with actuation, and inertia effects of the force and moment coefficients can be ignored. Each of the aerodynamic load coefficients is plotted with the angle that induces the most variation (i.e. C_L and C_M are plotted with α_y , and C_S and C_Y are plotted with α_z). Figure III.9c shows C_S with α_z , which is comparable to the variation of C_L with α_y in Figure III.9d, with the exception that C_S increases with negative α_z , while C_L increases with positive α_y . This sign reversal is an

the effect of the actuation changing with the orientation of the model (and possibly also with the model dynamics) similarly to what was seen previously when this model was varied under pitch and the jet momentum was held constant. This new increase in control authority at $k = 0.207$ yields a force suppression of $\sim 80\%$ and an augmentation of $\sim 100\%$ (Figure III.8g), as well as a moment augmentation of $\sim 40\%$ and suppression of $\sim 80\%$ (Figure III.8h). Also, the shedding frequency of the model becomes noticeably coupled to the motion in Figures III.8e-h, with a response which is approximately nine times faster than the motion in Figures III.8e and f, and

artifact of the coordinate system chosen (see Figure III.1); as the model moves in positive pitch ($+\alpha_y$), the lift force increases in the $+z$ direction, but the side force increases in the $-y$ direction for the model moving in positive yaw ($+\alpha_z$). Figure III.9e shows C_Y with α_z , which is also paired with the variation of C_M with α_y in Figure III.9f. In this case both of the slopes are of the same sense, as the model is unstable (i.e., as the model pitches in α_y or α_z , its moment tends to be in the same sense to continue motion in the same direction). Another interesting feature is that the augmentation of force coefficients (Figures III.9c and d in red) increases the hysteresis in the C_S vs. α_z and C_L vs. α_y paths, while suppression of the force coefficients (Figures III.9c and d in green) decreases the hysteresis in the force vs. angle path. There is a different trend in the moment where although hysteresis in C_Y vs. α_z and C_M vs. α_y is dramatically reduced with force augmentation (Figures III.9e and f in red), the hysteresis in C_Y vs. α_z and C_M vs. α_y is also slightly decreased in the force suppression actuation (Figures III.9e and f in green). The hysteresis in force and moment coefficients with angle of attack is expected in unsteady flow, as the wake response lags the model motion. In addition, the shedding frequency further affects all the dynamics seen in Figures III.9c-f, where each trace shows a prominent higher frequency attributed to the vortex shedding.

III.3 Stationary Actuation vs. Unactuated Rotation Wake Structure

This section further investigates the wake response of the representative unsteady 1:1 Lissajous rotation ($k = 0.207$) depicted in Figure III.6d. For the present investigation the wake is characterized with phase-locked PIV measurements over 600 averages and 12 phases throughout the motion cycle. The wake response lags the model motion by $\sim 30^\circ$ at this reduced frequency which is in agreement with both the measured hysteresis in the baseline aerodynamic load measurements in Figure III.9 as well as the prior study of phase-lag response at varying k in pure pitching motion. For simplicity, the velocity fields chosen to represent this phase averaged wake response are the four phases where the wake deficit is most horizontally and vertically deflected at $t/\tau_{yz} = 0.083, 0.333, 0.583, \text{ and } 0.833$ (representative phases of $30^\circ, 120^\circ, 210^\circ$ and 300° with respect to the motion, where $\tau_{yz} = 0.083\text{s}$ is the time of one motion cycle). The resulting streamwise velocity measured in these four phases is shown in Figures III.10a-d for the furthest wake deflections in $+y, -z, -y, \text{ and } +z$, respectively. In comparison to the baseline flow in Figure III.2a, the effect of the wires is similar, and the wake deficit is smaller than it was when the model was stationary. In addition, the phases show symmetry as expected, where Figure III.10a and Figure III.10c are similar to mirror images of each other across $y = 0$, and Figures III.10b and d are also symmetric across $z = 0$. For comparison, the streamwise wake behind the model when the sinusoidal modulation fluidic control scheme from Figure III.5d is applied on a *stationary* model in the equivalent phases to Figures III.10a-d are shown in III.10e-h, respectively. There are some key differences: as the wake deficit for actuation alone is larger than the wake deficit for motion alone, and the actuation leaves a prominent concave disruption in the wake deficit on the side where the fluidic actuator is activated (see the left side of Figure III.10e). However, these flow fields show that a similar wake to a precessing model can be induced on a stationary model with the synthetic jets, demonstrating the control authority that was observed in the forces measured in

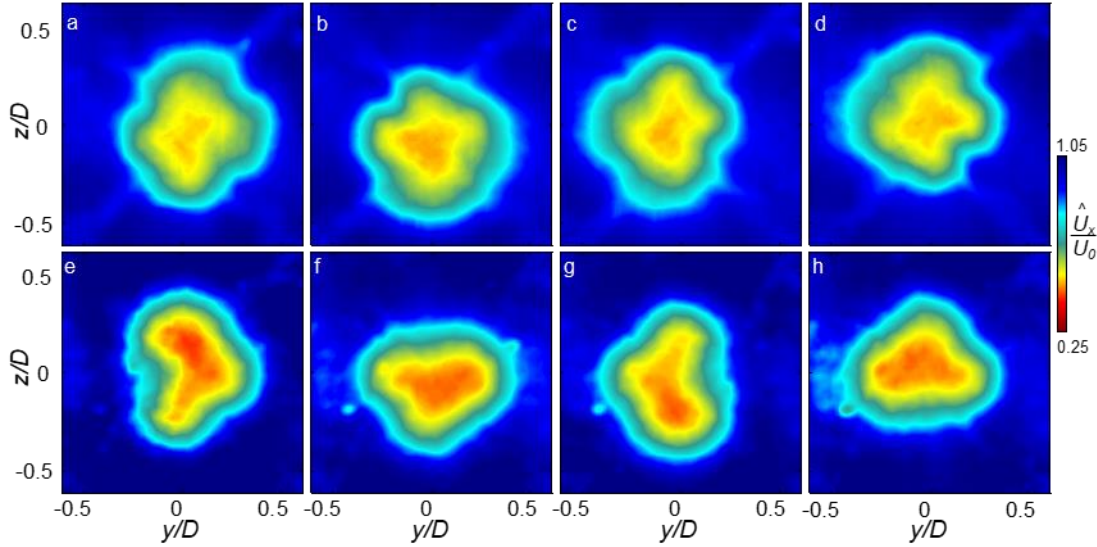


Figure III.10. Phase-locked color raster plots of streamwise velocity component, $\hat{U}_x$, at $t/\tau_{yz} = 0.083$ (a,e), 0.333 (b,f), 0.583 (c,g), and 0.833 (d,h) for the force coefficients shown in Figure III.8d on a stationary model with flow control actuation (a-d) and for a 1:1 Lissajous motion without flow control (e-h).

§III.2 (compare Figures III.10a-d with III.10e-h, respectively). The cross-stream velocity for the motion-induced and actuation-induced flow fields are omitted as they show similar effects seen in the streamwise velocity analysis.

The wake structure of the Lissajous motion-induced and stationary actuation-induced wakes are analyzed using the same POD analysis as in Figures III.3 and III.4 on a 7,200 image dataset formed by appending 600 instantaneous measurements for 12 phases. The result is shown in Figure III.11 with the streamwise velocity modes, $\vec{\varphi}_{n,u}$, for $n = 1$ (Figures III.11a and e), 2 (Figures III.11b and f), 3 (Figures III.11c and g), and 4 (Figures III.11d and h) of the motion-induced wakes (cf., Figures III.10a-d) shown in Figure III.11a-d and of the actuation-induced wakes (cf., Figures III.10e-h) shown in Figures III.11e-h. The first two modes of the motion-induced wakes resemble the first two modes of the baseline wake structure with a 45° rotation (compare Figures III.11a and b with Figures III.3a and b). This rotation of the primary sign change in the first two streamwise velocity modes shows that the preferential shedding (with highest energy) is now in the direction of yaw alone and the direction of pitch alone. It is argued that this results from the model longest residence times at its outmost positions where it moves slowest due to the nature of sinusoidal motion. This phenomenon is not observed for the actuation-induced wakes where the first two modes (Figures III.11e and f) remain unchanged in orientation and instead only slightly change in structure from the first two modes of the baseline case (Figures III.3a and b). A new mode that was not present in the POD modes of the baseline appears as the third strongest mode in the motion-induced in wake in Figure III.11c which represents the increase or decrease of velocity in the shear layer with respect to the free-stream and central region. This mode appears to have a similar shape to the baseline wake deficit on a stationary model (compare Figure III.11c with Figure III.2a), which suggests that the lack of axisymmetry in this mode may also be attributed to the model geometry. Figure III.11d shows the fourth mode in the motion-induced wake which is similar in shape to the third mode of the baseline flow (compare Figure III.11d and Figure III.3c), with slight variations in structure. The third mode in the actuation-induced wake is shown in Figure III.11c, which can be related to the fourth mode of the baseline flow (compare Figure III.11g with Figure III.3d). The

new motion that is induced by the spinning actuation is shown in Figure III.11h in the fourth mode, which is similar to the third mode of the motion-induced flow. This suggests that the difference that is observed between the streamwise velocity in the motion-induced wakes of Figures III.10a-d and actuation-induced wakes of Figures III.10e-h are synonymous with the change in shape of the dynamic modes (Figures III.11c and g) as well as the increased energy in this mode when the wake deflection is motion-induced.

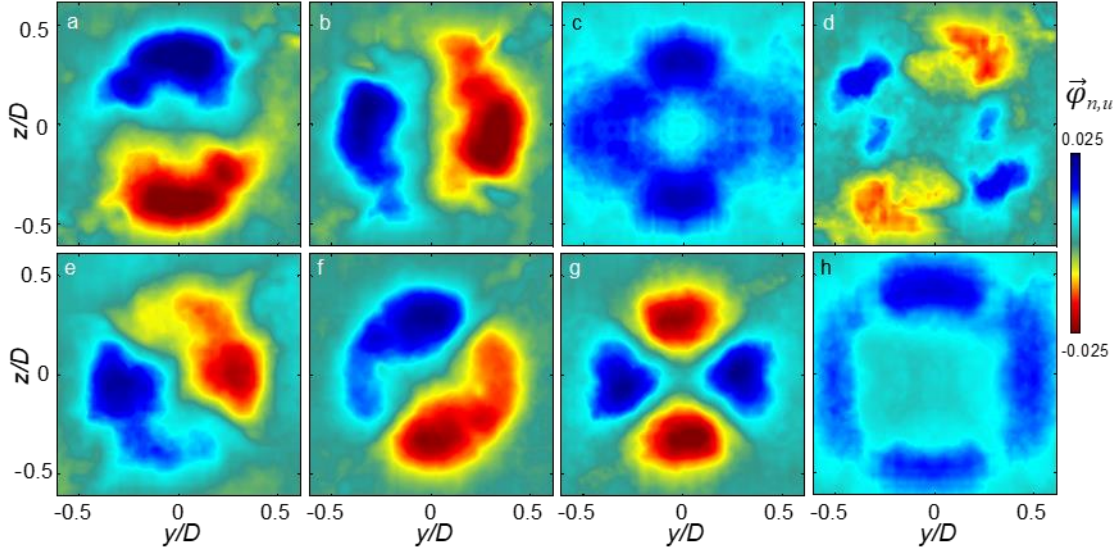


Figure III.11. Contour plots of the POD modes $n = 1$ (a,e), 2 (b,f), 3 (c,g), and 4 (d,h) of streamwise velocity, $\vec{\phi}_{n,u}$, for the equivalent Lissajous rotation without flow control (a-d, cf., Fig 6.10a-d) and for the overlapping sinusoidal modulation actuation on a stationary model (e-h, cf., Fig. 6.10e-h).

The coupling of the motion-induced and actuation-induced wakes together is investigated further in §III.4, and the remainder of this section considers the effect of the spinning actuation on the wake of a stationary model when the modulation frequency is increased to the model shedding frequency of $k = 1.434$ (commensurate with the forces measured in Figure III.6f). However, because the overlapping sinusoidal actuation is applied at a much higher frequency, the phase lag between the wake and the actuation is increased to around $\sim 210^\circ$, where this number was derived through the measured actuation force output and its corresponding modulation command (Figure III.6f). Similar to the dataset in Figure III.10, the flow fields are measured through phase-averaged PIV with 600 instantaneous realizations of 12 different phases, of which four representative phases are shown in Figure III.12. The velocity fields chosen to represent this phase-averaged wake response are the four phases where the wake deficit is most horizontally and vertically deflected at $t/\tau_{yz} = 0.583$ (Figures III.12a and e), 0.833 (Figures III.12b and f), 0.083 (Figures III.12c and g), and 0.333 (Figures III.12d and h), which are equivalent to phases of 210° , 300° , 30° and 120° with respect to the actuation modulation, where $\tau_{yz} = 0.012\text{s}$ is the period of one modulation cycle. The representative phases are shown in Figure III.12 with a contour plot of the phase averaged streamwise velocity, $\hat{U}_x$, in Figures III.12a-d and a quiver plot of the cross-stream velocities, $\hat{U}_y$ and $\hat{U}_z$, in Figures III.12e-h, with an overlaid raster plot of the streamwise vorticity, $\hat{\zeta}_x$. Clearly, there are large differences in this flow field from the equivalent actuation-induced flow field at $k = 0.207$ (Figures III.10e-h), as the magnitude of the velocity deficit decreases, and the span of the velocity deficit increases. In addition, the vertical deflection of the velocity deficit region is

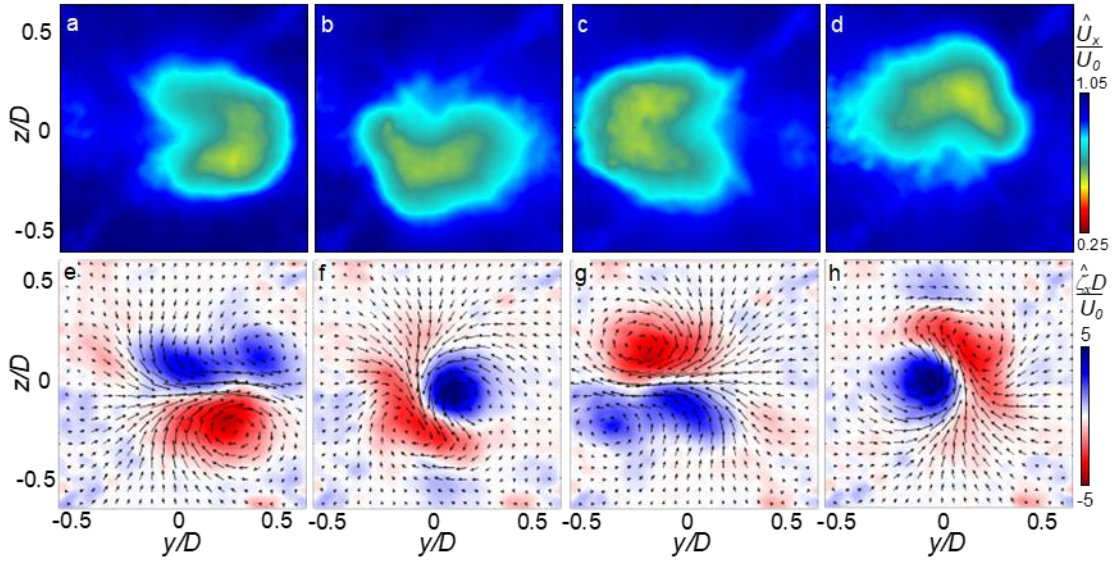


Figure III.12. Phase-locked contour plots of streamwise velocity, $\hat{U}_x$ (a–d), and the cross-stream velocities $\hat{U}_y$ and $\hat{U}_z$ colored by the streamwise vorticity $\hat{\zeta}_x$ (e–h) at $t/\tau_z = 0.583$ (a,e), 0.833 (b,f), 0.083 (c,g), and 0.333 (d,h) for the flow control actuation scheme on a stationary model, modulated at the model shedding frequency $k = 1.434$ (83 Hz).

similar to the lower frequency (compare Figures III.12b and d with Figures III.10b and d), but the horizontal deflection is much larger (compare Figures III.12a and c with Figures III.10b and d). This increased deflection in the horizontal direction is investigated further in the cross-stream velocities in Figures III.12e–h, where in Figures III.12e and g, a third significant counter-clockwise vortex appears in the flow, when the streamwise velocity wake deficit is deflected horizontally. If this vortex were removed, the magnitude of the streamwise vorticity in all of the phases would look similar to each other (compare the two vortices in Figures III.12f and h with the primary two

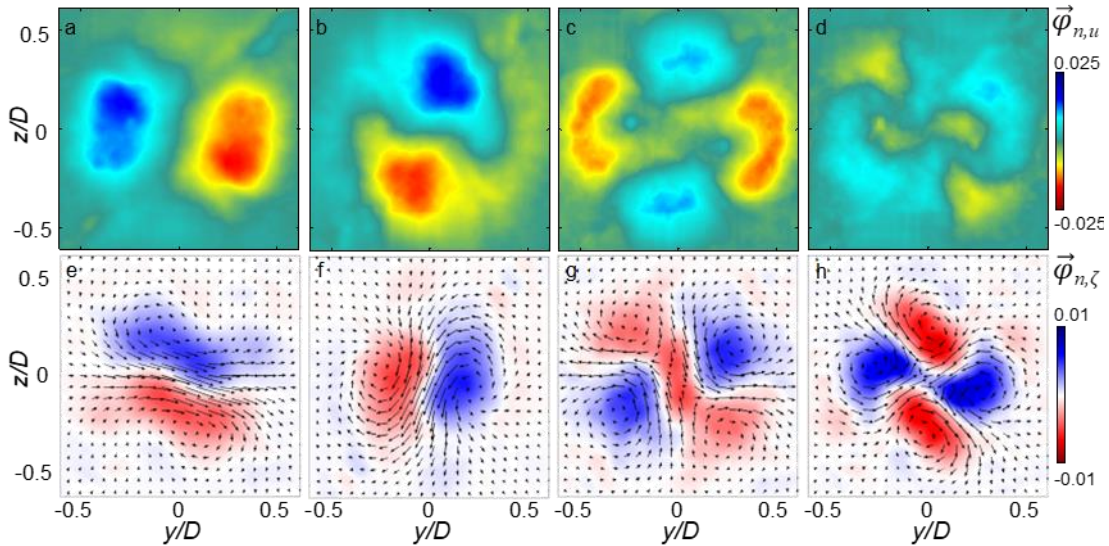


Figure III.13. Contour plots of the POD modes $n = 1$ (a,e), 2 (b,f), 3 (c,g), and 4 (d,h) of streamwise velocity, $\vec{\phi}_{n,u}$ (a–d), and the cross-stream velocity, $\vec{\phi}_{n,v}$ and $\vec{\phi}_{n,w}$ (e–h) colored by their streamwise vorticity, $\vec{\phi}_{n,\zeta}$, for the flow control actuation shown in Figure III.12.

vortices in Figures III.12e and g). This suggests that this extra shedding vortex which could, in principle, have been formed at any angle in this field of view, due to its axisymmetry, is maintained in the yawing direction, perhaps due to slight imbalances in the strength of the actuation. This result implies that timing the fluidic actuation to the shedding frequency of the wake even when the actuation is axisymmetric (has equal commands in the pitching and yawing directions) can maintain the shedding instability in a preferential pitch-yaw direction over a much larger time scale, suggesting that with the implementation of the right sensors for detecting the flow instability these fluidic actuators may be implemented for *control* of the vortex shedding.

The structure of the wake when actuation is modulated at the shedding frequency (Figure III.12) is also investigated analogous to the streamwise POD modes shown in Figure III.11a-d, and presented in Figures III.13a-d. The first two modes (Figures III.13a-b) represent a paired two-fold symmetry, similar to actuation at the lower frequency (Figures III.11e-f), without a bias along 45° from the top, instead being biased in the pitch and yaw directions. These structures, although induced on a stationary model, clearly resemble those of the motion-induced wake of Figures III.11a-b. Figure III.13c and d are similar to the modes of the fourth mode of the lower frequency modulation (with weak four-fold symmetry, compare Figures III.13c-d with Figures III.11d), but have a more diffuse structure. The mode depicted in Figure III.11h corresponding to the shear layer dynamics induced by the actuation at $k = 0.207$ is not present in the first four modes at $k = 1.434$. The commensurate first four modes of the cross-stream velocity are shown in Figures III.13e-h (in the same fashion as Figure III.4a-d on the stationary unactuated model). The addition of the new vortex in the yaw direction in Figures III.12e and f is reflected in the fact that the first two modes in Figures III.13e and f are now no longer rotated pairs. The mode that corresponds to shedding in the yawing direction has a secondary vortex encompassed in its mode (which is also the mode that has the most energy contribution). The second, third, and fourth modes are more diffuse versions of similar structures observed in the unactuated flow modes with slight changes in structure (compare Figures III.13f-h with Figures III.4b-d). These results show that the fluidic

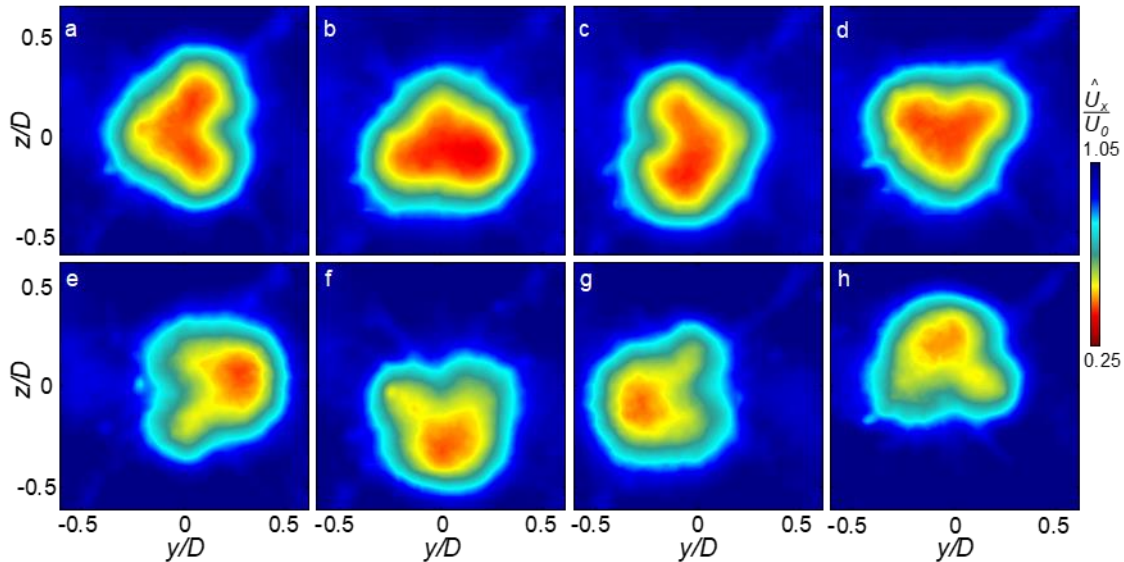


Figure III.14. Phase-locked contour plots of streamwise velocity component, $\hat{U}_x$, at $t/\tau_{yz} = 0.083(a,e)$, $0.333(b,f)$, $0.583(c,g)$, and $0.833(d,h)$ for the force suppression (a-d) and the force augmentation (e-h) flow control schemes at $Re_D = 1.8 \cdot 10^5$ and $k = 0.207$.

actuation, in this scenario, does not only excite the highest energy mode, but changes its coupled spatial distribution as well.

III.4 Dynamic Model: Controlled Near-Wake Structure

This section further investigates the near wake effects of the fluidic control schemes for force suppression and augmentation on a body undergoing 1:1 Lissajous rotation at $k = 0.207$, synonymous with the forces depicted in Figure III.8g and h. The application of both of these actuations are shown in Figure III.14, with the force suppression in Figures III.14a-d and the force augmentation in Figures III.14e-h. This wake is characterized with phase-locked PIV measurements in the same fashion as in Figure III.10a-d with the streamwise velocity fields, $\widehat{U}_x$, at $t/\tau_{yz} = 0.083$ (Figures III.14a and e), 0.333 (Figures III.14b and f), 0.583 (Figures III.14c and g), and 0.833 (Figures III.14d and h), which are representative phases of 30° , 120° , 210° , and 300° with respect to the motion, where $\tau_{yz} = 0.083s$. The force suppression actuation shown in Figures III.14a-d is the combined effect of the Lissajous 1:1 rotation induced wake (Figures III.10a-d) and the actuation-induced wake control scheme shown in Figures III.10e-h run out of phase in order to have opposing effects. When the force suppression scheme is applied, wake responds to the effect of activation of the right jet (Figure III.14a), the streamwise velocity wake deficit region develops a similar structure to when this actuation was applied on a stationary body (compare Figures III.14a and Figure III.10g, noting the actuation in Figures III.10e-h is 180° out of phase with the actuation in Figures III.14a-d). The main difference between Figures III.14a-d and Figures III.10e-h is that the vertical and horizontal extent of the wake deflection is altered and reduced (compare Figures III.14a and c showing less horizontal deflection and slightly more vertical deflection to Figures III.10g and e, and likewise compare Figures III.14b and d to Figures III.10f and h). With this fluidic actuation, the wake deficit region now is concave in the direction the wake would have deflected without actuation, minimizing the motion induced deflection, and increasing the magnitude of the wake deficit to a level similar to the baseline flow (compare Figures III.14a-d with Figure III.2a). Figures III.14e-h shows the force augmentation with fluidic actuation, where the same flow control pattern of Figures III.10e-h is applied on the model, similar to Figures III.14a-d, except that the actuation is in phase with the motion-induced response. Figure III.14e shows an increased deflection of the deficit in $\widehat{U}_x$ to the centerlines of $y = 0$ and $z = 0$. Because the wake response to this actuation in Figures III.14e-h is tilted in a counter-clockwise direction compared to the nominally vertical and horizontal phases seen before in Figures III.10a-h, the wake lags the model development by slightly more than 30° in phase measured in the baseline (i.e., the phase-averaged $\widehat{U}_x$, response lags the model to a larger extent under the force augmentation control). This increase in phase lag is commensurate with the increase in force hysteresis measured in the force augmentation case shown in red in Figures III.9c-d relative to the unactuated case shown in blue. It is emphasized that these flow control schemes not only decoupled / enhanced the coupling of the wake response (shown in Figure III.14), but they also consequently decoupled / enhanced the coupling of the aerodynamic forces from the model motion (as shown earlier in Figures III.9c-d), which shows substantial control authority for disturbance rejection of wake-induced forces that may potentially be implemented on this model in flight for stabilization or steering.

The unsteady wake structures of the force suppression and augmentation flow control schemes applied in Figure III.14 for the model undergoing Lissajous rotation are analyzed with the same POD mode analysis used in Figure III.11 and shown in Figure III.15. The streamwise velocity modes, $\vec{\phi}_{n,u}$, for $n = 1$ (Figures III.15a and e), 2 (Figures III.15b and f), 3 (Figures III.15c and g), and 4 (Figures III.15d and h) are shown for the force-suppressed wakes of Figures III.14a-d in Figures III.15a-d and of the actuation-induced wakes in Figures III.14e-h shown in Figures III.15e-h. For both the force suppression and augmentation cases, the first two modes (Figures III.15a-b and Figures III.15e-f) have the signature two-fold symmetry seen before in the baseline flow, actuation alone, and motion alone cases (Figures III.3a-b, Figures III.10a-b, and Figures III.10e-f, respectively). However, the tilting angle of the actuated cases is modified, where the first two suppression modes are tilted counterclockwise from the baseline modes (compare Figures III.15a-b with Figures III.10a-b) and the first two augmentation modes are tilted in the opposite, clockwise, direction (compare Figures III.15e-f with Figures III.10a-b). These modes in essence show that the suppression and augmentation actuations have the opposite effect on the baseline flow induced by Lissajous rotation, and the force suppression modes are altered to closer resemble the baseline modes shown in Figures III.3a-b. Effectively, it suggests that this actuation transforms the structure of the flow around this dynamic body to a structure that is reminiscent of the flow on a static body, and is decoupling the wake from the model motion. Following the same analysis, the coupling of the flow response to the motion is enhanced with model actuation. The third and fourth mode of the suppression actuation are similar to the baseline flow as well (compare Figures III.15c and d with Figures III.3d and c). The respective third and fourth mode of the augmented flow show an increased tilting angle and are further spread out in the radial direction than the suppressed mode (compare Figures III.15c-d with Figures III.15g-h). These modes also show a progression of the wake coupling from Figure III.15d (decoupled) to Figure III.11d (no actuation) to Figure III.15h (increased coupling), as seen in the suppressed mode in Figure III.15d, where the structure spreads out and new wake features develop closer to the center.

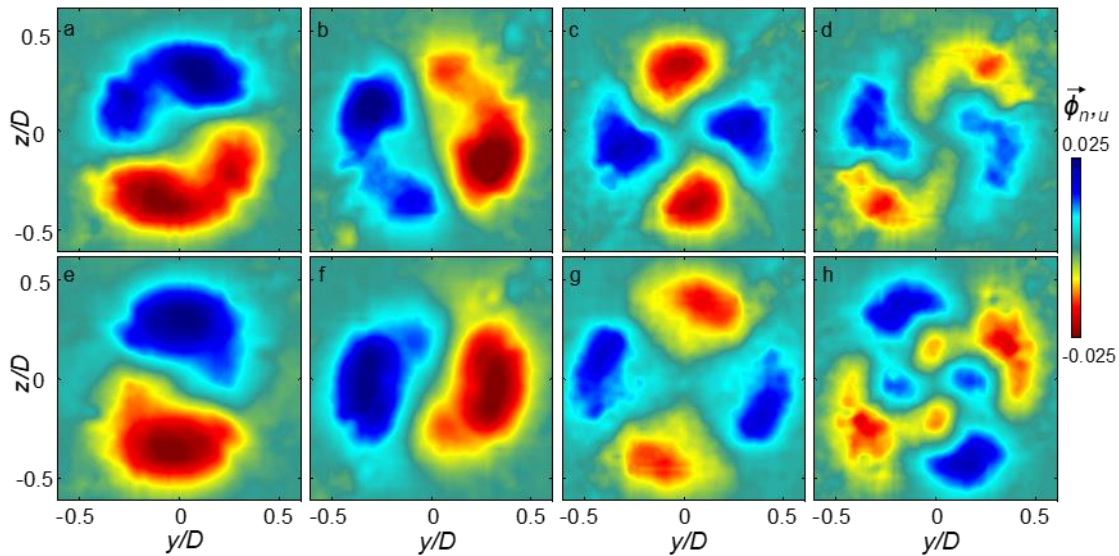


Figure III.15. Contour plots of the POD modes $n = 1$ (a,e), 2 (b,f), 3 (c,g), and 4 (d,h) of streamwise velocity $\vec{\phi}_{n,u}$ for the force suppression (a-d) and force augmentation (e-h) flow control of Figure III.14.

The combined result of the time development of all of the wake studies performed in this chapter are compared in Figure III.16 through a trace of the horizontal centerline (i.e. $z = 0$ with varying y) of all twelve phase averaged PIV flow fields which are equally spaced phases in time. All of these y - t data sets are taken with a frequency of actuation or motion set to $k = 0.207$ ($\tau_{yz} = 0.083$) at a $Re_D = 1.8 \cdot 10^5$ and $x/D = 1.9$ (i.e., one diameter downstream of the aft end of the model). The measured phase averaged velocities $\hat{U}_x$, $\hat{U}_y$, and $\hat{U}_z$, are shown in Figures III.16a-e, III.16f-j, and III.16k-o, respectively, and the measured streamwise vorticity, $\hat{\zeta}_x$, is shown in Figures III.16p-t. The wake responses of a stationary model without actuation (seen previously in Figure III.2) and with actuation (seen previously in Figures III.10e-h) are shown in Figures III.16a,f,k, and p, and Figures III.16b,g,l, and q, respectively. The wake responses of a dynamic 1:1 Lissajous rotating model are shown without actuation (Figures III.16c,h,m and r), with force suppression actuation (Figures III.16d,i,n, and s), and with force augmentation actuation (Figures III.16e,j,o, and t). The analysis is focused on two aspects of the applied flow control: **i)** the appropriate actuation on a stationary model (Figures III.16b,g,l, and q) can successfully mimic the effect dynamic motion has in the wake downstream of an axisymmetric bluff body (Figure III.16c,h,m, and r), and **ii)** timed fluidic actuation on a dynamic body can significantly decouple the wake response from the motion

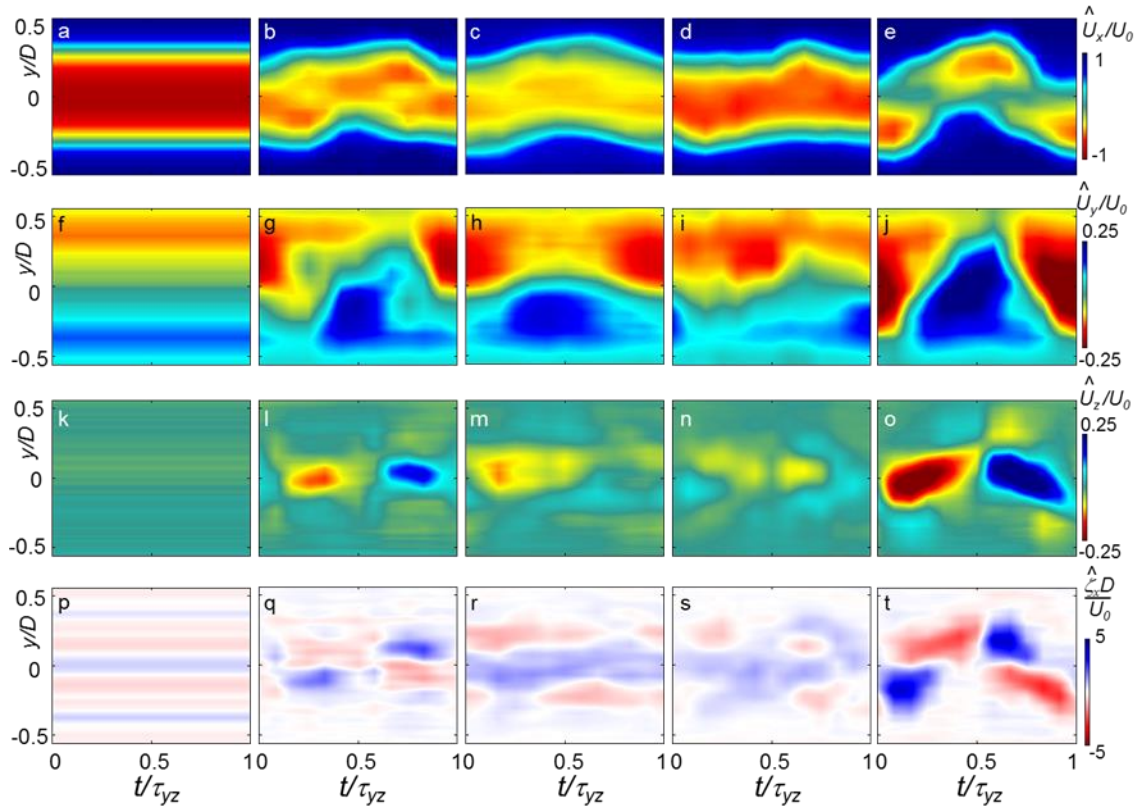


Figure III.16. Time resolved streamwise velocity $\hat{U}_x$ (a-e), cross-stream velocities $\hat{U}_y$ (f-j) and $\hat{U}_z$ (k-o), and the $\hat{\zeta}_x$ (p-t) along the horizontal symmetry line ($z = 0$) at $x/D = 1$ downstream of the aft end of the model, for the stationary model without (a,f,k,p), and with (b,g,l,q) spinning sinusoidal modulated actuation, and the model moving with 1:1 Lissajous rotation at 3° for unactuated (c,h,m,r), suppression (d,i,n,s), and augmentation (e,j,o,t) flow control, with all datasets at $k = 0.207$ and $Re_D = 1.8 \cdot 10^5$.

response, or alternatively enhance this coupling. These effects are clearly demonstrated in $\widehat{U}_x$, where the extent of the time development of the wake deficit on a stationary model resembles the wake deflection from the dynamic model (compare Figures III.16b and c). If the fluidic actuation in Figure III.16b is applied out of phase with the model rotation in Figure III.16d, the wake becomes decoupled and resembles the wake of a static body without actuation (compare Figures III.16a and d), and if the fluidic actuation is in phase (Figure III.16e), the deflection of the velocity deficit is larger with the model motion (i.e., the coupling is enhanced). The cross-stream velocity, $\widehat{U}_y$, shown in Figures III.16f-j which is in-line with the actuators that effect the horizontal centerline of the wake shows the largest changes when actuation is present (in Figures III.16g, i, and j) which dominate the motion-induced wake (Figure III.16h), even for the force suppression case which causes a slight reduction in the deflection of $\widehat{U}_y$, but is still smaller than the reduction of the deflection in $\widehat{U}_x$. The force augmentation introduces the largest values of $\widehat{U}_y$ (see Figure III.16j), which is larger than the effect of $\widehat{U}_y$ in the actuation alone (Figure III.16g) and motion alone (Figure III.16h) combined. The cross-stream velocity, $\widehat{U}_z$, shown in Figures III.16k-o has the smallest variation of all the velocity components, and shows slight deviations with actuation (compare Figures III.16k and l) or with motion (compare Figures III.16k and m), that seem to slightly decrease with force suppression (Figure III.16n) or to significantly enhance with force augmentation (Figure III.16o). The streamwise vorticity, $\widehat{\zeta}_x$, is shown in Figures III.16p-t which initially has a band structure commensurate with the weak eight lobe structure seen before in Figure III.3b shown in Figure III.16p. Upon actuation (Figure III.16q), $\widehat{\zeta}_x$ changes into a two-band structure which switches sign when actuation is effecting the wake (at $t/\tau = 0.083$ and 0.583 , respectively). This structure is far less organized when the model is rotating without actuation (Figure III.16r) where there appears to be a three-band structure with the outer bands severed at the extremes of motion. For the force suppression actuation scheme shown in Figure III.16s, $\widehat{\zeta}_x$ changes into a predominant weak counterclockwise field with little structure. Alternatively, with force augmentation actuation the two band structure shown with actuation is reintroduced except the clockwise $\widehat{\zeta}_x$ lobes are the dominant features (compare Figures III.16t and q).

The final analysis summarizes the effects of all the actuation and rotational motion studied in a spinning motion through tracking the centroid of the respective wake deficit regions (which are defined by having a velocity magnitude of $|\vec{U}| < 0.8U_0$). This centroid location is extracted as y_c and z_c , in the PIV interrogation plane, $1D$ behind the model aft end, and is converted into a wake angle with respect to the center of model motion as $\theta_y = \text{atan}\left(\frac{y_c}{1.9D}\right)$, and $\theta_z = \text{atan}\left(\frac{z_c}{1.9D}\right)$ where $1.9D$ is

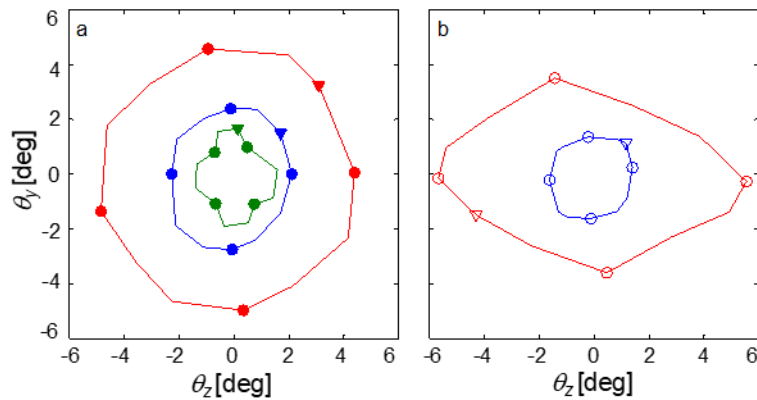


Figure III.17. Angular deflection of the centroid (θ_y , θ_z) of the wake region defined by $|\vec{U}| < 0.8U_0$ for the $k = 0.207$ moving model without (blue), and with force suppress (green), and augment (red) actuations (a), and the stationary model with actuation modulation at $k = 0.207$ (blue) and 1.434 (red) (b).

the streamwise position of the wake measurements relative to where the model pivots as it rotates. Figure III.17 shows these two representative angles of the velocity deficit region plotted against each other with the model undergoing Lissajous rotation (Figure III.17a, in closed symbols), and on a stationary model with applied actuation (Figure III.17b in open symbols). The moving model without actuation at $k = 0.207$ is shown in blue in Figure III.17a, and the application of force suppression and augmentation actuators are shown in green and red, respectively. In addition, there are representative markers in each data trace: the triangle represents when the Lissajous motion and sinusoidal modulation actuation strategies (shown in Figures III.5a and d, respectively) are first applied in time, and the circles mark phases that were used to illustrate the corresponding PIV flow fields of the actuation (i.e., the wake deflections shown in blue circles in Figure III.17a correspond to the phase averaged flow fields in Figures III.10a-d). Figure III.17a clearly shows that upon augmentation actuation (red), the wake deflection is increased by $\sim 80\%$ (from a $\sim 2.5^\circ$ to $\sim 4.5^\circ$ radius of deflection) of the unactuated value, and likewise the suppression actuation reduces the wake deflection by $\sim 40\%$ (from an $\sim 2.5^\circ$ to $\sim 1.5^\circ$ radius of deflection) of the baseline value. The change in phase response of the augmented wake observed in Figure III.14 is clearly present in Figure III.17a, as the red curve is tilted counterclockwise. In addition, the wake angles, θ_y with θ_z , of the suppressed wake centroid do not trace a circular path in time, where the actuation appears to have more of an effect near the points shown in circles (which is also when a maximum single jet actuation effect occurs in the wake, as opposed to a combination of two jet effects). However, these results still demonstrate substantial control authority, similar to what was seen in the lift and side forces in Figure III.9. Figure III.17b shows the deflection of the wake of the stationary model with the spinning actuation at $k = 0.207$ (blue) and $k = 1.434$ (red). These data show that the wake of a stationary model can be effectively controlled to a similar level as a dynamic model that is pitching and yawing out of phase (in this case, the actuation induced a $\sim 1.5^\circ$ radius of deflection in the wake, which was 60% of the dynamic model $\sim 2.5^\circ$). The red trace in Figure III.17b shows the coupling of the fluidic actuation to the natural shedding frequency (commensurate with Figure III.12), where the phase lag of the actuation onset is approximately 210° from the first PIV phase presented. This result shows that in addition to controlling forces induced by the body dynamics, these actuators have the potential to control forces induced by the natural shedding frequency as well, which can possibly be implemented to increase stabilization (or steering) control authority of airborne bodies in future work.

IV. Free-Moving Body in 3-DOF

This study considers the reciprocal dynamics of separated flows coupled to an axisymmetric bluff body platform ($1.3 \cdot 10^5 < Re_D < 1.94 \cdot 10^5$) whose axis is nominally aligned with the flow direction and supported such that it is free to pivot in three degrees of freedom in yaw, pitch, and roll. Flow separation off the aft end of the body results in an unsteady wake that couples to the aerodynamic loads on the body, leading to natural oscillations in yaw and pitch. An inherent difficulty with wind tunnel investigations of nominally ‘free’ aerodynamic bodies is related to their mounting in a wind tunnel’s test section. The model is connected by a low-friction gimbal bearing to a sting whose attitude and displacement are dynamically manipulated by wire mounting to a programmable 6-DOF ($x/y/z$ -translation and roll/pitch/yaw) eight-wire dynamic traverse that is electromechanically driven by a dedicated feedback controller (Lambert et al., 2016b). The model is free to precess in pitch, yaw, and roll in response to flow-induced aerodynamic loads that are controlled using fluidic modification of the aerodynamic coupling to the near wake by four independently-controlled aft mounted synthetic jet actuators. It is shown that this flow control approach can suppress or amplify the natural oscillations by controlling the stability and damping of the model’s motion and impose a desired directional attitude that is effectively decoupled from the trajectory of the moving sting.

IV.1 Experimental Apparatus

This wind tunnel investigation considers flow over the same bluff body model ($D = 90$ mm, $c = 165$ mm) which is now free to respond to flow induced forces, as shown in Figure IV.1. The model, which is scaled based on the earlier investigations of Lambert et al. (2014, 2015a, 2015b) is comprised of a stereo-lithographed outer shell that is mounted on a central bearing around a stationary nose piece, as shown in Figure IV.1a. The model is balanced by a counterweight that is placed in the inner part of the moving nose. Utmost care was put into minimizing the mounting friction and shell interference in response to external pitch, yaw, and roll moments. The bearing is placed at $x_0 = 0.25c$ downstream of the nose, and upstream of the static center of pressure on this model (approximately at $x_{cp} = 0.30c$). As in the study presented in Section III.1, aerodynamic control loads are generated using an azimuthal array of four aft facing independently-driven synthetic jet actuators (each with a rearward facing area $A_J = 0.38$ mm $\times$ 34.3 mm) that are equally distributed around the perimeter of the tail section, shown in Figure IV.1b. The downstream end

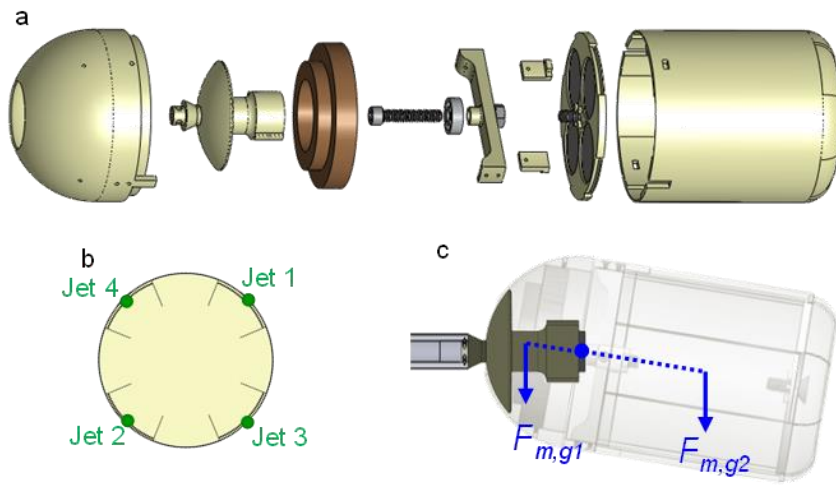


Figure IV.1. Deconstructed view of the 3-DOF model components (a), upstream view of the centered wind tunnel model with four hybrid actuators marked in green (b), and a depiction of the counterbalanced model when it is mounted through the inner nose piece (c).

stationary nose piece, as shown in Figure IV.1a. The model is balanced by a counterweight that is placed in the inner part of the moving nose. Utmost care was put into minimizing the mounting friction and shell interference in response to external pitch, yaw, and roll moments. The bearing is placed at $x_0 = 0.25c$ downstream of the nose, and upstream of the static center of pressure on this model (approximately at $x_{cp} = 0.30c$). As in the study presented in Section III.1, aerodynamic control loads are generated using an azimuthal array of four aft facing independently-driven synthetic jet actuators (each with a rearward facing area $A_J = 0.38$ mm $\times$ 34.3 mm) that are equally distributed around the perimeter of the tail section, shown in Figure IV.1b. The downstream end

of the model has a Coanda surface ($R_c = 12.7$ mm) while each jet is placed in an adjoining backward-facing step with height of $h_s = 1.5$ mm. Actuation leads to the partial attachment of the flow along the Coanda surface resulting in a reaction force by turning of the cross flow into the near wake. In the present investigations, the maximum jet expulsion speed of each jet is set to $U_j = 25$ m/s (a momentum coefficient of $C_\mu = \frac{4U_j^2 A_j^2}{U_0^2 \pi D^2} < 3 \cdot 10^3$), at an actuation frequency of $f_{act} = 0.9$ kHz. The stationary nose of the model is mounted to an external sting as shown in Figure IV.1c, and the realized angular ranges of the motion in pitch, yaw, and roll are $\pm 16^\circ$, $\pm 18^\circ$, and $\pm 180^\circ$, respectively. The center of gravity of the model components, upstream and downstream of the bearing, are placed along the axis of symmetry so that it can reach a moment equilibrium at any angle of pitch or yaw (shown in blue). It is noted that due to imperfections in the model's fabrication (density of the shell) and weight of the electrical wires that drive the actuators there is a preferential roll equilibrium at which the model comes to rest.

The sting that supports this model is wire mounted in an 0.91 m $\times$ 0.91 m open-return wind tunnel and tests are conducted at free stream speeds, U_0 , of up to 30 m/s ($Re_D = 1.95 \cdot 10^5$). The sting motion is controlled using a dynamic 6-DOF eight servo-motor traverse (Figure IV.2a) that is described in detail by Lambert et al. (2016b). Figure IV.2b shows the coordinate system that is

used for the motion of the sting (in red, x_s, z_s), and the model (in blue, x_m, z_m), where the cross-stream directions (y_s, y_m) are defined to make each coordinate system right-handed, and the angles of attack are defined: α_x roll about x axis, α_y pitch about y axis, and α_z yaw about z axis. The sting is $L_s = 0.305$ m long with a diameter $D_s = 16$ mm, and the center of the wire mounts are placed 0.258 m upstream of the model nose. The wire mounts are spaced by ± 0.033 m in the streamwise direction (and symmetrically placed with respective angles of 105° and 75° relative to the central horizontal and vertical planes of the sting). The eight support steel wires (0.96 mm diameter) are selected to be thin enough to decouple

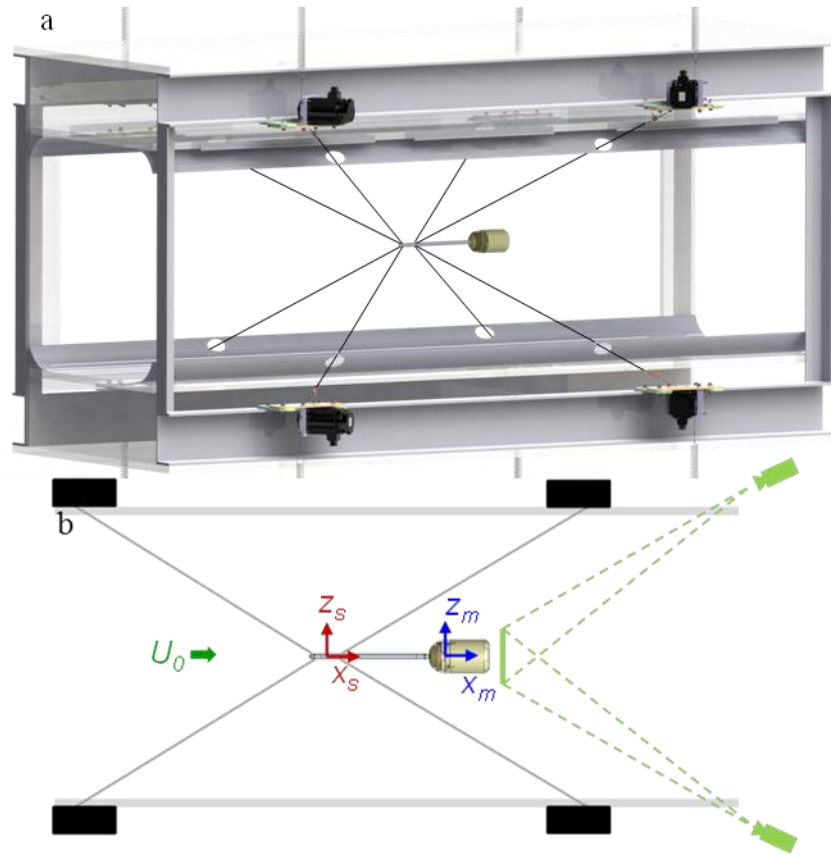


Figure IV.2. Six degrees of freedom traversing mechanism utilizing eight servo actuators and support wires to an upstream sting (a) and a top view schematic of the sting-mounted model, along with the high speed SPIV interrogation region (light green), sting coordinate system (red), and model coordinate system (blue) (b).

their vortex shedding from the model, while minimizing translational and rotational vibrations. Each support wire is fastened to a servo motor, with an in-line load cell, and each motor is attached to an external spring for pretension. The mass of the model is 625 g, and exerts a base gravitational moment on the traverse of 0.187 Nm. The servo motor commands are determined with a PID controller to reject disturbances and set a desired trajectory of the sting, and unless otherwise noted the default pitch and yaw of the sting is aligned with the free-stream direction. The electrical wires for the flow control synthetic jets are weaved along the back four support wires of the sting and through the tunnel walls, while the support wires and sting provide an electrical ground. An external six-camera, high-speed (600 fps) motion analysis tracking system measures the model's motion in three degrees of freedom and is simultaneously used for calculating the pitch and yaw of the sting. In addition, the velocity in the wake is measured using a high speed (500 fps) stereo particle image velocimetry (SPIV) system, as shown schematically in a top view in Figure IV.2b. The PIV system includes two triggered 14-bit cameras, each at an angle of 25° (shown in green) which resolve all three components of the velocity field in an interrogation region of $1.8D \times 1.8D$, $0.25D$ downstream of the aft end of the model ($1.63D$ downstream of the axis of pitch/yaw rotation).

The model dynamics are estimated as a combination of second order mass-spring-damper systems with spring constant K_X , damping constants C_X and C_{YZ} , inertias I_X and I_{YZ} , and external roll, pitch, and yaw moments (M_R , M_P , M_Y):

$$I_X \ddot{\alpha}_x + C_X \dot{\alpha}_x + K_X \alpha_x = M_R(t), \quad I_{YZ} \ddot{\alpha}_y + C_{YZ} \dot{\alpha}_y = M_P(t), \quad I_{YZ} \ddot{\alpha}_z + C_{YZ} \dot{\alpha}_z = M_Y(t) \quad (1)$$

This model excludes any frictional effects caused by the bearing and assumes that the system responds in a symmetric fashion to both pitch and yaw dynamics (with the same damping and inertia), and that these parameters are independent of the roll dynamics, which has its own set of damping, inertia, and spring constants. It is noted that the model can be in equilibrium at any realizable pitch and yaw angle, at a fixed roll angle (set to zero), and therefore the spring constant in pitch and yaw is neglected. To measure the damping and spring constants, the model is perturbed in all three degrees of freedom and the measured motion responses are shown in Figure IV.3, where the inertia components $I_X = 6.45 \cdot 10^{-4} \text{ kgm}^2/\text{rad}$ and $I_{YZ} = 1.25 \cdot 10^{-3} \text{ kgm}^2/\text{rad}$ are extracted from the known geometry of the model ignoring the weight of electrical wires. The roll is first tested in Figure IV.3a where the model is manually set to $\alpha_x = -50^\circ$ and is released at $t = 0$. The measured trace is marked in blue and a trace for the least squares fit to a mass-spring-damper ($K_X = 3.2 \cdot 10^{-3} \text{ Nm/rad}$, $C_X = 1.8 \cdot 10^{-4} \text{ Nms/rad}$) is marked in green. The traces of the respective

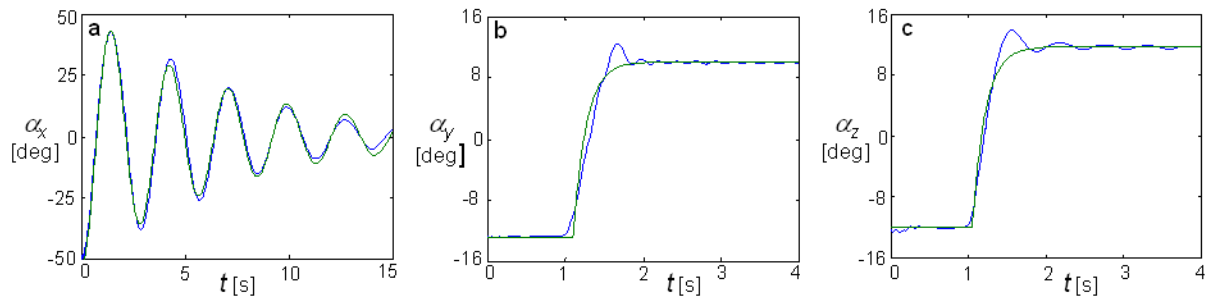


Figure IV.3. Realized perturbations of the model in roll (a), pitch (b), and yaw (c), shown in blue, and a least squares fit to a mass-spring-damper model shown in green.

pitch and yaw responses (marked in blue) are shown in Figures IV.3b and c where they are initially set to a negative value near the extremes of motion and at $t = 1$ s are prodded in the positive direction. Traces for a least squares fit to a damping model (requiring the damping constant to be the same for pitch and yaw) is shown in green with $C_{YZ} = 8.5 \cdot 10^{-3}$ Nms/rad.

IV.2 Coupled Aerodynamic Response of the Freely Precessing Model

A primary objective of the present research is to demonstrate efficacy and characterize the mechanisms of synthetic jet flow control for fluidic modification of the aerodynamic coupling between the axisymmetric bluff platform and its near wake to effect the desired attitude characteristics that overcome the natural response to the flow. To begin with, the baseline response of the freely precessing (roll, pitch, and yaw) model to its interaction with the embedding flow is characterized in detail. In the present investigations, the Reynolds number of the model is high enough so that initial flow perturbations (e.g., by coupling to its near wake) rapidly amplify, and the model oscillates within a finite envelope of vertex angles between its centerline and the flow direction. It should be noted that in free motion, small asymmetries in the model's enclosure and non-uniform friction effects in the gimbal's bearing can bias the temporal variations in the vertex angle. In the present experimental setup, the mounting wires of the sting are not spaced equally in

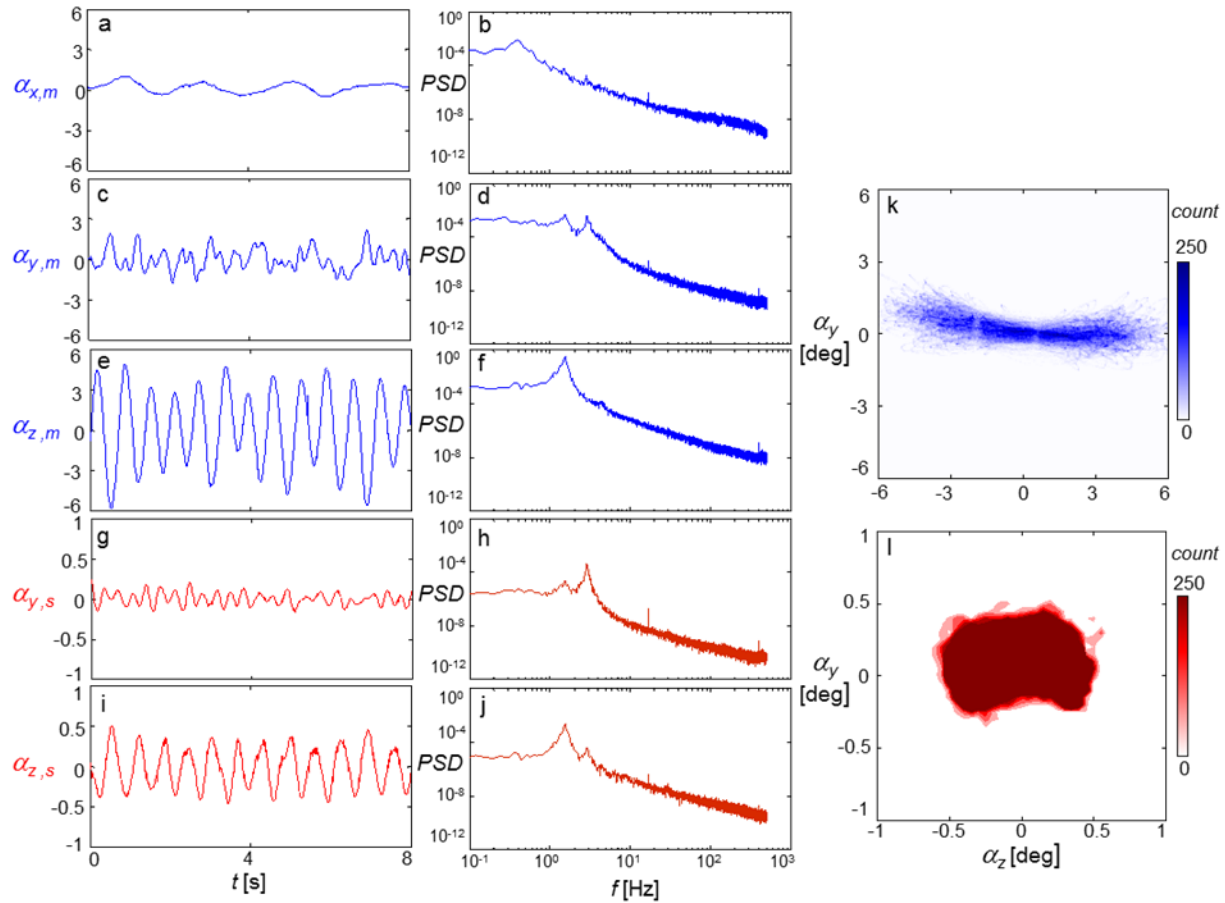


Figure IV.4. Flow-induced dynamics of the **model** and **sting** at $Re_D = 1.62 \cdot 10^5$ for instantaneous time traces of roll (a), pitch (c, g) and yaw (e, i), their respective power spectra (b, d, f, h, j), and histograms of the respective pitch and yaw over 25 measurements (k, l).

the azimuthal direction, in order to control the sting's roll angle, and, as a result, the sting is slightly less stable in yaw than in pitch. In the absence of flow control, this slight asymmetry causes the model's time periodic oscillations to be directionally biased in yaw. An illustration for an oscillation is shown in Figure IV.4 at $U_o = 25$ m/s ($Re_D = 1.62 \cdot 10^5$). Figures IV.4a, c, and e show an eight-second time trace of the model's angular positions in α_x (roll), α_y (pitch), and α_z (yaw), respectively, and the corresponding spectra (frequency resolution of 0.05 Hz) are shown in Figures IV.4b, d, and f. Each of these traces exhibits distinct frequency and amplitude responses in roll (0.4 Hz, 0.7°), yaw (1.55 Hz, 5.2°), and pitch (2.90 Hz, 1.5°), with a characteristic baseline period is defined off of yaw: $\tau_b = 0.65$ s. These data show that the characteristic pitch frequency is two times higher than the corresponding yaw frequency and the pitch spectrum also contains a second peak at the yawing frequency. Although the support sting is commanded to be held stationary, Figures IV.4g and i show that the sting pitch and yaw angles vary with amplitudes of 0.25° and 0.5° respectively, and the spectra of the sting motion (Figures IV.4h and j) contain the model's yaw and pitch frequencies indicating the model and sting are indeed coupled (Section III.1 shows that this coupling can be effectively enhanced or suppressed with actuation). The data for the base motion of the model are binned into a 2-D histogram of the yaw and pitch angles (200 seconds with a bin width of $\Delta\alpha_y = \Delta\alpha_z = 0.05^\circ$) and plotted for the model (Figure IV.4k) and sting (Figure IV.4l). These histograms show that the model undergoes its maximum pitch excursion at the extremes of the yawing angle, and its path is more exaggerated in yaw than the sting's path (compare the scatter in Figures IV.4k and l).

The root-mean-square (RMS) amplitudes of oscillation in roll ($\alpha_{x,RMS}$, Figure IV.5a), pitch ($\alpha_{y,RMS}$, Figure IV.5b), and yaw ($\alpha_{z,RMS}$, Figure IV.5c), vary with Re_D , and are shown for the sting (red) and model (blue) throughout $1.30 \cdot 10^5 < Re_D < 1.94 \cdot 10^5$ using 200 second records. Figure IV.5a shows the roll amplitude which has the smallest excursion, approximately twice as small as the pitch response and significantly smaller than the yaw response, although it grows throughout the measured range of Re_D . Figures IV.5b and c show that the RMS of the sting pitch and yaw, as well as the model pitch, increase with Re_D throughout the range $1.30 \cdot 10^5 < Re_D < 1.94 \cdot 10^5$. The model yaw however (the primary excursion) only increases to $Re_D = 1.62 \cdot 10^5$, with a maximum RMS level of 4° (about 5.6° amplitude), and then decreases to 3.1° (about 4.4° amplitude) at

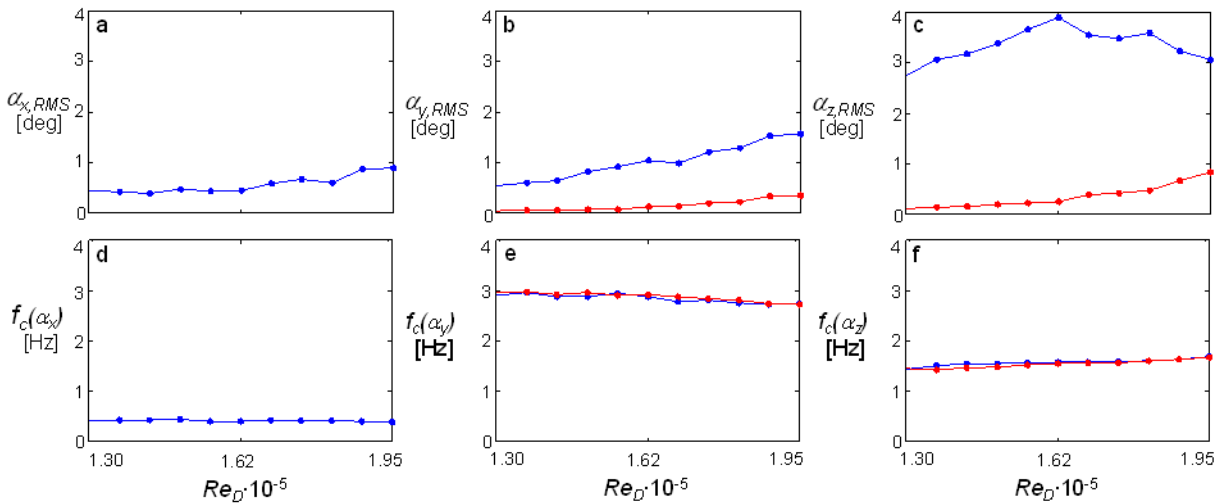


Figure IV.5. Variations with Re_D of the **model** and **sting**: average RMS amplitude (a-c) and characteristic frequency (d-f) in the roll (a, d), pitch (b, e), and yaw (c, f) directions.

$Re_D = 1.95 \cdot 10^5$. It is interesting to note that throughout the entire range of Re_D , the pitch to yaw ratio of the model increases (from 17% at $1.30 \cdot 10^5$ to 55% at $1.95 \cdot 10^5$), while the pitch to yaw ratio of the sting remains nearly invariant (about 42%) indicating that the asymmetries in the experimental setup become less relevant at higher wind speeds. The corresponding variation of f_c with Re_D for both the sting and model responses are shown in Figures IV.5d-f in roll, pitch, and yaw, respectively. Figure IV.5d shows the roll excursion is approximately six times slower than the pitch disturbance and three times slower than the yaw disturbance. Figure IV.5e shows that the disturbance frequency of the model and the sting in pitch is the same, suggesting that the flow initially disturbs the coupled model-sting system together and the difference in amplitude develops afterwards. Figure IV.5f also shows the matching disturbance frequency between the model and sting in yaw, but in addition shows the yawing frequency grows with Re_D (as opposed to Figure IV.5e where the pitching frequency decays with Re_D), indicating that the disturbance frequencies in pitch and yaw are not harmonics of each other even though they differ by nearly a factor of two. The majority of the remaining investigations are conducted at $Re_D = 1.62 \cdot 10^5$ for which the sting dynamics are considered stable and negligible (having an amplitude of less than 0.25°).

The near wake behind the unactuated model at $Re_D = 1.62 \cdot 10^5$ is measured using SPIV (cf. Figure IV.2b) yielding a time resolved (500 Hz) 3-D velocity field including the streamwise (u) and cross stream velocities (v and w) velocity components, in the x , y , and z directions, respectively. The interrogation region is placed in the y - z plane at $x_m/c = 0.9$ (or $0.25D$ downstream of the aft end of the model), spanning $1.8D$ in y and in z ($y = \pm 1.8R$, and $z = \pm 1.8R$, centered about the streamwise axis of the model's gimbal). The instantaneous measurements are not always resolved over the full interrogation window, (depending on the seeding particle density), and therefore the recorded set of 2,500 images is reconstructed out of the first 20 modes of a proper orthogonal decomposition (POD) algorithm of the wake, and are implemented in the same fashion as those of the wake behind a similar model with controlled pitch-yaw rotation dynamics (Lambert et al., 2016a). These modes are derived from the instantaneous velocity vectors that are decomposed as:

$$\vec{X}^*(t) = \vec{X}(t) - \langle \vec{X} \rangle = \sum_{n=1}^N a_n(t) \cdot \vec{\varphi}_n \quad (2)$$

where $\vec{X}(t)$ is an instantaneous state vector chosen to be a column vector of $u(t)$, $v(t)$, and $w(t)$ concatenated vertically, $\langle \vec{X} \rangle$ is the time averaged value of $\vec{X}(t)$, $\vec{\varphi}_n$ is the n th POD mode, $a_n(t)$ is the weighted time coefficient of the n th POD mode, and N is the rank of the data set (i.e. the maximum independent vectors either spatially or in time). The analysis for the POD modes in the current work is performed by extracting the first 20 modes, and the dataset includes 2,500 time realizations. In addition, as $\vec{\varphi}_n$ is effectively an eigenvalue of $\overrightarrow{X^*} \cdot \overrightarrow{X^*}^T$, the energy percentage of each mode can be defined through the commensurate eigenvalues λ_n :

$$E_n(\%) = \frac{100|\lambda_n|^2}{\sum_{i=1}^N |\lambda_i|^2} \quad (3)$$

The POD modes are then numbered from highest to lowest energy percentage.

Following the POD reconstruction of the flow field, the streamwise vorticity, ζ_x , is calculated for each instantaneous velocity field. Figures IV.6a-d show an instantaneous reconstructed flow field

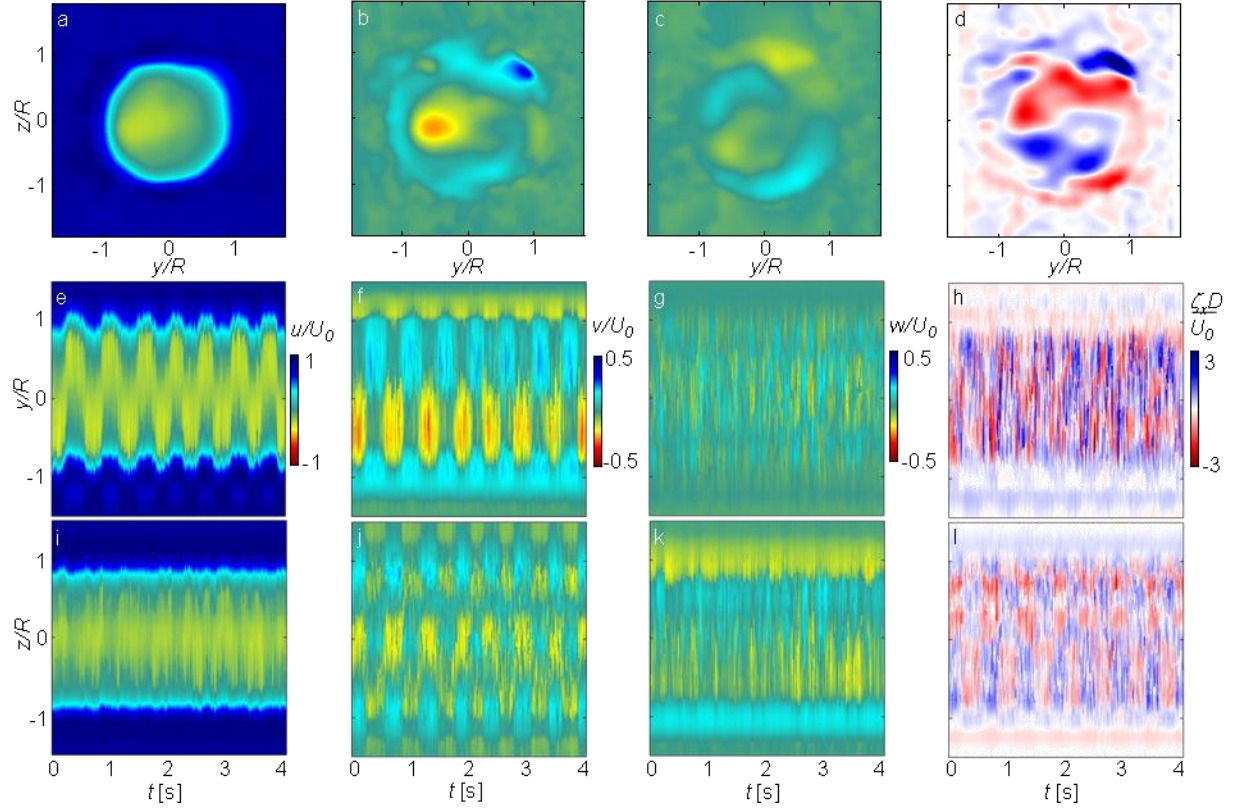


Figure IV.6. An instantaneous snapshot of the wake behind the unactuated model (a-d), and time-traces of the horizontal centerline (e-h), and vertical centerline (i-l) streamwise u (a, e, i), and cross-stream v (b, f, j) and w (c, g, k) velocity components, and streamwise vorticity (d, h, l) at $Re_D = 1.62 \cdot 10^5$.

with velocity components u (Figure IV.6a), v (Figure IV.6b), w (Figure IV.6c) and ζ_x (Figure IV.6d) using color raster maps on a 55×55 grid. The key features in this flow field are the velocity deficit at the center of the streamwise direction (Figure IV.6a), and inner and outer layers of cross-stream velocities of opposing senses (Figure IV.6b and Figure IV.6c). It is noted that because the primary oscillation is in the yaw direction, the horizontal velocity (v) is typically larger in magnitude than the vertical velocity (w). The corresponding vorticity field (Figure IV.6d) also has two dominant concentrations of opposing sign, although the dominant magnitudes typically coincide with the largest cross-stream velocity (compare Figures IV.6b and d). In order to characterize the time-dependence of the flow behind the model (and later characterize the differences effected by the actuation), four-second time traces of u , v , w , and ζ_x along the y axis ($-1.5 < y/R < 1.5$ and $z/R = 0$ in Figures IV.6a-d), and the z axis ($-1.5 < z/R < 1.5$ and $y/R = 0$ in Figures IV.6a-d) are shown in Figures IV.6e-h and Figures IV.6i-l, respectively. The time evolution of the streamwise velocity shown in Figures IV.6e and i shows a primary deviation in the horizontal centerline, comparable to the yaw dynamics, and a secondary deviation in the vertical centerline comparable to the pitch dynamics (compare Figure IV.6e with Figure IV.4e, and Figure IV.6i with Figure IV.4c). Other noteworthy features are in the horizontal centerline v (Figure IV.6f), and the vertical centerline w (Figure IV.6k) components, both of which have an outer layer where flow is advected towards the center, and an inner layer whose sign of oscillation depends on the pitch and yaw location of the model, where the magnitude of these regions approach 30% of the freestream speed. By comparison, these effects are smaller and more diffused

in the horizontal centerline w (Figure IV.6g) and vertical centerline v (Figure IV.6j) although still with a periodic pattern that is aligned with model motion. An interesting feature of this wake is that the horizontal centerline vorticity has the same structure as the vertical centerline velocity even though it is measured in different locations (compare Figure IV.6k and Figure IV.6h), and the same holds for the horizontal velocity and vertical vorticity (compare Figure IV.6f and Figure IV.6l). This suggests that part of the driving aerodynamic force that maintains the instability in the model yaw (as well as horizontal wake development) is induced by the structure of the streamwise vorticity concentrations in the vertical direction.

The POD modes of the subset of $\vec{\varphi}_n$ that correspond to the streamwise velocity, $\vec{\varphi}_{n,u}$ are shown in Figure IV.7 with $n = 1$ (Figure IV.7a), 2 (Figure IV.7b), 3 (Figure IV.7c), 4 (Figure IV.7d), 5 (Figure IV.7e), 6 (Figure IV.7f), and 7 (Figure IV.7g), along with the energy contribution of these modes (Figure IV.7h). It is noted that the first mode shown in Figure IV.7a which represents the yaw instability in the wake is 9% of the total energy, and has approximately three times the next mode's energy. The second mode (Figure IV.7b) represents the pitch instability and is similar to a rotated version of the first mode, with 3% of the total energy. The next four modes (Figures IV.7c-f) have a similar structure with a central velocity contribution and two dominant external contributions which are attributed to fluctuations in the shear layer. The seventh mode is a two lobe structure which has a preferential shedding in the diagonal direction which is similar to a mode observed in prior studies on this model (Lambert et al., 2016a) when the model was in a fixed stationary centered position, where the diagonal directional preference was attributed to the preferential shedding off of the aft end model due to the actuators and Coanda surface.

The subset of $\vec{\varphi}_n$ used to present the streamwise velocity modes in Figure IV.6 is also used to extract the corresponding cross-stream velocity modes, $\vec{\varphi}_{n,v}$ and $\vec{\varphi}_{n,w}$. These two velocity modes are then used to calculate their respective streamwise vorticity mode:

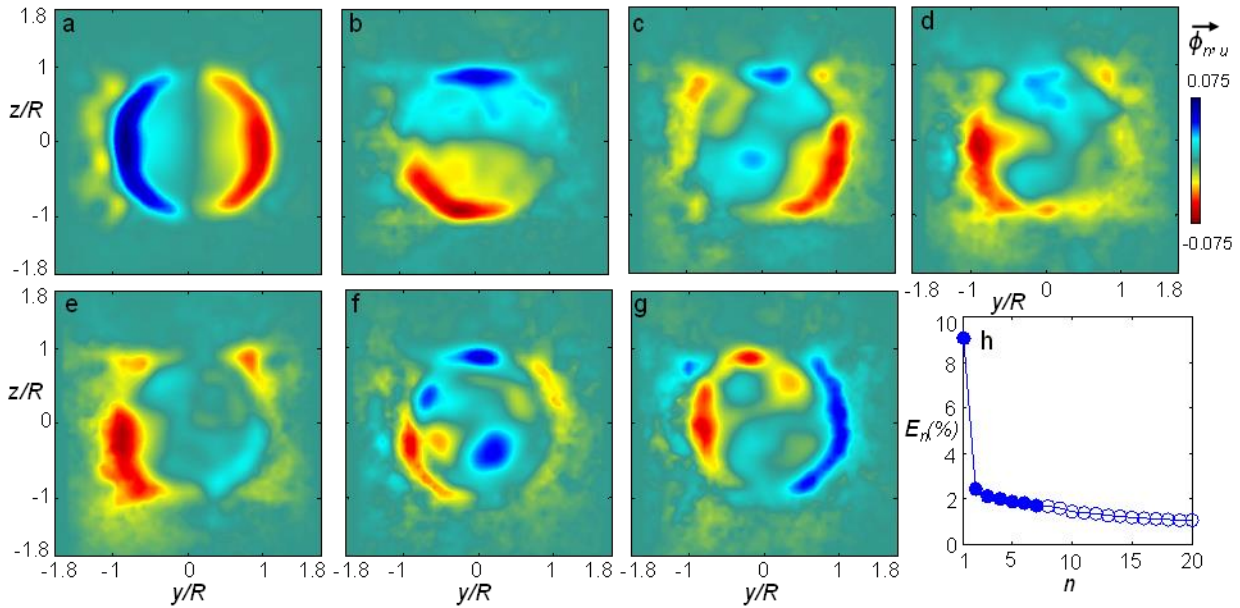


Figure IV.7. Color raster plots of the streamwise velocity POD modes, $\phi_{n,u}$, with mode number $n = 1$ (a), 2 (b), 3 (c), 4 (d), 5 (e), 6 (f), and 7 (g), and the energy distribution (h) of the modes for the base flow at $Re_D = 1.62 \cdot 10^5$.

$$\vec{\phi}_{n,\zeta} = \delta \cdot \left(\frac{d\vec{\phi}_{n,w}}{dy} - \frac{d\vec{\phi}_{n,v}}{dz} \right) \quad (4)$$

It should be emphasized that these vorticity modes are *not* POD modes of the vorticity field (rather a result of the POD analysis of the velocity field). The derivatives are taken in Equation (4) using a 3×3 stencil and a first order centered method, and therefore $\vec{\phi}_{n,\zeta}$ is normalized by the grid spacing between velocity vectors, $\delta = 2.9$ mm, which is uniform in the y and z directions. The resultant normalized cross-stream velocity modes and their derivative-normalized vorticity are plotted in Figure IV.8 with the first seven modes, similar to Figures IV.7a-g. The pairing of the first and second mode seen in the streamwise velocity in Figures IV.7a and b is observed in the cross-stream velocities in Figures IV.8a and b, each with predominant counter-rotating vorticity concentrations, where the counter-rotating concentration pair in the vertical direction in Figure IV.8a effect a larger area of the wake. In the cross-stream velocity modes, it becomes apparent that modes 3 (Figure IV.8c) and 6 (Figure IV.8f) are rotated pairs with an internal vertical structure that is centered between two external opposing vortices. Likewise, modes 4 (Figure IV.7d) and 5 (Figure IV.8e) are rotated pairs with an internal vorticity concentration that is off center from two external vorticity pairs. The modes 3-6 are associated with the shear layer dynamics as mentioned in the discussion of Figures IV.7d-f, but the cross-stream modes primarily indicate whether the flow fluctuations cross through the center of the wake (modes 3 and 6), or are asymmetric about the center of the wake (modes 4 and 5). Although the streamwise velocity component of the seventh mode was similar to a mode observed on the stationary model, the commensurate cross-stream velocity component of this mode is significantly different, having four counter-rotating vorticity pairs instead of a single one that spreads across the entire wake. This difference is attributed to the vorticity structure breaking up and becoming less organized when the wake drives model oscillation as opposed to when the model is constrained to a fixed, centered orientation.

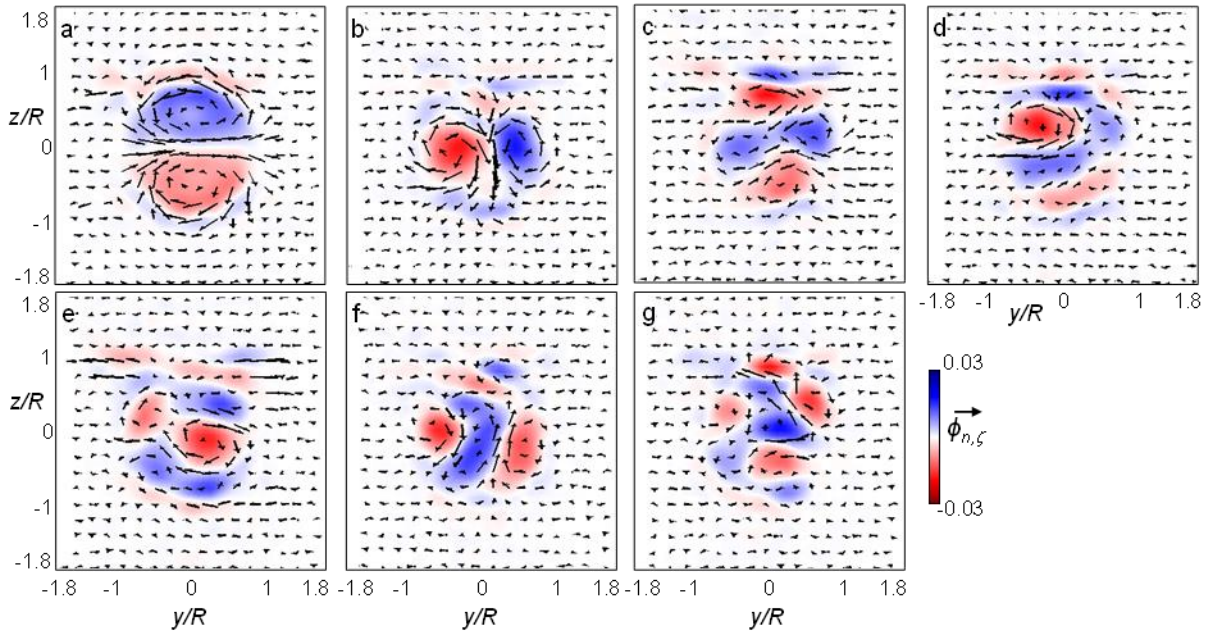


Figure IV.8. Quiver plots of the cross stream velocities' POD modes, $\phi_{n,v}$ and $\phi_{n,w}$, with mode number $n = 1$ (a), 2 (b), 3 (c), 4 (d), 5 (e), 6 (f), and 7 (g), colored by their streamwise vorticity, $\phi_{n,\zeta}$, for the base flow at $Re_D = 1.62 \cdot 10^5$.

IV.3 Effects of Open-Loop Actuation

The present investigations implement both open- and closed-loop flow control approaches. While open-loop control is characterized by continuous jet actuation regardless of the model's attitude, in closed-loop control the jets are driven (by the computer-based controller) in response to the measured, time-dependent attitude of the model.

The effect of open-loop actuation is investigated at $U_o = 25$ m/s ($Re_D = 1.62 \cdot 10^5$) using single-jet or four-jets control. Instantaneous pitch (α_y) and yaw (α_z) angles of the model are measured over sampling intervals of three seconds where actuation is activated $t = 1$ s. The angular velocities, $\dot{\alpha}_y$ and $\dot{\alpha}_z$, as well as the corresponding accelerations are computed from time series of the angles and used to compute the aerodynamic moments on the model using Equation (1). The pitch and yaw moment coefficients (C_M and C_Y) are then calculated from these moments as $C_{M,Y} = \frac{M_{y,z}}{\frac{\pi}{8} \rho U^2 D^2 c}$.

Phase plots of the angular velocities and aerodynamic moments plotted against the instantaneous angle (in both pitch and yaw) are shown in Figure IV.9 in three separate regimes: unactuated in black ($0 < t < 1$ s), ‘transient’ in cyan ($1 < t < 2$ s), and the controlled dynamics in blue ($1 < t < 2$ s). Figures IV.9a, b, e, and f show single-jet actuation while Figures IV.9c,d,g, and h show four-jets actuation, with phase plots of the pitch rate (Figures IV.9a and c), yaw rate (Figures IV.9b and d), pitch moment (Figures IV.9e and g), and yaw moment (Figures IV.9f and h). It is interesting to note that although the motion excursion is significantly larger in yaw than in pitch, the magnitudes of the unactuated moments are similar; the maximum in the pitch moment is approximately 80% of the yaw moment without actuation. In addition, the baseline yaw moment response (Figures IV.9f and h, black trace) agrees with the yaw response of the model constrained to a free 1-DOF yaw motion (Lambert et al., 2013). Upon activating single-jet control (jet 3 in Figure IV.1), α_y gains a bias of 2° and decreases in the fluctuations in position and velocity of about 40% (Figure IV.9a), and in its moment by about 30% (Figure IV.9e). A similar trend occurs in α_z , with a bias of 2° and decreases in fluctuations of its position and velocity of about 60%

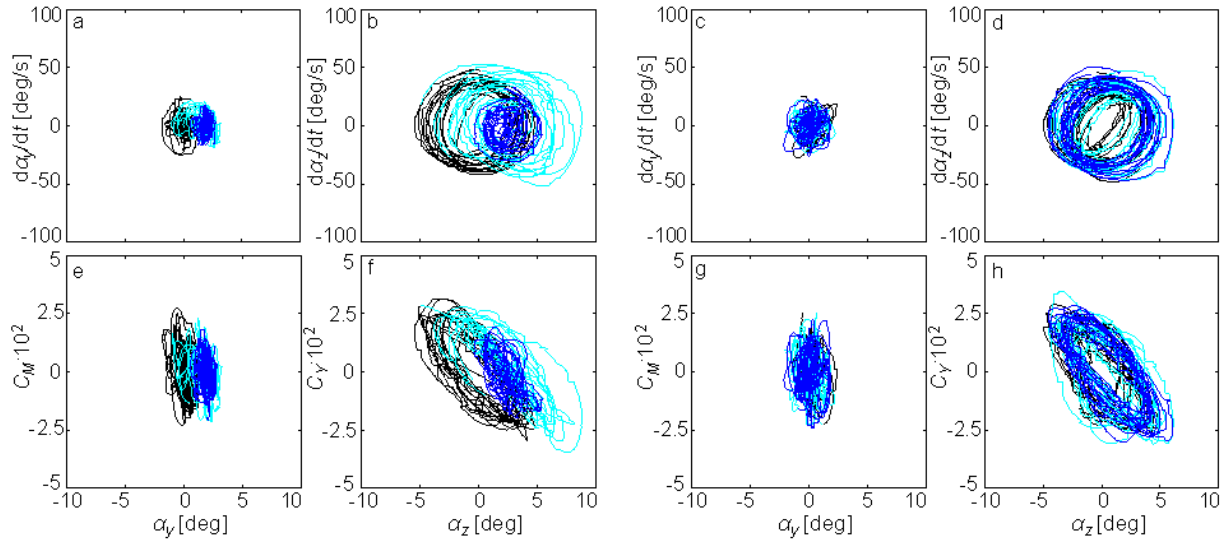


Figure IV.9. Phase plots of 10 instantaneous traces of the model pitch rate, $\dot{\alpha}_y$ /dt (a, c), yaw rate, $\dot{\alpha}_z$ /dt (b, d), and moment coefficients, C_M (e, g) and C_Y (f, h), for open-loop actuation by a single jet (a, b, e, f) and all four jets (c, d, g, h) for 0-1 s before (black), and 0-1 s (cyan) and 1-2 s (blue) after the onset of actuation at a fixed $C_\mu = 0.003$ and $Re_D = 1.62 \cdot 10^5$.

(Figure IV.9b), and of its moment by about 50% (Figure IV.9f). Although the transient behavior in α_y shown in cyan is a somewhat direct transition between the unactuated and actuated states, α_z instead has a significant amount of overshoot, with a growth of amplitude from 5.5° to 7° about a bias of 2° and then a collapse into its developed amplitude of 2.5° . This transition from the unactuated flow to the new developed amplitude occurs in about 1 second (approximately 0.6 baseline oscillation periods, τ_b , and 150 convective time scales, $\tau_{\text{conv}} = c/U_0$). In contrast, four-jets actuation (Figures IV.9c, d, g and h) does not have a well-defined state transition, where the actuated dynamics appear to be similar to the unactuated model with a slight increase of the fluctuations in α_z and $\dot{\alpha}_z$ during the transition (see Figure IV.9d and h) and almost no changes in pitch (see Figures IV.9c and h). The significance of the lack of change in the system response with four-jets actuation, along with the overshoot and temporary increased vibrations with activation of an open-loop single-jet, shows the need for timed actuation in controlling this free responding model.

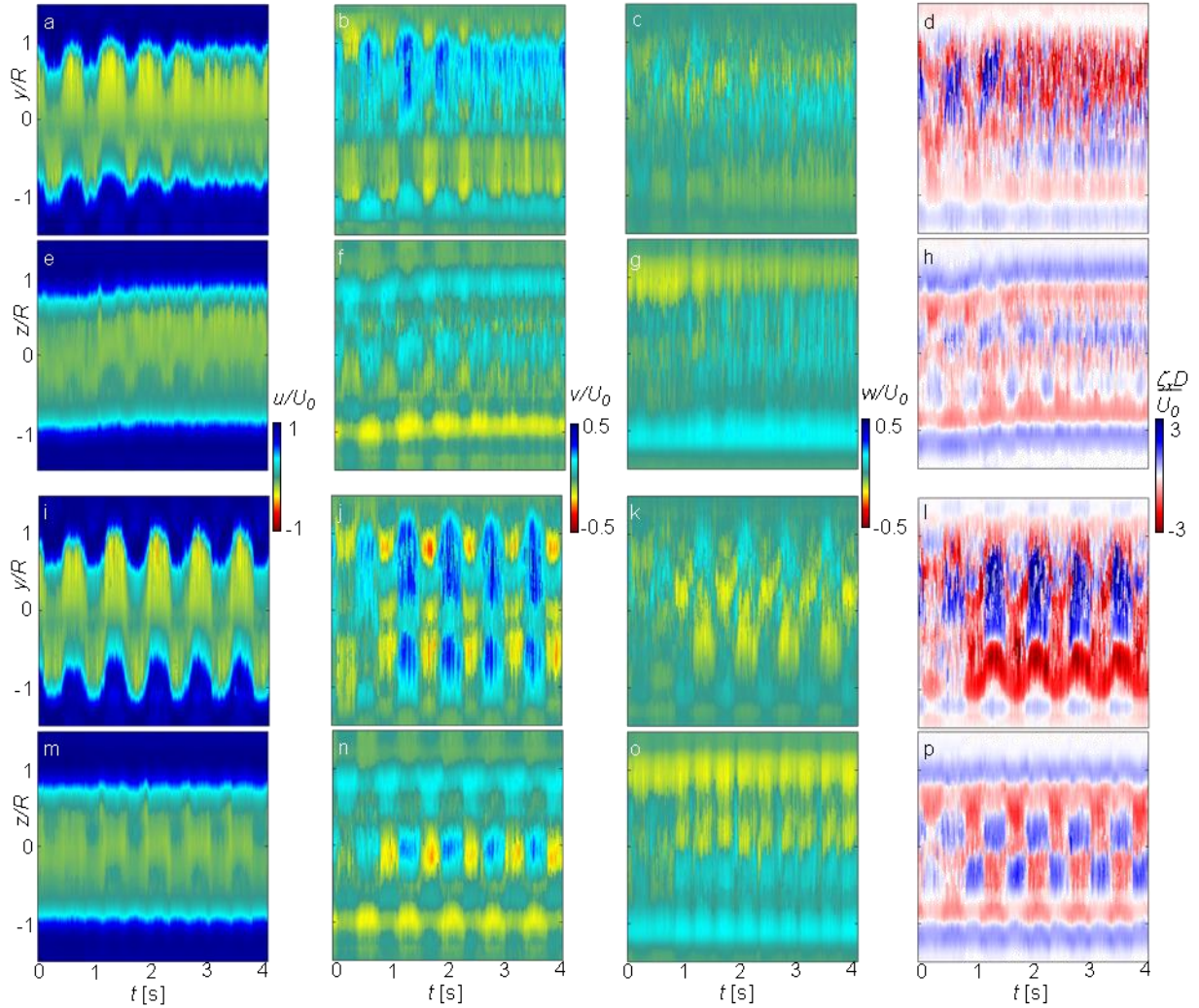


Figure IV.10. The time-traces of the horizontal centerline (a-d, i-l) and vertical centerline (e-h, m-p) streamwise, u (a,e,i,m), and cross stream v (b,f,j,n) and w (c,g,k,o) velocity components, and streamwise vorticity (d,h,l,p) for the open-loop single-jet (a-h) and four-jets (i-p) actuation applied at $t = 1$ s, at a fixed $C_\mu = 0.003$ and $Re_D = 1.62 \cdot 10^5$.

The wake behind the model upon the onset of the open-loop actuations shown in Figure IV.9 is analyzed using the time-dependent PIV measurements along the wake's y and z axes that are shown in Figures IV.10a-h and Figures IV.10i-l for single-jet and four-jets actuation, respectively. These data are presented in a similar fashion to Figures IV.6e-l over a four-second time trace, with actuation applied at $t = 1$ s. First, the single-jet actuation is analyzed in detail in Figures IV.10a-h (control is applied using jet 1, rather than jet 3 in Figure IV.7), which leads to an attitude bias in $-\alpha_y$ and $+\alpha_z$, and can be clearly observed in Figures IV.10a and e, with the model wake having a bias in $+y$ and $+z$ by about 10 mm each. The time evolution of the u velocity for the single-jet actuation shows a reduction of the wake oscillation by about 40% at $t = 2$ s and about 70% at $t = 3$ s in the horizontal direction (Figure IV.10a), while the oscillation amplitude does not noticeably change in the vertical direction (Figure IV.10e). It is noted that the development of the wake deficits in the horizontal and vertical directions are comparable with the development of the yaw and pitch angular velocities (compare Figure IV.10a with Figure IV.9b and Figure IV.10e with IV.9a). The v component of the wake velocity in the horizontal centerline is altered by the jets from its initially symmetric distribution by losing its negative v band at $y/R = 1$ and gaining a large region of positive v (see Figure IV.10b). The actuation also develops a 'rolling' feature in the wake with a $-v$ component at $z/R = 1$, and a $+v$ component at $z/R = -0.9$. This rolling feature is not observed in the horizontal wake centerline, and similar to the baseline flow, the w component of the wake is not significant on the horizontal centerline (cf. Figure IV.10c and Figure IV.6g). The w component in the vertical direction is similar to the v component in the horizontal direction where it is also dominated by a $+w$ component throughout the wake, but this added positive velocity region is added in the inner wake, and the $-w$ band at $z/R = 1$ remains intact. The streamwise vorticity, ζ_x , becomes more organized in the horizontal centerline of the wake (Figure IV.10d), where the time-dependent oscillations of vorticity develop into a predominantly negative ζ_x at $y/R > 0$, and bands of alternating signs of ζ_x for $y/R < 0$, which are roughly time-invariant. Figure IV.10h shows that the structure of ζ_x does not have a significant change in the vertical centerline, and primarily is only translated in the $+z$ direction. Next, the open-loop four-jets actuation is shown in Figures IV.10i-p. For this actuation, the oscillation of the location of the wake deficit increases in both y and z , as shown in Figures IV.10i and m, respectively. The horizontal centerline v component shown in Figure IV.10j shows the introduction of oscillating negative and positive regions with magnitudes larger than the baseline, with a slightly larger positive component attributed to slight mismatches in the strength of the synthetic jet actuators. Similar to the single-jet, the four-jets actuation develops a 'rolling' feature in the wake seen by the $-v$ component at $z/R = 0.9$, and a $+v$ component at $z/R = -1$ in Figure IV.10n. It is noteworthy that the corresponding baseline v component has bands of the same sense, and did not have the 'rolling' feature in the vertical centerline (see Figure IV.6j). The cross stream w components of the wake in both the vertical and horizontal centerlines are similar to the baseline flow, with a notable increase in magnitude in the inner wake region ($|y/R| < 0.8$ in Figure IV.8k, and $|z/R| < 0.8$ in Figure IV.10o). This trend is also true with the vorticity, seen in Figures IV.10l and p, where the structure remains similar to the baseline with an increased magnitude in the inner wake, except this increase is significantly more substantial in the horizontal centerline as opposed to the vertical centerline (compare Figures IV.10l and p).

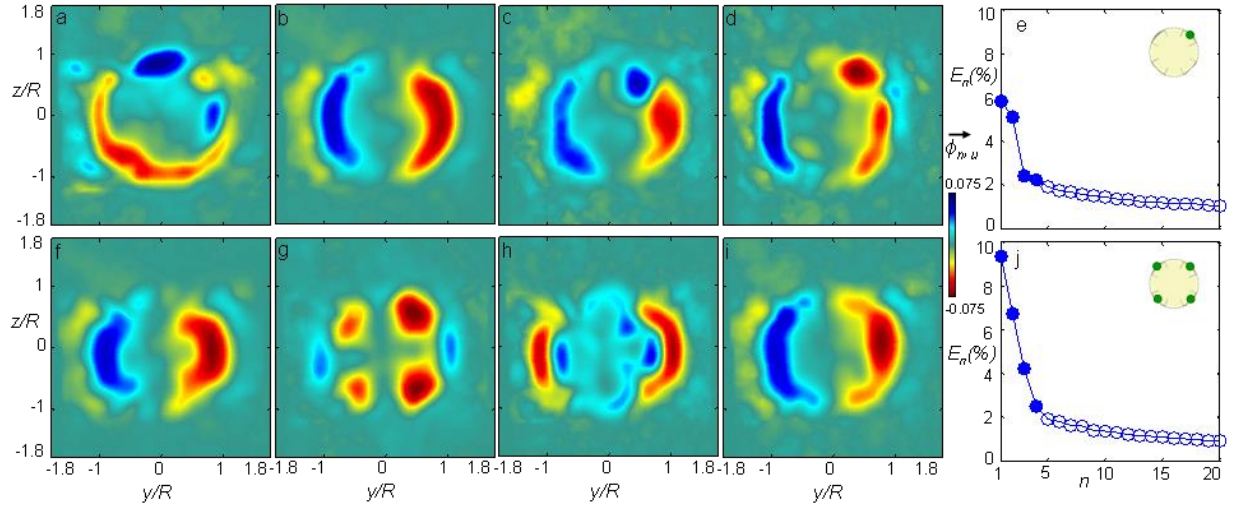


Figure IV.11. Color raster plots of the streamwise velocity POD modes, $\phi_{n,u}$, with mode number $n = 1$ (a,f), 2 (b,g), 3 (c,h), and 4 (d,i) for open-loop single-jet (a-e), and four-jets (f-j) actuations, and the energy distribution of the modes (e,j) for $Re_D = 1.62 \cdot 10^5$.

The POD modes of the wake behind the model with open-loop actuation are calculated using the wake data that was used in Figure IV.10 (using 2,500 images over a five-second trace). The streamwise component of these modes, $\vec{\phi}_{n,u}$ is shown in Figure IV.11 for the open-loop single-jet (Figures IV.11a-e) and the four-jets (Figures IV.11f-j) actuation with $n = 1$ (Figures IV.11a and f), 2 (Figures IV.11b and g), 3 (Figures IV.11c and h), 4 (Figures IV.11d and i), along with the energy contribution of these modes (Figures IV.11e and j). Single-jet open-loop actuation leads to three new streamwise velocity modes that were not observed in the absence of actuation, where the only repeating mode is attributed to the yaw instability (compare Figure IV.11b and Figure IV.7a), but this mode now has the second highest energy contribution, as opposed to the dominant contribution (dropping from 9% in the baseline to 5.5%). The new highest energy mode in Figure IV.11a is most comparable to the unactuated pitch instability mode (Figure IV.7b), except that the entire mode is rotated by about 45° , while the top right velocity lobe is severed in a location which coincides with the active jet. The third and fourth modes in Figures IV.11c and d are both variations of the unactuated yaw instability mode, with a severed velocity distribution is downstream of the active jet, where the difference between these modes is determined by the severed velocity concentration sense, which is either the opposite (Figure IV.11c) or the same (Figure IV.11d) as the unactuated mode. Compared to the unactuated flow, the 9% energy in the baseline yaw instability is redistributed to three different mode shapes with varying influence by the active jet (mode 2-4), while the pitch mode is energized from 3% to 6%, with a change in shape due to the active jet, becoming a new dominant mode. Four-jets actuation compresses the highest energy unactuated mode in the z direction, but remains intact with a similar energy percentage of the flow (see Figures IV.11f and j). In addition, a secondary mode that resembles the highest energy baseline mode appears as mode 4 (compare Figures IV.11i and IV.7a). Modes 2 (Figure IV.11g) and 3 (Figure IV.11h) are two new modes that are not present in the unactuated flow and are attributed to continuous jet actuation, where mode 2 shows a four-lobe distribution downstream of the active jets (with an additional velocity concentration in the $\pm y$ extremes, attributed to the oscillation of the model). The shape of mode 3 is a central lobe that is concave at the location of the jets, surrounded by an external velocity distribution of opposing sense in the $\pm y$ direction but absent in the $\pm z$ direction. The energy distribution after applying the four-jets control has some

differences from the baseline energy distribution (compare Figures IV.11j and IV.7h), where it introduces two new high energy modes (mode 2 and 3) and has more energy in its yaw fluctuations (between mode 1 and 4) which is commensurate with the slight model yaw oscillation growth upon actuation observed in Figures IV.9d and h, and Figure IV.10i.

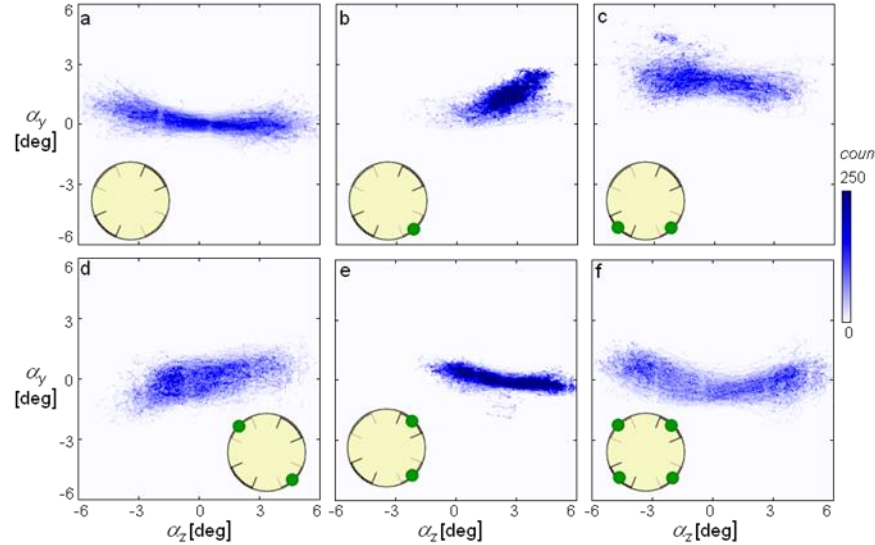


Figure IV.12. Spatial histograms of flow-induced dynamics of the model over 25 instantaneous measurements of eight-second intervals for the unactuated flow (a), along with actuation by open-loop single-jet (b), two adjacent jets in the pitch direction (c), two opposite jets (d), two adjacent jets in the yaw direction (e), and four-jets (f), at $Re_D = 1.62 \cdot 10^5$.

In order to characterize the new dynamic states of the model achieved with open-loop actuation (beyond the transitory state), multiple different open-loop actuation programs are applied. These programs are recorded over ten traces, 2 s following the onset of actuation (200 s total), and the data are binned into a histogram of the realized model yaw and pitch coordinates (with a bin widths of $\Delta\alpha_y = \Delta\alpha_z = 0.05^\circ$), as in Figures IV.4k and l. Five open-loop actuation programs are investigated in Figure IV.12: single-jet

(Figure IV.12b), two adjacent jets in the pitch direction (Figure IV.12c), two opposite jets (Figure IV.12d), two adjacent jets in the yaw direction (Figure IV.12e), and four-jets (Figure IV.12f). Figure IV.12a shows the flow in the absence of actuation using the data in Figure IV.4k, and is included for reference. The application of single-jet control shown in Figure IV.12b biases the motion by about 2° yaw and 1.5° in pitch, and reduces the oscillation by about 60% (in agreement with Figures IV.9b and IV.10a), and it is noted that this offset direction depends on which jet is activated (jet 3 is active in Figure IV.12b), and different offsets can be implemented by utilization of different jets. Upon activation of the lower two jets (jets 2 and 3, Figure IV.12c), the model becomes biased at about 2.5° in pitch and centered in yaw, while its yaw oscillations are reduced by 20% while its pitch oscillations are increased by 5%. When the right side jets are active (jets 1 and 3, Figure IV.12e), the model becomes biased at about 2.5° in yaw, while remaining approximately centered in pitch, and its oscillation amplitude is reduced by 40% in both yaw and pitch. Figure IV.12d shows the effect of two opposing actuators (jets 3 and 4), where the actuation effectively rotates the α_y with α_z response (the slope changes from -0.23 to +0.31, compare Figures IV.12a and d), but the yaw amplitude is only reduced 25%, while the pitch amplitude is not significantly altered. Finally, four-jets actuation has a slightly detrimental response, increasing the oscillation amplitudes in yaw and pitch by about 5% and 10%, respectively (in agreement with the data shown in Figures IV.9c, d, g, and h, and Figures IV.10i-p). The induced dynamics in Figures IV.12d and f show that the effects of open-loop actuation on the model's dynamics are less pronounced when the active jets are opposite to each other.

IV.4. Development of a PD Controller

The final step of the present investigation is the development and implementation of a closed-loop controller to activate the synthetic jets based on feedback of the model's attitude. A proportional-derivative (PD) controller is implemented similar to a PID controller used for controlling a 1-DOF bluff body model (Lambert et al., 2013), which uses the difference between the prescribed and actual (measured) coordinates,

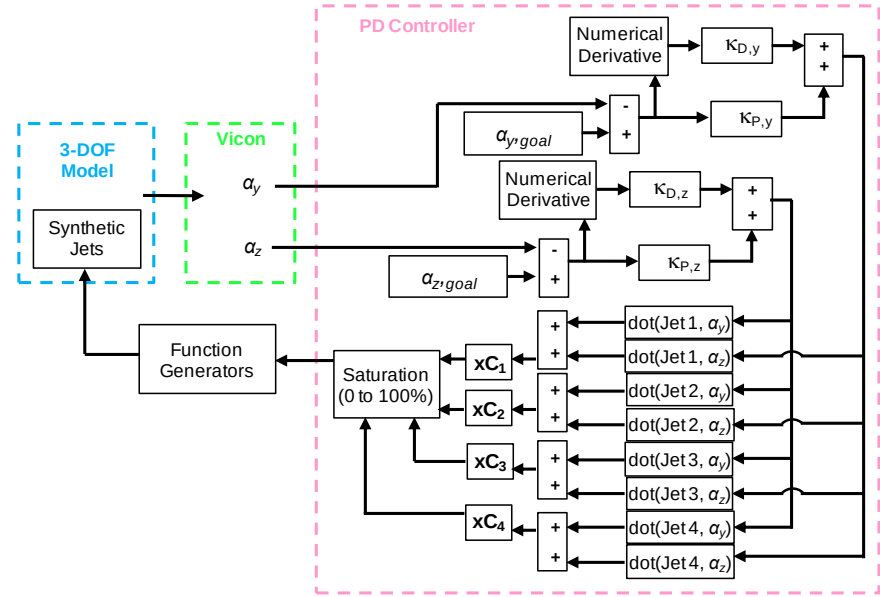


Figure IV.13. Schematics of the closed-loop PID controller.

$(\alpha_{y,goal}, \alpha_{z,goal})$, and (α_y, α_z) , as a proportional error for feedback. The derivative of this error is calculated numerically through a low-pass filter. This controller (Figure IV.13) updates at a rate of 1,000 Hz (the same update frequency as the wire traverse). Two command signals are created using a weighted superposition with four respective controller coefficients ($\kappa_{D,y}$ and $\kappa_{P,y}$ for pitch, and $\kappa_{D,z}$ and $\kappa_{P,z}$ for yaw), to determine the needed magnitudes of actuation in the pitch and yaw directions. These two command signals are then divided into four jet activation signals dependent on the roll angle of the model (for instance, if jets 1 and jet 3 are aligned with the vertical axis, their activation would only be determined by the pitch command, and the activation of jets 2 and 4 would only be determined by the yaw command), and in general all four jet activation signals are effected by each command signal. Additionally, the jets relative amplitude are allowed to be scaled by C_1 , C_2 , C_3 , and C_4 coefficients in the controller, to correct for their relative strength. These jet signals are then saturated from 0 to 100% (i.e., they can only apply force in one direction and can never be negative or larger than their maximum expulsion force). The resultant signals are sent as amplitude modulation signals to external function generators and corresponding amplifiers to transform each signal into an actuation wave packet for a synthetic jet (with the same carrier frequency of $f_{act} = 0.9$ kHz), where the maximum modulation amplitude corresponds to an output jet momentum coefficient of $C_\mu = 0.003$.

The effect of this closed-loop feedback driven controller is initially examined at $U_o = 25$ m/s ($Re_D = 1.62 \cdot 10^5$) with two different control algorithms aimed at holding the model at center and to amplify its pitch, and they are evaluated using the same procedure depicted in Figure IV.9. Figures IV.14a, b, e, and f show the closed-loop application of the PD controller with a goal of holding the model at $\alpha_y = \alpha_z = 0^\circ$, and Figures IV.14c, d, g, and h show the control goal of amplifying the pitch motion, with phase plots of the pitch (Figures IV.14a and c), yaw (Figures IV.14b and d) rates, and pitch (Figures IV.14e and g), and yaw (Figures IV.14f and h) moments. Upon activation of the ‘hold-center’ control, the model achieves a new state of $\pm 1^\circ$ in both pitch and yaw within about 0.1 seconds (approximately $0.06 \tau_b$ or $15 \tau_{conv}$) regardless of where the control started in the

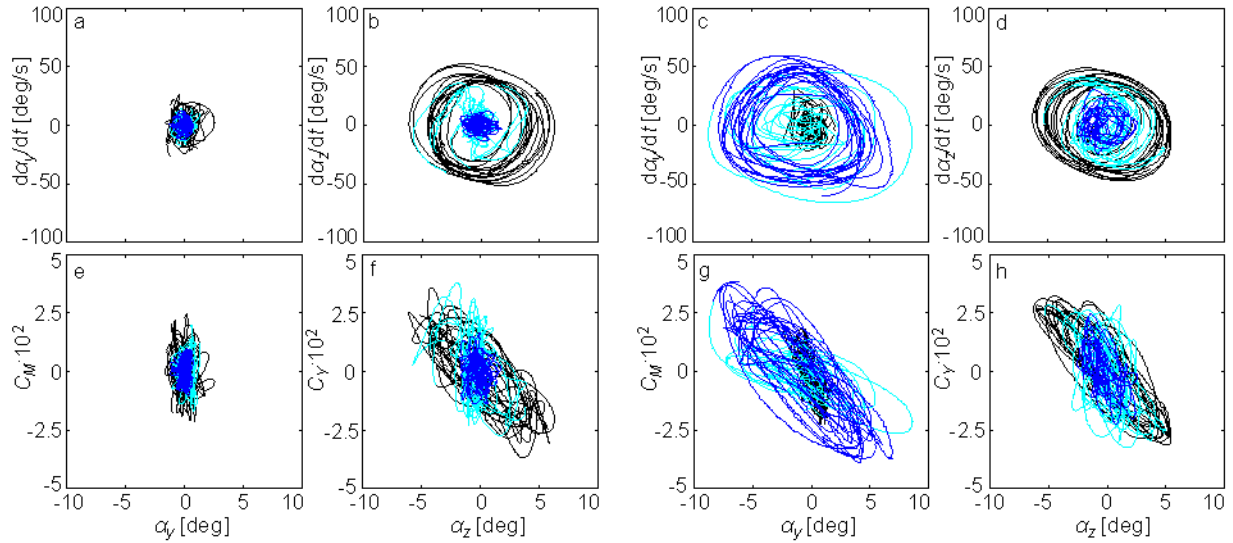


Figure IV.14. Phase plots of closed loop actuations: hold-center (a, b, e, f), and pitch amplification (c, d, g, h) presented in the same fashion as the data in Figure IV.9 using a maximum $C_u = 0.003$.

model's motion cycle. This is equivalent to a reduction of the model oscillation by 35% in pitch (Figure IV.14a) and 83% in yaw (Figure IV.14b). The commensurate moments, C_M and C_Y , are each reduced to ± 0.015 , or an equivalent reduction of 33% (Figure IV.14e) and 50% (Figure IV.14f), respectively. For the pitch amplification control (Figures IV.14c, d, g, and h), it is noted that a typical PD controller response would be dependent on the magnitude of the desired pitch, and therefore to implement the maximum amplification, the pitch goal is instead set to 0° and the controller coefficients determining the pitch motion ($\kappa_{P,y}$ and $\kappa_{D,y}$) are multiplied by -1 , causing the controller to drive the model to its maximum instability in pitch around 0° . The resulting actuation has the interesting feature that $\dot{\alpha}_y$ and C_M begin to resemble a larger amplitude version of the unactuated $\dot{\alpha}_z$ and C_Y (compare the actuated traces in Figures IV.14c and g to the unactuated in Figures IV.14d and h). Furthermore, the controlled yaw response during pitch amplification resembles the uncontrolled pitch response (compare the actuated traces in Figures IV.12d and h to the unactuated in Figures IV.14c and g). The combination of these two control algorithms shows that with appropriate feedback control the direction of the baseline instability in this model can be effectively rotated or even minimized.

The evolution of the near wake upon the onset of the closed-loop actuation shown in Figure IV.14 is analyzed in detail in Figure IV.15, with the PIV-measured time development of the wake centerlines presented in Figures IV.15a-h and Figures IV.15i-l for hold-center and pitch amplification control cases, respectively. These data are presented in a similar fashion to the data for open-loop actuation in Figures IV.10a-l. For the hold-center actuation, the horizontal and vertical wake deficits shown in Figures IV.15a and e are immediately transitioned into a higher frequency response of about 4 Hz, where the extent of the horizontal oscillation is significantly reduced, with a slight increase in the vertical extent. This higher frequency response continues in the cross-stream velocities v and w , which have a similar actuation structure to the baseline flow, with a much higher periodic frequency (compare Figures IV.16b, c, f, and g with Figures IV.6f, g, j, and k). It is also important to note that this control restores symmetry to the flow, where the actuation-induced horizontal centerline v resembles the vertical centerline w (compare Figures

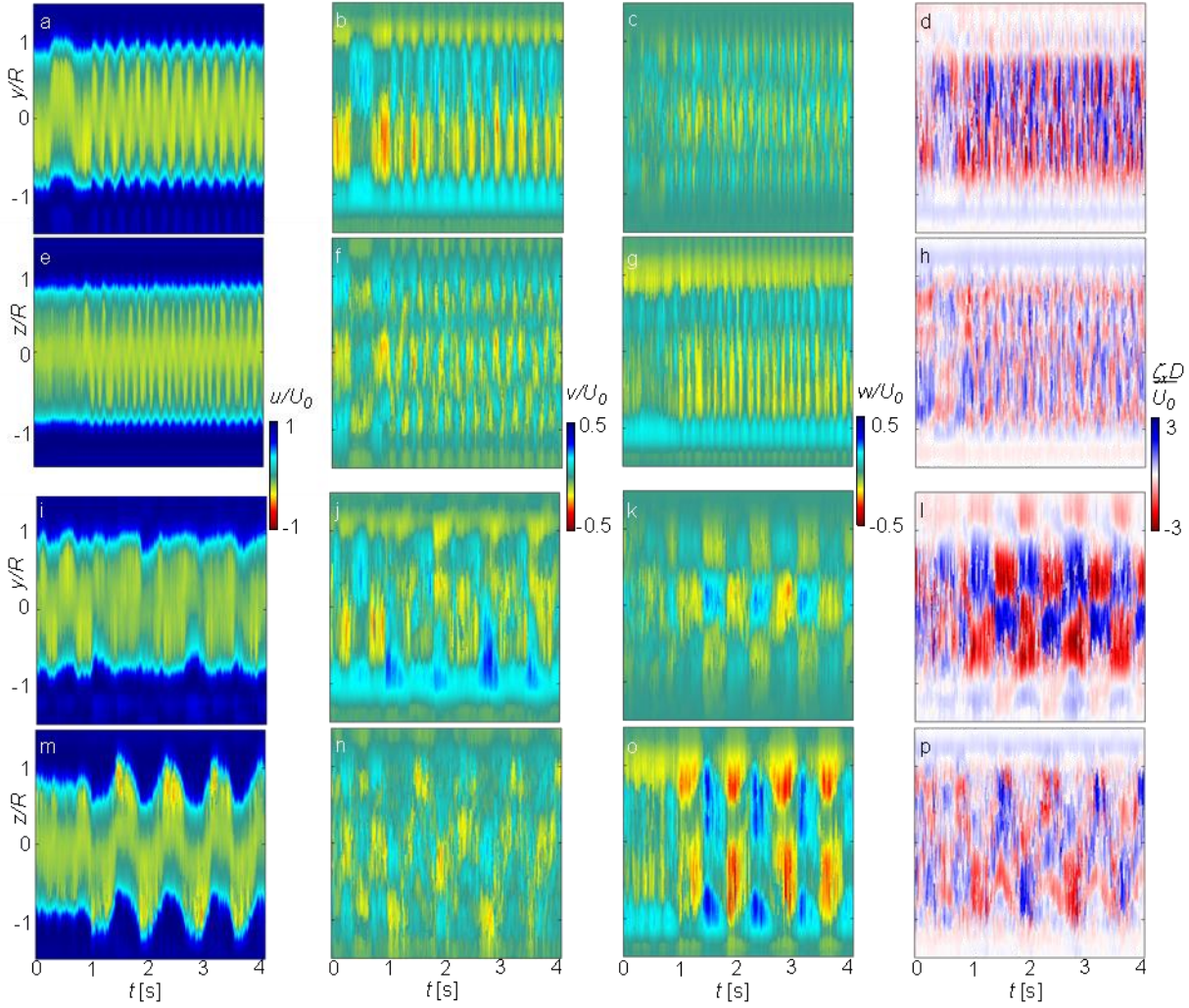


Figure IV.15. The time development of the wake for the closed-loop control algorithms hold-center (a-h), and pitch amplification (i-p), presented in the same fashion as the data in Figure IV.10, using a maximum $C_\mu = 0.003$.

IV.15b and g), and the horizontal centerline w resembles the vertical centerline v (compare Figures IV.16c and f), which is expected when the model is centered and not moving, due to its azimuthal symmetry. This trend is consistent for the streamwise vorticity, ζ_x , where both the horizontal and vertical centerlines show similar traces, each with an inner-wake region full of rapidly oscillating, low-amplitude concentrations of vorticity, and the outer-wake having a weak band structure (Compare Figures IV.15d and h). It is noted that the structure of the wake with closed-loop hold-center actuation is also in excellent agreement with low-speed stereo PIV velocity and vorticity time traces measured on a static centered model at a downstream location of $x/R = 2$ by Lambert et al. (2016a). In the presence of pitch amplification control (Figures IV.15i-p), the original oscillations over the wake deficit are horizontally reduced (Figure IV.15i) while new vertical oscillations are introduced with a larger magnitude than the unactuated yaw oscillations (compare Figure IV.15m with Figure IV.6e). In addition, the w velocity throughout the centerlines has a larger amplitude than the v velocity (compare Figures IV.15k and o with Figures IV.15n and j), with the highest amplitude oscillation introduced in the actuated vertical centerline w component, which resembles the same structure as the baseline v component (compare Figure IV.15o to

Figure IV.6f). The horizontal and vertical ζ_x fluctuations associated with pitch amplification now resemble the unactuated vertical and horizontal fluctuations, respectively, with the exception that the magnitude of the actuated ζ_x in the wake throughout the horizontal centerline has a more significant magnitude (compare Figure IV.15l with Figure IV.6l and Figure IV.15p with Figure IV.6h). The significance in these wake measurements is that the actuation for pitch amplification effectively switches the yaw dynamics and the pitch dynamics in the wake (equivalent to switching the horizontal and vertical centerline measurements), which is commensurate with the switched yaw and pitch motions and moments measured in Figures IV.14c, d, g, and h.

The respective POD modes of the wake behind the model with closed-loop actuation are calculated using the wake data in Figure IV.15 (a set of 2,500 images for a five-second trace), equivalent to the open-loop actuation POD modes in Figure IV.11. The streamwise component of these modes, $\vec{\phi}_{n,u}$ is shown in Figure IV.16 for the hold-center (Figures IV.16a-e), and pitch amplification (Figures IV.16f-j) closed-loop actuations already discussed in Figures IV.14 and IV.15, as well as an additional closed-loop actuation scheme for yaw amplification (Figures IV.11k-o). The first four modes are shown in Figure IV.16 with $n = 1$ (Figures IV.16a,f, and k), 2 (Figures IV.16b,g and l), 3 (Figures IV.16c, h, and m), 4 (Figures IV.16d, i, and n), along with the energy distribution of these actuated flows (Figures IV.16e, j, and o). The hold-center actuation changes the baseline modes significantly, restoring symmetry between the first and second mode in pitch and yaw (compare Figures IV.16a and b with Figures IV.7a and b). In addition, the energy between these modes are more evenly distributed, changing from 9% and 3% without actuation to 7% and 5% with the hold-center actuation, as shown in Figure IV.16e. Modes 3 and 4 were not present in the baseline flow and resemble mode 2 from the open-loop all-four actuation (compare Figures IV.16c

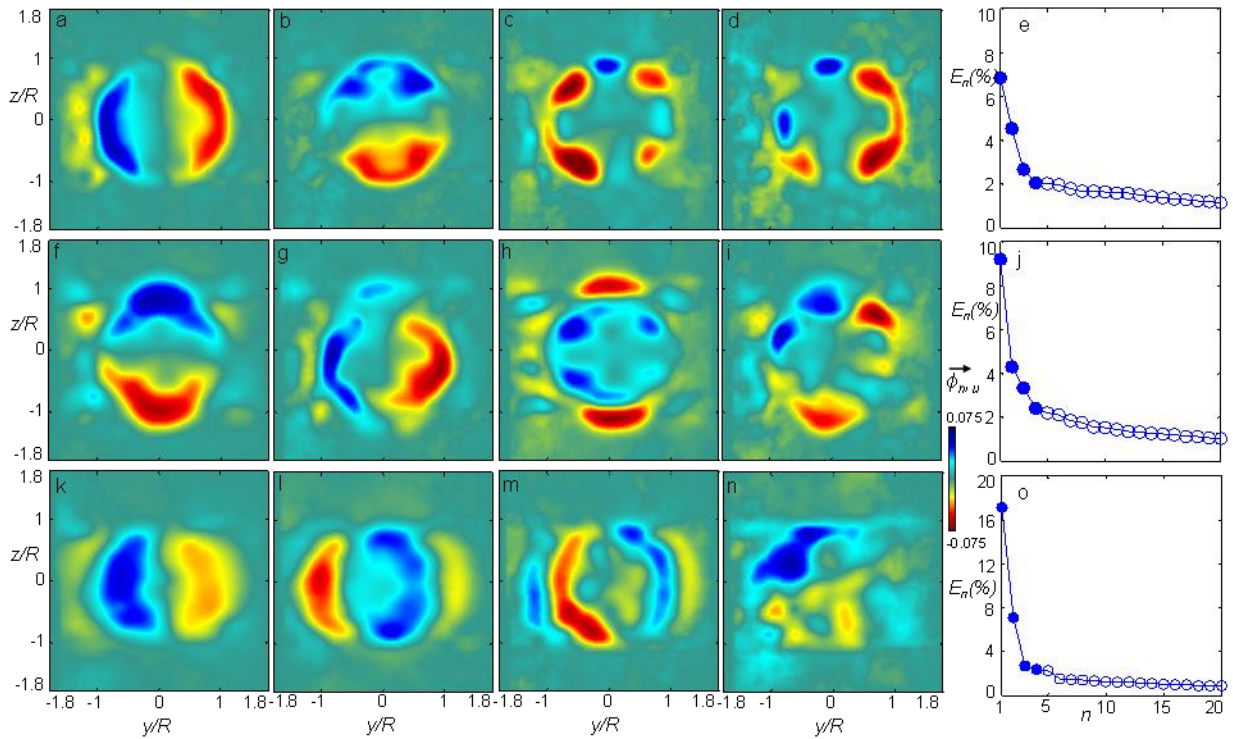


Figure IV.16. Color raster plots of the streamwise velocity POD modes, $\phi_{n,u}$, with mode number $n = 1$ (a,f,k), 2 (b,g,l), 3 (c,h,m), and 4 (d,i,n), and the energy distribution of the modes (e,j,o) for $Re_D = 1.62 \cdot 10^5$, for closed-loop oscillation suppression (a-d), pitch amplification (f-i), and yaw amplification (k-n).

and d with Figure IV.11g). The key difference between the closed-loop hold-center and the open-loop all-four modes is that the effects of the jets result in an asymmetric pair with a larger effect on the left (Figure IV.16c) and right (Figure IV.16d) side, respectively. This paired asymmetry in these two modes represent a left and right fluctuation introduced by the controlled closed-loop actuation to oppose the natural yaw instability of the model, and it is hypothesized the open-loop four-jets actuation was not able to reduce the unactuated yaw oscillation because it did not have significant asymmetric wake dynamics in the cross-stream direction to induce an opposing yawing moment. Application of the pitch amplification switches the energy contribution of the first and second modes of the baseline flow (Figures IV.16f and g), from 9% and 3% to 5% and 9%, respectively (Figure IV.16j). This switch of the pitch and yaw instabilities is commensurate with the switch in forces and wakes discussed in both Figures IV.14c, d, g and h and Figures IV.15i-p. In addition, the new third and fourth modes introduced by actuation have not been observed in previous actuation programs and primarily extend in the pitch direction with either a symmetry in the yaw direction (mode 3) or a weak anti-symmetry in the yaw direction (mode 4). The third actuation program designed in the current experiments is a closed-loop yaw amplification actuation which is similar to the pitch amplification. Upon yaw amplification control, the structure of the unactuated pitching mode is rotated and diffused, and its energy is dropped from the second to the fourth mode (Figure IV.16n). The three highest modes that are present are all preferentially in the yaw direction, with the first mode being the equivalent of the unactuated first mode. Mode 2 in yaw amplification is a rotated version of mode 3 in pitch amplification (compare Figures IV.16l and h), while a new mode 3 is introduced that appears to be excitation of the model's roll (Figure IV.16m). This new closed-loop control significantly increases the energy in the yaw modes from 9% to a total of 27% split across three modes, while reducing the energy of all the other measured modes, as shown in Figure IV.16o.

IV.5. Closed-Loop Control of Model's Attitude

While Section IV.4 considers the development and implementations of closed loop flow control approaches for stabilization or amplification of the dynamic response of the free 3-DOF model, the present section demonstrates that the ability to attenuate or amplify the natural oscillation of the bluff body can be used to effectively induce desired time-dependent steering of its attitude or stabilization in free flight. Furthermore, this stabilization control is utilized to demonstrate attitude disturbance rejection when the model's sting support is subject to deliberate external disturbances. To this end, two new actuation programs that use only two adjacent synthetic jet actuators for trajectory control are introduced for which the controller uses only derivative feedback to oppose the natural oscillations of the model. In this approach, the model reaches equilibrium when the nominal aerodynamic force induced by an active jet balances the flow-induced aerodynamic force. The control programs utilize either jets 1 and 3 (denoted as 'hold-left'), to hold the model in positive yaw (nose to the left), or jets 2 and 3 (denoted as 'hold-up'), to hold the model in positive pitch (nose up). Figure IV.17 shows histograms of the closed-loop actuated model dynamics when the sting is stationary (the data recording is the same as in Figure IV.12). Amplification of the pitch or yaw (Figures IV.17b and c, respectively) was discussed in Section IV.4, and yields ($\pm 8.5^\circ$, $\pm 2.5^\circ$), or ($\pm 1.9^\circ$, $\pm 16.2^\circ$) in (pitch, yaw), respectively. The motion data for the base flow ($\pm 1.5^\circ$ pitch and $\pm 5.5^\circ$ yaw, and the same data as in Figure IV.4k) are plotted in Figure IV.17a, for reference. Figure IV.17d shows the closed-loop hold-center actuation with a fluctuation of $\pm 1^\circ$ in both pitch and yaw around its goal of 0° . Both the hold-left and hold-up actuation cause a similar fluctuations with different offsets: $\pm 1.5^\circ$ yaw and $\pm 0.6^\circ$ pitch around an offset 2° pitch (Figure

IV.17e), and $\pm 1.6^\circ$ yaw and $\pm 0.8^\circ$ pitch around an offset 2° yaw (Figure IV.17f), respectively. For these new stabilized body states, it is noted that the closed-loop control reaches its new established state in less than 0.2 seconds (approximately $0.12 \tau_b$ or $30 \tau_{conv}$) after activation, with the hold-center actuation having the fastest transient of about 0.15 seconds (approximately $0.1 \tau_b$

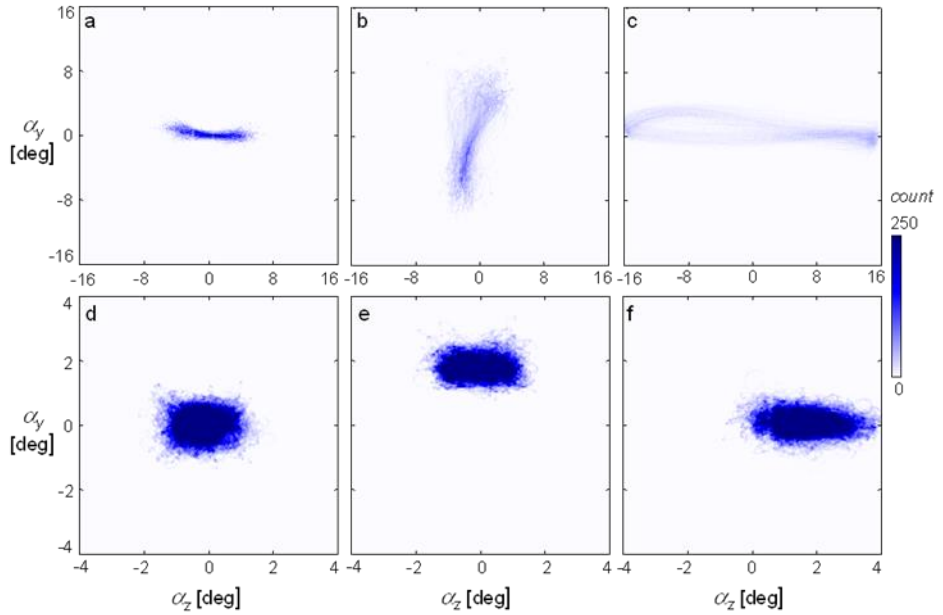


Figure IV.17. Spatial histograms of model dynamics in closed-loop actuations: unactuated (a), pitch amplification (b), yaw amplification (c), hold-center (d), hold-up (e), and hold-left (f).

or $20 \tau_{conv}$). Figures IV.17e-f demonstrate that the model can be controlled to a preferential angle (with about $\pm 1^\circ$ of high frequency fluctuation) at 2° pitch or yaw (hold-up and hold-left) with two adjacent jets, indicating that alternative control can easily be attained using the other two combinations of adjacent jets for hold-right, and hold-down. Intermediate attitudes can then be achieved by using weighted superpositions of these four control programs. For example, equal weights would yield the hold-center command shown in Figure IV.17d (although there would be slightly larger error due to the lack of proportional control). It should be noted that even if the model was excited in roll, it could still be controlled in the same manner, although the model would stabilize in the direction that corresponds to the midpoint of the two active jets (not necessarily in pure pitch or in yaw, depending on the roll angle). The combination of these control algorithms shows an ability to induce transient steering in a controlled direction (Figures IV.17b and c), or effectively stabilize the model (Figures IV.17d-f).

Closed-loop stabilization control of the model (cf., Figures IV.17a-c) is next applied for $1.30 \cdot 10^5 < Re_D < 1.94 \cdot 10^5$ (Figure IV.18). These closed-loop control programs are characterized by the maximum average offset value following either hold-up or hold-left actuation (Δa_x , Δa_y , and Δa_z in Figures IV.18a-c), the RMS oscillations when applying the hold-center actuation ($a_{x,RMS}$, $a_{y,RMS}$, and $a_{z,RMS}$ in Figures IV.18d-f), and their hold-center oscillation frequencies (Figures IV.18g-i). These data are calculated from 25 instantaneous 8 s time traces (the same number of samples as Figures IV.17a-c) with the model roll, pitch, and yaw in blue, and sting pitch and yaw in red. When the model is held at a fixed offset angle, the roll offset (0.5°) is approximately invariant with Re_D (Figure IV.18a), the pitch offset (2.0°) decreases with Re_D (Figure IV.18b), and the yaw offset (1.8°) increases with Re_D (Figure IV.18c). It is noted that the offset angle actuations are not applied for $Re_D > 1.62 \cdot 10^5$. The RMS oscillations of model pitch, for hold-center, show a reduction of the oscillation to 0.25° at $Re_D = 1.30 \cdot 10^5$, and to 0.55° at $Re_D = 1.95 \cdot 10^5$, with corresponding reductions of 50% to 65% from the model's motion in the

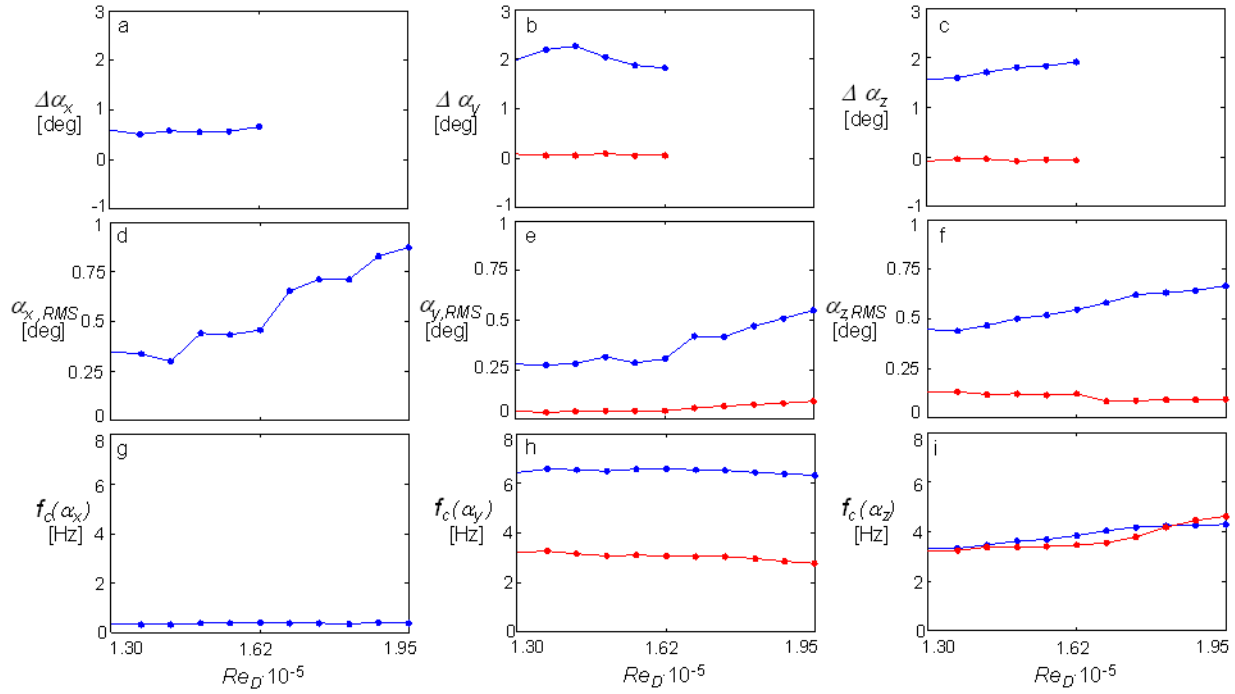


Figure IV.18. Variation with Re_D under closed-loop control for the **model** and **sting**: maximum induced deflections with hold-left or hold-up actuation (a-c), hold-center RMS amplitude (d-f) and hold-center characteristic frequency (g-i) in the roll (a,d), pitch (b,e), and yaw (c,f) directions.

absence of actuation (cf., Figure IV.18e and Figure IV.5b). Similarly, the RMS oscillations of model yaw for hold-center show a reduction of the oscillation to 0.45° at $Re_D = 1.30 \cdot 10^5$, and to 0.70° at $Re_D = 1.95 \cdot 10^5$, with corresponding reductions of 85% to 75% from the model's motion in the absence of actuation (cf., Figure IV.18f and Figure IV.5c). It is noted that the application of closed-loop hold-center actuation also reduces the sting oscillation by 30% at $Re_D = 1.30 \cdot 10^5$ and 85% at $Re_D = 1.95 \cdot 10^5$. The control does not have a significant effect on either the roll oscillation (cf., Figure IV.18d and Figure IV.5a) or its commensurate characteristic frequency (cf., Figure IV.18g and Figure IV.5d). Figure IV.18h shows that application of the hold-center control does not alter the pitch frequency of the sting, but more than doubles the model pitching frequency from 3 to 6.6 Hz, effectively forcing the model's frequency to be a harmonic of the sting frequency. In contrast, both the yaw oscillation frequency of the sting and the model increase two-fold at $Re_D = 1.30 \cdot 10^5$ to a factor of 2.5 at $Re_D = 1.95 \cdot 10^5$, remaining close to the same characteristic frequency at all wind speeds tested.

The results in Figures IV.17 and IV.18 indicate that the model can be directionally controlled with disturbance rejection within 2° in pitch and yaw. To test this, closed-loop actuation is applied when the sting is subjected to a commanded disturbance time-harmonic (2 Hz) trajectory of 2° in pitch and yaw that are 90° out of phase. Two different closed-loop actuation programs are applied with the objective of external disturbance rejection. The first program commands the model's orientation to follow the sting's orientation (effectively keeping the model's attitude locked to a 'target'), and the second program commands the model to preserve its orientation (selected to be 0°) regardless of the sting's (changing) orientation at $Re_D = 1.62 \cdot 10^5$ (Figure IV.19). The trajectories of the model and sting in the absence of actuation are shown in Figures IV.19a and d, respectively, and application of the 'sting-follow' control is shown in Figures IV.19b and e, and

the ‘sting-rejection’ control is shown in Figures IV.19c and f. It is noted that the commanded sting motions through the wire traverse are selected to be very stiff (the changes in the sting trajectory with application of the flow control programs are small) to emphasize the induced changes in the model dynamics (Figures IV.19d-f). The unactuated model dynamic response to the sting disturbance shown in Figure

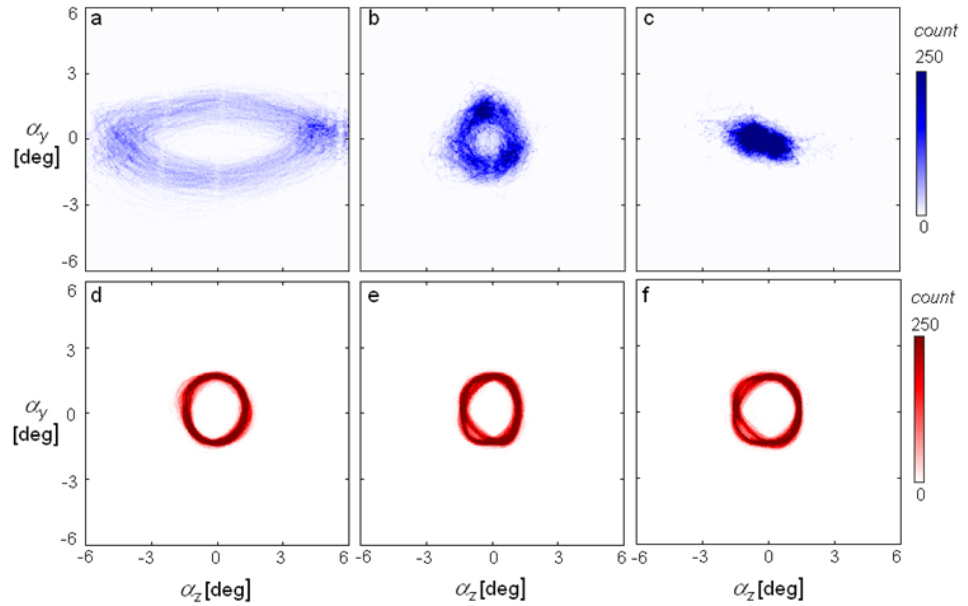


Figure IV.19. Spatial histograms of the **model** (a-c) and **sting** (d-f) dynamics in the absence of actuation (a,d), and with closed-loop flow control when the model is commanded to follow the sting motion (b,e), and to maintain its attitude independently of the sting (c,f) at $Re_D = 1.62 \cdot 10^5$.

IV.19a is approximately a superposition of the sting and model dynamics when the sting is held center (compare Figure IV.19a to Figures IV.19d and k). The application of ‘sting-follow’ control effectively forces the trajectories of the model and sting to be within $\pm 0.5^\circ$ in pitch and yaw (compare Figures IV.19b and e). The alternative ‘sting-rejection’ control (Figure IV.19c) fixes the model attitude to 0° regardless of the sting’s orientation with a fluctuation of $\pm 1.5^\circ$ in yaw and $\pm 1^\circ$ in pitch. Figures IV.19b and c indicate that this actuation induces a controlled directional bias of the model’s attitude of $\pm 2^\circ$ in pitch or yaw even when it is subject to external disturbances.

V. Preliminary Investigations of Controlled Loads on an Axisymmetric Body at High Incidence

This investigation explores prescribed modification of the global unsteady aerodynamic loads on a slender axisymmetric body at high incidence by exploiting the coupled body-wake instabilities using aerodynamic flow control approaches. These investigations build on earlier findings at Georgia Tech of the effects of fluidic actuation on the aerodynamic loads on stationary and moving axisymmetric bluff bodies at low incidence (cf., Lambert et al., 2018) and, more recently, on control of the loads on more slender axisymmetric body ($L/D = 4$) at moderate incidence at which the loads are dominated by the formation, evolution, and dynamics of the forebody counter-rotating vortex pairs (§V.1). Some aspects of the evolution and control of these vortices at higher incidence angles are described §V.2.

Either model is wire-supported in an open-return wind tunnel (test section measuring 91 cm on the side) by a dynamic 6-DOF eight-wire (1.2 mm dia) traversing mechanism described in detail by Lambert et al. (2016). Each support wire is controlled by an independent servo motor, with an in-line load cell, and electrical connection for the flow control actuators is provided by thin wires weaved along the back four support wires while the support wires provide electrical ground. The forces and moments on the model are calculated from the measured wire tensions projected onto the model (the resultant aerodynamic loads on the model are calculated relative to the loads in the absence of cross flow, and accounting for wire drag). The attitude of the model is commanded by a *Matlab Simulink* controller, which feedback utilizes inputs from *VICON* motion-capture camera system at an update rate of 500 Hz. Besides providing the feedback signal, the six-camera motion capture system resolves the spatial and temporal position of the model at any instant in time. This

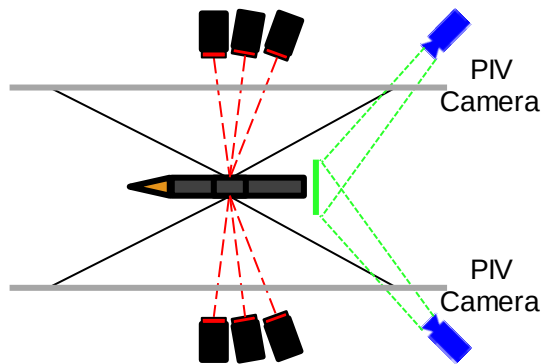


Figure V.1. Schematics of the top view of the supported model illustrating the stereo PIV wake measurements and positioning of the Vicon cameras for orientation tracking of the model.

information regarding the model position/orientation is used to update the wire orientation and accurately decompose the forces measured on each load cell into x , y , and z components in real time. In addition to the measurement of the aerodynamic loads, a stereo PIV (SPIV) system is used to characterize the model's wake dynamics using two CCD cameras that are each placed at an angle of 20° relative to an image plane normal to the oncoming flow at $x/D = 0.6$ downstream of the aft end of the model. Schematics in Figure V.1 illustrates orientations of the two PIV and six motion-capture cameras that are distributed evenly on both sides of the test section.

V.1 Low Aspect Ratio Inclined Cylinder

Control of the aerodynamic loads induced by the forebody vortices was first explored on a low aspect ratio wire-mounted cylinder ($L/D = 4$ including a $1D$ long conical forebody) at low inclination angles (up to $\alpha = 25^\circ$). The modular cylindrical body model comprises a fixed diameter cylinder ($D = 90$ mm) that includes azimuthal arrays of synthetic jet actuators at the forebody juncture and at the cylinder's aft end (Figure V.2). The model is mounted in the wind tunnel using an 8-wire traverse where each wire is connected to a servo actuator with an inline load cell (Lambert et al.,

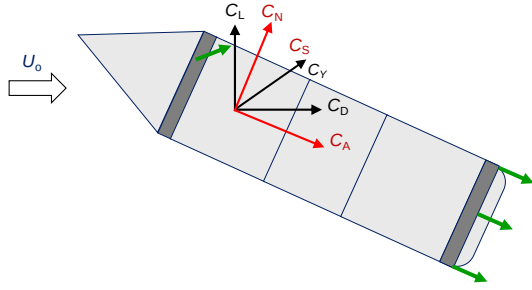


Figure V.2. Schematic diagram of the modular axisymmetric cylindrical model. The front and aft synthetic jets are marked by green arrows.

Wardlaw, 1977) established that the symmetry of the forebody vortices is extremely sensitive to minute surface features (imperfections) that can lead to significant variations in side forces, in the present investigation, the forebody cone was rotated azimuthally about the model's axis to deliberately form a pair of *asymmetric* vortices. These investigations were conducted at $Re_D = 2.4 \cdot 10^5$. The topology of an asymmetric vortex pair formed ($\alpha = 25^\circ$, $\beta = 0$) is assessed from stereo PIV measurements within the domain $-3.56 < y/R < 1.56$, $-3.78 < z/R < 3.78$ ($x/R = 1.22$,

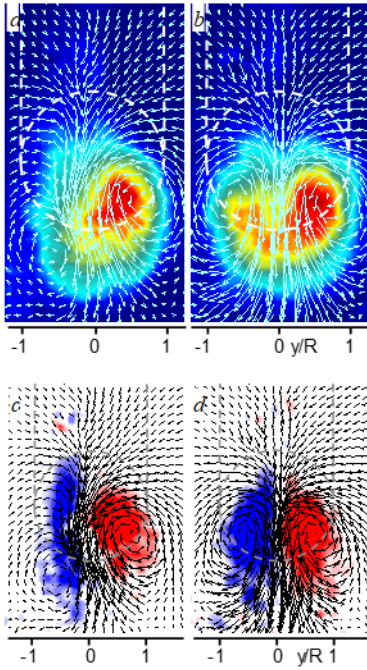


Figure V.3. Cross-stream mean velocity vectors overlaid on the contour plot of the mean streamwise velocity component U (a–b) and on the mean streamwise vorticity (c–d) measured at $x/R = 1.2$ for $U_0 = 30$ m/s, $\alpha = 25^\circ$, $\beta = 0^\circ$, and without (a,c) and with the upstream jet control at $\theta = 10^\circ$ (b,d).

2016). The aerodynamic loads are controlled by manipulation of the symmetry of the forebody vortices and, consequently, their interactions with the cylinder's wake using the forebody actuators, while the aft actuators control the near wake at aft end. In these investigations, the forebody actuators were deliberately placed *downstream* of the forebody tip (nominally $1D$) to explore the receptivity of the formation of these vortices to upwind actuation (such receptivity might be characterized by an absolute instability).

Since earlier investigations (e.g., Yanta and Wardlaw, 1977) established that the symmetry of the forebody vortices is extremely sensitive to minute surface features (imperfections) that can lead to significant variations in side forces, in the present investigation, the forebody cone was rotated azimuthally about the model's axis to deliberately form a pair of *asymmetric* vortices. These investigations were conducted at $Re_D = 2.4 \cdot 10^5$. The topology of an asymmetric vortex pair formed ($\alpha = 25^\circ$, $\beta = 0$) is assessed from stereo PIV measurements within the domain $-3.56 < y/R < 1.56$, $-3.78 < z/R < 3.78$ ($x/R = 1.22$, where x is measured from the aft end of the cylinder at rest position (when $\alpha = 0^\circ$, $\beta = 0$). Figure V.3 shows upstream views of color raster plots of the time-averaged streamwise velocity and vorticity. These images also include overlaid dashed contours that mark the projection of the body on the measurement plane. The flow asymmetry associated with the forebody vortices is evident in Figure V.3a. These data show that the streamwise velocity deficit is has a local minimum on the right side of the wake and is nearly aligned with the core of the stronger CCW forebody vortex while the CW vortex is weaker and appears to be stretched and partially wrapped about the CCW vortex (Figure V.3c). This imbalance in the vorticity distributions of the forebody vortices indicates an imbalance in the low pressure associated with the cores of each of the forebody vortices which gives a rise to a net side force. Perhaps the most noteworthy feature of the flow in the presence of actuation is the nearly complete restoration of symmetry of the forebody vortices. Although the distribution of the streamwise velocity is still not fully symmetric (Figure V.3c), the vorticity map (Figure V.3d) indicates that the vortices are far better balanced and that the tilt of the CCW vortex is somewhat relaxed, indicating a significant reduction in the asymmetric side force.

The effects of the upstream actuation on the asymmetric vortex pair is also investigated in yaw (at a fixed pitch angle), which further exacerbates the vortex pair asymmetry. Figure V.4 shows color raster plots of the time-averaged streamwise vorticity with overlaid cross-stream velocity vectors for yaw angles $\beta = 0^\circ$, -5° ,

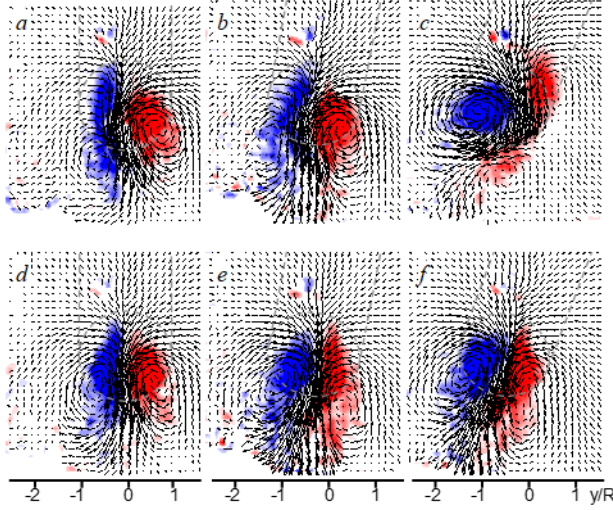


Figure V.4. Cross-stream mean velocity vectors overlaid on the contour plot of the mean mean streamwise vorticity measured at $x/R = 1.2$ for $U_o = 30$ m/s and $\alpha = 25^\circ$, for the yaw angle $\beta = 0^\circ$ (a,d), -5° (b,e), and -10° (c,f), without (a–c) and with the upstream jet control at $\theta = 10^\circ$ (d–f).

and -10° , ($\alpha = 25^\circ$) in the absence (a–c) and presence (d–f) of actuation. These data show that in the absence of actuation (Figure V.4a–c), when $\beta = -5^\circ$ (Figure V.4b) the CW (left) and CCW vortices tilt in the CW direction (in the direction of the yaw) and the already distorted CW vortex becomes wrapped around the CCW stronger vortex. However, as the yaw increases to $\beta = -10^\circ$, there is a complete reversal of the vortex asymmetry (Figure V.4c), where the CW vortex becomes the stronger vortex in the pair and the CCW vortex becomes considerably weaker. Symmetrically opposite to the structure in Figure V.4a, the vortex asymmetry switches sign, and the only coherent vortex in the wake has a negative sense of vorticity, while its pair appears to split and weaken. These changes in the strength of the vortices indicates a switch in the sense of the side force. In the presence of actuation, however, the symmetry of the two vortices is restored to a large extent and the effects of the yaw are restricted to a change in the CW tilt of the vortex pair (Figures V.4d–f). Force measurements in the absence and presence of actuation demonstrate that the increment of the side force coefficient ΔC_s between the actuated and unactuated flow switches sign between $\beta = 0$ and -10° ($\Delta C_s = -0.18, -0.09$, and $+0.22$ for $\beta = 0, -5^\circ$ and -10°).

Considering the relatively low incidence angle of the model, the effects of actuation using both the forebody actuator (set at $\theta = 10^\circ$) along with the aft end actuator ($\theta = 0^\circ$ which can affect the wake in reasonable proximity to the forebody vortices). The effects of the actuation were assessed from the changes in the aerodynamic forces. The increments of the forces in the absence and presence of actuation are computed for the normal, axial, and side force coefficients in the body coordinates ΔC_N , ΔC_A , and ΔC_s , respectively for $0 < \beta < 10^\circ$ ($\alpha = 25^\circ$) (Figure V.5). The addition of the aft actuation was motivated by the earlier work of Lambert et al. (2018) at low incidence angles that demonstrated significant vertical and/or horizontal forces (and corresponding pitch and yaw moments) that were effected by vectoring of segments of the separating shear layer into near wake. When $\theta = 0^\circ$, the induced force by the aft actuator has significant components in the body's normal and axial coordinates. The data in Figure V.5 show a decrease in all three force components in the presence of actuation. The most prominent is in the incremental decrease in the side force coefficient ΔC_s which

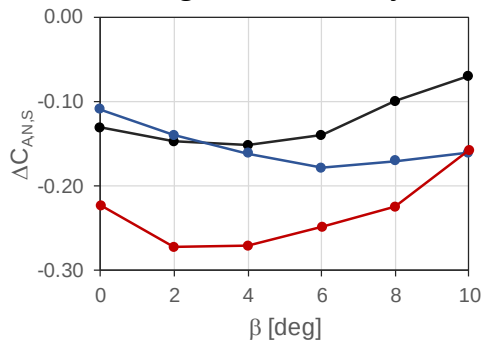


Figure V.5. Incremental change in aerodynamic force coefficients C_A (●), C_N (●), and C_s (●) (a), in the body frame of reference, with the model yaw angle β ($\alpha = 25^\circ$ and $U_o = 30$ m/s), for the cooperative upstream ($\theta = 10^\circ$) and the bottom downstream jet control.

and presence of actuation are computed for the normal, axial, and side force coefficients in the body coordinates ΔC_N , ΔC_A , and ΔC_s , respectively for $0 < \beta < 10^\circ$ ($\alpha = 25^\circ$) (Figure V.5). The addition of the aft actuation was motivated by the earlier work of Lambert et al. (2018) at low incidence angles that demonstrated significant vertical and/or horizontal forces (and corresponding pitch and yaw moments) that were effected by vectoring of segments of the separating shear layer into near wake. When $\theta = 0^\circ$, the induced force by the aft actuator has significant components in the body's normal and axial coordinates. The data in Figure V.5 show a decrease in all three force components in the presence of actuation. The most prominent is in the incremental decrease in the side force coefficient ΔC_s which

indicates a decrease excess of -0.2 over most of the range tested. The other two coefficients show reductions between -0.1 and -0.2. It is also noteworthy that the reduction in the side force has a local maximum between $\beta = 2^\circ$ and 4° and that the lift and drag forces exhibit a change in slope. These findings indicate that active control of the evolution of the separating shear layers along each side of the cylinder's body can lead to not only changes in the flow symmetry about the body's vertical center plane, but also augment the effects on the lift and drag forces.

V.2 Inclined Slender Cylinder

The second experimental investigation is mainly concerned with the flow dynamics over a slender axisymmetric cylinder model ($L/D = 11$, $D = 40$ mm, $Re_b = 7.9 \cdot 10^4$), including the tangent ogive forebody of the length $2D$. As the prior studies indicated that the ogive body geometry generally induces more prominent side forces when compared to the conical forebodies, the conical geometry used in §III.3.1 is replaced with the ogive geometry in the current investigation. The test model is wire-mounted in the same manner as the low aspect ratio model, but the investigations are focused on control of autonomously-formed forebody vortices over a range of high angles of inclination ($25^\circ < \alpha < 65^\circ$), while the wind tunnel was operated with uniform wind speed of $U_0 = 30$ m/s.

The axisymmetric model is comprised of three major modules: the ogive forebody, synthetic jet actuator module, and the central cylindrical body, as illustrated in Figure V.6a. Both the forebody and the jet module are designed such that can be rotated by the full azimuthal period. The jet module incorporates a single azimuthal orifice measuring 0.6×15.7 mm, imparting the jet momentum coefficient $C_\mu = 4 \cdot 10^{-3}$) while issuing normal to the surface at the frequency of about 2.3 kHz. To illustrate the forebody vortices that the control jet is designed to affect, the forebody vortex pair is visualized over the default $2D$ ogive forebody at the angle of attack $\alpha = 60^\circ$. For that purpose, a mixture of a titanium-oxide paint and the linseed oil is applied over the forebody, where its ratio is iteratively adjusted such that the oil mixture does not shear before the operating flow condition is attained. After the test section speed reached $U_0 = 30$ m/s, the oil is sheared for about 20 minutes, and the resulting flow pattern is shown in Figure V.6b in an upstream view from above. The two vortical traces are clearly seen in the image, forming off the forebody tip and evolving along the nose surface downstream. The strong traces along each line where the vortex lifts the flow away from the surface indicates that these vortices remain in the proximity of the surface over the full forebody extent.

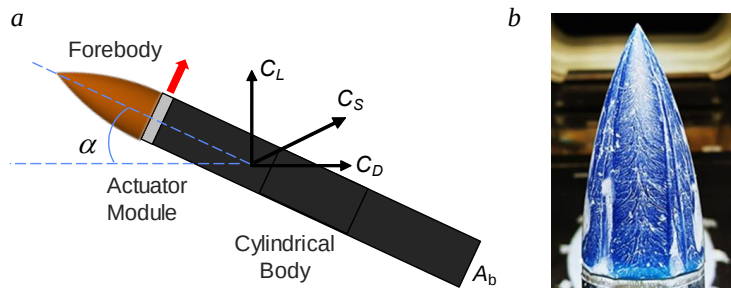


Figure V.6. Axisymmetric slender model ($L_c/D = 10$) with an ogive forebody having an integrated flow control module (a) and surface oil-flow visualization of the forebody vortices at an angle of attack $\alpha = 60^\circ$ ($L/D = 2$).

The aerodynamic loads on the cylindrical model were characterized in the absence of actuation over a range of inclination angles ($25^\circ < \alpha < 65^\circ$, $Re_D = 2.4 \cdot 10^5$) using the three ogive forebodies of $L/D = 1, 2$, and 3 . The inclination angle of each model was increased monotonically from the same base angle to avoid hysteresis effects. In order to enable meaningful comparison between the models with the different ogive

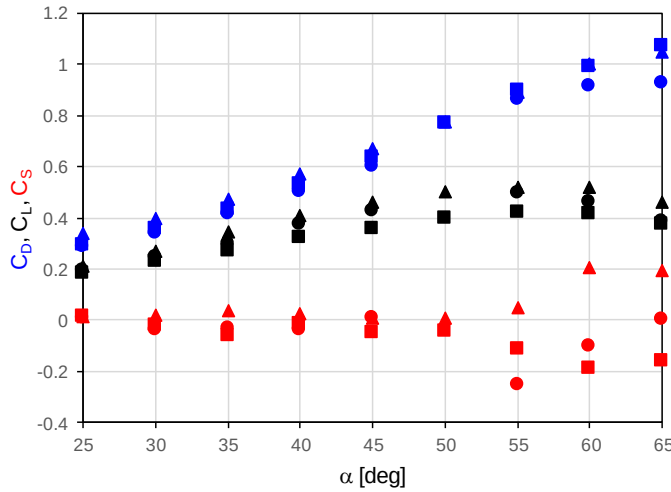


Figure V.7. Force coefficients C_D , C_L , and C_S with angle of attack α for the slender axisymmetric body ogive nose ratio $L/D = 1$ (square), 2 (triangle), and 3 (circle).

Both lift and drag forces are small for $\alpha \leq 15^\circ$, and as α increases, the drag increases monotonically and reaches a maximum at $\alpha = 90^\circ$, while the lift has a local maximum (around 55°), and then decreases monotonically and vanishes at $\alpha = 90^\circ$.

On the present model, as seen in Figure V.7, the drag increases monotonically over the entire range of α while the rate of increase of the lift begins to diminish for $\alpha > 35^\circ$ ostensibly as the forebody vortices begin to lift off the cylinder and the flow from the windward separates on its leeward surface. Similar to the observations of Hoerner and Borst (1985), while the drag force continues to increase monotonically, the lift force has a maximum around $\alpha = 55^\circ$, and then decreases at higher angles of incidence. While the drag coefficients for the three ogive forebodies are nearly identical through $\alpha \sim 60^\circ$ the drag coefficient of the $L/D = 3$ ogive is lower at higher α and appears to reach a local maximum that is lower than the corresponding drag coefficients of the $L/D = 1$ and 2 ogives. However, the corresponding peak lift coefficients of the $L/D = 1$ ogive is lower than the coefficients of the other ogives. It is apparent that these changes are associated the changes in the side forces that remains nominally symmetric about the cylinder's vertical (y - z) plane of symmetry up to $\alpha \sim 50^\circ$. According to earlier work (e.g., Keener and Chapman, 1977), the onset of the vortex asymmetry approximately scales with the forebody tip angle, which is 58° for the present model. As is evident from the variations of the side force coefficients, the onset of forebody vortex asymmetry which is affected by small variations in the ogive surfaces, varies between the different forebody models, and also affects both the lift and drag forces. The liftoff of one of the forebody vortices from the surface, was documented in detail; in a number of earlier studies (e.g., Lamont and Kennaugh, 1989, DeSpirito et al. 2017, Mahadevan et al. 2018). Lamont and Kennaugh (1989) showed a nearly periodic switching in the direction of the side force as the forebody is rotated azimuthally about the axis of the cylinder over a range of incidence angles, which reflects the switching vortex asymmetry in the flow. As shown by Mahadeven et al. (2018) even fine polishing of the forebody surface was insufficient to fully suppress the vortex asymmetry and the direction switch of the induced side force.

forebodies, the variation of the force coefficients C_D , C_L , and C_S were computed based on the model's planform area (including the forebody). Hoerner and Borst (1985) characterized the lift and drag on an inclined cylinder in the absence of a forebody. These authors noted that at low inclination ($\alpha \leq 15^\circ$), the flow over the cylinder is predominantly oriented along its axis and it may be thought of as low-aspect-ratio wing with a pair of counterrotating "tip" vortices that form over the suction surface resulting in lift-induced drag. At higher angles of attack, the flow from the windward to the leeward face of the cylinder separates on its leeward face and generates a normal force on the body whose respective cross-

It should be noted that this sensitivity of the forebody vortices to small perturbations indicates their potential receptivity to flow control actuation as well.

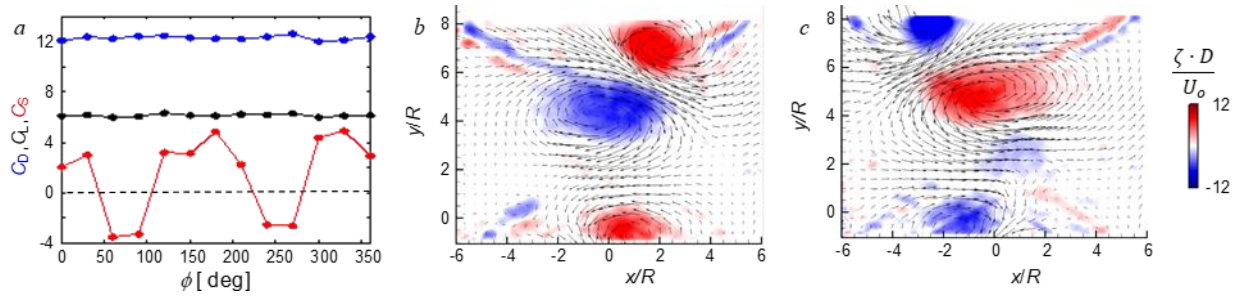


Figure V.8. Force coefficients C_D (●), C_L (●), and C_S (●) with the forebody azimuthal orientation ϕ (a), and the two spanwise mean velocity fields with overlaid contour plots of the streamwise vorticity component at $\phi = 90^\circ$ (b) and 180° (c) for the model at $\alpha = 60^\circ$ ($U_o = 30$ m/s).

The asymmetry in the evolution of the forebody vortices and the resulting side forces is investigated at $\alpha = 60^\circ$ ($U_o = 30$ m/s) over a full azimuthal rotation of the $L/D = 2$ forebody. The resulting drag, lift, and side force coefficients (each normalized by the cylinder's cross sectional area A_b) are shown in Figure V.8a. In concert with the earlier investigations, the side force exhibits azimuthally-periodic switching. However, $C_S > 0$ ($2.02 < C_S < 4.91$) for most of the azimuthal orientations $110^\circ < \phi < 220^\circ$, $275^\circ < \phi < 50^\circ$ and switches direction $C_S < 0$ ($-3.52 < C_S < -2.57$) only within narrow azimuthal domain centered about $\phi = 90^\circ$ and 270° . It is interesting to note that the induced side force does not change its direction when the forebody is rotated at 180° relative to some given azimuthal position (i.e., $C_S > 0$ or < 0 at both domains although the nominal magnitudes of the opposite side forces are not necessarily of the same). That the sense of the side forces does not change when the forebody is rotated at 180° indicates that the flow asymmetry is likely not brought about by a random surface imperfection, because a strong periodic behavior of the present data (and a number of the earlier studies) suggest that the origin of such behavior is in the regular geometry deviation with a preferential axis. The most obvious source of such deviation would be an imperfect tip of the forebody, particularly since many investigations indicated extreme flow sensitivity to small geometrical perturbations at the forebody tip. Examination of the tip of the current forebody model indicated small oval deviation from the perfectly circular termination of the tip, and it is argued that such an oval shaped with the dominant axis would be sufficient to induce preferential vortex asymmetry, depending on the dominant axis azimuthal orientation. Moreover, as the forebody is rotated by 180° , the oval orientation would assume the same orientation of its dominant axis, which would explain periodicity in the side force formation. As the oval manufacturing perturbation is not perfect, this would also explain that asymmetry in the magnitudes of the excursions and the disparity in their azimuthal extents are associated with the randomness of this deviation. It is remarkable that for fixed angles of incidence and yaw the lift and the drag are nearly invariant with ϕ even though the side force undergoes significant variations which are associated with topological changes in the trajectories of the streamwise forebody vortices. This indicates that once the vortices separate and migrate off the cylindrical body, their effects on the lift and drag diminish.

The changes in the topology of the vortex pair associated with the changes in the direction and magnitude of the side force in Figure V.8a is illustrated in color raster plots of the time-averaged streamwise vorticity superposed with vectors of the cross-stream velocity field captured using stereo PIV within the domain $-6 < y/R < 6$, $-1 < z/R < 8$ ($x/R = 3$) for $\phi = 90^\circ$ and 180° (Figure V.8b and c, respectively). The data in each of Figures V.8b and c show a dominant pair of counter-rotating

streamwise vortices where the CW vortices in this view are associated with the rollup at the left side of the forebody, along with additional, weaker streamwise vortices that would be shed within the cylinder's wake (cf. the simulations of DeSpirito et al., 2017). As is evident from the vorticity concentrations, the major axis of the dominant vortex pair (i.e., the axis centered between the vortices, nominally normal to a line through the centers of their cores and pointing in the direction of the induced flow) in Figure V.8b is *rotated* by 128° in Figure V.8c (from 26° to 154°). As can be seen from Figure V.8a, the directions of the major axes are asymmetric (i.e., the major axes in Figures V.8b and c are pointing to the right and the left, respectively) and commensurate with the changes in the directions of the side forces namely $C_S < 0$ at $\phi = 90^\circ$ and $C_S > 0$ at $\phi = 180^\circ$. It is also noteworthy that the change in the directions of the major axes of the primary vortex pair is also accompanied by changes in the sense of the other streamwise vortices that are captured within the field of view, ostensibly by reversal of the induced cross flow of the lower dominant vortex. It is noted in passing that the streamwise vorticity fields in Figures V.8b and c also exhibit some contamination by residual reflections from the traverse wires that are misinterpreted by the processing software as concentrations of streamwise vorticity.

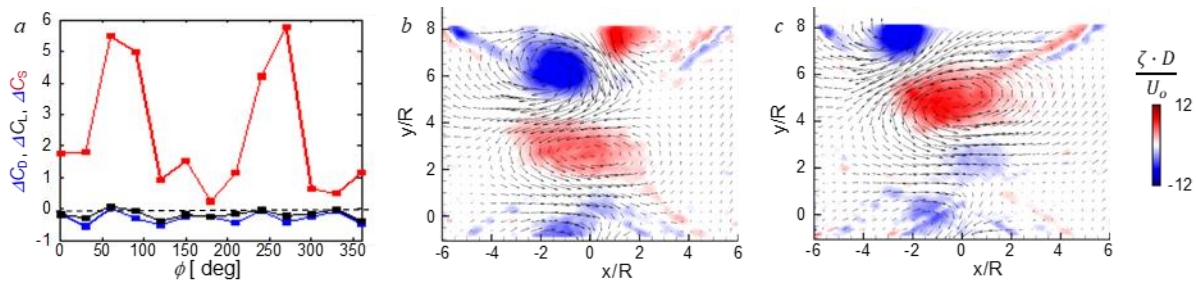


Figure V.9. Incremental change in the force coefficients C_D (■), C_L (■), and C_S (■) by the synthetic jet actuation with the jet azimuthal orientation ϕ (a), and the two spanwise mean velocity fields with overlaid contour plots of the streamwise vorticity component at $\phi = 90^\circ$ (b) and 180° (c) for the model at $\alpha = 60^\circ$ ($U_0 = 30$ m/s).

As noted in §II, the present flow control concept of the forebody vortices and the associated aerodynamic loads is based on upwind actuation that is effected at the juncture between the forebody and the downstream cylinder rather than near the upstream tip of the forebody. The effect of the actuation was first investigated by exploring the aerodynamic loads in Figure V.8a. To this end, the center of the synthetic jet actuator was set at an azimuthal angle of $\theta = 90^\circ$ (i.e., pointing to the right in an upstream view) while the ogive forebody was rotated as in Figure V.8a (for $\alpha = 60^\circ$, $U_0 = 30$ m/s) thereby incrementally varying the surface perturbations that alter the symmetry of the forebody vortex pair relative to the azimuthal orientation of the actuation jet. Figure V.9a shows the *increments* of the three global aerodynamic force coefficients at the same azimuthal position relative to the ogive forebody and cylinder as in the absence of actuation (Figure V.8a). These data show that the actuation significantly alters the magnitude and direction of the induced side forces within the domains centered about $\phi = 90^\circ$ and 270° while rendering the effect on the side forces throughout the rest of the azimuthal domain positive but of a much smaller magnitude. The presence of the actuation in these domains of the high effectiveness *reverses* the direction of each of the side forces that would otherwise be induced at $\phi = 90^\circ$ and 270° and alters its sign, therefore implying a change and reversal in the vortical asymmetry. In fact, the time average color raster plot of vorticity concentrations of the dominant vortex pair that is measured for the controlled flow at $\phi = 90^\circ$ (Figure V.9b) is inverted relative to the base flow vortical structure (see Figure V.8b) and similar to the

opposing-sign force flow field in Figure V.8c. Also note that the vortical asymmetry is not as pronounced as in Figure V.8c; hence the smaller magnitude of the side force. It is noteworthy that the corresponding vortex pair acquired for $\phi = 180^\circ$ (Figure V.9c) for which the side force increment is significantly lower, but still positive, there is no significant change in the measured flow field, if compared to the corresponding uncontrolled flow in Figure V.8c.

Although the analogous study is not conducted, it can be argued that the opposite arrangement, where the synthetic jet is fixed on the opposite side ($\theta = 270^\circ$) would induce the exactly the opposite effect to the studied jet orientation at $\theta = 90^\circ$. The mirrored jet would predominantly affect the opposing sense of the vortical asymmetry, thereby significantly opposing the positive side forces, in a similar manner that the jet at $\theta = 90^\circ$ is shown to affect the azimuthal subdomains that induce negative side forces. It is therefore inferred that integration of the two jets, at $\theta = 90^\circ$ and 270° would be sufficient for control of either vortical asymmetries. Also, if the objective would be to maintain the side force close to zero, some tuning of the control jet parameter would be done, as the present results indicate the “overshooting” of the suppression effect and induction of the side force of the opposite sign.

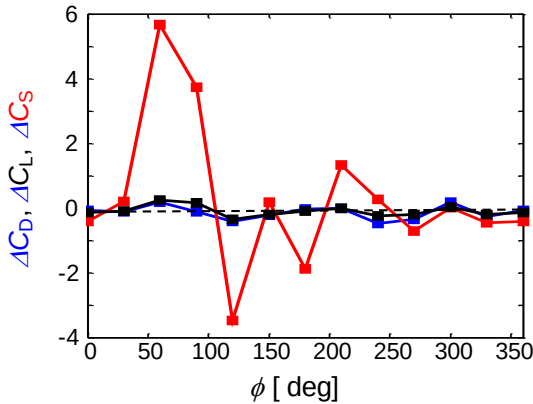


Figure V.10. Incremental change in the force coefficients C_D (■), C_L (■), and C_S (■) by the synthetic jet actuation with the forebody azimuthal orientation ϕ and the jet orientation fixed at $\theta = 90^\circ$.

while the tip disturbance imposes its full-rotation effect on the side force. The resulting changes in all the three force coefficients are shown in Figure V.10. As it was the case in the fixed jet orientation, no significant effect is detected for the drag and lift forces. Initial incremental effect of the side force, as the forebody orientation assumes $\phi = 60^\circ$ indicates the maximum jet effectiveness, as the jet itself is at $\theta = 90^\circ$. It is seen that even beyond this orientation, the jet imparts nearly as strong effect as both the forebody and the jet are rotated for another 30° increment. At the following increment, it is interesting to note that significant effect is achieved, but of the opposite sign. By examining the corresponding base flow force at this orientation $\phi = 120^\circ$ (Figure V.8a), it is seen that the base flow force already switched its sign at this orientation, but the flow control jet, although located at $\theta = 150^\circ$ is still capable of countering this now altered vortex asymmetry that brought about the change is sign of the side force. As the jet and the forebody continue to advance azimuthally, small further changes are measured until the forebody orientation reaches $\phi = 240^\circ$, and thereafter there is practically no jet effect. This is particularly interesting as a strong negative side force is generated

Once the most effective azimuthal flow control orientations are extracted from the study summarized in Figure V.9, the next step concerns the sensitivity of such preferential azimuthal distance to the full range of the absolute forebody rotation. To test this, the jet orientation of $\theta = 90^\circ$ is paired with the forebody orientation $\phi = 60^\circ$ (see Figure V.9), and then the both the forebody and the actuator azimuths are reoriented back to zero orientation for the forebody. After that, both the actuator and the forebody are incremented jointly across the full span of the azimuthal angles, and the jet effectiveness is tested, keeping in mind that for any forebody orientation ϕ , the jet orientation $\theta = \phi + 30^\circ$. By simultaneous rotation of both the forebody and the jet, the jet relative distance to the dominant axis of the forebody tip is maintained,

at $\phi = 240^\circ - 270^\circ$ in the base flow (Figure V.8a). The reason for the jet ineffectiveness for these orientations is in the fact that the base flow asymmetry is of the same sense for both $\phi = 90^\circ$ and 270° . As shown in Figure V.9a, the jet counters these asymmetries when oriented at $\theta = 90^\circ$. However, in the present test, the jet is oriented exactly on the opposite side of the body and hence ineffective in countering the flow asymmetry at $\phi = 240^\circ - 270^\circ$, i.e., the jet is oriented on the same side with the body orientation.

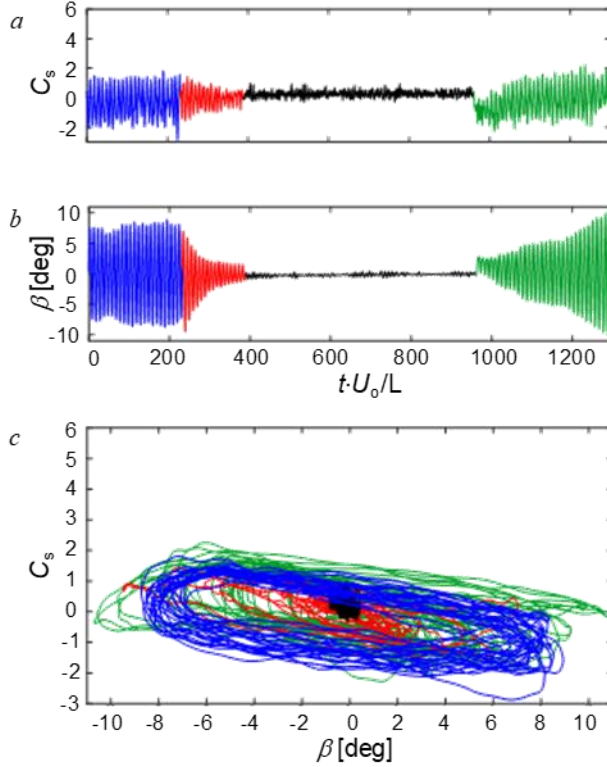


Figure V.11. Time evolution (a, b) and phase relationship (c) of the side force coefficient C_s and the yaw angle β for the $L/D = 2$ forebody model at $\alpha = 50^\circ$. Effect of aerodynamic model stabilization by the jet actuation is shown between $232 < t \cdot U_o / L < 961$.

This study indicates that the flow control is clearly sensitive to the particular (albeit unknown) surface disturbance orientation, but that there is still an azimuthal range of the jet orientation that is sufficient to impart significant changes in the side force, predominantly by countering the naturally induced side force.

The present investigations also demonstrated that within the narrow ranges of angles of incidence ($50^\circ < \alpha < 60^\circ$) the coupling between the forebody vortices and the cylinder's near-wake can lead to base flow instabilities of the wire-supported model that is manifested by significant pitch and yaw oscillations. Although such instabilities may not be directly detectable on sting-mounted model, the investigations of Zilliac et al. (1991) indicate their presence as the azimuthal position of forebody is adjusted at $\alpha \sim 60^\circ$. These observations clearly posed the question as to whether manipulation of forebody vortices and therefore their interaction with the cylinder's wake vortices (cf., §III.3) can be used to stabilize or destabilize the model when it is naturally unstable or stable within this incidence range.

The present investigation showed that when the azimuthal position of the ogive forebody was set to $\phi_{\text{ogive}} = 60^\circ$, the model exhibits significant time-periodic oscillations in incidence and yaw ($\pm 6^\circ$) at incidence of $\alpha = 50^\circ$. As noted in connection with Figure V.8, when the model is stable in the absence of actuation (e.g., at $\alpha = 60^\circ$), $C_s < 0$ and the major axis of the forebody vortex pair is slanted to the left (cf. Figure V.8b). Simultaneously-measured time traces of $C_s(t)$ and $\beta(t)$ in Figures V.13a and b for $0 < t^+ < 232$ ($t^+ = t U_o / L$) show that C_s oscillates between -2.12 and 1.82 (the nominal mean is about -0.34 with a characteristic frequency of 7 Hz ($St_L = 0.0093$)). Based on the results of §III.3, the azimuthal position of the jet relative to the forebody is selected such that if the model was stable, it would reverse the symmetry of the forebody vortices ($\phi_{\text{jet}} = 90^\circ$), and result in an increase in the side force. When jet actuation is activated at $t^+ = 232$ through 961, the traces of $C_s(t)$ and $\beta(t)$ (Figures V.11a and b) exhibit a strong decay in oscillations over a characteristic time $\Delta t^+ = 157$ (transitioning to stable for about 16 oscillations periods), and the peak-to-peak oscillations of C_s and β are attenuated by factors of about six and over ten, respectively. While the yaw angle effectively

becomes zero, the side force varies slightly around a mean level of +0.26 indicating that the jet actuation level could be adjusted so that the effected mean side force vanishes. The time-dependent traces show that when the jet actuation is terminated at $t^+ = 961$, there is a clear commensurate jump in the magnitudes of both C_s and β . The unsteady baseline oscillations resume with slowly-increasing magnitude while the oscillations commence about a negative “offset” in C_s which is first negative and then gradually increases. It is noteworthy that the rate of increase in the amplitude of β is slower than in C_s apparently as a result of the model’s inertia. However, it does not appear that the model fully returns to its original motion before the onset of actuation. This is further accentuated by the C_s - β phase plot in Figure V.11c, where the trajectory at the end of the model’s response appears offset from the initial limit cycle. It is also noted that the relatively long transitions associated with the onset and termination of the actuation might be associated with the gradual coupling (or decoupling) between the forebody vortices and the vortical structures in the wake of the cylinder.

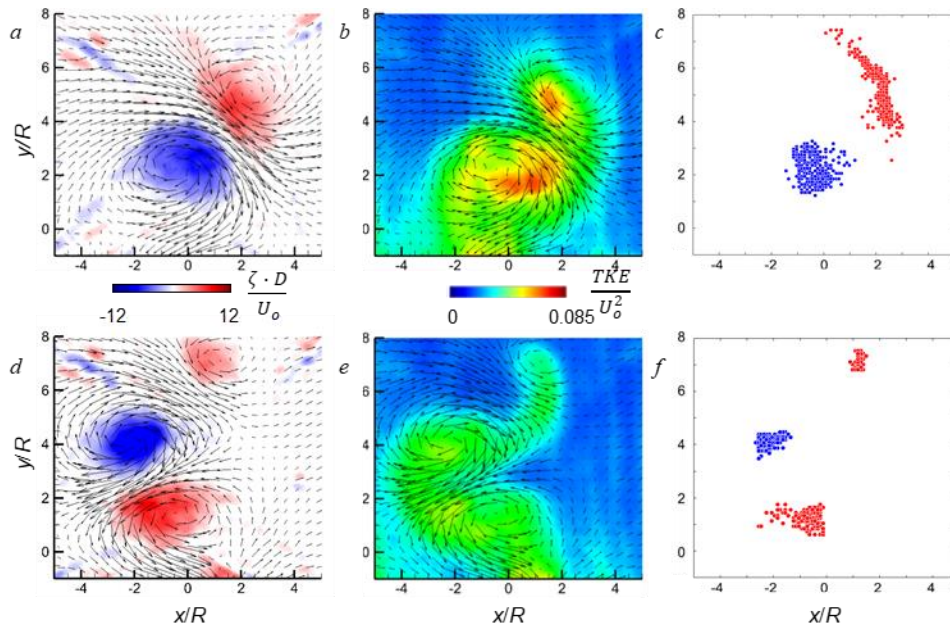


Figure V.12. Contour plots of the POD-reconstructed time-averaged vorticity (a, d) and turbulent kinetic energy (b, e) fields, along with the planar distributions of the vortex cores (c, f) for the unstable (a–c) and stabilized (d–f) model dynamics of Fig. III.47.

Based on the color raster plots of the streamwise vorticity downstream of the model, the actuation effects significant changes in the vortex topology (Figures V.12a and d, respectively). The unstable model is characterized by the formation of a vortex pair whose major axis is slanted to the right, and it is remarkable that although the model undergoes unsteady oscillations, the time-averaged vorticity distributions indicate that the coherence of the vortex pair is largely preserved although the time-averaged CCW vortex appears weaker and its streamwise vorticity is less concentrated than in the opposite CW vortex. Following the onset of actuation that leads to model stabilization, the symmetry of the forebody vortex pair is flipped horizontally, and the vorticity map indicates that the cores of the CW and CCW vortices have similar distributions and strength. This map also shows the appearance of an additional CCW vortex near the upper edge of the field of view that might be associated with the presence of the jet actuation. The corresponding concentrations of turbulent kinetic energy (TKE) within this view (Figures V.12b and e) show that compared to the stabilized

flow, in the unstable flow in the absence of actuation the TKE is somewhat more spread and is higher near the centers of the vortex cores suggesting evidence of some oscillations. This effect is also apparent in the map that depicts the centers of the vortex cores in instantaneous realizations based on equivalent Γ_1 vortex detection scheme on the POD-reconstructed flow field. In concert with the vorticity concentrations in Figure V.12a, the cores of the CCW vortices are indeed spread while the cores of the CW vortices appear clustered. In the presence of actuation the clustering of the cores is even more pronounced.

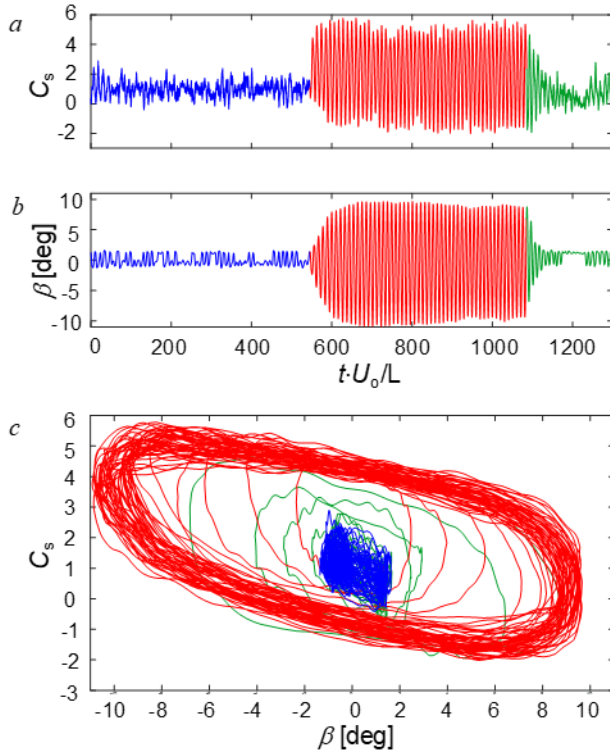


Figure V.13. Time evolution (a, b) and phase relationship (c) of the side force coefficient C_s and the yaw angle β for the $L/D = 2$ forebody model at $\alpha = 55^\circ$. The effect of aerodynamic model destabilization by the jet actuation is shown between $546 < t \cdot U_o / L < 1085$.

magnitude side force of the same sign, and the oscillating force remains offset. By comparison to the return of the model to the unstable state in Figures V.11a and b, the amplification here is significantly higher. When the actuation is turned off at $t^+ = 1085$, both C_s and β exhibit transitional decaying response that is somewhat longer for C_s . The phase plot in Figure V.13c shows the return to base state from the limit cycle of the unstable state.

Similar to the changes associated with stabilization in Figures V.12, the changes in the flow field that are associated with the destabilization are shown in Figure V.14. The time-averaged color raster plots of the streamwise vorticity concentrations (Figures V.14a and d) capture a distinct asymmetric vortex pair with its major axis tilted to the left. It is remarkable that the symmetry of this vortex pair does not change when the model becomes unstable in the presence of actuation (Figure V.14d) although both vortices are somewhat displaced upward and remain coherent. Because these data do not suggest significant coupling between the vortex pair and the stability of the body, it is conjectured

The receptivity of the model to unstable oscillations within this range of incidence was also explored by using the actuation to destabilize the model when the base flow is nearly-stable. When the azimuthal position of the ogive forebody was set to $\phi_{\text{ogive}} = 0^\circ$, the model appeared to be stable $\alpha = 55^\circ$ with relatively low-level time-varying oscillations in $C_s(t)$ and $\beta(t)$ with average means and amplitudes of 0.99 and 0.8 (C_s) and -0.06° and 0.7° (β) (Figures V.13a and d) prior to the onset of actuation at $t^+ = 546$. The corresponding C_s - β phase plot (Figure V.13c) is clustered about $(0.99, -0.06^\circ)$ and is nearly featureless. The actuation jet which is placed at $\phi_{\text{jet}} = 90^\circ$ is activated at $t^+ = 546$, and results in strong oscillations of the C_s at frequency of 7 Hz ($St_L = 0.0093$) that seem to amplify very rapidly to a nominal amplitude of 3.5 by comparison to the corresponding oscillations of the yaw angle that reach an amplitude of 10.3 within 9 cycles following the onset of the actuation as the body motion couples to the change in the force. It is noteworthy that following the actuation, the slightly positive C_s becomes almost instantly biased towards high

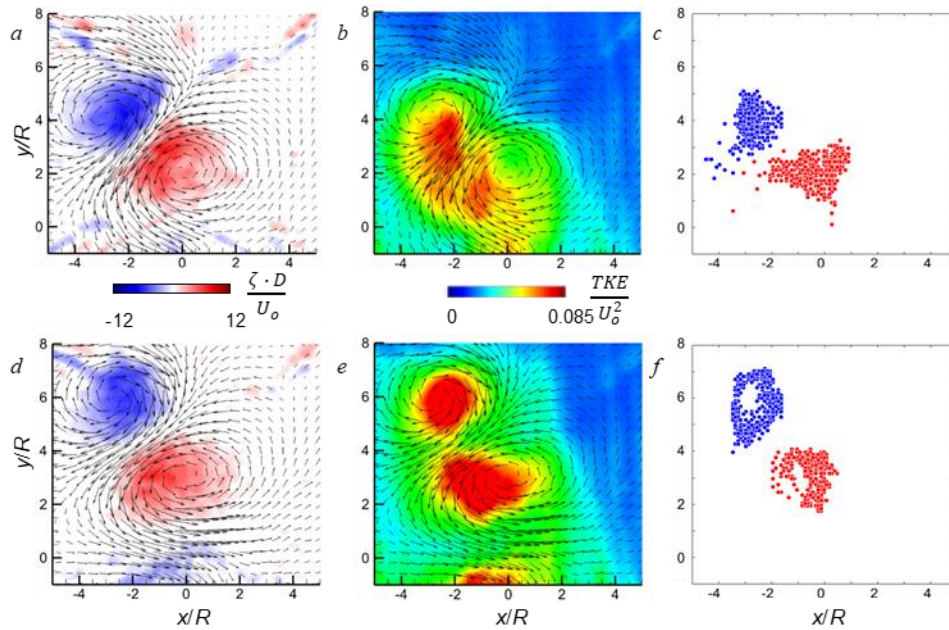


Figure V.14. Contour plots of the POD-reconstructed time-averaged vorticity (a, d) and turbulent kinetic energy (b, e) fields, along with the planar distributions of the vortex cores (c, f) for the stable (a–c) and destabilized (d–f) model dynamics of Fig. III.49.

that the unstable motion is triggered by coupling of the jet actuation to the wake of the cylinder rather than to the forebody. Some differences are noted, though, when examining the distributions of the turbulent kinetic energy for these two flow fields (Figures V.14b and e). These data show that during when the model is stable (Figure V.14b), the peak turbulent kinetic energy is measured in the interaction zone in between the two vortices. However, the turbulent kinetic energy levels generally increase when the model is unstable and the vortices appears to be farther separated. The cores of the instantaneous vortices (using vortex detection based on the Γ_1 criterion, Figures V.14c and f) show distinct orbits of the vortex cores when the model is unstable indicating precession about the central position of their trajectory.

VI. Summary

The experimental research program focused on the unsteady flow mechanisms associated with aerodynamic flow control of the coupling between wire-mounted moving axisymmetric bodies of varying fineness ratio ($L/D = 1.83, 4$, and 11) and their near-wakes using fluidic actuation at the flow boundary. This approach enabled prescribed modification of the time-dependent aerodynamic loads on the moving bodies to ultimately enable active control of their motion and stability. Actuation was provided by several configurations of individually-controlled synthetic jet actuators mounted at the aft end ($L/D = 1.83$) to induce partial flow attachment or at the forebody ($L/D = 4$, and 11) for manipulation of the forebody vortices. The interactions between the actuation and the flow over the model were investigated using load cells, particle image velocimetry (PIV), hot-wire anemometry, and a motion analysis system.

The near-wake stability dynamics and the associated aerodynamic loads were controlled using fluidic actuation at the aft end of the model having fineness ratio $L/D = 1.83$ when it was rigidly driven by the wire traverse in prescribed coupled pitch-yaw rotation at low angles of incidence (up to $\pm 5^\circ$). The prescribed motions were executed over a range of reduced frequencies that were designed to mimic natural unstable motions of a similar airborne platform in the absence of roll. The evolution of the wake was characterized using stereo PIV measurements in the y - z plane ($1.3 < y/R, z/R < 1.3$) at $x/D = 1$. Time-dependent actuation was effected using time-harmonic modulation of the resonance waveform of each of the four independent synthetic jet modules (two opposing jets for pitch or yaw) over the same range of reduced frequencies ($0.017 < k < 0.259$, $Re_D = 1.73 \cdot 10^5$). The actuation led to active enhancement or suppression of the cyclic aerodynamic loads (lift, side forces, and pitch and yaw moments), with negligible effect on the drag or rolling moment. It was demonstrated that the structure of the near wake of the *stationary model* in the presence of spin-modulated actuation (effected by phasing the actuation waveforms) was similar to the wake of the *moving model* when it was subjected to a “natural” unstable pitch/yaw motion at amplitudes of up to 3° . While similar to the earlier findings in pure pitch, these finding proved that cross-talk between the jets was minimal (i.e., the jets affecting pitch did not affect yaw cancellation/augmentation and vice-versa). At a lower reduced frequencies, the aerodynamic loads induced by the actuation were nearly identical to the corresponding aerodynamic loads of the base flow over the dynamically pitching/yawing body. As k increased at a given actuation level, the control authority diminished somewhat at $k = 0.207$ (the resonance frequency of the pitch/yaw motion), and then increased with k up to $k = 0.259$. When the actuation on the stationary model was applied at its vortex shedding frequency ($k = 1.43$, $St_D = 0.234$) it resulted in a strong coupling as demonstrated by the change in structure energy distribution of POD modes of the wake.

Two flow control strategies inspired by GT’s earlier studies were demonstrated when the model was undergoing combined pitch/yaw motions. The first focused on decoupling the wake response from the model motion, and was designed to render the wake response invariant and equivalent to the wake of a nominally static model. The second intensified the response of the wake to the prescribed motion of the body to mimic larger angular pitch/yaw deflection. These strategies were applied at reduced frequencies within the range $0.017 < k < 0.259$ and demonstrated up to 75% reduction or 100% augmentation of the motion-induced side and lift forces. Alternatively, the actuation led to variation of the motion-induced pitch and yaw moments resulting in up to 80% reduction or 40% augmentation, without affecting the rolling moment. Measurement of the velocity distribution in the near wake showed that when the actuation to decouple the wake from the model motion was applied, the first four POD modes became similar to the modes of the

stationary model in the absence of actuation, even though the body was still moving, and there was a 40% suppression in the motion of the wake centroid (defined about $0.8U_0$). Alternatively, when the actuation enhanced the coupling to the wake, the POD modes extended radially, commensurate with the increase in wake deflection, and there was an 80% augmentation in the motion of the wake centroid.

Trajectory control of the axisymmetric body was investigated in 3-DOF (pitch, yaw, and roll) using time-dependent fluidic actuation. The free motions were realized by using the wire traverse to support a sting attached to the free-pivoting model such that it was free to precess in pitch, yaw, and roll in response to flow-induced aerodynamic loads. In the absence of actuation, the interaction of the cross flow with the model led to natural, time-periodic roll, pitch, and yaw oscillations whose characteristic amplitudes and frequencies varied with tunnel speed.

Open-loop actuation that was independent of the model attitude was applied to assess the long term coupling of the actuation with the model response for combinations of one, two, and all four actuation jets. These effects were derived from the model dynamic orientation and its near-wake response using step-like transition from an unactuated state to continuous actuation. It was found that the characteristic time scale needed for reaching a quasi-steady response to the actuation varies with tunnel speed and was within 150 convective time scales or $0.6 \tau_z$ at $Re_D = 1.92 \cdot 10^5$ (τ_z is the nominal baseline oscillation period, extracted from the baseline yawing motion). Activation of a single control jet shifted the center of the body oscillation by about 2° in pitch and yaw, and suppressed the amplitude of natural oscillations of the model by approximately 60%. It was shown that the model's response to actuation by combinations of two *adjacent* jets could affect the yaw and pitch coupling by orienting the model by up to 2.5° in pitch or in yaw while suppressing the amplitude of its natural oscillation by 20% or 40%, respectively. However, the actuation by an *opposing* jet pair was less effective and led to damping of the pitch-yaw coupling with a 25% reduction in oscillations. Simultaneous actuation with all four jets resulted in a 5% increase in the model's oscillations.

To fully exploit the capabilities of active flow control to achieve a desired attitude of the freely-precessing bluff body, a PD closed-loop controller based on an earlier 1-DOF PID controller (developed under the previous program) was utilized to effect a required trajectory. The alteration of the near-wake structure and dynamics under closed-loop control are analyzed using the measured three velocity components and the streamwise vorticity. It was shown that suppression or amplification of the natural oscillations of the model yields changes in the model cross-stream velocity along with the magnitude and extent of the wake deficit and the structure of vorticity concentrations within the inner wake, which lead to significant changes in the wake stability characteristics. The alteration of the near-wake structure and dynamics under closed-loop control were analyzed using dynamic mode decomposition (DMD) from high speed SPIV measurements of the velocity field in the near wake which revealed changes in the symmetry of the primary dynamic modes in their frequency response.

The present work demonstrated that PD closed-loop control could significantly suppress model oscillation by more than 80% over the present range of Re_D , and that at $Re_D = 1.92 \cdot 10^5$ this suppression occurred within $0.1 \tau_z$ (about 20 convective time scales). Furthermore, the same control scheme could be inverted for amplification of the natural yaw or pitch oscillation by more than 225% of the amplitude of the base flow. In addition, this controller was used to effect a steady deflection of the model attitude (within $\pm 2^\circ$) in pitch or yaw, when the wire-supported sting was

stationary. This segment of the investigations was extended by prescribing controlled external disturbances to the sting using the 6-DOF in which the sting precessed at $\pm 2^\circ$ in pitch and yaw, 90° out of phase, and the controller could impose a desired directional attitude that either tracked the sting motion (within 0.5°), or decoupled the model from the moving sting using the actuation for effective disturbance rejection.

The scope of the ARO investigations at small low attitude angles was expanded to include preliminary investigations of controlled modifications of the aerodynamic characteristics of axisymmetric slender bodies at moderate angles of attack (or inclination) for which the aerodynamic loads are dominated by the formation, evolution, and dynamics of counter-rotating vortex pairs that are engendered near the tip of the cylinder's forebody. These preliminary investigations were conducted using a modular axisymmetric cylinder ($L/D = 4$, $D = 90$ mm) having a $1D$ long conical forebody. Modules of azimuthal arrays of synthetic jet actuators were placed at the juncture between the forebody and the cylinder, and at the aft end of the cylinder. While the front module was designed for controlling the symmetry of the forebody vortices and their interactions with the wake, the aft module was intended for cooperatively controlling the near wake at the aft end and thereby affecting the drag, lift, and side forces.

In contrast to earlier investigations of fluidic control for affecting the symmetry of the forebody vortices, in the present investigations *the upstream actuation jets were deliberately placed well downstream of the forebody tip* (nominally $1D$). The objective was to test whether the rollup of these vortices would be susceptible to upwind (upstream) actuation, and therefore might indicate that the rollup of the vortices is affected by absolute instability. Initial tests of the upwind flow control indicated that when there was an imbalance in the vortex pair circulation, giving a rise to the net side force, a nearly complete restoration of symmetry of the forebody vortices was achieved in the presence of actuation. As the controlled vortices become far better balanced a significant reduction in the asymmetric side force was achieved. The effects of *cooperative actuation* using the upstream actuator along with the aft end actuator were assessed from increments of the normal ΔC_N , axial ΔC_A , and side ΔC_S forces for $0 < \beta < 10^\circ$ ($\alpha = 25^\circ$), between the presence and absence of actuation. The induced force by the aft actuator had significant components in the body's normal and axial coordinates that resulted in a decrease in all three forces in the presence of actuation. The most prominent is in the incremental decrease in the side force ΔC_S in excess of -0.2 over most of the range tested while ΔC_N and ΔC_A showed reductions between -0.1 and -0.2 . It is also noteworthy that the reduction in the side force has a local maximum between $\beta = 2^\circ$ and 4° and that the lift and drag forces exhibited a change in slope.

Finally, these preliminary investigations were also extended to include a slender axisymmetric body with $L/D = 11$ at moderate-to-high angles of inclination (25° to 65°). This study confirmed the flow sensitivity to small perturbations near the tip of the forebody in that a full azimuthal rotation of the forebody induced a nearly-period switching in the direction of the resulting side force that is attributed to small manufacturing imperfections near the forebody tip. It was shown that rotating the actuation jet azimuthally altered the magnitude and direction of the induced side forces significantly. The presence of the actuation can *reverse* the direction of each of the side forces that would be induced in the absence of control and alter its sign, implying a reversal in the symmetry of the forebody vortices. These changes in topology were confirmed by direct SPIV measurements, which showed that the dominant vortex pair in the controlled flow is inverted relative to the vortices of the vase flow.

These investigations also demonstrated that within the narrow ranges of angles of incidence (between 50° and 60°), the coupling between the forebody vortices and the cylinder's near-wake can lead to base flow instabilities of the model that are manifested by significant pitch and yaw oscillations. These observations led to consideration of whether manipulation of forebody vortices and therefore their interaction with the wake can be used to stabilize or destabilize the model when it is naturally unstable or stable, respectively, within this incidence range. The highly-unstable mode was observed for a model incidence of 50° , exhibiting the side force $C_s(t)$ and the yaw angle $\beta(t)$ oscillations of about ± 2 and $\pm 8^\circ$, respectively. Following activation of flow control, the peak-to-peak oscillations of C_s and β were attenuated by factors of about six and over ten, respectively. The receptivity of the model to unstable oscillations within this range of incidence was also explored by using the actuation to destabilize the model when it is nearly stable in the base flow at incidence of 55° , having oscillation amplitudes of about 0.8 and 0.7° in $C_s(t)$ and $\beta(t)$, respectively. When the actuation jet was activated, it effected strong oscillations of C_s that amplified rapidly to a nominal amplitude of 3.5 , while the corresponding oscillations of the yaw angle reached an amplitude of 10.3° within 9 cycles following the onset of actuation.

These findings indicate the receptivity of the flow over the slender to fluidic actuation and has paved the way to a follow-on ARO program "Cooperatively-Controlled, Coupled Body-Wake Flow Instabilities of Slender Cylindrical Bodies at Large Incidence." It is anticipated that fundamental understanding of the coupled stability mechanisms of the cylindrical body would ultimately provide a robust in-flight steering approach for controlling the global aerodynamic loads on such slender bodies.

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