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A Wind-tunnel Investigation of the Dynamic Manipulation of Forebody Vortices

by

RICHARD LEE

B.Tech. (Mech), B.Eng. (Aero)

A thesis submitted to
the Faculty of Graduate Studies and Research
in partial fulfilment of
the requirements for the degree of
Master of Engineering

Ottawa-Carleton Institute for
Mechanical and Aerospace Engineering

Department of
Mechanical and Aerospace Engineering
Carleton University
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of Forebody Vortices**
submitted by
Richard Lee, B.Tech., B.Eng.
in partial fulfilment of the requirements for
the degree of Master of Engineering

Thesis Supervisor

Professor R. Bell
Chair, Department of Mechanical and Aerospace Engineering

Carleton University
April 28, 1995

Abstract

A novel scheme has been proposed in which the behaviour of forebody vortices at high angles of attack is dynamically manipulated to generate side forces and yawing moments which provide suitable directional control of a fighter aircraft as a conventional rudder loses its effectiveness. Exploiting the bi-stable nature of the vortices, the method involves deliberately switching the vortices, between the two stable states, at high frequency with two forward-blowing nozzles located near the apex of the forebody nose. A previous water-tunnel experiment has demonstrated that the side force can be reliably switched by the efflux from the nozzles with a small blowing coefficient and at a certain alternating frequency. To continue the research on this control scheme, a wind-tunnel experiment has been conducted to determine the variation of time-average side force and yawing moment with blowing duty cycle at various alternating frequencies, and to determine whether the method will perform at high Reynolds number.

Results from force and moment measurements have shown that in a non-sideslip condition, time-average side force and yawing moment vary linearly with the duty cycle of the valves, and symmetrically about a zero-axis. This signifies the potential of this technique as an alternate form of side force control. Control was demonstrated at a Reynolds number of 1.76×10^5 with a minimum blowing coefficient of 0.00066 and a maximum reduced frequency of 0.32, both of which are improvements over the water-tunnel findings. In sideslip, the linear variation is retained to a large extent but the symmetry of the side force and yawing moment response is about non-zero values. Unfortunately a desirable side force response due to blowing could not be demonstrated at a Reynolds numbers above 1.76×10^5 . Unsuitable nozzle location is suspected as the cause.

Acknowledgements

Several people from the National Research Council of Canada and Carleton University have been involved with this project. Because my dealings with these individuals have been pleasant and, in many cases, instructive I feel I am obligated to acknowledge the contributions of everyone:

- From the Science and Technology Center (Carleton) I thank shop supervisors **Attila Peter** and **Larry Boissoneault** for their discussions of the manufacturing details of the wind tunnel model; and machinist **Jim Whitehorne**, for his superb workmanship on the model.
- From the Engineering Technology Center – Mechanical (Carleton) I am thankful to shop supervisor **John Batky** for fitting my odd-jobs into his schedule on a moment's notice and for occasionally taking time to advise on the machinability of my ideas; and to machinists **Rob Hill** and **Pierre Trudel**, for doing those odd-jobs.
- From the Department of Mechanical & Aerospace Engineering (Carleton), I am grateful to **Bob Ellis** for his skill with mounting the strain gauges and wiring the balance; to **Fred Barrett** for designing and assembling the valve control unit; and undergraduate-student **Jeremy Dillon**, for assisting me with the volume flow rate calibration and with other preparations leading to wind-tunnel entry.
- From the Applied Aerodynamics Laboratory (NRC), I am thankful to shop supervisor **Victor Maloney** for arranging the subcontracting of the strut support and sting to Otal Precision, his finishing work on these assemblies, and his occasional sage advice; to **Todd Brown**, wind tunnel operations supervisor, who was always helpful to me before, during and after the test; and

to summer-student **Tamas Bertenyi**, the “button pusher” from Carleton – thanks for staying with us on those long days.

- From Dr. Hanff’s staff, I wish to thank **Kul Kapoor** for his valuable insights into mechanical design; and **Dr. Xing Zhong Huang** and **Hong Xia** (Algonquin College) who comprised the “flow vis team” during the wind tunnel test.
- A special acknowledgement goes to **Armando Prini** – the “basket case” – who introduced me to the bizarre world of data acquisition, put up with my barrage of questions, and provided data acquisition support during four weeks of testing. I could not have done without Armando’s enthusiasm, skill and energy.

For documenting this work, I also appreciate the assistance of my $\text{\LaTeX}$ mentors: my brother **James Lee** and colleague **Matt Wuhr**. Thanks is given to another brother, **Wayne Lee**, for creating the computer-generated rendering appearing in Figure 3.2.

My sincerest thanks is reserved for **Dr. E.S. Hanff** from the National Research Council, and **Prof. R.J. Kind** from Carleton University for giving me the opportunity to perform this work, for sharing their knowledge and expertise, providing guidance, and for being generous and patient with a fledging “kiwi”.

Rick Lee

*On behalf of my brothers,
this work is dedicated
to the memory
of our great-aunt and uncle,
Ksenia and Nikolai
Nurm*

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Nomenclature

[a]	Matrix of slopes from straight-line fitting of bridge output
A	Cross-sectional area of nozzle
A _t	Cross-sectional area of restriction
[C]	Matrix of sensitivity coefficients
C _m	Nozzle mass flowrate coefficient, $\frac{\dot{m}_j}{\rho V_\infty S}$
C _N	Instantaneous yawing moment coefficient, $\frac{N}{q_\infty S l_{ref}}$
$\bar{C}_N$	Time-average yawing moment coefficient, $\frac{1}{n} \sum_1^n C_N$
C _P	Center of pressure
C _Y	Instantaneous side force coefficient, $\frac{Y}{q_\infty S}$
$\bar{C}_Y$	Time-average side force coefficient, $\frac{1}{n} \sum_1^n C_Y$
C _μ	Coefficient of blowing momentum, $\frac{\dot{m}_j V_j}{q_\infty S}$
$\bar{C}_\mu$	Average coefficient of blowing momentum
d	Nozzle diameter
D	Base diameter of model
f	von Karman shedding frequency
[f]	Matrix of constant calibration load ratios
l, l _{ref}	Overall length of model
$\dot{m}_j$	Nozzle mass flowrate, $\rho \frac{\pi}{4} d^2 V_j$
n	Number of samples acquired within one alternating blowing cycle
N	Yawing moment about the resolving center of the internal balance

p	Static pressure upstream of restrictor
q_{∞}	Freestream dynamic pressure, $\frac{1}{2}\rho V_{\infty}^2$
Q	Nozzle volume flow rate
$\bar{Q}$	Average nozzle volume flow rate
r_n	Hemispherical radius of a blunt nose
Re_D	Reynolds number, $\frac{V_{\infty} D}{\nu}$
S	Reference area, $\frac{\pi D^2}{4}$
St	Strouhal number, $\frac{fD}{V_{\infty}}$
T	Period of an alternating blowing cycle
V_{∞}	Freestream velocity
V_j	Blowing nozzle exit velocity
x	Longitudinal location of nozzle exit relative to the nose apex
X_{CP}	Location of center of pressure
Y	Side force

Greek Symbols

α	Angle of attack
β	Angle of sideslip
ε	Estimated error in $\bar{C}_\mu$
ν	Kinematic viscosity
ρ	Density
σ	Body-axis inclination to freestream velocity (i.e. the turntable angle)
τ	Duration a valve is open during the alternating blowing cycle
ϕ	Roll angle
ψ	Yaw angle
ω	Angular frequency of alternating blowing, radians/time
ω^*	Reduced frequency of alternating blowing, $\frac{\omega D}{V_{\infty}}$

Subscripts

1 Port valve

Chapter 1

Introduction

1.1 Background

The evolution of fighter aircraft is largely driven by the need to achieve tactical effectiveness and enhance combat survivability against a superior number of hostile aircraft at least cost. Citing that the conventional approach of enhancing performance by increasing the thrust-to-weight ratio is no longer cost effective, Herbst (1980) advocates that the evolution must rely on advances in aerodynamics and control technology.

Herbst's concept of a new fighter involves satisfying three requirements: high supersonic speed, short take-off performance and subsonic combat capability, e.g. for subsonic maneuvers. To realize the latter requirement, he feels that the application of "supermaneuverability" technology will be necessary. Because of the effectiveness of short-range missiles, the maneuverability of the aircraft becomes essential to surviving an engagement with a hostile aircraft at close range. Since maneuvering under these conditions can lead to a deceleration to slow speeds and high angle of attack, the aircraft must have a "direct force" capability which enables the pilot to orient it independently of flight path and to sustain a precise firing posture. To achieve an advantage in pursuit, the aircraft must also have a "post-stall" capability. This is the ability of an aircraft to perform a rapid, controlled maneuver within a short distance beyond the range of maximum lift and up to at least $\alpha = 70^\circ$, where α is angle of attack. For tactical superiority, post-stall capability is required for

$\alpha > 50^\circ$ [Herbst, 1980].

Conventional fighter aircraft configurations are unable to produce the yawing moments that are required for direct force and post-stall capabilities. At increasing angles of attack, the effectiveness of the rudder deteriorates as it becomes increasingly immersed in the wake of the wing and body of the aircraft. But within the same α range where the rudder loses the ability to provide yaw control, streamwise vortices are shed asymmetrically from the forebody of the aircraft and give rise to significant lateral force and yawing moment. These can cause abrupt responses in yaw and roll, potentially taking the aircraft into an undesirable spin mode. Researchers have recognized, however, that the controlled manipulation of the same asymmetric forebody vortices is a potential solution to realizing supermaneuverability technology. Highly energetic, the vortices appear in pairs, shedding from the tip of the nose, one to each side of the longitudinal axis of the body. Typically beyond an angle of attack of 30° the vortices adopt an asymmetric stance, as shown in Figure 1.1, that is bi-stable in nature – meaning that a slight perturbation will cause the vortices to switch between the stable states and change the orientation of the side force and yawing moment. To generate desired forces and moments, the manipulation of forebody vortices essentially reduces to introducing artificial, controlled perturbations.

Identified as an alternate means of providing directional control for fighter aircraft at high angles of attack, forebody vortex manipulation has been an active research area for several years. But the manipulation methods under investigation are essentially steady schemes [Malcolm, 1993]. That is, quasi-steady loads are generated by forcing the forebody vortices to assume a prescribed stable state. Since forebody vortices are inherently bi-stable over a significant range of angles of attack, the steady methods involve first forcing them to adopt a symmetrical stance, typically by tailoring the forebody geometry, and then altering their position by pneumatic or mechanical means to generate desired control forces or moments. The need to overcome the artificially induced symmetry may require considerable power

and furthermore, it is difficult to implement control laws due to the severely non-linear behaviour of the vortices and resulting aerodynamic loads.

A novel dynamic vortex manipulation scheme has been proposed [Alexan et al, 1994], however, that has the potential of avoiding the aforementioned problems associated with steady schemes.

1.2 Concept of the Dynamic Manipulation of Forebody Vortices

Originally suggested by Hanff, of the National Research Council of Canada, this scheme takes advantage of the bi-stable nature of the vortices and exploits the fact that minute perturbations at the nose of the forebody can cause them to switch between the two stable states. Specifically, the forebody vortices are deliberately switched, alternating between the stable states, at a relatively high frequency selected so as not to cause a significant response of the aircraft. The aircraft would, however, respond to the time-average loads generated by the vortex manipulation; these can be controlled by varying the fraction of the switching-cycle period during which the vortices are in one or the other stable state, i.e. by modulating the switching duty cycle. The concept is illustrated in Figure 1.2(a) and the effect of changing the duty cycle is shown in Figure 1.2(b). In theory, the perturbation can be caused by pneumatic or mechanical methods.

The research effort to explore this concept began with water-tunnel experiments conducted at Carleton University [Alexan, 1992].

1.3 Objectives of the Research

The work reported in this thesis continued the investigation of the dynamic manipulation concept. The work had two main objectives: to determine the variation of time-average side force and yawing moment over a blowing duty cycle range of 0 to

100% for various switching reduced frequencies, and to determine if the technique will perform at high Reynolds number. To be consistent with the water-tunnel investigation, forward nozzle blowing was retained as the chosen vortex manipulation method and the model shape as well as the location of the nozzles were essentially unchanged.

1.4 Outline of Thesis

Chapter 2 briefly reviews previous work performed in the area of forebody vortex control since Alexan (1992) has thoroughly documented research activity prior to this period. Emphasis is placed on the water-tunnel results and other work closely related to the forward-blowing technique.

Chapter 3 describes the equipment used and procedures followed during the wind tunnel tests.

Tests were performed for steady and alternating blowing. Side force and yawing moment were measured and the minimum blowing coefficient, C_{μ} , required to manipulate the vortices was determined, as was the maximum frequency with which the vortices could be switched between their two stable states. Measurements were carried out at different values of Reynolds number with and without sideslip. **Chapter 4** reports and discusses the test results.

Chapter 5 presents conclusions, and **Chapter 6** offers recommendations for future work in the areas of data acquisition, flexure design and nozzle design.

Chapter 2

Review of Previous Work

2.1 Scope

This work has been preceded by Alexan (1992) who reported on results from water-tunnel experiments, which were the first investigations into the feasibility of dynamic manipulation of forebody vortices. For this reason, a review of Alexan's work will receive emphasis.

Since 1992 researchers have published several reports on forebody vortex control, an indication of the level of activity and progress being made in this area of applied aerodynamics. Because Alexan presents a comprehensive review of the literature prior to 1992, this work will only review the publications appearing in the interim period, concerning work related to the forward-blowing technique.

A comprehensive review of forebody vortex control methodologies under investigation, complete with an extensive list of references, is also presented in Malcolm (1993).

2.2 Water-Tunnel Investigation of Dynamic Manipulation of Forebody Vortices

The objectives of the water-tunnel investigation [Alexan, 1992 and Alexan et. al, 1994] were to assess the ability of forward blowing from two nozzles, located near the apex of a forebody model, to force the forebody vortices to switch their asymmetric positions; and to determine whether the alternation of the bi-stable states can be sustained at a relatively high frequency of switching of blowing, between port and

starboard nozzles.

The forebody model used in Alexan's experiments had a slenderness ratio (l/D) of 2.25 and an included angle of 60° at the apex of the nose. Aligned parallel to the longitudinal axis of the model, the injection nozzles were located at $x/D = 0.25$ and radially positioned at $\pm 135^\circ$ from the windward meridian, as shown in Figure 2.1. The exits of the nozzles were machined flush with the surface of the nose. Force and moment measurements were obtained at Re_D between 7000 and 9000.

Under steady blowing conditions, at $\alpha = 50^\circ$ for example, Alexan observed that the side force, arising from the asymmetric vortices, could be reliably switched from port to starboard or vice versa with a minimum blowing coefficient, $C_\mu \approx 0.0013$, as depicted in Figure 2.2. The minimum blowing coefficient is defined as the limit beyond which increasing the blowing rate does not produce significant increases in side force, i.e. the knee of the curve in Figure 2.2. The ratio of side force to blowing momentum flux (C_Y/C_μ) as seen in Figure 2.2, is more than 2000. This is excellent in comparison to the results of other researchers. For subsequent testing with alternating blowing, C_μ was set to 0.0013.

The maximum frequency for alternating blowing is defined as that which allows just sufficient time for the forebody vortex pattern to reach the stable configuration before being switched back again. Expressing the results as a non-dimensional parameter, referred to as the reduced circular frequency of oscillation, ω^* , Alexan found that the forebody vortices could respond to the alternating blowing up to a maximum reduced frequency of approximately 0.16 with $C_\mu \approx 0.0013$.

Using the experimental results, Alexan estimated the response of a typical fighter aircraft to the fluctuating side force and yaw moment. The response was determined for an F-18 fighter aircraft flying at a velocity of 330 ft/s and altitude of 15,000 ft. in an attitude of $\alpha = 50^\circ$ and no sideslip. For $C_\mu \approx 0.0013$ and $\omega^* = 0.16$, and assuming a harmonic variation of the yawing moment, the apex of the nose would experience a side-to-side motion of ± 0.28 inches and ± 0.68 inches at maximum take-

off and empty weights, respectively. The real-time frequency was estimated to be 3 Hz. Alexan points out that the estimated motion is not negligible: its acceptability is dependent on the physical tolerance of the pilot, who is subjected to g-forces and vibrations due to buffeting in a post-stall maneuver; and also on the ability to maintain a precise firing stance momentarily. Further calculations show that doubling the reduced frequency to 0.32 would reduce the side-to-side amplitude by a factor of four.

2.3 Combining Nose Blunting and Pneumatic Blowing

Roos and Magness (1993) report on a wind-tunnel investigation of a forebody vortex control methodology that combines the blunting of a tangent-ogive forebody (to suppress the development of a natural asymmetric flowfield at high angles of attack) with steady forward blowing, as the means of introducing the small perturbations necessary to cause flow asymmetry leading to controllable side forces. One of the noteworthy features of this work is the location of the nozzle exits – because of the blunting, the nozzle exits are much closer to the tip of the forebody vortices thereby improving the ability to influence vortex asymmetry and while reducing the sensitivity of the blowing effects to sideslip.

A typical forebody model used in this investigation is depicted in Figure 2.3 with the anticipated effect of the forward blowing jets in causing flow asymmetry. The model tested was a 3.5-caliber tangent-ogive with a base diameter of 7.62cm (3.0 inches). The majority of the testing was performed with the nose hemispherically blunted to a radius of 20% of the base radius, i.e. $r_n/D = 0.10$. The nozzle axes were aligned parallel to the model axis and the exits were located at the point of tangency of the tangent-ogive surface and the hemispherical nose. In azimuth, the nozzles were located at $\pm 135^\circ$ from the windward meridian. The diameter of the nozzles were 1.3mm (0.051 inches), i.e. $d/D = 0.017$. Longitudinal grit bands were employed as boundary-layer trips to permit the study of the flow control method under fully turbulent flow conditions. About 10° wide, the grit bands were centrally

located at $\pm 70^\circ$ from the windward meridian.

Figure 2.4 shows the effect of blunting on the side force for the forebody model. Clearly, in the absence of a nose disturbance, a blunted nose is able to suppress the natural forebody vortex asymmetry through a wide range of α . Figure 2.5 shows the typical results for steady nozzle blowing with laminar separation. Significant side force develops at $\alpha = 40^\circ$, increasing to a maximum at $C_Y \approx 0.5$ at $\alpha = 50^\circ$, then a sharp decrease occurs. The response of C_Y to the blowing of the right and left nozzles, however, is not entirely symmetrical. It is noted that Roos and Magness define the blowing rate in terms of the mass flowrate coefficient, $C_{\dot{m}}$, rather than the blowing momentum coefficient, C_μ . They feel that a “displacement effect” of the nozzle flow is responsible for causing a forebody vortex to detach from the surface, as opposed to “any momentum-related entrainment and/or flow energizing effect”. Figure 2.6 shows the blowing effectiveness is insensitive to Reynolds number and, as a result, the side force scales to $C_{\dot{m}}$ rather than C_μ according to Roos and Magness.

A comparison of laminar and turbulent separation results, Figure 2.7 shows that blowing under turbulent-separation conditions is more effective. Figure 2.8 shows the yawing moment and side force response to steady blowing through one nozzle for various blowing rates. It is noted that the largest side force generated at $\alpha = 30^\circ$ requires a blowing rate as high as $C_{\dot{m}} = 0.0002$. Blowing effectiveness in turbulent conditions is illustrated in Figure 2.9 for $\alpha = 50^\circ$ and 55° . The variation of side force with blowing rate has a near-linear behaviour about $C_{\dot{m}} = 0$ and is limited by the maximum C_Y available at α .

The effect of sideslip on side force effectiveness was assessed for the yaw angle range $-15^\circ \leq \psi \leq 15^\circ$ in both laminar and turbulent separation conditions. The results shown in Figure 2.10 for $\alpha = 50^\circ$, indicate that side force capability is almost insensitive to sideslip.

Chapter 3

Equipment and Procedures

3.1 Overview

The investigation was performed in the 6.3ft $\times$ 9ft (2m $\times$ 3m) low-speed wind tunnel at the National Research Council of Canada (Figure 3.1). Shown in Figure 3.2, the test apparatus consisted of a tangent-ogive cylinder model that was sting-mounted on a symmetric strut that spanned the height of the wind-tunnel test section, between the floor and roof turntables. Angle of attack was adjusted by synchronously rotating the turntables, while sideslip was obtained by rolling the model about its axis of symmetry.

Each of the two forward-blowing nozzles, located near the apex of the model, was separately supplied with air through a high-speed solenoid-actuated on/off valve mounted inside the nose of the model. Air supply pressure was controlled by a regulator outside the test section. Side forces and yawing moments were measured in the body-axis frame of reference with an internal strain gauge balance located near the free-end of the sting. Air supply and instrumentation lines were passed through the hollow sting and strut.

Data acquisition and valve control was provided by a customized MS-DOS based, real-time system. This system monitored the internal strain gauge balance and pressure transducers, and generated the signals for the two solenoid valves, controlling their frequency and duty cycle.

To visualize the forebody vortices, a traversing laser light sheet normal to the axis of the model was used to illuminate a smoke-seeded flow. Surface flow visual-

ization was performed utilizing a mixture of special pigment and oil that fluoresced under ultraviolet light. Video cameras were used to record the results of the flow visualizations.

Details of the various aspects of the installation are provided in the following sections.

3.2 Wind Tunnel

The wind tunnel is a closed return circuit with a contraction ratio of 9:1. A 15 ft diameter fixed pitch fan is driven by an external 2000 horsepower DC variable speed motor capable of sustaining a maximum freestream velocity of 350 ft/s (107 m/s) in the test section.

Shown in Figure 3.2, the test section is 15 ft long, 76 inches high and 108 inches wide at the balance centreline. It vents to atmosphere through a breather plate at the downstream end. With 14-inch high, 45° corner fillets the cross-sectional area of the test section is 54.25 ft². At 90 ft/s, the turbulence level is 0.1% which increases to 0.3% with velocity; velocity varies by $\pm 0.25\%$ and the total and static pressure coefficients vary by $\pm 0.5\%$. These variations are based on measurements made at one longitudinal location and three vertical locations [Brown, 1986]. Flow angularity in the transverse and vertical directions has been estimated to be less than 0.25°.

Turntables in the floor and roof of the test section provide support for the sting-strut system. The floor turntable has a diameter of 81.25 inches and the roof turntable has a diameter of 73.25 inches. The center of the turntables is located 74 inches from the upstream end of the test section. Both can be rotated synchronously to a maximum positive rotation of 210° (positive rotation is shown in Figure 3.2) and a maximum negative rotation of 95°. A narrow rectangular slot in the floor turntable permits access to a mounting pedestal, placed approximately 7 inches beneath the floor. The pedestal is rigidly attached to the facility's external force and moment balance via a traversing mechanism that allows the pedestal to be located at any

point along the diameter of the turntable. As such, the pedestal provides fixed-end support for the strut. For this investigation, the pedestal was placed at the downstream traversing limit, i.e. close to the perimeter of the turntable. The roof turntable provided pinned support to the strut.

3.3 Model

The profile of the forebody model was comprised of a tangent-ogive nose and a long cylindrical afterbody. Consisting of a body and an internal blowing system, the model measured 30.3 inches in overall length with a base diameter of 5.3 inches, and altogether weighed 14.3 lbs. A cross-section of the sting-mounted model is shown in Figure 3.3.

3.3.1 Design of Body

Since this investigation was a continuation of the water-tunnel study, it was decided to maintain dimensional similarity with the water-tunnel model. It was expected that comparable results would be obtained. To size the wind-tunnel model, the dimensions of the water-tunnel model were upscaled to a limit at which the blockage of wind-tunnel model profile, oriented normal to the freestream, would not exceed 2% of the test section area. Given this limitation, a scale of 5.3:1 was found to be adequate.

The nose profile of the water-tunnel model was not a true ogive but a composite of curves. To form a true tangent-ogive on the wind-tunnel model, the length-to-diameter ratio (l/D) of 2.25 from the water-tunnel model was retained. Given l/D and the fact that an ogive is, by definition, a part of a circle, a profile was formed with a radius of 28.156 inches and an included angle of 56° at the apex of the nose. By comparison, this angle on the water-tunnel model was 60° – in view of the smallness of the difference, similar test results would be expected. A comparison of the nose profiles from the models is shown in Figure 3.4.

The body of the model was comprised of three components: the nose, middle cylinder and aft skirt. The latter two components form the cylindrical afterbody.

The revolution of the ogive about the longitudinal axis forms the nose. The tangency of the ogive profile occurs at the base of the nose where it blends with the middle cylinder. A low model weight was desirable to maximize the resonant frequency of vibration of the model-sting-strut system. For a lightweight design, the nose was manufactured from aluminum alloy 6061-T6 and was hollowed-out by successive stepped-boring operations. The minimum wall thickness was 3/16 inches. Some material was left inside at the tip of the nose for the purpose of forming the nozzles and supporting brass supply tubes. An inside flange at the base of the nose provides support for the platform of the valve assembly. A high-precision sleeve, extending 1/4 inch from the base of the nose, locates the nose inside the middle cylinder; a steel dowel pin aligns the nose relative to the middle cylinder, and three bolts fasten them together.

To facilitate the assembly of the model on the sting, it was necessary to divide the cylindrical afterbody into two components. The middle cylinder serves to attach the model to the sting and, in turn, supports the nose and aft skirt. Since all aerodynamic loads must be carried by this component, it was made of mild steel and bored out to form a 3/16 inch wall all around. With the model assembled, the cavity houses the valve assembly and cable connections for the pressure transducers and solenoid valves. The middle cylinder is located on a high-precision sleeve on the end of the internal balance; the cylinder is aligned with a steel dowel pin and fastened to the sting with bolts.

The aft skirt is a simple hollow cylinder that completes the profile of the model and also shields the internal strain gauge balance on the sting. With a wall thickness of 3/16 inch and made from aluminum alloy 6061-T6, the skirt threads onto the middle cylinder.

3.3.2 Blowing System

The technique of dynamic manipulation controls the switching of the forebody vortices, between the two bi-stable states, by introducing an artificial perturbation into the flow near the apex of the nose. Investigations of steady schemes have shown that a number of methods can successfully manipulate the vortices and, in principle, could also be used for dynamic manipulation. Of course, the technique requires these methods to be effective at sufficiently high switching frequencies.

The pneumatic and mechanical approaches for controlling the forebody vortices were considered for this investigation. For pneumatic control, a blowing scheme similar to Alexan's water-tunnel model was envisioned: actively deployed hinged conformal strakes, or pivoted normal strakes were contemplated for the mechanical scheme. The apex of the model, however, would demand the size of the strakes and the mechanism for deploying them to be very small. This requirement would have called for more design effort and high manufacturing precision, translating into high cost. It was doubtful that a miniaturized mechanism for the model could have been produced at a reasonable cost and be robust enough to withstand aerodynamic loads and high-frequency cycling.

It was therefore decided to implement a pneumatic scheme for this investigation. An approach using forward blowing was deemed practical for the model because of its simplicity of design and manufacture. Indeed, with the relatively small mass flow rates indicated by Alexan's work, forward blowing would appear to be simpler to implement reliably in a real aircraft where engine bleed air could be used as the air source. Furthermore, literature supports that pneumatic methods are effective over a wide range of angle-of-attack and sideslip (as are mechanical means) [Malcolm, 1991]; and key results from the water-tunnel study, such as the maximum reduced frequency and minimum blowing momentum coefficient, would be useful design criteria.

The forward blowing method employed by Alexan was adopted. Because of the success in influencing the forebody vortices in the water-tunnel study, the location

of the nozzle exits used by Alexan was also adopted for the water-tunnel model. Accordingly, the exits were placed at a longitudinal distance of 1.325 inches from the apex of the nose so that $x/D = 0.25$; and azimuthally positioned at $\pm 135^\circ$ from the windward meridian. The exits were machined flush with the surface of the nose producing an elliptical opening when viewed normal to the surface.

The diameter of the nozzles was determined for a blowing coefficient of 0.0015, an approximation of the minimum $C_\mu = 0.0013$ from the water-tunnel results. To size the nozzle diameter, it was assumed that the density of the air in the freestream and nozzle jet were equal and that the ratio of the nozzle jet velocity to the freestream velocity was 1.5 (the velocity ratio in the water-tunnel study was found to be 1.275 for $C_\mu \approx 0.0013$). This gave a nozzle diameter of 0.0968 inch; 0.096 inch was actually selected (i.e. $d/D = 0.018$) because this was conveniently available as a standard drill size.

The cross-section of the nose assembly, shown in Figure 3.5, illustrates the port side of the blowing system designed for this investigation. Instead of a single three-way valve to supply both nozzles, a dual two-way (on-off) valve system was implemented because it offered operational flexibility: one valve could be open with the other closed; by closing both valves, the blowing system could be turned off; and if the need arose, the phase shift of the valve control signals could be readily altered. Moreover, the response time of the two-way valve selected was faster than that of the three-way valves considered. A disadvantage of the dual-valve approach is that the data acquisition system requires an additional control channel for the second valve; it was not difficult to incorporate the channel, however.

In Figure 3.5, a solenoid-actuated two-way valve, mounted on an aluminum platform, receives pressurized air from an external source. On an enabling signal from the data acquisition system, the pressurized air is ported momentarily to the nozzle at the apex of the nose. On its "source" side, the valve is connected to a Y-shaped brass manifold (not shown in Figure 3.5). Coupled to an external supply

line which passes through the bore of the sting, the manifold distributes pressurized air to both sides of the blowing system.

The two-way valve chosen was an ASCO/ANGAR solenoid high-speed valve. It has an open/close response time of 3 to 5 milliseconds, is normally closed and has a life expectancy of 10 million cycles. This valve was selected for its small size and, primarily, for its fast response time and long life. Longevity was an important consideration because at a reduced frequency of 0.16 (taken from water-tunnel results) in a 300 ft/s freestream, the valves would be required to operate at a frequency of 17.3 Hz. In the normal course of testing, higher frequencies would be investigated so the life of the valve could be consumed quickly. With the long life expectancy of the ASCO valve, it was expected that testing would be complete before significant deterioration of the valves occurred.

The external source of pressurized air was regular shop air, supplied at approximately 90 psig and a mass flow rate of no more than 1 lb_m/s. The air was filtered to 0.5 micron particle size and could be controlled by regulators with indicated ranges of 15 psig or 30 psig.

On the nozzle side, each solenoid valve is connected to a plastic T-joint which serves to incorporate an IC Sensor Model 30 differential pressure transducer into the line. The transducer measures static pressure, which was used to indicate the state of the valve (i.e. open or closed) and to determine the volume flow rate of blowing air supplied to the nozzle. The transducer is a bi-directional, piezoresistive pressure sensor with a 100 mV output span and operates with a 5 volt excitation. A third pressure transducer was incorporated into the brass manifold, upstream of the solenoid valves, to measure the stagnation pressure of the supply air. The reference ports of the three transducers were interconnected with Tygon tubing and collectively vented to ambient pressure in the hollow sting-support strut. The transducers were not temperature compensated.

Downstream of the T-joint, a brass restrictor is inserted into each supply line to

produce a pressure drop between the transducer and the nozzle exit. This pressure drop was calibrated against the volume flow rate, leaving the nozzle, to provide the means of measuring blowing volume flow rate. The restriction was created by a 50% reduction of the 0.096 inch internal diameter of the insert. This reduction was thought to be adequate to choke the nozzle flow so that the volume flow rate would be linear with supply pressure.

Between the restrictor and the nozzle passage in the apex of the nose, the nozzle supply line consists of a brass tube with an internal diameter of approximately 0.100 inch. The tube is inserted into the nozzle passage with a light interference fit which allows the blowing system to be readily detached from the nose. The length-to-diameter ratio of the passage between the restrictor and nozzle exit is about 80:1.

The components of the blowing system were joined together with Tygon tubing. All Tygon connections were clamped with at least two turns of lock wire.

3.4 Model Support

Depicted in Figure 3.2, the model is carried by a sting assembly which is fixed-end supported by a strut. The support strut spans the height of the test section between the floor and roof turntables, which respectively provide fixed-end and pinned support to the strut.

The sting assembly is a two-piece welded structure consisting of an internal balance and a sting. At the welded joint, the assembly is reinforced by a steel split collar. When installed into the strut support, the assembly has a length of 46-1/4 inches from the face of the strut to the flexure/model interface. The distance between the face of the strut and the aft end of a mounted model is approximately 31-3/4 inches, which is equivalent to six base diameters of the model. At a turntable angle of 80°, there was a clearance of 21 inches between the tip of the model's nose and the sidewall of the test section, which was considered sufficient.

3.4.1 Internal Balance

The internal balance is a two-component strain gauge balance that resolves side force and yawing moment in the body-axis frame of reference. Extending the free-end of the sting by 8-1/2 inches, the balance was made from 17-4 PH stainless steel which has superior yield and endurance strength over ordinary mild steel. The balance has the high-precision sleeve that is used to locate the model on the sting assembly.

Shown in Figure 3.6, the balance consists of two flexures which are flats located 3 inches apart and 1-1/2 inches from the model/sting interface. Each flexure comprises a full-bridge of strain gauges which provides increased sensitivity and accuracy, and inherently compensates for temperature effects in the strain gauges. The flats were milled to a thickness of 0.520 inch, symmetrical about the longitudinal axis of the balance. Because the sting is a hollow tube, as can be seen in Figure 3.6, the tube wall actually forms the flats and provides four surfaces for mounting a full-bridge. The corners of the milled slot were filleted to alleviate stress concentrations. The flexures were, nevertheless, the weakest part of the sting assembly.

A foil-type strain gauge, manufactured by Showa Measuring Instruments, was selected. It is a general-purpose strain gauge with a gauge length of 1 mm, has a nominal resistance of 350 ohms and a gauge factor of approximately 2.2 (the ratio of the fractional change of gauge resistance to the strain experienced by the gauge), and is temperature compensated for stainless steel. The dimensions of the gauge backing were adequate for it to be mounted onto the surface of the flat without trimming. Each strain gauge was carefully aligned with orthogonal centerlines, lightly inscribed on the flat. Appendix A describes in detail the procedure of surface preparation and application of the strain gauges to the flats, and the steps taken to protect and waterproof each installation.

The wiring scheme for one flexure is shown in Figure 3.7. Figure 3.7(a) illustrates a typical full-bridge layout with excitation leads and signal outputs to the data acquisition system. The actual wiring layout shown in Figure 3.7(b), however, is

different because it incorporates the ability to reconfigure the bridge in the event an inoperable strain gauge was detected. This ability permits a flexure to be operated as a full-, half- or quarter-bridge (i.e. an individual strain gauge) by disconnecting gauge leads at the terminal block, rather than desoldering them at the terminal pads on the gauge. Figure 3.7(b) shows a flexure configured for full-bridge operation. A pair of strain gauges on each side of the flexure share a nearby terminal pad, as seen in Figure 3.6, which provides 5 volt excitation and serves as the junction for the two output signals. Each terminal pad was connected to the terminal block with a twisted and shielded 3-conductor lead. The wire gauge of the conductors was 26. A monitoring lead shown in Figure 3.7(b), was employed to monitor the delivery of a 5-volt excitation to the strain gauge bridges.

As shown in Figure 3.6, the three-conductor leads from the terminal pads at the flexures were securely anchored to the balance to provide strain relief. All leads were passed through the inside of the hollow sting, down the back side of the strut support to the terminal block, located beneath the test section floor. All shielding was grounded.

The natural frequency of the balance-model assembly was approximately 25 Hz. With the model and support assembly installed in the test section, this frequency was found by applying a momentary side force to the model at a point ahead of the balance flexures (i.e. near the base of the nose). The output from the strain gauges was used to capture the response of the flexures, which is shown in Figure 3.8(a).

3.4.2 Sting

The sting is a mild-steel seamless tube that has an outside diameter of 3 inches, at its joint with the support strut, and is tapered to a 2-inch diameter before it enters the aft end of the model. The diameter of the hollow is 1 inch, which is sufficient to permit a pressurized air line and instrumentation/control cable bundles to be passed from the support strut to the model. At the strut-end, the air supply line and cable bundles are routed through the open end of the sting and down through the floor

turntable, being carefully placed and taped to the downstream side support strut.

The sting is fastened to the support strut with a self-holding 3/4 inch per foot tapered joint. The joint is tightened by a large hex nut threaded onto the protruding end of the sting. A self-holding tapered joint was desirable because it provides a firm joint, without excessive tightening torque, effectively creating a fixed-end support condition; also, when loosened, the taper permits the sting to roll.

A Lucas-Schaevitz digital inclinometer was used to indicate the roll angle of the sting. The inclinometer was mounted near the base of the sting using an adapter that slipped into two slots machined on the sting. Through the series of angular alignment pins which fix the nose of the model relative to the sting, the angle indicated by the inclinometer is also the roll angle of the model. The actual angular misalignment between the plane of the nozzles and the sting slots for the inclinometer adapter, was not ascertained. However, the error in the angle readings, which includes the effects of a possible assembly misalignment, should not exceed an estimated maximum of $\pm 0.35^\circ$.

The internal balance was welded to the sting and a nondestructive inspection was performed to ascertain the integrity of the weld. The inspection revealed apparent defects which caused some concern [Holt et al., 1994]. As a precaution, a bolted split collar was designed and manufactured to reinforce the welded area and, in the event of a weld failure, to carry the entire load.

Milling of the flexures and the slots for the inclinometer bracket and the drilling of alignment and attachment holes were done after the sting assembly was welded, machined and the collar installed.

With the model mounted on the sting assembly, the natural frequency of the strut-sting-model assembly was estimated to be 180 Hz. The trace of the sting response is shown in Figure 3.8(b). The apparatus was supported as previously described in Section 3.4.1. For this case, the momentary side force was applied

directly to the sting behind the aft skirt of the model.

3.4.3 Support Strut

The support strut is a mild steel seamless tube with an outside diameter of 5 inches and a wall thickness of 1/2 inch. The length of the strut is about 83 inches, spanning the entire height of the test section and extending beneath the floor turntable. By spanning the test section, the strut can be supported at both ends thereby providing sturdier support to the sting, and thus a higher natural frequency of vibration. Furthermore, should the presence of the strut have some influence on the upstream flowfield, particularly the forebody vortices, the effect will be symmetrical about the model.

With a steel attachment flange welded at the bottom, the support strut was tightly bolted to the external balance of the facility beneath the floor turntable. To ensure a firm base and prevent its movement, the external balance was locked during testing. At the top, the strut is supported by a solid steel spindle that passes through the roof turntable and slides into a sleeve, which forms part of the cap assembly welded to the top of the strut. The fit between the sleeve and spindle is precise and does not allow for significant slack. In turn, the spindle was firmly clamped between two channel beams that are part of the roof turntable structure.

As described earlier, the sting is attached to the strut by means of a self-holding taper and a retainer nut.

3.5 Calibration

This section describes the setup and procedures used to calibrate the internal balance, the pressure transducers, and the nozzles for volume flow rate. These calibrations were carried out before entering the wind tunnel but were not checked during or after the tests.

3.5.1 Internal Balance

The internal balance was calibrated by a procedure described by Galway (1980) for multi-component strain gauge balances. Galway's generalized calibration procedure determines the sensitivity matrix of the balance by applying a variable load to the balance so that all components, i.e. both flexures in this case, are loaded simultaneously. This allows the selection of the calibration loading spectrum to be representative of the loading expected under test conditions. To influence the solution of the sensitivity coefficients for the flexures, a large number of loading cases can be used.

For the formulation of the balance equations used in this calibration procedure and the optimized solution for the balance sensitivity coefficients, reference should be made to Galway (1980). Since a two-component balance (i.e. two flexures) was used in this investigation and a linear variation of strain gauge bridge output to applied load was expected, the size of the sensitivity matrix is reduced. From Galway, the resulting sensitivity matrix and its solution is as follows:

$$[C] = \begin{bmatrix} C_{fY} & C_{fN} \\ C_{aY} & C_{aN} \end{bmatrix} = [a] [f]^T ([f] [f]^T)^{-1} \quad (3.1)$$

where the contents of matrix $[C]$ are the sensitivity coefficients: the first row contains the forward flexure (subscript "f") sensitivity coefficients for the side force (subscript "Y") and the yawing moment (subscript "N"); likewise for the aft flexure (subscript "a") sensitivity coefficients, in the second row. Each row in matrix $[a]$ contains the slopes from the straight-line fitting of bridge voltage output against calibration loads, applied at a known location, for each flexure; each corresponding row in matrix $[f]$ contains the constant calibration load ratios. More details about the content of $[a]$ and $[f]$ are given later in this section. The sizes of $[a]$ and $[f]$ are governed by the number of calibration load cases, i.e. the number of points at which the calibration loads were applied. An overdetermined system results from a large number of calibration load cases, however, Equation 3.1 determines the optimum

solution for the sensitivity coefficients using a least squares fit.

The calibration load spectrum was defined by a constant maximum bending moment and the expected range of the center of pressure (C_P). Emphasis was placed on ensuring the maximum load was applied between the flexures. The maximum applied load was determined from a design bending moment of approximately 385 lb-in. and the center of pressure location. The range of C_P location was estimated by surveying the water-tunnel data presented by Alexan (1992). Twenty data points were surveyed, involving typical test conditions with zero or non-zero sideslip, blowing or no blowing, and angles of attack from 10° to 50° . The furthest aft C_P was found to be 23.3 inches behind the apex of the nose of the wind-tunnel model (7.4 inches behind the sting/model interface); the C_P closest to the apex was 12.6 inches behind it (3.3 inches forward of the interface); and the average C_P location nearly coincided with the forward flexure. The findings of this survey were used to size a calibration model which enables calibration loads to be applied at known locations.

The calibration model is shown mounted on the sting in Figure 3.9. The model is 12 inches long, and is bolted to the sting via a steel flange and aligned on the balance mounting sleeve with a steel dowel pin. Spaced evenly around the circumference of the cylinder, four lines of tapped holes, or loading points, allow side forces and normal loads to be applied. Weights carried in a pan provide the load; the pan is suspended by a twisted steel cable which is supported by a brass screw threaded into one of the tapped holes in the calibration model. Positive or negative calibration side forces were applied at 11 stations along the model, as shown in Figure 3.9. These stations have a one-inch spacing. By rolling the calibration model 90° in either direction, four points were available to apply positive or negative normal loads to the flexures.

To minimize errors during the calibration, it was necessary to ensure the calibration model remained level. The base of the support strut was shimmed to provide a level model; and, as shown in Figure 3.9, an additional support for the sting was

placed behind the reinforcing collar so as to prevent the sting from deflecting under load.

Twenty-two side force calibration load cases were performed, involving both positive and negative orientations. In each case, the balance was loaded, typically in 10 lb increments, to the maximum permissible limit, then unloaded incrementally; forward and aft flexure voltages were recorded for each increment. All readings were adjusted for tare. For each flexure, the corresponding loading and unloading output voltages were averaged and normalized by the excitation voltage, then plotted against the corresponding calibration load. Using a discrete least-squares approximation, a straight line was fitted to the flexure calibration curves to find the slopes, i.e. the coefficient, a_j , for each flexure where "j" is the calibration load case. In all cases, the correlation coefficient was approximately unity, indicating a good fit. Thus matrix $[a]$ was formed.

In the constant load-ratio matrix $[f]$, the first column is always unity because it is a ratio of the applied calibration load and itself. The second column is the ratio of applied moment and the load, resulting in a moment arm. The applied moment was determined with respect to a resolving center on the calibration model, arbitrarily chosen to be 7.5 inches from its front end, or 0.84 inch behind the front flexure. Hence the second column of $[f]$ contains the location of the applied load relative to the resolving center.

Using the results from the side force calibration, Equation 3.1 was solved (see Appendix B). The inverse of the sensitivity matrix $[C]$ was determined to be:

$$[C]^{-1} = \begin{bmatrix} -98.375 & 98.342 \\ 215.868 & 84.043 \end{bmatrix} \quad (3.2)$$

where the coefficients in the first row have units of lb/mV/volt excitation; in the second row, the units are lb-in/mV/volt excitation. Thus the side force and yawing moment were determined by multiplying Equation 3.2 by the vector of flexure output

signals.

To check the accuracy of the calibration, arbitrary known loads were applied to the internal balance and measured. The errors in the forces and moments calculated by the data acquisition system were on the order of 0.5%. The balance was virtually insensitive to normal loads, e.g. forces due to lift. It was estimated that the forward and aft flexures were, respectively, 580 and 410 times less sensitive to normal force than side force.

The balance was calibrated with full installation wiring, including connectors and leads. Precision voltmeters were used to measure the output voltages of the flexures and to monitor the excitation voltage.

3.5.2 Pressure Transducers

The outputs of the three IC Sensor differential pressure transducers were calibrated over a range from 0 to 50 psig. A dead weight pressure calibration unit was used for calibration over the entire pressure range, and a one-meter U-tube water manometer was used to perform a calibration over a range from 0 to 1 psig. Data points from both calibrations were superimposed and a straight line fit to the data was made for each pressure transducer.

For both calibration ranges, a transducer was incrementally loaded and unloaded. The output signals recorded for each load increment were corrected for tare, then corresponding load and unload corrected output signals were averaged (the differences between these output signals were typically on the order of a few microvolts, indicative of a slight hysteresis; some larger differences were attributed to voltmeter drift). The results from the small and large calibrations for each pressure transducer, were plotted together on a graph of average output signal against applied pressure, Figure 3.10, and a straight line was fitted to each data set using a least-squares approximation. A correlation coefficient of approximately 1.0 was obtained for all fits. The sensitivity of the pressure transducers was approximately 0.90 mV/psig,

which is consistent with the manufacturer's specification of 1.0 mV/psig.

The transducers were calibrated with full installation wiring, including connectors and 20-foot leads used for connecting each transducer with the data acquisition system. Each transducer was excited by 5 volts DC. Precision voltmeters were used to monitor the excitation voltage and the transducer output signal.

The error in the calibrated pressure applied by the water manometer was negligible at ± 0.002 psig. For the dead weight pressure unit, the error in the calibration pressure was less than +0.2%.

During testing the supply pressure working range was approximately from 0.5 to 35 psig. Most test runs, however, were conducted with a supply pressure of approximately 3 psig.

3.5.3 Nozzle Flow Rate

To control the nozzle blowing momentum, the volume flow rate of each nozzle was calibrated against the output signal of its upstream pressure transducer. Because the airflow leaving the nozzles can be assumed to be incompressible, the volume flow rate can be incorporated into the expression for coefficient of blowing momentum and be readily determined for a desired blowing coefficient.

Displacement of water was used to measure the volume flow rate from the nozzles. A schematic of the apparatus is shown in Figure 3.11. The efflux of the nozzle is captured by Tygon tubing, forced over the nose of the model; carried by a tube, the flow exhausts into a cavity in the top of an inverted pail. The accumulation of air forces water out the bottom of the inverted pail, through a notched wood base, into a larger pail. Lines inscribed on the inside wall of the larger pail indicate displaced volume. The volume was calibrated by filling the apparatus (with the inverted pail, base, stand, and supply tube in situ) with 0.25 ft³ increments of water, which were determined by weight.

For the calibration of the nozzle flow rates, the apparatus was filled with water to a level that filled about two-thirds of the inverted pail. To measure the flow rate through the nozzle, at a given indicated regulator pressure, the time for the water level in the apparatus to displace the equivalent of 0.5 ft^3 was observed along with the output signal of the corresponding pressure transducer (a volume of 0.5 ft^3 was equivalent to a displacement height of approximately 12 inches). In this manner, nozzle flow rates were determined at 5 psig (indicated) increments to a maximum of 50 psig. In Figure 3.12, the volume flow rates are plotted against transducer pressure – the calibration results for the nozzles are in good agreement with each other. An uncorrected calibration curve is fitted to the data for the starboard nozzle. According to this calibration curve, the restrictor is choked, i.e. the curve becomes linear, at a static pressure of approximately 13 psig. This is reasonably consistent with the pressure ratio of 1.89 for choking of isentropic air flow. Because the column of displaced water volume imposes a back pressure on the nozzles, the calibrated volume flow rate will be less than the volume flow rate without back pressure. In the wind-tunnel experiments back pressure was expected to be approximately zero, necessitating a correction to the blowing system calibration in order to obtain true C_μ values. The correction calculations are presented in Appendix C. The correction decreases with increasing transducer pressure, i.e. with increasing C_μ and/or Reynolds number. Figure 3.12 includes a corrected calibration curve. Unless otherwise noted, all C_μ values in this thesis are the corrected values.

The complete blowing system was installed in the nose of the model for the calibration. Manual valve control was provided by an electronic control unit adapted from Alexan's water-tunnel rig (for the water-tunnel experiments, this device provided the frequency control for a single three-way valve). Precision voltmeters were again used to monitor the excitation voltage and output signal of each pressure transducer.

3.6 Data Acquisition System

The data acquisition system was provided by the Applied Aerodynamics Laboratory of the Institute for Aerospace Research. Developed by the laboratory, the system is proven, having been used in dynamic wind-tunnel experiments involving high roll or pitch rates of a test model at high subsonic Mach number. Changes to hardware and modification of software were not extensive because of the relatively simple requirements of the tests performed in this investigation.

3.6.1 Hardware

The data acquisition system is a MS-DOS based, real-time system with a maximum throughput of 20 kHz. The hardware consisted of a micro-computer and associated electronic equipment, consolidated in a rack. The micro-computer is based on the Intel 80486 processor, operating at 33 MHz; an internal high-capacity hard disk provides on-line storage of data files. The system also contains an A/D converter board which has 12-bit resolution and can accommodate up to 16 data acquisition channels. The electronics rack carries items such as signal filters, amplifiers, oscilloscopes, DC power supplies, and devices required for the internal calibration of the system.

Eight acquisition channels were allocated, permitting a maximum sampling rate of 2,500 samples per second per channel. Seven channels were required for the acquisition of data:

- valve control port side;
- valve control, starboard side;
- internal balance, forward flexure;
- internal balance, aft flexure;
- pressure transducer, port side;
- pressure transducer, starboard side;

- pressure transducer, manifold.

The eighth channel was not used. Anticipating susceptibility to noise and attenuation, the channels of the internal balance were passed through an isolation pre-amplifier to boost the output signals. The output voltages of the pressure transducers were sufficiently high to bypass the pre-amplifier.

Modifications to the hardware of the data acquisition system involved the installation, in the computer, of an expansion card containing a timing counter; and the addition of a valve control unit. The timing counter device performs the task of generating and synchronizing TTL valve control signals, thereby allowing the computer to control the valves and acquire data simultaneously. The counter runs independently of the computer after the computer has configured it to produce a desired control waveform. The valve control unit is an amplifier that translates the TTL signals from the counter, to control the solenoid-actuated valves. The amplification was accomplished by the relay action of a field-effect transistor. The unit contained two transistor circuits, one for each valve. Two input modes were available for each valve: in automatic mode, the valve was driven by the timing counter; in manual mode, the closing of a momentary switch opened the valve.

3.6.2 Software

The programming for the data acquisition system is extensive. Hence the objective of this section will be to outline the program, describe the process of averaging acquired data signals, and to highlight changes that were implemented for adapting the software for this investigation.

The scope of the data acquisition system software is indicative of its major functional groupings. These include configuring the data acquisition system, acquiring test parameters and data, performing post-processing (such as generating "load" files), and controlling hardware (such as the valves in the blowing system). The various functions within these groups are coordinated by a graphical user interface

which permits an operator to call a function by a menu selection or a key request.

A key element of the data acquisition function is the process of averaging the acquired data signals. The acquired signals are synchronously ensemble-averaged with the time base of the forebody vortex switching frequency. The maximum number of samples available during one switching cycle of a valve is the result of the maximum sampling rate per channel, i.e. 2,500, divided by the valve frequency. The available samples are evenly distributed throughout the period of the valve cycle so that the occurrence of a sample is fixed at known points within the cycle period. The ensemble-average is the "running" average of the samples, over many cycles, taken at each known point of time within the cycle period. Over a large number of cycles the ensemble-averages will stabilize, however, the acquisition programming does not provide for an automatic termination of data acquisition when the averaged signals stabilize during a test run. Instead visual convergence, i.e. visually apparent steady-state, of the ensemble-average responses of the internal balance was used as the criterion for terminating a test run.

After completion of a test run, the ensemble-average response of a channel is written to an ASCII file in raw form, i.e. voltages. For each test condition, a "wind" and "tare" run are performed to produce two raw data files. As a post-processing activity, the wind and tare files are manipulated to extract the instrumentation response that is due to the aerodynamic component. Subsequently a "load" file is created that contains the response of each channel in engineering units.

Changes to the system software involved the addition of a waveform generation module for valve control. Also, the acquisition code was optimized so that it could provide real-time performance within the valve control cycle, at blowing switching rates as high as 35 Hz. As a consequence, this process set the maximum throughput at 20 kHz.

3.7 Flow Visualization

This section describes the techniques employed for off-surface and surface flow visualization.

3.7.1 Off-surface

To view the cross-section of the forebody vortex trajectories, a smoke-seeded flow was illuminated by a laser-light sheet. Generated by vapourizing kerosene, smoke was released into the freestream from a wand upstream of the model near the front of the test section. With each angle of attack, the smoke wand was positioned so that the smoke trail would be entrained into the forebody vortices, aft of the blowing nozzles.

Using a Spectra-Physics 5 Watt Argon-Ion laser, the light sheet was created by a head assembly which reflected the incident source beam through a diverging lens. The head assembly traversed along a track and was manually aligned so that the light sheet projected normal to the axis of the model, as best as possible. With the model at $\sigma = 50^\circ$, a sheet width of approximately one foot at the model was sufficient to illuminate the leeside flow field. The model was spray-painted black to minimize reflection of the light sheet.

The light sheet could be traversed through ten equally-spaced observation stations, labelled on the leeside of the model. As part of the Kodak ETKA PRO 1000 high-speed imaging system, an image-intensified high-speed video camera, operating at 250 frames per second, was used to capture the cross-section of the vortices at various longitudinal stations. The camera was located outside the test section approximately in front of the model so that its view was parallel to the model's longitudinal axis and looking down the vortex trajectories. To determine the response time of the forebody vortices to the nozzle blowing, a diode laser beam triggered synchronously with the port valve, illuminated a spot on the model that could be captured within the video frame at all attitudes of the model.

The laser source, diode laser, and traversing head assembly were located in the wind tunnel control room. Due to concerns of causing excessive bending stresses in the smoke wand, the freestream velocity in the test section was limited to 125 ft/s during off-surface flow visualization testing.

3.7.2 Surface

For surface flow visualization, a special fluorescent pigment mixed in oil was applied to a small region on the leeside of the nose tip. The pigment was a Dayglo product, Saturn Yellow. After experimenting with matching of oil viscosity to test section wind conditions, a Fisher Brand 19 mechanical pump fluid was selected to be mixed with the pigment. The amount of pigment mixed with a fluid was arbitrary but sufficient to make an opaque mixture. Using a small brush, the oil mixture was applied to a surface area that extended approximately 6 to 7 inches from the tip of the nose.

The pigment was illuminated by a bank of ultra-violet fluorescent tubes installed in the ceiling and floor fillets of the test section. The fluorescent tubes supplied a light intensity of 500 footcandles [Brown, 1986]. The evolution of the surface patterns was recorded on a super VHS tape with a Panasonic low-speed colour video camera fitted with a telephoto lens. Some black and white 35 mm photographs were also taken with a telephoto lens, using ASA 400 film without a flash.

3.8 Experimental Procedures

Three types of tests were conducted in this investigation: force measurements (i.e. resolving aerodynamic side force and yawing moment), off-surface and surface flow visualization. Of primary interest, the force measurement tests were more numerous. The experimental procedures followed for each type of test, are described in this section.

The majority of force measurements typically involved sweeping the model from

$\sigma = 30^\circ$ to 70° , in increments of 10° , at a fixed roll angle, blowing frequency and momentum, and test section velocity. The procedure for performing the sweep typically involved the following steps:

- The roll angle of the model was set using the digital inclinometer, mounted on the sting. Usually after a roll adjustment, the blowing system was manually tested to check for blockages potentially caused by "pinching" of Tygon tubing in the air supply line.
- With manual valve control, a desired coefficient of blowing momentum was set by adjusting the supply pressure to produce a specific voltage output from the starboard pressure transducer, as required by the calibration curve in Figure 3.12.
- The valve control parameters, such as frequency and duty cycle, were entered into the data acquisition system.
- With the wind on, an internal calibration of the data acquisition system was conducted after setting the gains to accomodate the highest balance outputs: typically these outputs were found at $\sigma = 50^\circ$ or 60° .
- The sweep usually began at $\sigma = 70^\circ$. Data acquired at a given angle of attack was considered as one test run and identified with a sequential number. Each test run was completed with the manual termination of data acquisition, as previously described in Section 3.6.2.
- As σ was decreased, adjustments were made to maintain constant test section velocity.
- Following the completion of a wind-on sweep, a sweep of corresponding tare runs (i.e. wind off) was conducted.

Load files were usually processed after a few sets of test runs were completed. Approximately 1,350 test runs were conducted. Blowing configurations, such as baseline

(i.e. no blowing), steady blowing, alternating blowing at different frequencies and duty cycles, were tested at various coefficients of blowing momentum. These blowing configurations were performed for model attitudes involving no sideslip (zero roll angle) and sideslip (non-zero roll angle); for the latter cases, the model was rolled to $\pm 20^\circ$ in 5° increments. A listing of the force-measurement test runs is available in PostScript form, and is stored with the test data at IAR/AAL. The listing provides details such as run identification, test section velocity, model attitude and blowing configuration.

About 330 sessions of off-surface flow visualization were conducted and recorded. These runs were typically carried out at $\sigma = 40^\circ$, 50° and 60° , at which asymmetric forebody vortices prevail. Only zero and negative roll angles (i.e. 0° , -10° , -15° and -20°) were investigated because the nozzles were then rotated upward and visible to the camera. At constant blowing coefficient, two blowing frequencies and three duty cycle combinations were investigated at test section velocities of 62 ft/s and 120 ft/s. For each test condition, the cross-sections of the forebody vortex trajectories were recorded at two stations along the length of the model; for some tests, the trajectories were recorded continuously as the laser-light sheet moved from the front to the rear of the model.

The surface flow visualizations were carried out at $\sigma = 50^\circ$, 60° and 70° , at the same roll angles and blowing coefficient as for off-surface visualizations. Test velocities went as high as 230 ft/s. Manual steady nozzle blowing was used because alternating blowing would have distorted the surface patterns. Each video recording included the evolution of the surface pattern as the velocity increased from the start of the test. About 30 sessions of surface flow visualization were conducted and recorded.

3.9 Data Reduction

Time-average side force and yawing moment coefficients were obtained by performing an arithmetic average of the ensemble-average coefficients over one blowing cycle. The center of pressure position, X_{CP} , that is the point of application of the time-average side force, was calculated as follows:

$$X_{CP} = \frac{\bar{C}_N}{\bar{C}_Y} l_{ref} \quad (3.3)$$

where $\bar{C}_N$ and $\bar{C}_Y$ are the time-average yawing moment and side force coefficients respectively, and l_{ref} is the overall length of the model. The datum for $\bar{C}_N$ and X_{CP} is the resolving center of the internal balance described in Section 3.5.1 and is located 18.42 inches behind the tip of the nose.

Plotting of the time-average coefficients against different abscissa, such as turntable angle, duty cycle parameter, and roll angle, was required. To expedite processing, a C program, called UTIL2, was written to read an ASCII input file that listed the abscissa and ordinate pairs for creating a desired graph – the abscissa is a real value and the ordinate is a test run identification number. For each abscissa, the program would access the load data associated with the run identification number, read the ensemble-average data, and calculate $\bar{C}_Y$ and $\bar{C}_N$, and X_{CP} . Each of these results, together with the corresponding abscissa, were written to separate output files. As a result, three ASCII files were created, each containing information for one graph, of either $\bar{C}_Y$, $\bar{C}_N$ or X_{CP} against the specified abscissa; these files are readily importable into a plotting package. UTIL2 can also be executed in batch mode so that several graphs can be processed conveniently.

A listing of the source code of UTIL2, an example of an input batch file, and the resulting output files are shown in Appendix D.

Chapter 4

Results and Discussion

4.1 Axis System and other Conventions

The axis system and sign conventions for the force measurements are shown in Figure 4.1. The axis system is the same as that used for the calibration of the internal balance, and conforms to the accepted right-handed (forward, starboard, and down) body-axis system used for aircraft stability analysis. Thus side force, Y is positive to starboard and produces a positive yawing moment about the vertical axis when acting forward of the origin. The sideslip angle, β is considered positive when the model is yawed to port. The origin of the axis system corresponds to the resolving center of the internal balance and is located 18.42 inches aft of the nose apex of the model.

According to Lamont and Kennaugh (1991), a non-zero sideslip orientation can be simulated by setting the model to a non-zero roll angle. Using this technique, a positive roll angle, ϕ (as shown in Figure 4.1) will synthesize a yawing of the model to the port side, hence a positive sideslip angle. Two coordinate systems result as a consequence of applying the technique: one system describes the model orientation with the turntable angle, σ (which Lamont and Kennaugh refer to as the “total incidence plane”) and roll angle, ϕ ; the other is the conventional body-axis system which describes the orientation in terms of angle of attack, α and sideslip angle, β . It is readily apparent that for $\phi = 0^\circ$ the two coordinate systems coincide, i.e. $\sigma = \alpha$. Otherwise simple geometric transformations, shown in Appendix E, relate the coordinate systems.

The sign convention for nozzle blowing is as follows: starboard blowing is con-

sidered to be a positive value while blowing from the port nozzle has a negative value.

The coefficients of blowing momentum, time-average side force and yawing moment, C_μ , $\bar{C}_Y$, $\bar{C}_N$ are based on freestream dynamic pressure, model base area and overall model length. In the figures, $\bar{C}_Y$ and $\bar{C}_N$ are indicated as "time-average" side force and yawing moment coefficient.

4.2 Zero Roll Results

The experimental results presented in this section are side force and yawing moment measurements with the model set to a roll angle of 0° , i.e. simulating zero sideslip.

4.2.1 Steady Blowing

Baseline (i.e. no blowing) and steady blowing data were obtained at three different Reynolds numbers. These Reynolds numbers correspond to the three conditions a two-dimensional cylinder experiences in a cross-flow: subcritical, critical, and supercritical. Based on experimental data available in Hoerner (1965) for a smooth cylinder in a cross-flow, the critical Reynolds number and that for the end of the transition to a turbulent boundary layer were considered to be approximately 3.5×10^5 and 5×10^5 respectively.

Contrary to expectation, a supercritical Reynolds number of about 8.5×10^5 corresponding to $V_\infty = 300$ ft/s, was difficult to achieve because the model was vibrating considerably without blowing at $\sigma = 50^\circ$. It is thought that the presence of shedding von Karman vortices may have been the cause of this excitation, considering that shedding typically occurs in the range $10^2 \leq Re_D \leq 10^7$ [White, 1986]. Based on an average Strouhal number (St) of 0.2 and the normal component of the velocity, the von Karman shedding frequency is estimated to be 104 Hz. It is possible that this shedding frequency is a harmonic of 25 Hz, the natural frequency estimated for the balance-model assembly. However, according to Degani and Zilliac (1988),

von Karman vortex shedding is apparent at lower St near the downstream portion of the model for $50^\circ \leq \sigma \leq 70^\circ$. Using their theoretical relation for Strouhal number, $St = \sin \alpha$, the estimated shedding frequency is revised to be 84 Hz. The relation generally overestimates the experimental results of Degani and Zilliac (1988), so the possibility remains that the shedding frequency is a multiple of the balance-model resonant frequency.

In order to maintain a supercritical Reynolds number, the test section velocity was reduced to 230 ft/s, corresponding to $Re_D = 6.50 \times 10^5$. This is considered to be marginally supercritical Reynolds number since it is close to the transition region. The estimated shedding frequency for this test section velocity and $St = 0.2$ is 64 Hz. The model did not vibrate at this velocity, suggesting that the vibrations at 300 ft/s were indeed due to a near-coincidence of the forcing frequency (i.e. the Strouhal frequency) with a harmonic of the balance-model resonant frequency.

Figure 4.2 shows the side force and yawing moment response to steady blowing through a single nozzle with $C\mu = 0.00066$, for a range of σ from 0° to 70° . The Reynolds number is 1.76×10^5 , corresponding to $V_\infty = 63$ ft/s. The baseline curve depicts the response without blowing.

The baseline data in Figure 4.2 clearly indicates an asymmetry in the forebody vortex pattern. As expected, between $\sigma = 0^\circ$ and 30° in Figure 4.2(a), near-zero side forces indicate the presence of a vortex-free flowfield (typically up to $\sigma = 10^\circ$) and a steady symmetrical flowfield (between $\sigma = 10^\circ$ and 35°) [Alexan, 1992]. The asymmetry begins to develop at $\sigma = 30^\circ$, increasing to a maximum $\bar{C}_Y$ at approximately $\sigma = 60^\circ$. Beyond 60° the response decreases with increasing σ . A change in sign of the baseline yawing moment data, Figure 4.2(b), indicates that the center of pressure, X_{CP} is moving towards the nose of the model.

At this Reynolds number, however, the data in Figure 4.2 for $C\mu = 0.00066$ clearly demonstrate that the forebody vortices can be readily controlled by the forward blowing technique. Port blowing always gives a positive force which begins

to increase with σ for $\sigma > 30^\circ$; $\bar{C}_Y$ reaches a maximum at approximately $\sigma = 50^\circ$, after which it has a decreasing trend through $\sigma = 70^\circ$. Varying in the same manner with σ , negative side forces are always generated by starboard blowing. Thus an important feature of the results shown in Figure 4.2(a) is that the port and starboard side force responses with blowing are nearly symmetrical. The symmetry is also reflected in the yawing moment, Figure 4.2(b), and is readily evident when it is observed that the change of sign in the yawing moment occurs at the same σ .

Figure 4.3 depicts the response to steady blowing at $Re_D = 3.53 \times 10^5$, corresponding to $V_\infty = 125$ ft/s. The coefficient of blowing momentum is lower at 0.00053. The baseline and steady blowing behaviours are markedly different than at the lower Re_D of Figure 4.2. The baseline side force increases continuously with σ . The side forces generated by steady blowing also increase with σ , are greater in magnitude than the baseline, but do not have the symmetrical characteristic previously observed. In Figure 4.3(b), the baseline yawing moment is relatively constant over the range of angle of attack. Similar to the side force response, the yawing moments due to steady blowing are asymmetric and increase with σ .

Figure 4.4 illustrates the response to steady blowing at a still higher Reynolds number of 6.50×10^5 . With a blowing coefficient of 0.00094, it is clear from these results that the blowing system is incapable of influencing the forebody vortices to provide the same desirable control behaviour demonstrated at $Re_D = 1.76 \times 10^5$ (Figure 4.2). The asymmetry in the baseline behaviour is different, possibly because turbulent flow conditions have locked the forebody vortices into a different orientation. Steady blowing appears to be effective between $\sigma = 30^\circ$ and 40° because moderate side forces are increasing with σ . However at $\sigma = 50^\circ$, the magnitude of the side force due to steady port or starboard blowing quickly decreases towards the baseline response. Thereafter the behaviour of the blowing responses is comparable to the baseline. Now it appears the nozzles are ineffective, unable to generate a positive side force for $\sigma > 50^\circ$. The nozzles were still unable to overcome the baseline asymmetry at a blowing coefficient as high as 0.0334, created by the maximum

available supply pressure of approximately 35 psig. The steady blowing responses at this maximum blowing capacity exhibited the same behaviour shown in Figure 4.4.

It is apparent from the test results presented in Figures 4.2 through 4.4 that the baseline and steady blowing responses have a strong Reynolds number dependency. At a subcritical Reynolds number, side force control is possible with steady blowing. For the model used in this test, this capability is lost at a critical and supercritical Reynolds number. While reasons for this behaviour are not clear, it has already been speculated that the nozzle locations are too far aft to be effective in influencing the three-dimensional separation.

In view of the results at the higher Reynolds number (i.e. at test section velocities of about 125 ft/s and higher) it was decided to conduct the remainder of the investigation at the low Reynolds number of 1.76×10^5 (i.e. test section velocity of about 63 ft/s). The declared objective of testing the technique of dynamically manipulating the forebody vortices at a high Reynolds number, was not achievable with the present model.

4.2.2 Minimum Blowing Coefficient

For the remainder of the investigation, testing was conducted at the minimum coefficient of blowing momentum (or minimum blowing coefficient). Recall from Section 2.2 that the minimum blowing coefficient represents the lowest blowing momentum at which significant side forces are generated. Below this coefficient the magnitude of the side force will decrease rapidly towards an asymmetric baseline value, thereby reducing yaw control effectiveness. Higher blowing momentum, on the other hand, produce relatively little gain in the magnitude of the side force. The effectiveness of the blowing is the ratio of the side force coefficient to blowing coefficient – obviously the effectiveness is a maximum when the blowing coefficient is at a minimum value as defined here.

Early in the test the minimum blowing coefficient was estimated to be about 0.00050. This estimate was obtained at $Re_D = 6.50 \times 10^5$ with the model set at $\sigma = 50^\circ$. The supply pressure was initially set to approximately 25 psig (indicated). Using alternating blowing at a frequency of 1 Hz and $\tau_1/T = 0.5$, the "raw" side force response was observed on an oscilloscope as the supply pressure was decreased (in spite of the behaviour shown in Figure 4.4, there was sufficient response to obtain full available side force). Significant decreases in side force response occurred slightly below a supply pressure of approximately 4.5 psig (indicated), which corresponds to $C_\mu \approx 0.00050$. This blowing coefficient was accepted as the minimum and several test runs were performed, such as those discussed in the previous section, under this presumption.

To be certain that $C_\mu = 0.00050$ was in fact the minimum blowing coefficient, tests were later conducted to document the variation of side force response with decreasing blowing rate. Turntable angles of 40° , 50° and 60° were examined at $Re_D = 1.76 \times 10^5$ with steady blowing. The results are plotted in Figure 4.5. The side force response to steady port and starboard blowing is near-symmetric, and at all turntable angles the side force behaviour is generally asymptotic for $C_\mu > 0.00050$. For all three turntable angles, $C_\mu = 0.00066$ was regarded as the minimum blowing coefficient. For this minimum, the blowing effectiveness is a ratio of about 5500 for $\sigma = 50^\circ$. These results represent an improvement over Alexan's water-tunnel findings by a factor of 2.0 for the minimum blowing coefficient, and over 2.75 for the blowing effectiveness.

It is interesting to note that the minimum blowing coefficient of about 0.00050, found at a supercritical Re_D (i.e. $Re_D = 6.50 \times 10^5$), is comparable to 0.00066 found at the subcritical Re_D . A search for the minimum blowing coefficient at $Re_D = 3.53 \times 10^5$ was also conducted but unfortunately the majority of the data was lost. From the results of a search performed at $Re_D = 2.82 \times 10^5$ (100 ft/s), shown in Figure 4.6, a minimum C_μ of 0.00066 is still apparent for both turntable angles. This Reynolds number, however, is not significantly higher than $Re_D = 1.76 \times 10^5$ so

similar results are to be expected. Nevertheless, these observations tend to suggest that the minimum blowing coefficient is relatively insensitive to Reynolds number.

4.2.3 Maximum Alternating Frequency

In Section 2.2 the maximum allowable frequency for alternating blowing was defined as the frequency which allows sufficient time for the forebody vortex pattern to react to a change caused by the blowing nozzles and momentarily reach a stable configuration before being switched back. In the water-tunnel investigation, Alexan applied this criterion for maximizing the alternating blowing frequency at a duty cycle parameter (τ/T) of 0.5, and found the maximum reduced frequency, ω^* to be 0.16.

Figure 4.7(a) and (b) show the wind-tunnel results of a search for the maximum alternating blowing frequency at $\tau_1/T = 0.5$. The search was conducted at $Re_D = 1.76 \times 10^5$ with the model oriented to $\sigma = 50^\circ$ and $\phi = 0^\circ$. Figure 4.7(a) depicts the time-history of C_Y (i.e. instantaneous C_Y) over one cycle of blowing at $\omega^* = 0.16$ (3.6 Hz). Figure 4.7(b) shows the time-history of C_Y at $\omega^* = 0.32$ (7.2 Hz). Superimposed on these traces is the controlling waveform for the port valve - a non-zero value indicates the valve is open. From these traces it is immediately observed that the C_Y responses oscillate about a mean non-zero value. The periods of these oscillations correspond to frequencies of approximately 22 and 23 Hz in Figure 4.7(a) and (b) respectively. It is apparent the rapid switching of the forebody vortices is exciting vibrations of the balance-model system, which has a resonant frequency of 25 Hz. Note that the forcing function (i.e. the alternating side force) is approximately a square-wave function so that its frequency spectrum is broad. That is, it is capable of providing excitation over a wide range of frequencies. The response of the balance-model system is unfortunate but unavoidable unless the balance is redesigned with a substantially higher stiffness.

Figure 4.7(a) indicates that the maximum reduced frequency found by Alexan is substantially lower than that found for this investigation. After reacting to the open-

ing of the port valve, the forebody vortices proceed through a change of orientation with sufficient time to reach the new positions and remain there for a substantial time, as indicated by the approximately constant instantaneous side force. In Figure 4.7(b) the reduced blowing frequency is higher, at 0.32. The response is still symmetric but the time that the forebody vortices remain in a steady-state is reduced to approximately 43 milliseconds, down from 104 milliseconds at the lower frequency of Figure 4.7(a). By Alexan's criterion, the maximum allowable frequency has still not been reached – a higher frequency would be required to reduce the interval during which the vortices linger in a steady-state, to approximately zero.

Alexan's criterion requires rethinking, however, because it does not account for the variation of the valve duty cycle. Figures 4.8 to 4.11 show the variation of the C_Y response, for $\omega^* = 0.16$ and 0.32, as the duty cycle parameter of the port valve is reduced from $\tau_1/T = 0.4$ to 0.1 (correspondingly, the duty cycle parameter of the starboard valve increases from 0.6 to 0.9). As the duty cycle is reduced, the forebody vortex pattern remains, for an increasing duration, in an orientation that generates a negative side force. It is evident from these traces that if the maximum allowable frequency is chosen with $\tau_1/T = 0.5$, then the optimized response is lost for $\tau_1/T \neq 0.5$; this is simply because the “on” time for one or the other nozzles is less than the maximum established with $\tau_1/T = 0.5$.

There is a certain aerodynamic lag time required by the forebody vortices to switch positions. When the “on” duration of a valve is less than this aerodynamic lag time, the vortices will not be permitted to reach the new positions because the other valve will open, tending to cause the vortex pattern to switch back. As a result, the side force response – due to the valve with low τ/T – will be “clipped” at a sufficiently high switching frequency. Clipping will affect the time-average of the side force and yawing moment, causing the magnitudes to increase above values that would otherwise be constant at lower alternating blowing frequencies. Put another way, if the frequency is very high, $\bar{C}_Y$ at low τ/T will approach $\bar{C}_Y$ for $\tau_1/T = 0$. This effect will cause non-linearities to appear in the variation of side force and

yawing moment with duty cycle parameter (discussed in detail in the next section). The non-linearity is expected to be predominant at low τ/T .

Hence a new method is proposed for detecting the maximum allowable frequency. With data acquired over a number of frequencies, $\bar{C}_Y$ is plotted against ω^* for a given τ/T . For constant τ/T , $\bar{C}_Y$ is expected to be constant with increasing ω^* until, at a sufficiently high frequency, $\bar{C}_Y$ will begin to increase as clipping occurs. The maximum allowable frequency will be established by the low τ/T curves because the non-linearities will appear in these data first. Unfortunately, the proposed test could not be tried in this investigation because data at another ω^* greater than 0.32 was not acquired.

4.2.4 Alternating Blowing

Up to this point, the results of the force measurements at low Reynolds number have, in a general sense, corroborated Alexan's water-tunnel findings. In addition to performing high Reynolds number testing, another point of departure from the water-tunnel study is the investigation of the effects on $\bar{C}_Y$ and $\bar{C}_N$ due to the variation of the valve duty cycle.

Figures 4.12 and 4.13 show the variation of $\bar{C}_Y$ and $\bar{C}_N$ with the port valve duty cycle parameter τ_1/T . The results are shown for the reduced alternating blowing frequencies of $\omega^* = 0.16$ and 0.32 respectively, at $C\mu = 0.00066$ and $Re_D = 1.76 \times 10^5$. The turntable angles considered were $\sigma = 30^\circ, 40^\circ, 50^\circ, 60^\circ$ and 70° . Recall that $\tau_1/T = 0$ indicates that the port valve is closed and the starboard valve is fully open throughout the blowing cycle; for $\tau_1/T = 1$, the state of each valve is the opposite.

In general the variation of $\bar{C}_Y$ and $\bar{C}_N$ is linear with τ_1/T at the two ω^* values investigated. At $\sigma = 30^\circ, 40^\circ$ and 50° the linearity of $\bar{C}_Y$ and $\bar{C}_N$ is very good throughout the range of τ_1/T for both $\omega^* = 0.16$ and 0.32 . $\bar{C}_Y$ is symmetric about the $\bar{C}_Y = 0$ axis, crossing at approximately $\tau_1/T = 0.5$. For $\omega^* = 0.16$, a slight non-linearity is observed in $\bar{C}_Y$ at $\sigma = 60^\circ$ and 70° in the vicinity of $\tau_1/T = 0.1$ and 0.9 .

For $\omega^* = 0.32$, the non-linearity of $\bar{C}_Y$ is more obvious at these attitudes. Apart from an aerodynamic cause, the nonlinearity is occurring where the duty cycles of the port and starboard valves are very small which may serve as evidence that the maximum allowable alternating frequency has been reached, in the manner described in the previous section, at these σ values. If this is the case, then $\omega^* = 0.32$ is estimated to be the maximum reduced alternating frequency for the present conditions.

The linear variation of $\bar{C}_N$ is very similar to $\bar{C}_Y$ results for both reduced frequencies at $\sigma = 30^\circ, 40^\circ$ and 50° . It does appear, however, that the $\bar{C}_N$ variation crosses the $\bar{C}_N = 0$ axis more precisely than $\bar{C}_Y$ - an exception occurs for $\omega^* = 0.16$ at $\sigma = 70^\circ$ where $\bar{C}_N$ readily appears to cross the axis at $\tau_1/T < 0.5$. Similar to the $\bar{C}_Y$ results, a non-linearity is appearing in the $\bar{C}_N$ data at low valve duty cycles at $\sigma = 60^\circ$. At $\sigma = 70^\circ$, the non-linearity is predominant, particularly for $\omega^* = 0.32$.

A linear variation of $\bar{C}_Y$ and $\bar{C}_N$ with duty cycle parameter has significant practical ramifications. Firstly, the yaw control laws at high angles of attack are simplified; and secondly, side force and yawing moment are readily controlled at a constant blowing supply pressure. Moreover, two independently controllable parameters are available to control the magnitude of the side force and yawing moment: blowing momentum and valve duty cycle. It is perceived that blowing momentum will be scheduled with aircraft speed to set the maximum available side force and yawing moment, and then the variation of valve duty cycle "regulates" this capacity to attain a desired sideslip attitude at a certain yaw rate.

4.3 Non-Zero Roll Results

The experimental results presented in this section are time-average side force and yawing moment measurements with the model rolled to simulate a sideslip condition. By rolling the model to $\phi = \pm 20^\circ$ by 5° increments, a total of eight sideslip conditions were created. In Table E.1 of Appendix E, these model orientations are shown in the total incidence plane, described by σ and ϕ , and in the conventional body-axis

system described by α and β . To be consistent with the discussion in Section 4.2, the model orientation will continued to be described in the total incidence plane.

4.3.1 Steady Blowing

Figures 4.14 through 4.17 present the results for $\overline{C}_Y$ and $\overline{C}_N$ for steady blowing with $C\mu = 0.00066$ and $Re_D = 1.76 \times 10^5$, over the range $30^\circ \leq \sigma \leq 70^\circ$, and up to a maximum roll angle of $\phi = \pm 20^\circ$. Each figure depicts the results from roll angles of similar magnitude for the purpose of illustrating the level of symmetry of the responses.

By viewing Figures 4.14 through 4.17 successively, it will be readily observed that the steady blowing and baseline responses for $\overline{C}_Y$ and $\overline{C}_N$ gradually depart from the symmetry about $\overline{C}_Y = 0$ and $\overline{C}_N = 0$, seen in Figure 4.2 for $\phi = 0^\circ$. Consider the responses at $\phi = 5^\circ$ in Figure 4.14(a). There is a noticeable downward shift or displacement of the baseline and steady blowing responses compared to those for $\phi = 0^\circ$, indicating a reduction in positive $\overline{C}_Y$ and $\overline{C}_N$ but an increase of these quantities in the negative direction. At $\phi = 5^\circ$ there is still some semblance of symmetry in the shape of the steady blowing responses for $\overline{C}_Y$ and $\overline{C}_N$, but the apparent axis of symmetry is now non-zero. As would be expected, the steady blowing responses for $\overline{C}_Y$ and $\overline{C}_N$ at $\phi = -5^\circ$, shown in Figure 4.14(b), are near-mirror images with the responses at $\phi = 5^\circ$ (however this is not the case with the baseline responses). With increasing positive roll angles, the steady blowing responses continue to shift downwards, further degrading positive $\overline{C}_Y$ and $\overline{C}_N$ while enhancing negative $\overline{C}_Y$ and $\overline{C}_N$. It is interesting to note that the shape of the $\overline{C}_Y$ and $\overline{C}_N$ responses to port and starboard blowing appear to remain unchanged, respectively, with $\phi > 0^\circ$ and $\phi < 0^\circ$. However, the corresponding responses have slightly altered shapes, for $|\phi| \geq 10^\circ$, because $\overline{C}_Y$ seems to be fixed at a value of approximately $|2.0|$ when $\sigma = 40^\circ$. Presently there is no explanation for this behaviour.

The change in the side force and yawing moment responses are not surprising after considering the change of orientation the forebody vortex system must un-

de-go in a sideslip condition. Recall that a positive roll of the model simulates a positive sideslip, i.e. yawing the model to port. Sideslip alters the orientation of the freestream velocity vector thereby causing the forebody vortex system to effectively rotate about the longitudinal axis of the model. Since the nozzles are fixed to the model, the primary separation line on the starboard side rotates closer to the starboard nozzle allowing the efflux from the nozzle to more effectively influence the three-dimensional separation of the flow and give rise apparently to a larger magnitude of side force and yawing moment. Conversely the port nozzle becomes less effective as it moves further away from the port side separation line with increasing positive sideslip.

The effect of ϕ (i.e. β) on $\bar{C}_Y$ is illustrated in Figure 4.18. The results presented are for steady blowing with $C\mu = 0.00066$ at $\sigma = 50^\circ$ and $Re_D = 1.76 \times 10^5$. For $\phi < 0^\circ$ ($\beta < 0^\circ$), positive $\bar{C}_Y$ increases while negative $\bar{C}_Y$ diminishes in magnitude; for $\phi > 0^\circ$ ($\beta > 0^\circ$), the effect is the opposite. Hence for this model, side force capability is sensitive to sideslip. By contrast, Roos and Magness (1993) have demonstrated a side force capability that is insensitive to sideslip over a comparable range. In addition to improving performance at higher Reynolds number, it is thought that locating the nozzles closer to the apex of the nose will also improve side force capability with sideslip because the nozzles will be in closer proximity to the primary separation lines over a broader range of sideslip.

4.3.2 Alternating Blowing

Figures 4.19 through 4.22 present the results for $\bar{C}_Y$ and $\bar{C}_N$ for alternating blowing at $C\mu = 0.00066$ and $\omega^* = 0.32$, up to a maximum roll angle of $\phi = \pm 20^\circ$. For positive roll angles, results are presented for the entire range of σ from 30° to 70° , however due to time limitations, data for the negative roll angles was acquired only for $\sigma = 40^\circ, 50^\circ$ and 60° . Nevertheless, the latter results are useful for exemplifying the near-symmetry of responses between roll angles of the same magnitude.

Similar to the steady blowing results discussed in the previous section, the $\bar{C}_Y$

and $\bar{C}_N$ responses shift downwards, for example, as the roll angle is increased in the positive direction, causing negative $\bar{C}_Y$ and $\bar{C}_N$ to increase in magnitude while positive $\bar{C}_Y$ and $\bar{C}_N$ decrease. When the model is rolled in the negative direction the responses are shifted upwards and the magnitude effects are the opposite. The $\bar{C}_Y$ and $\bar{C}_N$ responses at $|\phi| = 5^\circ$, Figure 4.19, appear to be comparable to the responses for $\phi = 0^\circ$, shown in Figure 4.13. Of course at $|\phi| = 5^\circ$, $\bar{C}_Y$ and $\bar{C}_N$ are symmetrical about a non-zero value, but the linear variation of $\bar{C}_Y$ and $\bar{C}_N$ with τ_1/T is retained to a substantial extent for $30^\circ \leq \sigma \leq 60^\circ$ - a non-linearity is noticeable, however, in the responses for $\sigma = 70^\circ$ in Figure 4.19(a). The $\bar{C}_Y$ responses intersect approximately at $\tau_1/T = 0.5$; but the $\bar{C}_N$ responses intersect precisely, as in Figure 4.13(b) for $\phi = 0^\circ$, though the intersection occurs at slightly more than $\tau_1/T = 0.5$. It seems that responses similar to those at $\phi = 0^\circ$ are limited to small roll angles.

As Figures 4.19 to 4.22 are viewed successively, it can be seen that not only are the $\bar{C}_Y$ and $\bar{C}_N$ responses further displaced from the symmetrical appearance at $\phi = 0^\circ$, but the individual $\bar{C}_Y$ and $\bar{C}_N$ responses for each σ are spreading apart from each other as ϕ increases in magnitude. That is, the single intersections of responses for $\phi = 0^\circ$ and $\pm 5^\circ$ are not seen at $|\phi| \geq 10^\circ$. This is not surprising considering the fact that steady blowing gives rise to the largest magnitude of $\bar{C}_Y$ and $\bar{C}_N$ at $\tau_1/T = 0$ and 1.0, and varying the duty cycle merely controls the responses between the two maximums, generally in a linear fashion. So if the steady blowing limits become unsymmetric with increasing ϕ , as described in Section 4.3.1, then it follows that the variation of $\bar{C}_Y$ and $\bar{C}_N$ must also become unsymmetric. Again, it is anticipated that relocating the nozzles closer to the tip of the nose will improve the variation of $\bar{C}_Y$ and $\bar{C}_N$ with τ_1/T and restore the responses to the appearance of the results for $\phi = 0^\circ$.

4.4 Flow Visualization

4.4.1 Off-surface

A considerable amount of smoke-visualization imagery was acquired during this investigation. While the images generally verified the response of the forebody vortices to the alternating blowing from the nozzles, it is unfortunate that images could not be obtained for the high speed case, i.e. 230 ft/s. While the results from force measurements at this velocity (Figure 4.4) indicate an inability to influence the vortices, it would have been of interest to verify that the vortices in fact remain in an asymmetry pattern, or fluctuate slightly in these patterns, while blowing was on.

Images taken with a test section velocity of 62 ft/s indicate that the forebody vortices are well-formed for $\sigma = 40^\circ$, 50° and 60° . The images also show that the vortices detach farther from the surface of the model with increasing σ . For $\omega^* = 0.32$ (7.2 Hz), the vortices respond well to the alternating blowing although a lag time in the response to the blowing disturbance is apparent (this delay is due in part to the time required for air to travel from the valve to the nozzle opening). At $\sigma = 70^\circ$, the flow close to the leeside surface of the model appears as wake-like while large vortices form farther off the surface. Figures 4.23 and 4.24 are two images of the forebody vortices with the model at $\sigma = 50^\circ$. The laser light sheet is illuminating the model near the base of the nose. In Figure 4.23 the port valve is open (indicated by the isolated white dot on the right side of the image) and the port vortex is detached from the surface of the model, giving rise to a positive side force. In Figure 4.24 the starboard vortex is still detached from the surface after the port valve opened approximately 8 milliseconds earlier. In Figure 4.24, a negative side force is generated.

At a test section velocity of 120 ft/s, images were only taken at $\sigma = 40^\circ$, 50° and 60° . At this speed, the behaviour of the vortices was comparable to that at 62 ft/s. However, two differences were observed about the form of the forebody vortices: at

$\sigma = 60^\circ$, the radius of the vortices appeared to be much larger than at 62 ft/s; and for $\sigma \geq 50^\circ$, the "low" vortex appeared to be almost detached from the surface, unlike the low vortices shown in Figures 4.23 and 4.24.

4.4.2 Surface

The surface flow visualization results were useful for observing the location of the primary and secondary lines, and the effects of blowing on the separation lines with the model in sideslip and non-sideslip attitudes.

A separation line is the locus of points at which a boundary layer separates from a surface. On a slender forebody at high angles of attack, the windward boundary layer will separate and give rise to so-called primary vortices that form on the leeside of the nose. Consequently a primary separation line is formed on the surface of the nose, appearing like a meridian that originates at the apex of the nose. Flow from the primary vortex reattaches on the leeside of the nose, eventually separating and forming a smaller secondary vortex beneath the primary vortex. Hence a secondary separation line is formed [Rom, 1992].

Figures 4.25 and 4.26 show photographs of the surface visualizations with the model at $\sigma = 50^\circ$ and $\phi = 0^\circ$ for a test section velocity of 120 ft/s. Figure 4.25 depicts a no-blowing condition. The primary separation lines are identified by the thick band of accumulated oil and the secondary lines by the thin band. The primary separation line is estimated to be located between 100° and 110° from the windward meridian, and the secondary line is at about 145° . Each nozzle is located between a primary and secondary separation line. Figure 4.26 shows the effect of blowing through the port nozzle with $C_\mu = 0.00053$. The port side primary and secondary separation lines seem unaffected between the nozzle opening and the apex of the nose. Behind the port nozzle opening, the primary separation line is distorted and the secondary line appears severed.

Figures 4.27 and 4.28 show the surface visualization photographs with the model

at $\sigma = 50^\circ$ and $\phi = -10^\circ$ for $V_\infty = 62$ ft/s. For the no-blowing condition, Figure 4.27, it seems the locations of the separation lines are about the same as shown in Figure 4.25. The effect of a negative sideslip is apparent. The port primary separation line moves closer to the port nozzle opening while the starboard line correspondingly moves farther away. Note that the starboard nozzle opening coincides with the starboard secondary separation line. Figure 4.28 shows a significant effect of blowing through the port nozzle with $C_\mu = 0.00066$: the entire visible length of the port primary separation line is distorted, while a large break in the secondary separation line appears after the port nozzle. The separation lines on the starboard side appear to be unaffected.

Chapter 5

Conclusions

This investigation has verified the findings of the initial water-tunnel experiments and confirmed the potential of dynamic forebody vortex manipulation as an alternate approach for a fighter aircraft to maintain post-stall directional control when conventional means are ineffective.

Wind-tunnel experiments have shown that the controlled manipulation of the forebody vortex pattern is possible with a minimum coefficient of blowing momentum of 0.00066 with $Re_D = 1.76 \times 10^5$ ($V_\infty = 63$ ft/s). Results also seem to indicate that the minimum C_μ is relatively insensitive to Reynolds number. The minimum C_μ found in this investigation is less than the water-tunnel findings by 2.0 times, and the blowing effectiveness is also an improvement by a factor of about three.

At 0.32, the maximum reduced frequency of alternating blowing was found to be approximately twice that observed in the water-tunnel experiments. The maximum was not established by the criterion used by Alexan in the water-tunnel investigation because it did not take into consideration the variation of the duty cycle. To account for this, another technique was proposed which involves the detection of a non-linearity in the variation of time-average side force and yawing moment with the duty cycle parameter. The non-linearity is expected to initially appear at low τ/T . Although insufficient data was available, a maximum ω^* was estimated by this method. Further investigation will be required to confirm the validity of the proposed criterion for finding the maximum reduced frequency of alternating blowing.

The key finding of this investigation was that time-average side force and yawing moment vary linearly and symmetrically with the duty cycle of the valves. At zero sideslip, this variation was clearly evident; at non-zero sideslip angles, linearity is maintained but the symmetry of the variation is about increasing non-zero values of $\bar{C}_Y$ and $\bar{C}_N$. Another significant finding was that the time-average side force capability of the model was sensitive to sideslip; that is, for example, as the model continues to yaw to port, the side force directed towards the port side increases while the side force directed towards the starboard (which would be the restoring force) decreases. It is anticipated that a more forward location for the nozzles will cause the side force and yawing moment capability to be insensitive to sideslip.

An inappropriate location for the nozzles is also suspected of limiting the effectiveness of the blowing system at higher Reynolds number. At $Re_D = 6.50 \times 10^5$ ($V_\infty = 230$ ft/s) with a blowing coefficient that was twice the minimum, a positive side force could not be generated above an angle of attack of 40° . It is thought the nozzles are located too far aft to be effective in influencing the separation of the boundary layer at high speed. Moreover, with the present nozzle location, it is apparent that the baseline and steady responses of time-average side force and yawing moment have a strong dependency on Reynolds number. It is hoped that this dependency reduces as the nozzle location move towards the apex of the nose.

Chapter 6

Recommendations for Future Work

One of the main objectives of this work was to determine whether the technique of dynamic manipulation of forebody vortices will perform at high Reynolds number. The technique could not be investigated as planned for two reasons: first, the flexure stiffness of the internal balance was inadequate to prevent the model from vibrating at a test section velocity of 300 ft/s; second, it is believed that the nozzle locations are too far aft to effectively influence the separation of the boundary layer at high speed.

6.1 Flexure Design

To increase the bending stiffness, the second moment of inertia of the flexures must be increased. The sensitivity to side force will decrease but it is expected to remain acceptable. The output signals from a stiffer balance will be lower but the use of the isolation amplifier will ensure the integrity of these signals is not affected by electrical noise.

Modifying the existing sting-balance assembly, to eliminate the oscillation in the side force response, seems unlikely. An obvious means of damping the oscillation would be to place an insert of damping material between the aft skirt of the model and the sting. The insert, however, will serve as an additional support for the model, causing a redistribution of the reaction load, i.e. the balance will sense less side force. Consequently a redesign of the balance is unavoidable.

6.2 Nozzle Design

With a forward-blowing scheme, nozzle design involves locating the nozzles and sizing the internal diameter. The suggestion has already been raised that the nozzles be moved towards the apex of the nose. For example, this could involve moving the nozzles along their present meridians, which will in turn move the nozzle exits closer to the primary separation line. It is anticipated that blowing effectiveness will improve, particularly at higher Reynolds number. Moreover a forward location should make the blowing effectiveness insensitive to sideslip.

As the nozzles are moved forwards, the diameter of the current nozzles becomes too large for the nozzles to be accommodated in the narrow confines of the nose tip. Reducing the diameter of the nozzles will be necessary.

Further improvements to blowing effectiveness and insensitivity to sideslip may be realized by optimizing the nozzle orientation (e.g. normal or tangential to the surface). For such an investigation, a model with an interchangeable nose tip design will be necessary.

6.3 Data Acquisition System

The data acquisition system used in this investigation required manual operation. Automating the acquisition of data relieves an operator of labourious tasks within the process and shortens testing time. Some suggestions for automation in future investigations are made:

- Terminate the ensemble-averaging process automatically. Presently the criterion for stoppage is visual convergence of an ensemble-average waveform. A stopping algorithm should be incorporated in the data acquisition code. One approach could be to store the sample variance for each sample point. Like the ensemble-average, the sample variances will stabilize over a large number of cycles. Convergence will have occurred when the sample variances, or maybe a majority of the variances, have fallen below a certain threshold value.

Whatever the approach, the sampling rate is expected to decrease as a result of including a stopping algorithm.

- Modify the software to allow for a 100% duty cycle, i.e. for steady blowing. Presently the valve control unit must be operated in manual mode to simulate steady blowing through a nozzle.
- Establish hardware links to the wind tunnel facility. This will enable the data acquisition system to control the turntable angle and obtain test section conditions such as freestream velocity, dynamic pressure and Reynolds number.

Cost permitting, the incorporation of a motorized roll mechanism in the strut support would complete the automation of the rig. The roll mechanism would be controlled by the data acquisition system, allowing it complete control of the model attitude.

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Figure 1.1: Asymmetric bi-stable vortices appearing at $\alpha \geq 30^\circ$. *Reproduced from Figure 2.33 of Rom(1992).*

Figure 1.2: Concept of the dynamic manipulation of forebody vortices.

Figure 2.1: Schematic of water-tunnel model. *Reproduced from Figure 1 of Alexan et al. (1994).*

Figure 2.2: Side force versus blowing coefficient, $\alpha = 50^\circ$, $\beta = 0^\circ$, $Re_D \approx 7000$. *Reproduced from Figure 3 of Alexan et al. (1994).*

Figure 2.3: Anticipated effect of blowing in inducing asymmetric flow on the blunted tangent-ogive forebody. *Reproduced from Figure 1 of Roos and Magness (1993).*

Figure 2.4: Side force effects from blunting a 3.5D tangent-ogive. *Reproduced from Figure 8 of Roos and Magness (1993).*

Figure 2.5: Typical side force response to steady nozzle blowing for the blunted tangent-ogive with laminar separation. *Reproduced from Figure 10 of Roos and Magness (1993).*

Figure 2.6: Effectiveness of nozzle blowing on tangent-ogive at $\alpha = 46^\circ$ for three Reynolds numbers. *Reproduced from Figure 13 of Roos and Magness (1993).*

Figure 2.7: Comparison of blowing effectiveness for laminar and turbulent separation at $\alpha = 50^\circ$ and 55° . Reproduced from Figure 2.4 of Roos and Magness (1993).

Figure 2.8: Yawing moment and side force response to steady blowing on the blunted tangent-ogive with turbulent separation. *Reproduced from Figure 21 of Roos and Magness (1993).*

Figure 2.9: Demonstrating side force control with steady blowing at $\alpha \approx 50^\circ$ and 55° with turbulent separation. *Reproduced from Figure 22 of Roos and Magness (1993)*

Figure 2.10: Yaw effect on side force capability at $\alpha = 50^\circ$ with laminar and turbulent separation. *Reproduced from Figure 28 of Roos and Magness (1993).*

(a) Balance-model assembly.

(b) Strut-sting-model assembly.

Figure 3.8: Frequency response of balance-model and strut-sting-model assemblies.

Figure 3.2: Wind-tunnel test section with test model and support installed.

Figure 3.3: Cross-section of model.

Figure 3 4: Comparison of nose profiles from the water and wind-tunnel models.

Figure 3.5: Cross-section of nose assembly.

Figure 3.6: Two-component internal balance.

(a) Typical full-bridge layout

(b) Actual wiring layout

Figure 3.7: Strain gauge wiring scheme for one flexure.

(a) Balance-model assembly.

(b) Strut-sting-model assembly.

Figure 3.8: Frequency response of balance-model and strut-sting-model assemblies.

Figure 3.9: Calibration model.

Figure 3.10: Calibration curves for pressure transducers.

Figure 3.11: Schematic of the setup for measuring volume flow rate through the nozzles.

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Figure 3.12: Calibration curve for nozzle volume flow rate.

Figure 4.1: Axis system and sign convention.

Figure 4.2: Time-average side force and yawing moment versus turntable angle for steady blowing, with $\phi = 0^\circ$, $C\mu = 0.00066$, $Re_D = 1.76 \times 10^5$ ($V_\infty = 63$ ft/s).

Figure 4.3: Time-average side force and yawing moment versus turntable angle for steady blowing, with $\phi = 0^\circ$, $C\mu = 0.00053$, $Re_D = 3.53 \times 10^5$ ($V_\infty = 125$ ft/s).

Figure 4.4: Time-average side force and yawing moment versus turntable angle for steady blowing, with $\phi = 0^\circ$, $C\mu = 0.00094$, $Re_D = 6.50 \times 10^5$ ($V_\infty = 230$ ft/s).

Figure 4.5: Time-average side force versus blowing momentum for steady blowing, with $\phi = 0^\circ$, $Re_D = 1.76 \times 10^5$.

Figure 4.6: Time-average side force versus blowing momentum for steady blowing, with $\phi = 0^\circ$, $Re_D = 2.82 \times 10^5$.

Figure 4.7: Side force time-history for alternating blowing with reduced frequencies of 0.16 and 0.32. $\tau_1/T = 0.5$, $\sigma = 50^\circ$, $\phi = 0^\circ$, $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.8: Side force time-history for alternating blowing with reduced frequencies of 0.16 and 0.32. $\tau_1/T = 0.4$, $\sigma = 50^\circ$, $\phi = 0^\circ$, $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.9: Side force time-history for alternating blowing with reduced frequencies of 0.16 and 0.32, $\tau_1 / \Gamma = 0.3$, $\sigma = 50^\circ$, $\phi = 0^\circ$, $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.10: Side force time-history for alternating blowing with reduced frequencies of 0.16 and 0.32, $\tau_1 / \Gamma = 0.2$, $\sigma = 50^\circ$, $\phi = 0^\circ$, $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.11: Side force time-history for alternating blowing with reduced frequencies of 0.16 and 0.32. $\tau_1/T = 0.1$, $\sigma = 50^\circ$, $\phi = 0^\circ$, $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

(a) Side force

(b) Yawing moment:

Figure 4.12: Time-average side force and yawing moment versus duty cycle parameter for alternating blowing, with $\omega^* = 0.16$, $\phi = 0^\circ$, $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.13: Time-average side force and yawing moment versus duty cycle parameter for alternating blowing with $\omega^* = 0.32$, $\phi = 0^\circ$, $C\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.14: Time-average side force and yawing moment versus turntable angle for steady blowing, with $\phi = 5^\circ$ and -5° , $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.15: Time-average side force and yawing moment versus turntable angle for steady blowing, with $\phi = 10^\circ$ and -10° , $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.16: Time-average side force and yawing moment versus turntable angle for steady blowing, with $\phi = 15^\circ$ and -15° , $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.17: Time-average side force and yawing moment versus turntable angle for steady blowing, with $\phi = 20^\circ$ and -20° , $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.18: Effect of roll angle on time-average side force for steady blowing, with $\sigma = 50^\circ$, $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.19: Time-average side force and yawing moment versus duty cycle parameter for alternating blowing, with $\omega^* = 0.32$, $\phi = 5^\circ$ and -5° , $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.20: Time-average side force and yawing moment versus duty cycle parameter for alternating blowing, with $\omega^* = 0.32$, $\phi = 10^\circ$ and -10° , $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.21: Time-average side force and yawing moment versus duty cycle parameter for alternating blowing, with $\omega^* = 0.32$, $\phi = 15^\circ$ and -15° , $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.22: Time-average side force and yawing moment versus duty cycle parameter for alternating blowing, with $\omega^* = 0.32$, $\phi = 20^\circ$ and -20° , $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Figure 4.23: Smoke visualization image showing the port vortex detached from the surface near the base of the nose. $V_{\infty} = 62$ ft/s and $\sigma = 50^{\circ}$.

Figure 4.24: Smoke visualization image showing the starboard vortex detached from the surface near the base of the nose. $V_{\infty} = 62$ ft/s and $\sigma = 50^{\circ}$.

Figure 4.25: Surface flow visualization without blowing, with $\sigma = 50^\circ$, $\phi = 0^\circ$, $Re_D \approx 3.53 \times 10^5$.

Figure 4.26: Surface flow visualization for steady port blowing, with $\sigma = 50^\circ$, $\phi = 0^\circ$, $C_\mu = 0.00053$, $Re_D \approx 3.53 \times 10^5$.

Figure 4.27: Surface flow visualization without blowing, with $\sigma = 50^\circ$, $\phi = -10^\circ$, $Re_D = 1.76 \times 10^5$.

Figure 4.28: Surface flow visualization for steady port blowing, with $\sigma = 50^\circ$, $\phi = -10^\circ$, $C_\mu = 0.00066$, $Re_D = 1.76 \times 10^5$.

Appendix A

Strain Gauge Mounting

This appendix describes the procedures followed for mounting the Showa foil strain gauges on the flexures of the internal balance.

Flexure surface preparation:

- 300 grit emery paper was used in one direction to remove surface markings, such those due to machining.
- 600 grit emery paper was applied in the orthogonal direction to attain a smooth surface but the necessary roughness for adhesion.
- The surface was degreased by washing it in acetone and then dry it thoroughly with a clean cotton swab.
- The surface was washed again with M-Prep Conditioner A, then with M-Prep Neutralizer (an alkaline).

Adhering a strain gauge to a flexure surface:

- MBOND 206 catalyst was applied to the back of the strain gauge.
- MBOND 200 adhesive was applied in a small amount to the prepared flexure surface.
- Using cellophane tape, the strain gauge was picked up and placed on the flexure surface.

- After applying thumb pressure onto the strain gauge for approximately 1 minute, the cellophane tape was removed by peeling back onto itself.

Waterproofing and protection of the strain gauge:

- Three coats of M-Coat air-drying polyurethane were applied to the mounted strain gauge.
- The last coat of polyurethane was allowed to dry for 24 hours.
- A final coat of crystalline wax was applied to the strain gauge for additional protection against moisture.

Appendix B

Balance Sensitivity Coefficients

Matrix manipulation software was used to solve Equation 3.1 with the 22 side force calibration load cases. The balance sensitivity coefficients were originally calculated by Dr. E.S. Hanff; the program listing shown in this appendix is an adaption of his code for implementation in MATLAB. Using normal force calibration data, this program was also used to calculate the balance sensitivity coefficients for normal loads, enabling a comparison of side force and normal force sensitivities.

```
// Filename: Sideforce_cal.mat
// Implementation: MATLAB

clear      // Clear variables

// Linear coefficients for each load case (i.e. station)
// These are slopes from a straight line curve fit of the flexure outputs
// plotted against calibrations load applied
// 1st column = forward flexure; 2nd column = aft flexure
c1 = < 1.38165e-2, 2.40709e-2>;
c2 = < 1.04982e-2, 2.07029e-2>;
c3 = < 7.18324e-3, 1.73753e-2>;
c4 = < 3.81518e-3, 1.40130e-2>;
c5 = < 4.79116e-4, 1.06597e-2>;
c6 = < -2.83220e-3, 7.33316e-3>;
c7 = < -6.15945e-3, 3.99471e-3>;
c8 = < -9.49821e-3, 6.60545e-4>;
c9 = < -1.28417e-2, -2.68901e-3>;
c10 = < -1.61861e-2, -6.02617e-3>;
c11 = < 1.71513e-2, 2.73902e-2>;
c12 = < 1.38234e-2, 2.40363e-2>;
c13 = < 1.04740e-2, 2.03091e-2>;
c14 = < 7.15493e-3, 1.73453e-2>;
c15 = < 3.79352e-3, 1.39916e-2>;
c16 = < 4.71484e-4, 1.06531e-2>;
c17 = < -2.86052e-3, 7.30919e-3>;
c18 = < -6.19290e-3, 3.97665e-3>;
c19 = < -9.51715e-3, 6.44929e-4>;
c20 = < -1.28635e-2, -2.68002e-3>;
c21 = < -1.61992e-2, -6.02317e-3>;

// Form linear coefficients matrix [a]
a = < c1; c2; c3; c4; c5; c6; c7; c8; c9; c10; c11; c12; c13; c14; c15;
      c16; c17; c18; c19; c20; c21 >;
```

```

// Load ratios for each load case
// 1st column = unit load; 2nd column = moment arm relative to resolving
// center
c1 = <1, 5>;
c2 = <1, 4>;
c3 = <1, 3>;
c4 = <1, 2>;
c5 = <1, 1>;
c6 = <1, 0>;
c7 = <1, -1>;
c8 = <1, -2>;
c9 = <1, -3>;
c10 = <1, -4>;
c11 = <1, 6>;
c12 = <1, 5>;
c13 = <1, 4>;
c14 = <1, 3>;
c15 = <1, 2>;
c16 = <1, 1>;
c17 = <1, 0>;
c18 = <1, -1>;
c19 = <1, -2>;
c20 = <1, -3>;
c21 = <1, -4>;

// Form load ratios matrix [f]
f = <c1; c2; c3; c4; c5; c6; c7; c8; c9; c10; c11; c12; c13; c14; c15;
    c16; c17; c18; c19; c20; c21>;

// Calculate sensitivity matrix [c]
c = a * f' * inv(f * f');

// Calculate the inverse of the sensitivity matrix [c]
short, cinv = inv(c)

// Check: Recover load ratios matrix [f]
<cinv * a>

```

The result for the inverse of the sensitivity matrix is repeated here for convenience:

$$[C]^{-1} = \begin{bmatrix} -98.375 & 98.342 \\ 215.868 & 84.043 \end{bmatrix} \quad (\text{B.1})$$

where the coefficients in the first row have units of lb/mV/volt excitation; in the second row, the units are lb-in/mV/volt excitation.

Appendix C

Correction of Blowing System Calibration

As outlined in Section 3.5.3, the volume flow rate calibration for the nozzles is flawed because an increasing back pressure acts on the restrictors as the column of displaced water increases in height. Hence the volume flow rate decreases with increasing back pressure. According to the calibration procedure, the time for the displacement of 0.5 ft³ of water was measured, so the calibrated volume flow rate indicates an average for a range of increasing back pressures from 0 to 12 inches of water (i.e. 0 to 0.434 psig). This average is expected to be lower than the volume flow rate with zero back pressure, necessitating a correction to the calibration data.

Incompressible flow theory is applied to determine the ratio of average volume flow rate with back pressure, to the volume flow rate with zero back pressure. From the Bernoulli and continuity equations, it can be shown that the volume flow rate, Q , is a function of the pressure difference across an obstruction (i.e. the restrictor in this case):

$$Q \approx A_t \sqrt{\frac{2(p - p_{bp})}{\rho}} \quad (C.1)$$

where A_t is the cross-sectional area of the restriction; p , the static pressure upstream of the restriction; and p_{bp} is the back pressure acting on the restrictor. For zero back pressure, Equation C.1 becomes:

$$Q \approx A_t \sqrt{\frac{2p}{\rho}} \quad (C.2)$$

Now the average volume flow rate, with back pressure, is given by

$$\bar{Q} \approx \frac{1}{P_{bp}} \int_{p_{bp}=0}^{P_{bp}} A_t \sqrt{\frac{2(p - p_{bp})}{\rho}} dp_{bp} \quad (C.3)$$

where P_{bp} is 0.434 psi for a maximum water column height of 12 inches. The ratio $\bar{Q}/Q$ is obtained by evaluating the integral in Equation C.3, substituting for $p_{bp} = 0.434$ psi and then dividing through by Equation C.2:

$$\frac{\bar{Q}}{Q} = \frac{2}{3} \left[\frac{p^{3/2} - (p - 0.434)^{3/2}}{0.434\sqrt{p}} \right] \quad (C.4)$$

By definition, the coefficient of blowing momentum is given by

$$C\mu = \frac{\rho Q^2}{q_\infty SA} \quad (C.5)$$

and so for average volume flow rate

$$\bar{C}\mu = \frac{\rho \bar{Q}^2}{q_\infty SA} \quad (C.6)$$

Therefore

$$\frac{\bar{C}\mu}{C\mu} = \left(\frac{\bar{Q}}{Q} \right)^2 \quad (C.7)$$

where $C\mu$ and Q are corrected values. The estimated error in $\bar{C}\mu$ is

$$\varepsilon = 1 - \frac{\bar{C}\mu}{C\mu} = 1 - \left(\frac{\bar{Q}}{Q} \right)^2 \quad (C.8)$$

As an example, consider $\bar{C}\mu = 0.00047$ for $Re_D = 1.76 \times 10^5$ which required a static pressure of approximately 0.77 psig, upstream of the restrictor. From Equations C.4 and C.8, respectively

$$\frac{\bar{Q}}{Q} = 0.842 \quad \text{and} \quad \varepsilon = 0.291 \quad (\text{or } \approx 29\%)$$

hence $C\mu = 0.00066$. If an upstream static pressure of 5 psig is required, then

$$\frac{\bar{Q}}{Q} = 0.978 \quad \text{and} \quad \varepsilon = 0.0436 \quad (\text{or } \approx 4.4\%)$$

Clearly, as the supply pressure increases, the error in $C\mu$ reduces. For supply pressures greater than about 13 psig, choking will occur and the back pressure then has no effect on the volume flow rate and no correction of the calibration data is required. Figure 3.12 includes a curve for the zero back pressure case; this curve was determined with the above relations. As already mentioned in Section 3.5.3, all $C\mu$ values quoted in this thesis are corrected values (i.e. true values) unless otherwise noted.

Appendix D

Listing of Program UTIL2

This appendix is to lists the source code of the program UTIL2, gives an example of an input batch file, and to show the contents of the resulting output files.

```
/******  
File name:  util2.c
```

The purpose of this program is to calculate the time-averaged side force coefficient, the time-averaged yawing moment coefficient, and location of the C_p (X_{cp}), using the coefficient results, with respect to the resolving center of the internal balance. The resolving center is located 18.42 inches behind the tip of the nose.

The command line input to this program is a batch file which lists abscissa and run ID pairs - the abscissa may be the angle of attack or blowing coefficient, for example. Three output text files are created for time-averaged side force coefficient, yawing moment coefficient and X_{cp} . Similiar to the batch file, the output file lists pairs of abscissa and a corresponding calculated quantity. The output files are intended for import to a plot package.

All output files names are derived from the input batch file name. The file extension differs according to the calculated quantity the file contains:

time-averaged side force	: .cy
time-averaged yawing moment	: .cn
X_{cp}	: .xcp

List of Variables =====

abscissa	x-coordinate
cn	yaw moment coefficient
cnsum	sum of moment coefficients
cy	side force coefficient
cysum	sum of side force coefficients
i	counter
flag	indicates the absence of a load file
n	number of load files in the input batch file
p1	source voltage for valve 1 pressure transducer (mV)
p2	source voltage for valve 2 pressure transducer (mV)
samples	number of samples
time	sample time
v1	value of drive function for valve 1
v2	value of drive function for valve 2


```

*****/

#include <fcntl.h>
#include <stdio.h>
#include <stdlib.h>
#include <string.h>

#define REF_LEN 30.30 /* reference length = overall length of model */

main(argc, argv)
int argc;
char *argv[];
{
    int i, flag, n, samples;
    double abscissa[15], time, v1, v2, cy, cn, p1, p2, cysum, cnsum, cyavg;
double cnavg;
    char infile[51], run[15][5], cyfile[51], cnfile[51], xcpfile[51];
    char *loadfile();
    void help();
    FILE *fopen(), *fpin, *fpload, *fpcy, *fpcn, *fpxcp;

    /* Analyze command line for the name of the input batch file;
       open the file */

    /* Was an input batch file given on the command line? */
    if (argc == 1)
    {
        help();
        exit(0);
    }

    strcpy(infile, argv[1]); /* copy input batch file name */

    /* Does the input batch file exist? */
    if ((fpin = fopen(infile, "r")) == NULL)
    {
        printf("Input file %s not found ...\n", infile);
        exit(0);
    }

    /* Read the abscissa and run id from the input file; count and verify
       existence of each load file */

    i = 0;
    flag = 0;
    while (fscanf(fpin, "%lf %s", &abscissa[i], run[i]) != EOF)
    {
        if (access(loadfile(run[i]), 0) != 0)
        {
            printf("Load file %s not found ...\n", loadfile(run[i]));
            flag = 1;
        }
        i++;
    }
    if (flag == 1)
        exit(0);

    n = i - 1; /* save the number of entries found in the input file */

```

```

/* Create output file names for the side force and yawing moment
   coefficients; open the respective files for writing */

strcpy(cyfile, infile);
strcpy(rindex(cyfile, '.'), ".cy");
fpcy = fopen(cyfile, "w");

strcpy(cnfile, infile);
strcpy(rindex(cnfile, '.'), ".cn");
fpcn = fopen(cnfile, "w");

strcpy(xcpfile, infile);
strcpy(rindex(xcpfile, '.'), ".xcp");
fpxcp = fopen(xcpfile, "w");

printf("Computing time-averages for Cy and Cn\n\n");

/* Read each load file and calculate the time-averages for the side
   force and yawing moment coefficient; write the averages to the
   respective output files */

for (i = 0; i <= n; i++)
{
    fpload = fopen(loadfile(run[i]), "r");
    samples = 0;
    cysum = 0.0;
    cnsum = 0.0;
    printf("processing load file %s ...\n", loadfile(run[i]));
    while (fscanf(fpload, "%le %le %le %le %le %le %le\t\r",
                  &time, &v1, &v2, &cy, &cn, &p1, &p2) != EOF)
    {
        cysum += cy;
        cnsum += cn;
        samples++;
    }
    fprintf(fpcy, "%-9.6lf      %-9.6lf\n", abscissa[i], (cyavg=cysum/samples));
    fprintf(fpcn, "%-9.6lf      %-9.6lf\n", abscissa[i], (cnavg=cnsum/samples));
    fprintf(fpxcp, "%-9.6lf      %-7.6lf\n", abscissa[i], cnavg*REF_LEN/cyavg);
    fclose(fpload);
}

printf("\nProcessing complete ...\n");

/* Close files */

fclose(fpin);
fclose(fpcy);
fclose(fpcn);
fclose(fpxcp);

} /* end of program */

char *loadfile(s)
/* This routine creates the loadfile name from the given run number */
char *s;
{
    static char name[25]; /* buffer to create file name */

    if (strlen(s) == 4)
        sprintf(name, "d10%s.fbc", s);
    else if (strlen(s) == 3)

```

```

        sprintf(name, "d100%s.fbc", s);
    else
        sprintf(name, "d1000%s.fbc", s);
    return(name);
}

void help()
/* This routine sends help text to stdout, e.g. screen */
{
    printf("\n Usage:  util2 <batch file>\n\n");
    printf("This program calculates the time-averaged side force coefficient, ");
    printf("the time-\n");
    printf("averaged yawing moment coefficient, and location of the Cp (Xcp), ");
    printf("using the\n");
    printf("coefficient results, with respect to the resolving center of the ");
    printf("internal \nbalance.\n\n");
    printf("The command line input to this program is a batch file which lists ");
    printf("abscissa\n");
    printf("and run ID pairs - the abscissa may be the angle of attack or ");
    printf("blowing\n");
    printf("coefficient, for example. Three output text files are created for ");
    printf("time-\n");
    printf("averaged side force coefficient, yawing moment coefficient and Xcp.");
    printf(" The\n");
    printf("The output files lists pairs of abscissa and a corresponding ");
    printf("calculated\n");
    printf("quantity, and are intended for import to a plotting package.\n\n");
    printf("All output file names are derived from the input batch filename. The");
    printf(" file\n");
    printf("extension differs according to the calculated quantity the file ");
    printf("contains:\n\n");
    printf("                time-averaged side force      : .cy\n");
    printf("                time-averaged yawing moment    : .cn\n");
    printf("                Xcp                            : .xcp\n");
}

```

The contents of a typical input batch file, e.g. `example.bat`, would be as follows:

```

30      423
40      425
50      427
60      429
70      431

```

where the first column lists the angle of attack, and the second column shows the run identification number associated with the model attitude.

Processing `example.bat` with UTIL2 produces three ASCII output files.

Contents of <code>example.cy</code> :	30.000000	0.194867
	40.000000	2.990915
	50.000000	4.008603
	60.000000	3.627898
	70.000000	1.310566
Contents of <code>example.cn</code> :	30.000000	-0.026491
	40.000000	-0.241503
	50.000000	0.160653
	60.000000	0.727388
	70.000000	0.489304
Contents of <code>example.xcp</code> :	30.000000	-4.119147
	40.000000	-2.446590
	50.000000	1.214334
	60.000000	6.075105
	70.000000	11.312590

where, in each file, the first column echoes the angle of attack. The values in the second column are identified by the file extension. As such, these files can be readily imported into a plotting package.

Multiple processing of batch files can be performed by another batch file containing consecutive calls of UTIL2:

```
util2 example_1.bat
util2 example_2.bat
util2 example_3.bat
util2 example_n.bat
```

Appendix E

Geometric Transformation Equations

Lamont and Kennaugh (1991) describe an approach by which investigations into sideslip behaviour of slender forebodies, with circular cross-sections, at high angles of attack can be performed by simulating combinations of angle of attack and sideslip with a range of angle of incidence and roll.

In effect, the approach is a geometric transformation between two coordinate systems: the conventional body-axis system which describes the orientation of the model with angle of attack α and sideslip angle β ; and the “total incidence plane” which describes the model orientation by the total incidence angle σ and roll angle ϕ . The relation between these coordinate systems is illustrated in Figure E.1. The total incidence plane is formed by the body-axis and the freestream vector; the included angle in this plane is the total incidence angle. At zero yaw, it is clear that the systems coincide so that $\alpha = \sigma$ and $\phi = 0^\circ$. As yaw increases, the total incidence plane rotates about the freestream velocity vector causing σ to increase above α , and the effective roll angle to increase. The transformation between the coordinate systems is described by the following equations:

$$\cos(\sigma) = \cos(\alpha) \cdot \cos(\beta) \quad (\text{E.1})$$

$$\tan(\phi) = \frac{\tan(\beta)}{\sin(\alpha)} \quad (\text{E.2})$$

For this investigation, the angle σ is the turntable angle. Given σ and ϕ , Equations D.1 and D.2 must be solved iteratively to find the equivalent combination of α and β . Table E presents the equivalent (α, β) for the range of (σ, ϕ) used in the investigation.

Lamont and Kennaugh also present equations to transform the aerodynamic coefficients (subscript “aero” in Figure E.1) measured in the total incidence plane

to the body-axis system. The transformations were not required in this investigation because the side force and yawing moment were measured in the body-axis system.

Figure E.1: Relation between the total incidence plane and body-axis system. Reproduced from Figure 2 of Lamont and Kennaugh (1991).

σ	$\phi = \pm 5^\circ$		$\phi = \pm 10^\circ$		$\phi = \pm 15^\circ$		$\phi = \pm 20^\circ$	
	α	β	α	β	α	β	α	β
30°	29.9°	$\pm 2.5^\circ$	29.6°	$\pm 5.0^\circ$	29.1°	$\pm 7.4^\circ$	28.5°	$\pm 9.8^\circ$
40°	39.9°	$\pm 3.2^\circ$	39.6°	$\pm 6.4^\circ$	39.0°	$\pm 9.6^\circ$	38.3°	$\pm 12.7^\circ$
50°	49.9°	$\pm 3.8^\circ$	49.6°	$\pm 7.6^\circ$	49.0°	$\pm 11.4^\circ$	48.2°	$\pm 15.2^\circ$
60°	59.9°	$\pm 4.3^\circ$	59.6°	$\pm 8.6^\circ$	59.0°	$\pm 13.0^\circ$	58.4°	$\pm 17.2^\circ$
70°	69.9°	$\pm 4.7^\circ$	69.7°	$\pm 9.4^\circ$	69.0°	$\pm 14.1^\circ$	68.8°	$\pm 18.7^\circ$

Table E.1: Non-zero roll orientations expressed in the total incidence plane (σ , ϕ) and conventional body-axis system (α , β).

Appendix F

Equipment List and Specifications

Solenoid valves: ASCO/Angar Model 008 high-speed, 2-way, 2-position, steel-cased solenoid valves with a 3-5 ms response time, 0-100 psi operating pressure range, a flow factor of 0.098 and a life duration of approximately 10 million cycles.

Strain gauges: Showa Measuring Instruments Co. Ltd., general purpose foil strain gauges (type N11-FA-1-350-16) with a gauge length of 1 mm, a nominal resistance of 350 Ω and a gauge factor of 2.19. The strain gauges are temperature compensated for steel.

Pressure transducers: IC Sensor OEM piezoresistive differential pressure sensor (part number 30A-100D) with a full-scale output span of 100 mV for a 100 psig range, zero pressure output ± 5 mV, linearity $\pm 0.05\%$ of span and a response time of 1 ms.

Inclinometer: Lucas-Schaevitz Digital Protractor Model DP-60 with a range of $\pm 60^\circ$; resolution is 0.01° from 0° to 20° , and 0.1° from 20° to 60° .

Laser: Spectra-Physics argon-ion laser Model 2016 with 5 watts output.

High-speed imaging system: Kodak ETKA PRO 1000 high-speed video imaging system, including image-intensified camera, recorder and viewer. The camera has a maximum filming rate of 250 frames/second.

Surface visualization: Oils used were Fisher Brand 19 mechanical pump fluid for a test section velocity of 120 ft/s or less; and Aeroshell Oil 80 (SAE 40) for a velocity of 230 ft/s. The fluorescent pigment mixed with the oil was a Dayglo

product, Saturn Yellow (product number A-17-N).

Low-speed camera: Panasonic colour video camera Model WV-F300 with a fixed filming rate of 30 frames/second.

The following equipment was used for the calibration of the balance and pressure transducers:

Inclinometer: Sperry Micro Level Model 20 with an angular range of $\pm 19.99^\circ$ and a measuring accuracy of $\pm 0.03^\circ$ up to 3° and $\pm 1\%$ over 3° .

Voltmeters: A Fluke 8840A multimeter (CU S/N 008936) was used for monitoring the output signal of the rear flexure of the balance and the pressure transducers; a Philips PM2525 multimeter (NRC S/N 079153) was used for monitoring the output signal from the forward flexure.

A one-meter U-tube water manometer and a dead weight pressure calibration unit were used to apply known pressures to the pressure transducers.

END

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