Dynamic Manipulation of Asymmetric Forebody Vortices to Achieve Linear Control

by

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

Ottawa-Carleton Institute for
Mechanical and Aerospace Engineering

Department of
Mechanical and Aerospace Engineering
Carleton University
Ottawa, Ontario
October 2004

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ISBN: 0-494-00802-4

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ISBN: 0-494-00802-4

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Abstract

A wind tunnel experiment was performed to further investigate the potential of the dynamic manipulation of forebody vortices as a means of supplementing directional control of fighter aircraft at high angles of attack. Tests were conducted on a 65-deg delta-wing model fitted with a slender, pointed tangent-ogive forebody of circular cross-section and 12.8 deg semi-apex angle. Forward-blowing nozzles located near the apex of the forebody served as the means of manipulating the forebody vortices. As expected, forward blowing was very effective, i.e., little blowing effort was required to cause the forebody vortex on the blown side to assume the 'high' position. However, the magnitudes of yawing moment and side force developed by the slender forebody with blowing do not differ significantly from that of the no-blowing, baseline case. Moreover, blowing above a certain threshold value produced an unexpected reversal, with blowing causing the vortex on the blown side to assume the 'low' position instead and the yawing moment and side force to change sense. The results have shown that the dynamic manipulation scheme is very successful in producing a linear variation of time-average yawing moment with a duty-cycle parameter, even with sideslip, for the aircraft-like model. The results also show that, by switching the vortex pattern rapidly, the linearity can be maintained up to a reduced frequency of at least 0.32, which is expected to be very satisfactory for practical applications. Cross-coupling effects were also identified between the control method and time-average rolling moment, pitching moment and normal force. The cross-coupling effects in rolling moment and normal force appear to benefit the flight mechanics, whereas the effect in pitching moment may not. Excellent effectiveness of forward-blowing in sideslip was demonstrated with nozzles located in close proximity to the apex of the forebody ($x_n/D=0.095$); a more aft location ($x_n/D=0.159$) may reduce effectiveness in sideslip but raises the threshold at which vortex reversal occurs and thus increases the margin for low blowing momentum coefficients. The preferred azimuthal position of 120 deg from the windward meridian

was found to be associated with a steady leeward flow field. The freestream Reynolds number was limited to 2.0×10^5 for most of the wind tunnel experiment so as not to trigger a serious resonance in the model support.

A subsequent water tunnel experiment with the forebody alone was undertaken to conduct off-surface flow visualizations that confirmed the vortex reversal phenomenon. Based on the flow visualization studies, a hypothesis was formed regarding the cause of the reversal phenomenon; it postulates that at the reversal threshold the nozzle flux interrupts the formation of the high forebody vortex on the blowing side and encourages the shear layer to form a replacement vortex that lies close to the forebody. The findings in the water tunnel also suggest that the ‘stacked’ arrangement of the vortices for a slender forebody may be responsible for the finding that yawing moment and side force with blowing are no greater than the baseline magnitudes.

The research indicates that the dynamic manipulation scheme using forward blowing, either below or above the reversal threshold, is a viable and attractive means of obtaining linear yaw control of an aircraft-like configuration. Considerably more test data are still required, however, to better define the design space and to reduce technical risk.

Acknowledgements

I feel privileged to have been the benefactor of the skills and efforts of many people who contributed to this project. To all of them, I am deeply grateful. But I feel obligated to explicitly acknowledge the efforts and contributions of some key individuals.

From the Aerodynamics Laboratory of the Institute for Aerospace Research at the National Research Council Canada, I am grateful to **Armando Prini** for the programming of the data acquisition system; to **Steve Jenkins** for maintaining the data acquisition system and his assistance during the wind tunnel experiment; and to **Kul Kapoor** for the design of key mechanical components. I am also thankful to **Dr. Steven Zan**, currently the Acting Director of the Aerodynamics Laboratory and my supervisor from the beginning of my employment in the laboratory since the summer of 2000, for his continuous support and for granting me leave from my duties to complete this work.

From the University of Ottawa, I am thankful to **Professor Stavros Tavoularis** for the use of his water tunnel facility and for his guidance during the experiments, and to his graduate student **Warren Dunn** for his assistance with the water tunnel experiments.

Finally, I thank my thesis co-supervisors **Dr. Ernest Hanff** of the Aerodynamics Laboratory, and **Professor Dick Kind** of Carleton University for giving me the opportunity to expand on our previous findings in the Master thesis and for their ongoing support.

*To my parents
and
Maryam Ghannad*

I still haven't found what I'm looking for

U2, The Joshua Tree

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Nomenclature

a_0, a_1	Coefficients for the nozzle volume flow rate calibration equations
b_0, b_1	Exponents for the nozzle volume flow rate calibration equations
$[a]$	Matrix of linear sensitivities of flexure output voltage to applied load
A	Planform area; inverse of total signal gain ($G \times G_o$)
A_n	Cross-sectional area of nozzle
A'	Frontal area
b	Wing span
$[b]$	Matrix of non-linear sensitivities of flexure output voltage to applied load
B	Systematic uncertainty; $1/(qS_w)$ or $1/(qS_w l)$
c	Mean geometric chord
c_{ij}	Coefficient of the inverse sensitivity matrix
c_y	Section side force coefficient, $\frac{Y \text{ per unit length}}{q_\infty d \sin^2 \alpha}$
C_b	Bleed coefficient, $\frac{d^2 V'^2}{D^2 V_\infty^2}$
C_d^*	Critical discharge coefficient
C_{ij}	Coefficient of sensitivity matrix
C_F, C_{L_i}	Aerodynamic load coefficient
$C_{\dot{m}}$	Nozzle mass flow rate coefficient, $\frac{\dot{m}_j}{\rho_\infty V_\infty S_D}$
C_ℓ	Rolling moment coefficient, $\frac{\mathcal{L}}{q_\infty S_w b}$
$\overline{C}_\ell$	Time-average rolling moment coefficient, $\frac{1}{s} \sum_{i=1}^s C_{\ell_i}$
C_m	Pitching moment coefficient, $\frac{M}{q_\infty S_w c}$

$\bar{C}_m$	Time-average pitching moment coefficient, $\frac{1}{s} \sum_{i=1}^s C_{m_i}$
C_n, C_N	Yawing moment coefficient, $\frac{N}{q_\infty S_w b}, \frac{N}{q_\infty \frac{\pi}{4} D^3}$
$\bar{C}_n$	Time-average yawing moment coefficient, $\frac{1}{s} \sum_{i=1}^s C_{n_i}$
C_p	Pressure coefficient, $\frac{p - p_\infty}{q_\infty}$
C_Q	Suction mass flow rate coefficient (same as C_m but S_w replaces S_D)
C_v	Valve flow factor
C_Y	Side force coefficient, $\frac{Y}{q_\infty S_w}$
$\bar{C}_Y$	Time-average side force coefficient, $\frac{1}{s} \sum_{i=1}^s C_{Y_i}$
C_Z	Normal force coefficient, $\frac{Z}{q_\infty S_w}$
$\bar{C}_Z$	Time-average normal force coefficient, $\frac{1}{s} \sum_{i=1}^s C_{Z_i}$
C_μ	Coefficient of blowing momentum, $\frac{\dot{m}_j V_j}{q_\infty S_D}$
[C1]	Matrix of linear sensitivity coefficients
[C2]	Matrix of non-linear sensitivity coefficients
d	Diameter of nozzle; diameter of cylindrical orifice
D	Base diameter of forebody model
f	Frequency of alternating blowing; unsteady bleed frequency; component load ratio
F	Load component applied to a balance flexure

F_g	Graph factor
F_{sg}	Specific factor
F_t	Temperature compensation factor
G	Variable signal gain
G_o	Fixed signal gain
h	Clearance between wing leading-edge vortex and support strut (Fig. 3.17)
K	Gain of an amplifier/filter or sensitivity of an accelerometer
l	Length of forebody; reference length
l_{offset}	Distance between the balance resolving centre and moment reference point
l_s	Length of blowing slot
L	Overall length of model
L_F	Aerodynamic force
L_M	Aerodynamic moment
$[L1]$	Matrix of linear load ratios
$[L2]$	Matrix of non-linear load ratios
$\mathcal{L}$	Rolling moment about the resolving centre of the internal balance
$\dot{m}_j$	Nozzle mass flow rate, $\rho \frac{\pi}{4} d^2 V_j$
$\dot{m}_{ref}$	Reference mass flow rate
M	Pitching moment about the resolving centre of the internal balance; Mach number
n	Number of measurements; number of balance components
N	Yawing moment about the resolving centre of the internal balance
p	Static pressure
P	Single-varying calibration load; precision uncertainty
P_o	Barometric pressure
q_∞	Freestream dynamic pressure, $\frac{1}{2} \rho V_\infty^2$
Q_n	Nozzle volume flow rate
r	Local forebody cross-sectional radius

r_o	Zero offset voltage of a balance flexure bridge
R	Base radius of forebody model; output voltage of balance flexure bridge
R_d	Starboard valve duty cycle fraction
Re_D	Reynolds number, $\frac{V_\infty D}{\nu}$
$R_{xy}(\eta)$	Time cross-correlation function
s	Number of samples acquired within one alternating blowing cycle
S	Sample standard deviation
S_D	Base area of forebody model, $\frac{\pi D^2}{4}$
S_{ref}	Reference area
S_w	Wing reference area
t	Time for flow development; length of cylindrical orifice; Student's t for number of degrees of freedom
t_{fall}, t_{rise}	Valve transient response time
t_v	Forebody vortex response time
$\bar{t}$	Non-dimensional time parameter, $\frac{x}{R} \tan \alpha$
t^*	Non-dimensional time parameter, such as $\frac{x}{r} \tan \alpha$ or $\frac{\Delta t}{\tau}$; normalized time-delay
T	Period of an alternating blowing cycle; temperature
U	Total uncertainty
V	Flow velocity; voltage; crossflow component of freestream velocity
V_j	Blowing nozzle exit velocity
V_m	Measured balance voltage
V_o	Differential-pressure transducer output voltage
V_{offset}	Voltage offset
V_s	Voltage at source
V_∞	Freestream velocity
$V_{1\Delta p}$	Differential-pressure voltage at the port in-line metering device
V'	Root-mean-square velocity of the unsteady bleed

x	Axial coordinate relative to the forebody apex (Fig. 2.3)
x_j, x_k	Actual numerical value of random variables
x_n	Axial coordinate of nozzle exit relative to the forebody apex
x_s	Axial coordinate of laser light-sheet relative to the forebody apex
X_{cp}	Instantaneous centre of pressure position
$\tilde{X}_{cp}$	Mean centre of pressure position
y	Amplitude
Y	Side force
Z	Normal force

Greek Nomenclature

α	Angle of attack
β	Angle of sideslip; the circumferential location in the crossflow plane (clockwise looking forward); the ratio of orifice diameter to nozzle diameter
δ_N	Semi-apex angle
δ_s	Strake deflection angle
$\delta_{s,d}$	Differential strake deflection angle, $\delta_{s,starboard} - \delta_{s,port}$
ϵ_t	Blockage correction
η	Time displacement
θ	Azimuthal location in the crossflow plane; sensitivity of a load component to the bridge output voltage of a flexure
μ_j, μ_k	Mean value of random variables
ν	Kinematic viscosity
ρ	Density
$\rho_{xy}(\eta)$	Cross-correlation coefficient function
σ	Body-axis inclination to freestream velocity (i.e., the turntable angle)
τ	Duration a valve is opened or closed during an alternating blowing;

	convective time-scale
υ	Time parameter for describing a valve control waveform
ϕ	Roll angle; angular displacement of the zero vorticity contour
ϕ_m	Model roll angle
ϕ_s	Strake azimuthal position
ϕ_t	Tip roll angle
ψ	Yaw angle
ω	Angular frequency of alternating blowing, radians/time
ω^*	Reduced frequency of alternating blowing, $\frac{\omega D}{V_\infty}$
Λ	Leading-edge sweep angle

Abbreviations

CFD	Computational fluid dynamics
CP	Centre of pressure
DC	Duty cycle
FS	Full scale
HARV	High Angle of Attack Research Vehicle
HIRM	High Incidence Research Model
IAR	Institute for Aerospace Research
IBIM	Intelligent bus interface module
LDV	Laser Doppler velocimetry
LEX	Leading-edge extension
NASA	National Aeronautics and Space Administration
NFS	Network file system
NRC	National Research Council Canada
ONERA	Office National d'Etudes et de Recherches Aérospatiales
RAE	Royal Aeronautical Establishment
SARL	Subsonic Aerodynamics Research Laboratories

SCFH	Standard cubic feet per hour
ZVC	Zero vorticity contour

xxx

Chapter 1

Introduction

This thesis documents an investigation of a novel scheme for the manipulation of forebody vortices to improve lateral control of high performance aircraft at high angles of attack.

1.1 Changing Tactics and the Problem of Asymmetric Forebody Vortices

Air combat engagements are foreseen to shift from beyond-visual-range to within-visual-range encounters. The change of tactics will be largely due to advancements of low-observable technology and electronic countermeasures that will cause long-range air-to-air missiles, released at beyond-visual-range, to miss the intended target, or hamper the ability of a pilot to obtain a positive identification of an opponent. Whether as a result of a missed long-range missile shot or constrained by rules of engagement, a fighter pilot will eventually be compelled to make visual contact with an opponent. In a close-in-combat engagement, however, a fighter aircraft must maneuver at relatively slow speeds and high angles of attack. The advantage will be captured by the aircraft which can maneuver with agility at these extreme flight conditions, i.e., be able to change attitude with ease and swiftness.

Simulation studies have shown that the agile aircraft is superior over the conventional aircraft (Herbst, 1980). In fact, the X-31 technology demonstrator (Fig. 1.1) has demonstrated in actual flight trials that it can repeatedly defeat a F/A-18 in one-on-one engage-

ments (Gutter et al., 1996). Agility is achieved with post-stall maneuverability which allows the pilot to maintain flight control beyond the conventional stall angle of attack of the aircraft. The X-31 employs thrust-vectoring technology which enables the aircraft to reach very high angles of attack while simultaneously providing yaw (and pitch) control authority as the effectiveness of the vertical tail surface diminishes due to its immersion in the wake of the wing. With thrust-vectoring the X-31 was able to perform the aggressive combat maneuvers illustrated in Fig. 1.2. During each maneuver, the aircraft passes through a range of angles of attack that can reach 70 deg. Throughout this range, the flow field over the pointed forebody of an aircraft will undergo a series of changes, accompanied by an evolving aerodynamic side force (Fig. 1.3). As illustrated in Fig. 1.3, the flow field patterns are generally recognized to occur within four angle-of-attack regimes, the delineation of which depends typically on forebody geometry. At *low* angles of attack the forebody flow field is vortex-free and the side force is zero. There is also no side force in the *moderate* regime of angle of attack owing to the development of symmetric forebody vortices. When the aircraft reaches *high* angles of attack, the forebody vortices become asymmetric which gives rise to an aerodynamic side force of significant magnitude. At these attitudes an aircraft is susceptible to experiencing uncommanded, adverse yawing moments that can potentially lead to a sudden undesirable yaw departure of the vehicle. For *very high* angles of attack the side force diminishes as a wake-like flow field evolves over the forebody. The X-31 has encountered adverse yawing moments during flight test at high angles of attack and passive techniques of forebody vortex control, such as increased nose bluntness and strakes, were applied to alleviate the problem (Alcorn et al., 1996). For the F-22 (Fig. 1.4), the next generation of air combat aircraft, a chined forebody and a slightly blunted nose are combined to improve directional stability at angles of attack that are expected to reach 60 deg.

The problem of asymmetric forebody vortices has been known for many years. As the understanding of this complex flow over the forebody developed, the efforts of several early investigations were aimed at developing means of alleviating this undesirable condition. Over the last decade, however, the potential benefit of asymmetric forebody vortices was recognized and efforts have been refocused on developing methods of controlling

asymmetric side forces and yawing moments as an additional means of providing directional control of a fighter aircraft during a post-stall maneuver. If the asymmetric yawing moment arising from the aerodynamics of the forebody can be controlled effectively, then it might be possible to use it to replace the lateral control moments provided by the vertical tail which becomes ineffective in the high-angle-of-attack regime. Moreover, this additional control power might also be scheduled within the flight control system to augment the directional control provided by a vectored-thrust system. A variety of pneumatic and mechanical techniques have been devised to control asymmetric vortices. However, amid these developments, deficiencies in the understanding of the underlying flow physics continue to persist.

1.2 The Principle of Dynamic Manipulation of Forebody Vortices

Forebody vortex manipulation is an alternative means of providing directional control of fighter aircraft at high angles of attack. Most methods of forebody vortex manipulation are essentially steady schemes. That is, quasi-steady loads are generated by forcing the forebody vortices into a prescribed orientation with respect to the forebody so that desired control forces or moments are generated. Since forebody vortices are inherently bi-stable over a range of angle of attack, i.e., one vortex tends to remain close to the forebody while the other one lifts off, the steady methods involve first forcing the vortices to adopt a symmetrical stance, typically by tailoring the forebody geometry (such as employing an elliptical or chined cross-section, or by the addition of strakes) and then altering their positions by pneumatic or mechanical means to generate the desired control forces or moments. To overcome the artificially induced symmetry, considerable power may be required; furthermore, it may also be difficult to implement control laws due to the often severely non-linear behaviour of the vortices and resulting loads.

A *dynamic* vortex manipulation scheme has been developed that has the potential of avoiding the aforementioned problems. Rather than relying on an initial symmetric stabilization of the vortices, it takes advantage of their bi-stable nature and makes use of the

well-known fact that minute disturbances to the flow in the vicinity of the forebody tip will cause the vortices to switch between stable states. Specifically, the scheme requires the orientations of the forebody vortices to be deliberately switched, alternately, between their two stable states at a high enough frequency that the inertial characteristics of the aircraft prevent it from significantly responding to the instantaneous load. The aircraft can, however, respond to the time-average loads which may be controlled by varying the fraction of the switching-cycle period, or duty-cycle parameter (τ/T), during which the vortices are in one state or the other. The principle is illustrated in Fig. 1.5(a) and the effect of changing the duty cycle is shown in Fig. 1.5(b) (Lee et al., 1995). As a result, load modulation is accomplished with a time-varying perturbation of fixed intensity, as opposed to varying the intensity which is central to steady schemes. Ideally the technique requires that equal but opposite magnitudes of time-average yawing moment and side force be generated at $\tau/T=0\%$ and 100% , conditions under which the perturbation is applied to a given vortex throughout the switching period. If such symmetry exists, then the time-average yawing moment and side force would be expected to be zero at $\tau/T=50\%$ and vary linearly with τ/T over the range $0 < \tau/T < 100\%$, at least for cycle periods that are not too short compared to the time required by the vortices to change orientation. The deliberate flow perturbation can be implemented by either pneumatic or mechanical means.

Previous investigations of this technique are described in Section 2.2.3.

1.3 Objectives of the Research

The research reported in this thesis continues the investigation of the technique of dynamic manipulation of forebody vortices and focuses on three objectives:

- To ascertain whether the excellent results obtained on simple ogive-cylinder models could be replicated on a more realistic aircraft-like configuration, in particular, whether yawing moment and side force will be linear with duty-cycle parameter and to determine approximate upper limits of reduced frequency;

- To investigate the influence of axial and azimuthal location of forward-blowing nozzles on a forebody with a circular cross-section; and
- To investigate Reynolds number effects on the effectiveness of the technique.

This work was conducted using a completely experimental approach. Resource and time constraints did not permit augmentation of the investigation with a computational study.

Portions of the work have been published in several conference papers (Lee et al., 1999; Hanff et al., 1999; Lee et al., 2000a,b).

1.4 Outline of Thesis

Chapter 2 presents background information on forebody vortices. The nature of forebody vortices is discussed with regard to the crossflow and longitudinal development of the vortices, as well as the origins of vortex asymmetry, and how the vortices are affected by various parameters such as Reynolds number, Mach number, forebody geometry, and sideslip. Techniques to control forebody vortices and the effect of the vortices on the aerodynamic moments acting on a full aircraft configuration are also reviewed. Chapter 3 describes the principal part of the investigation that was conducted in a low-speed wind tunnel at the National Research Council Canada. The chapter describes the installation of the schematic delta-wing aircraft model, the set of forebody tip models, the data acquisition system, and the test program and procedures. Because the laser light-sheet apparatus in the low-speed wind tunnel was unavailable for use, an additional experiment was undertaken in the water tunnel facility at the University of Ottawa to further investigate, with dye-injection and laser-light sheet illumination, the cause of the unusual reversal of side force and yawing moment observed in the results obtained in the wind tunnel. Chapter 4 describes the experiment in the water tunnel. Chapter 5 presents and discusses the force and moment data acquired in the wind tunnel, and the flow visualization images recorded in the water tunnel. The conclusions of the current work are found in Chapter 6, and Chapter 7 offers some recommendations on the course of the research. Appendices appearing at the end of the thesis address valve timing issues; describe the calibration of the nozzle

flow rate and the calibration of the internal strain gauge balance; present the equations used for the data reduction process, and the geometric transformation equations between the conventional body-axis system and the total incidence plane, the system followed in the wind tunnel experiment.

(a)

(b) X-31 performing the Herbst maneuver

Figure 1.1. X-31 Enhanced Fighter Maneuverability Demonstrator. (*Photos, NASA Dryden Flight Research Center*)

(a) Herbst maneuver

(b) Mongoose

(c) Helicopter loop

Figure 1.2. Examples of post-stall maneuvers. (Braybrook, 1995)

Figure 1.3. Effect of angle of attack on forebody vortices. *Adapted from Ericsson and Reding (1986).*

(a)

(b) F-22 at high angle of attack during flight testing

Figure 1.4. F-22 advanced tactical fighter. (*Photos, U.S. Air Force*)

(a) Idealized time history of side force.

(b) Expected variation of a time-average load with duty cycle.

Figure 1.5. The principle of the dynamic manipulation of forebody vortices. (Lee et al., 1995)

Chapter 2

Background

Much has been written on the subject of vortices in the flow over pointed forebodies. The collection of literature is substantial, a reflection of many contributions by researchers from the experimental, computational, and flight test communities. Progressive reviews of the subject are found in Hunt (1982), Ericsson and Reding (1986, 1992), Malcolm (1993), and Williams (1997).

The intent of this chapter is not to embark on a comprehensive review of forebody vortices. Instead this chapter focuses on those aspects relevant to forebody vortex control. The nature of forebody vortices will be addressed, specifically the crossflow and longitudinal development of the vortices, the current thinking on the origins of vortex asymmetry, and effects of various parameters. Developments with control techniques since the review by Alexan (1992) will be discussed. A review of the effects of forebody vortices on the aerodynamic forces and moments of a full aircraft configuration, is also presented.

2.1 The Nature of Forebody Vortices

2.1.1 Effect of Angle of Attack

The flow field pattern on the leeward side of a pointed forebody undergoes a series of changes as the angle of attack of the forebody increases from 0 to 90 deg (Fig. 1.3). Generally the evolution of the flow field has been categorized into four distinct regimes, the boundaries of which have been generically identified by onset angles of attack (Ericsson and Reding, 1986). The bounding of the regimes suggests that the onset of a flow field

pattern can be dependent on the forebody itself. In fact, for pointed ogive forebodies with circular cross-sections, the onset angle of attack for asymmetric vortices (α_{AV}) in Fig. 1.3, was correlated with the semi-apex angle of the forebody, i.e., $\alpha_{AV} = 2.1\delta_N$, where δ_N is the semi-apex angle (Keener and Chapman, 1974).

For α -values less than the angle of attack for the onset of symmetric vortices (α_{SV}), the flow over the forebody remains attached although a thick boundary layer develops on the leeward side (Keener, 1986). The flow is symmetric in this flow regime, and the side force is zero. For $\alpha_{SV} \leq \alpha < \alpha_{AV}$, the crossflow separates on both sides of the forebody and the detached shear layer feeds vorticity to two symmetric primary vortices. These vortices originate at the apex of the forebody and extend along the length of the forebody on a track that closely parallels the longitudinal axis. Experimental measurements reported by Genxing et al. (1986) show that the axial velocity of the vortex core and circulation of the vortex are equal between the symmetric vortices and both quantities increase along the vortex track.

At α_{AV} the primary vortices are affected by an instability causing an asymmetry in the orientation of the vortices that develops near the base of the forebody. Viewed in the crossflow plane, one vortex is high off the leeward surface while the other vortex remains low and near the surface. As the angle of attack continues to increase through this regime, the asymmetry increases and propagates forward. For a pointed tangent-ogive cylinder model, Genxing et al. have found that the circulation of the vortices increases with angle of attack, and the circulation and the axial velocity of the lower vortex are greater than for the higher vortex. At some α , the high vortex sheds or detaches from the body and adopts a curved track that tends to align the vortex with the freestream. Although it is still attached at the apex, the vortex is considered shed or detached because it ceases to receive vorticity from the separated shear layer. The position of vortex shedding moves forward with increasing angle of attack (Keener, 1986).

The practical feature of this flow field regime is the significant side force that arises from the presence of asymmetric vortices. The side force is the result of an asymmetric pressure distribution which features a low pressure region due to the low vortex along one

side of the forebody. The side force is oriented toward the low vortex and its maximum magnitude has been found to be as high as 1.4 times the maximum normal force acting on a pointed tangent-ogive forebody at $\alpha = 55$ deg (Keener and Chapman, 1974). While the side force has a significant magnitude, the yawing moment due to this force is also substantial because the moment arm, relative to an aircraft centre of gravity, is long. The generation of side force on the forebody has thus attracted attention to the energetic vortical flow structures as a basis for developing alternative yaw control schemes for fighter aircraft at high angles of attack.

The asymmetric vortices are steady and bi-stable. As described in the previous chapter, the term bi-stable refers to a condition in which the asymmetric vortices are stable in one of two possible orientations — either the port vortex is high and the starboard vortex is low, or vice versa — but the state of either orientation has been found to be very sensitive to minute disturbances in the vicinity of the apex. Indeed, the forebody vortices are sensitive to very slight geometrical variations that inevitably arise, for example, from manufacturing. There is ample experimental evidence in the literature demonstrating the sensitivity of the vortices as the forebody is rolled through 360 deg (Thomson and Morrison, 1971; Pick, 1971; Keener and Chapman, 1974; Lamont, 1982b), and to deliberate surface perturbations introduced in the vicinity of the apex (Dexter, 1984; Moskovitz et al., 1988; Zilliac et al., 1991). The effect of introducing a symmetry-breaking perturbation on the surface of the apex was also numerically-simulated with success (Degani and Tobak, 1991).

Beyond α_{UV} , the angle of attack at which forebody vortices become increasingly unsteady, a random wake, similar to two-dimensional flow around a circular cylinder, develops near the base of the forebody and progresses forward as the angle of attack continues to increase. In such a flow, vortices are shed from the forebody in an alternating pattern that could be periodic. In the vicinity of the forebody apex, vortices have been observed to persist (Genxing et al., 1986; Keener, 1986), however, as α approaches 90 deg, the flow in this area eventually becomes immersed in the random wake. Generally within this flow regime, the mean side force is zero. Unlike α_{AV} , a correlation for predicting α_{UV} has not

appeared in the literature.

It is interesting to note the remarks of Ericsson and Reding (1986) regarding the diminishing axial flow component (as opposed to the increasing crossflow component of the freestream velocity) as the angle of attack increases. Specifically, Ericsson and Reding comment that “the axial flow component is still sufficiently large to produce steady vortices” within the flow regime that produces asymmetric vortices. The remark is notable for the suggestion that the axial flow component plays a crucial role in enabling a steady vortex arrangement, unlike the unsteady wake of a two-dimensional flow over a cylinder where vortices periodically shed downstream when the amount of fluid and vorticity in them accumulates to a high level.

2.1.2 Structure of the Leeward Flow Field

The illustration of the response of forebody vortices to changing angle of attack in Fig. 1.3 is a typical representation of a leeward flow field that is, in fact, quite elaborate. To understand this complex three-dimensional flow field, its development is examined separately in the crossflow and longitudinal planes.

Crossflow Development

The crossflow plane is a cross-sectional view of the leeward flow field taken perpendicular to the longitudinal axis of the model at a particular axial location. A schematic of the crossflow and a typical corresponding surface pressure distribution are presented in Fig. 2.1. Given that the pressure measurements were taken in the lateral plane, then clearly the pressure distribution is representative of the crossflow. Such may not be the case for the streamlines, however, since due to an axial flow component, fluid within a separated shear layer or a vortex may not originate from within the same plane. Regardless of this caveat, a crossflow schematic is considered to be a reasonable representation of the leeward flow field and, as a result, the artifice is a useful means of gaining some insight into its structure.

In Fig. 2.1 the crossflow component ($V_\infty \sin \alpha$) of the freestream velocity approaches

the windward side of the forebody where a stagnation point is created on the surface at $\theta \approx 0$ deg; in fact, Neuteboom (2002) observed that the location of the windward stagnation point was biased towards the side with the low vortex (i.e., the starboard vortex in Fig. 2.1). On both sides of the stagnation point, the crossflow near the forebody is diverted to follow the contour of the surface. Over this surface the crossflow accelerates, static pressure decreases, and vorticity develops within a thickening boundary layer. The pressure gradient on the starboard side of the section is steeper than on the port side, an immediate indication of an evolving asymmetric pressure distribution. For $\theta > 90$ deg the boundary layer encounters an adverse pressure gradient that develops as the crossflow begins to decelerate. Depending on the crossflow Reynolds number (based on local diameter), the boundary layer typically does not have sufficient momentum to overcome the adverse pressure gradient and separation of the layer will occur at the point of primary separation (S_1). A slight asymmetry of these separation points is observed in the pressure distribution. Fluid with vorticity leaves the body along the detached layer which curls to form a primary vortex above the leeward surface of the forebody. The starboard vortex is the closest of the two primary vortices to the leeward surface. Due to an inductive effect of the primary vortices, fluid is directed between the vortices towards the leeward surface; the flow reattaches at A_1 which is offset from $\theta = 180$ deg, towards the high vortex. The reattached flow moves outward and circumferentially on both sides of A_1 along the leeward surface of the forebody towards a region of low pressure set up by the primary vortex. Eventually the induced flow encounters an adverse pressure gradient which leads to a secondary separation point (S_2) and the formation of a secondary vortex located between the primary vortex and the surface, but rotating in a direction opposite to the primary vortex. As a result of the inductive influence of the secondary vortex, a second attachment point (A_2) appears between the primary and secondary points of separation. Smaller secondary vortical structures are known to exist between the primary vortex and the leeward surface.

Since the static pressure coefficient is lower over the entire starboard side of the forebody, it is clearly evident that the surface pressure distribution leads to a non-zero sectional side force. Contributing significantly to the side force is the local pressure minimum

occurring before S_2 as a result of the close proximity of the starboard primary vortex to the leeward surface. However, the asymmetric pressure distribution is closely connected with the steep pressure gradients that occur for $0 \leq |\theta| \leq 90$ deg, as indicated earlier. In particular, the steep gradient on the starboard side arises from the fact that the starboard boundary layer separates later than that on the port side. The delayed separation of the starboard boundary layer corresponds with a low starboard primary vortex.

Off-surface flow studies have shown that with increasing angle of attack the structure of the leeward flow field can become increasingly complex. Lowson and Ponton (1991) used smoke flow visualization to illuminate the leeward flow field over isolated conical forebodies with 5, 10, and 20 deg semi-apex angles. A sequence of flow topologies for a 5-deg conical forebody, was found to occur with increasing angle of attack (Fig. 2.2). Note that angle-of-attack cases α_A , α_B , and α_C correspond to the symmetric and asymmetric vortex patterns shown previously in Fig. 1.3. As α increases to α_D the starboard shear layer has extended significantly as a result of the asymmetry. The shear layer discontinues feeding the first primary vortex (i.e., the original starboard vortex) and forms a second primary vortex closer to the body on the starboard side. The first primary vortex is regarded as a free vortex because it is no longer fed by the separated shear layer*. Three primary vortices now appear in the wake of the forebody. After a slight increase to α_E the first primary vortex on the starboard side detaches from the second primary vortex and is *captured* by the first primary port vortex, as signified by the movement of the connecting stream surface. This capture process occurs simultaneously over the entire length of the forebody. Lowson and Ponton observed that if α is held at this capture state, the forebody tends to oscillate as the vortex patterns switch between those seen at α_D and α_E . Following another increase to α_F , the wake elongates above the forebody and the connected vortex system moves farther away from the leeward surface. The port shear layer now exhibits an instability and begins to form a second primary port vortex that is closer to the leeward surface than the second primary vortex on the starboard side. In effect a reversal

*The terms *first primary* and *second primary* vortices are due to Genxing et al. (1986) who have also observed the occurrence of multiple primary vortices on a tangent-ogive cylinder model with a 16.5-deg semi-apex angle.

in the orientation of the primary vortices closest to the forebody has occurred. The second primary vortices are expected to exert considerable influence on the pressure distribution over the forebody and, as a result, a reversal of side force should accompany the change in vortex orientation. With a slight increase to α_G the free vortex system is captured by the second primary starboard vortex. Once again the capture process occurs simultaneously over the length of the model and the flow field will tend to alternate between that seen at α_F and α_G if α is held near this critical condition. By α_H the alternating shedding of free vortices and model buffet continues to repeat in the aforesaid manner. Variations in this development occur with variation of semi-apex angle, i.e., with a larger semi-apex angle, each process described occurs at a higher angle of attack.

The leeward flow field has also been examined through a series of off-surface flow visualization experiments for an inclined ogival forebody with a semi-apex angle of 16.3 deg ($l/D=3.5$) (Ward and Katz, 1989a,b,c). These experiments were performed in a large tow tank and a laser light-sheet technique was employed to illuminate the crossflow structure. While confirming that the leeward flow field is asymmetric over the entire length of the forebody for a given angle of attack, Ward and Katz also concluded that it is “dominated . . . by multiple, large-scaled [steady] flow structures” that are “continuously generated” along the forebody (Ward and Katz, 1989c). Results similar to the findings of Lowson and Ponton are found in the work of Ward and Katz (1989a). In addition to corroborating the effect of increasing angle of attack (with Reynolds number fixed), Ward and Katz have found that steady, multiple vortices also develop with increasing Reynolds number at a fixed angle of attack of 40 deg or more. In either case of increasing α or Re_D , Ward and Katz observed the port primary vortex, for instance, lifting away from the leeward surface while in its formerly occupied space a second port primary vortex began to develop. As this second primary vortex evolved on the port side of the forebody, the starboard primary vortex subsequently began to lift away from the model surface. The movement of a primary vortex from the surface to a higher position is described as a process of detachment: the cross-section of a detaching primary vortex elongates, the streamlines break and start forming into two smaller vortices which recombine farther downstream. According to Ward and Katz, the detachment process is complete when

a primary vortex becomes contained by streamlines (as in α_D , Fig. 2.2) and does not receive fluid from the boundary layer of the forebody. Ward and Katz (1989c) also present results for surface pressure distributions. For $\alpha = 45$ deg the pressure distributions show a reversal of sectional side force between forward and aft-located crossflow planes. The reversal is attributed to the proximity of the primary vortices to the leeward surface, i.e., the side force is oriented in the direction of the primary vortex closest to the model. The result is an indicator of the presence of multiple vortices (i.e., greater than two) along the forebody.

Additional studies of the crossflow structure, using experimental and computational techniques, appear in the literature. For example, unsteady surface pressures and the crossflow velocity field, using a two-component laser Doppler velocimeter, were measured by Wardlaw and Yanta (1984) but the presence of multiple forebody vortices was not observed. Keener (1986) has inferred the crossflow by applying guidelines, suggested by Tobak and Peake (1982), to interpret the topology of surface flow patterns. In this study a Schlieren visualization method provided evidence of multiple vortices. Computationally, the surface pressure distribution has been accurately simulated for symmetric forebody vortices under laminar flow conditions (Degani et al., 1991) and asymmetric vortices under turbulent flow conditions (Degani and Levy, 1992). In these studies a third forebody vortex seems evident at high angles of attack with the aid of computed off-surface streamlines.

Longitudinal Development

The discussion of the crossflow structure has given some indication of the longitudinal development of forebody vortices, namely that vortices may shed alternately along the length of the forebody. A model describing this behaviour — and arguably the crossflow structure — exists.

Initially put forward by Allen and Perkins (1951) and cited frequently in the literature, the *impulse flow analogy* draws a similarity between two-dimensional viscous flow around a circular cylinder and the three-dimensional flow field over a forebody inclined at a high

angle of attack. The model relates the spatial development of forebody vortices, as viewed in the crossflow plane which moves aft from the apex of the forebody at the longitudinal component of the freestream velocity $V_\infty \cos \alpha$ (Fig. 2.3) — to the temporal development of vortices in a flow accelerated from rest over a circular cylinder[†]. The analogy has been used often to estimate the normal force acting on bodies of revolution for the purpose of predicting missile aerodynamic characteristics (Ericsson and Reding, 1986).

A typical longitudinal distribution of local side force coefficient (c_y) is presented in Fig. 2.4(a) for a tangent-ogive cylinder model at various angles of attack. The behaviour of c_y is similar to a decaying sinusoid which tends to *compress* towards the nose as α increases from 40 to 70 deg (Hunt, 1982). According to the impulsive flow analogy, the variations of c_y should collapse onto a single curve irrespective of the angle of attack. Lamont and Hunt (1976) have plotted extensive local side force measurements over a range of α against a non-dimensional time parameter ($\bar{t}$). The derivation of $\bar{t}$ is based on the non-dimensional time parameter for an impulsively started cylinder, Vt/R , where V is the flow velocity and R is the radius of the cylinder. As seen in Fig. 2.3, with a cylinder inclined at an angle of attack α , V becomes the crossflow component of the freestream velocity ($V_\infty \sin \alpha$) and the time for flow development t is $x/(V_\infty \cos \alpha)$. After substituting, one obtains $\bar{t} = (x/R) \tan \alpha$. The results for local side force coefficient reported by Lamont and Hunt for various angles of attack demonstrate sinusoidal behaviour with $\bar{t}$. However, the collapse of the data breaks down at high values of $\bar{t}$. Moreover, these results only reflect measurements taken over the cylinder portion of the model. To account for the effect of the nose, Hall (1987a) has suggested an “ad hoc form” of the impulsive flow analogy which uses a slightly different non-dimensional time parameter, $t^* = (x/r) \tan \alpha$. Hall argues that the local radius r is the appropriate length scale for the non-dimensional time parameter because, in the case of a cone, if near-conical flow conditions are to exist, then the time parameter must be constant along the length of the cone. For an ogive

[†]Momentarily following the start of fluid motion, the flow around a two-dimensional cylinder is separation-free and has the appearance predicted by potential flow theory. Separation takes place shortly afterwards and vortices begin to grow symmetrically. An asymmetry in the vortices gradually develops, followed eventually by shedding in the downstream direction. A visual example of this vortex development with time is given by Sarpkaya (1966)

forebody, however, t^* will not be constant along the length of the forebody. The effect of plotting c_y against t^* is illustrated in Fig. 2.4(b). Note that these results include several measurements taken over the length of the forebody. As highlighted previously, c_y varies sinusoidally when plotted against non-dimensional distance x/D over a range of angle of attack. When plotted against t^* , on the other hand, an excellent collapse of c_y onto a single curve is achieved, demonstrating the usefulness of t^* to correlate local side force coefficient.

The successful correlation of c_y with t^* has led Hall to believe that t^* can be used to identify vortex shedding occurrences, much like Thomson and Morrison (1971) can predict such occurrences with an equivalent two-dimensional Strouhal number. It is believed that a vortex is shed with each change in slope of local side force coefficient as depicted in Fig. 2.5 (Champigny, 1984). It is also evident in Fig. 2.5 that multiple vortices will appear in crossflow planes beyond the first shedding occurrence along the body.

2.1.3 Origins of Vortex Asymmetry

Although the subject of forebody vortices has been studied for more than thirty years, the cause of asymmetry at high angles of attack remains an unresolved issue (Williams, 1997). There is consensus in the literature supporting the belief that the symmetric flow field over a forebody is inherently unstable and susceptible to a small disturbance that triggers the vortices into an asymmetric orientation. The nature of the instability, however, is uncertain.

Keener and Chapman (1977) hypothesize that a pair of symmetric forebody vortices are unable to coexist side-by-side beyond a certain angle of attack, causing an inviscid instability to develop and subsequently lead to an asymmetry in the vortex formation. The basis for the hypothesis is a similarity in vortex asymmetries over a forebody and a delta-wing. From a comparison of experimental data, Keener and Chapman found that the side force exerted on a forebody and the rolling moment acting on a delta-wing, for a given angle of attack, increase with slenderness ratio after a certain onset ratio. Since vortices over a slender delta-wing are generated by flow separation at the leading edge at a mod-

erate angle of attack, the rolling moment must arise from asymmetrically-displaced wing vortices (oddly, no mention is made of possible contributions due to asymmetric vortex bursting over the wing). Since the separation point on the wing leading edge is fixed, the increase in rolling moment with slenderness ratio is thought to be related to increased crowding of the vortices. By way of analogy, Keener and Chapman believe the same conditions occur in the flow field over a forebody. In Woolward (1982) an inviscid analysis of the asymmetries over a forebody and a delta-wing is presented in support of Keener and Chapman. Applying slender body theory and conformal transformations of the crossflow plane, Woolward derived an approximate relation between the flow about a circular cone with symmetric separation points located arbitrarily, and a zero-thickness wing of similar planform shape with separation points fixed at the leading edge. He concluded that the onset of vortex-asymmetry for circular cones could be attributed to the presence of the hydrodynamic instability. But Woolward also conceded that the inviscid analysis merely sheds light on a complex flow phenomenon and is by no means conclusive, adding that asymmetric boundary layer separation has not been entirely ruled out as possible cause of vortex asymmetry.

Peake et al. (1980) postulate that the formation of an asymmetric flow field is centred on the conditions of the flow field at the enclosing saddle point above the forebody vortices (Fig. 2.6), and that irregularities of the model surface serve to determine the direction of the asymmetry. The hypothesis is based on the findings of Nishioka and Sato (1978) who identified part of the mechanism that initiates an asymmetric wake for a two-dimensional cylinder. Using the two-dimensional case as an analogy, Peake et al. rationalized that the development of vortex asymmetry on a forebody at high angles of attack may be connected to the deteriorating stability of the velocity profiles at the enclosing saddle point. They believe that the structural growth of a symmetric forebody wake continues with increasing angle of attack until, at a certain attitude, the flow near the enclosing saddle point becomes unstable and susceptible to small perturbations that could disrupt the symmetric orientation of the vortices. Peake et al. are not specific, but presumably a geometric imperfection on the forebody surface is an example of a symmetry-breaking disturbance. In Wardlaw and Yanta (1984) the results of velocity measurements in the

crossflow of a forebody are believed to support the hypothesis of Peake et al.

Confidence in numerically simulating the three-dimensional subsonic flow field over asymmetric bodies at high incidence has evolved to a degree that makes computational fluid dynamics (CFD) a useful research tool in the area of high-angle-of-attack aerodynamics. Based on CFD studies, Degani developed the belief that the onset of forebody vortex asymmetry is due to the presence of a “convective-type instability of the originally symmetric flow”. Initially, he thought that vortex asymmetry was “forced by space-fixed, time-fixed disturbances” after finding that the insertion of a small jet at the apex of the forebody was necessary to coerce a symmetric flow field — computed for a flawless axisymmetric forebody — into an asymmetric orientation (Degani and Schiff, 1987, 1991). The effects were found to be comparable to experimental findings. In subsequent studies the fluid jet was replaced with a small geometrical imperfection in the vicinity of the apex as the symmetry-breaking perturbation (Degani and Tobak, 1991; Degani, 1991). The vortex asymmetry was found to be small near the geometrical imperfection. However, beyond the point where the vortices detached from the forebody and adopted a curved track, the asymmetry in the vortices developed rapidly. It was then realized that a geometrical imperfection could not be the cause of vortex asymmetry because this mechanism did not account for the development of asymmetric vortices at a certain angle of attack, nor the growth of the perturbation as it convected downstream, or for the tendency of the flow field to return to its symmetric state after the perturbation was removed. Instead, the geometrical imperfection is a trigger that, if sustained with time, disturbs a convectively unstable symmetric flow which reforms into an asymmetric flow field.

2.1.4 Effect of Reynolds Number

The ad hoc form of the impulsive flow analogy developed by Hall successfully predicts the occurrences of vortex shedding with a non-dimensional time parameter, t^* (Fig. 2.4). The use of this formulation, however, is conditional. Its application is limited to slender forebodies and requires laminar flow conditions to prevail over the length of the forebody (Hall, 1987b). The formulation does not account for a change in local boundary layer

state to either transitional or turbulent flow conditions, both of which inevitably occur with increases in angle of attack or freestream velocity.

The longitudinal development of local side force coefficient is a useful quantity to demonstrate the influence of Reynolds number. The results from experiments conducted by Lamont on a tangent-ogive cylinder model ($l/D=3.5$) illustrate the effect of Reynolds number in Fig. 2.7 (Hall, 1987b). Consider the case of $\alpha=40$ deg in Fig. 2.7(a). For $Re_D=0.2 \times 10^6$, c_y is positive and reaches a maximum value on the cylindrical portion of the model. For $Re_D=0.8 \times 10^6$, the magnitude of c_y is lower but remains positive over the length of the forebody. The point of maximum of c_y has moved closer to the apex of the forebody compared to the previous Reynolds number case. For $Re_D=3.8 \times 10^6$, the magnitudes of c_y are reduced further and a sign change in c_y has occurred on the forebody at $x/D=3.0$. Once again the location of maximum c_y has moved closer to the forebody apex.

The feature to consider from the foregoing results is the forward migration of the point of maximum c_y with increasing Reynolds number and the impact of this movement on the total side force acting on the forebody. The forward migration of maximum c_y is also seen with the longitudinal development of c_y at $\alpha=50$ and 60 deg in Figs. 2.7(b) and (c) respectively. At these angles of attack, however, the tendency of c_y to change sign over the forebody is more pronounced and could lead to a sign change of the integrated side force acting on the forebody. A variation of total side force with Reynolds number raises two points of concern. First, predicting the full-scale behaviour of aerodynamic loads due to a forebody at high angle of attack based on wind tunnel data will be unreliable unless Reynolds similitude is enforced. Second, a fighter aircraft performing a post-stall maneuver may encounter a sudden reversal of forebody side force because Reynolds number is expected to increase during the maneuver (Ericsson and Beyers, 1997; Beyers, 1999).

The vortex shedding events are perhaps the key to expanding the understanding of the Reynolds number effect on forebody side force. Recall that the occurrence of maximum local side force coefficient is thought to be associated with the detachment of a primary vortex from the forebody (Fig. 2.5). Moreover, Keener (1986) has observed with

Schlieren photography that the shedding of a primary vortex moves forward towards the forebody apex with increasing Reynolds number. A second primary vortex was found to evolve close to the surface of the forebody underneath the shed vortex. Aft of the shedding location, a change of vortex orientation has effectively taken place and the local side force coefficient is expected to decrease from the maximum magnitude, possibly followed by a change of sign. It appears that the presence of transitional boundary layer separation is connected with the shedding of the primary vortex.

Mixed boundary layer separation conditions are expected to evolve because the local Reynolds number will change along the length of a forebody due to the variation of local diameter. In the extreme case, laminar, transitional, and turbulent separation conditions can exist simultaneously as illustrated by the flow field model envisioned by Keener (1986), which is based on surface flow visualization results (Fig. 2.8). The surface pressure distributions shown in Fig. 2.9 are evidence of mixed boundary layer conditions. The pressure distributions pertain to the aforementioned $3.5D$ tangent-ogive cylinder model tested by Lamont at $\alpha = 40^\circ$ for $Re_D = 0.8 \times 10^6$. Results are shown for three longitudinal stations along the forebody and the condition of the boundary layer at each station was inferred from a comparison against typical pressure distributions for laminar, transitional, and turbulent boundary layer separation conditions for a two-dimensional cylinder. For instance, at $x/D = 0.5$, Fig. 2.9(a), laminar separation conditions are found to prevail; at $x/D = 2.0$ transitional separation conditions exist; and at $x/D = 3.5$, the base of the forebody, separation of the boundary layer is considered to occur under turbulent flow conditions. The contribution of the integral of each pressure distribution to the longitudinal distribution of c_y is shown in Fig. 2.9(d). Note that at $x/D = 2.0$, where transitional separation conditions prevail, c_y is slightly less than the maximum magnitude. Also, the location of maximum c_y precedes $x/D = 2.0$. This suggests that a region with a state of transitional boundary layer separation may be connected with the departure of the primary forebody vortex. Specifically, the effect may be due to a reduction of vorticity which occurs between the laminar and transitional flow regimes (Keener, 1986). In the transition region (Fig. 2.9(b)) the separation of the boundary is delayed, occurring at a larger azimuthal angle than in the laminar region. Owing to the pressure recovery at the

larger azimuthal angle, the velocity and hence the vorticity flux released at the point of separation is lower than in the laminar flow region. The reduction of vorticity leads, it is thought, to a disruption in the vorticity sheet feeding the first primary vortex, allowing this vortex to become *free* and move away from the leeward surface of the forebody. The delayed separation of the transitional boundary layer may also be a factor aggravating the continuity of the vorticity sheet.

The orientation of the total side force due to the forebody has been shown to be sensitive to Reynolds number. A “moving three-dimensional transition front” (Beyers, 1999) is suspected of being responsible for a deterioration of the vorticity sheet feeding the first primary vortex. The deteriorated sheet allows the vortex to shed, leading to an effective switch of vortex orientation downstream of the shedding point. If this conjecture is correct, then Keener’s flow field model requires modification. In Fig. 2.8 the model should depict one primary vortex being shed at the transition region while a second primary vortex evolves beneath it.

Lamont (1982a,b, 1985) found that Reynolds number affects the maximum magnitude of asymmetric side force. A typical variation of maximum overall side force is illustrated in Fig. 2.10 for a tangent-ogive cylinder model at $\alpha = 55$ deg. The side force at each Reynolds number in Fig. 2.10 is the maximum side force identified in a sample of different roll angles. In the laminar and fully turbulent separation regimes, the maximum overall side forces are on the same order of magnitude and behave somewhat similarly with Reynolds number. A significant reduction of maximum side force in the transition region is a noteworthy feature of the findings. Although the results in Fig. 2.10 show that maximum overall side force is greatest in the laminar or the fully turbulent flow regimes, they conceal the fact that for a fixed roll angle, the side force can potentially change orientation with increasing Reynolds number as previously discussed.

A Reynolds number effect that is beneficial to forebody vortex manipulation schemes, is the fact that in the laminar region the boundary layer thins with increasing Reynolds number (Hall, 1987b). The advantage is that at a high Reynolds number the flow field becomes more sensitive to perturbations introduced in this vicinity. Pneumatic or me-

chanical means of vortex manipulation should be placed in the laminar flow region so that the techniques are effective with minimum effort.

2.1.5 Effect of Forebody Geometry

The geometry of a forebody is described by the planform (e.g., tangent-ogive or cone), the cross-sectional shape (circular, elliptical, or chined), nose bluntness (the ratio of nose radius to forebody base diameter or radius), and the semi-apex angle and slenderness ratio (the ratio of forebody length to the base diameter). These features are known to affect the onset angle of attack and the magnitude of the asymmetric side force.

The asymmetry onset angle of attack (α_{AV} , Fig. 1.3) is largely affected by the semi-apex angle of the forebody. As mentioned earlier, the onset angle of attack was correlated with the semi-apex angle (or the slenderness ratio) for an isolated pointed forebody (Fig. 2.11). Note that the semi-apex angle can be expressed as a function of slenderness ratio for tangent-ogive and conical planforms. The presence of a cylindrical afterbody, however, tends to lower the onset angle below the value predicted by the correlation; for an aircraft, the onset angle is at least the value derived from the correlation or higher because the “presence of the wing tends to reduce or eliminate the influence of [the] afterbody on asymmetric side forces” (Keener and Chapman, 1974). Cross-sectional shape and nose bluntness also affect the onset angle of attack. An elliptical forebody, with the minor-axis or the major-axis horizontal, has a lower onset angle than a circular forebody for a given semi-apex angle[‡]. Nose bluntness, on the other hand, tends to delay the onset angle of attack (Keener and Chapman, 1974).

Asymmetric side force is influenced by slenderness ratio, nose bluntness, and cross-sectional shape. The side force tends to increase with slenderness ratio for pointed or slightly blunted forebodies, a trend that is attributed to differences in the separation characteristics of the crossflow boundary layer (Pick, 1971). On a forebody with a high slenderness ratio, flow visualization revealed that the crossflow boundary layer separates ear-

[‡]For elliptical forebodies a semi-apex angle is associated with the major- and the minor-axis. Keener and Chapman (1974) tested circular forebodies with corresponding semi-apex angles.

lier (i.e., a circumferential distance closer to the windward meridian) than for a forebody with a low slenderness ratio. As a result, the early boundary-layer separation releases higher vorticity that feeds more intense vortices, leading to an asymmetric side force of greater magnitude on a high-slenderness forebody. Nose bluntness reduces the magnitude of asymmetric side forces (Fig. 2.12) owing to the formation of a laminar separation bubble on the blunted nose. The presence of the separation bubble causes the flow to become turbulent and reattach on the leeward side of the model, thereby delaying the formation of the vortices (Pick, 1971). Caution has been raised about the limited effectiveness of nose bluntness — beyond a certain bluntness ratio, the effectiveness of blunting to alleviate asymmetric side force may be reduced. A comparison of circular, elliptical, and chined isolated forebodies revealed an effect of cross-sectional shape on side force (Roos and Kegelman, 1991). All three models shared the same slenderness ratio and the planform of a tangent-ogive forebody. For laminar-separation conditions, the forebody with a circular cross-section developed a significantly higher asymmetric side force compared to the elliptical forebody. For turbulent-separation conditions, the maximum side force was roughly the same for both cross-sectional shapes, but the maximum value for the elliptical forebody occurred at a lower angle of attack. The chined forebody did not develop asymmetric side force under either separation condition, largely due to the sharp edges (characteristic of the chined cross-section) that fix the points of separation.

2.1.6 Effect of Mach Number

In general, for subsonic flow, side force coefficient has been found to decrease with increasing Mach number. Experimental evidence indicates that Mach number affects the shape of the variation of side force with changing angle of attack (Fig. 2.13). The results also show that Mach number does not strongly affect the onset angle of attack for asymmetric vortices and for wake-like flow. The trend of decreasing C_Y is also reflected in a survey of maximum C_Y with Mach number for various forebody and apex planform shapes. Figure 2.14 shows that regardless of profile, the maximum side force coefficient decreases to zero. In Fig. 2.14(b), maximum side force reduces to zero for Mach num-

bers between 0.8 and 1.2, although a non-zero side force was reported within this range for a 20-deg cone. A similar claim can be made in Fig. 2.14(c) if the envelope, encompassing the maximum side force data, is extrapolated to $C_Y = 0$. Based on their findings, Keener et al. (1976) have identified a maximum Mach number boundary beyond which side force and yawing moments have been reduced to zero. The boundary is expressed as $M_0 \sin \alpha = 0.5$, where M_0 is the freestream Mach number, and it is shown in Fig. 2.15 superimposed over the four regimes of forebody wake flow. The horizontal lines bounding the flow regimes in Fig. 2.15 imply that the onset angles of attack for each regime are not significantly affected by Mach number.

Ericsson and Reding (1986) attribute the elimination of side force at high subsonic and supersonic Mach number to compressibility effects. Ericsson and Reding invoke an analogy with a circular cylinder in a two-dimensional compressible flow in which the *drag bucket*, observed at low Mach number, is no longer evident at high subsonic Mach number. For a forebody at high angle of attack and high subsonic Mach number, Ericsson and Reding contend that at some circumferential position on the forebody, such as the lateral meridian, the local flow velocity will reach sonic conditions and a normal shock wave will form. An adverse pressure gradient due to the presence of the shock wave can cause the boundary layer to separate. Presumably the formation of shock waves would be symmetrical on the forebody and the forced separation of the boundary layer will lead to symmetric vortices. Evidence that somewhat supports the contention of Ericsson and Reding can be found in Keener (1986). For $M = 0.8$, $Re_D = 0.8 \times 10^6$ and using vapour-screen as an off-surface flow visualization method, Keener observed symmetric vortices and zero side force for a tangential-ogive forebody ($l/D = 3.5$) up to an angle of attack of 55 deg, at which point the vortices displayed unsteadiness. Keener did not report the presence of shock waves, and neither was there any indication that shock waves were expected under these flow conditions.

A post-stall maneuver will typically be performed by an agile fighter aircraft at a low subsonic Mach number. The maneuver Mach number could reach as high as 0.42 based on the example of the X-31 — the airframe of the X-31 was designed for flight at

$\alpha = 70$ deg up to 265 kts, or Mach 0.42 at 4,570 m altitude (Braybrook, 1995). As such, the forebody vortices should remain asymmetric and the maximum side force coefficient should be unaffected throughout the Mach number range. Yet, in view of Fig. 2.13, Mach number effects should be expected to influence the variation of side force coefficient with angle of attack.

2.1.7 Effect of Sideslip

In Fig. 2.16 typical results for a pointed tangent-ogive forebody at a high angle of attack depict the effect of sideslip on side force coefficient. A discontinuity in the variation of C_Y with sideslip angle (β) — represented by a change of sign for C_Y between $\beta = 5$ and 15 deg — is a prominent feature of these results. The change of sign for C_Y and the repeatability of the results reflect a switch of vortex orientation arising from the effect of the change in location of a geometrical imperfection in the vicinity of the forebody apex relative to the flow field, within the range of β . Such changes of vortex orientation inevitably occur as a forebody is rolled 360 degs about its longitudinal axis.

Despite the switch of orientation, the magnitude of C_Y is observed in Fig. 2.16 to decrease with increasing β , i.e., $\partial C_Y / \partial \beta < 0$. A flow model reflecting the variation is presented in Fig. 2.17. The model is based upon flow visualization results showing that the windward stagnation point effectively rotates from the windward meridian of the forebody as the angle of sideslip increases from zero (Brandon and Nguyen, 1988). The primary separation points and the leeward flow field rotate accordingly with the windward stagnation point. Put another way, the *crossflow* plane (axes y_c - z_c in Fig. 2.17) rotates with respect to the fixed *forebody* plane (axes y - z) in a sideslip condition. At $\beta = 0$ in Fig. 2.17 the orientation of the asymmetric forebody vortices gives rise to a negative side force corresponding to the results in Fig. 2.16. The ratio of side force to normal force of approximately 1.0 is reflected by the vectors representing the sectional quantities (Ericsson and Reding, 1992). Lateral and normal sectional forces (Y_c and N_c , respectively) are shown to develop in the crossflow plane for non-zero sideslip angles β_1 and β_2 . As β increases from zero to β_1 and β_2 , the effect of sideslip is revealed as a component of N_c

increasingly contributes to reducing the magnitude of total negative sectional side force in the forebody plane. For positive sideslip, assuming the vortex orientation is unchanged, a component of the sectional normal force will contribute to increasing the magnitude of total negative sectional side force.

2.2 Control Techniques

The control techniques that have been investigated generally rely on the premise that the orientation of the forebody vortices can be manipulated by either promoting or delaying the separation of the boundary layer. These methods can be broadly classified according to the means by which separation control is exerted, namely by pneumatic or mechanical means.

2.2.1 Pneumatic

Pneumatic techniques control boundary layer separation by either introducing (blowing) or removing air (suction) from the forebody flow field. Blowing through nozzles or slots essentially classifies this form of pneumatic control as a fluidic device, i.e., control of a high-energy stream (the forebody crossflow) is achieved via an interaction with a low-energy fluid stream (the blowing nozzle or slot).

The blowing or suction rate is the primary means of influencing primary separation and modifying the orientations of the forebody vortices. An appropriate scaling parameter is required to non-dimensionalize the rate so that the side force and yawing moment coefficient will correlate well with such a parameter across Reynolds number, Mach number, and model scale (Kramer et al., 1993). Two scaling parameters for blowing rate appear in the literature. One form is the coefficient of blowing momentum,

$$C_\mu = \frac{\dot{m}_j V_j}{q_\infty S_{ref}} \quad (2.1)$$

where $\dot{m}_j$ is the nozzle mass flow rate, V_j is the blowing nozzle exit velocity, q_∞ is the freestream dynamic pressure, and S_{ref} is the reference area. An alternative form is the mass flow coefficient,

$$C_{\dot{m}} = \frac{\dot{m}_j}{\rho_\infty V_\infty S_{ref}} = \frac{\dot{m}_j}{\dot{m}_{ref}} \quad (2.2)$$

where ρ_∞ and V_∞ are freestream density and velocity, respectively. The mass flow coefficient is also referred to as the mass flow ratio. The difference between the two parameters is that for C_μ the blowing rate is proportional to $\rho_\infty V_\infty^2$, whereas for $C_{\dot{m}}$ the blowing rate is proportional to $\rho_\infty V_\infty$.

Blowing Jet. Fluid is forced through a circular orifice on the surface of the forebody, forming a jet which interacts with the boundary layer. Three variations of this technique are shown in Fig. 2.18:

- *forward blowing* – nozzles point predominantly forward;
- *aft blowing* – nozzles point aft but often canted inward so that the jet is directed over the leeward portion of the forebody; and
- *normal blowing* – the orientation of the nozzle exits is normal to the model surface.

Except for the work reported by Peake et al. (1980), normal blowing appears to have not received the degree of attention given to forward and aft blowing in the literature. For this reason the normal blowing technique will not be considered further.

Forward blowing promotes boundary layer separation. Placed in the vicinity of the forebody apex, a nozzle jet acts as a perturbation that reduces the streamwise momentum of the near-wall flow and also effectively changes the local geometry of the forebody surface. This is expected to cause the forebody vortex on the blowing side to lift away from the surface. This action is somewhat similar in nature to that of deployable strakes, a manipulation technique described later. With the upward movement of the vortex on the blowing side, the adjacent vortex moves down creating a high-suction region and giving rise to a side force and yawing moment that is directed away from the blowing side.

The primary mechanism for this control effect is believed to be related to the interaction between the boundary layer flow and the nozzle jet. Specifically, the momentum of the boundary layer flow opposes the momentum of the nozzle jet causing a “stagnation zone” to develop upstream of a nozzle exit and forces the local boundary layer to separate from the surface (Ng and Malcolm, 1991a). This effect has been described as a “viscous fairing effect” (Ericsson and Beyers, 2000) and a “displacement effect” (Roos and Magness, 1993).

Roos and Magness (1993) investigated forward-blowing nozzles with a unique forebody model (Fig. 2.19). A tangent-ogive blended with a hemispherical cap at its apex, this model combined forward blowing with nose bluntness which is known to reduce the effect of vortex asymmetry on side force and yawing moment. The model had a base diameter of 3.0 inches (7.62 mm, $l/D=3.5$) and a nozzle diameter of 0.051 inches (1.30 mm, $d/D=0.017$). Each nozzle was aligned parallel with the longitudinal axis of the forebody and the exits were located at the point of tangency between the ogive surface and the hemispherical cap. The response of side force to steady blowing over a range of angle of attack is shown in Fig. 2.20. Steady blowing from forward-facing nozzles gives rise to increasing side force for $\alpha > 40$ deg; the response of side force to blowing from either nozzle is nearly symmetric and reaches a maximum at an angle of attack just beyond 50 deg. Beyond this maximum the side force remains significant but erratic as an unsteady wake evolves at higher angles of attack. Figure 2.21 shows that the variation of C_Y with C_m at constant angle of attack is insensitive to Reynolds number. The results indicate that very little blowing through a forward-facing nozzle is required to produce an asymmetric flow field over the forebody, and a measure of proportional control is apparent with the control parameter C_m . This work was extended to the forebody of a 10%-scale F-15E model (Roos, 1996). Again, significant yawing moments were generated and controlled with a small blowing effort. Roos has also applied the forward-blowing technique in a “pulsing” fashion (Roos and Manor, 1997). A brief review of this work appears later.

Additional investigations of the forward-blowing technique have been reported by Peake et al. (1980) and Zeiger and Telionis (1997). The work by Zeiger and Telionis is

noteworthy because the forward-blowing nozzles were angled 20 deg with respect to the longitudinal axis of the tangent-ogive model.

Aft blowing, typically from nozzles pointed inboard, has the effect of delaying boundary layer separation. In fact, blowing aft across the forebody vertical plane of symmetry leads to an interaction between the nozzle jet and the forebody vortex on the nonblowing side (Cornelius et al., 1994). The interaction forces the separation line windward on the nonblowing side and pushes the associated primary vortex farther away from the surface, thereby allowing the vortex on the blowing side to settle closer to the vertical plane of symmetry (Fig. 2.22). As a result, the crossflow boundary layer on the blowing side remains attached over a greater extent, creating a large suction region which leads to side force and yawing moment that are directed toward the blowing side. This effect is opposite to that expected for forward-blowing nozzles. The protrusion of aft-blowing nozzles on the forebody surface is seen to pose two potential problems: the protrusions would constrain the placement of the nozzles in the vicinity of the forebody apex and also produce high drag.

Aft-blowing nozzles investigated for the X-29 aircraft are shown in Fig. 2.23. The nozzles were designed to swivel so that blowing could be directed straight aft or canted inboard. A notch at the nozzle exit, seen in Fig. 2.23(b), was found to allow “the jet to expand supersonically into a two-dimensional sheet which provides for greater aerodynamic interaction between the expanding jet flow and the flow field about the forebody” (Cornelius et al., 1994). Note that low aspect ratio strakes were necessary on the X-29 to stabilize the vortex flow field.

The aft-blowing control scheme was investigated on a 1/8-scale X-29 forebody in isolation and with a full aircraft configuration (Guyton and Maerki, 1992; Cornelius et al., 1994). The results presented in Fig. 2.24 show yawing moment coefficient plotted against angle of attack for various nozzle pointing angles. For pointing angles less than 90 deg, yawing moment coefficient increases with angle of attack. Slight asymmetries in these variations were found to occur between starboard and port blowing (Fig. 2.25). A more important observation from the results in Fig. 2.24, however, is that as the pointing angle

increases (i.e., the nozzle points inboard) the yawing moment coefficient increases at a given angle of attack. The investigators concluded the optimum pointing angle to be 60 deg, at which the highest levels of yaw control were achieved. Clearly, nozzles blowing straight back (0 deg) were least effective in generating yawing moment. With nozzles at the optimum pointing angle, yawing moment coefficient was found to increase non-linearly with C_μ (Fig. 2.26). Compared to the forward-blowing technique (in particular, the results reported in Chapter 5), the C_μ required for the aft-blowing technique appears to be relatively high.

The optimum nozzle geometry tested on the X-29 was also applied to the forebodies of other fighter aircraft. For a 6%-scaled isolated F-16 forebody, the largest yawing moments were also achieved with nozzles canted inboard at 60 deg (Mosbarger, 1994). But on a 10%-scaled F-15E model, the optimum pointing direction was found to be aftwards and outboard (Iwanski and O'Rourke, 1996). In this case, it is presumed the primary vortex on the blowing side is displaced by the jet. Other aft-blowing nozzle designs were tested on a 6%-scale F/A-18 model (Kramer et al., 1993), on a full-scale F/A-18 (Lanser and Meyn, 1994), and on a 10%-scale F-16 forward fuselage (Smith and Ng, 1994b). Again it was generally found that nozzles pointed 60 deg inboard was the best orientation. Lanser and Meyn, however, found blowing straight aft was more effective.

Slot Blowing. This pneumatic technique involves blowing fluid tangentially to the surface of the forebody through a narrow slot, which is typically located 90 deg from the windward meridian (Fig. 2.27). The subsequent interaction of this jet with the crossflow has the effect of encouraging the boundary layer to remain attached on the blowing side to a greater extent, thereby creating a large suction region which leads to a side force and yawing moment that are directed towards the blowing side. The Coanda effect — the tendency of a tangentially-oriented jet to remain attached to a surface curving away from the axis of the jet — is accepted as the underlying fluid mechanism of the slot-blowing technique. The effect adds the momentum of the slot-jet to the momentum deficit in the crossflow boundary layer, causing the separation of the boundary layer to be delayed. As a result, the forebody vortex on the blowing side lies close to the leeward surface; the

vortex on the non-blowing side is forced to the higher position, enhancing the production of side force.

Wood and Crowther (1994) liken slot-blowing to the circulation control technique applied elsewhere in aerodynamics (Kind and Maull, 1968). In fact, they maintain that forebody vortices are not necessary for the production of side force, specifically for $\alpha > 60$ deg. At very high angles of attack, slot blowing has the potential to exert yaw control unlike the forward-blowing jet schemes. Slot blowing will suppress unsteady vortex shedding at this high angle of attack, they argue, because “the separation points become dependent primarily on the wall jet attachment and are relatively insensitive to crossflow instabilities”.

Wood and Crowther tested slot blowing on a model of a schematic combat aircraft, which featured a 75-deg leading-edge extension preceding a 40-deg delta-wing, and a tangent-ogive forebody with $l/D=4.0$. Side force and yawing moment results demonstrate that control is achieved over a range of angle of attack which extends beyond 60 deg (Fig. 2.28). Although slot blowing is effective to a higher angle of attack compared to forward-blowing nozzles, these results reveal a deficiency in the symmetry of the responses. The variation of yawing moment with C_μ for three angles of attack is shown in Fig. 2.29. For $\alpha=20$ deg yawing moment is proportional to C_μ although a slight non-linearity at low C_μ is observed; at $\alpha=50$ deg the non-linearity at low C_μ has developed into a reversal of yawing moment, however, beyond $|C_\mu| \approx 0.007$ a proportional relationship between yawing moment and C_μ is evident. The cause of this reversal was not clear to Wood and Crowther. At $\alpha=70$ deg the yawing moment reversal at low C_μ vanishes, leading to the belief held by Wood and Crowther that the occurrence of the reversal is limited to the angles of attack for steady asymmetric vortices. For the model investigated, the best configuration found was a slot of medium length ($l_s/l=0.43$) located away from the apex ($x/l=0.32$) at $\theta=90$ deg. With $|C_\mu|=0.008$ (Fig. 2.28), the blowing effort required for slot blowing is comparable to that for the aft-blowing scheme.

Slot blowing was also tested on a F/A-18 at full-scale in a wind tunnel (Lanser and Murri, 1993; Lanser and Meyn, 1994). This investigation produced results in agreement

with the findings of Wood and Crowther: Yawing moment was found to increase with angle of attack and C_m ; a small yawing moment reversal occurred at low C_m ; and the most effective slot was of *medium* length, located aft of the forebody apex at $\theta = 90$ deg. Lanser and Meyn suggest that the necessity of locating a slot away from the forebody apex may be due to a diminishing Coanda effect in the vicinity of the apex where nose radii are smaller. Additional investigations of the technique were conducted on a 1/15-scale of a F-16 model (Lemay et al., 1992); a 6%-scale F/A-18 model (Kramer et al., 1993); a 10%-scale F-16 forward fuselage (Smith and Ng, 1994b); and on a 1/9-scale model representing a next-generation fighter, which featured a chined forebody (O'Rourke and Ralston, 1996).

Suction. The effect of suction control on the forebody flow field is similar to that of slot blowing (Fig. 2.30). Suction encourages the crossflow boundary layer to remain attached to a greater extent over the forebody surface, creating a large low-pressure zone on the suction side. The vortex on the suction side lies closer to the leeward surface of the forebody while the adjacent vortex moves to the higher position. Side force and yawing moment are directed towards the suction side. The fluid mechanisms are also similar in that these techniques produce a fuller velocity profile in the crossflow boundary layer which delays separation on the control side. The approach to altering the velocity profile, however, is different: Whereas slot blowing energizes the momentum-deficient portion of the boundary layer, suction removes the decelerated fluid which is replaced with higher-momentum fluid from the upper region of the boundary layer.

Suction control has been applied through narrow slots and discrete holes located in the forebody apex. The latter form of control was demonstrated on a 4/9-scale version of the RAE High Incidence Research Model (HIRM) (Ross et al., 1990, 1991). The suction source on this model were two 0.030 inch-diameter (0.76 mm) holes aligned normal to the surface of the forebody and connected to separate chambers inside the tip (Fig. 2.31). The holes were located 0.160 inch (4.06 mm) from the forebody apex and 150 deg in azimuth from the windward meridian. Figure 2.32 presents the variation of yawing moment coefficient with angle of attack for constant mass flow rate coefficient, $C_Q = 2.5 \times 10^{-6}$.

The increase of C_n with α , due to either port or starboard suction, is almost linear and symmetrical about $C_n=0$ [§]. To compare C_Q with the blowing effort required for forward-blowing nozzles (i.e., the work by Roos and Magness), the wing reference area must be altered to reflect forebody base area. With this change, C_Q becomes 5.3×10^{-5} which compares favourably with $C_m=11 \times 10^{-5}$ (Fig. 2.20) and indicates that the suction effort is remarkably small.

The variation of C_n with C_Q at different α is shown in Fig. 2.33. As suction increases from $C_Q=0$ on one side, C_n increases non-linearly until an asymptotic magnitude is reached. These maximum values are reasonably symmetric about $C_n=0$. As should be expected, $C_Q=0$ is associated with a non-zero C_n which increases with angle of attack. Thus, in order to maintain $C_n=0$ at high α , starboard suction is required.

An explanation for the success of this form of forebody flow field control may be found in the work of Bauer and Hemsch (1994). In an attempt to alleviate the production of side force at high angle of attack, Bauer and Hemsch tested two tangent-ogive cylinder models with forebody slenderness ratios of $l/D=2.5$ and 5.0 . A distribution of 0.020 inch-diameter (0.51 mm) holes covered 22% of the surface area of each forebody. The porous surface drew in air from the flow field into a single plenum inside the forebody cavity. Bauer and Hemsch believe that a porous surface connected to a plenum “[reduces] the circumferential surface pressure gradients” which, in turn, “[reduces] the generation of vorticity in the boundary layer”; in addition, the equalization of pressure over the porous surface on both sides of the forebody would also tend to equalize any generated vorticity. For the small amount of porosity, results showed a remarkable reduction of side force to near-zero magnitudes up to $\alpha=50$ deg, the highest angle tested. In view of this finding, the suction ports used by Ross et al. may effectively represent a large degree of porosity in the apex region where the surface area is small and the flow field is highly sensitive to perturbations.

Suction has also been applied through slots located in the vicinity of the forebody

[§]Concern for the safety of the model limited the range of angle of attack in this investigation to about 40 deg.

apex. An investigation of this approach on a 6%-scale F/A-18 model found that short slots provide the most effective control, and $|C_\mu| < 0.001$ gives rise to maximum side force at zero sideslip (Ng and Malcolm, 1991a).

Pulsed Blowing. The blowing schemes discussed previously require the steady application of air to maintain a certain orientation of the forebody vortices. Blowing continuously, however, has been found to be unnecessary. Pulsed blowing is a technique which takes advantage of the time-lag of the vortex system, which is the total time for the forebody surface flow to react to a blowing perturbation and for the vortices to switch orientation. If the frequency of pulsed blowing is sufficiently high (i.e., the pulse period is shorter than the time-lag), then it is possible to maintain a desired vortex pattern with repeated perturbations. The potential advantage of this approach is that the demand on engine bleed air or stored air could be less than the requirements for steady blowing.

The feasibility of pulsed blowing was demonstrated in a water-tunnel investigation with a F-16 model fitted with aft-blowing nozzles (Malcolm and Ng, 1991). Flow visualization revealed that the forebody vortices become essentially steady and their orientation appears *locked* with a sufficiently high pulse frequency. For a low pulse frequency, the period of which is longer than the time-lag of the forebody vortex system, the orientation of the vortices fluctuates at a rate similar to the pulse frequency. The investigation also revealed that the time-lag is largely comprised of the time required by the vortex system to change orientation because the response of the surface flow to a blowing perturbation was found to be immediate.

The effect of pulsed blowing on yawing moment coefficient was examined in a wind-tunnel investigation involving a 10%-scaled F-15E model (Sedor, 1996). The model was fitted with a pair of aft-blowing nozzles canted inwards towards the longitudinal plane of symmetry. Pulse frequencies as high as 200 Hz were possible with the blowing apparatus. The effectiveness of pulse frequency on yawing moment coefficient is demonstrated in Fig. 2.34 over a range of angle of attack and two blowing momentum coefficients. For $C_\mu = 0.017^{\dagger}$, pulsed blowing gives rise to variations of yawing moment coefficient that

[†]Presumably the value for C_μ was set under steady blowing conditions.

are comparable with the results for continuous blowing. At a lower blowing momentum coefficient, however, pulsed blowing generally tends to perform better than continuous blowing for $44 < \alpha < 80$ deg but overall the magnitudes for C_n are lower than encountered at the higher C_μ . The investigation also found that pulsed blowing mitigates an adverse rolling moment that arises under continuous blowing conditions.

The effect of pulsed forward-blowing on side force is reported in Roos and Manor (1997). The forebody model employed for this wind tunnel test was a tangent-ogive combination used previously by Roos (Section 2.2.1). The investigation is notable for the measurement of the time-lag of the vortex system (t_v), which the investigators define as the time between the onset of nozzle flow and the side force response. The time-lag is expressed in normalized form as

$$t^* = \frac{t_v}{\tau} \quad (2.3)$$

where $\tau = D/V_\infty \sin \alpha$ is the convective time-scale of the forebody crossflow. The response of the vortex system to a blowing perturbation was found to be approximately five convection units (τ) and was reasonably constant with V_∞ for $\alpha = 50$ and 60 deg. For pulse frequencies of 1.2 and 30 Hz at $\alpha = 58$ deg, the side force is shown to successfully oscillate between the baseline magnitude (no blowing) and the maximum magnitude (steady blowing) in response to periodic blowing perturbations. The investigators, however, were not convinced of the utility of pulsed blowing because the time-averaged side force fell short of the maximum magnitude developed under steady blowing conditions. But the shortfall may be attributed to a low pulse frequency which allows the forebody vortices to reach their no-blowing orientation. According to their measurements of Δt , the pulse frequency should have been at least 66 Hz. The highest frequency reported with the data is 50 Hz, which is also the maximum usable frequency of the blowing apparatus. Nevertheless, this work appears to have served as a precursor to the development of a zero-net-mass-flux scheme.

Zero-Net-Mass-Flux. Zero-net-mass-flux schemes essentially operate on the same principle as pulsed blowing except that a controlled fluctuating disturbance affecting the

forebody vortices is created without the requirement for drawing on an air source on-board the aircraft. The disturbance is typically created by a device, such as a vibrating membrane, which excites the volume of air in the delivery tube of an exhaust port so that air is forced through the port as a free jet and then drawn back in repeatedly. The time-averaged mass flow rate through the port is effectively zero. Two approaches to the zero-net-mass-flux scheme have been explored: *unsteady bleed* and *synthetic-jet*.

A series of investigations were undertaken to develop unsteady bleed. An early wind tunnel experiment, involving a cone-cylinder model with a single line of bleed ports located along the windward meridian, demonstrated the effectiveness of the scheme in preventing the formation of asymmetric forebody vortices (Williams et al., 1989; Williams and Papazian, 1991). Control of the leeward flow field was essentially independent of the pulse frequency (as long as the pulse period falls within an order of magnitude of one convective-time unit, D/V_∞) but strongly dependent on the forcing amplitude, i.e., the pressure signal within the delivery tube. Moreover, Williams et al. concluded that the leeward flow field responds to the momentum imparted by the fluctuating disturbance. A follow-up experiment was conducted to determine whether unsteady bleed can exert proportional control over asymmetric forebody vortices (Williams and Bernhardt, 1990; Bernhardt and Williams, 1998). The forebody model was a cone-cylinder combination with a base diameter of 2.56 cm. The conical section had a fineness ratio of 10.7 and a semi-apex angle of 7.5 deg, and featured a rounded tip. Bleed ports (1.5-mm diameter) were located symmetrically near the tip of the forebody at a circumferential angle of 135 deg from the windward stagnation line. The direction of the bleed port exhaust was normal to the model surface. Each port was driven by a vibration exciter at a pulse frequency of 60 Hz. Compared to the convective time-scale of 8.64 ms for this experiment, the pulse period adequately satisfied the aforementioned condition for successful control of the leeward flow field. The momentum flux from a bleed port is expressed in nondimensional form with respect to the freestream momentum flux, and is referred to as the bleed coefficient:

$$C_b = \frac{d^2 V'^2}{D^2 V_\infty^2} \quad (2.4)$$

where d is the diameter of a bleed port, and V' is the root-mean-square velocity of the unsteady bleed. The potential of unsteady bleed is demonstrated in Fig. 2.35 where the sectional side force coefficient C_y is shown to vary proportionally with the bleed coefficient over a narrow range of C_b at $\alpha = 45$ deg. At the limits of this range of proportional control, C_y reaches a maximum magnitude and then as C_b increases further for either bleed port, C_y decreases somewhat although the orientation of the vortices does not change.

Wind tunnel experiments investigating the control achievable with a synthetic-jet were conducted with a hemisphere cylinder forebody model (Roos, 1998). The hemispherical nose was fitted with two forward-facing nozzles, unlike the unsteady bleed approach. The nozzles ($d/D = 0.0167$) were located circumferentially 135 deg from the windward meridian, and each was driven by a piezoelectrically-actuated membrane that oscillated at a pulse frequency of 1200 Hz. The results presented in Fig. 2.36 demonstrate that synthetic-jet blowing was equally effective as steady blowing in generating C_N and C_Y over the range of angle of attack examined. In Fig. 2.37 the similarity in the variations of C_Y with time-averaged C_m ($\overline{C_m}$), due to steady blowing and synthetic-jet, indicates that proportional control of C_Y with synthetic-jet actuation is comparable to steady blowing. It should be noted that in the last two figures C_Y is considered positive towards the port side, contrary to convention. Aside from the differences of comparing sectional and total side force, the form of the controlling parameter, and the side force convention, the resemblance of the results appearing in Figs. 2.35 and 2.37 highlights the similarity of the unsteady bleed and synthetic-jet approaches to the net-zero-mass-flow technique.

C_μ or C_m . Controversy over the suitability of C_μ or C_m has developed. The coefficient of blowing momentum is generally used to non-dimensionalize the nozzle momentum. Employing aft jet blowing on an F-18 model with nozzles canted inboard at an optimum angle of 60 deg, Kramer et al. (1993) found that the yawing moment coefficient correlated well with C_μ over a small range of dynamic pressure (Fig. 2.38). The measurements did not correlate as well with C_m . A compelling case for C_μ is reported by Mosbarger (1994). Forebody vortex control was achieved on a F-16 forebody model with pneumatic jets that could be directed forward, aft, or at an arbitrary azimuthal angle. Over a Reynolds

number range from 0.7×10^6 to 2.44×10^6 , with Mach number held near-constant, the side force collapsed very well for constant C_μ (Fig. 2.39). Side force also collapses very well for constant C_μ as Mach number is varied from 0.41 to 0.61 and Reynolds number is near-constant (Fig. 2.40). Presumably these results were obtained with the nozzles in the optimum longitudinal position, pointing aft and canted towards the model centreline. Williams and Bernhardt (1990) believe that control forces arising from the unsteady bleed technique scale with bleed coefficient (C_b). The bleed coefficient is the ratio of momentum flux through the bleed holes to the freestream momentum, which clearly makes the parameter similar to C_μ .

With some exceptions the coefficient of mass flow $C_{\dot{m}}$ has been found to be a useful scaling parameter for tangential slot blowing. Kramer et al. (1993) reports that $C_{\dot{m}}$ offers an excellent correlation for slot results (Fig. 2.41), whereas C_μ provided a good correlation only at high Reynolds number. Investigating tangential slot blowing on a full-scale F-18, Meyn et al. (1992) has shown that the variation of yawing moment coefficient with blowing rate non-dimensionalized as $C_{\dot{m}}$ is very similar over a range of dynamic pressure (Fig. 2.42). The collapse of the results, however, appears to be limited to $C_{\dot{m}} \lesssim 0.39 \times 10^{-3}$. Using suction through small holes (as opposed to slots) located near the forebody apex, Ross et al. (1990) found that side force results did not collapse well for different combinations of hole size and freestream velocity when the flow rate is expressed as C_μ (Fig. 2.43). When the flow rate is non-dimensionalized as $C_{\dot{m}}$, however, the data collapses into a reasonable linear variation. Ross et al. (1990) remark that the choice of $C_{\dot{m}}$ for the suction technique has merit on the basis of physical considerations because the suction mechanism which triggers a change of vortex orientation, is caused by the removal of mass.

The remarks by Ross et al. (1990) would appear to be in contrast with the belief of Roos and Magness (1993) that $C_{\dot{m}}$ is the suitable parameter for expressing blowing rate for pneumatic jets. Using forward-blowing nozzles in combination with a blunt nose, Roos and Magness have shown that the variation of side force with $C_{\dot{m}}$, rather than C_μ , is insensitive to Reynolds number over a range of $0.07 \times 10^6 < Re_D < 0.22 \times 10^6$ (Fig. 2.21).

They believe the yawing moment arising from blowing is due to the *displacement effect* of the blowing nozzle, which causes the vortex of the blowing-side to lift off the model surface and the other vortex to move closer, rather than any “momentum-related entrainment and/or flow-energizing effects”. Hence, Roos and Magness believe that C_m reflects the displacement effect. Indeed, the momentum of a jet may not be imparting energy to the boundary layer given the very small blowing rates achieved by Roos and Magness. Alternatively the displacement effect may be a by-product of a complex equilibrium of the momenta of the nozzle jet and the apex flow field approaching the nozzle exit.

The debate between C_μ and C_m as the appropriate parameter to non-dimensionalize the blowing rate remains unresolved in the literature (Williams, 1997). On the other hand, findings such as those by Ross et al. suggest that each parameter may be applicable to certain pneumatic techniques. It would seem reasonable to expect results to correlate with C_m when displacement is the dominant physical effect, and with C_μ when momentum effects are dominant. When the ratio of jet velocity to freestream velocity (V_j/V_∞) is high, C_μ would be expected to best correlate results; when the ratio is low, C_m would be best.

2.2.2 Mechanical

Mechanical techniques of forebody vortex control essentially rely on actively altering the apparent forebody cross-sectional geometry encountered by the crossflow. Changes to the cross-section can be overt to control the location of boundary layer separation (e.g., actuated strakes, rotatable strakes). Alternatively cross-sectional changes can be subtle: given the sensitivity of the forebody flow field, vortex control can be achieved by altering the relative position of a geometrical perturbation at the apex with respect to the freestream flow (e.g., rotatable elliptical tip, movable nose stagnation point).

Actuated Strakes. Forebody strakes are low-aspect ratio thin aerodynamic surfaces that are intended to force the separation of the crossflow boundary layer. Typically located in the near-apex flow field and circumferentially placed at the forebody waterline, forebody strakes are fixed or deployable. *Fixed* forebody strakes are passive devices commonly used to stabilize the forebody flow field and reduce asymmetric side force and

yawing moment at high angles of attack. Fixed strakes have been applied with success to missiles (Yuan and Howard, 1991), and to the X-29 (Cornelius et al., 1994) and the X-31 aircraft (Alcorn et al., 1996). In the latter case, as mentioned earlier in Chapter 1, the strakes were combined with nose bluntness to reduce asymmetric side force and yawing moment during flight testing.

In contrast, deployable strakes are active devices which control the location of the boundary layer separation on one or both sides of the forebody to generate desired side force and yawing moment. The concept of *actuated* strakes is illustrated in Fig. 2.44. The strakes are longitudinally-hinged and can be deployed symmetrically or asymmetrically. With asymmetric deployment one strake is deflected or both strakes are deflected differentially, i.e., at different non-zero deflection angles. When fully retracted the strakes conform to the surface contour of the forebody. Side force and yawing moment arise due to the effect of strake deflection on the velocity of the crossflow. On a strake-deflected side, boundary layer separation is forced and the flow decelerates; a strong vortex is created and lifted away from the forebody because of the strake deflection angle. On the strake-retracted side, where separation is delayed, the flow accelerates and a weak vortex lies close to the leeward surface of the forebody. This velocity field is reflected in pressure measurements which reveal that pressure on the strake-deflected side is higher than on the strake-retracted side (Lanser and Murri, 1993; Fisher and Murri, 1998). Thus side force and yawing moment are directed towards the strake with the smallest deflection angle.

Wind-tunnel results exemplifying the effectiveness of actuated strakes are presented in Fig. 2.45 for a 16%-scaled model of the F-18 HARV (Murri et al., 1995). In Fig. 2.45(a) the variation of side force and yawing moment with angle of attack shows a monotonic increase until maximum magnitudes are reached; thereafter C_Y and C_n decline. The variations are reasonably symmetric with the deflection of a single strake (δ_s), and the levels of C_Y and C_n are sufficiently high to overcome natural asymmetry. The effect of strake deployment on rolling and pitching moments is considered small, and the effect on lift is negligible. Figure 2.45(b) presents an example of a strake deflection schedule and the corresponding yawing moment response. The variation of C_n with differential strake de-

flection ($\delta_{s,d}$) is considered “well-behaved”, although when strake deflection is symmetric ($\delta_{s,d}=0$ and $\delta_s \neq 0$) a yawing moment asymmetry persists. In sideslip, this technique of forebody vortex control remains effective in generating yawing moment (Fig. 2.45(c)). A Reynolds number effect is evident in the results presented in Fig. 2.45(d) where yawing moments are compared from wind-tunnel tests of a 16%-scale model and a full-scale model at a higher Reynolds number. Full-scale C_n is observed to be slightly higher; this is believed to be due to a delay in the separation of the boundary layer on the strake-retracted side of the full-scale model.

An unusual orientation change in the yawing moment was found to occur at small angles of a single-deployed strake during wind-tunnel (Murri et al., 1995) and flight tests (Fisher and Murri, 1998). In Fig. 2.46, C_n changes sign for differential deflection angles less than 30 deg. Flow visualization revealed that the vortex created by the deployed strake remained close to the leeward surface of the forebody and the vortex on the strake-retracted side lifted away. Measurements confirmed that the pressure was highest on the strake-retracted side, giving rise to a yawing moment directed towards the strake-deployed side. A differential strake deployment schedule, shown in Fig. 2.45(b), was found to correct this problem. The scheme initially deploys the strakes symmetrically to a certain angle, then differentially about this angle to achieve a desired yawing moment. Presumably the symmetric deployment of the strakes solves the problem by pushing the forebody vortices farther away from the leeward surface *before* the strakes are deflected differentially.

Rotatable Strakes. Murri and Rao (1987) found that, with a single strake extending the length of a forebody, yawing moment coefficient varies with azimuthal position (ϕ_s) in the manner shown in Fig. 2.47 ($\phi_s=0$ corresponds with the windward meridian of the forebody). A change of yawing moment orientation at $\phi_s \approx 81$ deg is believed to be due to a change in the structure of the crossflow. Surface pressure measurements for $\phi_s=60$ deg (Fig. 2.48) show the suction pressure is highest on the side of the forebody with the strake, giving rise to a side force directed towards the left-hand side. Murri and Rao inferred that the strake forces separation and the formation of a vortex to occur; the

vortex remains close to the forebody and eventually the flow reattaches further toward the leeward side. On the right-half the flow separates early towards the windward side. With the strake placed at $\phi_s = 90$ deg, however, the separated flow does not reattach and the vortex created by the strake is pushed away from the forebody; meanwhile the flow on the right-half side is encouraged to remain attached to a larger extent. As a result the suction pressure on the strake-side is lower and the side force is directed towards the right-half. In view of these results Murri and Rao suggested mounting strakes on a rotatable segment of the forebody as another means of regulating the magnitude and orientation of the yawing moment. The concept is illustrated in Fig. 2.49.

Unlike Murri and Rao, Ng and Malcolm (1991b) placed a pair of miniature strakes on a small rotatable portion of a forebody apex. They found strakes located in this sensitive area of the forebody flow field can influence the orientation of the forebody vortices. Water-tunnel investigations were performed with a 6%-scaled partial F-18 model with a pair of miniature forebody strakes mounted on a rotatable apex (Fig. 2.50). Note the circumferential position of the strakes is closer to the windward side of the forebody than proposed by Murri and Rao in Fig. 2.49. Off-surface flow visualization for $\alpha = 50$ deg with no sideslip revealed that symmetrically-oriented strakes lead to symmetric forebody vortices and, presumably, zero side force (Fig. 2.50). With strakes rotated clockwise, as viewed from the front of the forebody, the starboard vortex is forced away from the forebody while the port vortex lies close to the surface, an orientation which gives rise to side force directed to port. For a counter-clockwise rotation the results are vice versa. In sideslip up to $\beta = -20$ deg at $\alpha = 50$ deg, Ng and Malcolm report the forebody vortices were manipulated successfully with this technique.

Based on flow visualization studies of a single miniature strake, Ng and Malcolm believe the following fluid mechanisms are at work. For $\phi_s < 60$ deg a small strake forces separation of the local flow field over the rotatable segment of the forebody; the flow becomes turbulent and subsequently reattaches and the natural separation of the boundary layer is delayed. At the same time a vortex, formed from the trailing edge of the strake, influences the crossflow aft of the strake in a way (possibly by adding momentum) that

delays boundary layer separation. As a result the forebody vortex on the strake-side remains close to the forebody. For $\phi_s > 60$ deg the strake is too far on the leeside of the forebody to influence the natural separation of the crossflow boundary layer.

Miniature rotatable tip strakes have also been investigated on a 6%-scaled full F/A-18 model (Suarez et al., 1993) and a 10%-scaled F-16 forward fuselage (Smith and Ng, 1994a). Both are wind-tunnel studies.

Rotatable Elliptical Tip. Moskovitz et al. (1991) proposed that side force asymmetry could be controlled by introducing a rotatable tip which embodies a “known geometric perturbation that would tend to dominate the flow field at the tip”. The premise is that geometric irregularities at a forebody tip are responsible for the variation of side force with forebody roll angle at high angles of attack. The geometric perturbation suggested by Moskovitz et al. is an elliptically-shaped tip combined with a suitably shaped base (e.g., circular) that allows the tip to blend with the contour of a forebody (Fig. 2.51).

Wind-tunnel experiments were conducted to test this hypothesis. Normal tips (i.e., sharp and blunted) were compared against rotatable elliptical tips; all types were mounted on conical and tangent-ogive forebodies. The results for the tangent-ogive model will only be considered here. The effect of rotating an elliptical tip is reflected in the variation of the sectional side force coefficient C_y in Fig. 2.52(a). The variation of C_y with tip roll angle ϕ_t is essentially sinusoidal in behaviour, undergoing two cycles through one tip rotation for each α shown. The sectional side force coefficient also increases with α . By contrast in Fig. 2.52(b), a regular variation of C_y with model roll angle ϕ_m is not apparent for a sharp tip. For elliptical tips C_y was found to increase with greater ellipticity. In addition, at sideslip angles of 10 and 20 deg some restoring side force was generally evident throughout the range of α tested.

The control potential of a rotatable elliptical tip is recognized upon the inspection of C_y over a small interval of ϕ_t in Fig. 2.53. Over the interval $0 \leq \phi_t \lesssim 30$ deg a smooth variation of C_y occurs and the rate of change of C_y with ϕ_t is less with the major-axis horizontal than vertical^{||}. A low gradient would allow a degree of precise control of C_y .

^{||}Moskovitz et al. do not indicate that $\phi_t = 0$ deg represents major-axis horizontal. This has been assumed

In view of the sinusoidal nature of C_y with ϕ_t , it seems that a controlled variation of side force is feasible between maximum attainable magnitudes of C_y by rotating an elliptical tip over a small ϕ_t -interval such as ± 30 deg in this particular case.

Movable Nose Stagnation Point. Unlike the preceding rotatable tip methods, this scheme does not rely on mechanical artifices to control the forebody vortices other than the means to rotate the tip. Instead, a geometrically-flawed forebody tip is accepted as fact; its rotational displacement is correlated with a measure of vortex asymmetry so that the forebody tip can be rotated to achieve a desired vortex orientation.

Vortex asymmetry is measured relative to the *zero vorticity contour* (ZVC) (Darden and Komerath, 1997b). In Fig. 2.54 the ZVC is the streamline which divides the flow induced by the counter-rotating forebody vortices. According to Darden and Komerath the thin-layer of high velocity fluid between the vortices is inviscid and has zero vorticity perpendicular to the centreline of the layer. A measurable angular displacement ϕ , with respect to the vertical plane of symmetry, is obtained when the ZVC is projected to the leeward surface. Darden and Komerath observed that the vortices rotate about the ZVC as the vortices change orientation. They conclude the angular displacement ϕ is a measure of vortex asymmetry. Rotating the forebody tip changes ϕ and the degree of the vortex asymmetry. For instance a symmetric vortex orientation was obtained by rotating the forebody tip appropriately for $\alpha \leq 40$ deg (Fig. 2.55). The results in this figure also demonstrate that the vortices respond dynamically to tip rotation and was found to do so at high rates. Darden and Komerath believe a relationship exists between ϕ and the angular displacement of the forebody tip.

This technique of vortex control has been used on a delta-wing model with a tangent-ogive forebody to evaluate the response of the forebody vortices, forebody pressure, and the wing vortex system to tip rotations (Section 2.4), and to provide a basis for a model of the rolling moment response (Darden and Komerath, 1997a).

since C_y is small at $\phi_t = 0$ deg.

2.2.3 Dynamic Manipulation

The principle of the dynamic manipulation of forebody vortices is explained in Section 1.2. In view of the previous discussion of pneumatic techniques, it is important to recognize the distinction between the pneumatic implementation of dynamic manipulation and the technique of pulsed blowing. The techniques differ in two ways: First, dynamic manipulation continuously changes the vortex pattern to generate a desired control effect, whereas the intent of pulsed blowing is to create a quasi-steady vortex pattern. Second, since dynamic manipulation takes advantage of the bi-stable nature of the forebody vortices, the lowest duty-cycle time (τ in Fig. 1.5) must be longer than the time-lag required by the vortex system to switch orientation. Pulse blowing, on the other hand, requires the half-period of the pulse frequency to be shorter than the time-lag. Therefore, pulse frequencies will be higher than the frequency of alternating blowing associated with dynamic manipulation.

The research of the dynamic manipulation of forebody vortices began with a water-tunnel investigation that set out to determine whether an alternation of the bi-stable states could be sustained by oscillatory blowing at a relatively high frequency (Alexan, 1992; Alexan et al., 1994). The model used for this experiment was a tangent-ogive cylinder combination (Fig. 2.56, Table 2.1). The results of the experiment demonstrated that very low blowing momentum coefficients were sufficient to reliably switch the forebody vortices from one stable configuration to the other, and that a reasonably high reduced switching frequency was possible (up to at least $\omega^* = 0.16$). Alexan defined the maximum frequency for alternating blowing as that which allows just sufficient time for the forebody vortex pattern to reach the stable configuration before being switched back again. Roos obtained similar results in his investigation of pulsed blowing (Section 2.2.1).

Having established that the forebody vortices could reliably react to oscillatory blowing from a forward-facing nozzle, a follow-on investigation was undertaken to determine the effect of valve duty cycle on time-average side force and yawing moment, and to test the technique at a Reynolds number higher than the water-tunnel experiments** (Lee,

** A time-average aerodynamic quantity is defined in Section 5.1. For consistency within the context of

Details of Model	Alexan (1992)	Lee (1995)	Francois ONERA (2000)	Rodriguez ONERA (2002)	Present Work
Forebody: Planform	tangent- ogive	tangent- ogive	cone	cone	tangent- ogive
δ_N (deg)	28	30	15	15	12.8
D (mm)	25.4	134.6	310	54	80
l/D	2.25	2.25	1.87	1.87	4.44
Nozzle:					
d/D	0.020	0.018	0.0029	0.017	0.019
x_n/D	0.25	0.25	0.016	0.093	Fig. 3.4
θ (deg)	± 135	± 135	± 115	± 115	Fig. 3.4
Afterbody	cylinder	cylinder	cylinder	none	delta-wing

Table 2.1. Forebody geometry and nozzle location on models tested with the dynamic manipulation technique

1995; Lee et al., 1995). The investigation was conducted in a wind tunnel with a model that was essentially a 5.3-scaled replica of the water-tunnel model (Table 2.1). Typical steady blowing results are presented in Figs 2.57 and 2.58. The variation of time-average side force ($\bar{C}_Y$) and yawing moment coefficient ($\bar{C}_n$) with steady blowing from one nozzle (Fig. 2.57) shows that $\bar{C}_Y$ increases sharply as the blowing momentum coefficient (C_μ) is increased from zero until a plateau is reached, beyond which further increases in C_μ produce relatively little change in $\bar{C}_Y$ and $\bar{C}_n$. The value of C_μ required to reach the plateau is referred to as the minimum blowing coefficient. At the angle of attack of $\sigma = 50$ deg, $C_\mu = 0.66 \times 10^{-3}$ was regarded as the minimum blowing coefficient. The ratio $\bar{C}_Y/C_\mu$ at this condition is about 5,500 which represents an improvement by a factor of two over the water-tunnel results. Figure 2.58 illustrates the variation of $\bar{C}_Y$ and $\bar{C}_n$ with angle of attack under conditions of steady blowing through a single port with $C_\mu = 0.66 \times 10^{-3}$. These results clearly show the presence of a natural asymmetry (i.e., a baseline response) of the forebody vortices for $\sigma > 40$ deg but the vortex pattern was reliably controlled with steady blowing, i.e., starboard blowing always produced negative side force and vice

the dynamic manipulation technique, the notation for time-average quantities is used to report the results for baseline, steady blowing, and dynamic blowing case. These blowing cases are described in Section 3.8.

versa. An important feature of these results is that the variations of $\bar{C}_Y$ and $\bar{C}_n$ are nearly symmetrical.

The variations of $\bar{C}_Y$ and $\bar{C}_n$ with port valve duty-cycle parameter (τ/T , Fig. 1.5) appear in Figs. 2.59 and 2.60 for a reduced alternating frequency of $\omega^*=0.32$ (7.2 Hz). Note that for $\tau/T=0\%$ the port valve is closed and the starboard valve is fully open throughout the blowing cycle; for $\tau/T=100\%$, the states of the valves are the opposite. Clearly $\bar{C}_Y$ and $\bar{C}_n$ vary linearly with duty cycle and vanish at $\tau/T=50\%$ (Figs. 2.59), which is consistent with the expected behaviour illustrated in Fig. 1.5(b). For $\sigma=40$ and 50 deg the linearity of $\bar{C}_Y$ and $\bar{C}_n$ is very good throughout the range of τ/T . The maximum ω^* was considered to be 0.32, which is twice the value reached in the water-tunnel experiments. Smoke flow visualizations tests confirmed that the forebody vortices responded well over the range of duty cycle. In addition, by illuminating the vortices in crossflow with a laser-light sheet and capturing the motion on high-speed video, the response time of the forebody vortices to change orientation was found to be approximately the freestream convection time (x_s/V_∞ , where x_s is the length between the forebody apex and the plane of the laser-light sheet). In this experiment sideslip was simulated by using non-zero roll angles (ϕ). The roll angle is positive when the model is rolled to starboard as shown in Fig. 3.2. In a sideslip condition, at roll angles of $\phi=\pm 10$ deg, the linearity is retained to a large extent as the variations of $\bar{C}_Y$ and $\bar{C}_n$ undergo a vertical offset (Fig. 2.60). At $\phi=\pm 20$ deg the offset and the linearity have diminished, suggesting that the effectiveness of dynamic manipulation is limited in sideslip.

Wind-tunnel experiments were conducted at ONERA to validate the concept of dynamic manipulation and to optimize the control of the forebody vortices (Francois, 2000). The experiments were performed on a generic cone-cylinder model (Table 2.1). Alternating nozzle blowing was implemented using by a pair of miniature valves which could be operated up to a maximum frequency of 300 Hz. Typical results from the investigation at ONERA are presented in Figs. 2.61 to 2.64. Although only results for C_n are reported by Francois, each of these figures corresponds in theme to Figs. 2.57 to 2.60 respectively. An exception is Fig. 2.63 which shows the effect of frequency on the variation of C_n with duty

cycle, rather than the effect of angle of attack as in Fig. 2.59. In general, nevertheless, the results of Lee (1995) and Francois (2000) are in good qualitative agreement. Also, judging from the extent of the linearity of the variation of C_n with duty cycle in Fig. 2.63, it appears that the maximum ω^* for this model configuration is about 5.84, which represents a significant improvement over the value reported in Lee (1995). In regard to validating the dynamic manipulation technique, Francois' results are in good agreement with those of Lee (1995).

It is interesting to note that the sign change of C_n which occurs when C_μ increases beyond about 1.2×10^{-3} (Fig. 2.61), is a feature that is also found in the results presented in this thesis (Section 5.4.1). But according to Francois' description of the vortex behaviour, the response of the forebody vortices to forward-blowing nozzles is opposite to the behaviour demonstrated in Alexan (1992), Lee (1995) and the present work. For instance, for steady blowing from the starboard nozzle at $C_\mu = 0.5 \times 10^{-3}$ (before the sign change in Fig. 2.61), the nozzle flux is described as retarding the detachment of the starboard vortex, placing it in the low position and the port vortex in the high position. This behaviour is analogous to the effect of suction control and is the opposite of that observed in the aforementioned work with forward-blowing. For $C_\mu = 2.0 \times 10^{-3}$ (after the sign change), the starboard flux is said to promote the detachment of the starboard vortex, causing a switch in the vortex pattern. Flow visualization appeared to confirm the reported orientation of the forebody vortices. This vortex response to forward-blowing appears to be also reflected in a water-tunnel investigation described in Rodriguez et al. (2002). Using a scaled-down replica of Francois' conical forebody model (Table 2.1), Rodriguez et al. also found that the orientation of the vortices switches when a certain C_μ -threshold is reached, accompanied by a sign change of the side force. Moreover, for low C_μ from the starboard nozzle the side force was directed toward the starboard side. Although the investigators do not comment on the corresponding vortex orientation, the direction of the side force implies that the port vortex is high and the starboard is low, i.e., the same vortex response as observed by Francois.

The discrepancies regarding vortex response to forward-blowing and the sign change

of C_n above a certain value of C_μ , underscore the fact that fluid mechanics of subtle nature are at work. In an attempt to uncover the reasons for the disparities, a water-tunnel investigation was undertaken to make detailed flow-visualization observations of forebody vortices responding to forward-blowing (Neuteboom, 2002). Tangent-ogive cylinder models — of various combinations of semi-apex angle, and nozzle axial and azimuthal locations — were tested. Neuteboom identified three vortex manipulation events:

- *Low-blowing reversal*. At low C_μ , forward-blowing favours vortex attachment on the blowing side. This type of reversal occurs for only some combinations of semi-apex angle and nozzle locations.
- *Switch*. At medium C_μ , forward-blowing favours detachment of the vortex on the blowing side.
- *High-blowing reversal*. At high C_μ , forward-blowing favours attachment of the vortex on the blowing side.

Neuteboom was unable to determine in detail the physical mechanisms underlying the observed behaviour. However, he has proposed a *competing effects* hypothesis that is based on the established fact that “relatively rapidly moving fluid tends to entrain surrounding fluid, creating a region of reduced static pressure”. The hypothesis is that the blowing fluid has two competing effects, a displacement effect and the aforementioned entrainment effect:

- At very low C_μ the nozzle flux is drawn into the vortex without affecting the baseline vortex orientation.
- At low C_μ , *low-blowing reversal* occurs as the result of an entrainment effect, i.e., nozzle flux moving through the shear layer entrains some freestream flow, thereby encouraging the attachment of the vortex on the blowing side.
- As C_μ increases, an accumulation of nozzle flux leads to a displacement effect that dominates the entrainment effect, i.e., the accumulation acts as a *viscous fairing* or *bump* that promotes separation and causes *switch* to occur.

- As C_μ increases further, some accumulated nozzle flux escapes into the freestream, leading to the restoration of dominance of the entrainment effect and the occurrence of *high-blowing reversal*.
- At very high C_μ the nozzle flux forms a small-diameter jet that penetrates through the shear layer; the interaction of the nozzle flux with the shear layer is subdued, and the vortices adopt the baseline orientation.

Neuteboom's work indicates that the type of reversal encountered by a forebody and the range of C_μ over which it occurs, could depend on its planform and nozzle azimuth. For instance, the difference in forebody planform (i.e., tangent-ogival versus conical) shifts the interval of C_μ over which either entrainment or displacement effects dominate. Neuteboom's results appear to reconcile the aforementioned discrepancies; they suggest that *low-blowing reversal* and *switch* might have occurred with the cone model in the investigations of Francois and Rodriguez et al., and *switch* and *high-blowing reversal* might have occurred with the tangent-ogive model in the present work.

2.2.4 Nozzle Location

The location of a nozzle is generally specified by the axial distance between the apex of the forebody and the exit plane of the nozzle (i.e., the lateral plane where the nozzle centreline intersects the forebody surface) and azimuthally relative to the windward meridian. Central to the postulated mechanism by which forward-blowing jets exert control over vortex orientation, Ng and Malcolm (1991a) advocate that the closest feasible point to the apex of the forebody is the best location for nozzles. This is because blowing is then expected to have the most influence on the entire vortex structure owing to the small local scale of the flow near the apex. In most investigations the nozzles have been placed at $\theta = \pm 135$ deg from the windward meridian, which tends to fall between the primary and secondary separation lines.

For the technique of dynamic manipulation by forward blowing, a typical scheme with the nozzles located as close to the apex as possible, appears to be preferable in order to minimize the required blowing momentum or mass flow rate. Hence, the findings of

Roos and Magness (1993), discussed earlier, are of interest: their forebody vortex control methodology combines the blunting of a tangent-ogive with steady forward blowing. A noteworthy feature of this work is the location of the nozzle exits — because of the blunting, the nozzle exits are much closer to the origin of the forebody vortices thereby improving the ability to influence vortex asymmetry and reducing the sensitivity of the blowing effects to sideslip (Fig. 2.65).

2.3 Influence of a Full Aircraft Configuration

The present work tested a full generic aircraft model, unlike the investigations described in Alexan (1992) and Lee (1995) which involved isolated forebody models. The findings of other researchers have shown that the presence of the wing and leading-edge-extensions (LEX) affect the rolling and yawing moments.

2.3.1 Effect on Rolling Moment

An interaction between the forebody and wing vortex systems is known to have ramifications on the rolling moment and rolling motion of a fighter aircraft at high angles of attack. Investigations have shown that the side force and yawing moment arising from asymmetric forebody vortices are accompanied by non-zero rolling moments. The interaction of the vortex systems also sometimes leads to wing rock. Efforts have been made to suppress wing rock by manipulating the forebody vortices — a technique that shows promising results involves alternating blowing at high frequency from two nozzles situated near the tip of the forebody. The technique has the effect of reducing the wing-rock motion and eliminating adverse yawing moments.

The effect of forebody vortices on the rolling moment exerted on a complete aircraft is demonstrated in the experimental results of Wood and Crowther (1994). Previously described in Section 2.2.1, this wind-tunnel investigation involved a generic delta-wing model fitted with LEXs and a slender forebody. The forebody featured a tangential slot blowing scheme to control the orientation of the forebody vortices. The variation of base-

line (i.e., no blowing) rolling moment coefficient with angle of attack (Fig. 2.66) shows that rolling moment is non-zero for $24 \lesssim \alpha \lesssim 40$ deg. Flow visualization revealed that over this interval of α , a strong interaction exists between the forebody vortices and the vortices originating from the LEXs. Specifically, the asymmetry of the forebody vortices leads to asymmetric bursting of the LEX vortices which gives rise to the non-zero rolling moment, i.e., a low forebody vortex is associated with a forward-bursting LEX vortex on the same side, and a high forebody vortex corresponds to an aft-bursting LEX vortex (Fig. 2.67). Wood and Crowther cite the work of Gee et al. (1994) whose computational results reflect the same interaction of the two vortex systems for a F/A-18 aircraft (described later in Section 2.3.2). For $\alpha \gtrsim 40$ deg in Fig. 2.66 the rolling moment becomes zero as the forebody vortices are far above the LEX vortices, diminishing the interaction (Darden et al., 1997). With blowing from the tangential slots, the non-zero rolling moment persists for $20 \lesssim \alpha \lesssim 35$ deg — blowing from the port slot causes a negative rolling moment and vice versa for starboard blowing. These responses are consistent with the combination of forward-bursting of the LEX vortex on the blowing side (forebody vortex is low) and an aft-burst location of the unblown opposite side (forebody vortex is high). A similar influence of the forebody vortices on the rolling moment has been observed in wind-tunnel tests of a full-scale F/A-18 aircraft (Lanser and Murri, 1993) and a 6%-scaled F/A-18 model (Kramer et al., 1993). In these investigations, slot and aft-jet blowing were used respectively to influence the forebody vortices.

Delta-wing aircraft are susceptible to an undesirable condition called *wing rock*, which is a self-sustaining roll oscillation. While there are different causes of wing rock (Ericsson, 1990), it is believed that an advanced fighter aircraft will be exposed to that form which is induced by asymmetric shedding of forebody vortices (Ericsson, 1989a,b; Ericsson et al., 1996). Forebody blowing techniques have been extended to also serve as a means of suppressing wing rock. Celik et al. (1994) describes a wind-tunnel experiment designed to control wing rock and yaw of an aircraft model at high angles of attack using a tangential slot blowing scheme. The free-to-roll model was a generic fighter aircraft with a cylindrical fuselage, a conical nose and an interchangeable delta-wing ($\Lambda = 60$ or 70 deg). The results of the dynamic test show that the model (with $\Lambda = 70$ deg) ex-

perienced a significant wing-rock motion at $\alpha = 45$ deg with a frequency of 1.6 Hz and angular amplitude of 28 deg (Fig. 2.68(a)). But with steady tangential blowing from the port slot, the wing-rock motion was effectively suppressed, resulting in a steady-state roll angle of approximately 4 deg (Fig. 2.68(b)). Flow visualization observations in a water tunnel have shown that wing rock is suppressible by steady blowing from either side of the forebody because the forebody vortex orientation arising from the effect of blowing is steady, thus the interaction between the forebody/wing vortex systems is also steady (Ng et al., 1994). However, a yawing moment of significant magnitude is a by-product of the asymmetric forebody vortices.

To decouple the rolling and yawing moments, a wind-tunnel investigation was undertaken by Ng et al. (1994) to explore the potential of alternating blowing between left and right aft-blowing nozzles at high frequency. The approach is similar to the technique of dynamic manipulation, the focus of this thesis: an oscillating forebody vortex pattern will pre-empt the development of the conditions leading to wing rock, but the yawing moment will be zero on a time-average basis (for a 50% duty cycle). Ng et al. applied the technique to a delta-wing model ($\Lambda = 78$ deg) fitted with a slender forebody ($\delta_N = 9.25$ deg); aft-blowing nozzles were located 1.9 cm from the apex of the nose and $\theta = \pm 150$ deg from the windward meridian. In free-to-roll tests, the model experienced wing rock at $\alpha = 22$ deg with a natural wing-rock frequency of approximately 3 Hz. Ng et al. found that with alternating blowing frequencies (presumably with a 50% duty cycle) of 2 to 3 times the natural wing-rock frequency, significant reductions in wing-rock amplitude were achieved. At lower frequencies, alternating blowing was not effective. By oscillating the blowing at the natural wing-rock frequency, the rolling motion was either enhanced or damped, depending on the phase difference between the alternating blowing and motion of the model. Figure 2.69 shows the effect of alternating blowing on reducing the amplitude of the rolling motion. In Fig. 2.70, the time-averaged yawing moment reduces to zero with high alternating blowing frequency. In addition to suppressing wing rock, Ng et al. also found that a zero-roll attitude could be sustained simultaneously.

2.3.2 Effect on Yawing Moment

Yawing moments due to forebody blowing tend to be higher on full aircraft models than on an isolated forebody. In addition, yawing moment appears to be affected by the interaction between the forebody and wing/LEX vortex systems.

An effect of aircraft geometry on yawing moment is detected in the experimental results of Guyton and Maerki (1992). In this investigation involving an X-29 model, control of the forebody vortices was implemented by stabilizing the vortices with strakes, then perturbing them with optimally-oriented aft-blowing nozzles. With aft-blowing, the total yawing moment acting on the full model is shown to be higher than on the isolated forebody throughout the entire range of angle of attack (Fig. 2.71(a)). This contrast is not reflected in the side force comparisons shown in Fig. 2.71(b). In fact, the total side force begins to decrease significantly at $\alpha \approx 30$ deg. Believing that downstream influences were responsible for affecting the total side force at this attitude, Guyton and Maerki tracked the travel of the side force centre of pressure (CP) as a means of isolating these influences (Fig. 2.72). The location of the CP is shown to be relatively constant with angle of attack for the isolated forebody. For the complete model (i.e., tail-on results), the CP moves aft with increasing angle of attack until reaching its maximum rearward travel at $\alpha \approx 30$ deg. At about 36 deg, the CP begins to move forward with further increases in angle of attack. Tail-off results, compared to that of the isolated forebody, suggest that a significant local side force, developing in the vicinity of the fuselage/canard, may be responsible for the rearward shifting of the CP. In view of the degradation of total side force for $\alpha \gtrsim 30$ deg in Fig. 2.71(b), the sense of the local aft-fuselage side forces must be opposite to the local forebody side forces. If this is so, then the yawing moment for $\alpha \gtrsim 30$ deg would be largely due to a pure moment couple.

The experimental results reported by Lemay et al. (1992) for an F-16C model show a similar behaviour in the variation of yawing moment coefficient and the movement of the CP with angle of attack. With an aft-blowing nozzle, the yawing moment coefficient increases in a non-linear fashion with the angle of attack (Fig. 2.73). In Fig. 2.74 the CP moves aft to its furthest rearward point at $\alpha \approx 35$ deg; at $\alpha \approx 40$ deg, the CP begins to

move forward. Lemay et al. observed that the CP moves to the location where the LEX vortices are a major contributor to lift. The investigators attribute the rearward movement of the CP to the interaction of the forebody vortex system (under the influence of blowing) with the LEX vortex system.

A computational study by Gee et al. (1994) of tangential slot blowing on the F/A-18 provides additional insight into the findings of Guyton and Maerki and Lemay et al.. In Fig. 2.75 the computed off-surface streamlines for $\alpha = 30.3$ deg show an asymmetric bursting of the LEX vortices over the wing surface. Gee et al. indicate that asymmetric bursting arises from the influence of forebody slot blowing and the strong interaction that occurs between the forebody and LEX vortex systems. The longitudinal distribution of computed local yawing moment coefficient corresponding to this flow field, is shown in Fig. 2.76. Note that the local yawing moment is the product of the sectional side force and the moment arm between the section and the moment centre. In Fig. 2.76 significant local yawing moments develop in the vicinity of each burst location that are opposite in sign to the yawing moment developed by the forebody. These local yawing moments are associated with sectional side forces that change direction at each burst location (Gee et al., 1992). The interaction of the forebody and LEX vortices appears to have the effect of reducing the total yawing moment at $\alpha = 30.3$ deg. This could account for the slight decreases in total yawing moment seen in Figs. 2.71(a) and 2.73, in the vicinity of $\alpha = 30$ deg.

2.4 Time Scales of Flow Field Response

Knowledge of the time-lag response of the forebody vortices to a controlled perturbation have been shown to be necessary for control techniques such as pulsed blowing and zero-net-mass-flux, and the pneumatic form of dynamic manipulation. A series of investigations have shed further light on the time scales of the flow field response to manipulated forebody vortices (Peterson et al., 1996; Darden et al., 1997; Darden and Komerath, 1997b). On a delta-wing model ($\Lambda = 63.5$ deg) fitted with a forebody ($l/D = 3.2$) with a

conical tip ($\delta_n = 15$ deg), the technique of a movable nose stagnation point (Section 2.2.2) was used to apply controlled perturbations to the forebody vortices in order to develop an understanding of the response times of the forebody vortex system, forebody surface pressure and the wing vortex system.

The time for the forebody vortices to change orientation in response to nose movements was measured by tracking the zero vorticity contour (ZVC), considered to be a suitable standard that reflects the degree of asymmetry of forebody vortices. The results reveal that the response time of the forebody vortices to the perturbing movements of the nose is on the order of magnitude of freestream convection time, x/V_∞ , where x is the axial location of the ZVC measurement relative to the forebody apex. This result agrees with the vortex response time found for an isolated pointed tangent-ogive forebody (Section 2.2.3).

The response time of the forebody surface pressure, however, was found to be longer than the freestream convection time by one order of magnitude. The response was determined from measurements of pressure difference between a pair of static pressure taps, each port placed 110 degs in azimuth from the windward meridian. The pressure-difference signal followed the waveform of the nose movement very well.

Two responses were detected in rolling moment data, acquired with an external balance at the angle of attack for which the interaction between the forebody and wing vortex systems was greatest (i.e., $\sigma = 35$ deg). The fastest response was attributed to the direct effect of the forebody vortices on the pressure distribution over the wing. The second slower response was due to the wing vortex system; its response time was estimated to be longer than the freestream convection time by one order of magnitude.

The response times of forebody pressure and the wing vortex system are thus slower than the reaction of the forebody vortices. A longer wing-vortex response is understandable in view of the interaction of the forebody/wing vortices and the effects on the burst locations of the wing vortices. The fact that the forebody pressure response is longer is perhaps an indicator of the influence the forebody vortices exert on the pressure field over the forebody, particularly over the leeward surface. Regardless, the reason for the

difference in response times is unknown.

Figure 2.1. Schematic of the flow structure in the crossflow plane accompanied by a typical surface pressure distribution. *Adapted from Degani et al. (1991) and Lamont (1982b).*

Figure 2.2. Development of leeward flow field as revealed by smoke flow visualization over a conical forebody with a 5-deg semi-apex angle. The sequence of flow topologies is arranged in order of increasing angle of attack, i.e. $\alpha_I > \alpha_A$. Adapted from Lowson and Ponton (1991).

Figure 2.3. Basic premise of the impulsive flow analogy. Adapted from Hall (1987a).

Figure 2.4. In (a) sectional side force coefficient c_y varies with respect to longitudinal location x/D at different angles of attack. The same results are replotted in (b) against the non-dimensional time parameter, $t^* = (x/r) \tan \alpha$, according to the ad hoc form of the time analogy by R.M. Hall. These results are taken from Hall (1987a) and apply to a $3.5D$ tangent-ogive cylinder model for $Re_D = 0.2 \times 10^6$.

Figure 2.5. Presumed correlation of vortex shedding and longitudinal distribution of local side force coefficient on a body at high angle of attack. (Champigny, 1984)

Figure 2.6. The hypothesis proposed in Peake et al. (1980) suggests a flow instability in the vicinity of the enclosing saddle point (a) is responsible for initiating vortex asymmetry (b). The departure of the dividing streamline from the surface in (b) is described in Wardlaw and Yanta (1984).

Figure 2.7. Influence of Reynolds number on the longitudinal development of sectional side force coefficient for a $3.5D$ -long tangent-ogive cylinder model. The roll angle of 90 degs is the orientation at which large values of c_y were encountered at the first station of pressure transducers ($x/D = 0.5$). These results are a subset of a comprehensive database (Lamont, 1982a,b, 1985) and are reproduced from Hall (1987b).

Figure 2.8. Forebody flow field model. (Keener, 1986)

Figure 2.9. Sectional pressure distributions at three longitudinal stations for a 3.5D tangent-ogive cylinder model at $\alpha = 40^\circ$ with $Re_D = 0.8 \times 10^6$. These results reflect the influence of local Reynolds number on the forebody flow field. The contribution of these pressure distributions to the longitudinal development of sectional side force is indicated in (d). Significant flow field features revealed by surface flow visualization on a forebody of the same geometry is superimposed. Subscript K denotes features identified by Keener (1986), and the abbreviations LS, R, SS and TS are defined in Fig. 2.8. *Reproduced from Hall (1987b).*

Figure 2.10. Variation of overall maximum side force with Reynolds number for a tangent-ogive cylinder model ($l/D=2.0$) at $\alpha=55$ deg. (Lamont, 1982b)

Figure 2.11. Correlation of onset angle of attack for asymmetric vortices with semi-apex angle for isolated pointed forebodies. (Keener and Chapman, 1974)

Figure 2.12. The effect of nose bluntness on side force for a $3.5D$ tangent-ogive forebody. (Roos and Magness, 1993)

(a) Tangent-ogive forebody ($l/D = 3.0$, 5% apex bluntness ratio), with cylindrical afterbody. (Pick, 1971)

(b) Pointed tangent-ogive forebody ($l/D = 3.5$), no afterbody. (Keener and Chapman, 1974)

Figure 2.13. Variation of side force coefficient with angle of attack for various Mach numbers. Only the results in (b) are known to have been acquired at constant Reynolds number.

(a) Pick (1971)

(b) Keener et al. (1976)

(c) Wardlaw and Morrison (1976), taken from Ericsson and Reding (1986).

Figure 2.14. Variation of maximum side force coefficient with Mach number for various forebody and apex profiles. Only the results in (b) are known to have been acquired at constant Reynolds number.

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Figure 2.64. Effect of sideslip on the variation of C_n with starboard duty-cycle parameter. $\alpha = 50$ deg, $C_\mu = 0.5 \times 10^{-3}$, $Re_D = 0.45 \times 10^6$, $\omega^* = 1.07$ (11 Hz). (Francois, 2000)

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Chapter 3

Description of the Wind Tunnel Experiment

This experimental investigation was undertaken as two studies. The principal study involved testing an aircraft-like configuration in a wind tunnel; a complementary study, conducted in a water tunnel with the forebody model only, was undertaken to further understand some unexpected behaviour encountered during the wind tunnel test. The details of the water tunnel experiment are discussed in Chapter 4.

This chapter describes the wind tunnel experiment in which aerodynamic forces and moments were measured on a schematic fighter aircraft employing the dynamic manipulation technique to the control forebody vortices at high angles of attack. The existing aircraft model comprised a 65-deg delta-wing, a vertical tail and a forebody with a 12.8 deg semi-apex angle. The vortices were controlled by a pneumatic system that supplied air in an alternating cycle to forward-blowing nozzles in the tip of the forebody. The forebody was modified to hold interchangeable tips so that the effect of different combinations of nozzle placement could be investigated. The model was sting-mounted, and aerodynamic forces and moments were measured with an internal strain-gauge balance. The output signals of the balance and other instruments were acquired by a real-time data acquisition system and ensemble-averages over many alternating-blowing cycles were taken to minimize the effect of noise. The balance/sting arrangement substantially reduced the stiffness of the model support structure and contributed to a serious resonance condition. This condition required the development and installation of an emergency safety system within the model, and restricted the test envelope. In particular, it precluded the study of the effect of Reynolds number on the dynamic manipulation technique. To support the

findings revealed by the force and moment data, surface flow visualization was performed on the forebody of the model.

3.1 Wind Tunnel Facility

The test was performed in the 2m×3m low-speed wind tunnel at the Institute for Aerospace Research, National Research Council Canada. The wind tunnel is an atmospheric, return circuit facility with a contraction ratio of 9:1. The test section is rectangular in cross-section with filleted corners, and measures 1.93 m high by 2.78 m wide at the centre of the turntables (Fig. 3.1). The length of the test section is 5.2 m. Air flow is produced by a fixed-pitch fan driven by a 1500 kW DC variable-speed motor. The maximum Reynolds number per metre is 9.2×10^6 and the maximum sustainable speed is 140 m/s. At this speed the turbulence level is 0.13% at the centre of the test section. Flow angularity in the transverse and vertical directions was estimated to be less than 0.25 deg (Brown, 1986).

Turntables in the floor and roof of the test section provided support for the sting-strut system (Fig. 3.1). With centres located 188 cm from the entrance of the test section, the motorized turntables rotate synchronously to a maximum positive rotation of 210 deg (clockwise in Fig. 3.1) and a maximum negative rotation of 95 deg. The base of a floor-to-ceiling strut holding the sting was mounted on a pedestal located beneath the floor. The pedestal was rigidly attached to the external force and moment balance via a traversing mechanism that allowed the pedestal to be located at any point along the diameter of the turntable. As such, the pedestal provided fixed-end support for the strut. For this investigation the pedestal was placed at the downstream traversing limit, close to the perimeter of the floor turntable. A fixture clamped to the roof turntable provided pinned support to the top of the strut.

3.2 Models

3.2.1 Delta-wing Model

The schematic aircraft model featured a delta-wing with 65-deg leading edge sweep, and a long, slender forebody (Fig. 3.2). The model was originally designed for dynamic wind-tunnel experiments involving high roll and pitch rates at a subsonic Mach number up to 0.55. The wings and tail of the model were made of multi-layer carbon composite skin with a foam core, and the fuselage was entirely of carbon composite. The aluminium forebody was a shell with a circular cross-section and a tangent-ogive profile with a radius of 160.1 cm and a semi-apex angle of 12.8 deg. A cross-section of the assembled model is shown in Fig. 3.3. The aircraft model was sting-mounted and supported by the floor-to-ceiling strut with the plane of the wing vertically-oriented so that the angle of attack was governed by the rotation of the turntable (Fig. 3.1).

3.2.2 Tip Models

An objective of this investigation was to study the effect of location of forward-blowing nozzles. To accomplish this purpose the basic forebody was truncated by 63.5 mm from the apex to permit installation of removable tips with different nozzle configurations. These are defined by the longitudinal location of the nozzles (x_n) relative to the forebody apex, and the azimuthal position (θ) with respect to the windward meridian (Fig. 3.4). The coordinates coincide with the intersection of the nozzle centreline with the surface profile of the forebody. The x_n -plane at which the intersection occurs is referred to as the nozzle exit plane.

The tips were manufactured from mild steel so that a sharp apex could be formed and maintained. Each tip was supported in the forebody by a stem, aligned circumferentially relative to the forebody with a dowel pin, and held in place by friction produced by a captive O-ring. A pair of 1.52 mm diameter nozzles were carefully drilled through each tip, parallel to the longitudinal axis, and symmetrically arranged about the vertical plane. During the installation of the aircraft model the angular alignment of a tip with respect

to the forebody was checked by replicating the stem of a tip model on a reference block. Despite the presence of the dowel pin, the reference block was found to rotate by as much as 0.42 and 0.22 deg in the counter- and clockwise directions, respectively, as viewed from the tip. Numerous tip installations leading up to the wind tunnel entry were probably responsible for enlarging the close fit between the dowel pin and the aluminium forebody. Nevertheless, a tip was always carefully aligned on the forebody during the test.

Three families of nozzle configurations, differentiated by longitudinal location, were selected. The configurations are tabulated in Fig. 3.4*. The first two longitudinal locations ($x_n = 7.62$ and 12.70 mm) were placed as close to the apex as possible since it was necessary to maintain a minimum allowable separation between the nozzle passages at the highest azimuthal position, i.e., farthest from the windward meridian. The third family of nozzles at $x_n = 19.81$ mm approximates the location of forward-blowing nozzles on previous tangent-ogive models (Alexan, 1992; Lee, 1995).

A nominal azimuthal position of $|\theta| = 135$ deg was chosen to be common with each family of nozzles. This position is consistent with previous work. At the foremost location only one additional set of nozzles was placed at $|\theta| = 120$ deg because of space limitations. Due to a larger local radius at $x_n = 12.70$ mm, a greater array of nozzles were positioned at 4-deg intervals[†]. Only two additional azimuthal positions were selected for $x_n = 19.81$ mm to limit the scope of the test matrix.

3.2.3 Forebody Blowing System

The blowing system (Figs. 3.3 and 3.5) provides controlled air flow to the pair of nozzles in the forebody tip. The system is comprised of two pneumatic lines, each dedicated to supply air to one nozzle. The components of each pneumatic line primarily consist of a

*The tip configurations were identified early in the project. Nozzles were located relative to separation lines extrapolated from surface flow visualization data compiled for an earlier ogive-cylinder model (Lee, 1995). The apex semi-angle for this model, however, was 28 deg which is more than twice the apex semi-angle of the current model. In view of the caution raised by Beyers and Ericsson (1998) regarding the application of results for low-slenderness forebodies to those of high slenderness, it now appears that the process of selecting the nozzle locations for this experiment may have been somewhat misguided.

[†]One tip from this family was omitted from the test matrix because of a manufacturing defect.

solenoid on/off valve, an in-line metering device and a delivery tube. These components are mounted on an aluminium platform that is supported by two widely spaced rings, allowing the entire system to be readily installed or removed and safely contained within the cavity of the forebody model (Fig. 3.6). Receptacles at the end of the system corresponding to the base of the forebody, permit electrical contacts for instrumentation and valve excitation. A schematic of the pneumatic circuit is shown in Fig. 3.7.

The solenoid valve is an ASCO/Angar Model AL requiring an excitation of 12 volts DC and 2 watts. This model was not the valve of choice; the ASCO/Angar Model 008 used in the earlier experiments (Lee, 1995) was preferred because of its high-speed characteristics, i.e., fast valve-opening and closure times. Unfortunately the Model 008 was discontinued. The Model AL is a normally-closed valve with an orifice diameter of 1.40 mm, is rated to operate to a supply pressure of 690 kPa, and has a flow factor (C_v) of 0.038[‡]. Based on a test section velocity of 107 m/s and the anticipated need of attaining a C_μ as high as 0.018 for the family of nozzles located farthest from the apex, it was determined that for $C_v = 0.038$ the Model AL was acceptable to pass the desired range of flow rates.

The solenoid operates unidirectionally, opening the valve when it is energized. A return spring closes the valve once the magnetic field decays sufficiently to allow the spring force to overcome the retention force of the solenoid. The time for the valve to close can be hampered, however, if the collapse of the magnetic field is slow. In fact, the valve closing time was initially estimated to be about five times the opening response time. A slow closure time limits the highest switching frequency a valve can be operated at; also, the inequality of the opening and closure times has an impact on the switching control of

[‡]The flow factor is a non-dimensional representation of flow rates that can be passed by the valve. The flow factor relates the flow rate with other parameters by the expression

$$SCFH = C_v \times F_g \times F_{sg} \times F_t \quad (3.1)$$

where $SCFH$ represents standard cubic feet per hour; F_g is a *graph factor* which is dependent on the valve inlet pressure and the differential pressure across the valve; F_{sg} , is a *specific factor* that accounts for the type of fluid passing through the valve (for air, $F_{sg} = 1$); and F_t is a *temperature compensation factor* which is typically set to unity for room temperature conditions. Estimating F_g requires engineering data from the vendor.

the valves (Appendix A). To improve the closure time, a current switching circuit was designed (Fig. 3.8) to eliminate the residual current that momentarily maintains the magnetic field. Essentially this circuit has the effect of an energy sink so that when the solenoid is de-energized the magnetic field will collapse sooner. In fact, the current switching circuit reduced the closing time of a valve from approximately 20 ms to 8 ms. The switching circuit was located on the valve-command path between the power amplifier and the valves (Fig. 3.19).

An in-line metering device, mounted directly into the delivery port of the solenoid valve, serves as the means to measure flow rate and to feedback the valve state. The meter block (Fig. 3.9) has sharp-edged, cylindrical passages drilled into it to form an orifice obstruction that is necessary to create a pressure differential (Δp). The design of the metering device was governed by the desire to develop a linear relationship between the nozzle mass flow rate and Δp which occurs under choked flow conditions. The one-piece cylindrical design was selected because choked flow conditions can be made to occur when the length of the orifice (t) is long relative to its diameter (d). In a cylindrical orifice, choked flow takes place at the vena-contracta formed by an annular separation bubble which develops at the entrance to the sharp-edged restrictive passage. Another reason for the selection of this design is that it is easy to manufacture on a miniature scale unlike commonly-used thin-plate orifices. Supported by theoretical and experimental evidence, Ward-Smith (1979) showed that for sharp-edge orifices a range of t/d values exist within which the critical discharge coefficient (C_d^*) is constant and independent of Reynolds number (Fig. 3.10). The value of C_d^* is immaterial in the context of the present experiments since the mass flow rate was calibrated at the nozzle exits in the forebody tip. The calibration process is described in Appendix B. According to Fig. 3.10 a short metering device ($1 \leq t/d \leq 7$) yields a high C_d^* , requires a low supply pressure, and should set up an adequate Δp for a strong signal from a differential pressure transducer mounted across the orifice. Consequently it was decided to configure the meter using $t/d = 5$ and an orifice-to-blowing-nozzle-diameter ratio (β) of 0.5[§]. Since the delivery tube was sized

[§]For the metering device used in Lee (1995), $t/d = 5$ and $\beta = 0.5$. Sufficient data was available to estimate C_d^* to be approximately 0.844 just as choked flow conditions occurred in the meter. Surprisingly

for a 1.52-mm inside diameter (to match the nozzle diameter), the orifice diameter became 0.76 mm and the length of the restriction was set to 3.81 mm (Fig. 3.9). Static pressure taps of 1.52 mm diameter were located fore and aft of the orifice. The upstream pressure tap was located $5.4d$ from the orifice while the downstream tap was placed at a distance of $0.5d$, directly at the exit of the orifice in a region of separated flow. The placement of the downstream tap was intended to ensure that the pressure transducer sensed the maximum pressure difference across the orifice and generated a high output signal.

An IC Sensor Model 32A piezo-resistive differential pressure transducer measured the pressure across the metering orifice. This transducer is rated for a maximum differential pressure of 345 kPa, has a nominal sensitivity of 1.5 mv/psid, and is temperature compensated. The pressure side of the transducer is located and bonded to the static pressure tap upstream of the metering orifice (Fig. 3.9). Tight-fitting Tygon tubing connected the reference port of the transducer to the downstream static pressure tap. Teflon-coated, 30 AWG wires were soldered to the lead pins according to the schematic shown in Fig. 3.11. A third transducer (IC Sensor Model 32A, rated at 690 kPa) was bonded to the air supply manifold, upstream of the valves. This transducer monitored the gauge pressure of the supply air, using the atmospheric pressure of the forebody cavity as reference. The output signals of the three pressure transducers were sent to a pre-amplifier stage placed on the blowing system platform. The transducer output signals were amplified by a factor of approximately ten. The stage was conveniently fastened to a rail connecting the support rings, as can be seen in Fig. 3.5.

Thin-walled steel tubing delivered the flow of air from the metering device to the nozzles in the forebody tip. The tubing has an inside and outside diameter of 1.52 mm and 1.88 mm respectively. Each delivery tube was carefully formed into a shallow S-shape to connect the non-aligned flow axes of the metering device and the blowing nozzles. The tubes were firmly anchored to the metering devices with silver-soldering; at the tip end, a close sliding fit permitted the forebody tip to be readily installed or removed from the

the result is in reasonable agreement with the findings shown in Fig. 3.10 despite β being as high as 0.5 (the results by Ward-Smith are for smaller β). It was decided to retain these values of t/d and β for the current meter design.

tubes. The long length of the tubes made them sufficiently flexible to accommodate the distance between the nozzle passages, which varies with the azimuthal location of the nozzles in the tip. The delivery tubes, in-line metering devices and valves are shown as assembled in Fig. 3.12.

The base of the forebody model served as a logical junction point for all instrumentation and excitation wires leading to the blowing system. The junction consisted of two female Molex receptacles supported by a thin plate which sat firmly within the aft support ring. Wires emanating from the sting were connected individually to the junction with male pins (Fig. 3.13). This arrangement allowed the removal of the forebody to be relatively easy if it was required for inspection, for example. A layout of the junction array is presented in Fig. 3.14. Note the inclusion of an excitation monitor for one pressure transducer. The purpose of the monitor was to ensure that 5 volts DC was applied at the pressure transducers; a slightly higher voltage was applied at the DC supply to compensate for a voltage drop across the length of the conductors.

3.2.4 Cylindrical Afterbody

To investigate the technique of dynamic manipulation of forebody vortices in the absence of wings, the delta-wing model was replaced by a cylindrical afterbody. This afterbody is a replica of the cylindrical fuselage of the delta-wing model. The material selected for the cylindrical model was mild steel rather than aluminium. To properly bond the cylindrical model to a carbon-fibre trilobe adaptor (required to mount the model on the balance), a steel surface was recommended for the model in order to obtain the best performance of the adhesive. Unfortunately the use of steel has the undesirable effect of lowering the natural frequency of the model-support system. Fig. 3.15 illustrates the cylindrical afterbody and forebody installed on the balance.

3.2.5 Blockage Correction

Solid blockage due to the model, sting, and the strut was considered significant for this experiment: for an angle of attack of 50 deg and no roll, the blockage was estimated to be 8.8%. Efforts to develop blockage correction schemes that are suitable for high-angle-of-attack testing in wind tunnels have met with some success (Gili et al., 1990). Such schemes are, however, beyond the means of the current experiment. Instead, the simple blockage correction (ϵ_t) suggested by Barlow et al. (1999) was implemented:

$$\epsilon_t = \frac{1}{4} \frac{\text{model and support frontal area}}{\text{test section area}} . \quad (3.2)$$

The numerator in Eqn. 3.2 is the sum of frontal areas of four components, i.e., the forebody, delta-wing, sting, and the strut. The frontal area was based on the planform area of each component. For instance, the frontal area of the delta-wing is affected by the angle of attack (σ) and the roll angle (ϕ) so that

$$A'_{dw} = A_{dw} \sin(\sigma) \cos(\phi)$$

where A'_{dw} is the frontal area of the delta-wing component and A_{dw} is the planform area. The forebody and sting are both bodies of revolution that are affected by σ only:

$$A'_{forebody} = A_{forebody} \sin(\sigma)$$

$$A'_{sting} = A_{sting} \sin(\sigma) .$$

The post frontal area is equal to the planform area and is unaffected by σ and ϕ . The blockage correction was applied to the dynamic pressure (q_∞), freestream velocity (V_∞), and Reynolds number (R_e) as follows:

$$q_\infty = q_{\infty_u} (1 + 2\epsilon_t) \quad (3.3)$$

$$V_\infty = V_{\infty_u} (1 + \epsilon_t) \quad (3.4)$$

$$R_e = R_{e_u} (1 + \epsilon_t) \quad (3.5)$$

where q_{∞_u} , V_{∞_u} , and R_{e_u} are uncorrected quantities (Barlow et al., 1999).

3.3 Internal Balance

Aerodynamic forces and moments were measured with an existing five-component internal balance (Fig. 3.16). Axial force was not measured. The balance has a simple, single-piece design and is made from stainless steel (Custom 455). It features two flexures separated by a distance of 50 mm. Each flexure has an octagonal cross-section which provides flat surfaces for mounting strain gauges. A pair of foil strain gauges were bonded onto six of these flats. Strain-gauge pairs on diametrically opposing flats were connected to form a Wheatstone bridge for increased sensitivity and accuracy of an output signal that is proportional to either a pitching, yawing, or a rolling moment[¶], while also minimizing the effect of temperature^{||}. The balance design loads, evaluated at the forward and aft flexures, are 339 N-m for pitching and yawing moment, and 113 N-m for rolling moment. The resolving centre of the balance, i.e., the point where the flexure moments were converted to an equivalent force and moment pair, was located at the mid-point between the two flexures. The force and moment were transferred to the moment reference point located at 30% of the mean aerodynamic chord of the delta-wing (Fig. 3.2).

The balance was located on the end of the sting with a precision taper and alignment pins, and secured with setscrews. At the model end, the balance has a precision tapered trilobe onto which the model was conveniently mounted and secured with a retainer screw. A 12.7 mm diameter internal passage extends over the length of the balance, serving as a conduit for an air supply tube and wire bundles leading to the forebody.

Details of the calibration of the balance, the development of the inverse sensitivity matrix, and an uncertainty assessment are presented in Appendix C.

[¶]With two roll bridges available, measurement of the rolling moment was redundant. Only data from the forward roll bridge was acquired during the test.

^{||}Loads were estimated to increase by 0.5% per 100-deg F increase.

3.4 Support System

The sting is a stainless-steel seamless tube that is tapered from its joint with the support strut to the aft end of the model. The exposed length of the sting — the distance along the sting axis between the aft end of the model and the support strut — is 87.6 cm. As mentioned in the previous section, it has a precision taper to accommodate the strain-gauge balance. An internal bore extends over the entire length of the sting 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 were routed through the open end of the sting and into a slot in the strut support, which was used as a conduit to the space beneath the working section floor. The sting was fastened to the support strut with a self-holding, tapered joint. The joint was tightened by a large hex nut threaded onto the end of the sting protruding through the support strut. When the joint is relaxed the sting is permitted to roll.

A digital inclinometer measured the roll angle of the model. The inclinometer was placed on an adaptor that fits onto two flats machined on the sting. The inclinometer sits on a reference surface that is parallel with the plane of the wings, and so the angle indicated by the inclinometer corresponds to the roll angle of the delta-wing model. The angular misalignment between the plane of the wings and the reference surface was estimated to be approximately 0.05 deg. The error in setting the roll angle with the inclinometer was about ± 0.10 deg.

The support strut is a mild steel seamless tube with an outside diameter of 12.7 cm and a length of 211 cm, which spans the entire height of the working section and extends beneath the floor turntable. Fastened firmly to the roof and floor turntable, the strut provided sturdy support to the sting. The support strut was tightly bolted to the external balance of the facility with an attachment flange that was welded at its bottom end and strengthened with gussets. The external balance was locked during the test to ensure a firm base and prevent movement. The strut was supported at the roof by a solid steel spindle that passed through the roof turntable and slid into a sleeve which formed part of the cap assembly welded to the top of the strut. The fit between the sleeve and spindle was precise and

did not allow for significant slack. In turn, the spindle was firmly clamped between two channel beams that form part of the structure of the roof turntable.

3.4.1 Support Interference

The presence of a downstream support system has the potential to adversely affect aerodynamic measurements by influencing the wing leading-edge vortices (Ericsson, 1991). The support strut was the primary concern. At low angles of attack the proximity of the support strut to the wake of the model (denoted by h in Fig. 3.17) may be close enough to promote the breakdown of a wing leading-edge vortex and consequently influence C_z , C_m , and C_ℓ . It is not certain whether the effect of support interference would in turn affect the forebody vortices and influence C_Y and C_n in this regime of angle of attack. For high angles of attack, however, there was less concern about support interference because of the substantial clearance between the model and the support strut as can be seen in Fig. 3.1. The effect of the sting was not considered significant because the wing vortex tends to leave the trailing edge of the wing at an outboard point (Fig. 3.18) on the span and is believed to be beyond interference with the sting.

Thus the problem of support interference reduced to selecting a sting length that would give satisfactory clearance between the model and the support strut at low angles of attack. In addition the natural frequency of the sting/model system, which is governed significantly by the length of the sting, must be greater than the highest expected valve switching frequency. Given these selection criteria and without the guidance of previous work or knowledge of the track of a wing leading-edge vortex beyond the trailing edge, a sting length of 87.6 cm was chosen. Treating the sting and model as a cantilever and lumped mass system, a natural frequency of 60 Hz was estimated and deemed acceptable when the sting was designed. Following the installation of the apparatus in the wind tunnel, however, the natural frequency of the support system with the model attached was found to be 30 Hz in both the vertical and lateral axes of the model[†].

[†]The natural frequency was estimated from balance signals caused by an impulse load applied to the model at a point in front of the balance.

3.5 Data Acquisition and Reduction

This section describes the aerodynamic data acquisition system, its coordination with other information systems (such as the wind tunnel data system), reduction of the data, and validation of the data systems. A description of an emergency blowing system that operated separately from the data acquisition system follows in Section 3.6.

3.5.1 Aerodynamic Data Acquisition System

The aerodynamic data system conditioned and sampled the output signals of the balance and the pressure transducers inside the aircraft model. The data system also generated the control waveforms that operated the valves in the forebody and synchronized this control with the sampling of the balance and pressure transducers signals. Twelve channels of data were acquired, comprising:

- two channels for valve control waveforms;
- five for balance output signals;
- three for pressure transducer output signals; and
- two channels for monitoring the excitation voltage for the balance and pressure transducers.

The processed outputs of the balance, the pressure transducers, and the valve control waveforms were ensemble-averages taken over a sufficiently long period of time to minimize the effect of noise. The process of ensemble-averaging requires the sampling of data to be phase-locked with a reference signal so that the temporal relationship between a point in the reference signal and a point in the data record is consistent across all records acquired during the sampling period, thereby allowing a reliable computation of the ensemble-average record. For this experiment, a valve control waveform (i.e., the port valve control waveform) was the logical choice as the reference signal because the phenomenon under investigation responds to the cycle of alternating blowing arising from the control waveforms. Thus, the outputs of the transducers and the valve control waveforms are ensemble-averages taken over many alternating-blowing cycles.

The path of the signals, the conditioning process, and the various components of the aerodynamic data acquisition system are shown in the instrumentation block diagram (Fig. 3.19). As with the delta-wing model, this system was developed and used in the dynamic wind-tunnel experiments mentioned earlier. The computer in the system was a Series 7200 model by Concurrent Computer Corporation. This computer features two 68040 Motorola processors and a real-time UNIX operating system that quickly responds to interrupts. It is augmented by a high-performance digital signal processing subsystem known as the intelligent bus interface module (IBIM). The IBIM has its own RISC 9600 Intel processor, onboard memory, and access to a library of data acquisition and instrument control routines. The module also possesses a 16-channel analog-to-digital (A/D) conversion subsystem that provides 16-bit resolution and a sampling rate of 150 kHz per channel. The phase accuracy is 0.2 deg at 25 kHz between each channel. An accompanying input/output board contains clocks that were used to generate the valve control waveforms. The data acquisition program is actually performed on the IBIM, the accrued data stored in its own memory. Once an acquisition cycle is complete, the data is transferred to the host computer which organizes and commits the data to storage on a hard drive.

The valve control waveforms were created by a programmable waveform generator. The generator received the parameters to define each command waveform from the IBIM, and was triggered by the clocks on the IBIM to produce and send the waveforms. Both waveforms were directed together to be conditioned and sampled synchronously with the other ten channels, and to the forebody model via a power amplifier that raised the voltage of the signals to a level required for operating the solenoid valves.

The output signals of the balance and pressure transducers underwent three stages of progressive filtration/amplification to raise the signal level to the ± 10 -volt input range of the A/D converter. The first was a fixed-gain stage (no filtering) that boosts the output signals to make them less susceptible to common-mode noise. An isolation amplifier located immediately underneath the test section amplified the balance signals, and a pre-amplifier stage placed within the forebody model amplified the output signals of the

pressure transducers. The amplified signals of the balance and the pressure transducers, together with the valve control waveforms and the excitation monitors, were subsequently passed through the second-stage Preston low-pass filter/instrumentation amplifiers. The gain setting on the Preston devices was adjustable.

Following the Preston stage the balance and pressure transducer signals were directed to a DC Offset Controller which nullifies, as best as possible, the DC voltage content in an analog signal. Otherwise, small-amplitude alternating output signals may be overwhelmed by significant levels of voltage offset and may not be adequately captured during the A/D conversion process. The offsets were measured (a process referred to as *buck-ing*) and applied actively by the Offset Controller to each balance and pressure transducer output signal. These DC offsets were not invoked during data reduction and are not to be confused with the offsets derived by the electrical calibration of the instrumentation. The bucking program was executed separately from the data acquisition program.

All channels were passed through instrumentation filter/amplifiers, the last stage of signal conditioning before A/D conversion. The frequency cutoff of the low-pass filter was set to 500 Hz for the entire test. Since the sampling frequency of each channel was always greater than 2800 Hz, an unambiguous construction of each digital signal was possible and aliasing avoided. The gain setting on these filter/amplifier devices was also adjustable.

The adjustable gains of the Preston and the instrumentation amplifiers allowed the opportunity to maximize the dynamic range and take advantage of the resolution of the A/D converter. A change of total gain was required, most often, when a change of test conditions was perceived to lead to a corresponding change of maximum aerodynamic loading. The dynamic range was maximized by adjusting the gains of the Preston and the instrumentation amplifiers so that the voltage level of each output signal was approximately 20% less than the saturation values of the A/D converter (the margin allows for overshoot of the signal). With each change of gain setting, however, it was necessary to invoke a calibration subsystem (Fig. 3.19) to determine the voltage offset and the total gain across the Preston and the instrumentation amplifiers for each channel. Generally

the process involved injecting a known voltage at the input to the Preston amplifier, via the signal/calibration channel selector, and measuring the voltage at the output of the instrumentation amplifier with the A/D converter. After obtaining four pairs of these input and output voltages, a straight line fit of the calibration data is calculated, yielding the total gain and the DC offset. The fixed and variable gains, and DC offsets for each channel were stored with each applicable data set for convenient retrieval later during the data reduction process. The calibration program was also separate from the data acquisition program.

The data acquisition program is complex and will be described in general terms only. Its essential features include:

- building and following a test-points map, based on test conditions specified for each test run (refer to Section 3.5.2 for more details);
- generating the valve control waveforms for each test run and acquiring data phase-locked with the port valve control waveform over many blowing cycles;
- maintaining an ensemble-average record for each channel;
- accumulating the square of signal magnitude for each corresponding sample point of a channel;
- calculating and storing the time-average of each record acquired for a every channel; and
- storing the acquired data on file as measured voltages, together with configuration and calibration information, at the conclusion of each test run.

The summation of the square of the signal magnitude was used during data reduction to compute the standard deviation of each sample point in an ensemble-average record; the standard deviation serves as an indicator of sample point variance.

The purpose of retaining the time-average of each acquired record was to determine whether the time-average of a balance channel, in particular, was subject to drift. For other channels, such as the differential pressure transducer across the metering orifice, this

information has no value. However, to allow for these *exceptions* in the data acquisition programming requires additional IBIM processing-time and causes a curtailment of the highest possible valve-switching period.

3.5.2 Coordination of Systems

The aerodynamic data acquisition system operated in conjunction with the wind tunnel data acquisition system and an Excel spreadsheet. The spreadsheet contained the test matrix that controlled the progress of the experiment, and the wind tunnel data system acquired tunnel conditions and controlled the turntables.

Three text files linked the computer systems (Fig. 3.20). The test matrix spreadsheet creates the *Test Points* and *Yaw Map* files at the start of a series of runs, or test points. The Test Points file specifies the test conditions for each run to be performed, and the Yaw Map file contains the turntable angle required for each run. After receiving the instruction to acquire data, the aerodynamic data system read the Test Points file, prepared a test points matrix, and then waited for a cue from the wind tunnel data system to proceed with data acquisition. At the same time the wind tunnel operator instructs the wind tunnel data system to read and interpret the Yaw Map file. After the turntables have been rotated to the orientation required for the upcoming run, the wind tunnel data system writes a *Tunnel Conditions* file, containing information such as test section velocity, Reynolds number, turntable angle. The existence of this file is the cue required by the aerodynamic data system[‡]. At this point the aerodynamic data system retrieves the contents of this file and proceeds with the acquisition of data. Upon completing the acquisition of data for a test point, the operator was verbally notified to instruct the wind tunnel data system to proceed to the next test point in the yaw map.

The Test Points, Yaw Map, and Tunnel Conditions files reside on a network file system hard disk which is accessible to each of the three computer systems.

[‡]The aerodynamic data acquisition system frequently monitored the timestamp of the tunnel conditions file. A change in the timestamp indicated to the aerodynamic data system the readiness of the model for the next run.

3.5.3 Data Reduction

The reduction of raw data to produce non-dimensional aerodynamic loads and nozzle flow rates was performed separately from the data acquisition process. Executed on the main processor of the Concurrent computer, the data reduction program worked with corresponding wind and tare raw data files to produce *load* files. A load file stores the ensemble-average time-histories, the sample-point standard deviations, and the instantaneous time-averages for the non-dimensional aerodynamic loads and the nozzle blowing coefficients. The load file also contains information describing the associated test conditions.

The data reduction program is not considered in detail herein. Instead, the set of equations necessary to produce the non-dimensional aerodynamic loads and the sample-point standard deviations are described in Appendix D.

3.5.4 Validation

The validity of the data acquisition and reduction processes was confirmed with measurements of mean aerodynamic loads and nozzle volume flow rates. Mean aerodynamic load data was acquired at $\alpha = 30$ deg, $V_\infty = 91.4$ m/s, and for four roll angles ($\phi = 0, 10, -15$, and -30 deg) with an elliptical forebody model (major axis parallel with the plane of the wing) fitted to the delta-wing model. These measurements were compared with those compiled in the 7 ft $\times$ 10 ft (2m $\times$ 3m) wind tunnel at the Subsonic Aerodynamics Research Lab (SARL) for the same model configuration and test conditions. The results for C_Z , C_m , C_Y and C_n for the present installation are in good agreement with the SARL data (Fig. 3.21).

Nozzle volume flow rates were verified in situ with a rotameter. Measurements with the data system were found to be within 2% of the volume flow rate indicated by the flow meter.

3.6 Emergency Blowing System

During a preliminary wind tunnel entry a significant vibration was frequently encountered in which the model entered into a diverging lateral oscillation at the resonant frequency of the model-support system. The source of excitation was suspected to be a positive feedback between the motion-induced sideslip, which switched the forebody vortices, and the resulting change in side force. This appeared to be confirmed by the fact that blowing high-pressure air through one nozzle quickly alleviated the vibration, presumably by forcing the forebody vortices to remain steady and prevent further switching of orientation. Moreover, this observation indicated how the problem could be mitigated. If left unchecked, persistent vibration might have caused failure of the strain gauges on the balance or, worse, catastrophic failure of the balance and attendant damage to the model. It is recognized that this vibration problem is peculiar to these experiments because it results from the presence of a rather low frequency resonance of the model support system. Such a resonance would not be encountered in free flight.

A two-step emergency procedure was developed to limit the occurrence of the vibration. Once the amplitude of the vibration exceeded a prescribed threshold an emergency blowing system automatically applies steady, high-rate blowing through one nozzle. At the same time an aural cue alerts the wind tunnel operator to respond to the emergency by rapidly decreasing the wind velocity in the test section. The basis for the latter measure is that the reduction of the velocity decreases the strength of the forebody vortices and the intensity of the side force.

A block diagram of the emergency blowing system is presented in Fig. 3.22. Estimates of instantaneous tip displacement in the lateral and the vertical direction are computed by a LabView program monitoring the output signals of two accelerometers mounted within the forebody shell (Section 3.6.1). When the norm of these displacements exceeds a limit of 12.5 mm[§] the LabView program activates two switching relays to intervene with the forebody blowing system. The program triggers one relay switch that terminates the con-

[§]This limit provides some margin against a maximum forebody-tip deflection of 19.1 mm that was observed at the resonance frequency of 30 Hz.

trol of the valves by the data acquisition system, leaving the valves in the emergency configuration, i.e., port valve open, starboard closed. The trigger of a second relay energizes a solenoid valve that switches the source of blowing air from the regulated low-pressure line to the emergency high-pressure line (690 kPa; also shown in Fig. 3.7). The signal to the solenoid valve also activates the aural alarm that warns the wind tunnel operator of the emergency. Emergency blowing could also be initiated manually by software and hardware *panic buttons*.

The emergency blowing system proved to be very successful in suppressing the oscillation in the majority of occurrences. In two instances in which emergency blowing was ineffective, the reduction of the test section velocity alleviated the vibration, albeit tardily.

3.6.1 Tip Displacement Monitor

A pair of accelerometers, oriented to sense acceleration in the lateral and vertical directions, are placed at the fore end of the platform in front of the metering devices (Figs. 3.3 and 3.12), a location that is as close to the forebody tip as practicable. The instruments are Entran EGA-125-50 miniature, piezo-resistive accelerometers with a range of $\pm 50g$ and a nominal sensitivity of 4 mV/g. This model was selected to take advantage of its high sensitivity in spite of the fact that, with accelerations estimated to be $\pm 68g$, the rating of the instrument was expected to be exceeded during an encounter with resonance.

The detail of the blocks in Fig. 3.22 representing an accelerometer, signal amplification, filtering, and processing are shown in Fig. 3.23. The output voltages of the accelerometers are pre-amplified and passed through an anti-alias filter before being sampled and received by the LabView program. The amplifier and filter components are mounted on a board fitted within the forebody shell (Figs. 3.3 and 3.5). In LabView the signal is passed through a double-integrator to transform it from representing acceleration to a signal representing displacement. A calibration constant K_T , which converts the displacement from voltage to engineering units, is developed as follows: The voltage

representing lateral acceleration, for example, is

$$V_{Ay} = K_{Ay} \omega^2 \Delta y \quad (3.6)$$

where K_{Ay} represents the sensitivity of the accelerometer; ω , the angular frequency of vibration; and, Δy is the amplitude of lateral vibration in engineering units. The voltage V_y representing lateral displacement is determined from

$$\begin{aligned} V_y &= V_{Ay} K_{1y} K_{2y} \frac{K_{3y}}{\omega^2} \\ &= (K_{Ay} \omega^2 \Delta y) K_{1y} K_{2y} \frac{K_{3y}}{\omega^2} \\ \therefore V_y &= K_{Ty} \Delta y \\ \text{or } \Delta y &= \frac{1}{K_{Ty}} V_y \end{aligned} \quad (3.7)$$

where K_{1y} is the gain of the pre-amplification stage; K_{2y} , the gain of the anti-aliasing filter; K_{3y}/ω^2 is the gain of the double-integrator; and $K_{Ty} = K_{Ay} K_{1y} K_{2y} K_{3y}$ is a conglomerate of gains that is independent of frequency. To calibrate the system — i.e., determine the value of K_{Ty} — the accelerometer output signal was synthesized to simulate a resonance condition. The frequency of the artificial voltage signal was set to 30 Hz with an amplitude, in accordance with Eqn. 3.6, that is representative of the maximum deflection at the forebody tip ($\Delta y_o = 19.1$ mm). The signal was applied to the system and the displacement signal V_{yo} was measured. The constant K_{Ty} followed from Eqn. 3.7. The system produced good results for displacement when an accelerometer was tested on a shaker table at arbitrary frequencies and amplitudes.

The LabView program ran on a Pentium-based computer not linked to the other computers involved in the test. Consequently the accelerometer data was not synchronized with the data acquisition system. Moreover, the data was not stored.

3.7 Flow Visualization

Force and moment measurements were complemented with flow visualization on the forebody surface. Vacuum pump oil mixed with titanium dioxide was found to be an appropriate visualization fluid for the velocities investigated in this test. The mixture was pulse-sprayed onto the surface of the forebody, forming small dots that do not bias the direction in which the mixture will flow under the influence of shear stresses in the boundary layer (unlike applying the mixture with a brush). Unaffected oil dots were useful for identifying areas of separated flow. For *wind-on* conditions, a video record and still photographs were taken of the surface patterns on the leeside of the forebody as the skin friction lines evolved in the presence of steady blowing from either the port or starboard nozzle. Still photographs were also taken with *wind-off* before the results smeared under the influence of gravity. All photography was performed under normal fluorescent lighting.

Off-surface flow visualization, by means of laser-light sheet illumination, could not be performed because the required apparatus was unavailable. This is unfortunate because with a high-speed video camera and imaging system, cross-sections of the forebody flow could have been captured as compelling visual evidence of the dynamic manipulation of the forebody vortices. Moreover, the video record would have been useful for estimating the aerodynamic response time of the forebody vortices due to blowing at the tip, as demonstrated previously by Lee (1995).

3.8 Test Program and Procedures

The test program for this investigation is summarized in Table 3.1. The table is organized parametrically and does not necessarily reflect the sequence of measurements.

Force and moment measurements were made for three blowing modes, namely *no blowing* (i.e., baseline), *steady blowing*, and *dynamic blowing*. The term *steady blowing* signifies that only one valve is fully and continuously open throughout the data acquisition period, the other valve being closed (in this mode of operation there is no valve control period per se). For steady blowing, the duty-cycle fraction (τ/T) is 100% or 0%. *Dynamic*

blowing refers to the alternating operation of the valves, i.e., within one valve control period, the valves are open for certain periods of time according to the valve control waveforms. For a duty-cycle sweep — in which the duty cycle of the port valve is varied from 0% to 100% and the duty cycle of the starboard valve follows as the complement — both steady and dynamic blowing modes are evident.

Force and moment measurements were organized as *sweeps* of independent parameters such as the turntable angle (σ), coefficient of blowing momentum (C_μ), and valve duty cycle (τ/T). Sweeps of these parameters were performed for various combinations of nose tip, afterbody configuration (i.e., delta-wing or cylindrical afterbody), test section velocity (V_∞), roll angle (ϕ), and alternating blowing frequency (f). The majority of measurements were made for the steady and dynamic blowing modes. Baseline measurements were not taken with the delta-wing at the non-zero roll angles, nor was baseline data acquired for the cylindrical-afterbody configuration.

Flow visualization measurements are not shown in Table 3.1. Surface flow visualizations were conducted at velocities of 18.3 and 36.6 m/s with the model oriented at $\sigma=45$ deg with no roll. Force and moment measurements were not taken simultaneously.

The procedure for acquiring balance data typically involved the following sequence of tasks: setting the model configuration and roll attitude; conducting an electrical calibration of the data system (if necessary); preparing a selection of runs from the test matrix for submission to the aerodynamic data acquisition system; setting the desired nozzle blowing rate; initiating and monitoring the convergence of the ensemble-average data and the condition of the tip displacement during acquisition; and reducing the data after corresponding wind and tare data files were acquired. An elaboration of some tasks follows:

- *Electrical calibration of the data system.* Bucking (Section 3.5.1) the balance and the differential-pressure transducers, and the electrical calibration of the signal conditioning devices, were time-consuming steps that were conducted only when necessary. The conditions under which bucking and calibration were carried out include:
 - At the start of the test day;

- Following a substantial period of inactivity to account for drift in the offset voltages;
- After the amplifier gains were adjusted to account for a change in expected maximum aerodynamic loads.

An electrical calibration was performed more frequently for the last condition.

- *Setting the data acquisition parameters.* The ensemble-average record for each data channel is formed from a specified number of instantaneous records, each with a length of 512 sample points covering one alternating-blowing cycle. The number of instantaneous records taken during the data acquisition cycle depends on the blowing mode. For dynamic-blowing modes, 150 records (covering 150 blowing cycles) were acquired at the frequency of alternating blowing; for baseline and steady-blowing modes, for which there is no alternating blowing, 100 records were acquired at a fixed rate of 9 records per second. The resulting sampling frequency is the product of the number of sample points in the instantaneous record and the rate at which the records are acquired; for example, the sampling frequency of each channel for a dynamic-blowing case with an alternating-blowing frequency of 5.5 Hz (the lowest in the test), is

$$512 \text{ samples/record} \times 5.5 \text{ records/second} = 2816 \text{ Hz(samples/second)}.$$

The length and number of instantaneous records, and the rate at which these records are acquired, were entered in the Excel test matrix.

- *Monitoring the data acquisition cycle.* After the acquisition cycle was started, the convergence of the ensemble-average data and the condition of the tip displacement was carefully monitored. An array of windows was created by the aerodynamic data acquisition system on the terminal screen of the Concurrent computer to display the ensemble-average signal of each channel; the tip displacement was monitored on the data acquisition computer of the emergency blowing system (Section 3.6). The active display of the ensemble-averages also allowed the rapid detection of a disabled channel, such as a balance component or an inoperative valve. Oscilloscopes installed in the instrumentation rack for each channel (Fig. 3.19) served the same purpose.

- *Setting the nozzle blowing rate.* The volume flow rate required for the desired coefficient of blowing momentum was manually set by adjusting the supply pressure regulator in the control room (Fig. 3.7) to obtain the appropriate output voltage of the port differential-pressure transducer. The volume flow rate was always set with wind-on and with the port solenoid valve open. For a C_μ -sweep, it was often necessary to determine a range of C_μ within which the model was prone to serious oscillations due to resonance. The lower C_μ -limit was found by increasing C_μ from zero and marking the point at which the growth of tip displacement began on the monitor of the emergency-blowing data system. Likewise, an upper limit was found by decreasing C_μ from an initial value thought to be greater than that of the upper limit.

A tare file was acquired for each wind data file, i.e., each data point. During a tare run, nozzle blowing was activated in order to account for the effects of blowing momentum on yawing moment and pitching moment that may arise from the thrust force due to the nozzle flow (recall that the balance did not measure axial force). Because nozzle blowing was activated during the tare run and to preserve the nozzle flow rate data, the wind signal of the nozzle flow rate was not tared during the data reduction process.

Blowing Mode	Primary Sweep Parameter	Model Configuration		V_∞ (m/s)	Model Attitude		Blowing Parameters			
		Body	Tip		σ (deg)	ϕ (deg)	Target C_μ	f (Hz)	τ/T (%)	
No Blowing (baseline)	σ	Delta-wing	6-1	18.3	0–80, $\Delta=5$	0	0	n/a	n/a	
					15–70, $\Delta=5$	$\pm 20, \pm 10$				
				36.6	45	0				
					15–70, $\Delta=5$	$\pm 20, \pm 10$				
			2-2	18.3	15–70, $\Delta=5$	0				
					36.6					15–70, $\Delta=5$
				9-3	36.6					15–45, $\Delta=5$
Steady Blowing	σ	Delta-wing	6-1	18.3	0–80, $\Delta=5$	0	0.0052	n/a	n/a	
				36.6	15–70, $\Delta=5$		0.0026			
					54.9		10–70, $\Delta=10$			0.0026
				10–70, $\Delta=5$			0.0028			
			2-2		18.3		10–70, $\Delta=10$			0.0046
				15–70, $\Delta=5$			0.0015			
				36.6	15–70, $\Delta=5$		0.0055			
					15–70, $\Delta=5$		0.0044			
			9-3	18.3			15–70, $\Delta=5$			0.0095
					36.6		15–70, $\Delta=5$			0.0059
				18.3	15–70, $\Delta=5$		0.0055			
					36.6		15–70, $\Delta=5$			0.0035
			1-7	54.9	15–70, $\Delta=5$		0.0048			
					18.3		15–70, $\Delta=5$			0.0084
				36.6	15–70, $\Delta=5$		0.0053			
					18.3		15–70, $\Delta=5$			0.0112
			3-13	36.6	15–70, $\Delta=5$		0.0107			
					15–40, $\Delta=5$		0.0175*			
		Cylinder		6-1	18.3	15–70, $\Delta=5$	0	0.0072	n/a	n/a
					9-3	18.3		15–70, $\Delta=5$		
			3-13		18.3	15–70, $\Delta=5$		0.0148		
			Delta-wing	6-1	18.3	45		0		
	36.6				45	0.0063*				
	54.9				50	0.0063*				
	2-2	18.3	45	0	0.0047*					
			36.6		45	0.0098*				
		9-3	18.3		25–65, $\Delta=10$	0.0044*				
					36.6	45	0.0175*			
	1-7	36.6	45	0	0.0063*					
			18.3		45	0.0175*				
		11	36.6		45	0.0063*				
					18.3	45	0.0141*			
	3-13	36.6	45	0	0.0072*					
			18.3		45	0.0175*				
		Cylinder	6-1		18.3	45	0	0.0175*		
					9-3	18.3		45	0.0141*	
	3-13	18.3		45	0.0212*					

Table 3.1. Summary of test program. The asterisk (*) denotes the highest value reached in a C_μ -sweep; the symbol Δ represents *increment*.

Blowing Mode	Primary Sweep Parameter	Model Configuration		V_∞ (m/s)	Model Attitude		Blowing Parameters		
		Body	Tip		σ (deg)	ϕ (deg)	Target C_μ	f (Hz)	τ/T (%)
Dynamic Blowing	σ	Delta-wing	6-1	18.3	15-70, $\Delta=5$	0	0.0055	5.5	50/50
				36.6	15-70, $\Delta=5$		0.0026	11	
			2-2	18.3	15-70, $\Delta=5$		0.0055	5.5	
				36.6	15-70, $\Delta=5$		0.0044	11.64	
					15-70, $\Delta=5$		0.0014	11.64	
			9-3	18.3	15-70, $\Delta=5$		0.0095	5.82	
				36.6	15-70, $\Delta=5$		0.0059	11.64	
			1-7	18.3	15-70, $\Delta=5$		0.0055	5.5	
				36.6	15-70, $\Delta=5$		0.0063	11.64	
			11	18.3	15-70, $\Delta=5$		0.0084	5.82	
				36.6	15-70, $\Delta=5$		0.0053	11.64	
			3-13	18.3	15-70, $\Delta=5$		0.0055	5.5	
				36.6	15-70, $\Delta=5$		0.0021	11.64	
			6-1	18.3	15-70, $\Delta=5$		0.0072	5.82	
			9-3	18.3	15-70, $\Delta=5$	0	0.0095	5.82	50/50
	τ/T	Delta-wing	6-1	18.3	15-65, $\Delta=10$	0	0.0069	5.82	0-100 $\Delta=10$
					45		0.0069	11.64	
					45		0.0069	17.46	
					45		0.0069	20.01	
					40.45, 50.55		0.0069	5.82	
				36.6	45		0.0069	5.82	
					35-55, $\Delta=5$	0	0.0035	11.64	
					45	$\pm 20, \pm 10$	0.0024	19.0	
					45		0.0035	11.64	
					45, 50		0.0035	11.64	
				54.9	35	0	0.0031	17.46	

Table 3.1. Concluded.

Figure 3.1. Schematic of the delta-wing model installed in the test section. The view faces upstream toward the settling chamber. Positive rotation of the turntable is clockwise.

NOTE

- (1) The model moment reference centre corresponds to 30% of the mean aerodynamic chord and sits 4.7 mm behind the balance resolving centre.
- (2) Dimension represents the length of the ogive profile between the forebody apex and the point of tangency with the cylindrical fuselage.

Figure 3.2. 65-deg delta-wing aircraft model with a long, slender forebody of circular cross-section and a removable tip. The axis and sign convention are shown with respect to the moment reference on the model. The orientation of the positive z-axis is contrary to the right-handed axis system.

Figure 3.3. Cross-section of model mounted on the sting assembly.

Tip Identification	Axial Location, x_n (mm)	x_n/D	Azimuthal Position, θ (deg)	Local Radius, r (mm)	d/r
6-1 2-2	7.62	0.095	120 135	1.73	0.879
9-3 4 11-5 1-7 8 5-9 10 11	12.70	0.159	119 123 127 135 139 143 147 151	2.85	0.533
12 3-13 14	19.81	0.248	130 135 140	4.39	0.346

Figure 3.4. Nomenclature and location data for the various nozzle configurations. Note that D represents the forebody base diameter (80.01 mm).

Figure 3.5. Forebody blowing system.

Figure 3.6. Forebody blowing system with mounted tip shown beside the forebody shell.

Figure 3.7. Schematic of the pneumatic circuit for the forebody blowing system. The schematic shows the port nozzle being supplied with emergency blowing.

Figure 3.8. Schematic of the current switching circuit for the pneumatic valves.

Figure 3.9. Cross-section of the in-line metering device. The drawing is approximately 1.8 times full-size.

Figure 3.10. Relationship of discharge coefficient (C_d^*) with the nozzle length to diameter ratio (t/d). The solid line represents theory; experimental evidence is indicated by the symbols $\blacktriangle$ (Ward-Smith, 1979).

Figure 3.11. Schematic of the wire connections for an IC Sensor Model 32A piezo-resistive pressure transducer.

Figure 3.12. Front end closeup of the forebody blowing system showing the details of the delivery tubes, in-line metering devices, and solenoid valves.

Figure 3.13. External wires connected to the junction array on the forebody model.

Figure 3.14. Layout of forebody junction array. The view faces the base of the forebody model as seen in Figure 3.13.

Figure 3.15. Ogive-cylinder model shown mounted on the internal balance.

Figure 3.16. Five-component internal moment balance shown mounted on the sting.

Figure 3.17. Judging clearance between a shed wing leading-edge vortex and the downstream support strut.

Figure 3.18. The port wing leading-edge vortex appears as a condensation trail. $\sigma = 30$ deg and $\phi = 20$ deg.

Figure 3.19. Block diagram of the aerodynamic data acquisition system.

Figure 3.20. Block diagram illustrating the coordination of the aerodynamic data acquisition system, the wind tunnel data acquisition system, and the Excel test matrix.

Figure 3.21. Comparison of validation loads acquired for the delta-wing model fitted with an elliptical nose model (major axis parallel with the plane of the wing), with results reported by Hanff and Huang (1996) for similar test conditions. $V_\infty = 91.4$ m/s, $\sigma = 30$ deg.

Figure 3.22. Block diagram of the emergency blowing system.

Figure 3.23. Block diagram of the tip displacement monitor system. V represents voltage signal; Δx and Δy represent displacement in engineering units.

Chapter 4

Description of the Water Tunnel Experiment

During the course of the wind tunnel test, a peculiar sign reversal of side force and yawing moment coefficients was encountered with increasing blowing momentum coefficient. Surface flow visualization on the forebody revealed that the reversal of side force and yawing moment corresponded with a change of vortex orientation. Testing was subsequently continued in a water tunnel where flow visualization tools were available to help confirm the change of vortex orientation associated with the reversal and to investigate the cause of the phenomenon.

This chapter describes the water tunnel investigation in which two types of flow visualization experiments — dye injection and laser light-sheet — were conducted with the forebody only (i.e., no delta-wing and fin, or cylindrical afterbody). Dyed water is injected through the nozzles and upstream of the forebody to form an overall view of the forebody vortices; the laser light-sheet technique provides a different perspective with a cross-sectional view of the vortices at stations along the length of the forebody.

4.1 Motivation

An important aspect of the wind tunnel investigation was to ascertain the effect of nozzle blowing momentum on the side force and yawing moment at high angles of attack. The expected results are typified in Fig. 2.57 in which the negative side force and positive yawing moment coefficients increase proportionally over a small range of positive blow-

ing momentum coefficient (i.e., blowing through the starboard nozzle). At a certain value of blowing momentum, the side force and yawing moment are expected to level-off and become essentially independent of the blowing momentum. Similarly, negative blowing momentum is expected to give rise to positive side force and negative yawing moment. However, the results of the blowing momentum surveys for the present wind tunnel test (Section 5.4.1) show a significantly different behaviour. For example, in Fig. 5.6 the sign of the side force and yawing moment coefficients changes abruptly at a certain threshold value of blowing momentum coefficient. This *reversal* of sign signifies a change in the arrangement of the forebody vortices. Surface flow visualization performed in the wind tunnel at blowing momentum coefficients below and above the threshold indicated that the forebody vortices appeared to switch orientation.

Since the laser light-sheet system in the wind tunnel was not available during the experiment, it became increasingly apparent that a follow-on investigation in a water tunnel would be necessary to verify the vortex reversal phenomenon and to gain some insight into its cause. Testing of an isolated forebody was thought to be sufficient because side force and yawing moment measurements taken in the wind tunnel were found to be similar regardless of whether the forebody was mounted on the delta-wing model or on the cylindrical afterbody model. The water tunnel at the University of Ottawa was chosen because of its relatively large test section and the capability to conduct two types of flow visualization experiments, namely, dye injection and laser light-sheet. In the former, dyed water is injected through the nozzles of the forebody and from injection tubes located upstream of the model, and the overall forebody vortices are observed. In the latter a fluorescent dye is released from upstream and a laser light-sheet is used to obtain a cross-sectional view of the vortices over a range of stations.

4.2 Water Tunnel Facility

The water tunnel at the University of Ottawa is a closed-circuit, recirculating facility with a maximum capacity of 16 m^3 of water (Fig. 4.1). The test section is 4 m long with a

cross-section that is 50 cm wide and 74.7 cm high. It is enclosed on three sides by thick glass walls and is open on the top surface.

Flow is generated by an axial flow pump. A controller for the motor driving the pump permits the flow rate to be varied so that a range of test section velocities, from approximately 5 to 30 cm/s for a water depth of 70 cm, can be obtained. The flow in the settling chamber is accelerated through a three-sided contraction with an area-reduction ratio of 9.6. Perforated plates and a plastic screen installed in the settling chamber promote flow uniformity and dampen large-scale vortices. The tests were conducted in the first sector of the test section immediately following the contraction exit (Fig. 4.1).

The water tunnel features a model positioning system that is clamped to the structural frame above the test section (Fig. 4.2). The positioning system provides precise rotation in the pitch and yaw axes and is designed to rotate the model so that its centre of rotation remains in the same place as the model is pitched and/or yawed. A model is mounted on a C-strut (a sector of a circle with its centre corresponding to the model centre of rotation) which provides pitch control via the manual adjustment of a crank. The pitch range of the strut is limited to 40 deg. The C-strut was supported on a plate that allowed ± 30 deg of yaw and could be rotated independently of the mounting supports for the system. For these experiments, the plate remained locked at zero yaw because yaw cases were not studied.

The water tunnel is described in detail in Dunn (1997).

4.2.1 Test Section Velocity

A brief laser Doppler velocimetry (LDV) survey was conducted to determine the velocity and quality of the freestream flow in the test section. The survey was motivated by the addition of screens in the settling chamber some time after a calibration of the centreline velocity with the pump rotational speed was completed and reported in Dunn (1997). As a consequence of the modification, it was suspected that the velocity calibration of Fig. 4.3 was no longer valid.

Longitudinal and lateral components of the freestream velocity were measured with a two-dimensional LDV system. The system comprised of an Optikon 5-Watt argon-ion laser and related Dantec LDV components and software. The laser supplied a cyan beam composed of green light (448 nm wavelength) and blue light (514.5 nm wavelength). The laser was operated at 500 mW for the velocity survey. The laser head, containing the transmitting/receiving optics, was fitted with a 300-mm focal-length lens so that the measurement volume could be placed at the centre of the test section width. The laser head was mounted on a traversing carriage that allowed the head to be moved in the longitudinal and vertical directions along one side of the test section.

The freestream velocity was measured for a pump rotational speed of 1800 rpm with the forebody model installed at $\alpha = 50$ deg and the water level at 70 cm in the test section. The velocity was measured at 50-mm intervals from the vertical centreline of the test section to a height of 300 mm. This vertical sweep was conducted 100 mm upstream of the forebody model, in-line with the plane of symmetry of the model. The extent of the vertical sweep covered most of the projected length of the forebody. A second vertical sweep was performed at the same longitudinal station, laterally-offset by 40 mm. Measurements at each point were taken over a period of 120 seconds at a sample rate that was found to vary from point to point. Based on second-order polynomials fitted to the total mean velocities of each sweep, the average freestream velocity was estimated to be 18.9 cm/s (the uncertainty associated with the velocity measurements is unknown). The longitudinal and lateral turbulence intensity were roughly 2% and 4%, respectively; the lateral turbulence intensity is suspect because the average of the lateral velocity component was lower than the resolution of the LDV system which likely accounts for the significant variation. An average freestream velocity of 3.5 cm/s was extrapolated for a pump rotational speed of 540 rpm by applying the difference of the average freestream velocity and Dunn's centreline velocity for 1800 rpm to the centreline velocity for 540 rpm. The premise for the extrapolation is the assumption that the effect of the additional screens in the settling chamber causes a reduction of total pressure at the entrance to the contraction, and leads to a downward translation of the calibration curve in Fig. 4.3.

4.3 Forebody Model and Nozzle Blowing System

Since the results of the wind-tunnel tests demonstrated that the presence of the delta-wing afterbody had no bearing on the occurrence of the reversal phenomenon, only the forebody shell and tips of the model (Section 3.2.2) were necessary for the water tunnel tests. The forebody blowing system was not required for these experiments because it was decided that only steady blowing cases would be investigated. Moreover, the blowing assembly was not designed for operation in water.

The forebody model was adapted to the model positioning system with the centre of rotation located at a point one-third of the forebody length from the apex, at a height of 32.8 cm above the floor of the test section. The model was attached upside-down to the C-strut (i.e., the nozzles faced the floor of the test section) with an adaptor consisting of a standoff, sting, and an attachment flange (Fig. 4.2). The attachment flange was a replica of the forebody attachment flange on the fuselage of the delta-wing model. The adaptor assembly was designed to place the designated centre of rotation of the forebody at the centre of the C-strut while allowing a range of angle of attack from 30 to 60 degs to be within the pitching limits of the strut. On a bench setup, care was exercised to ensure the correct angular alignment of the flange/sting combination with the standoff, i.e., using a digital inclinometer, the plane of the nozzles (represented by a pair of alignment pins placed in the web of the attachment flange) was made to be perpendicular to the plane of the standoff. In turn, the alignment of the forebody model became dependent on the attachment of the adaptor assembly to the C-strut. Since the plate thickness of the standoff and strut were the same, the alignment became a procedure of ensuring that the surfaces of these two parts were flush. The angular scale inscribed on the C-strut was calibrated for this installation with a digital inclinometer mounted on the sting. The uncertainty of the indicated angles was estimated to be ± 0.2 deg.

The nozzle blowing system was simplified for the water-tunnel experiment. Dyed water was fed from one-litre pressurized cylindrical canisters (one canister per nozzle) to a flow meter which regulated the volume flow rate. The pressurized water was then directed to the forebody model via Tygon tubing through the free water surface and through access

holes in the web of the attachment flange. Inside the forebody cavity, each Tygon tube was attached to a brass tube that was inserted in a nozzle passage in the tip. Only steady blowing through one nozzle at a time was possible with this system.

The flow meter was comprised of a tapered tube and a needle valve, which provided excellent control of the volume flow rate. A simple apparatus was assembled to calibrate the flow meter for water: from an elevated reservoir, water was fed under the action of gravity to the flow meter. The needle valve was adjusted to align the centre of the spherical float with a desired scale mark on the tube, and the outflow was directed to a glass cylinder graduated for volume. The flow rate was determined by timing the accumulation of a certain volume of water. The tube delivering water from the flow meter was moved with the rising surface of the water column in the graduated cylinder to prevent a build up of back pressure.

The uncertainty in the blowing momentum coefficient was estimated to be 0.16×10^{-3} for $C_\mu = 2.9 \times 10^{-3}$ and $V_\infty = 18.9$ cm/s. The uncertainty is entirely based on the bias error in the nozzle flow rate and does not include an estimate of the precision error in the flow rate nor the uncertainty in the freestream velocity.

4.4 Flow Visualization Apparatus

4.4.1 Dye Injection

The flow visualization of the forebody vortices by dye injection was performed in two ways: dye was injected externally into the freestream, becoming entrained in the outer layer of a forebody vortex and forming a spiral streakline; or dye was injected through a blowing nozzle, becoming entrained in the core of a forebody vortex.

Two colours of dye were used. Methyl violet produced a deep purple streak while Congo red made a somewhat light red streak. The dyes were mixed in water and supplied from pressurized canisters*. The needle valve of a rotameter inserted into the delivery lines, regulated the flow of the dyed water. For external injection, the dyed water was introduced in-line with the freestream through a thin-walled, stainless-steel injection tube with an internal diameter of 0.39 mm (Fig. 4.4). The flow rate was adjusted so that the dyed water left the injection tube as a thin streak.

To reduce the intrusiveness of the injection tube and its thick support stem, the dye injection tube was oriented horizontally as shown in the upstream end view in Fig. 4.4. An injection tube was typically located 5 to 7.5 cm from the surface of the model, introducing the dye above the forebody tip and off the model centreline. In the case of two dye streaks issuing simultaneously, the injection tubes were vertically-offset so that the streaks could be readily identified on the video record.

The dye streaks were illuminated with two halogen lamps, one located below the test section floor and the other above the free surface of the water. Each lamp was aligned to provide the optimum illumination of the leeside surface and the port side of the model.

4.4.2 Laser Light-Sheet

The fibre-optic cable that conveyed the laser beam was connected to a Powell cylindrical lens that focused the light into a sheet. The lens was held in a bracket that was attached to a manually-operated, two-dimensional traversing mechanism (in the longitudinal and lateral axes of the test section) clamped to the frame beneath the glass floor of the test section (Fig. 4.5). The lens bracket could be pitched so that the incidence angle of the light-sheet relative to the glass floor and to the model could be adjusted.

*A non-Newtonian fluid used by Dunn (1997) was also available. With properties similar to water and a shear-thickening property that makes this fluid become more viscous and less diffusive in response to high shear, this fluid was expected to be useful for external dye injection at high Reynolds number. After some trials, however, the non-Newtonian fluid was abandoned because, as was found with water, a steady dye streak could not be maintained at a high freestream velocity. The fluid also frequently plugged the dye injection tube.

The traversing range of the light-sheet along the longitudinal axis of the model was restricted by the placement of the traversing mechanism and the effect of refraction. Nevertheless, a satisfactory range of $0.65 \leq x_s/l \leq 0.15$, where l is the length of the forebody model and x_s is a station along the length relative to the apex, was achieved with the light-sheet inclined 5 deg to the perpendicular of the model longitudinal axis. The 5-deg offset was considered to be acceptable because, since the forebody vortices would not be parallel to the model axis, a light-sheet slightly inclined in the proper direction would closely illuminate the true cross-section. To capture an unobstructed view of the cross-section of the forebody vortices from beneath the test section flow (see the *camera view* in Fig. 4.5), it was necessary to set the forebody model at a 50-deg angle of attack[†].

Particle seeding was provided by a fluorescent sodium salt dye which appeared as a translucent yellow-green when dissolved in water. The dye was introduced in-line with the freestream using an injector tube having a larger internal diameter than the tube described in the previous section. The seeding fluid was delivered at a flow rate that was adequate to create a diffuse turbulent plume wide enough to supply seeding particles to both sides of the model. Recognizing that a turbulent plume may affect the baseline orientation of the forebody vortices, the dye was not introduced in the vicinity of the apex. To ensure the dye was fully entrained by the vortices before being illuminated by the light-sheet, it was necessary to vertically-place the dye injection tube below the intersection of the light-sheet and the longitudinal axis of the model. Considerable effort was often required to laterally place the injection tube so that the dye stream was used productively on both sides of the model, especially with regard the illumination of the detached vortex[‡].

4.5 Test Procedures

Three forebody tips were selected for testing with dye streak and laser light-sheet visualization. Tips 2-2, 1-7, and 3-13 (see Table 3.4) were selected because these tips are

[†]For an angle of attack of 45 deg, the frame of the test section interfered with the view.

[‡]In hindsight, two injection tubes for particle seeding would have been useful in the same arrangement described for external dye injection.

representatives from each family of longitudinal locations and all have nozzles located on the 135-deg meridian (Fig. 3.4), an azimuthal position that has been used in many tests reported in the literature.

Care was taken to prevent corrosion of the model and its support assembly. Upon removal from the water after the conclusion of a test session, the forebody shell and tip were thoroughly dried. As an added measure, a protective coating of oil was applied to the unpainted surfaces of the steel tips.

The water level in the test section was maintained at 70 cm throughout the visualization tests.

4.5.1 Dye Streak Visualization

For the dye streak flow visualizations, the model was set to an angle of attack of 45 deg with no sideslip. Digital video recordings were made with each tip for cases of baseline (no blowing), and steady port and starboard nozzle blowing. A video record was made from three different views: a port side view; a view of the leeward surface, taken through the glass floor of the test section as a reflection from an inclined mirror; and a longitudinal view, taken towards the forebody tip through the floor of the test section[§]. These views are identified in Fig. 4.4. Each video record was made with voice recordings of the nozzle flow rates. Visualizations were conducted at two freestream velocities: for best results with external dye injection — a smooth and continuous, dyed-water streak — the freestream velocity was approximately 3.5 cm/s, corresponding to a pump rotational speed of 540 rpm; for dyed nozzle flux, the velocity was 18.9 cm/s for a pump speed of 1800 rpm. For recording of the leeward surface, only dyed nozzle visualization could be performed because the necessity of a mirror restricted the type of visualization that could be used[¶].

[§]The longitudinal view was slightly distorted due to refraction.

[¶]The plate supporting the mirror blocked the lighting from underneath as the mirror was moved down the length of the model. For this reason, the view of the mirror was limited to the leeward surface in the region of the tip.

For steady nozzle blowing, a continuous recording was made of the forebody vortices as the nozzle flow rate was increased from zero, reached some arbitrary maximum beyond the reversal threshold, and then was reduced to zero. This method served as a useful hysteresis check, i.e., to observe whether the forebody vortices would switch back to their original orientation at the threshold as the nozzle flow rate was decreasing.

4.5.2 Laser Light-Sheet Visualization

The laser light-sheet visualizations were performed with a freestream velocity of 18.9 cm/s. The model was set to 50 deg angle of attack (as described in Section 4.4.2) with no sideslip. Good illumination was achieved with the laser power set to 700 mW.

The cross-sectional illumination of the leeward flow field was captured from a longitudinal view (the *camera view* in Fig. 4.5) with a digital video camera. Video recordings were made of cross-sections at three stations along the longitudinal axis of the model, namely $x_s/l = 0.17$, 0.40, and 0.62. Recordings included cases of baseline, and steady blowing through the port and starboard nozzles. For the steady blowing cases, similar to the dye streak visualization, the recordings documented the response of the forebody vortices as the nozzle flow rate increased from zero, reached an arbitrary maximum above the reversal threshold, and then was reduced to zero. Video records of laser light-sheet illumination were made with voice recordings of the nozzle volume flow rates.

Five additional tips were tested using laser light-sheet illumination. The baseline flow field, and the response to port and starboard blowing were recorded at $x_s/l = 0.40$ for Tips 6-1, 9-3, 11, 12, and 14.

Figure 4.1. Schematic of the recirculating water tunnel at the University of Ottawa. Adapted from Dunn (1997). Original drawing was used with permission.

Figure 4.2. Model positioning system used in the test section of the water tunnel. Adapted from Dunn (1997).

Figure 4.3. Centreline velocity and average freestream velocity against pump rotational speed for a 70-cm water level. Adapted from Dunn (1997).

Figure 4.4. Setup for external dye injection. The view is of the port side of the forebody model. The carriage supporting the injection tubes has been omitted. *Adapted from a drawing in Dunn (1997).*

Figure 4.5. Setup for laser light-sheet illumination. The view is of the port side of the forebody model. The carriage supporting the injection tube has been omitted. *Adapted from a drawing in Dunn (1997).*

Chapter 5

Results and Discussion

This chapter presents and discusses the findings of the wind tunnel experiment described in Chapter 3, and of the water tunnel experiment described in Chapter 4. The wind tunnel experiment was designed with three objectives in mind: to determine whether the dynamic-blowing results from a previous investigation (Lee, 1995) with a tangent-ogive cylinder model could also be obtained for a realistic aircraft configuration; to investigate the influence of axial and azimuthal location of the forward-facing nozzles on the forebody; and to ascertain whether Reynolds numbers has a bearing on the effectiveness of the dynamic manipulation technique.

The results of the wind tunnel investigation demonstrate that the technique of dynamic manipulation of forebody vortices can be applied to an aircraft configuration, i.e., a configuration that includes a delta-wing and a vertical tail. The findings, however, are accompanied by an unexpected reversal of vortex orientation when the blowing momentum coefficient reaches a certain threshold value; in addition, maximum magnitudes of yawing moment and side force with blowing were not significantly greater than the no-blowing or baseline magnitudes. Not surprisingly, there are some cross-coupling effects in rolling moment, pitching moment and normal force. These are not, however, unduly large and may, in some cases, have favourable implications for flight mechanics. The effectiveness of forward-blowing in non-zero sideslip was very good with nozzles located very near the apex. Placing the nozzles farther back was found to raise the blowing-momentum threshold; a preferred azimuthal position for the nozzles was also identified. Unfortunately, a serious resonance condition placed a limit on freestream velocity and precluded

the study of Reynolds number effects. The flow visualization investigation in the water tunnel verified the occurrence of the vortex reversal phenomenon observed in the results from the wind tunnel; also, based on the water-tunnel findings, plausible explanations are suggested for the cause of the vortex reversal and the lack of capacity of the forebody to produce gains in yawing moment and side force.

5.1 Axis System, Nomenclature, and Conventions

The axis system and sign conventions observed in this investigation for the force and moment measurements are shown in Fig. 3.2. The origin of the axis system coincides with moment reference of the model. The axis system differs from the right-handed body-axis system, commonly used for aircraft stability analysis, in that the z -axis is oriented upward instead of downwards*. The forces are positive when acting to starboard and upwards†; except for yaw, moments are positive when the direction of rotation is clockwise about the corresponding axis when viewed from the origin in the positive direction of the axis. Yawing moment (N) is positive clockwise when looking in the negative z -direction. The sideslip angle (β) is considered positive when the model is yawed to port. The origin of the axis system coincides with the moment reference for the model which is located 652 mm aft of the nose apex, 4.7 mm behind the balance resolving centre.

Non-zero sideslip and angle of attack (α) orientations can be obtained by using appropriate combinations of roll angle (ϕ) and turntable angle (σ) in the total incidence plane (Beyers et al., 1984; Lamont and Kennaugh, 1991); see Figs. 3.1 and E.1. With this approach, a positive roll angle will synthesize a yawing of the model to the port side, hence a positive sideslip. The coordinate systems (α, β) and (σ, ϕ) coincide, i.e., $\sigma = \alpha$, when $\phi = 0$ deg. Otherwise geometric transformations relate the two coordinate systems (Appendix E). Note that for cases of zero roll angle, σ and the term *angle of attack* are used synonymously. For the most part, the orientation of the model will be described in terms

*Here *upwards* and *downwards* refer to a wings-level orientation of the model. In the wind-tunnel installation (Fig. 3.1), the z -axis is nominally in the horizontal plane.

†The axial force (X) is shown in Fig. 3.2 for completeness. This component was not measured in these experiments.

of σ and ϕ .

Time-Average Forces and Moments. As described in Section 1.2 the measured quantities of primary interest are time-average aerodynamic force and moment coefficients. A time-average quantity is a simple arithmetic average of the ensemble-averaged record, which is a time-series of measurements (i.e., sample points) over the period of a blowing cycle, each sample point averaged over many blowing cycles. All aerodynamic force and moment coefficients are based on freestream dynamic pressure (q_∞) and wing reference area (S_w). The reference length for pitching moment coefficient is the mean geometric chord (c); the wing span (b) is the reference length for the yawing and rolling moment coefficients. At a given sample point within the period of a blowing cycle, the *instantaneous* yawing moment coefficient, for example, is determined by

$$C_{n_i} = \frac{N_i}{q_\infty S_w b}$$

where N_i is the measured yawing moment at a certain time in the ensemble-averaged record. In turn, the time-average yawing moment coefficient is

$$\bar{C}_n = \frac{1}{s} \sum_{i=1}^s C_{n_i}$$

where s is the number of sample points in the ensemble-averaged record. Time-average side force is represented by $\bar{C}_Y$; normal force, $\bar{C}_Z$; pitching moment, $\bar{C}_m$; and time-average rolling moment, $\bar{C}_\ell$. An assessment of the uncertainty in the force and moment coefficients is presented in Appendix C and the results are listed in Table 5.1.

V_∞ (m/s)	Uncertainty				
	U_{C_n}	U_{C_Y}	U_{C_m}	U_{C_Z}	U_{C_ℓ}
18.3	0.0061	0.14	0.0094	0.16	0.010
36.6	0.0015	0.035	0.0023	0.039	0.0026
54.9	0.00068	0.016	0.0010	0.017	0.0011

Table 5.1. The measurement uncertainty in the aerodynamic force and moment coefficients for three freestream velocities.

The majority of the results presented in this chapter for side force coefficient exhibit a relatively high degree of uncertainty. Because a moment balance was employed in this investigation, forces are derived from the difference of moments measured at two flexures separated by a certain distance. Assuming the centre of pressure for an aerodynamic force lies between the flexures, the uncertainty in the force measurement is typically the sum of the uncertainty in the moment measurements. This is the basis for the uncertainties cited for C_Y and C_Z in Table 5.1. At high angles of attack, however, the side force centre of pressure lies on the forebody, a considerable distance ahead of the balance which is located in the fuselage portion of the model (Fig. 3.3). As a result, the flexures sense yawing moments of large and nearly equal magnitudes, the difference of which lead to a significant error in the side force measurement. Thus, in Table 5.1, the uncertainty in C_Y is underestimated; this is not the case for the uncertainty in C_Z , however, because the normal force centre of pressure remains between the flexures sensing pitching moment. In Lee (1995), the balance was located inside the forebody model, near the centre of pressure for side force, and yielded excellent side force results.

Mean Centre of Pressure. For a given C_n and a corresponding side force coefficient (C_Y) the centre of pressure of the side force is

$$X_{cp,Y} = b \frac{C_n}{C_Y}.$$

Since C_n and C_Y are instantaneous quantities, the time-average centre of pressure is

$$\bar{X}_{cp,Y} = \frac{b}{s} \sum_{i=1}^s \left(\frac{C_n}{C_Y} \right)_i. \quad (5.1)$$

The right-hand side of Eqn. 5.1, however, does not reflect the time-average quantities $\bar{C}_n$ and $\bar{C}_Y$. In the context of the present forebody vortex manipulation scheme, a *mean* centre of pressure for time-average side force and normal force is proposed:

$$\begin{aligned} \tilde{X}_{cp,Y} &= b \frac{\bar{C}_n}{\bar{C}_Y}, \\ \tilde{X}_{cp,Z} &= c \frac{\bar{C}_m}{\bar{C}_Z}. \end{aligned} \quad (5.2)$$

These equations are considered appropriate for several reasons. First, Eqn. 5.2 incorporates time-average force and moment. Secondly, as the duty-cycle parameter $\tau/T \rightarrow 50\%$, $\tilde{X}_{cp,Y}$ will become indeterminate as $\bar{C}_Y \rightarrow 0$ and $\bar{C}_n \rightarrow 0$; $\bar{X}_{cp,Y}$ may not necessarily yield the same result as a simple arithmetic average of instantaneous centre of pressure. Lastly, the sign of $\tilde{X}_{cp}$, which locates a time-average force fore or aft of the moment reference, is consistent with the orientations of the time-average force and moment; $\bar{X}_{cp}$, on the other hand, may not be reflective of the orientation.

Baseline, Steady and Dynamic Blowing. These terms are used frequently to denote blowing modes that are described in Section 3.8. For consistency with the dynamic blowing mode, the notation for time-average aerodynamic forces and moments and the notation for mean centre of pressure is also used for describing the results for the baseline and steady blowing modes.

Coefficient of Blowing Momentum. The coefficient of blowing momentum (C_μ) is considered positive when air is applied through the starboard nozzle, and negative through the port nozzle. Nozzle blowing was always applied asymmetrically, i.e., blowing was applied either to the starboard or port nozzle, not simultaneously. The uncertainty in C_μ is addressed in Appendix B.

Delta-Wing Model. Unless otherwise noted, all balance results presented in this chapter are for the full aircraft-like configuration, comprising the forebody, delta-wing and vertical tail.

5.2 Reynolds Number Limit

In Section 3.6 a serious vibration condition was described in which the delta-wing model entered into a diverging lateral oscillation in its yaw plane at the resonance frequency of the model-support system. The suspected source of the excitation was described as a coupling between forebody flow behaviour and the motion of the model. An emergency blowing system put in place to detect and suppress the oscillation was largely successful. But the oscillation was encountered frequently. A pattern in the combination of angle of

attack and freestream velocity could not be detected, although the tendency of the model to vibrate was reduced at lower freestream velocities. Interestingly, these circumstances were not encountered in the previous investigation with the tangent-ogive cylinder model (Lee, 1995), even though the model-support system was similar to that of the current work.

The possibility of encountering the vibration was a serious concern during the experiment in the wind tunnel. Consequently, the majority of the test cases were limited to freestream velocities of 18.3 or 36.6 m/s, which were found to be vibration-free. A limited amount of data was successfully acquired at the freestream velocity of 54.9 m/s. The constraint prevented a comprehensive study of behaviour at Reynolds numbers above 2.0×10^5 (based on the base diameter of the forebody and approximately corresponding to 36.6 m/s), meaning that subcritical flow conditions (i.e., laminar-separation conditions) prevailed over the entire forebody. In Fig. 5.1, surface flow visualization on the forebody for $Re_D = 2.0 \times 10^5$ shows that the primary separation line of the starboard vortex lies approximately on the 90-deg meridian, signifying the separation of a laminar boundary layer.

5.3 Baseline Results

Baseline results (i.e., no blowing) from angle-of-attack and roll-angle surveys at two Reynolds numbers are presented in this section. The surveys were conducted with the delta-wing model featuring two forebody tips with nozzles for the angle-of-attack survey and only one tip for the roll survey. Reliable baseline data for the forebody tip without nozzles could not be acquired because, with this tip, the model was highly susceptible to the serious vibration condition that was briefly described in the previous section.

5.3.1 Effect of Angle of Attack

The results of the baseline angle-of-attack surveys are shown in Fig. 5.2 for Tip 6-1 ($x_n/D = 0.095$, $\theta = 120$ deg) and Tip 2-2 ($x_n/D = 0.095$, $\theta = 135$ deg). Measurements

were taken at freestream velocities of 18.3 and 36.6 m/s for each tip, corresponding to $Re_D = 9.5 \times 10^4$ and 1.9×10^5 respectively. The roll angle was zero for these surveys.

Yawing moment and side force. The behaviour of $\bar{C}_Y$ and $\bar{C}_n$ with σ are representative of the variations expected for an axisymmetric forebody. Yawing moment and side force are very small for $15 \leq \sigma \leq 25$ deg indicating a symmetrical forebody vortex orientation, although vortex-free flow over the forebody may exist at the lower angles of the interval. For the lower Re_D case, however, small non-zero $\bar{C}_Y$ is observed for both tips. Since the corresponding yawing moment is approximately zero, $\bar{C}_Y$ must be acting at the moment reference of the model, suggesting that the side force is not due to the forebody or the rudder. On the other hand, the non-zero $\bar{C}_Y$ may simply be experimental uncertainty ($U_{CY} = 0.14$ for $V_\infty = 18.3$ m/s) because the side force is expected to be small for zero sideslip at low dynamic pressure.

When the angle of attack exceeds 25 deg, $\bar{C}_Y$ and $\bar{C}_n$ begin to increase, signifying the onset of asymmetric forebody vortices. The onset angle of attack for asymmetric vortices is in reasonable agreement with the correlation of Keener and Chapman (1974), which predicts the onset angle to be $2.1\delta_N = 26.9$ deg, where δ_N is the semi-apex angle of the forebody. Beyond the onset of asymmetry $\bar{C}_Y$ and $\bar{C}_n$ increase steadily with a positive sense which indicates that the port vortex (i.e., the first primary port vortex) is high off the leeward surface of the forebody and the starboard vortex is low. Maximum $\bar{C}_Y$ is generally reached by $\sigma = 40$ deg while maximum $\bar{C}_n$ is reached at the higher angle of attack of 45 deg. Yawing moment and side force begin to decrease as σ increases further beyond 45 deg. For $\sigma > 60$ deg, $\bar{C}_Y$ and $\bar{C}_n$ tend to level-off at small non-zero magnitudes, reflecting a condition of wake-like flow that is lacking of forebody vortex structure.

The reduction in $\bar{C}_Y$ and $\bar{C}_n$ for $\sigma > 45$ deg is believed to be largely the result of a diminishing influence of the starboard vortex to sustain the low-pressure region as the vortex gradually lifts away from the forebody with further increases in angle of attack. The effect would be reflected as a reduction in positive $\bar{C}_Y$. The decrease in positive $\bar{C}_Y$ may also be attributed to a low-pressure region emerging on the port side. In view of the potential for multiple vortices to appear, it is conceivable that the first primary port vortex

has detached from the forebody, allowing a second primary vortex to develop beneath it. The second primary port vortex would lie close to the forebody, forming a low-pressure region on that side. This notion was not pursued during the experiment.

For $35 \leq \sigma \leq 50$ deg significant decreases in $\bar{C}_Y$ and $\bar{C}_n$ occur as Reynolds number increases from 9.6×10^4 to 1.9×10^5 . Maximum $\bar{C}_Y$ and $\bar{C}_n$, for example, drop by a factor of about 1.5. Decreases in $\bar{C}_Y$ and $\bar{C}_n$ with increasing Reynolds number typically indicate the onset of critical flow conditions over the forebody. On the one hand, the emergence of critical flow conditions does not appear to be the case in view of Lamont (1982b), who defined boundaries that delineate the subcritical, critical, and supercritical flow regimes for a tangent-ogive cylinder in terms of Re_D and angle of attack. Because the subcritical/critical boundary for $\sigma = 45$ deg is $Re_D \approx 2.9 \times 10^5$ according to Lamont, subcritical flow conditions are expected to prevail over the forebody for both the present Reynolds numbers. On the other hand, the ratio of the nozzle diameter to the local radius of the forebody cross-section is relatively large, making the cutouts of the nozzles (Fig. 3.4) significant features, in the vicinity of the apex, that could raise the effective Reynolds number of the forebody flow.

The maximum magnitudes for $\bar{C}_Y$ and $\bar{C}_n$ for $Re_D = 9.6 \times 10^4$ are observed only for the no-blowing case. As will be seen later in Section 5.4.2 with the steady blowing cases, the maximum magnitudes $\bar{C}_Y$ and $\bar{C}_n$ do not even exceed 0.2 and 0.09, respectively, for Reynolds numbers of 2.0×10^5 and 2.9×10^5 . These observations have led to speculation that the presence of the nozzles may have some bearing on the baseline results for zero roll angle.

Pitching moment and normal force. It is well understood that a significant component of the lift force acting on a delta-wing is generated by the wing leading-edge vortices (so-called vortex lift), and that the breakdown locations of these vortices move forward with increasing angle of attack. Once the vortices break down over the wing, the contribution of vortex lift begins to wane and the slope of the lift-curve starts to decrease, becoming zero at the point of maximum lift when the breakdown locations have reached the apex of the delta-wing (i.e., without a forebody). For the present delta-wing model

without a forebody, the breakdown locations of the wing leading-edge vortices occur *symmetrically* over the wing at about 60% of the wing centreline chord at $\sigma = 30$ deg (Hanff and Huang, 1991). By $\sigma \approx 40$ deg, the maximum lift of the delta-wing has been achieved[‡].

In Fig. 5.2(d) the variation of normal force coefficient ($\bar{C}_Z$) with angle of attack, for $15 \leq \sigma < 30$ deg, is as expected for a delta-wing. After $\sigma = 30$ deg, however, the normal force experiences a decrease (most noticeably for Tip 6-1) and then undergoes a recovery at 35 deg. The fact that the decrease in $\bar{C}_Z$ coincides with maximum rolling moment coefficient ($\bar{C}_\ell$) at $\sigma = 35$ deg suggests that it could be related to the interaction taking place between the forebody and wing leading-edge vortex systems (described in Section 2.3.1). A by-product of this interaction would be the asymmetry that develops in the wing leading-edge vortex system — the breakdown location of one vortex moves forward (towards the apex) and the breakdown location of the other vortex moves aft (towards the trailing edge) — which could be responsible for the deficit in total normal force. At $\sigma = 40$ deg, $\bar{C}_Z$ reaches its maximum magnitude; the disposition of the breakdown locations of the wing leading-edge vortices at this angle of attack, however, is uncertain.

The variation of pitching moment coefficient ($\bar{C}_m$) is small with angle of attack, resembling neutral longitudinal static stability up to $\sigma = 30$ deg. Beyond 30 deg, $\bar{C}_m$ increases and typically reaches a maximum at 40 or 45 deg before starting to decrease. The increase at $\sigma = 30$ deg is abrupt and almost coincides with the onset of asymmetric forebody vortices. If in fact these occurrences are related, a normal force due to the forebody could be contributing positive increments to $\bar{C}_m$ owing to a long moment arm with respect to the moment reference centre. In general, the variation in the longitudinal static stability is considerable over the range of angle of attack shown.

Rolling moment. There is little response in rolling moment for $\sigma < 30$ deg. Beyond

[‡]Wentz and Kohlman (1971) found that the breakdown locations of the wing leading-edge vortices reach the apex of a 65-deg flat-plate delta-wing planform, with a sharp leading edge, at $\alpha \approx 36$ deg. Hanff and Huang (1991) have found that the breakdown location of the leading-edge vortices over the present 65-deg delta-wing model (without a forebody) agrees well with the findings of Wentz and Kohlman after a 4-deg reduction in the angle of attack of the present delta-wing model is taken into consideration. That is, the present delta-wing model must reach a higher angle of attack (i.e., 40 deg) than the equivalent flat-plate planform before the breakdown locations of the vortices reach the wing apex. The correction accounts for the presence of a leading-edge bevel of 10 deg on the upper surface of the wing.

$\sigma = 30$ deg, $\bar{C}_\ell$ increases in a positive sense to a maximum magnitude that is roughly half the magnitude of the corresponding yawing moment. A positive rolling moment indicates that the normal force acting on the port wing is greater than that on the starboard wing; the imbalance is presumably due to the asymmetrical breakdown of the wing leading-edge vortices described earlier, illustrating the cross-coupling effect of the interaction between the forebody and wing leading-edge vortex systems. The results in Fig. 5.2(e) show that positive $\bar{C}_\ell$ corresponds with positive $\bar{C}_n$, which signifies that the starboard forebody vortex lies close to the forebody surface and also in close proximity to the starboard wing leading-edge vortex farther downstream of the forebody base (Fig. 2.67). As described earlier the ensuing interaction between the vortices leads to a forward breakdown of the starboard wing vortex while the port leading-edge vortex bursts farther downstream, giving rise to a normal force that is greater on the port wing.

For $40 < \sigma < 50$, $\bar{C}_\ell$ decreases with angle of attack. Although the total normal force is also decreasing, it is presumed that the port wing continues to generate more normal force than the starboard in order to sustain a non-zero rolling moment. An interaction similar to that at lower σ , albeit weaker, is occurring within this range of angle of attack.

Mean centre of pressure. The results for the mean centres of pressure for side force ($\tilde{X}_{cp,Y}$) and normal force ($\tilde{X}_{cp,Z}$) are shown in Figs. 5.2(f) and 5.2(g) for the low and high Re_D cases respectively.

For vortex-free flow or symmetric vortices in the low Re_D case, the mean centre of pressure for side force is located in the vicinity of the moment reference because $\bar{C}_n$ is small. The mean centre of pressure moves toward to the fore of the model with the onset of asymmetry forebody vortices at $\sigma = 25$ deg and tends to lie just aft of the junction of the forebody and the delta-wing afterbody. The location is reasonably consistent up to $\sigma = 55$ deg, beyond which $\tilde{X}_{cp,Y}$ returns to the moment reference as $\bar{C}_n$ diminishes with the development of wake-like flow over the wing. For Tip 6-1 and $\sigma = 65$ deg, $\tilde{X}_{cp,Y}$ is seen to lie off the model beyond the apex of the forebody. The location of the centre of pressure is large because the side force coefficient approaches zero at this angle of attack. It is believed this unusual result bears little significance.

For the high Re_D case, the variation of $\tilde{X}_{cp,Y}$ with σ is erratic, reflecting the corresponding behaviour of $\bar{C}_n$ and $\bar{C}_Y$. In the presence of asymmetric vortices, the forward movement of $\tilde{X}_{cp,Y}$ with increasing σ is significantly greater than that at the lower Re_D . As wake-like flow develops over the wing, only $\tilde{X}_{cp,Y}$ for Tip 6-1 returns to the moment reference after $\sigma = 60$ deg.

Unlike $\tilde{X}_{cp,Y}$, the variation of $\tilde{X}_{cp,Z}$ with σ is negligible. As it happens, the moment reference for the model is the centre of pressure for the normal force.

Unsteadiness. The baseline results for Tips 6-1 and 2-2 in Fig. 5.2 are repeated separately in Figs. 5.3 and 5.4, respectively, together with the standard deviation associated with each data point. The standard deviation is a measure of the variation of an aerodynamic load and it generally appears to be small compared to the magnitude of the time-average load.

For Tip 6-1 (Fig. 5.3) the unsteadiness in the aerodynamic loads is generally higher for $Re_D = 9.8 \times 10^4$ over most of the range of angle of attack. For this Reynolds number there is no discernible pattern in the variation of unsteadiness with angle of attack. For $Re_D = 1.9 \times 10^5$ the unsteadiness in $\bar{C}_n$ and $\bar{C}_m$ appears to build toward a maximum at $\sigma = 40$ deg; otherwise, for normal force, side force, and rolling moment there is also no discernible pattern in the unsteadiness. Champigny (1986) found a similar increase in the unsteadiness of side force coefficient with angle of attack from measurements for a ogive nose ($l/D = 3.0$) mounted on a cylindrical afterbody. Based on LDV measurements within the leeward flow field, Champigny concluded that high levels of turbulence intensity within the vortices (as high as 35% in the low vortex, 25% in the high vortex) were a contributing factor to high frequency fluctuations in the side force measurements.

An examination of the power-spectral density of the unsteady aerodynamic loads revealed that the unsteadiness is dominated by a 178-Hz peak (± 4.5 Hz). The magnitude of the power-spectral density at this frequency is the highest for $Re_D = 9.8 \times 10^4$. Except for the possibility of being a harmonic of the natural frequency of the model/sting/support assembly (i.e., 30 Hz), the origin of the frequency peak is unknown.

Similar results were found for Tip 2-2 (Fig. 5.4).

5.3.2 Effect of Sideslip

In Fig. 5.5 the baseline results for simulated sideslip are presented for Tip 6-1 at two Reynolds numbers, $Re_D = 9.8 \times 10^4$ and 2.0×10^5 . Sideslip was simulated by rolling the model up to $\phi = \pm 20$ deg in increments of 10 deg while holding σ constant at 45 deg. See the caption of Fig. 5.5 for the relationship between α, β and σ, ϕ . The standard deviation associated with each data point is also shown to indicate the relative level of unsteadiness in the force and moment coefficients. Note that the results for all force and moment coefficients at $\phi = 0$ deg compare favourably with corresponding results from the angle-of-attack survey (Fig. 5.3).

Yawing moment and side force. The results for $\bar{C}_n$ and $\bar{C}_Y$ for $Re_D = 2.0 \times 10^5$ appear to follow the sideslip model described in Section 2.1.7. The slopes $\partial \bar{C}_n / \partial \phi$ and $\partial \bar{C}_Y / \partial \phi$ are both negative so the model is directionally unstable. That is, $\bar{C}_n$ and $\bar{C}_Y$ are destabilizing because they tend to yaw the aircraft so as to increase sideslip. This trait may be desirable for a fighter aircraft because its agility will be enhanced, provided that overall yaw control can be maintained, perhaps by use of control augmentation in the flight control system.

The results for $Re_D = 9.8 \times 10^4$ clearly have a different behaviour. The apparent departure in $\bar{C}_n$ and $\bar{C}_Y$ at $\phi = 0$ deg does not resemble the sudden change of forebody vortex orientation commonly observed in the results of a roll survey. At non-zero roll angles, the magnitudes of $\bar{C}_n$ for both sets of results match very well; for $\bar{C}_Y$, there is reasonable agreement. The standard deviations are, for the most part, roughly equal for both Reynolds numbers at all non-zero roll angles. The unsteadiness for $\bar{C}_n$ and $\bar{C}_Y$ is exceptionally large only at $\phi = 0$ deg for $Re_D = 9.8 \times 10^4$. Earlier it was suggested that at zero roll angle the cutouts of the nozzles, as significant features in the vicinity of the apex, could be an important factor contributing to the difference in $\bar{C}_n$ and $\bar{C}_Y$ at two subcritical Reynolds numbers. The fact that the magnitudes of $\bar{C}_n$ and $\bar{C}_Y$ appear to be somewhat independent of Reynolds numbers at non-zero roll angles also suggests that the nozzle cutouts have a diminished role in affecting the leeward flow field under sideslipping conditions.

Pitching moment and normal force. The fact that the variation of time-average pitching moment with roll angle corresponds with the movement of the mean centre of pressure for the normal force (Fig. 5.5(g)), and that the variation in total normal force is small, suggests that a normal-force contribution developed by the forebody is responsible for the variation of $\bar{C}_m$. In keeping with the sideslip model, it is suggested that as the aircraft model is rotated in sideslip the forebody vortices have a more intense effect on the upper-surface static pressure of the forebody. For example, consider the vortex orientation for $\phi = 0$ deg as the datum case; for positive $\bar{C}_n$, the starboard vortex is low on the leeward surface of the forebody and the port vortex is high. As the model is rolled in the positive direction — toward the starboard side — a region of static pressure lower than the datum, and possibly existing underneath the shear layer feeding the starboard vortex, begins to have an increasing influence on the upper surface of the forebody, giving rise to a positive increment in the normal force due to the forebody and thus a positive increment in $\bar{C}_m$. The effect is the opposite for $\phi < 0$ deg: a region of static pressure higher than the datum instead influences the upper surface of the forebody and causes a decrease in the forebody normal force and $\bar{C}_m$. Changes in the normal force due to the forebody are not evident in the results for $\bar{C}_Z$; the variation of the forebody contribution may be obscured by the redistribution of normal force developed by the wing in a sideslip condition (as discussed below).

Rolling moment. The results for $\bar{C}_\ell$ show that $\partial \bar{C}_\ell / \partial \phi < 0$. This appears to indicate that an effect of cross-coupling with the normal force is superimposed on the interaction between the forebody and wing leading-edge vortex systems. For positive σ and $\phi < 0$ deg the port wing leads into the wind and generates additional normal force while the lagging starboard wing experiences a decrease in normal force. A positive increase in $\bar{C}_\ell$ ensues; conversely for $\phi > 0$ deg, $\bar{C}_\ell$ decreases. The rolling moment has reasonable symmetry about the non-zero magnitude for $\bar{C}_\ell$ at $\phi = 0$ deg.

5.4 Steady Blowing Results

Steady blowing is a mode of operation that does not alternate blowing between the nozzles; instead, one valve remains fully open while the other valve is closed. It is important to establish the effect of blowing momentum under steady blowing conditions because the technique of dynamic manipulation relies on realizing the highest possible magnitudes of side force and yawing moment with minimum blowing momentum. Equally important to understand is the variation of side force and yawing moment with angle of attack under steady blowing conditions. For the dynamic manipulation technique to be successful, steady blowing with minimum C_μ from either nozzle must give rise to symmetrical variations of side force and yawing moment (i.e., the same magnitudes and opposite signs for blowing from the port or the starboard nozzle) that suppress the asymmetric baseline response.

In this section results are presented to illustrate the effect of blowing momentum, angle of attack and sideslip under steady blowing conditions. Typical sets of results are shown for one forebody tip.

5.4.1 Effect of Blowing Momentum

The effects of C_μ on all time-average forces and moments are depicted in Fig. 5.6. The results are for Tip 6-1 with $\sigma = 45^\circ$ and $Re_D \simeq 2.0 \times 10^5$. Note that the magnitude of all force and moment coefficients for the no-blowing cases ($C_\mu = 0$) are in good agreement with the corresponding baseline results in Fig. 5.2 (i.e., Tip 6-1, $Re_D = 1.9 \times 10^5$).

Yawing moment and side force. The response of $\bar{C}_n$ and $\bar{C}_Y$ to C_μ are comparable. In spite of the similarity, $\bar{C}_Y$ appears to be more affected by experimental uncertainty. The contrast in $\bar{C}_n$ and $\bar{C}_Y$ is further demonstrated in Fig. 5.7 where the results for two additional freestream velocities are superimposed on those appearing in Fig. 5.6. In Table 5.1, the uncertainties associated with C_n and C_Y are listed for three freestream velocities. Clearly, the uncertainty in the aerodynamic coefficients decreases with increasing dynamic pressure. However, the uncertainty in the side force coefficient is significant and

could be responsible for the relatively large variation in the results for $\bar{C}_Y$ seen in Fig. 5.7.

In the yawing moment response in Fig. 5.6, it is observed that, as negative blowing momentum (i.e., blowing through the port nozzle) increases from zero, $\bar{C}_n$ remains reasonably constant at the baseline magnitude. Positive $\bar{C}_n$ indicates that the port vortex is situated farther above the forebody than the starboard vortex. Apparently for low C_μ , port blowing does not affect the baseline orientation of the forebody vortices. At $C_\mu \approx -0.0013$, $\bar{C}_n$ begins to decrease rapidly until, at $C_\mu \approx -0.0025$, the yawing moment has changed sign and stabilized at -0.095, which is nearly the same magnitude as before the change in sign. A switch in the arrangement of the vortices, from one stable state to the other, appears to have occurred. The reversal of vortex orientation is also observed for positive C_μ , i.e., the sign of $\bar{C}_n$ changes from negative to positive at approximately the same *reversal threshold*. But unlike blowing from the port nozzle, very little starboard blowing (as low as $C_\mu \approx 0.0004$) is required to switch the vortices away from the baseline orientation. A comparison of the results for the delta-wing and the cylindrical afterbodies presented in Fig 5.8, shows that the effect of C_μ was not changed by the presence of the wing. In general, the magnitudes of $\bar{C}_n$ and $\bar{C}_Y$ are approximately the same before and after the reversal for steady blowing from either the starboard or port nozzle. Moreover, these magnitudes are not significantly different from those of the baseline.

Surface and off-surface flow visualization confirm the switching of the vortex orientation after the magnitude of C_μ has surpassed a threshold value. Surface flow patterns developed by oil flow in the wind tunnel are illustrated in Fig. 5.9. The results are shown for Tip 6-1 with steady blowing through either the port or starboard nozzles, at blowing coefficients below and above the reversal threshold. The test conditions under which these patterns evolved were $Re_D = 2.0 \times 10^5$ and $\sigma = 45$ deg. The prominent features of these surface flow patterns are two secondary separation lines and a primary attachment line[§]. The secondary separation lines are more visible than the primary attachment line because of the accumulation of oil along the separation front. There are two indicators in these

[§]A primary attachment line is the locus of primary attachment points (point A_1 in Fig. 2.1) where the flow from a primary vortex reattaches on the leeward surface. A secondary separation line is the locus of secondary separation points (point S_2 in Fig. 2.1) along which flow emanating from the primary attachment line separates in front of a region of reverse flow.

flow patterns that help identify the orientation of the forebody vortices. The secondary separation lines are one indicator. These separation lines have different lengths: one typically spans the entire length of the forebody and the other disappears after a distance of about one-third the forebody length from the apex. Keener (1986) reported similar observations for a pointed, tangent-ogive forebody ($\delta_N = 16.3$ deg, $l/D = 3.5$) and ascribed the disappearance of a secondary separation line to the lifting-off of the corresponding primary vortex from the leeward surface[‡]. After the lift-off point, a single primary vortex is seen to lie on the leeward surface. The inclined oil streaks, appearing between the primary attachment line for this vortex and the remaining secondary separation line, are the second indicator; the inclined streaks represent the surface flow induced by the vortex and are indicative of its sense of rotation.

In Fig. 5.9(a), blowing is steady from the port nozzle with a blowing momentum coefficient that is below the reversal threshold seen in Fig. 5.6. The short secondary separation line on the port side and the inclination of the oil-streaks due to the vortex sitting on the leeward surface (signifying a clockwise rotation as viewed from the apex), describe a vortex arrangement that has a high port vortex and a low starboard vortex. This arrangement gives rise to positive side force and yawing moment that corroborate the results for $\bar{C}_Y$ and $\bar{C}_n$ at $C_\mu = -1.2 \times 10^{-3}$ in Fig. 5.6. In Fig. 5.9(b), port- C_μ exceeds the reversal threshold. A switch in the vortex orientation is evident: the arrangement of the primary attachment line and secondary separations lines has reversed, and the inclination of the oil-streaks now indicate that the port vortex lies on the leeward surface. Negative $\bar{C}_n$ and $\bar{C}_Y$ are expected with this orientation, which agrees again with the results in Fig. 5.6. In Fig. 5.9(c) and (d), the same behaviour is observed for steady blowing from the starboard nozzle (i.e., positive C_μ) with the port vortex closest to the forebody for C_μ below the reversal threshold and vice versa for C_μ above the reversal threshold. Similar results were also obtained at the lower Re_D of 9.5×10^3 but are not shown here.

[‡]It is interesting to note that before this point of lift-off, video closeups in the vicinity of the apex, taken during the development of the surface oil streaks, revealed the appearance of a primary attachment line between the secondary separation lines, indicating the presence of two vortices in close proximity to the leeward surface. The orientation of the forebody vortices, however, could not be determined from the video or photographs.

Off-surface flow visualization in the water tunnel using dye injection provided further evidence of the reversal of the forebody vortices. In Fig. 5.10(a), dyed flux issues steadily from the port nozzle at a C_μ below the reversal threshold^{||}, forms a stagnation zone over the nozzle cutout, and then becomes entrained in the core of the port forebody vortex which is seen to lift off from the leeward surface. At the same time the external dye streak passing over the port side of the model, becomes entrained in the inviscid outer region of the vortex. The starboard vortex lies close to the leeward surface, its core illuminated by dye leaked from the port nozzle flow. The entrainment of the starboard external dye streak in the starboard vortex is not visible in the picture. This asymmetric vortex arrangement is expected to give rise to a positive side force. In Fig. 5.10(b), C_μ is above the threshold giving the port nozzle flow sufficient momentum to penetrate the boundary layer and develop as a jet, inclined to the freestream. The external dye streaks confirm that the positions of the vortices are reversed.

When the results for side force and yawing moment in Fig. 5.6 are compared with those from Alexan (1992) and Lee (1995), in Fig. 2.57, two differences emerge in the variation of these quantities with blowing momentum coefficient. Clearly, one such difference is the observed reversal when C_μ increases above a threshold in the present work. The other difference is the capacity, seen only in the earlier investigations, to generate side force and yawing moment greater than baseline magnitudes under steady blowing conditions. Regarding the latter, in the present work the side force and yawing moment generated under steady blowing conditions have approximately the same magnitudes as the baseline. Moreover, a small amount of blowing through one nozzle has the effect of causing the forebody vortices to switch from their baseline orientation; blowing with the same amount through the other nozzle, however, has no effect on the orientation of the vortices. That is, in the present experiments only one magnitude (either positive or negative) appeared to be obtainable for side force and yawing moment. In the tangent-ogive cylinder experiments of Alexan (1992) and Lee (1995), on the other hand, it was possible to obtain intermediate values of side force and yawing moment between the maximum

^{||}The nozzle volume flow rate was not recorded on audio for this particular set of flow visualization observations.

positive and negative limits, for a small range of C_μ values near zero. The limit magnitudes were typically greater than the baseline ($C_\mu = 0$) values. The difference in behaviour is sketched in Fig. 5.11. At this point, it is important to recognize the difference between the forebody models: for the current work, the semi-apex angle of the forebody is $\delta_N = 12.8$ deg, and in the earlier work, $\delta_N \approx 30$ deg. Keener and Chapman (1977) advocated that the forebody vortex flow field develops an instability “resulting from the crowding together of the vortices as the apex angle is decreased”. In other words, as the apex angle decreases, asymmetric forebody vortices are unable to maintain a stable, nearly side-by-side coexistence, presumably because of their relatively large size within the narrow ‘enclosure’ of the leeward flow field. With this insight in mind, it is suggested that for the forebodies tested earlier by Alexan and Lee, the width of the enclosed flow field was relatively large so that the asymmetric vortices were able to coexist in nearly side-by-side arrangements over a finite range of C_μ near zero, allowing forward blowing to control their arrangement in varying degree, including a limiting arrangement that yields the highest side force and yawing moment possible. For the forebody of the present work, the width of the enclosed flow field is relatively small due to the small apex angle and it is suggested that the vortices can only coexist in the limiting arrangements, with or without blowing; intermediate arrangements are not possible regardless of the amount of forward-blowing. For a forebody vortex control technique to produce side force and yawing moment greater than baseline magnitudes, the semi-apex angle of the forebody must be relatively large, such as 30 deg.

The reversal of vortex orientation at relatively high blowing momentum is a phenomenon observed in the current work that was not seen in the experiments of Alexan (1992) and Lee (1995). The cause of the reversal was investigated in the water tunnel and off-surface flow visualization (presented and discussed in detail in Section 5.7.1) appears to indicate that high-momentum nozzle flux interrupts the formation of the high vortex on the blowing side, and encourages the shear layer to form a new vortex that is situated instead in the low position. The vortex on the non-blowing side consequently adopts the high position, completing the reversal. As discussed previously in Section 2.2.3, Neuteboom (2002) suggests that the high-blowing reversal arises from the dominance

of entrainment effects that favour attachment of the vortex on the blowing side. The reversal phenomenon is certainly undesirable from a flight control perspective. But it is not perceived to be a serious problem with the control scheme under investigation because a blowing momentum coefficient above or below the threshold could be used for dynamic blowing.

Pitching moment and normal force. In Fig. 5.6, as the magnitude C_μ increases and crosses into the reversal threshold, $\bar{C}_m$ and $\bar{C}_Z$ both increase, reaching maximum magnitudes as the forebody vortices are undergoing a change of orientation. After the reversal is complete, $\bar{C}_Z$ settles back to pre-reversal levels; $\bar{C}_m$ returns to magnitudes that are slightly higher than before the reversal.

Increased contributions from the forebody may likely account for the small increase in $\bar{C}_Z$ and $\bar{C}_m$. With increasing C_μ during a change of orientation, the forebody vortices must pass through a symmetric arrangement for which $\bar{C}_n$, $\bar{C}_Y$, and $\bar{C}_\ell$ will theoretically be zero. With symmetric vortices and even near-symmetric vortices, the combined influence of the vortices has the potential to reduce the static pressure over the leeward surface of the forebody to levels that are lower than for an asymmetric vortex arrangement, giving rise to an increment in $\bar{C}_Z$ and $\bar{C}_m$, owing to a long moment arm.

It is not certain whether there is a contribution of the wing to the increases in normal force during the reversal of the forebody vortices. A zero rolling moment is an indicator that the wing leading-edge vortices are oriented symmetrically over the wing with vortex bursting occurring at the same chord. It is unknown whether the normal force generated in the presence of symmetrically-oriented leading-edge vortices, differs from that due to asymmetric vortices.

Rolling moment. The response of $\bar{C}_\ell$ to C_μ in Fig. 5.6 is very similar to $\bar{C}_n$ and reflects the strong interaction that exists between the forebody vortices and the wing leading-edge vortices at $\sigma = 45$ deg. Moreover, the fact that rolling moment is positive when yawing moment is positive, and vice versa, has the potential to be a favourable juxtaposition for post-stall maneuvering (Fig. 1.2).

Unsteadiness. The unsteadiness in C_n and C_Y tend to be relatively high at the C_μ

bordering the interval of vortex reversal. The increased unsteadiness in the yaw plane highlights the difficulty encountered in attempting to acquire data at these points under conditions that could readily excite the model to resonance.

For some forebody tips, there was also difficulty in acquiring data at low blowing momentum coefficients between the baseline condition and the threshold of vortex reversal. As an example, the results for Tip 2-2 are shown in Fig. 5.12 for the same test conditions as for Tip 6-1 in Fig. 5.6. The axial location of the nozzles in Tip 2-2 is the same as in Tip 6-1 ($x_n/D=0.095$), but the azimuthal positions, relative to the windward meridian, are 135 and 120 deg, respectively. In the results for Tip 2-2, significant departures in $\bar{C}_n$ and $\bar{C}_Y$ are observed to occur at low blowing momentum coefficients for both port and starboard blowing. Associated with these results is a significant level of unsteadiness not seen in the results for Tip 6-1 at the same C_μ . The contrast is attributed to the difference in the azimuthal position of the nozzles and is discussed further in Section 5.6.2. The uncertainty in acquiring useful results for $\bar{C}_n$ and $\bar{C}_Y$ at low C_μ for some tips, motivated the decision to acquire steady blowing and dynamic blowing data with C_μ exceeding the reversal threshold instead, where $\bar{C}_n$ and $\bar{C}_Y$ were found to be steadier.

5.4.2 Effect of Angle of Attack

The effects of angle of attack on all time-average forces and moments, and mean centres of pressure are presented in Fig. 5.13. The results are for Tip 6-1, an average Reynolds number of 2.0×10^5 and $\phi=0$ deg. The average C_μ of 2.64×10^{-3} exceeds the reversal threshold in Fig. 5.6; it follows that steady blowing from the port nozzle gives rise to negative yawing moment and side force. The baseline results for Tip 6-1 ($Re_D \simeq 1.9 \times 10^5$) shown in Fig. 5.2, are superimposed for comparison.

Yawing moment and side force. Some departure from symmetry in the variations of $\bar{C}_n$ and $\bar{C}_Y$ with angle of attack is evident. The variation of $\bar{C}_Y$ with σ is not symmetric about $\bar{C}_Y=0$, unlike the results shown in Fig. 2.58 for a tangent-ogive cylinder model. The extent of the asymmetry is revealed by the result for $\bar{C}_Y$ at $\sigma=45$ deg: for starboard blowing, the magnitude of positive $\bar{C}_Y$ is slightly greater than that obtained for the same C_μ

in Fig. 5.6; for port blowing, the magnitude is slightly lower. In contrast, the symmetry of the $\overline{C}_n$ -response is quite reasonable. The values of $\overline{C}_n$ at $\sigma = 45$ deg are in good agreement with the results for the same C_μ in Fig. 5.6, particularly for port blowing. It is surprising, however, that for $30 \leq \sigma < 45$ deg starboard blowing (above the reversal threshold level) is unable to produce $\overline{C}_n$ that at least matches the baseline, which is observed in the results for $\overline{C}_Y$. Of note is that a change in the sign of $\overline{C}_n$ appears to occur at the same angle of attack, between 25 and 30 degs; the change in sign typically is an indication that the centre of pressure for side force has passed the moment reference during its forward movement with increasing angle of attack as shown in Fig. 5.13(f). In fact, it is unknown whether this is actually the case because a coincident change in sign of $\overline{C}_Y$ did not occur. Also of note, the results for $\overline{C}_n$ show that the reversal phenomenon occurs over the entire σ -range for asymmetric vortices.

The departures from symmetry in the $\overline{C}_Y$ -response appear to be another consequence of a high level of uncertainty in $\overline{C}_Y$. Further evidence of this can be seen in the somewhat poor repeatability in $\overline{C}_Y$ in Fig. 5.14. Moreover, in Fig. 5.15, the results of a σ -sweep at the higher freestream velocity of 54.9 m/s, reflect an improvement in the symmetry of the $\overline{C}_Y$ -response, probably because of the reduced uncertainty in the measurement of side force at this velocity.

The results for $\overline{C}_n$ and $\overline{C}_Y$ in Fig. 5.13 are expected to be similar, albeit with signs reversed, for blowing momentum coefficients below the reversal threshold.

Pitching moment and normal force. Steady blowing from either the port or starboard nozzle produces almost identical responses in $\overline{C}_m$ and $\overline{C}_Z$, which are comparable to that of the respective baseline over most of the range of angle of attack.

Rolling moment. The response of rolling moment under steady blowing conditions is symmetrical about $C_\ell = 0$ and somewhat similar to the behaviour of $\overline{C}_n$, highlighting again the interaction between the forebody and wing leading-edge vortex systems. However, steady nozzle blowing affects $\overline{C}_\ell$ over a σ -range (approximately 20 deg) that is smaller than that for $\overline{C}_n$. The benefit of having rolling moment coordinated with yawing moment during a post-stall maneuver occurs over only a limited range of angle of attack. As with

$\bar{C}_n$ and $\bar{C}_Y$, the results for $\bar{C}_\ell$ should be similar for blowing momentum coefficients below the reversal threshold, except the sense will be the opposite.

Unsteadiness. With the onset of asymmetry in the forebody vortices, the unsteadiness in $\bar{C}_n$ and $\bar{C}_Y$ increases with angle of attack, until reaching peak magnitudes at $\sigma \approx 45$ deg. The magnitude of peak unsteadiness is about the same for $\bar{C}_n$ and $\bar{C}_Y$. Interestingly, the unsteadiness in $\bar{C}_m$ appears to follow that of $\bar{C}_n$ and $\bar{C}_Y$, suggesting that the trend is a product of the interaction between the forebody and wing leading-edge vortex systems. In spite of the interaction, however, the unsteadiness in $\bar{C}_\ell$ is relatively small.

Hysteresis. The possibility of hysteresis affecting the forces and moments under steady blowing conditions was not investigated, i.e., measurements were not taken with decreasing angle of attack starting from $\sigma = 70$ deg, for instance. There are probably no implications for the dynamic manipulation scheme since the alternating blowing would be expected to obviate any hysteresis effects that may or may not occur under steady blowing conditions.

5.4.3 Effect of Sideslip

The effects of sideslip on the time-average forces and moments, and mean centres of pressure are presented in Fig. 5.16. The results are for Tip 6-1, an average Reynolds number of 2.0×10^5 and $\sigma = 45$ deg. The average C_μ of 3.5×10^{-3} exceeds the reversal threshold in Fig. 5.6.

In spite of the sideslip condition, it is apparent that blowing from forward-facing nozzles located in the vicinity of the forebody apex ($x_n/D = 0.095$ for Tip 6-1) is effective up to a roll angle of at least 20 deg, which is equivalent to $\beta = 14$ deg at $\sigma = 45$ deg. The most forward placement of the nozzles, as with Tip 6-1, was anticipated to make blowing effectiveness insensitive to sideslip due to a large ratio of nozzle diameter to local cross-sectional radius of the forebody, at the point where the nozzle exits are located. With a large ratio, the fraction of forebody surface area removed by the nozzle cutout is significant which permits a nozzle jet to continuously influence the local separated shear layer,

irrespective of the circumferential movement of the primary separation lines caused by sideslipping.

Steady blowing at non-zero sideslip has an effect on $\bar{C}_n$, $\bar{C}_Y$, $\bar{C}_m$ and $\bar{C}_\ell$, but not $\bar{C}_Z$. For the forces and moments affected by sideslip, the results for starboard blowing are not significantly different from the baseline case because blowing from the starboard nozzle beyond the reversal threshold returns the vortex orientation back to the baseline (for blowing below the reversal threshold, the port-blowing results should correspond with the baseline instead). Moreover, the variations of $\bar{C}_n$, $\bar{C}_Y$ and $\bar{C}_\ell$ with ϕ that arise from steady port and starboard blowing, are almost parallel in each case so that these quantities are symmetric about zero for sideslip attitudes of equal angle but opposite sign. Thus, directional control is achievable by manipulating $\bar{C}_n$ and $\bar{C}_Y$ by switching the blowing conditions, i.e., from port to starboard, or vice versa. However, a switch in blowing conditions at non-zero sideslip is accompanied by a cross-coupling effect in the pitch axis, namely a change in the sense of $\bar{C}_m$. This abrupt change in pitching moment is looked upon as an undesirable feature in the results for $\bar{C}_m$ from a flight-mechanics perspective. The response of $\bar{C}_\ell$ to steady blowing indicates that the interaction between the forebody and wing leading-edge vortex systems continues to persist even in a sideslip attitude.

5.5 Dynamic Blowing Results

The preceding section has shown that the current forebody has the potential, under steady blowing conditions, to generate yawing moment and side force at magnitudes that are equal and opposite in sign depending on whether blowing is from the port or starboard nozzle. These magnitudes vary with angle of attack and, to a lesser extent with this forebody, with the amount of blowing momentum; indeed, the behaviour of yawing moment and side force with blowing momentum differs dramatically in some respects from the previous work due, it is believed, to the difference in the semi-apex angle of the forebodies. Regardless, the levels of yawing moment and side force that arise from steady blowing from each nozzle and the attitude of the aircraft, represent *limiting* magnitudes

between which the time-average of the aerodynamic parameters can be modulated with the duty-cycle parameter.

In this section, results are presented to demonstrate that the dynamic manipulation technique can be successfully applied to a realistic aircraft-like configuration. The effects of duty-cycle parameter, alternating blowing frequency, angle of attack and sideslip on the variation of the time-average aerodynamic forces and moments, are examined. Estimates of response time-delay to dynamic blowing are also presented.

5.5.1 Typical Result of Dynamic Blowing

Typical responses of time-average forces and moments to the port duty-cycle parameter (τ/T) are shown in Fig. 5.17. In addition, the corresponding time-histories — superimposed with the time-histories of the blowing momentum coefficients for both the port and starboard nozzles — are shown in Fig. 5.18 for the purpose of providing insight into the development of the time-averages. The results are for Tip 6-1, an average Reynolds number of 2.0×10^5 , $\sigma = 45$ deg and $\phi = 0$ deg. The average magnitude of C_μ , 3.5×10^{-3} , surpasses the reversal threshold in Fig. 5.6 for both port and starboard blowing. At this magnitude of C_μ , blowing through the port nozzle gives rise to negative yawing moment and side force, and vice versa for starboard blowing. The frequency of alternating blowing is 11.6 Hz, or $\omega^* = 0.16$, the maximum reduced frequency reported by Alexan (1992).

For a discussion about the performance of the blowing system — reflected in the time-histories of the blowing momentum coefficient in Fig. 5.18 — reference should be made to Section A.5 in Appendix A.

Yawing moment and side force. The symmetry and linearity in the variation of $\bar{C}_n$ is considered to be very good. The magnitudes of $\bar{C}_n$ under steady blowing conditions ($\tau/T = 0\%$ and 100%) compare very well with each other and also reasonably well with the corresponding results for steady blowing in Fig. 5.6. For $\tau/T = 50\%$, $\bar{C}_n$ is not precisely zero, but with a magnitude of 0.0029, this result is considered reasonable. The results for $\bar{C}_n$ conform to expectations (Fig. 1.5(b)) and to the previous results for the

tangent-ogive cylinder (Fig. 2.59). In comparison to these previous results, the principal difference in the current work is the reversed slope of the linear variation, which is a consequence of the reversal phenomenon and of using a magnitude of C_μ above the reversal threshold.

In Fig. 5.18(a), dynamic blowing provokes a yawing moment response that oscillates at the alternating blowing frequency, follows the variation of the duty cycle, and, not surprisingly, lags behind the controlled perturbation represented by C_μ . The response of C_n nominally oscillates between the limiting magnitudes arising from steady blowing conditions, i.e., at $\tau/T=0$ and 100%. *Response-cutoff* — a condition that arises from a relatively long time-delay in the response to dynamic blowing — appears to occur in C_n for $\tau/T \leq 20\%$ and $\tau/T \geq 80\%$. For instance, at $\tau/T=20\%$, C_n , after responding to a change of blowing state, is unable to reach the other limiting magnitude before being forced to return**. The effect of cutoff in a response is an unwanted biasing that tends to raise the magnitude of the time-average of the response, leading to the appearance of a local non-linearity in the variation of the time-average quantity with τ/T . The linearity can be expected to improve at lower ω^* where response-cutoff would be less or absent. Although response-cutoff is evident in the time-history for C_n , offsetting effects elsewhere in the time-history may be responsible for making the effect of response-cutout hardly noticeable in the time-average results.

In Fig. 5.17(b), the variation of $\bar{C}_Y$ with τ/T is approximately linear. The effect of uncertainty in the measurement of side force is exemplified by the poor repeatability between the current results for $\bar{C}_Y$ at $\tau/T=0$ and 100% and the corresponding results under steady blowing conditions shown in Fig. 5.6. Under dynamic blowing conditions, the side force response is forced at the alternating blowing frequency (Fig. 5.18(b)), but it is exacerbated by the superposition of a relatively large amplitude oscillation. The oscillation has a frequency of 35 ± 4.5 Hz, which is close to the natural frequency of model/sting/support system (30 Hz). Most probably the oscillation is thus due to forced vibration of the model/sting/support system, something that would not occur in free flight.

**The term *response-cutoff* shares a similar definition with the term *premature switching* appearing in Appendix A.

The yawing moment and side forces responses (Fig. 5.18) clearly contain a frequency component that is greater than that for alternating blowing. For side force, as described above, the frequency is attributed to the natural frequency of the model and its support system. The variation superimposed on the C_n -response, however, is about twice the natural frequency. This observation has led to speculation that the sinusoidal variation of C_Y and $X_{cp,Y}$, associated with the resonance and presumably of the same frequency, combine to form a variation of C_n that is twice the frequency (i.e., similar to the double-angle trigonometric identity).

Pitching moment and normal force. In Fig. 5.17, the variation of τ/T appears to have an effect on $\bar{C}_Z$ and $\bar{C}_m$ at small duty-cycle fractions, i.e., $\bar{C}_Z$ and $\bar{C}_m$ are slightly higher at $\tau/T = 10, 20, 80$ and 90% than elsewhere. The small magnitudes of $\bar{C}_m$ indicate that the centre of pressure for $\bar{C}_Z$ is close to the moment reference of the model. The fact that the variation of $\bar{C}_m$ with τ/T mirrors that of $\bar{C}_Z$, signifies that the centre of pressure has a very small range of movement (Fig. 5.17(g)). The small travel and close proximity of the centre of pressure to the moment reference are indicators that the increase in $\bar{C}_Z$, under dynamic blowing conditions, is the result of an increased contribution by the wing rather than the forebody. Thus, the behaviour at small duty-cycle fractions may be related to an apparent gain in $\bar{C}_Z$ — attributed to the wing under dynamic blowing conditions — that is discussed in Section 5.5.4.

The pitching moment and normal force time-response, shown in Figs. 5.18(d) and (e), respectively, are different in comparison to those of yawing moment, side force, and rolling moment. Unlike C_n , C_Y and C_ℓ , the responses of C_m and C_Y do not appear to oscillate at the alternating blowing frequency, nor follow the variation of the duty-cycle parameter. In fact, the frequency of oscillation is typically between 25 and 28 Hz, a range that is close to the estimated natural frequency of 30 Hz in the z-axis and is observed in both the dynamic and steady blowing cases. It appears the forced vibration in the lateral axis due to dynamic blowing, triggers the amplification of a low-amplitude oscillation in the z-axis.

Rolling moment. In view of the interaction between the forebody and the wing

leading-edge vortex systems at $\sigma = 45$ deg, a linear variation of the $\bar{C}_\ell$ with τ/T is expected (Fig. 5.17(e)). The fact that the direction of the slope $\partial\bar{C}_\ell/\partial(\tau/T)$ corresponds with that of $\partial\bar{C}_n/\partial(\tau/T)$ shows that dynamic blowing does not affect the potential for coordinating yawing moment and rolling moment during post-stall maneuvering. The linearity of $\bar{C}_\ell$, however, is distorted somewhat for duty-cycle intervals of $0 < \tau/T < 20\%$ and $80 < \tau/T < 100\%$. Although $\bar{C}_\ell$ is not precisely zero at 50% duty cycle, it represents a substantial reduction of the non-zero rolling moment found in the baseline case (Fig. 5.2). The reduction confirms an earlier finding by Ng et al. (1994) that alternating blowing with 50% duty cycle has the potential to eliminate asymmetric rolling moments due to forebody vortex asymmetry (Section 2.3.1).

The effect of cutoff in the C_ℓ -response is responsible for the local non-linearities in the variation of $\bar{C}_\ell$ with τ/T at small and large duty-cycle fractions. Examples of response-cutoff are clearly shown in Fig. 5.18(e) at $\tau/T = 10$ and 90%: after responding to a change of blowing state, the rolling moment is unable to reach the magnitude of the other steady-state limit before being forced to return. As a result, the magnitude of $\bar{C}_\ell$ is higher than it should be at these duty cycles. Response-cutoff occurs to a lesser extent at $\tau/T = 20\%$ and 80%. The time-delay in the response of $\bar{C}_\ell$ to dynamic blowing again plays a significant role in allowing cutoff to occur.

Like yawing moment, the rolling moment response shown in Fig. 5.18(e) oscillates at the alternating blowing frequency, follows the duty-cycle variation, and lags behind the input from the blowing system. However, C_ℓ appears to oscillate between limits that tend to decrease together from the magnitudes under steady blowing conditions at $\tau/T = 0$ and 100%, to a minimum at $\tau/T = 50\%$. In contrast, the responses of C_n and C_Y oscillate between the limiting magnitudes arising from steady blowing conditions. It is speculated that this behaviour in rolling moment is due to the combination of the slow speed of movement of the wing leading-edge vortex breakdown locations and the slow response of these vortices to dynamic blowing. Related to the effect of cutoff, it is believed there is insufficient time, as the duty cycle decreases from 100% (or increases from 0%), for the breakdown locations to change completely to their alternate positions, thereby preventing

the magnitude of the rolling moment seen under steady blowing conditions to evolve. Regardless, the behaviour appears to have a minimum impact on $\overline{C}_\ell$ since its variation with τ/T is acceptable.

5.5.2 Time-Delay of Response

A time cross-correlation analysis was undertaken to estimate the time-delay of the yawing moment and rolling moment in response to dynamic blowing. Given two functions, $x(t)$ and $y(t)$, the time cross-correlation function is

$$R_{xy}(\eta) = \frac{1}{T} \int_0^T x(t) y(t + \eta) dt \quad (5.3)$$

where η is a time-displacement variable. The cross-correlation coefficient function is the non-dimensional form of $R_{xy}(\eta)$ and is expressed as

$$\rho_{xy}(\eta) = \frac{R_{xy}(\eta)}{\sqrt{R_x(0) R_y(0)}} \quad (5.4)$$

where $R_x(0)$ and $R_y(0)$ are the mean square values of $x(t)$ and $y(t)$ respectively^{††}. The function $\rho_{xy}(\eta)$ has a range between 1 and -1. For perfect similarity between the two functions, $\rho_{xy}(\eta) = 1$; when $\rho_{xy}(\eta) = -1$, there is also perfect similarity with a change of sign; and $\rho_{xy}(\eta) = 0$ shows lack of correlation, possibly independence. The time cross-correlation analysis yields two time-delays (η_D) because a response can be correlated with both port and starboard C_μ . For example, since blowing above the reversal threshold from the starboard nozzle gives rise to positive yawing moment, the similarity between C_n and starboard C_μ will yield positive $\rho_{xy}(\eta)$; the similarity between C_n and port C_μ will yield negative $\rho_{xy}(\eta)$. In both cases, the highest magnitude of $\rho_{xy}(\eta)$ occurs at η_D . The time-delays were averaged (ideally, the two results should be equal) and normalized by the freestream convection time, i.e.,

$$t^* = \frac{\eta_D \times V_\infty}{L}, \quad (5.5)$$

^{††} $R_x(0)$ and $R_y(0)$ are also known as the autocorrelations for $x(t)$ and $y(t)$ at zero lag (i.e., $\eta = 0$).

V_∞ (m/s)	σ (deg)	ω^*	Port τ/T	C_n		C_ℓ		Fig.
				η_D (ms)	t^*	η_D (ms)	t^*	
36.6	45	0.16	40	8.31	0.323	24.91	0.967	5.18
			50	13.34	0.518	25.25	0.981	
			60	14.60	0.567	25.75	1.000	
			40	12.67	0.492	22.64	0.879	Not shown
			50	13.67	0.531	21.73	0.844	
			60	12.67	0.492	24.83	0.964	

Table 5.2. The time-delay and normalized time-delay of the yawing moment and rolling moment responses to dynamic blowing.

where t^* is the normalized time-delay and L is the overall length of the model. The time-delays of the C_n and C_ℓ -responses were computed for three duty cycles in Fig. 5.18, specifically for $\tau/T = 40, 50$ and 60% . These duty cycles were selected because each response has reasonably good definition that facilitates the cross-correlation computation.

The results for η_D and t^* are listed in Table 5.2. Taking into account the results for a set of responses not shown, the time-delay for the yawing moment response is approximately 13 ms, which appears to be a reasonable time delay for C_n in all the dynamic blowing cases in Fig. 5.18(a). The average normalized time-delay of the C_n -response is about 52% of the freestream convection time. For rolling moment, the time-delays of the responses appearing in Fig. 5.18(e) are reasonably consistent. In fact, for all the dynamic blowing cases, a nominal time of about 25 ms appears to be an appropriate delay for each rolling moment response. The time-delays in the second set of results are comparable in duration but vary slightly. Not surprisingly, since the wing is a considerable distance downstream of the forebody, the time-delay for a rolling moment response is longer than that for yawing moment. All the results for rolling moment t^* in Table 5.2 indicate it is on the order of the freestream convection time. In contrast, Peterson et al. (1996) found that the time-delay of the rolling moment response to the controlled perturbation of a movable stagnation point on a delta-wing model, was longer than the freestream convection time by one order of magnitude.

5.5.3 Effect of Alternating Blowing Frequency

An essential element of a dynamic manipulation system is the capability to deliberately switch the forebody vortices at a sufficiently high alternating blowing frequency that the inertial characteristics of the aircraft prevent it from significantly responding to the oscillating instantaneous aerodynamic loads. In this section, results are presented to illustrate the effect of alternating blowing frequency on the variation of time-average forces and moments with the duty-cycle parameter. For these results, the alternating frequency of the blowing system was restricted to 21 Hz because of limitations in the data acquisition system. Although the range of actual frequencies was limited, the range of reduced frequencies could be expanded by changing the freestream velocity.

In Fig. 5.19 the variation of the time-average forces and moments are shown for two reduced frequencies with $V_\infty = 36.6$ m/s and $\sigma = 45$ deg. Generally, the increase of ω^* from 0.16 to 0.27 does not appear to have a significant impact on time-average moments and forces. For $\omega^* = 0.27$, C_n , C_Y and C_ℓ were all found to be forced at the alternating blowing frequency of 19 Hz, and responding to the duty-cycle variation. Time-average side force shows slight increases with ω^* but the linearity of the variation with duty-cycle parameter remains largely unaffected. Slight increases in $\bar{C}_m$ and $\bar{C}_Z$ are also evident between $\tau/T = 10\%$ and 90% . Time-average rolling moment is consistent except for slight changes at $\tau/T = 40\%$ and 50% . Note the asymmetry in the steady-blowing magnitudes for $\bar{C}_n$. It happens that $C_\mu = 2.4 \times 10^{-3}$ falls within the reversal threshold for $\bar{C}_n$ (Fig. 5.6) and steady blowing from the port and starboard nozzles give rise to unequal magnitudes of $\bar{C}_n$.

Lowering the freestream velocity increases the range of reduced frequency to be investigated within the alternating-frequency limits of the blowing system. In Fig. 5.20, results are presented for three reduced frequencies with $V_\infty = 18.3$ m/s and $\sigma = 45$ deg. In Lee (1995), the highest ω^* investigated was 0.32. For $\omega^* = 0.48$, C_n and C_Y were driven at the alternating frequency of 17.5 Hz, and responding to the variation of duty cycle. Time-average yawing moment appears to be largely unaffected by the increase in ω^* , although an examination of its response for $\omega^* = 0.32$ and 0.48 shows that cutoff occurs at small

duty-cycle fractions such as $\tau/T = 10, 20$ and 30% . The results for $\bar{C}_Y$ are not reliable because of poor signal quality. The effects in $\bar{C}_m$ and $\bar{C}_Z$ are similar to those observed in Fig. 5.19 but are more evident at the higher reduced frequencies. For $20\% < \tau/T < 80\%$, $\bar{C}_m$ increases slightly with ω^* , peaking at $\tau/T = 50\%$. The variation of $\bar{C}_Z$ becomes increasingly non-linear at higher ω^* , with peaks occurring typically at $\tau/T = 20$ and 70% . These effects may be related to the dynamics of the wing leading-edge vortices that arises from the interaction of the forebody and wing vortex systems at $\sigma = 45$ deg. The results for $\bar{C}_\ell$ are not useful because of the long time-delay in the C_ℓ -response at this low freestream velocity — it is evident from Fig. 5.19 that the results improve with velocity.

Lee (1995) defined the maximum reduced frequency as the ω^* at which local non-linearities in the variation of $\bar{C}_n$ and $\bar{C}_Y$ begin to emerge at small duty-cycle fractions. Based on the limited evidence made available by the present work, it appears the maximum ω^* for the current aircraft configuration has the potential to exceed a value of 0.32.

It is expected that any flow-field unsteadiness, due to aircraft maneuvering, will be unimportant relative to the unsteadiness associated with the dynamic manipulation scheme because the effective ω^* of any maneuvering motion would be much less than ω^* for alternating blowing (due to the effect of aircraft inertia).

5.5.4 Effect of Angle of Attack

The effects of angle of attack on the variation of time-average forces and moments with duty-cycle parameter are shown in Figs. 5.21 and 5.22. The results are for Tip 6-1, $\omega^* = 0.16$ and $\phi = 0$ deg. For the results in Fig. 5.21 the average Reynolds number is 2.0×10^5 and the average C_μ is 2.4×10^{-3} — the blowing momentum coefficient falls on the edge of the C_μ -range where reversal occurs (i.e., approximately at the reversal threshold in Fig. 5.6). In Fig. 5.22 the average Reynolds number is 9.8×10^4 and the average C_μ is 6.9×10^{-3} , surpassing the reversal threshold.

The results for $\bar{C}_n$, $\bar{C}_Y$ (to a limited extent) and $\bar{C}_\ell$ show that the linear variation of these quantities with τ/T is maintained at other attitudes within the σ -range for asymmet-

ric forebody vortices. In Fig. 5.21 the variation of $\bar{C}_n$ with τ/T is reasonably linear as the angle of attack is changed. There tends to be less symmetry about $\bar{C}_n = 0$ in these results because C_μ falls on the edge of the reversal threshold, leading to unequal magnitudes of $\bar{C}_n$ at $\tau/T = 0$ and 100% (in contrast, the symmetry in the results for $\bar{C}_n$ is improved in Fig. 5.22 because C_μ exceeds the reversal threshold). Trends in the data for $\bar{C}_Y$ are ambiguous because of a high level of uncertainty, however, for $\sigma = 40, 45$ and 50 degs, the results show approximate linearity. Time-average rolling moment is independent of τ/T after $\sigma = 50$ deg because the interaction between the forebody and wing leading-edge vortex systems has diminished. Although the modulation of $\bar{C}_n$, $\bar{C}_Y$ and $\bar{C}_\ell$ with τ/T remains reasonably linear at various angles of attack, it is important to recognize that the slopes of the variations are governed by the magnitude of these quantities (as a function of forebody geometry and C_μ) at $\tau/T = 0$ and 100%, i.e., under steady blowing conditions.

The results for time-average normal force in Fig. 5.21 show an interesting gain in $\bar{C}_Z$ at some σ values for dynamic blowing as opposed to steady blowing conditions. This is particularly evident at $\sigma = 35$ deg and $\tau/T = 50\%$, where the increase is about about 15%^{‡‡}. At that condition, $\bar{C}_n$ and $\bar{C}_\ell$ are small or zero, indicating that the increase in $\bar{C}_Z$ is not accompanied by ancillary effects in the other axes. The increase in $\bar{C}_Z$ under dynamic blowing conditions was found to be somewhat similar to the production of dynamic lift for a pitching delta-wing. In Fig. 5.23 the results for $\bar{C}_Z$ at the 50% duty-cycle fraction (taken from Figs. 5.21 and 5.22) are accompanied by the results for C_Z measured during the upstroke of a pitching cycle (Hanff and Huang, 1996), and also the results for steady port and starboard blowing as well as baseline conditions. It is understood that above a certain angle of attack, about 30 deg in Fig. 5.23, C_Z due to the pitching motion of the model will be greater than the corresponding static result (i.e., the baseline result). The apparent increase in C_Z is attributed to a delay in the forward propagation of the breakdown location of the leading-edge vortices relative to the pitching rate, causing the production of C_Z to lag behind the increasing angle of attack during the upstroke. The dynamic blowing results for $\tau/T = 50\%$ closely resemble the pitching motion results in that both trends

^{‡‡}This increase occurs at the same angle of attack as the sudden decrease of $\bar{C}_Z$ under either steady blowing or baseline conditions (Fig. 5.23).

reach comparable peak magnitudes, albeit at different angles of attack. The similarity suggests that the rapid switching of the forebody vortices could be responsible for the gain in $\bar{C}_Z$ by exerting a time-average effect on the breakdown locations of the wing leading-edge vortices via the interaction of the two vortex systems at $\sigma = 35$ deg. Since the aft propagation speed of breakdown for a wing leading-edge vortex is considerably higher than its forward propagation speed (Hanff and Huang, 1991), the average location of breakdown may be further aft under dynamic blowing conditions than the corresponding static value, leading to the observed increase in $\bar{C}_Z$.

The gain in time-average normal force is of little value for post-stall combat maneuvers since the angle of attack at which the increase occurs, is too low. For the aircraft configuration under investigation, however, the gain in $\bar{C}_Z$ may be useful for “extremely short landings” which require a combat aircraft during a landing approach to maintain an angle of attack of approximately 40 deg and a speed of 100 kts (Kandebo, 2001). For this maneuver, the aim of dynamic manipulation would be to augment lift capacity (in conjunction with vectored thrust) rather than provide directional control.

5.5.5 Effect of Sideslip

The effects of sideslip on the variation of time-average forces and moments with duty-cycle parameter are shown in Fig. 5.24. The results are for Tip 6-1, $\omega^* = 0.16$ and $\sigma = 45$ deg. The average Reynolds number is 2.0×10^5 and the average C_μ is 3.5×10^{-3} , which exceeds the reversal threshold (Fig. 5.6). The results clearly demonstrate that the forebody vortices are responding to dynamic blowing under sideslipping conditions. In addition, these results re-emphasize the fact that the technique of dynamic blowing modulates the time-average aerodynamic quantities with duty-cycle parameter between the limiting magnitudes that arise under steady blowing conditions.

5.6 Nozzle Location

The excellent dynamic blowing results shown in Lee (1995) were obtained with forward-blowing nozzles located axially at $x_n/D = 0.25$ and placed azimuthally at $\theta = 135$ deg. Lee recommended that the nozzles be moved closer to the apex of the forebody to improve blowing effectiveness, i.e., to achieve success in the production and control of side force and yawing moment with minimum blowing effort at high Reynolds number and high angles of sideslip. This recommendation was embodied as an objective in the current work, i.e., to investigate the influence of axial and azimuthal location of forward-blowing nozzles on the forebody. Accordingly, a series of removable forebody tips were designed and manufactured with nozzles located at various axial and azimuthal positions. This objective was only partially met because of the preclusion of the Reynolds number study.

The data suitable for this purpose are steady-blowing results such as the blowing momentum surveys, primarily, and the angle-of-attack surveys, to a lesser extent. Dynamic blowing data is not useful for this purpose because it is steady blowing which establishes the limiting magnitudes between which time-average aerodynamic quantities are modulated by the duty-cycle parameter. The blowing momentum and angle-of-attack survey data are for $Re_D \approx 1.9 \times 10^5$ and $\phi = 0$ deg; for the blowing momentum surveys, $\sigma = 45$ deg; and for the angle-of-attack surveys, C_μ exceeds the reversal threshold. Results for six tips are presented, covering three axial locations and three azimuthal positions. The discussion will focus primarily on the results for $\bar{C}_n$ and $\bar{C}_Y$ because nozzle location does not appear to have a significant effect on $\bar{C}_m$, $\bar{C}_Z$ and $\bar{C}_\ell$. The latter results are, nonetheless, included for completeness.

5.6.1 Effect of Axial Location

The effects of nozzle axial location on the variation of forces and moments with blowing momentum coefficient and angle of attack are shown in Figs. 5.25 to 5.28. In these figures the results of C_μ - and σ -surveys are superimposed for tips with nozzles that share the same azimuthal position but are placed at different axial locations.

The C_μ -surveys are presented in Figs. 5.25 and 5.26. Figure 5.25 shows the results for Tips 6-1 and 9-3 which have nozzles located at $x_n/D=0.095$ and 0.159 respectively, along the 120-deg meridian. It is evident for Tip 9-3 that the reversal threshold occurs at a slightly higher C_μ than for Tip 6-1. Although a gap appears in the data for Tip 9-3 for starboard blowing below $C_\mu=2.0\times 10^{-3}$, the results for the same C_μ -range at a lower Reynolds number indicate that the magnitudes for $\bar{C}_n$ and $\bar{C}_Y$ are comparable to the results for Tip 6-1. The results for $\bar{C}_Y$ are a reflection of the $\bar{C}_n$ -results for both tips. In Fig. 5.26, results are shown for three tips placed along the 135-deg meridian. Tips 2-2, 1-7 and 3-13 have nozzles located respectively at $x_n/D=0.095$, 0.159 and 0.247 . With Tip 3-13, blowing is ineffective in affecting the vortex orientation, presumably because the nozzles are too far aft for this forebody. The reversal threshold for Tip 1-7 appears to occur at a slightly greater C_μ than for Tip 2-2. The apparent departure in $\bar{C}_n$ and $\bar{C}_Y$ for Tip 2-2 at $C_\mu=-1.3\times 10^{-3}$ and for Tip 1-7 at $C_\mu=1.3\times 10^{-3}$ may be attributed to a relatively high degree of variability in the measurements — in each case, the standard deviation was found to be significantly higher than that of the neighbouring data point at lower C_μ . The significance of the fact that the nozzles for these tips lie on the 135-deg meridian is revisited in the following section. The results for $\bar{C}_Y$ are similar in nature to $\bar{C}_n$. Of note in both figures, the magnitudes of $\bar{C}_n$ and $\bar{C}_Y$ are about the same as the baseline magnitude, before and after vortex reversal.

The σ -surveys for Tips 6-1 and 9-3 are overlayed in Fig. 5.27, and for Tips 2-2, 1-7 and 3-13, Fig. 5.28. In Fig. 5.27, the variations for $\bar{C}_n$ with σ are similar for $\sigma < 50$ deg with slight differences in peak magnitude at $\sigma = 50$ deg for port and starboard blowing. The $\bar{C}_Y$ -response for Tip 9-3 has good symmetry about $\bar{C}_Y=0$; the lack of symmetry in the $\bar{C}_Y$ -response for Tip 6-1 is more likely due to the close proximity of C_μ to the reversal threshold rather than an effect of nozzle location. In Fig. 5.28, the $\bar{C}_n$ -responses for Tips 2-2 and 1-7 agree reasonably well except for the differences in $\bar{C}_n$ due to starboard blowing. The data set for Tip 3-13 is incomplete because a high degree of unsteadiness in the model was encountered for $\sigma > 40$ deg.

For the current forebody, the effect of axial location is to increase the blowing momen-

tum coefficient, at which vortex reversal occurs, as the nozzles are moved farther away from the apex. The placement of the nozzles at $x_n/D=0.095$ on a forebody with a semi-apex angle of 12.8 deg, results in a rather small range of blowing momentum coefficient before the reversal occurs. If a wider margin of low blowing momentum coefficient is favoured, the nozzles should be located farther back from the apex at $x_n/D=0.159$. However, some insensitivity to sideslip may be lost as a result, since the effectiveness of the downwind nozzle to influence the vortex on the same side could diminish somewhat.

5.6.2 Effect of Azimuthal Position

The effects of nozzle azimuthal position on the variation of forces and moments with blowing momentum coefficient and angle of attack are presented in Figs. 5.29 to 5.32. The results of C_μ - and σ -surveys are overlayed in these figures for tips with nozzles that have the same axial location but different azimuthal positions.

The C_μ -surveys for Tips 6-1 and 2-2 are superimposed in Fig. 5.29. These tips have nozzles placed azimuthally at $\theta = 120$ and 135 deg, respectively, at $x_n/D=0.095$. As was indicated in the previous section, the results for $\bar{C}_n$ and $\bar{C}_Y$ for Tip 2-2 at $C_\mu = 1.3 \times 10^{-3}$ may be affected by a high degree of variability; in contrast, the results for Tip 6-1 are unaffected at low C_μ . This difference could reflect an effect in nozzle azimuthal position — with nozzles placed closer to the windward meridian than Tip 2-2, the edge of the nozzle cutout for Tip 6-1 could be serving as a fixed-edge of separation that tends to stabilize the forebody vortices. This suggests that as the nozzles are placed in higher azimuth, the forebody becomes more vulnerable to unsteadiness in the yaw plane. On the other hand, at high C_μ , above the reversal threshold, the results for $\bar{C}_n$ and $\bar{C}_Y$ for both tips agree very well — in this case, the presence of a nozzle jet could be having a stabilizing influence regardless of azimuthal position. Further evidence is seen in the results for Tips 9-3, 1-7 and 11 in Fig. 5.30. The nozzles in these tips are located at $x_n/D=0.095$; azimuthally, the nozzles are placed at $\theta = 119, 135$ and 151 deg, respectively. At $C_\mu = 1.3 \times 10^{-3}$ for Tip 1-7, a departure in $\bar{C}_n$ and $\bar{C}_Y$ occurs; with nozzles placed on the 135-deg meridian, a significant level of variability is associated with these results. With nozzles placed at even

greater azimuth, the responses of $\bar{C}_n$ and $\bar{C}_Y$ for Tip 11 are notable for having a baseline magnitude with an opposite sign compared to the other tips. Moreover, the behaviour of $\bar{C}_n$ and $\bar{C}_Y$ with C_μ differs considerably from those of the other tips: for starboard blowing, the vortices switch orientation at a magnitude of C_μ that is significantly lower than for Tips 9-3 and 1-7; for port blowing, a reversal does not occur at all; and the levels of unsteadiness were significant at low C_μ . Clearly, 151 deg is not a good azimuthal position for forward-blowing nozzles unless C_μ is relatively high.

The σ -surveys for Tips 6-1 and 2-2 are superimposed in Fig. 5.31. The $\bar{C}_n$ -response for Tip 2-2 has slightly higher magnitudes than for Tip 6-1 between the onset of asymmetry at $\sigma \approx 25$ deg up to $\sigma = 50$ deg. At the same time, the $\bar{C}_Y$ -responses for both tips have the same shape but neither is symmetric about $\bar{C}_Y = 0$. In Fig. 5.32, the peak magnitudes for $\bar{C}_n$ occurs at about the same angle of attack for tips 9-3, 1-7 and 11. Between the onset of asymmetry and $\sigma \approx 40$ deg, $\bar{C}_n$ has higher magnitudes for Tips 1-7 and 11. The $\bar{C}_Y$ -responses for all tips agree very well. The responses of $\bar{C}_n$ and $\bar{C}_Y$ for Tip 11 compare well with the other tips because the level of blowing effort gives rise to comparable values of $\bar{C}_n$ and $\bar{C}_Y$.

As the nozzles are placed at higher positions of azimuth, relative to the windward meridian, it appears the model becomes susceptible to increased levels of unsteadiness in the yaw plane at low blowing momentum coefficients. Based on the family of tips examined, the best azimuthal position is judged to be $\theta = 120$ deg for the current forebody.

5.7 Off-Surface Flow Visualization

Earlier in this chapter two images of off-surface visualization using dye injection were shown to illustrate the reversal of the forebody vortices in the water tunnel (Fig. 5.10). These images are in fact part of a larger sequence that shows in detail the response of the forebody vortices to steady blowing and reveals a probable cause of vortex reversal. This sequence of dye-injection visualizations in the water tunnel is presented in this section, followed by a similar series of images showing the response of the forebody vortices in a

cross-sectional view that is illuminated with a laser light-sheet.

5.7.1 Results of Dye Injection

A series of visualizations shown in Fig. 5.33 depicts the response of forebody vortices to blowing momentum coefficient as the nozzle flux increases gradually and continuously (i.e., without steps) from zero through the reversal threshold. Each frame in Fig. 5.33 was taken from a digital video recording and captures a momentary state in a flow that is changing gradually in time. The forebody is inclined at $\sigma = 45$ deg with zero roll angle. The freestream velocity is approximately 3.5 cm/s and steady blowing is from the port nozzle. Two dyed streaks, originating upstream of the forebody, illuminate the forebody vortices by becoming entrained in the swirling, inviscid region of the vortices. The nozzle flux is also dyed.

The sequence of visualizations, described hereafter, has shed light on a plausible cause for the reversal of the forebody vortex orientation. Note that some features appearing in the visualizations were also observed by Neuteboom (2002). In frame A of Fig. 5.33, naturally-occurring asymmetric vortices are observed with a high port vortex and a low starboard vortex. In frame B a dyed vortex core indicates that blowing through the port nozzle has begun. In C the nozzle flux is fully entrained in the core of the port vortex. The vortices at this moment appear to be symmetric, which may indicate that nozzle blowing has not yet influenced the port vortex. In D the vortex arrangement is shown at the instant prior to the movement of the port vortex. A bubble or *stagnation zone* — arising from the equilibrium of surface flow and nozzle jet momenta — forms over the cutout of the port nozzle. In E the port vortex is moving away from the leeward surface. With increasing C_{μ} , the stagnation zone over the exit of the port nozzle enlarges and the port vortex moves farther from the model, i.e., the vortex appears to be responding in proportion to the size of the stagnation zone — presumably the stagnation zone is influencing primary separation. In frame F blowing through the port nozzle has forced asymmetry with the port vortex high. In G two effects appear to accompany the enlargement of the stagnation zone. First, the formation of the port vortex appears to be interrupted, as indicated by a

discontinuity in the dyed core, but continues to be fed vorticity from the separated shear layer. Secondly, two additional dye streaks emanate from the stagnation zone. These streaks may represent the cores of a pair of vortices shed from the surface of the stagnation zone which, as a result of its growth, may appear as a blunt body to the freestream. In H, leading up to this moment, the separated shear layer on the port side briefly continued to feed the interrupted port vortex. However, in this frame, the separated shear layer appears to no longer feed this vortex, implying that the interrupted vortex is free. The port external dye streak now indicates that the shear layer forms a second port primary vortex closer to the model surface — the presence of the enlarging stagnation zone appears to somehow favour delayed primary separation in the vicinity of the apex, thus encouraging the formation of a low port vortex. In frame I the shear layer on the port side feeds the lower second port primary vortex. Fluctuations in the dyed streaks emanating from the stagnation zone over the port nozzle signal the imminent creation of a jet. In J penetration of the shear layer and formation of a nozzle jet is complete. In frame K external dye streaking clearly shows the second port primary vortex on the model surface, indicating that a reversal of the vortex orientation has occurred. In L further increase in C_μ extends the nozzle jet past the forebody apex without any apparent effect on the leeward flow field. Although nozzle blowing beyond the reversal threshold can no longer be considered to have complete control of primary separation, it nonetheless appears to have a stabilizing influence on the vortices. As C_μ is reduced from the magnitude in frame L, the forced asymmetric vortex arrangement seen in F can be recovered. Presumably, as the nozzle jet diminishes and the stagnation zone over the port nozzle reforms, influence of primary separation and thus control of the second port primary vortex is re-established.

On a lesser note, conducting dye visualization under no-blowing conditions afforded to opportunity to observe the sensitivity of the forebody vortices to external disturbances. Without steady blowing, the vortices were observed to switch orientation frequently, without regularity. Presumably the switch of orientation was triggered at the apex of the forebody by an unsteady freestream. The presence of large-scale eddies in the freestream flow was evident as the forebody vortices tended to sway; for example, one vortex was observed to cross over the leeward surface of the forebody under the other vortex. At

3.5 cm/s, the velocity was considered low enough to allow the freestream to be influenced by eddies that may have originated in the contraction or the settling chamber of the water tunnel*. With steady blowing, however, the random switching of the vortex orientation did not occur.

5.7.2 Results of Laser Light-Sheet Illumination

A series of visualizations in Fig. 5.34 show a sequence of cross-sectional views of the forebody vortices, illuminated by a laser-light sheet, at three stations along the longitudinal axis of the forebody. Each series depicts the response of the forebody vortices to steady blowing from the port nozzle, the amount of blowing increasing continuously from zero through the reversal threshold. Captured from a digital video recording, each frame represents a certain value of C_μ that is either below, within or above the reversal threshold. Frames bearing the same identification letter correspond approximately on the basis of C_μ . For a given frame, the variation of C_μ over the three series is small, except in the case of frame F. The observations were made at stations $x_s/l = 0.62, 0.40$ and 0.17 . The forebody is inclined at $\sigma = 50$ deg with zero roll, and the freestream velocity is 18.9 cm/s.

In Fig. 5.34, frame A, each x_s/l shows an asymmetric vortex arrangement with a high port vortex and a low starboard vortex for a magnitude of C_μ that is below the reversal threshold. A positive side force is expected to arise with this vortex arrangement that is very similar to the baseline case. In frame B the forebody vortices are on the verge of reversal. At $x_s/l = 0.62$ and 0.40 , the starboard vortex is clearly visible and remains down on the leeward surface, whereas the port vortex is difficult to identify. At $x_s/l = 0.17$, on the other hand, the port vortex is evident. Frame B appears to be corroborating evidence that substantiate observations with dye-streak visualization, specifically that the port vortex is interrupted as a nozzle jet forms and penetrates the local shear layer (Fig. 5.33, frame G). In frame C of Fig. 5.34 the port vortex is visible again at $x_s/l = 0.62$ and 0.40 . If in fact the shear layer forms a new vortex closer to the forebody, then the vortex seen in this frame should be the second primary port vortex. Both port and starboard vortices are

*The flow quality in the water tunnel has improved markedly since these tests.

in close proximity to the leeward surface in an arrangement that situates the port vortex slightly higher than the starboard vortex. The resulting side force is positive and lower in magnitude relative to the circumstances in frames A and B. In D the vortex arrangement is momentarily symmetric and a zero side force is expected. In frame E the vortex asymmetry is a near-mirror image of the arrangement appearing in frame C. The side force is negative and small in magnitude. Between the instances represented by frames C and E, the arrangement of the forebody vortices fluctuated independently of C_μ without regularity as C_μ increased continuously through the reversal threshold. In fact, at $x_s/l=0.62$ and 0.40 , the vortex orientation was observed to switch twice, indicating the degree of unsteadiness in the disposition of the vortices as C_μ passes through the reversal threshold. In frame F the reversal is complete and the asymmetry has developed further with a higher starboard vortex. The side force is expected to be negative and higher in magnitude relative to the circumstances in frames D and E.

The behaviour of the forebody vortices observed in the water tunnel corresponds to the behaviour that was encountered in the wind tunnel. This can be shown in two ways. First, since the variation of time-average yawing moment with blowing momentum coefficient was found to be independent of the presence of the delta-wing body (Fig. 5.8), it is possible to correlate the images of Fig. 5.34 with the measurements of yawing moment taken in the wind tunnel. In Fig. 5.35, the yawing moment data acquired with the forebody mounted on the delta-wing model is plotted against port- C_μ for two Reynolds numbers. Superimposed on these data are the flow visualization results which are represented by vertical lines, each plotted at a blowing momentum coefficient corresponding to a frame in Fig. 5.34 — each C_μ is an average taken over the three cross-sectional stations. The length of a line and its placement along the $\bar{C}_n$ -axis represents an estimate of expected yawing moment. For frames D, A and F of Fig. 5.34, there is a measure of certainty in the estimates: in frame D, a zero yawing moment is clearly expected to arise due to symmetric vortices; in both frames A and F, the arrangement of the forebody vortices is at the limit, thus the yawing moment produced should correspond with the limiting yawing moment in the wind-tunnel results. The comparison in Fig. 5.35 demonstrates that the off-surface flow visualizations in the water tunnel correlate well with wind-tunnel mea-

surements. Secondly, the off-surface flow visualizations in the water tunnel corroborate the surface flow visualizations acquired in the wind tunnel. In frame A, at $x_s/l = 0.62$ and 0.40 , the sense of rotation of the starboard vortex and its position on the forebody surface at the leeward meridian corresponds with the track of the starboard vortex identified in the surface flow visualization shown in Fig. 5.9(a). In frame A at $x_s/l = 0.17$, the close proximity of the vortices to the surface confirms the appearance in the surface flow visualization of a primary attachment line between two secondary secondary lines in the vicinity of $x_s/l = 0.17$. Likewise, frame F corresponds with Fig. 5.9(b).

The limiting vortex arrangements described in Section 5.4.1 for forebodies with narrow semi-apex angles, are clearly evident in frames A and F of Fig. 5.34 at $x_s/l = 0.62$ and 0.40 . At $x_s/l = 0.17$, however, the vortices appear to be in a near side-by-side existence. The change of vortex arrangement between $x_s/l = 0.17$ and 0.40 results in the apparent lift-off of a vortex from the surface of the forebody. Why does a change of vortex arrangement occur? In keeping with the *crowding* hypothesis of Keener and Chapman (1977), it is suggested that at a certain point along the forebody axis, the growth of the cross-sectional size of the vortices — with respect to the distance from the apex — exceeds the growth of the width of the leeward flow field enclosure and the vortices exchange the near side-by-side arrangement at $x_s/l = 0.17$ for the stacked arrangement seen at $x_s/l = 0.40$. If the semi-apex angle is larger, it is believed the change in arrangement will not occur as readily because the growth of the enclosure width will be more rapid.

Figure 5.1. Surface flow visualization in the wind tunnel illustrating the primary separation line of the starboard vortex, extending from the apex to the base of the forebody, approximately on the 90-deg meridian. Blowing is steady through the port nozzle of Tip 6-1. $Re_D = 2.0 \times 10^5$ (36.6 m/s), $\sigma = 45$ deg and $\phi = 0$ deg.

Figure 5.2. Variation of forces, moments, and mean centres of pressure against angle of attack without nozzle blowing for Tips 6-1 and 2-2. Results are shown for two Reynolds numbers and $\phi = 0$ deg.

(f) Mean centres of pressure for $Re_D \approx 9.6 \times 10^4$ (g) Mean centres of pressure for $Re_D \approx 1.9 \times 10^5$ **Figure 5.2. Concluded.**

Figure 5.3. The unsteadiness associated with forces and moments at each angle of attack without nozzle blowing for Tip 6-1 at two Reynolds numbers and $\phi = 0$ deg. Note different scales for standard deviations and averages.

Figure 5.4. The unsteadiness associated with forces and moments at each angle of attack without nozzle blowing for Tip 2-2 at two Reynolds numbers and $\phi = 0$ deg. Note different scales for standard deviations and averages.

(f) Mean centre of pressure, side force.

(g) Mean centre of pressure, normal force.

Figure 5.5. Concluded.

Figure 5.6. Variation of forces and moments, and mean centres of pressure with C_μ for Tip 6-1. $Re_D \approx 2.0 \times 10^5$ (36.6 m/s), $\sigma = 45$ deg and $\phi = 0$ deg.

(a) Yawing moment

(b) Side force

Figure 5.7. Variation of $\bar{C}_n$ and $\bar{C}_y$ with σ under steady blowing conditions for three freestream velocities. Tip 6-1 is installed on the delta-wing model and $\phi = 0 \text{ deg}$.

(a) Yawing moment

(b) Side force

Figure 5.8. Comparison of the variation of time-average yawing moment and side force with blowing momentum coefficient for Tip 6-1 on the delta-wing and cylindrical-afterbody models. $Re_D \approx 9.7 \times 10^4$ (18.3 m/s), $\sigma = 45$ deg and $\phi = 0$ deg.

(a) $C_\mu \simeq -1.2 \times 10^{-3}$, below the reversal threshold; the starboard forebody vortex is closest to the leeward surface.

(b) $C_\mu \simeq -7.8 \times 10^{-3}$, above the reversal threshold; the port forebody vortex is closest to the leeward surface

Figure 5.9. Surface flow visualization, performed in the wind tunnel, illustrates the effect of C_μ on the orientation of the forebody vortices. In (a) and (b), blowing is steady from the port nozzle. Tip 6-1 is installed on the forebody. $Re_D = 2.0 \times 10^5$ (36.6 m/s), $\sigma = 45$ deg and $\phi = 0$ deg.

(c) $C_\mu \simeq 1.2 \times 10^{-3}$, below the reversal threshold; the port forebody vortex is closest to the leeward surface.

(d) $C_\mu \simeq 7.8 \times 10^{-3}$, above the reversal threshold; the starboard forebody vortex is closest to the leeward surface

Figure 5.9. Concluded. In (c) and (d), blowing is steady from the starboard nozzle.

(a) With C_μ below the reversal threshold, the nozzle flux becomes entrained in the port vortex. The port forebody vortex is high above the model surface; the starboard vortex is closer to the surface. A positive side force arises with this vortex arrangement.

(b) With C_μ above the reversal threshold, the nozzle flux penetrates the shear layer. Now the port vortex is closest to the model surface; the starboard vortex is high above. This arrangement yields a negative side force.

Figure 5.10. Off-surface flow visualization, performed in the water tunnel, shows steady blowing from the port nozzle. The view is taken from the port side. The forebody model features Tip 2-2. $Re_D \approx 4.4 \times 10^3$, $\sigma = 45$ deg and $\phi = 0$ deg.

Figure 5.11. A comparison of the variations of side force and yawing moment with blowing momentum coefficient between a forebody with a *narrow* semi-apex angle and another with a *wide* semi-apex angle. Note that for a narrow semi-apex angle, it is postulated that the forebody vortices can only exist in a vertical plane and only one magnitude of side force and yawing moment is possible. For a wide semi-apex angle, the vortices are believed to coexist in nearly side-by-side arrangements, as sketched, with some possibility of variation in their relative positions. Side force and yawing moment can be controlled to a varying degree with forward-blowing nozzles up to a maximum magnitude. In this figure, the baseline magnitudes for C_Y and C_n are arbitrary for both forebodies and have no relative significance.

Figure 5.13. Variation of forces and moments, and mean centres of pressure with angle of attack under steady blowing conditions with Tip 6-1. $Re_D \approx 2.0 \times 10^5$ (36.6 m/s) and $\phi = 0^\circ$.

(f) Mean centre of pressure, side force

(g) Mean centre of pressure, normal force

Figure 5.13. Concluded.

Figure 5.14. Repeatability of the results for $\bar{C}_n$ and $\bar{C}_Y$ shown in Fig. 5.13.

Figure 5.15. Variation of $\bar{C}_n$ and $\bar{C}_Y$ with σ under steady blowing conditions with Tip 6-1. $Re_D \approx 2.9 \times 10^5$ (54.9 m/s) and $\phi = 0^\circ$.

Figure 5.16. The variation of forces and moments, and mean centres of pressure with roll angle under steady blowing conditions with Tip 6-1. $|C_\mu| \approx 3.5 \times 10^{-3}$, $Re_D \approx 2.0 \times 10^5$ (36.6 m/s) and $\sigma = 45^\circ$.

(f) Mean centre of pressure, side force.

(g) Mean centre of pressure, normal force.

Figure 5.16. Concluded.

Figure 5.17. Typical variation of forces and moments, and mean centres of pressure with duty-cycle parameter for Tip 6-1. $|C_\mu| = 3.5 \times 10^{-3}$, $Re_D = 2.0 \times 10^5$ (36.6 m/s), $\sigma = 45^\circ$, $\omega^* = 0.16$ (11.6 Hz) and $\phi = 0^\circ$.

(f) Mean centre of pressure, side force

(g) Mean centre of pressure, normal force

Figure 5.17. Concluded.

Figure 5.18. The corresponding time-histories of the force and moment results shown in Fig. 5.17. The duty-cycle fractions are shown in the top left-hand corner of each frame.

(b) Side force

Figure 5.18. (continued)

(c) Pitching moment

Figure 5.18. (continued)

(d) Normal force

Figure 5.18. (continued)

(e) Rolling moment

Figure 5.18. Concluded.

Figure 5.19. Effect of blowing frequency on the variation of forces and moments with duty-cycle parameter for Tip 6-1. $|C_\mu| \approx 2.4 \times 10^{-3}$, $Re_D \approx 2.0 \times 10^5$ (36.6 m/s), $\sigma = 45^\circ$ and $\phi = 0^\circ$.

Figure 5.20. Effect of blowing frequency on the variation of forces and moments with duty-cycle parameter for Tip 6-1. $|C_\mu| \approx 6.8 \times 10^{-3}$, $Re_D \approx 9.8 \times 10^4$ (18.3 m/s), $\sigma = 45$ deg and $\phi = 0$ deg.

Figure 5.21. Effect of angle of attack on the variation of forces and moments with duty-cycle parameter for Tip 6-1. $|C_\mu| \approx 2.4 \times 10^{-3}$, $Re_D \approx 2.0 \times 10^5$ (36.6 m/s), $\omega^* = 0.16$ (11.6 Hz) and $\phi = 0$ deg.

Figure 5.22. Effect of angle of attack on the variation of forces and moments with duty-cycle parameter for Tip 6-1. $|C_\mu| \approx 6.9 \times 10^{-3}$, $Re_D \approx 9.8 \times 10^4$ (18.3 m/s), $\omega^* = 0.16$ (5.8 Hz) and $\phi = 0$ deg.

(a)

(b)

Figure 5.23. Comparison of normal force, due to dynamic and steady blowing, with instantaneous normal force from pitching motion experiments. The pitching motion results are for the delta-wing model without a forebody (Hanff and Huang, 1996).

Figure 5.24. Effect of roll angle on the variation of forces and moments with duty-cycle parameter for Tip 6-1. $|C_\mu| \approx 3.5 \times 10^{-3}$, $Re_D \approx 2.0 \times 10^5$ (36.6 m/s), $\sigma = 45$ deg and $\omega^* = 0.16$ (11.6 Hz).

Figure 5.25. Effect of nozzle axial location on the variation of forces and moments with C_μ for steady blowing with Tips 6-1 and 9-3. Each nozzle is located azimuthally at $\theta = 120$ deg. $Re_D \approx 1.9 \times 10^5$ (36.6 m/s), $\sigma = 45$ deg and $\phi = 0$ deg.

Figure 5.26. Effect of nozzle axial location on the variation of forces and moments with C_μ for steady blowing with Tips 2-2, 1-7 and 3-13. Each nozzle is located azimuthally at $\theta = 135$ deg. $Re_D \approx 1.9 \times 10^5$ (36.6 m/s), $\sigma = 45$ deg and $\phi = 0$ deg.

Figure 5.27. Effect of nozzle axial location on the variation of forces and moments with σ , under steady blowing conditions, for Tips 6-1 and 9-3. The nozzles are located nominally at $\theta = 120$ deg. $Re_D \approx 2.0 \times 10^5$ (36.6 m/s) and $\phi = 0$ deg.

Figure 5.28. Effect of nozzle axial location on the variation of forces and moments with σ , under steady blowing conditions, for Tips 2-2, 1-7 and 3-13. The nozzles are placed azimuthally at $\theta = 135$ deg. $Re_D \approx 1.9 \times 10^5$ (36.6 m/s) and $\phi = 0$ deg.

Figure 5.29. Effect of nozzle azimuthal position on the variation of forces and moments with C_μ for steady blowing with Tips 6-1 and 2-2. Each nozzle is located axially at $x_n/D=0.095$. $Re_D \approx 2.0 \times 10^5$ (36.6 m/s), $\sigma = 45$ deg and $\phi = 0$ deg.

Figure 5.30. Effect of nozzle azimuthal position on the variation of forces and moments with C_μ for steady blowing with Tips 9-3, 1-7 and 11. Each nozzle is located axially at $x_n/D = 0.159$. $Re_D \approx 2.0 \times 10^5$ (36.6 m/s), $\sigma = 45^\circ$ and $\phi = 0^\circ$.

Figure 5.31. Effect of nozzle azimuthal position on the variation of forces and moments with σ , under steady blowing conditions, for Tips 6-1 and 2-2. Each nozzle is located fixed axially at $x_n/D = 0.095$. $Re_D \approx 2.0 \times 10^5$ (36.6 m/s) and $\phi = 0$ deg.

Figure 5.32. Effect of nozzle azimuthal position on the variation of forces and moments with σ , under steady blowing conditions, for Tips 9-3, 1-7 and 11. Each nozzle is located axially at $x_n/D = 0.159$. $Re_D \approx 2.0 \times 10^5$ (36.6 m/s) and $\phi = 0$ deg.

Figure 5.33. Sequence of off-surface flow visualizations in the water tunnel, illustrating the response of the forebody vortices to increasing steady blowing from the port nozzle. Each view faces the port side of the forebody. Tip 2-2, $Re_D \approx 4.4 \times 10^3$ (approximately 3.5 cm/s), $\sigma = 45$ deg and $\phi = 0$ deg. (A) Natural vortex asymmetry. (B) Blowing through port nozzle begins. (C) Momentary vortex symmetry. (D) Movement of port vortex imminent. (E) Movement of port vortex begins. (F) Forced vortex asymmetry. (G) Port vortex interrupted. (H) Interrupted vortex is free and a new port vortex appears. (I) Nozzle jet penetrates local shear layer. (J) Nozzle jet develops. (K) Reversed vortex asymmetric arrangement. (L) Nozzle jet extends beyond forebody apex.

(a) Laser light-sheet longitudinal location, $x_s/l = 0.62$

Figure 5.34. Effect of the variation of blowing momentum coefficient on the orientation of the forebody vortices as revealed by laser light-sheet visualization in the water tunnel. Blowing is steady and increasing from the port nozzle. The views are taken from the tip of the forebody. Tip 1-7, $Re_D \approx 2.1 \times 10^4$, $\sigma = 50$ deg and $\phi = 0$ deg.

Figure 5.34. (continued)

Figure 5.34. Concluded.

Figure 5.35. Comparison of forebody vortex orientations between the wind and water tunnel tests. A thick vertical line identified by a letter represents a laser light-sheet observation. Each letter corresponds to a frame in Fig. 5.34. Blowing is steady and increasing through the port nozzle. Tip 1-7, $\phi = 0$ deg. σ : wind tunnel, 45 deg; water tunnel, 50 deg.

Chapter 6

Conclusions

The technique of dynamic manipulation of forebody vortices is a novel scheme for actively controlling forebody vortices that has been demonstrated, in the previous work of Lee (1995), to be a promising means of augmenting the directional control of fighter aircraft at high angles of attack. In the present work, a wind tunnel experiment was undertaken to further investigate the potential of the technique with an aircraft-like model comprising a slender tangent-ogive forebody with a semi-apex angle of 12.8 deg, attached to a 65-deg delta-wing model with a vertical tail. The forebody featured forward-blowing nozzles near the apex of the forebody and a blowing system to serve as the means of perturbing the forebody vortices with alternating blowing. The objectives of the experiment were to determine whether the very good results obtained in the previous work on a tangent-ogive cylinder model could be obtained and improved upon on an aircraft-like configuration, i.e., would time-average yawing moment and side force for such a configuration be linear with duty-cycle parameter and could a higher reduced frequency of switching be attained; to investigate the influence of axial and azimuthal location of the forward-blowing nozzles; and to examine the effects of Reynolds number on the effectiveness of the control technique.

In the early stages of the wind tunnel experiment, an unusual and unexpected reversal of vortex orientation was detected in the yawing moment and side force measurements when nozzle blowing exceeded a certain threshold value. Although the occurrence of the vortex reversal was verified with surface flow visualization in the wind tunnel, a water tunnel experiment utilizing off-surface visualization techniques was subsequently under-

taken with the forebody alone to furnish additional confirmation of vortex reversal and to investigate the cause of the phenomenon.

A linear variation of time-average yawing moment with duty-cycle parameter was still found, even with sideslip, for the aircraft configuration. The forebody vortex orientation was found to switch successfully up to a reduced frequency, based on forebody base diameter, of at least 0.32, the highest tested by Lee (1995). While most of the data were obtained with blowing momentum above the reversal threshold, these findings are expected to hold for blowing below the threshold level as well.

As expected, the dynamic manipulation of the forebody vortices on the aircraft model was found to produce cross-coupling effects in rolling moment, pitching moment and normal force. For rolling moment and normal force, the cross-coupling effects appear to have favourable implications for flight mechanics; the effect in pitching moment occurs in sideslip and may have negative implications.

The effectiveness of forward-blowing was found to be insensitive to non-zero sideslip with nozzles placed as close to the apex as practicable ($x_n/D=0.095$). However, a more aft location for the nozzles ($x_n/D=0.159$) was found to increase the margin for low blowing momentum coefficient between the baseline and the reversal threshold, possibly at the cost of increasing the sensitivity of blowing effectiveness to sideslip. The preferred azimuthal position for the nozzles is considered to be 120 deg from the windward meridian because it was found to be associated with a steady leeward flow field.

The effect of Reynolds number on the effectiveness of the control technique could not be studied because a serious resonance condition with the model support limited the attainable freestream Reynolds number to about 2.0×10^5 , for the majority of the investigation.

The off-surface flow visualizations in the water tunnel provided corroborating evidence of vortex reversal when blowing exceeded the threshold. Moreover, the results led to the formulation of a hypothesis on the cause of the reversal: it is postulated that at the reversal threshold, the blowing momentum is sufficient to cause the nozzle flux to interrupt the formation of the high forebody vortex on the blowing side, and to encourage

the shear layer to form a replacement vortex that lies closer to the leeward surface of the forebody.

The magnitudes of the yawing moment and the side force produced under steady blowing conditions in the wind tunnel were found to be about the same as the magnitudes for the no-blowing, baseline case. This contrasts with previous work, where less slender forebodies were studied. The findings in the water tunnel suggest that this lack of gain may result from a difference in vortex arrangement — a stacked vortex arrangement for a slender forebody (with a narrow semi-apex angle of ~ 15 deg), in contrast to a near side-by-side baseline arrangement for a less slender forebody (with a wide semi-apex angle of ~ 30 deg).

The vortex reversal phenomenon and the absence of any substantial gain above baseline magnitudes of yawing moment and side force with forward blowing at high angles of attack are features not seen previously in the work with less slender forebodies. In view of these complexities, consideration should be given in the future to using a less slender forebody than the current one so that yawing moment and side force of more advantageous magnitudes may be produced. Also, possibly, the reversal phenomenon can be delayed to higher blowing momentum coefficients, or perhaps prevented altogether.

On the whole, the results of this work indicate that the dynamic manipulation scheme using forward blowing, either below or above the reversal threshold, is a viable and attractive means of obtaining linear yaw control for an aircraft-like configuration at high angles of attack. More research is required, however, to better define the design space and to mitigate technical risk.

Chapter 7

Recommendations

The research into the technique of dynamic manipulation of forebody vortices has focused on static model testing. To further demonstrate the viability and attractiveness of the technique, it would be logical to progress to dynamic model testing, such as an experiment involving a free-to-roll rig that allows the model to rotate about its velocity vector.

However the static-model research phase is not complete. The effect of Reynolds number on the technique remains an unanswered question, i.e., whether forward-blowing nozzles can influence the forebody vortices and whether dynamic blowing can modulate the vortices with mixed flow conditions (i.e., laminar-, transitional- and turbulent-separation conditions) prevailing over the forebody at high Reynolds numbers. The fact that yawing moments were generated on the X-29A with an aft-blowing system at high angles of attack in full scale flight tests (Malcolm, 1993), suggests that a forward-blowing scheme may be equally successful at least for steady blowing.

In future testing, consideration should be given to returning to a less slender tangent-ogive forebody, such as the forebodies examined in Alexan (1992) and Lee (1995), for the reasons outlined in the Conclusions. Particular care should be taken to ensure adequate stiffness of the model-support system. If the present system were to be used again, the installation of guy-wires to provide additional support to the sting is a simple inexpensive solution that should not have significant support-interference implications.

Recall that in principle, the technique of dynamic manipulation can be implemented using either pneumatic or mechanical means. In view of the difficulties encountered in the current work with pneumatic means, perhaps attention should be directed toward de-

veloping an alternative mechanical means.

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Appendix A

Valve Timing

To ensure that the desired duty-cycle characteristics are correctly reflected in the nozzle flow response, the timing of the drive signals to the solenoid valves in the forebody blowing system must take into account the lag times associated with the opening and closing action of each valve. This appendix describes the formulation of the equations that coordinate the timing of the solenoid valves, the measurement of valve transient response times, the limits of operation, and presents an appraisal of the performance of the forebody blowing system.

A.1 Valve Synchronization Equations

The response of the nozzle flow to the opening and closing action of a solenoid valve is illustrated in Fig. A.1. The flow is detected by the differential-pressure transducer located at the in-line metering device downstream of the delivery port of the valve. The output signal of the transducer is considered to correctly reflect the response of the flow because of its close proximity to the delivery port. Not surprisingly, the nozzle flow does not show an instantaneous response to a change of valve input signal. The time for the valve to open is shorter than its closure time, i.e., withdrawing the plunger from the flow passage with a solenoid is faster than returning it with a spring. The inequality of these transient response times leads to an inequality of duty cycle in the flow response because the duration of an open valve is longer than that of a closed valve. The valve control waveforms that energize the solenoids must therefore be tailored to provide the desired duty-cycle characteristics in the nozzle flow response.

To suitably alter the valve control waveforms, a redefinition of valve state (i.e., whether the valve is *open* or *closed*) is required. A sensible change of valve state is defined as the point at which the output signal of the differential-pressure transducer reaches 50% of the steady-state magnitude of a fully open valve (Fig. A.2), referred to as the *state threshold*. When the output signal exceeds the threshold level the valve is considered open; below the level, the valve is closed. It is assumed that nozzle flow below the threshold does not impart a significant influence on the forebody vortices. The valve transient response times, identified in Fig. A.2, are measured with respect to the state threshold: t_{rise} represents the time for the nozzle flow to increase from the condition of a fully closed valve to the threshold; t_{fall} represents the time for the nozzle flow to decay from the condition of a fully open valve to the threshold. The measurement of transient response time is described in Section A.2.

The valve control waveforms are rectangular pulse signals that are formed by two time parameters: the time delay to energize the solenoid of a valve, and the duration the solenoid remains energized. The time delays and durations are shown in Fig. A.1 and are described as follows:

- v_{1D} is the delay until the port valve is *energized* relative to the start of the data acquisition cycle;
- v_{1on} is the period the port valve is *energized*;
- v_{2D} is the delay until the starboard valve is *energized* relative to the *energizing* of the port valve; and
- v_{2on} is the period the starboard valve is *energized*.

Subscripts 1 and 2 denote the port and starboard valves respectively. These time parameters are adjusted to ensure that the desired duty-cycle characteristics are reflected in the nozzle flow responses.

The adjustments to the time delays and durations are developed in Fig. A.3. The desired control waveform for the port valve and the nozzle flow response to the action of the valve are shown in Fig. A.3(a) and (b) respectively. In (c) the nozzle flow response

is shifted so that its intersection with the state threshold conforms to the desired duty-cycle characteristics appearing in (a). The shift is achieved by reducing the delay of energizing the port valve by $t_{1_{rise}}$, and the energized-period of the port valve is altered by the difference between $t_{1_{rise}}$ and $t_{1_{fall}}$. The actual port valve control waveform, shown in Fig. A.3(d), is governed by

$$v'_{1D} = v_{1D} - t_{1_{rise}} \quad (A.1)$$

$$v'_{1on} = v_{1on} + t_{1_{rise}} - t_{1_{fall}} \quad (A.2)$$

The prime symbol (') represents the delay and duration times for the actual control waveform. Likewise, the starboard nozzle response is adjusted by $t_{2_{fall}}$ and $t_{2_{rise}}$ (Fig. A.3(e)) so that the time delay and open-duration for the actual starboard valve control waveform (Fig. A.3(f)) are

$$v'_{2D} = v_{1on} + t_{1_{rise}} - t_{2_{rise}} \quad (A.3)$$

$$v'_{2on} = v_{2on} + t_{2_{rise}} - t_{2_{fall}} \quad (A.4)$$

Equations A.1 to A.4 are referred to as *valve synchronization equations* because they ensure the state change of opening and closing valves occurs at the threshold. Figure A.4 show the nozzle flow response when the valve synchronization equations are applied.

Owing to an inherent inversion of the valve control waveform within the data acquisition system, it was necessary to formulate the actual valve control waveform in the inverted form. Thus the time delays and durations that define the control waveforms must refer to the closed state instead of the open state. In the manner previously shown, the valve synchronization equations can be recast as follows:

$$v'_{1D} = v_{1D} - t_{1_{fall}} \quad (A.5)$$

$$v'_{1off} = v_{1off} + t_{1_{fall}} - t_{1_{rise}} \quad (A.6)$$

$$v'_{2D} = v_{1off} + t_{1_{fall}} - t_{2_{fall}} \quad (A.7)$$

$$v'_{2off} = v_{2off} + t_{2_{fall}} - t_{2_{rise}} \quad (A.8)$$

Equations A.5 to A.8 were applied in this investigation. The time parameters are calculated in the test matrix spreadsheet and passed to the data acquisition system (Section 3.5.2).

A.2 Measurement of Valve Transient Response Time

Detailed measurements of the transient response times (t_{rise} and t_{fall}) were necessary for implementing the valve synchronization equations. These valve characteristics were measured by first capturing on a digital oscilloscope, a portion of the on/off cycle (e.g., as shown in Fig. A.2) in the output signal of the differential-pressure transducer at the in-line metering device. With the captured trace, the voltage difference (ΔV) between the steady on-state magnitude and the off-state was measured, then the voltage associated with the state threshold ($0.5\Delta V$) was determined; in turn, t_{rise} and t_{fall} were measured for the valve as shown in Fig. A.2. Because the transient response times were found to vary with supply pressure, measurements were taken over a range of differential pressures expected to be applied during the test. The results are plotted in Fig. A.5 against the differential-pressure voltage at the port in-line meter ($V_{I\Delta p}$). The selection of the port in-line meter was arbitrary. The correlations of the transient response times with $V_{I\Delta p}$ are reasonably linear. The data point for $V_{I\Delta p} = 0$ was excluded from the linear fits because very low supply pressures were not expected for the test. The results in Fig. A.5 show that for each valve t_{fall} is longer than t_{rise} , and the response times of the port valve are longer than those for the starboard valve. In addition, the slope of the linear fits for t_{1fall} and t_{2fall} are roughly equal as might be expected. This is not the case, however, for t_{1rise} and t_{2rise} indicating that the *opening* performance of the valves was not affected equally by the supply pressure owing, perhaps, to a mechanical shortcoming.

A.3 Premature Switching

Because of the transient response characteristics, it is possible to reverse the state of a valve before it becomes fully open or closed. This situation, referred to as *premature*

switching, can arise with a small duty-cycle fraction for which the period of an open or closed valve is comparable to the transient response times. Premature switching can be prevented by ensuring that the period of an open valve, τ_{on} , is greater than its t_{rise} and the t_{fall} of the other valve. Thus, for the port valve

$$v_{1on} > t_{1rise} \quad \text{and} \quad v_{1on} > t_{2fall} , \quad (\text{A.9})$$

and for the starboard valve

$$v_{2on} > t_{2rise} \quad \text{and} \quad v_{2on} > t_{1fall} . \quad (\text{A.10})$$

The tests described by Eqns. A.9 and A.10 were applied in the test matrix spreadsheet. Since each valve will always be open for a certain period during an alternating-blowing cycle, the tests need only be applied to the lesser of v_{1on} and v_{2on} .

Although not implemented for the current work, the tests for premature switching could be modified to take into account the response of the forebody vortices to the perturbation introduced by nozzle blowing. For example, Eqn. A.9 would be modified as follows:

$$v_{1on} > t_{1rise} + t_v \quad \text{and} \quad v_{1on} > t_{2fall} + t_v \quad (\text{A.11})$$

where t_v represents the time for the forebody vortices to change orientation following a nozzle perturbation. Satisfying the tests described in Eqn. A.11 would allow the forebody vortices to reach the intended orientation before the state of the valve is changed.

A.4 Frequency Limits

In view of the discussion in the previous section, limits on the alternating-blowing frequency of the forebody blowing system will be encountered under conditions of extreme duty-cycle operation, i.e., when the duty cycle is close to 0% or 100%. This suggests that an operating envelope can be developed to describe the available range of valve operation based on valve duty cycle and supply pressure as represented by the differential-pressure,

$V_{1\Delta p}$. To begin the development of the envelope, let the valve *on* duty-cycle fraction be defined as

$$DC = \frac{v_{on}}{T} \quad (A.12)$$

where T is the control period. Since t_{fall} is longer than t_{rise} (Fig. A.5), the critical test from Eqns. A.9 and A.10 is

$$v_{on} > t_{fall}$$

Substituting for τ_{on} in Eqn. A.12

$$DC > \frac{t_{fall}}{T} = t_{fall} \cdot f$$

Rearranging to solve for f , the alternating-blowing frequency,

$$f < \frac{DC}{t_{fall}}. \quad (A.13)$$

But t_{fall} is essentially a linear function of $V_{\Delta p}$, and since the port valve has the longest t_{fall} , then

$$t_{1_{fall}} = 0.004276 V_{1\Delta p} + 4.122$$

from Fig. A.5 will govern the limit on the alternating-blowing frequency. After substituting for $t_{1_{fall}}$ in Eqn. A.13, the frequency limit is described by

$$f_{limit} = \frac{DC}{0.004276 V_{1\Delta p} + 4.122}. \quad (A.14)$$

For a given duty-cycle fraction and differential-pressure voltage, Eqn. A.14 defines a boundary which the frequency of alternating blowing cannot exceed. Limit frequencies derived from Eqn. A.14, are tabulated in Fig. A.6 for a range of $V_{1\Delta p}$ and the duty-cycle fractions of 5%, 10% and 15%. These results are also plotted in Fig. A.6 to illustrate that the frequency boundaries are curves of constant duty-cycle fraction.

A.5 System Performance

In Fig. A.7 time-histories of C_μ for the port and starboard nozzle at various duty cycles, demonstrate the results of implementing the valve synchronization equations and the models of the valve transient response times. The frequency of alternating blowing is 11.6 Hz and the differential-pressure voltage is about 173 mV. In Fig. A.6 it is readily apparent that the operating point does not exceed the frequency limit for a duty-cycle fraction of 10%. In fact, a duty-cycle fraction as low as 6% appears to be possible. Compared to the expected effect of the adjusted valve control waveforms depicted in Fig. A.4, the results in Fig. A.7 show that the performance of the blowing system was largely successful. The intersection of the C_μ -waveforms, formed by a closing port valve and an opening starboard valve, occurs as it should and is consistent for all five duty cycles shown in Fig. A.7. For an opening port valve and a closing starboard valve, the intersection is poor for the 50%/50% duty cycle, however, the intersection improves as the 90%-port/10%-starboard duty cycle is reached.

A duty-cycle verification provides an overview of the performance of the blowing system (Fig. A.8). Actual *on* duty-cycle fractions (τ_{on}/T) are plotted against desired τ_{on}/T for the port and starboard nozzles. Actual τ_{on}/T was derived from the time-histories of C_μ , such as those appearing in Fig. A.7, based on the intersection of the time-history with the state threshold. The results of 29 duty-cycle sweeps conducted during the test are shown in Fig. A.8. With regard to average error (i.e., actual τ_{on}/T minus desired τ_{on}/T), the starboard nozzle performed the best — in terms of absolute magnitude, the error in starboard τ_{on}/T is generally half that for the port nozzle. For both nozzles, the average error increases as τ_{on}/T decreases from 90% to 10%. The variation of actual τ_{on}/T , as indicated by the standard deviation, is small and consistent for the starboard nozzle throughout the duty-cycle range. For $60 \leq \tau_{on}/T \leq 90\%$, the standard deviation of actual port τ_{on}/T is comparable to that of the starboard side; however, for $\tau_{on}/T \leq 50\%$ it deteriorates considerably, a sign that the port valve may have operated inconsistently. Overall, the performance of the starboard side of the blowing system is better. The valve synchronization equations are ruled out as a source for the performance disparity: The results for

actual τ_{on}/T on the starboard side are satisfactory and consistent; even the performance of the port side of the blowing system for $\tau_{on}/T \geq 60\%$ is looked upon favourably. Of the factors perceived to affect the mechanical performance of a valve (such as roll angle of the model, frequency of operation, and supply pressure), only the supply pressure appeared to have some correlation, i.e., the largest errors for the port valve occurred more or less at high $V_{l\Delta p}$. This finding suggests that a mechanical shortcoming affected the port valve.

Figure A.1. The effect of valve opening and closing characteristics on nozzle flow response.

Figure A.2. Definition of valve state based on the output voltage of the differential-pressure transducer at the in-line metering device.

Figure A.3. The adjustments to the valve control waveforms necessary to achieve the desired duty cycle characteristics in the nozzle flow responses.

Figure A.4. The expected effect of the adjusted valve control waveforms on the nozzle flow responses. The duty cycle is τ/T .

Figure A.5. Relation of t_{rise} and t_{fall} , for the port and starboard valves, as a function of the differential-pressure voltage across the port in-line metering device.

$V_{1\Delta p}$ (mV)	Frequency, f (Hz)		
	DC 5%	DC 10%	DC 15%
50	11.5	23.1	34.6
100	11.0	22.0	33.0
200	10.0	20.1	30.1
300	9.3	18.5	27.8
400	8.6	17.1	25.7
500	8.0	16.0	24.0
600	7.5	15.0	22.4
700	7.0	14.1	21.1

Figure A.6. Limits on the frequency of alternating blowing for duty-cycle fractions of 5%, 10% and 15%. Solid dots represent the data in the accompanying table; the open dot represents the operating point for the results shown in Fig. A.7.

(a) 50% port, 50% starboard

(b) 60% port, 40% starboard

(c) 70% port, 30% starboard

(d) 80% port, 20% starboard

(e) 90% port, 10% starboard

Figure A.7. Examples of C_μ time-histories for the port and starboard nozzles at various duty cycles. Operating point: $f = 11.6$ Hz, $V_{1\Delta p} = 173$ mV. Note that a negative C_μ has no meaning in this context.

Figure A.8. Comparison of actual *on* duty-cycle fraction (τ_{on}/T) with desired values for the port and starboard sides of the forebody blowing system. The results of 29 duty-cycle sweeps conducted during the test are shown.

Appendix B

Nozzle Flow Rate Calibration

In this investigation the coefficient of blowing momentum is based on the nozzle volume flow rate which is measured by a differential-pressure transducer placed across a metering device in the nozzle blowing system, as outlined in Section 3.2.3. This appendix describes the calibration of the output voltages of the pressure transducers with nozzle volume flow rate. The calibration apparatus and procedures are described, the results are presented, and the uncertainty in the coefficient of blowing momentum is assessed.

B.1 Coefficient of Blowing Momentum

The coefficient of blowing momentum (C_μ) is defined as

$$C_\mu = \frac{\dot{m}_j V_j}{q_\infty S_D} \quad (\text{B.1})$$

where $\dot{m}_j$ is the nozzle mass flow rate, V_j is the blowing nozzle exit velocity, q_∞ is the freestream dynamic pressure, and S_D is the base area of the forebody model. For this experiment it is assumed that incompressible flow conditions prevail at the nozzle exit and the density of the nozzle air (ρ_j) is approximately equal to the density of the freestream air (ρ_∞). After expanding freestream dynamic pressure, invoking $\rho_j \approx \rho_\infty$, and substituting for $\dot{m}_j = \rho_j Q_n$ and $V_j = Q_n/A_n$, Eqn. B.1 becomes

$$C_\mu = \frac{2 Q_n^2}{V_\infty^2 S_D A_n} \quad (\text{B.2})$$

where Q_n is the nozzle volume flow rate, and A_n is the cross-sectional area of a nozzle. Thus, the calibration process reduces to correlating the output voltage of the differential-pressure transducer across the in-line metering device (Section 3.2.3) with nozzle volume flow rate.

B.2 Apparatus and Procedures

The setup for the calibration of nozzle volume flow rate is shown schematically in Fig. B.1. The volume flow rate standard was an Aalborg rotameter that was connected to the forebody tip with a capture hose. The rotameter featured a tapered glass tube (Aalborg tube number FM034-39) with a stainless-steel spherical float. The tube was graduated with a 150-mm scale which was readable to within ± 0.5 mm and was calibrated by the vendor up to approximately 18,000 cc/min. At its inlet the rotameter was fitted with a needle valve with an orifice diameter of 2.08 mm. The valve remained fully open during the calibration. The capture hose was thick-walled Tygon tubing that fit tightly over the forebody tip and the inlet fitting of the rotameter. A silicon sealant was applied to both ends of the hose to prevent the loss of nozzle flux during the calibration. A soap bubble test did not detect any leakage through the seal. The forebody, delta-wing afterbody, the support system, and the nozzle blowing system were fully assembled for the calibration. The unamplified output voltages of the differential-pressure transducers were measured with separate voltmeters. The calibration was conducted outside the wind tunnel in a setup area.

The output voltages for the port and starboard differential-pressure transducers were measured for volume flow rates between 0 and 140 mm at increments of 10 mm so that each voltage measurement was associated with a volume flow rate listed in the vendor's calibration data for the rotameter. Voltage measurements were taken for increasing flow rates only, and were repeated for each tip configuration described in Table 3.4.

B.3 Results

The calibration data is plotted and tabulated in Fig. B.2. The output voltages for each scale reading are averages of the measurements taken for each tip. The data is plotted with the nozzle volume flow rate appearing on the ordinate axis so that curve fits would produce calibration equations in the form $Q_n = f(V_o)$, where V_o is the output voltage of a differential-pressure transducer. This form of the calibration equations was convenient for data reduction purposes.

The results of the calibration show that the volume flow rate initially increases non-linearly with the output voltage of the pressure transducer (i.e., the pressure across the in-line metering device). At about 22 mV, the variation of the output voltage begins to adopt a linear behaviour indicating choked flow conditions*. For unchoked flow conditions, curve fitting produces a calibration equation that is almost parabolic; for choked flow conditions, the fit is linear. The parameters describing the calibration equations are tabulated in Fig. B.2.

Over most of the range of the calibration, it is observed that the volume flow rate for the port nozzle is slightly lower than that for the starboard nozzle at a given transducer output voltage. The discrepancy is likely due to the difference in the sensitivities of the differential-pressure transducers. Before the blowing system was assembled, the sensitivity of the port transducer was found to be 1.5% higher than the sensitivity of the starboard transducer.

B.4 Assessment of Uncertainty in C_μ

Recalling Eqn. B.2, the uncertainty in Q_n and V_∞^2 are the leading contributors to the uncertainty in C_μ ; the contributions of S_D and A_n are considered negligible. Thus the certainty

*There are two orifices to consider in this setup: at the in-line metering device and the needle valve. Because the diameter of the metering orifice is significantly smaller than that of the needle valve, it can be shown with one-dimensional isentropic flow analysis that choked flow conditions will be governed by the metering device.

in C_μ (U_{C_μ}) is estimated with the expression

$$U_{C_\mu}^2 = \left(\frac{\partial C_\mu}{\partial Q_n} U_{Q_n} \right)^2 + \left(\frac{\partial C_\mu}{\partial V_\infty^2} U_{V_\infty^2} \right)^2 \quad (\text{B.3})$$

where

$$\frac{\partial C_\mu}{\partial Q_n} = \frac{4 Q_n}{V_\infty^2 S_D A_n}, \quad (\text{B.4})$$

$$\frac{\partial C_\mu}{\partial V_\infty^2} = \frac{-2 Q_n^2}{(V_\infty^2)^2 S_D A_n}, \quad (\text{B.5})$$

U_{Q_n} is the uncertainty in the nozzle flow rate, and U_{V_∞} is the uncertainty in the freestream velocity. By substituting Eqns. B.4 and B.5 into Eqn. B.3 and solving for $U_{C_\mu}^2/C_\mu^2$, it can be shown that Eqn. B.3 reduces to

$$\frac{U_{C_\mu}^2}{C_\mu^2} = 4 \left(\frac{U_{Q_n}}{Q_n} \right)^2 + \left(\frac{U_{V_\infty^2}}{V_\infty^2} \right)^2. \quad (\text{B.6})$$

The uncertainty U_{Q_n} is composed of bias (B_{Q_n}) and precision (P_{Q_n}) uncertainty components so that

$$U_{Q_n}^2 = B_{Q_n}^2 + P_{Q_n}^2. \quad (\text{B.7})$$

The bias uncertainty is largely based on the accuracy of the nozzle volume flow rate calibration. An in situ validation with the Aalborg rotameter showed that the accuracy of the volume flow rates derived by the calibration equations was about ± 200 cc/min, i.e., $B_{Q_n} = 200$ cc/min. The precision uncertainty is a measure of the repeatability of measurements and is based on the standard deviation of a sample of measurements:

$$P_{Q_n} = t S_{Q_n}$$

where S_{Q_n} is the sample standard deviation, and t is *Student's t* for the number of degrees of freedom and the desired level of confidence. For a sample of nine measurements of constant nozzle volume flow rate, the standard deviation was found to be 20 cc/min; for

Table B.1. Sample results for the uncertainty in C_μ .

V_∞ (m/s)	C_μ	$\frac{U_{C_\mu}}{C_\mu}$
18.3	1.08×10^{-3}	16%
54.9	4.75×10^{-3}	2.5%

nine samples and a 95% level of confidence, $t = 2.306$. Hence,

$$P_{Q_n} = 2.306 (20 \text{ cc/min}) = 46.12 \text{ cc/min.}$$

Substituting for B_{Q_n} and P_{Q_n} in Eqn. B.7, the uncertainty in the nozzle volume flow rate is $U_{Q_n} = 205.2 \text{ cc/min.}$

The uncertainty $U_{V_\infty^2}$ is based on the uncertainty in the freestream dynamic pressure. From the expression for dynamic pressure

$$q_\infty = \frac{1}{2} \rho_\infty V_\infty^2$$

it can be shown that the uncertainty in V_∞^2 is determined by

$$\frac{U_{V_\infty^2}^2}{V_\infty^2} = \left(\frac{U_{q_\infty}}{q_\infty} \right)^2 - \left(\frac{U_{P_o}}{P_o} \right)^2 - \left(\frac{U_T}{T} \right)^2 \quad (\text{B.8})$$

where P_o is the barometric pressure, and T is the temperature in the test section. For the 2m×3m wind tunnel, $U_{q_\infty} = 0.863 \text{ Pa}$, $U_{P_o} = 100 \text{ Pa}$, and $U_T = 0.1 \text{ deg C}$. Thus, the ratio U_{C_μ}/C_μ can be determined for a given test point by substituting measured data for the quantities Q_n , q_∞ , P_o , and T in Eqns. B.6 and B.8. The results of two sample calculations for the uncertainty in C_μ are presented in Table B.1; these results correspond the lowest and highest nozzle volume flow rates measured in the test (Fig. B.2). The ratio U_{Q_n}/Q_n was found to dominate the uncertainty in C_μ .

Figure B.1. Schematic of nozzle flow rate calibration setup.

Nozzle	Output Voltage Interval (mV)	Calibration Equation, $Q_n = f(V_o)$ $Q_n = a_o V_o^{b_o} + a_1 V_o^{b_1}$			
		a_o	b_o	a_1	b_1
Port	0 – 21.81	1629.1	0.526	0	0
	21.81 – 65	3831.3	0	198.8	1
Starboard	0 – 22.33	1631.9	0.536	0	0
	22.33 – 65	3922.9	0	207.0	1

Figure B.2. Calibration of nozzle volume flow rate against the output voltage of the port and starboard differential-pressure transducers. The results were plotted with nozzle flow rate as the ordinate so that calibration equations for data reduction (appearing in the accompanying table) could be readily derived. In these equations, Q_n is nozzle volume flow rate (cc/min), and V_o represents the differential-pressure transducer output voltage (mV). The calibration data is tabulated on the following page.

Vendor Calibration Data		Differential-Pressure Transducer	
Scale Reading (mm)	Volume Flow Rate (cc/min)	Average Output Voltage (mV)	
		Port	Starboard
0	0	0.000	0.000
10	1100	0.481	0.485
20	2452	2.172	2.148
30	3743	4.765	4.608
40	5072	8.432	8.077
50	6319	13.019	12.372
60	7501	18.203	17.253
70	8676	24.230	22.902
80	9912	30.554	29.007
90	10984	36.952	34.982
100	12241	42.764	40.607
110	13462	48.560	46.104
120	14593	54.107	51.543
130	15695	59.640	56.823
140	16773	64.941	62.105

Figure B.2. Concluded.

Appendix C

Moment Balance Calibration

The procedure used to calibrate the internal strain gauge balance loads all flexures simultaneously with a single-varying load. Through the application of matrix algebra, an inverse sensitivity matrix is produced from the calibration data that relates balance output voltages to forces and moments sensed by the balance. This appendix reviews the formulation of the balance calibration equation, describes the calibration apparatus and procedures, and presents the results and an uncertainty assessment.

C.1 Balance Calibration Equation

A generalized procedure for calibrating an internal, multi-component strain gauge balance is described in Galway (1980). The procedure applies a single-varying load to the balance so that all its flexures are loaded simultaneously. A balance calibration equation, expressed in matrix form, reduces the calibration data to form a sensitivity matrix, the inverse of which transforms the balance output voltages to applied forces and moments. An attractive feature of this approach is the ability to create, with relative ease, a calibration loading spectrum that is representative of the loading expected under test conditions.

A review of Galway's formulation of the balance calibration equation is presented first to understand the fundamentals of this procedure. The output voltage of a strain-gauge bridge of a certain flexure of the balance is proportional to the linear form and non-linear forms of the component load corresponding to the flexure, and also to contributions from the linear form and non-linear forms of the other components loads corresponding to other

flexures. For the i -th balance flexure, this can be represented as a polynomial function of the form,

$$\begin{aligned} R_i = & C_{i,1}F_1 + C_{i,2}F_2 + \dots + C_{i,n}F_n + C_{i,11}F_1^2 + C_{i,22}F_2^2 + \dots + C_{i,nn}F_n^2 \\ & + C_{i,12}F_1F_2 + \dots + C_{i,(n-1)n}F_{(n-1)}F_n \end{aligned} \quad (C.1)$$

where R_i is the bridge output or response voltage of the i -th flexure, n is the number of balance flexures, F_j is the load component applied to the j -th flexure. The sensitivity coefficient ($C_{i,j}$) determines the contribution of a *linear load* (F_1), *load squared* (F_1^2), or *load cross-product* (F_1F_2) to the output voltage R_i . A single-varying load P is introduced in Eqn. C.1 by differentiating with respect to load P :

$$\begin{aligned} \frac{\partial R_i}{\partial P} = & C_{i,1} \frac{\partial F_1}{\partial P} + C_{i,2} \frac{\partial F_2}{\partial P} + \dots + C_{i,n} \frac{\partial F_n}{\partial P} \\ & + 2C_{i,11}F_1 \frac{\partial F_1}{\partial P} + 2C_{i,22}F_2 \frac{\partial F_2}{\partial P} + \dots + 2C_{i,nn}F_n \frac{\partial F_n}{\partial P} \\ & + 2C_{i,12} \left[F_1 \frac{\partial F_2}{\partial P} + F_2 \frac{\partial F_1}{\partial P} \right] + \dots + 2C_{i,(n-1)n} \left[F_{(n-1)} \frac{\partial F_n}{\partial P} + F_n \frac{\partial F_{(n-1)}}{\partial P} \right]. \end{aligned} \quad (C.2)$$

If $P = 0$, then all flexure loads F_i will also be zero (but not $\partial F_i / \partial P$) and:

$$\frac{\partial R_i}{\partial P} = a_i = C_{i,1} \frac{\partial F_1}{\partial P} + C_{i,2} \frac{\partial F_2}{\partial P} + \dots + C_{i,n} \frac{\partial F_n}{\partial P} \quad (C.3)$$

where a_i represents the linear sensitivity of the output voltage R_i to the applied load P . If the point of application of P and its direction are fixed as the applied load is varied, then define

$$f_j = \frac{\partial F_j}{\partial P} = \frac{F_j}{P} \quad (C.4)$$

where f_j , the *component load ratio*, is the fraction of the single-varying load P applied to the j -th flexure, such as

$$f_1 = \frac{\partial F_1}{\partial P} = \frac{Z}{P}, \quad f_2 = \frac{\partial F_2}{\partial P} = \frac{M}{P}$$

and so on. The component load ratios are constant as long as the point of application and the direction of P remains unchanged. Substituting for component load ratio, Eqn. C.3 becomes

$$a_i = C_{i,1}f_1 + C_{i,2}f_2 + \dots + C_{i,n}f_n. \quad (\text{C.5})$$

By differentiating Eqn. C.3 once more with respect to P , it can be shown that the non-linear sensitivity coefficients can be defined by

$$\frac{1}{2} \frac{\partial^2 R_i}{\partial P^2} = b_i = C_{i,11}f_1^2 + C_{i,22}f_2^2 + \dots + C_{i,12}f_1f_2 + C_{i,(n-1)n}f_{(n-1)}f_n \quad (\text{C.6})$$

where b_i is the sensitivity coefficient of the non-linear term. Equations C.5 and C.6 form a set of linear simultaneous equations which can be cast in matrix notation as

$$[a \mid b] = [C1 \mid C2] \begin{bmatrix} L1 & 0 \\ 0 & L2 \end{bmatrix} \quad (\text{C.7})$$

where the submatrices $[C1]$ and $[C2]$ contain the linear and non-linear sensitivity coefficients, respectively; submatrix $[L1]$ contains the linear component load ratios, and $[L2]$ the non-linear component load ratios — essentially these submatrices contain the ‘load cases’, the combinations of component load ratios that describe the loading spectrum. The contributions of the non-linear terms can be ignored if the design loads are not expected to cause a significant deflection of the balance. Since this is the case in the present wind-tunnel tests, Eqn. C.7 reduces to

$$[a] = [C1][L1] \quad (\text{C.8})$$

Thus a linear calibration of an internal balance is a process of forming matrices $[a]$ and $[L1]$. Matrix $[C1]$ is square with a size of five for a five-component balance. The contents of matrix $[a]$ are slopes obtained from linear curve fits of each flexure output voltage with the applied load. Specifically, if

$$R_i = a_iP + r_{oi} \quad (\text{C.9})$$

where r_{oi} is the zero offset of the strain gauge bridge, then by differentiating with respect to P ,

$$\frac{\partial R_i}{\partial P} \approx \frac{\Delta R_i}{\Delta P} = a_i .$$

For each f_i appearing in $[L1]$ there is a corresponding coefficient a_i that must be determined from calibration data.

A minimum of five calibration load cases are required to solve for $[C1]$ (Eqn, C.8) for a five-component balance. However, an optimum solution of the calibration equations is obtained when a large number of calibration loading cases is considered, resulting in an overdetermined system of equations that appear as

$$\begin{matrix} [a] & = & [C1] & [L1] \\ (n,z) & & (n,n) & (n,z) \end{matrix} \quad (C.10)$$

where z is the number of calibration load cases and $z > n$. Galway indicates that an overdetermined system described by Eqn. C.10 can be solved by first multiplying both sides of the equation by the transpose of $[L1]$,

$$[a] [L1]^T = [C1] [L1] [L1]^T .$$

Rearranging to solve for the sensitivity matrix $[C1]$,

$$\begin{matrix} [C1] & = & [a] & [L1]^T & \left[[L1] [L1]^T \right]^{-1} \\ (n,n) & & (n,z) & (z,n) & (n,n) \end{matrix} . \quad (C.11)$$

Equation C.11 is the *balance calibration equation*. In view of this solution, the spectrum of calibration load cases can be selected to be representative of the measured loadings expected during the test.

The steps required to obtain a linear calibration of a multi-component strain gauge balance are as follows:

- (1) Identify a representative selection of calibration load cases and form matrix $[L1]$.

- (2) For each load case, vary the single load P and acquire the bridge output voltage of each balance flexure. With this calibration information, determine the coefficients $a_1, a_2, \dots, a_n$ and form matrix $[a]$.
- (3) Solve Eqn. C.11 for the balance sensitivity matrix $[C1]$.

The inverse of the sensitivity matrix ($[C1]^{-1}$) is used during post-processing to transform the bridge output voltage to applied forces and moments sensed by the internal balance.

C.2 Apparatus

The balance was calibrated in a six-component, auto-levelling, balance calibration rig (Fig. C.1) specifically designed to calibrate an internal strain-gauge balance by the procedure described above. The single-varying load is distributed with the aid of a calibration body mounted on the balance so that pitching, yawing, and rolling moment can be applied simultaneously. Loads were applied hydraulically to the calibration body with a hand-operated pump and were reacted by a rigid frame. Two orthogonally-aligned inclinometers mounted on the calibration body provide sensor-feedback in a closed-loop control system that keeps the balance level in pitch and roll. Levelling ensures that the single-varying load is applied consistently in the vertical direction and prevents a reduction in load caused by the sine of the deflection angle and a possible load interaction arising from the cosine of the deflection angle. The output voltages of each flexure bridge were acquired by a computer. Since sampling was initiated manually by the operator, the load interval between samples was not consistent.

The calibration body was a thick-walled cylinder, drilled both longitudinally and circumferentially at evenly-spaced intervals to accept dowel pins. A loading truss can be yoked onto the calibration body and locked into place with a pair of dowel pins locks. The truss transfers a calibration load, applied at any discrete point along its span, to the calibration body. With this arrangement, it was possible to apply all force and moment components to the balance simultaneously. The axis and sign convention used for the calibration of the balance conformed to that shown for the delta-wing model in Fig. 3.2.

C.3 Procedure

Two factors must be considered when forming a representative calibration loading spectrum: the maximum load expected to be sensed by each component of the balance, and the variety of combinations of loaded balance components (i.e., the contents of matrix $[L1]$). The maximum calibration loads are presented in Table C.1. These loads were not expected to be exceeded at a maximum planned test section velocity of 350 ft/s (107 m/s). The limits for normal force, pitching moment, and rolling moment are based on previous experience with the delta-wing model. Results from Wood and Crowther (1994) were used as guidelines for estimating the limits for yawing moment and side force at a high angle of attack. The model used by Wood and Crowther was also a schematic aircraft featuring a delta-wing with leading-edge extensions, and a forebody with a tangent-ogive profile and semi-apex angle of 14.3 deg. For $\alpha=47$ deg, maximum C_n and C_Y were found to be 0.13 and 0.2, respectively, corresponding to 400 lb-in and 50 lb for a velocity of 350 ft/s (107 m/s).

Nineteen load cases were identified for the calibration of the balance (Table C.2). Of these, seventeen were used to derive the balance sensitivity matrix $[C1]$ and the remaining two cases were used for assessing uncertainty (Section C.5). In Table C.2 the loading of a balance component is described by its load ratio: an inactive balance component is identified by a zero load ratio; for an active component, the load ratio is non-zero. For Load Case 16, for example, all components were active: the balance was rolled 45 deg to equally distribute P between the normal and side force; similarly, the moment due to P was equally distributed between the pitching and yawing moment. With P offset from the centreline of the calibration body, a rolling moment was also applied.

Table C.1. Maximum calibration loads (full scale) for each balance component.

Balance Component	Maximum Load
normal force	600 lb
pitching moment	600 lb-in
side force	300 lb
yawing moment	600 lb-in
rolling moment	600 lb-in

Load Case	Component Load Ratio (f_j)				
	Normal Force, Z/P	Pitching Moment, M/P (inch)	Side Force, Y/P	Yawing Moment, N/P (inch)	Rolling Moment, $\mathcal{L}/P$ (inch)
1	1.0	-2.007	0.0	0.0	0.0
2	0.0	0.0	1.0	-2.007	0.0
3	0.0	0.0	1.0	-1.007	0.0
4	1.0	-1.007	0.0	0.0	0.0
5	1.0	0.993	0.0	0.0	0.0
6	0.0	0.0	1.0	0.993	0.0
7	0.0	0.0	1.0	1.993	0.0
8	1.0	1.993	0.0	0.0	0.0
9	1.0	-0.007	0.0	0.0	0.0
10	1.0	-0.007	0.0	0.0	3.0
11	1.0	-0.007	0.0	0.0	-3.0
12	0.0	0.0	1.0	-0.007	-3.0
13	0.0	0.0	1.0	-0.007	3.0
14	0.0	0.0	1.0	-0.007	0.0
15	0.707107	-0.004950	0.707107	-0.004950	0.0
16	0.707107	-0.004950	0.707107	-0.004950	1.0
17	0.707107	-0.004950	0.707107	-0.004950	-1.0
18	0.707107	1.409264	0.707107	1.409264	-1.0
19	0.707107	-1.419163	0.707107	-1.419163	1.0

Table C.2. Load cases expressed in the form of component load ratios. Cases 1 through 17 inclusive formed matrix $[L1]$ for Eqn. C.11. Note that the component load ratios for force are unitless; for moments, the load ratios have units consistent with moment arm.

The following steps were performed to obtain calibration data for each load case in Table C.2:

- (1) The roll orientation of the balance and the point of application of the single-varying load were set for the specific load case.
- (2) An increasing positive load P was gradually applied to the calibration body until the maximum component loads (Table C.1) were reached or exceeded, then the load was gradually relieved. Throughout this cycle, the output voltages of the six flexure bridges (including both roll bridges) were acquired at roughly regular load intervals.
- (3) Step (2) was repeated for a negative load P .
- (4) Compute the slope of the linear fit and zero offsets for each flexure, i.e., a_i and r_{oi} in Eqn. C.9.

Load Case	Sensitivity of Bridge Output to Load P (mv/volt excitation/lb)				
	Forward Pitch	Aft Pitch	Forward Yaw	Aft Yaw	Forward Roll
1	-0.195×10^{-2}	-0.664×10^{-3}	-0.297×10^{-5}	-0.240×10^{-5}	-0.285×10^{-4}
2	0.173×10^{-5}	0.296×10^{-5}	-0.195×10^{-2}	-0.666×10^{-3}	0.189×10^{-4}
3	0.664×10^{-6}	0.457×10^{-5}	-0.130×10^{-2}	-0.174×10^{-4}	0.130×10^{-4}
4	-0.130×10^{-2}	-0.131×10^{-4}	-0.114×10^{-5}	-0.735×10^{-6}	-0.202×10^{-4}
5	0.371×10^{-5}	0.129×10^{-2}	0.226×10^{-5}	0.234×10^{-5}	-0.400×10^{-5}
6	0.158×10^{-6}	0.775×10^{-5}	0.260×10^{-5}	0.128×10^{-2}	0.624×10^{-6}
7	-0.769×10^{-6}	0.843×10^{-5}	0.654×10^{-3}	0.193×10^{-2}	-0.540×10^{-5}
8	0.655×10^{-3}	0.194×10^{-2}	0.395×10^{-5}	0.413×10^{-5}	0.457×10^{-5}
9	-0.647×10^{-3}	0.638×10^{-3}	0.310×10^{-6}	0.132×10^{-5}	-0.114×10^{-4}
10	-0.616×10^{-3}	0.635×10^{-3}	-0.739×10^{-5}	0.232×10^{-4}	0.131×10^{-2}
11	-0.676×10^{-3}	0.643×10^{-3}	0.842×10^{-5}	-0.205×10^{-4}	-0.133×10^{-2}
12	-0.283×10^{-4}	0.977×10^{-5}	-0.639×10^{-3}	0.613×10^{-3}	-0.131×10^{-2}
13	0.304×10^{-4}	0.846×10^{-6}	-0.655×10^{-3}	0.656×10^{-3}	0.133×10^{-2}
14	0.102×10^{-5}	0.596×10^{-5}	-0.648×10^{-3}	0.632×10^{-3}	0.713×10^{-5}
15	-0.457×10^{-3}	0.456×10^{-3}	-0.458×10^{-3}	0.448×10^{-3}	-0.183×10^{-5}
16	-0.447×10^{-3}	0.454×10^{-3}	-0.461×10^{-3}	0.456×10^{-3}	0.438×10^{-3}
17	-0.467×10^{-3}	0.457×10^{-3}	-0.456×10^{-3}	0.441×10^{-3}	-0.440×10^{-3}

Table C.3. Sensitivity of flexure bridge output voltage to the single-varying load P . The contents of this table formed matrix $[a]$ in Eqn. C.11.

An example of calibration data for a load case is tabulated and plotted in Fig. C.2. The coefficients a_i for all calibration load cases are tabulated in Table C.3.

C.4 Results

Having formed matrices $[a]$ and $[L1]$, the sensitivity matrix $[C1]$ was computed according to Eqn. C.11. Since only one roll flexure is necessary to resolve the rolling moment — unlike the pitch and yaw axes for which two flexures are needed to resolve force and moment — two sensitivity matrices were determined: one was based on the forward roll bridge, the other was based on the aft roll bridge. The sensitivity matrix with the forward roll bridge was selected to be used for the test because the error in the recovered rolling moment was smallest. The results for the sensitivity matrix and the inverse sensitivity matrix appear in Tables C.4 and C.5 respectively. An interaction between the balance components is evident in the coefficients of the sensitivity matrix. The sensitivity of a

Bridge Output	Sensitivity of Bridge Output to Load Component (mv/volt excitation/lb or mv/volt excitation/lb-in)				
	Normal Force	Pitching Moment	Side Force	Yawing Moment	Rolling Moment
Forward Pitch	-6.426×10^{-4}	6.515×10^{-4}	6.915×10^{-7}	-5.499×10^{-7}	9.905×10^{-6}
Aft Pitch	6.432×10^{-4}	6.513×10^{-4}	5.787×10^{-6}	1.412×10^{-6}	-1.402×10^{-6}
Forward Yaw	4.593×10^{-7}	1.724×10^{-6}	-6.439×10^{-4}	6.518×10^{-4}	-2.696×10^{-6}
Aft Yaw	1.079×10^{-6}	1.613×10^{-6}	6.378×10^{-4}	6.499×10^{-4}	7.171×10^{-6}
Forward Roll	-1.098×10^{-5}	8.228×10^{-6}	7.459×10^{-6}	-6.096×10^{-6}	4.396×10^{-4}

Table C.4. Coefficients of the balance sensitivity matrix [C1] using the forward roll bridge.

bridge to its corresponding load component is at least two orders of magnitude greater than other components. For instance, the sensitivity of the aft pitch bridge to normal force and pitching moment is on the order 10^{-4} , whereas its sensitivity to side force, yawing moment and rolling moment is on the order of 10^{-6} . The exception is the sensitivity of the forward roll bridge to normal force, for which there is only an order of magnitude difference. For the most part, the degree of interaction is small and considered satisfactory for this balance.

C.5 Uncertainty Assessment

C.5.1 Uncertainty in Measured Forces and Moments

The total uncertainty (U_F) in the loads measured with the balance is composed of bias uncertainty (B_F) and precision uncertainty (P_F) such that

$$U_F^2 = B_F^2 + P_F^2. \quad (\text{C.12})$$

Load Component	Sensitivity of Load Component to Bridge Output (lb/mv/volt excitation or lb-in/mv/volt excitation)				
	Forward Pitch	Aft Pitch	Forward Yaw	Aft Yaw	Forward Roll
Normal Force	-777.860	777.896	2.132	-4.298	20.091
Pitching Moment	768.160	767.160	3.213	-4.377	-14.770
Side Force	0.701	-0.353	-779.315	781.341	-17.541
Yawing Moment	-0.931	-2.905	764.536	771.893	-7.891
Rolling Moment	-33.862	5.068	23.819	-2.578	2275.822

Table C.5. Coefficients of the inverse sensitivity matrix $[C1]^{-1}$ using the forward roll bridge.

Coleman and Steele (1995) suggest that the bias uncertainty can be estimated by

$$B_F^2 = \sum_{i=1}^n \theta_i^2 B_i^2 + 2 \sum_{i=1}^{n-1} \sum_{k=i+1}^n \theta_i \theta_k B_{ik} \quad (C.13)$$

$$B_{ik} = \sum_{\alpha=1}^L (B_i)_{\alpha} (B_k)_{\alpha} \quad (C.14)$$

where θ_i is the sensitivity of load component F_i to the output voltage R_i of the i^{th} flexure bridge; B_i is the bias uncertainty in the output of the i^{th} flexure bridge; and L represents the number of bias error sources that are shared by the output voltages R_i and R_k . The second term in Eqn. C.13 represents the influence of correlations which may arise between the bias uncertainties of flexure bridge outputs that are dependent on each other. The term B_{ik} accounts for these correlations and is approximated satisfactorily with Eqn. C.14 (Coleman and Steele, 1995). The precision uncertainty of the measurement result is determined by

$$P_F^2 = \sum_{i=1}^n \theta_i^2 P_i^2 \quad (C.15)$$

$$P_i = 2S_i$$

Table C.6. The results of an estimate of balance measurement uncertainty.

Load Component	Uncertainty (% of FS)		
	Bias B_F	Precision P_F	Total U_F
Normal Force	0.15	0.15	$0.21 \approx 0.20$
Pitching Moment	0.15	0.15	$0.21 \approx 0.20$
Side Force	0.28	0.28	$0.39 \approx 0.40$
Yawing Moment	0.14	0.14	$0.19 \approx 0.20$
Rolling Moment	0.23	0.23	$0.33 \approx 0.35$

where S_i is the sample standard deviation, and $2S_i$ is the 95% confidence precision for a large sample (greater than 10 samples). Coleman and Steele assume there are no correlated precision uncertainties. Substituting for P_i^2 , Eqn. C.15 reduces to

$$P_F^2 = 4 \sum_{i=1}^n \theta_i^2 S_i^2 = 4S_F^2 \quad (\text{C.16})$$

where S_F is the standard deviation for the load component.

The assessment of the bias and the precision uncertainties for the balance requires the measurement of a static load over many samples. Measurements for the specific purpose of assessing uncertainty, however, were not taken in this investigation. To obtain a reasonable estimate of the uncertainty, the alternative was to use the voltage measurements of the calibrated loads, such as those tabulated in Fig. C.2. As mentioned earlier, two calibration loads cases (Load Cases 18 and 19 in Table C.2) were omitted from the formation of the balance inverse sensitivity matrix for the purpose of estimating uncertainty. With each load case consisting of 36 measurements of various applied loads, it is thought a reasonable estimate of the bias uncertainty can be obtained by taking an average of the bias errors computed for the measured loads. An estimate of the precision uncertainty is not possible in these circumstances because the repeatability of measurement cannot be evaluated for a varying static load. Instead, the precision uncertainty is estimated to be equivalent to the bias uncertainty.

If the voltage measurements in the calibration data are regarded as appropriate standards by which the bias uncertainty in the flexure bridge outputs (B_i) can be estimated, then B_i is the difference between the measured output voltage for an applied load and the output voltage predicted by the product of the balance sensitivity matrix (Table C.4) and the applied component loads. With B_i for each flexure bridge, Eqns. C.13 and C.14 were evaluated to produce B_F^2 for each load component. For Eqn. C.14, $L=1$ because there was only one bias error source that was shared by the flexure bridges, namely the single-varying load P . Results for B_F are presented in Table C.6 as a percentage of the corresponding full-scale load listed in Table C.1. These results are taken from Load Case 19 because they represent the highest averages for B_F .

C.5.2 Uncertainty in the Force and Moment Coefficients

An aerodynamic force coefficient is defined as

$$C_F = \frac{F}{q_\infty S}$$

where F represents the aerodynamic force, q_∞ is the freestream dynamic pressure, and S represents the reference area. For aerodynamic moment coefficients a reference length also appears in the denominator. The uncertainty in C_F (U_{C_F}) is estimated with

$$U_{C_F}^2 = \left(\frac{\partial C_F}{\partial F} U_F \right)^2 + \left(\frac{\partial C_F}{\partial q_\infty} U_{q_\infty} \right)^2 \quad (\text{C.17})$$

where

$$\frac{\partial C_F}{\partial F} = \frac{1}{q_\infty S}, \quad \frac{\partial C_F}{\partial q_\infty} = \frac{-F}{q_\infty^2 S} \quad (\text{C.18})$$

and the uncertainties in the reference area and length are assumed to be negligible. By substituting Eqns. C.18 into Eqn. C.17, it can be shown that Eqn. C.17 reduces to

$$\frac{U_{C_F}^2}{C_F^2} = \left(\frac{U_F}{F} \right)^2 + \left(\frac{U_{q_\infty}}{q_\infty} \right)^2.$$

The uncertainties for measured aerodynamic loads (U_F) are listed in Table C.5.1 as ratios of full-scale load. For the 2m×3m wind tunnel $U_{q_\infty}=0.863$ Pa and for a freestream velocity of 18.3 m/s the ratio $U_{q_\infty}/q_\infty=4.3\times 10^{-3}$; for 54.9 m/s the ratio is smaller by one order of magnitude. Clearly, the uncertainty in U_{q_∞} has little impact on U_{C_F} . Therefore,

$$\frac{U_{C_F}}{C_F} \approx \frac{U_F}{F} ,$$

i.e., the uncertainties stated for balance measurements (Table C.5.1) also apply to the aerodynamic coefficients.

Figure C.1. General setup of the six-component, auto-levelling balance calibration rig. A typical balance calibration setup is shown with all balance flexures loaded simultaneously (Bowker et al., 1982). This rig is located in the High-Speed Aerodynamics Laboratory at IAR/NRC.

Figure C.2. An example of the calibration data acquired (Load Case 16) is tabulated and plotted. A least square fit of the data for each flexure yields the linear coefficient a_i and voltage offset.

Appendix D

Data Reduction Equations

This appendix describes the equations implemented in the data reduction program to compute aerodynamic loads and the standard deviation of a sample point in the ensemble-average record.

D.1 Computation of Aerodynamic Loads

An aerodynamic force or moment (L_i) is a linear combination of the net output voltages from the balance:

$$L_i = \sum_{j=1}^n c_{ij} V_{s,aero_j} \quad (D.1)$$

where n is the number of balance flexures, $V_{s,aero_j}$ is the net output voltage of the bridge for the j -th flexure, and c_{ij} is a coefficient of the inverse sensitivity matrix that denotes the contribution of $V_{s,aero_j}$ to L_i . The net output voltage is derived from

$$V_{s,aero} = V_{s,wind} - V_{s,tare} \quad (D.2)$$

where $V_{s,wind}$ and $V_{s,tare}$ represent respectively the wind and tare voltages at source. The voltage at source (V_s) is the actual output voltage of a Wheatstone bridge on the balance recovered from the voltage measured (V_m) at the A/D converter on the IBIM. The recovery of V_s for a balance channel is a process of reversing the effect of amplification and voltage offset as follows:

$$V_s = \frac{V_m - V_{offset}}{G \times G_o} \quad (D.3)$$

where V_{offset} is the offset voltage, G is the variable gain, and G_o is the fixed gain for the balance channel. V_{offset} and G were determined by the electrical calibration of the data acquisition system (Section 3.5.1) and were stored with G_o in both the wind and tare raw data files.

The pitching and yawing moments were transferred from the balance resolving centre to the moment reference point on the model by

$$L_{M_{mr}} = L_{M_{rc}} - L_F \times l_{offset} \quad (D.4)$$

where $L_{M_{mr}}$ is the aerodynamic moment about the model reference point and $L_{M_{rc}}$ is the moment with respect to the resolving centre; L_F is the aerodynamic force associated with $L_{M_{rc}}$, and l_{offset} is the distance between the balance resolving centre and the moment reference point. The model moment reference point, which corresponds to 30% of the mean aerodynamic chord, was located 4.7 mm behind the balance resolving centre (Fig. 3.2).

The aerodynamic forces and moments were non-dimensionalized with the constants $q_\infty S_w$ and $q_\infty S_w l$, respectively, where q_∞ is the freestream dynamic pressure, S_w is the wing reference area, and l is a reference length. The wing span is the reference length for yawing and rolling moment; for pitching moment, the reference length is the mean geometric chord of the wing (Fig. 3.2).

Equation D.3 was also applied to the measured output voltage of the differential-pressure transducers of the forebody blowing system for the recovery of nozzle volume flow rate.

D.2 Sample-Point Standard Deviation

A standard deviation was calculated for each of the 512 sample points that compose the ensemble-average record of a non-dimensionalized aerodynamic load. For each sample

point, the standard deviation reflects the degree of variability in the data that contributed to the particular average. Because the instantaneous records that form the ensemble-average record were not stored, it is not possible to compute the sample-point standard deviation based on a time-series of the contributing data. Instead, variance theorems (Gnedenko, 1967; Scheaffer and McClave, 1990) are invoked to estimate the sample-point variance (hence the sample-point standard deviation) based on the variance of the voltage measured at source. The constituents required to compute the variance of a measured voltage are stored during the data acquisition cycle.

From Eqn. D.1 it follows that

$$\text{Var}(L_i) = \sum_{j=1}^n c_{ij}^2 \text{Var}(V_{s,aero_j}) + 2 \sum_{j < k} c_{ij} c_{ik} \text{Cov}(V_{s,aero_j}, V_{s,aero_k}) \quad (\text{D.5})$$

where the symbols Var and Cov represent variance and covariance respectively. In Eqn. D.5 all $V_{s,aero}$ are considered to be dependent random variables. This is the case with the balance due to the interaction of the balance components, which is evident by the non-zero, off-diagonal elements in the inverse sensitivity matrix (Table C.5). Otherwise, the covariance term in Eqn. D.5 reduces to zero if all $V_{s,aero}$ are independently random*.

In the first term of Eqn. D.5, the variance of an aerodynamic signal is derived from Eqn. D.2 as follows:

$$\begin{aligned} \text{Var}(V_{s,aero}) &= \text{Var}(V_{s,wind} - V_{s,tare}) \\ &= \text{Var}(V_{s,wind}) + \text{Var}(V_{s,tare}) . \end{aligned} \quad (\text{D.6})$$

*Covariance, defined in Eqn. D.12, assesses the relationship between two variables, X_1 and X_2 , that are caused to vary by a common source. X_1 and X_2 will have positive covariance if X_1 and X_2 tend to be large or small simultaneously. X_1 and X_2 will have negative covariance if X_1 tends to be small while X_2 is large, and vice versa (Scheaffer and McClave, 1990).

The variance of the voltage at source is based on Eqn. D.3 and appears as

$$\begin{aligned}\text{Var}(V_s) &= \text{Var}\left(\frac{V_m - V_{offset}}{G \times G_o}\right) \\ &= \text{Var}\left(\frac{V_m}{G \times G_o}\right) + \text{Var}\left(\frac{V_{offset}}{G \times G_o}\right).\end{aligned}\quad (\text{D.7})$$

The first term of Eqn. D.7 involves multiplication by a constant, so

$$\text{Var}\left(\frac{V_m}{G \times G_o}\right) = \text{Var}(A V_m) = A^2 \text{Var}(V_m) \quad (\text{D.8})$$

where $A = 1/(G \times G_o)$. The second term of Eqn. D.7 is a collection of constants for which the variance is zero. Thus, Eqn. D.7 reduces to

$$\text{Var}(V_s) = A^2 \text{Var}(V_m). \quad (\text{D.9})$$

After substituting Eqn. D.9 for the variance of the wind and the tare signals, Eqn. D.6 becomes

$$\text{Var}(V_{s,aero}) = A_{wind}^2 \text{Var}(V_{m,wind}) + A_{tare}^2 \text{Var}(V_{m,tare}). \quad (\text{D.10})$$

The variance of measured voltage is computed as

$$\text{Var}(V_m) = \frac{1}{n-1} \left(\sum_{i=1}^n V_i^2 - n \bar{V}^2 \right) \quad (\text{D.11})$$

where $\sum V_i^2$ is the sum of the squares of the measured sample-point voltage; $\bar{V}$ is the average sample-point voltage; and n represents the number of instantaneous records that are acquired and contribute to the ensemble-average record. The data acquisition process produces $\sum V_i^2$ and $\bar{V}$ for each sample point of every channel. These quantities were stored in both the wind and tare raw data files.

The computational form of the covariance term in Eqn. D.5 is

$$\text{Cov}(X_j, X_k) = \overline{x_j x_k} - \mu_j \mu_k \quad (\text{D.12})$$

where x_j and x_k represent the actual numerical values of two random variables, and μ_j and μ_k are their means. In terms of balance data, the mean values μ_j and μ_k are the ensemble-averages of a sample point for the j -th and k -th balance components. These mean values are readily available. The difficulty arises with the term $\overline{x_j x_k}$ which is the average product of the instantaneous output of the j -th and k -th balance components. This meant that ten additional summations — of a product between channels — must be performed during the data acquisition cycle to fully implement the second term of Eqn. D.5. Although the IBIM processor is designed to perform simple calculations repetitively on each channel, the demands of Eqn. D.12 require the incorporation of logic control in the data acquisition program. The ramifications of the additional operations are twofold: first, a significant reduction of the sampling rate during the data acquisition process was anticipated; second, an additional 5120 memory locations would have been necessary for each balance component because the term $\overline{x_j x_k}$ also requires ten accumulators for every sampling point. Most of the memory on the IBIM had already been allocated and the data acquisition system had operated satisfactorily. Thus, expecting a degradation of performance of the data acquisition system, the summations required for the covariance calculations were omitted. Since the covariance calculation was incomplete and presumably meaningless without the term $\overline{x_j x_k}$, it was decided to assume zero covariance (thus implying the balance components behave independently). Hence, the variance of an aerodynamic load was determined from a reduced form of Eqn. D.5:

$$\text{Var}(L_i) = \sum_{j=1}^n c_{ij}^2 \text{Var}(V_{s,aero_j}) . \quad (\text{D.13})$$

In turn, the variance of a non-dimensionalized aerodynamic load (C_{L_i}) is

$$\text{Var}(C_{L_i}) = B^2 \text{Var}(L_i) \quad (\text{D.14})$$

where $B = 1/(q_\infty S_w)$ or $1/(q_\infty S_w l)$ depending on whether L_i is a force or moment. The sample-point standard deviation for C_{L_i} is the square root of the result produced by Eqn. D.14.

An accurate calculation of variability was ultimately viewed as unnecessary since the purpose of calculating the variance in this investigation was to obtain an indication of the relative unsteadiness of the aerodynamic forces and moments. The omission of the covariance term in Eqn. D.5 was therefore considered acceptable. Equally acceptable, the variance transformation for pitching and yawing moment did not include the transfer of these moments from the balance resolving centre to the model reference point, as described by Eqn. D.4, because the effect of l_{offset} on the result of the transformation was considered negligible.

Equations D.6, D.10, D.11, D.13, and D.14 were implemented in the data reduction program to compute the standard deviation of a sample point in the ensemble-average record.

Appendix E

Geometric Transformation Equations

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 (Beyers et al., 1984; Lamont and Kennaugh, 1991).

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 Fig. 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 the coordinate systems coincide so that $\alpha = \sigma$ and $\phi = 0$ deg. As yaw increases the total incidence plane rotates about the freestream velocity vector causing σ to increase above α , and the roll angle to increase. The transformation from (α, β) to (σ, ϕ) is described by the 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, σ is the turntable angle. The transformation from (σ, ϕ) to (α, β) is

accomplished with the equations

$$\sin(\beta) = \sin(\sigma) \cdot \sin(\phi) \quad (\text{E.3})$$

$$\tan(\alpha) = \tan(\sigma) \cdot \cos(\phi) . \quad (\text{E.4})$$

In Table E.1, equivalent (α, β) are shown for the range of (σ, ϕ) considered in the investigation.

Figure E.1. Relation between the total incidence plane and the body-axis system (Lamont and Kennaugh, 1991).

σ (deg)	$\phi = \pm 5$ deg		$\phi = \pm 10$ deg		$\phi = \pm 15$ deg		$\phi = \pm 20$ deg	
	α	β	α	β	α	β	α	β
15	14.9	± 1.3	14.8	± 2.6	14.5	± 3.8	14.1	± 5.1
20	19.9	± 1.7	19.7	± 3.4	19.4	± 5.1	18.9	± 6.7
25	24.9	± 2.1	24.7	± 4.2	24.2	± 6.3	23.7	± 8.3
30	29.9	± 2.5	29.6	± 5.0	29.1	± 7.4	28.5	± 9.8
35	34.9	± 2.9	34.6	± 5.7	34.1	± 8.5	33.3	± 11.3
40	39.9	± 3.2	39.6	± 6.4	39.0	± 9.6	38.3	± 12.7
45	44.9	± 3.5	44.6	± 7.1	44.0	± 10.5	43.2	± 14.0
50	49.9	± 3.8	49.6	± 7.6	49.0	± 11.4	48.2	± 15.2
55	54.9	± 4.1	54.6	± 8.2	54.1	± 12.2	53.3	± 16.3
60	59.9	± 4.3	59.6	± 8.6	59.0	± 13.0	58.4	± 17.2
65	64.9	± 4.5	64.7	± 9.1	64.2	± 13.6	63.6	± 18.1
70	69.9	± 4.7	69.7	± 9.4	69.0	± 14.1	68.8	± 18.7

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