

Wind Tunnel Studies on Rotational Effects
in Lightly-Iced Transmission Line Galloping

by

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Abstract

Overhead transmission lines are prone to undergo large amplitude, low frequency vibrations when exposed to freezing rain and steady side winds. These vibrations are referred to as galloping. They involve a dominant vertical motion in addition to twisting and horizontal swaying. Field reports indicate that the majority of galloping cases are associated with lightly-iced lines with thin ice accretions. Previous studies have failed to explain this trend satisfactorily. The present thesis involves a series of wind tunnel experiments to understand the rotational effects in lightly-iced transmission line galloping. The work to restore and upgrade the wind tunnel used for the experiments are also reported.

Aerodynamic loads are measured first on a stationary model of a short, representative section of a lightly-iced conductor. Subsequently, automated controls force the model to undergo rotational oscillations, and the aerodynamic loads measured from these dynamic tests are compared to the stationary results. The airflow in both sets of experiments is visualised by using a laser-based system. The stationary test shows that the well-established den Hartog criterion for predicting vertical galloping does not explain why lightly-iced lines gallop. The dynamic experiments however confirm the presence of rotation-induced lift, unaccounted for by quasi-steady theory and the den Hartog criterion. This additional lift force increases the coupling between the rotational and vertical directions and may promote coupled aerodynamic instability. Visualisations indicate that the surface irregularities of the ice and the rotational motion are jointly responsible for the rotation-induced lift forces observed in the aerodynamic measurements.

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List of Nomenclature

A_y :	Peak vertical galloping amplitude, m
A_θ :	Peak rotational galloping amplitude, degrees
C_D :	Normalised aerodynamic drag coefficient
C_L :	Normalised aerodynamic lift coefficient
C_M :	Normalised aerodynamic moment coefficient
d :	Bare conductor diameter, m
d_p :	Nominal seeding particle diameter, m
D :	Aerodynamic drag, N
D' :	Uncompensated aerodynamic drag strain reading, $10^{-6}/\text{N}$
f_y :	Vertical galloping frequency, Hz
f_N :	Nyquist frequency, Hz
f_θ :	Rotational galloping frequency, Hz
$\{F\}$:	Compensated aerodynamic load vector containing side force, drag, and yawing moment elements
$\{F'\}$:	Uncompensated aerodynamic load vector containing side force, drag, and yawing moment elements
$[K]$:	Compensation matrix for removing cross-coupling in the aerodynamic balance readings
K_{ij} :	Individual element of the aerodynamic balance compensation matrix
$[K_{NN}]$:	Aerodynamic balance compensation matrix for a negative lift and negative yawing moment case
$[K_{NP}]$:	Aerodynamic balance compensation matrix for a negative lift and positive yawing moment case

- $[K_{PN}]$: Aerodynamic balance compensation matrix for a positive lift and negative yawing moment case
- $[K_{PP}]$: Aerodynamic balance compensation matrix for a positive lift and yawing moment case
- h_{LTS} : Inside height of the wind tunnel's lower test section, measured at 53.34cm
- k_d : Derivative gain of the updated wind tunnel speed controller
- k_i : Integral gain of the updated wind tunnel speed controller
- k_p : Proportional gain of the updated wind tunnel speed controller
- l : Length of the iced-conductor wind tunnel model, m
- L : Aerodynamic lift, N
- M : Aerodynamic moment, Nm
- M' : Uncompensated aerodynamic moment strain reading, $10^{-6}/N$
- r : Characteristic radius used in the quasi-steady modelling of rotational galloping, m
- Re : Reynolds Number, defined here as $V d / \nu$
- SF : Aerodynamic side force, N
- SF' : Uncompensated aerodynamic side force strain reading, $10^{-6}/N$
- Sk : Stokes Number, defined here as τ_p / τ_f
- t : Time, s
- T_θ : Period of rotational oscillation, s
- U : Reduced or normalised wind speed, defined here as $V / f_y d$
- U_{rel} : Relative reduced or normalised wind speed, defined here as $V / f_y A_y$
- V : Free-stream wind speed, m/s
- V_c : Centerline wind speed in the lower test section, m/s

V_{eff} :	Effective wind speed experienced by a galloping cross-section, m/s
w_{LTS} :	Inside width of the wind tunnel's lower test section, measured at 76.2cm
$\dot{y}$:	Vertical speed of a galloping cross-section, m/s
α :	Angle of attack in quasi-steady testing, degrees
α_0 :	Initial angle of attack in forced-vibration testing, degrees
α_{eff}	Effective angle of attack experienced by a galloping cross-section, degrees
Δt	Discrete time interval in a path-controlled motion, s
ε	Estimate of turbulence dissipation used to calculate the Kolmogorov time scale, m^2/s^3
θ	Rotational position of the model in forced-vibration testing, degrees
θ_i :	i^{th} discrete rotational position of the model in forced-vibration testing, degrees
θ_{i+1} :	$(i+1)^{th}$ discrete rotational position of the model in forced-vibration testing, degrees
$\dot{\theta}$:	Rotational speed of the model in forced-vibration testing, degrees / s
$\dot{\theta}_i$:	i^{th} discrete rotational speed of the model in forced-vibration testing, degrees / s
$\dot{\theta}_{i+1}$	$(i+1)^{th}$ discrete rotational speed of the model in forced-vibration testing, degrees / s
$\ddot{\theta}$:	Rotational acceleration of the model in forced-vibration testing, degrees / s^2
$\ddot{\theta}_i$:	i^{th} discrete rotational acceleration of the model in forced-vibration testing, degrees / s^2

- $\ddot{\theta}_{i+1}$ $(i+1)^{\text{th}}$ discrete rotational acceleration of the model in forced-vibration testing, degrees / s²
- ν : Kinematic viscosity of air, taken here as $1.56 \times 10^{-5} \text{m}^2/\text{s}$
- ρ_f : Density of air, taken here as 1.16kg/m^3
- ρ_p : Density of oil for seeding particles, taken here as 917kg/m^3
- τ_f : Characteristic time scale of a flow, s
- τ_K : Kolmogorov or smallest turbulence time scale of a flow, s
- τ_p : Seeding particle response time in a flow, s

1 INTRODUCTION AND REVIEW OF TRANSMISSION LINE GALLOPING

1.1 Introduction

Wind-induced vibrations have received considerable attention from engineers and researchers alike due to the complexity of the problem and its practical implications. Iced transmission lines, bridge decks and cables, and many other types of structures are all susceptible to wind-induced vibrations. Galloping is a specific type of wind-induced motion that is often associated with overhead transmission lines, when freezing rain causes ice to form along the span of a transmission line. In the presence of sustained side winds, an iced-conductor becomes aerodynamically unstable which leads to large vertical vibrations. Documented cases of iced transmission line galloping show that vertical peak-to-peak (p-p) amplitudes can reach as high as 15m, with frequencies of about 0.1 to 0.5 Hz [1]. These large sustained galloping motions may damage transmission towers and supporting hardware, disrupt electrical service, and result in loss of revenue.

In addition to a dominant vertical motion, galloping transmission lines also have a tendency to sway horizontally, up to about 3m (p-p) [2]. Furthermore, galloping lines tend to twist up to about 40 degrees, with a rotational frequency from about 1 to 8 times the vertical galloping frequency [3,4]. These rotational motions may significantly change the aerodynamic loads acting on a galloping line, although their role in the galloping mechanism is not yet well understood. For example, all anti-galloping strategies are only partially effective [5] and, in some cases, actually promote galloping rather than suppress it [6]. Without a firm understanding of the aerodynamics that cause and sustain galloping, it is unlikely that a robust and cost-effective control strategy can be developed.

1.2 Background

Detailed data for cases of natural iced transmission line galloping in the field are not available. The ice shape and twisting motion of a single galloping conductor line are very difficult to observe, let alone measure from the ground. In the absence of detailed field data, general visual reports and full-scale field tests have played an important role in identifying the characteristics of iced transmission line galloping. North American field reports collected over the last half of the 20th century show two important trends: lightly-iced lines, or lines with a maximum ice thickness of 10% or less of the bare conductor's diameter, are the most prone to gallop [1] and steady side winds of about 5-15m/s are the most common wind speeds for galloping [7,8]. Of these two trends, the former is the most significant as transmission line galloping associated with thin ice shapes has not yet been explained satisfactorily. The present thesis addresses this issue by focusing solely on galloping associated with lightly-iced transmission lines. Although recent field testing has shown that non-perpendicular winds may also cause galloping [8], only the most common case of a perpendicular wind, normal to the conductor is considered here.

Wind tunnel studies of iced transmission line galloping have been used by many investigators [9-13] as an alternative to expensive full-scale field testing. Unlike a full-scale outdoor test line, where the wind speed and direction cannot be controlled, a wind tunnel can provide controlled wind conditions. However, due to the limited size of wind tunnels, large turbulence scales inherent to atmospheric wind cannot be reproduced. Testing in both atmospheric wind and low-turbulence wind tunnels have shown that large scale turbulence effects (100m) have little effect on the aerodynamic loads in galloping

[14]. Such scales are several orders of magnitude larger than a typical conductor's diameter (0.03m) [14]. Furthermore, Tunstall [15] suggests that grid-generated wind tunnel turbulence, characterised by length scales having the same order of magnitude as the conductor diameter, is less representative than uniform, low-turbulent flow conditions for studying galloping in wind tunnels. Based on these findings, the scope of the present work is limited to wind tunnel studies involving uniform, low-turbulent airflow.

Similar to size limitations for the scale of turbulence, most wind tunnels can accommodate only a relatively short section of a transmission line. Despite this limitation, Novak [16] has shown that the vertical galloping response of a rigid, spring-supported model is representative of a finite section of a continuous span. To complement spring-supported models, several studies [17,18,19] of the flutter of bridge-decks have also used forced-vibration models. Then controlled motors are used to oscillate a wind tunnel model in a steady manner whilst studying the aerodynamic loads acting on the moving model. Such forced-vibration models are suitable for studying the aerodynamics associated with steady-state galloping or flutter after initiation of the instability has occurred. In contrast, spring-supported models are useful for examining the initiation of galloping from rest. Both types of models have their own advantages and limitations, but almost all wind tunnel studies on transmission line galloping have relied on the spring-supported method to study the problem. The present wind tunnel study uses the forced-vibration method to extend and complement existing findings from spring-supported models.

1.3 Literature Review

The galloping problem has been studied since about the start of the electric power industry so that an extensive body of literature exists. A more detailed review of the literature is needed here for three important reasons: to summarise previous work, provide more focus for the present work, and avoid the needless repetition of previous studies. The following review is not exhaustive but it covers mainly experimental investigations on iced transmission lines and rectangular bridge-decks. Emphasis is placed on the common deficiencies in previous work and how the present experiments address these deficiencies. As most studies focus on either vertical or rotational effects in galloping or flutter, the survey is organised into the following subsections; 1-degree-of-freedom (DOF) vertical galloping, 1-DOF rotational galloping or flutter, and 2-DOF rotational and translational galloping. The most important points from the literature review are summarised.

1.3.1 1-DOF Vertical Galloping

Based on the work of Glauert, den Hartog [20] was the first person to explain the vertical galloping behaviour of smooth, idealised structures such as rectangular and semi-circular (D-section) cylinders. The study was based solely on steady, time-averaged lift and drag forces acting on a stationary model. By plotting the static lift and drag forces versus angle of attack, den Hartog found a simple criterion for predicting if vertical galloping may initiate from rest: “A section is dynamically unstable if the negative slope of the lift curve is greater than the ordinate of the drag curve [21].” The general approach

of using static aerodynamic forces to model or predict galloping instability is commonly referred to as the quasi-steady theory. The theory implies that a moving galloping structure experiences the same aerodynamic loads as a stationary one providing they both experience the same effective angle of attack and wind speed at any instant.

At the time (in 1932) of den Hartog's original paper, the rotational motions involved in galloping transmission lines had not yet been identified and hence, rotational effects were not considered. Despite these simplifications, den Hartog's explanation of the galloping mechanism is still used to explain the galloping of lightly-iced transmission lines. Some studies [22,10] purport to show that lightly-iced conductor profiles do not satisfy den Hartog's criterion for predicting galloping, while others contest that such profiles do [15,23]. This disagreement provides further evidence that the aerodynamic mechanism behind lightly-iced transmission line galloping is still not well understood. Therefore additional work on the aerodynamic aspects of the problem is certainly warranted.

Parkinson and Smith [24] refined the work of den Hartog much later. They used the quasi-steady theory to predict the 1-DOF vertical galloping behaviour of a spring-supported rectangular cylinder. Nakamura and Mizota [25] and Washizu [26] also showed that, for 1-DOF vertical galloping, the quasi-steady theory is limited to high normalised or reduced wind speeds, above about 20, where the effects of vortex-shedding and vortex-induced vibrations are negligible. The normalised or reduced wind speed, U , is defined as

$$U = \frac{V}{f_y d}, \quad (1.1)$$

where V is the steady free-stream wind speed, f_y is the vertical galloping frequency, and d is a characteristic length scale or diameter of the galloping structure's cross-sectional shape. For a typical transmission line with a bare conductor diameter, d , of 30mm, with $V=8\text{m/s}$, and $f_y=0.2\text{Hz}$, gives a reduced wind speed of $U \sim 1333$. This result is about two orders of magnitude higher than the limit of 20, and suggests that the influence of vortex-induced vibrations in lightly-iced transmission line galloping is unimportant.

Despite the result of the previous simple calculation, some researchers have argued that vortex-shedding plays an important role in galloping. For example, based on the work of Bearman and Currie [27] on bridge deck flutter, Zdero et al. [28] proposed that a more appropriate non-dimensional parameter for iced transmission line galloping is a relative reduced wind speed, U_{rel} , defined as

$$U_{rel} = \frac{V}{f_y A_y}, \quad (1.2)$$

where A_y is the vertical galloping amplitude. However, Bearman and Currie's original study involved tests with circular cylinders, much lower normalised wind speeds (10-20), and higher vertical frequencies (1-5 Hz) than those of transmission lines. With these parameters, vortex-synchronisations and sub-harmonic lock-in phenomena take place. However such tests are not representative of galloping transmission lines. This specific example illustrates how too often the results from studies on bridge decks are directly and inappropriately applied to transmission-line galloping. Studies on the flutter of bridge

decks may serve as an useful guide on transmission line galloping, but key differences exist between the two phenomena.

A common factor in almost all studies of 1-DOF vertical galloping is that smooth models having sharp corners, such as rectangular and D-section cylinders, are used. Such ideal models are unrepresentative of a typical lightly-iced conductor shape. Tunstall [15] has shown that smooth, ideal wind tunnel models have fundamentally different aerodynamic characteristics than “naturally” iced conductors. The latter have irregular and rough surface conditions due to icing and conductor stranding. To obtain representative experimental results, the wind model for the present work is selected from a set of realistic plaster of Paris replicas of iced conductor samples created in an outdoor freezing rain simulator at the University of Manitoba [22]. The replica icing profiles are full-scale in cross-section and they resemble the limited number of natural ice shapes collected in the field [4,15]. Realistic wind speeds and galloping frequencies are also employed to match all the appropriate non-dimensional parameters, including the normalised or reduced wind speed and the Reynolds Number, $Re = Vd / \nu$, where ν is the kinematic viscosity of air.

1.3.2 1-DOF Rotational Galloping

In comparison to 1-DOF vertical galloping, only a few studies have been conducted on 1-DOF rotational galloping or flutter. This may be due to the more complicated aerodynamic nature of the problem and its controversial role in galloping. Indeed, the corresponding aerodynamic moment is very small in magnitude and, hence, it

is usually difficult to measure experimentally [29]. As early as 1950, however, Cheers [30] suggested that rotational instability may be much more influential in iced transmission galloping than the vertical den Hartog mechanism. The influence of difficult to observe rotations on the field galloping of iced transmission lines still remains unresolved. However, the extension of the quasi-steady theory to include a rotational DOF for a lightly-iced conductor profile requires further discussion and consideration due to its possible important role.

Recall from the previous discussion on 1-DOF vertical galloping, that the quasi-steady theory implies that a stationary and a moving galloping structure experience the same aerodynamic loads if they have the same effective angle of attack, α_{eff} , and wind speed, V_{eff} . As shown in Figure 1.1(a), for the simple case of 1-DOF vertical galloping, the effective wind speed and angle of attack depend on the vertical speed of the galloping structure, $\dot{y}$, and the actual wind speed, V . Similarly, when applying the quasi-steady theory to rotational galloping, the effective wind speed and angle of attack are assumed to depend on three parameters: a characteristic radius, r , the galloping or fluttering structure's angular speed, $\dot{\theta}$, and the actual wind speed, V [31]. Several numerical models [32,33,34] for simulating iced transmission line galloping use this approach and they arbitrarily choose a characteristic radius as a means to extend the quasi-steady theory to the rotational DOF. Experiments are needed to verify this modelling approach for the galloping of lightly-iced transmission lines.

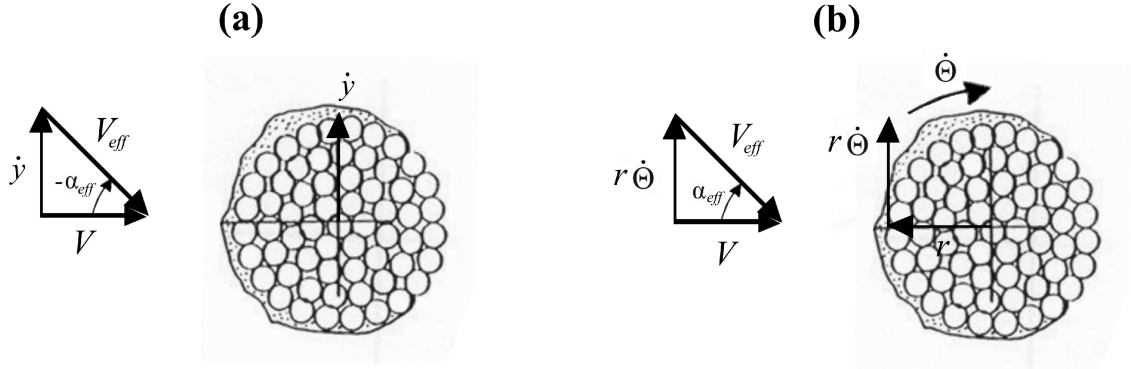


Figure 1.1. Quasi-steady modelling for (a) 1-DOF vertical and (b) 1-DOF rotational galloping.

Wind tunnel tests involving spring-supported models [35], forced-vibration models [18], or both [29,36], have been conducted to study the aerodynamic mechanism of purely rotational galloping or flutter. The models used in these studies, however, are unrepresentative of naturally iced conductors. Their cross-sectional shapes were rectangular cylinders having sharp corners, or cylinders having perfectly smooth, ideal “ice” shapes. Despite the use of these smooth, ideal wind tunnel models, experimental results on rotational galloping suggest that unsteady aerodynamic effects may be important even though such effects are neglected in the quasi-steady theory [35,29]. Hence, the results for ideal cross-sectional shapes call into question the validity of the quasi-steady theory for modelling rotational effects. Similar experiments involving more realistic lightly-iced conductor models would be useful to determine the importance of unsteady aerodynamic and rotational effects in lightly-iced transmission line galloping.

In addition to the previous experimental work on rotational galloping, a relatively recent numerical study [37] has proposed that aerodynamic coupling effects caused by

rotations play a central role in lightly-iced conductor galloping. Note that the quasi-steady theory neglects such coupling effects as they do not occur in any equivalent, static situation. To model the unsteady lift force caused by the rotational motion of a galloping conductor, McComber and Paradis [37] used the results from traditional studies on spinning cylinders [38]. However, empirical relations for a steadily spinning, smooth cylinder may not be representative of a rocking oscillation likely in galloping. Despite this discrepancy, the study raises important questions on the aerodynamic nature of the coupling between the rotational and vertical motions. Such questions are best answered with representative experiments rather than numerical simulations based on hypothetical empirical relations.

1.3.3 Coupled Rotational and Translational Galloping

Almost all wind tunnel studies on multi-DOF galloping or flutter have used spring-supported models. For example, by restraining a model's rotational or translational motion, Scanlan and Tomko [39] showed that six flutter derivatives can be measured and used to characterise the difference between the flutter of bridge decks and aircraft wings. Nigol and Buchan [11] conducted a similar study for various iced conductor samples, including a lightly-iced shape. Instead of using Scanlan's flutter derivative approach, however, they simply examined the individual initial stabilities of iced conductors for purely vertical, rotational, or unrestrained motion. Nigol and Buchan concluded that iced conductors are stable in the vertical direction and that a self-excited rotational motion is a prerequisite for most cases of iced transmission line galloping [11].

Unlike Scanlan's work on bridge deck flutter, Nigol and Buchan's study on transmission line galloping ignored key issues such as the effects of aerodynamic coupling. The present work is able to address these issues by using a forced-vibration model for rotations. The forced-vibration method is able to produce steady oscillations on command, thereby avoiding the strict damping requirements and inherent complexities of restraining spring-supported models.

1.4 Summary of Literature Review and Objective

The aerodynamic mechanism for the simple case of purely 1-DOF vertical galloping is well understood. However, disagreement exists in the literature on the need for lightly-iced conductors to satisfy the den Hartog instability criterion for vertical galloping. Furthermore, spring-supported, iced-conductor models, which are restrained to only move vertically, show that such profiles are stable and do not gallop. On the other hand, several studies have suggested that additional rotational effects play an important role in the vertical galloping of lightly-iced transmission lines. In contrast to vertical galloping, the aerodynamic mechanisms involved in rotational galloping are not yet well understood. Previous wind tunnel experiments that focused on 1-DOF rotational galloping have used ideal models that are not representative of lightly-iced conductors. Moreover, most studies on transmission line galloping have used only spring-supported models. The objective of this thesis is to examine the effect and role of rotations in lightly-iced transmission line galloping. This objective is achieved by conducting representative wind tunnel experiments using a forced-vibration method with a realistic

plaster replica of a lightly-iced conductor model.

1.5 Organisation of Thesis

The first chapter of the thesis has provided an introduction, background, and literature review. The previous discussions give an overview of the galloping problem, and the scope and objective of the thesis. Chapter 2 summarises the essential actions taken to prepare, restore, and upgrade the wind tunnel and instrumentation for the planned experiments. Details of the wind tunnel experiments and methodology are presented in Chapter 3. Chapter 4 presents the experimental results and a discussion of these results, respectively, while Chapter 5 concludes the thesis. Recommendations for future work are given in Chapter 6.

2 RESTORATION OF WIND TUNNEL AND INSTRUMENTATION

2.1 Introduction

Significant work was done from July 2007 to September 2009 to recommission a previously dismantled low-speed wind tunnel and upgrade its instrumentation. The restoration of the wind tunnel and instrumentation was by no means a singular effort; all persons involved are noted in the acknowledgement section of this thesis. The following discussion summarises the results of the restoration process, especially the calibration of the wind tunnel test section and the aerodynamic balance.

2.2 Calibration of the Wind Tunnel Test Section

The elevation of the low-speed wind tunnel is shown in Figure 2.1. The wind tunnel is a return-circuit type, powered by two counter-rotating fans driven by hydraulic motors. All the tunnel's corner sections feature turning vanes, and straighteners are located before and after the fan section. The upper section is free of flow-conditioning devices in order to accommodate any future studies involving large models. The tunnel's main flow-conditioning apparatus is located just upstream of the lower octagonal test section. It includes a 5:1 contraction cone and two identical screens. The lower test section's cross-sectional area increases gradually along the streamwise or axial direction to account for boundary layer growth. The range of wind speeds in the upper section and lower test section are about 0-5m/s and 0-30m/s, respectively. Figure 2.2 shows the inside dimensions of the upper and lower test section.

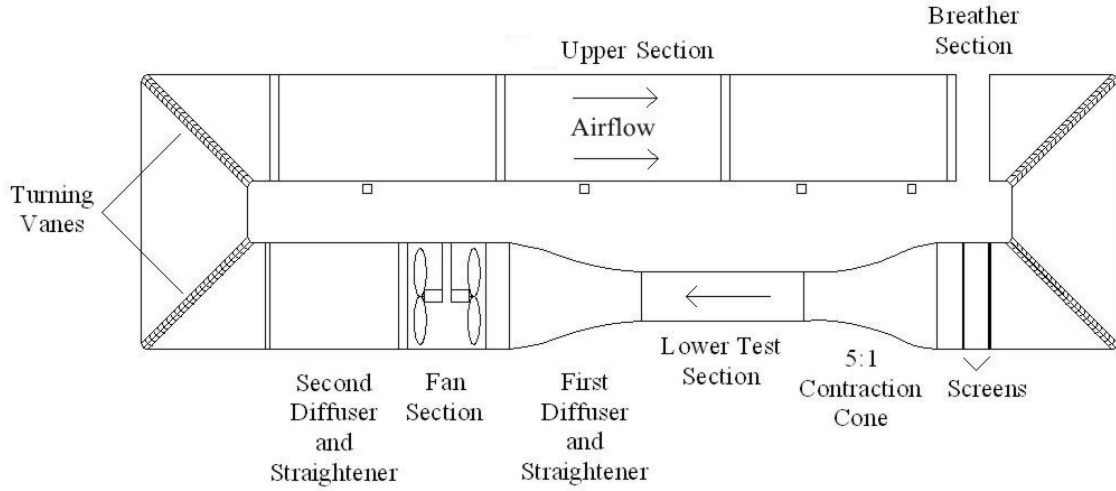


Figure 2.1. Side elevation of the low-speed wind tunnel

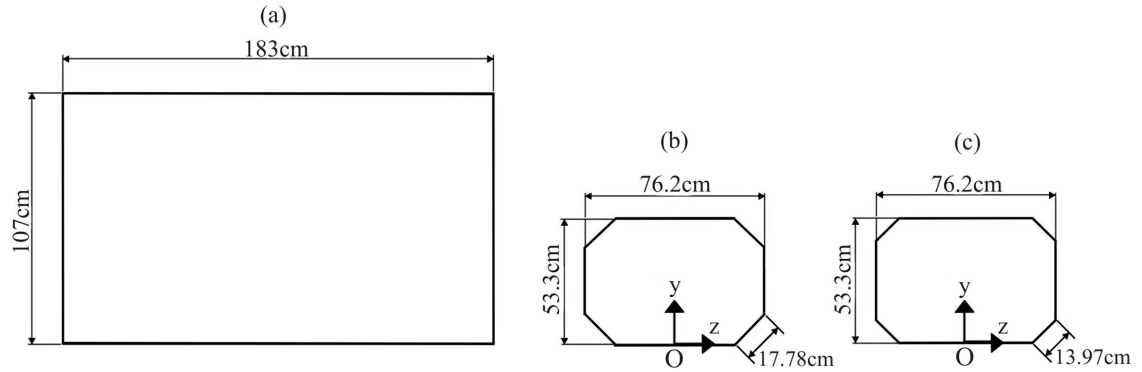


Figure 2.2. Dimensions of the wind tunnel's (a) upper section, and lower test-section (b) inlet and (c) outlet.

After the wind tunnel was reassembled, the upper section was refinished and the mean flow in the lower test section was checked by using a simple Pitot-static tube and projection micro-manometer. The Pitot-static tube's diameter was 2.2mm and the accuracy of the micro-manometer was 0.05mm of water pressure. Two winds speed of

about 8 and 23m/s were examined. The accuracy of the micro-manometer resulted in a sufficiently low measurement error of about 0.07% or less in the wind speed data. For each wind speed, three sets of measurements across the width of the tunnel were taken near the inlet and outlet of the test section. For each of the six measurement sets, the Pitot-static tube was traversed vertically across the test section. As the Pitot-static tube approached the wind tunnel floor, finer traverse steps were taken to give a better profile of the boundary layer in the lower test section. Note that the tube could not be positioned directly at the top inner surface of the test section due to the branched design of the Pitot static-tube.

Figures 2.3 and 2.4 show the measured streamwise velocity profiles at 51.4cm from the inlet and outlet of the lower test section, respectively, for a centerline wind speed, V_c , of about 7.4m/s. Figures 2.5 and 2.6 show the corresponding measured velocity profiles for a centerline wind speed of 22.9m/s. Note that in Figures 2.3 though 2.6, a reference datum corresponding to zero velocity was added at the origin, O, where $y = 0.0\text{cm}$ to preserve the no-slip boundary condition. Further, the data are plotted in non-dimensional form, where the height of the Pitot-static tube's tip is measured from the origin, O, shown in Figure 2.2, and normalised by the inside height of the lower test section, h_{LTS} , measured at 53.34cm (21.0"). The z-location of the Pitot-static tube is normalised by the inside width of the test-section, w_{LTS} , measured at 76.2cm (30.0").

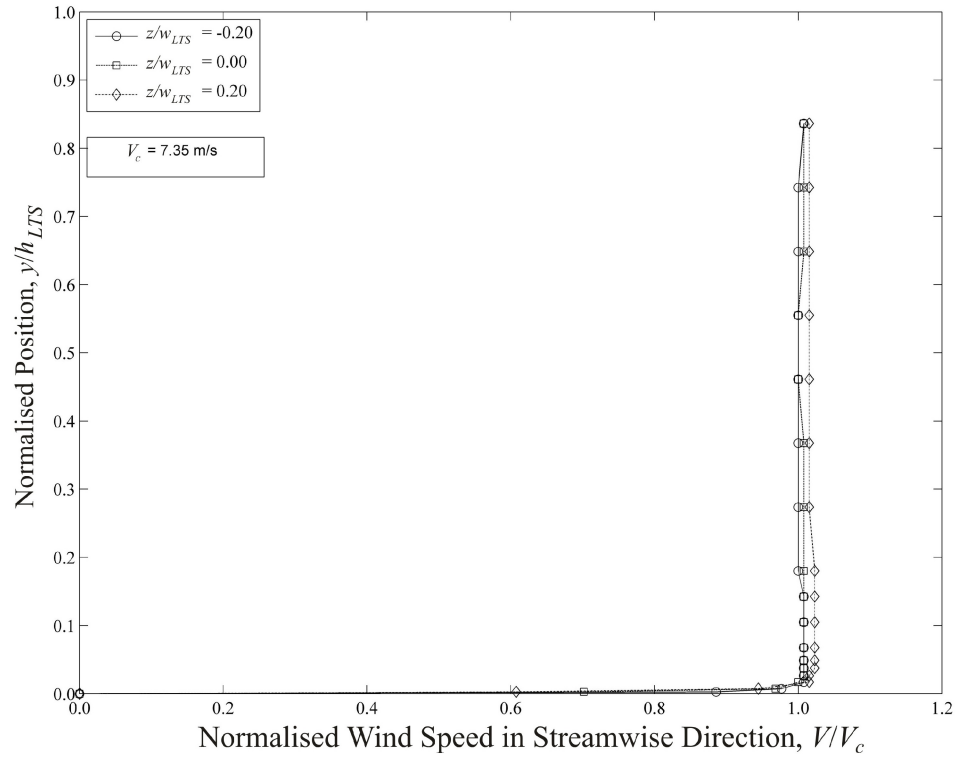


Figure 2.3. Airflow velocity profiles at 51.4cm from the lower test section inlet, measured with a centerline wind speed of 7.35m/s.

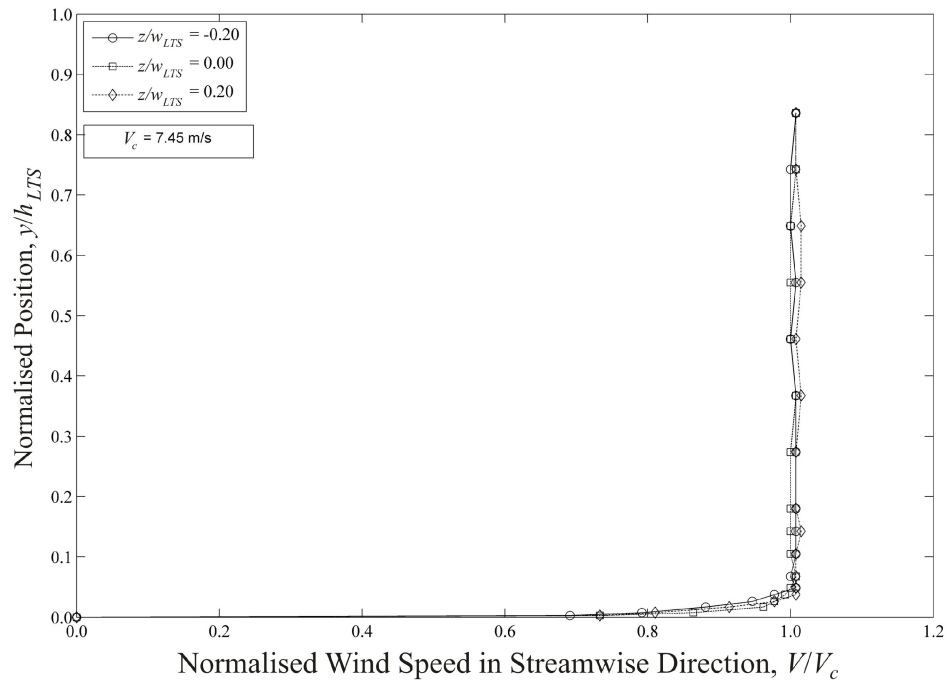


Figure 2.4. Airflow velocity profiles at 51.4cm from the lower test section outlet, measured with a centerline wind speed of 7.45m/s.

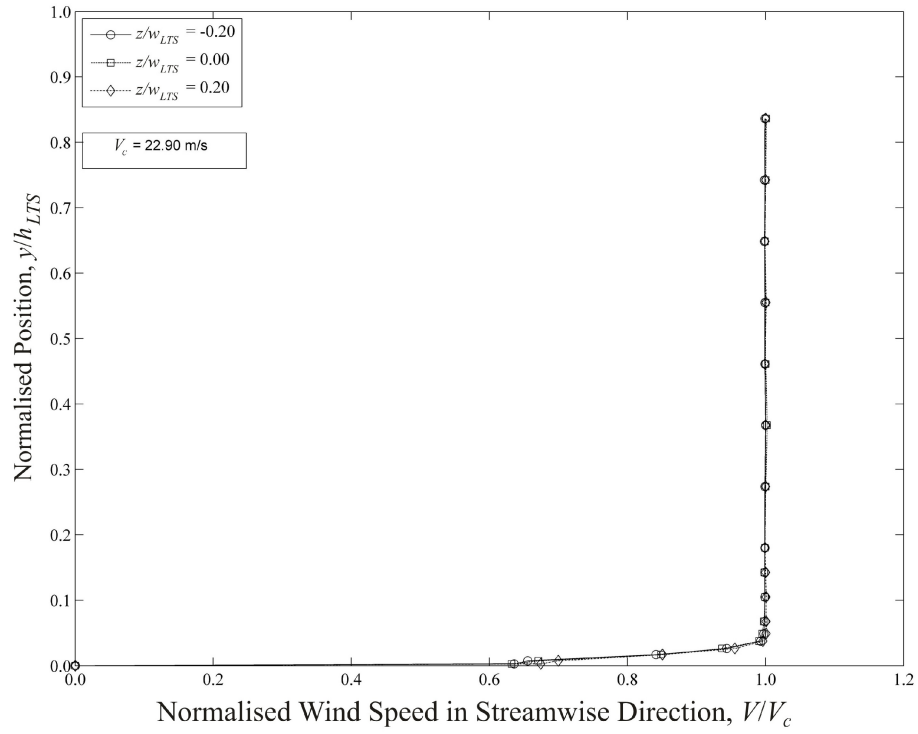


Figure 2.5. Airflow velocity profiles at 51.4cm from the lower test section inlet, measured with a centerline wind speed of 22.90m/s.

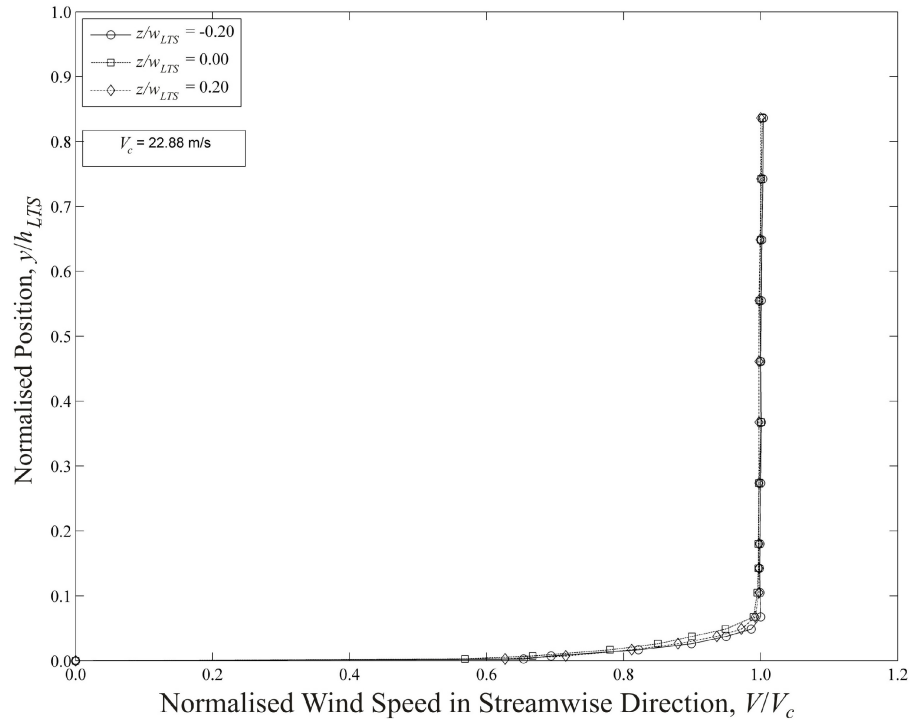


Figure 2.6. Airflow velocity profiles at 51.4cm from the lower test section outlet, measured with a centerline wind speed of 22.88m/s.

Distinct boundary layer and uniform free-stream regions can be seen in the velocity profiles given in Figures 2.3 and 2.4. The boundary layer is limited to a region approximately 2.5cm (1") from the test section's floor. Comparing Figures 2.3 and 2.4, and also Figures 2.5 and 2.6, the boundary layer is slightly thicker near the outlet of the test section than near the test section's inlet. This result is somewhat expected as the boundary layer growth along the test section's floor is restrained, although not eliminated completely, by the test section's gradual increase in cross-sectional area from the inlet to the outlet, as seen in Figure 2.2. Considering the free-stream region of the test section, the velocity variations at 8 and 23m/s are less than about 1.5 and 0.2%, respectively, in both the horizontal and vertical directions. These velocity profiles are acceptable for the present research and future teaching activities.

2.3 Wind Speed Controller

The existing wind tunnel controller for the low-speed wind tunnel was developed originally to account automatically for changes in blockage in the test section [13]. Blockage refers to the phenomenon of air speeding up around a wind tunnel model in a closed test section due to the test section's finite cross-sectional area [40]. For example, consider a wind tunnel model such as a thin airfoil. As the airfoil is rotated to large angles of attack, the model's projected area that "blocks" the test section increases and, due to the Conservation of Mass, the airflow must also increase in speed around the model. The wind speed controller can measure this increase in wind speed from a pressure transducer monitoring the static pressure drop across the contraction cone. The

controller system uses a stepper motor attached to a hydraulic valve to adjust the hydraulic flow to the fans in order to maintain a constant wind speed at the test section's inlet.

A diagram of the existing wind speed controller system is shown in Figure 2.7. The wind tunnel operator must set a desired wind speed by using the pressure transducer's zero-offset potentiometer. This method of setting the wind speed is both tedious and undesirable as the pressure transducer's zero-offset potentiometer is not designed to be adjusted frequently. Furthermore, overshoot behaviour and steady-state errors are inherent to a closed-loop control system using only a proportional control scheme [13]. The system's upgrades in 2009 address these problems. A diagram of the updated controller is shown in Figure 2.8, where a personal computer is used to add a convenient user interface and provide proportional, integral, and derivative (PID) control. The PID gain factors, k_p , k_i , and k_d , can also be tuned and adjusted easily with a personal computer.

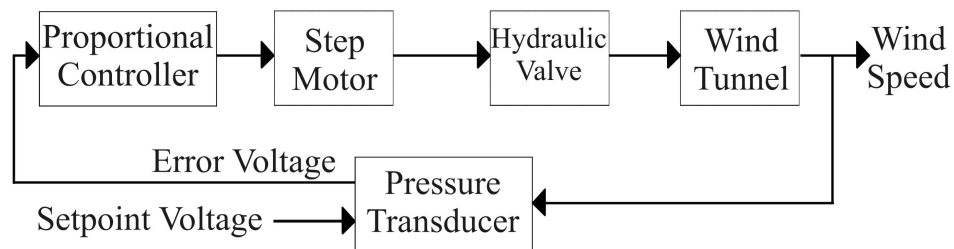


Figure 2.7. Block diagram of the existing wind speed controller.

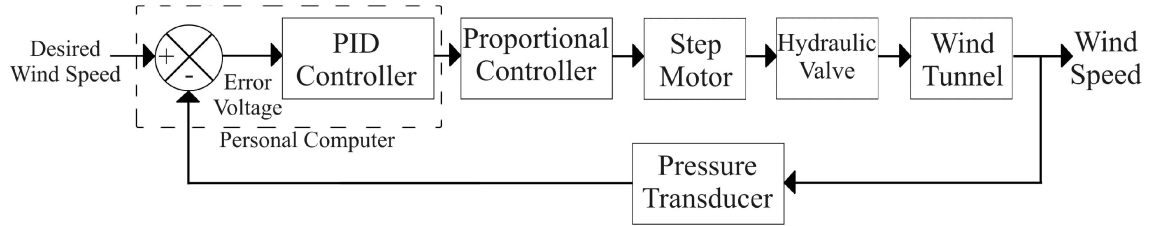


Figure 2.8. Block diagram of the updated wind speed controller.

From several preliminary trials to tune the upgraded controller, PID gains of $k_p=0.012$, $k_i=0.0001$, and $k_d=0.25$ yield the best performance. The results of two final tests using relatively large step inputs of +4 and -8m/s for the change in wind speed are shown in Figures 2.9 and 2.10. These step changes for the wind speed are much larger than those caused by typical changes in blockage, and represent a rigorous test for the new controller system. The results in Figures 2.9 and 2.10 indicate that the wind speed controller's performance has improved as no overshoot is seen and the steady-state error is reduced from $\pm 5\%$ in the original configuration to about $\pm 2\%$, when taking the desired wind speed as a reference. Note that a PID controller is capable theoretically of eliminating the steady-state error completely. In practice, however, noise introduced from the pressure transducer's excitation source and the reaction time of the wind tunnel causes a $\pm 2\%$ steady-state error that cannot be eliminated. Replacing the power supply for the pressure transducer may slightly decrease this error. However, such action is not needed presently.

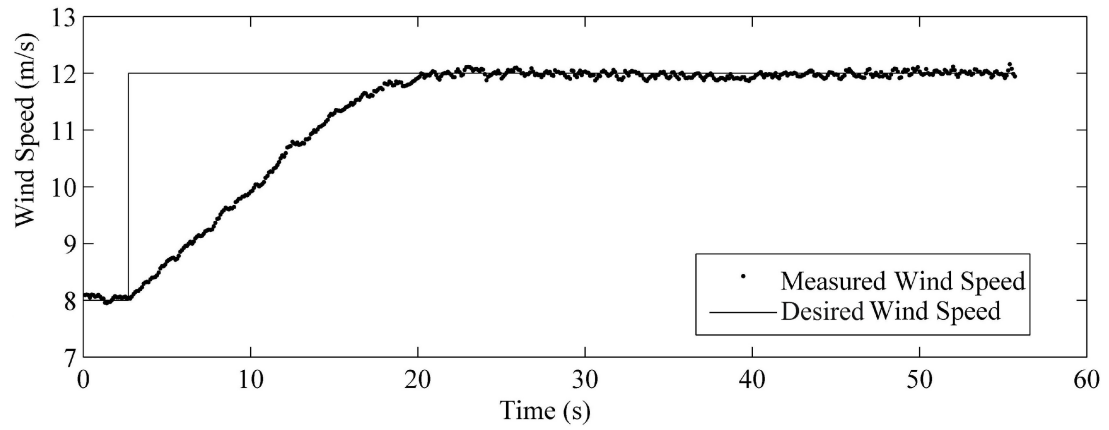


Figure 2.9. Performance of the updated wind speed controller when responding to a 4m/s increase in the desired wind speed.

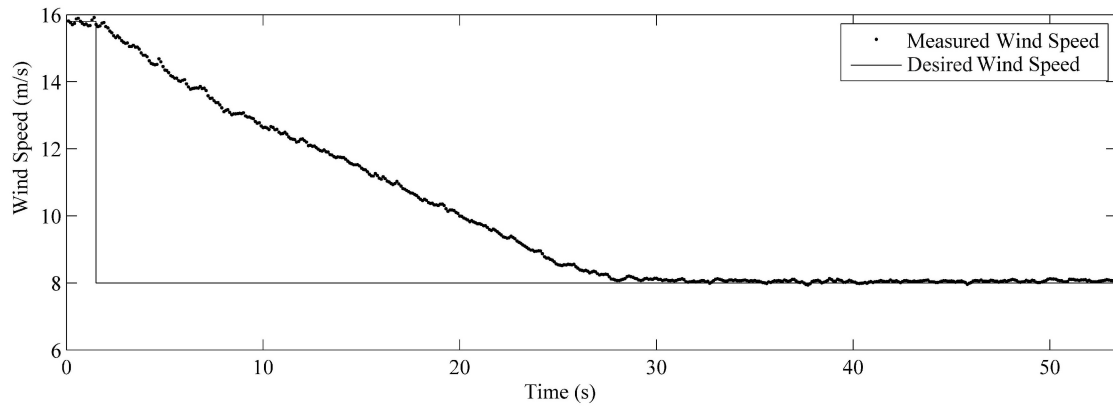


Figure 2.10. Performance of the updated wind speed controller when responding to a 8m/s decrease in the desired wind speed.

2.4 Aerodynamic Balance

Shortly before construction began in the Engineering Complex, the wind tunnel's six-component Aerolab aerodynamic balance needed attention. In addition, the data acquisition system used with the balance was based on a significantly outdated personal computer, which limited the signal processing capabilities. The balance required significant restoration efforts: new strain gauges, a new data acquisition system, and new automated controls for the planned forced-vibration experiments on rotational galloping. The actions taken to restore, align, and calibrate the balance are summarised next. As a very limited amount of literature and documentation existed for the laboratory's aerodynamic balance, the following sections provide a guideline for future wind tunnel operators charged with maintaining the balance and performing alignment as well as calibration procedures. Additional materials such as the wind tunnel's safe operating procedure and software are included as digital attachments with this thesis.

2.4.1 Strain Gauge Selection

New strain gauges were selected for the load cells of the aerodynamic balance based on the guidelines provided in Vishay Micro-Measurements' Technical Note TN-505-4 [41]. Several helpful recommendations were also made by T. Rummage of Vishay-Micro-Measurements [42]. Transducer-class gauges (Model J2A-S038M-350) having a resistance of 350Ω were chosen to replace the existing 120Ω gauges in order to increase the load cells' sensitivity. Gauges with a slightly narrower grid were also selected to reduce averaging error, although this choice also made installing the gauges

slightly more tedious and challenging. Constantan alloy was chosen for the gauges' foil due to its overall combination of desired properties, including a relatively high gauge factor of about 2 and self-temperature-compensation [41]. High performance and elevated-temperature curing M-Bond 610 adhesive was recommended [42] for bonding the strain gauges to the load cells' beams.

2.4.2 Strain Gauge Installation

The installation of the strain gauges began by scraping off the old strain gauges from the load cells' beams and cleaning the beams with a wire brush. The beams' were prepared for the new gauges by following the instructions outlined in Vishay Micro-Measurements Application Note B-129-8 [43]. The beams were degreased by using 99% isopropyl alcohol, sanded and polished, and scribed very lightly with a pin to locate the edges of the new gauges. Conditioner and neutraliser were applied before setting the gauges and solder pads in position using adhesive and Mylar tape, as shown in Figure 2.11. The setting procedure is also outlined in Vishay Micro-Measurements Instruction Bulletin B-130 [44]. Finally, the gauges were covered with Teflon tape and silicon gum pads and clamped using small C-clamps as shown in Figure 2.12. Note that spring-loaded clamps were tried before using the C-clamps. The spring-loaded clamps could not provide enough clamping pressure during the elevated-temperature curing cycle which resulted in several ruined strain gauges.

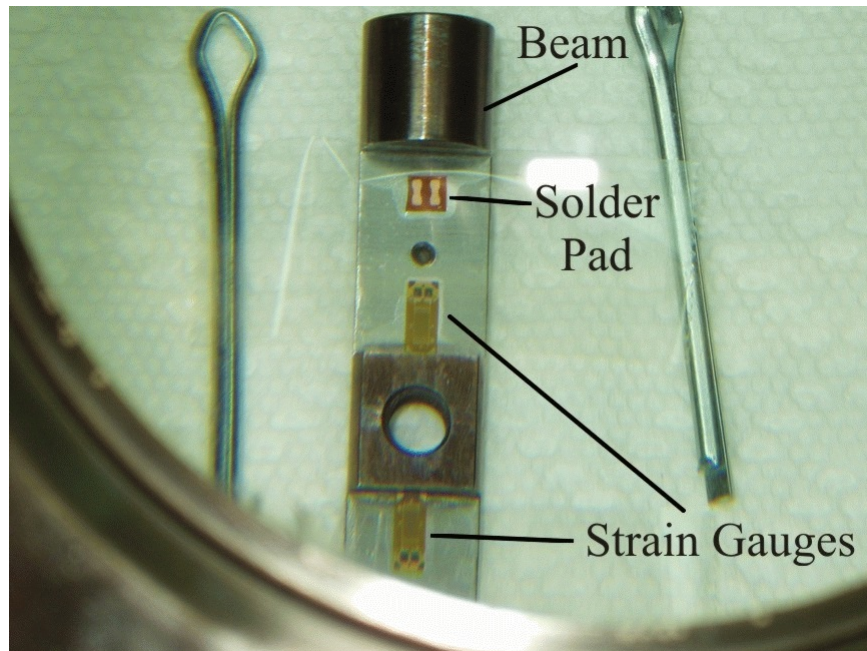


Figure 2.11. Strain gauges and solder pads held in place using Mylar tape.

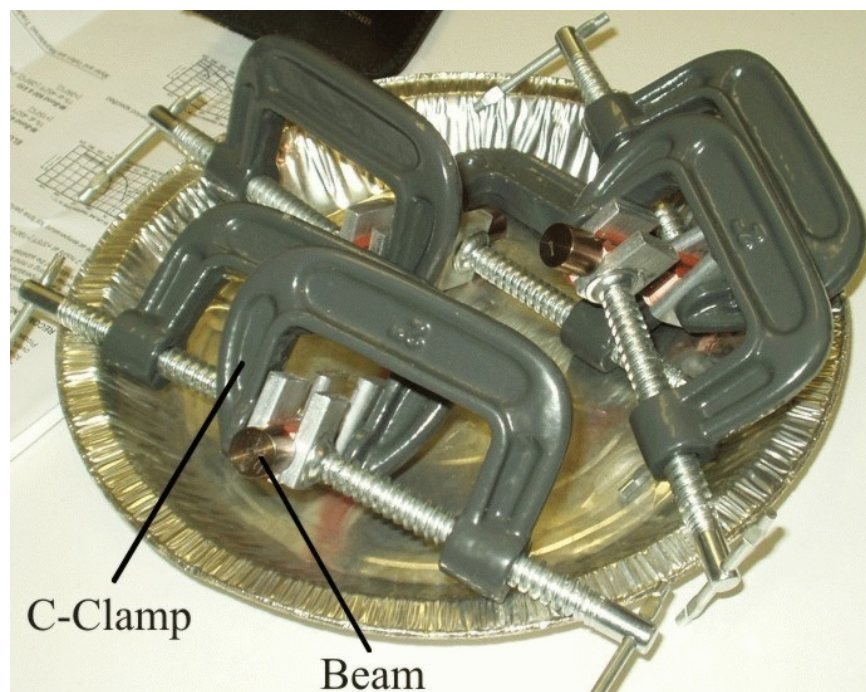


Figure 2.12. Strain gauges clamped on the load cells' beams.

Solder pads were used in each load cell to provide a common soldering point for wires. The pads also allow a defective gauge to be replaced without disturbing the rest of the Wheatstone Bridge circuit. A temperature-controlled soldering iron was used throughout the soldering process because a regular soldering iron was tried first but it ruined several gauges by overheating and lifting the foil tab on the strain gauge. Common crazy glue was found to work very effectively in holding multiple wires in place on the solder tab. The circuit of each load cell was soldered according to Figure 2.13. At this stage, the loading direction of different cells was not taken into account as the sign convention of each cell can be changed easily in software. Following the soldering process, the resistance of each circuit was checked with a digital multimeter. Finally, M-Coat FBT sealer was applied over each gauge for protection and longevity. Figure 2.14 shows a beam completed with its strain gauges and mounted in a load cell housing.

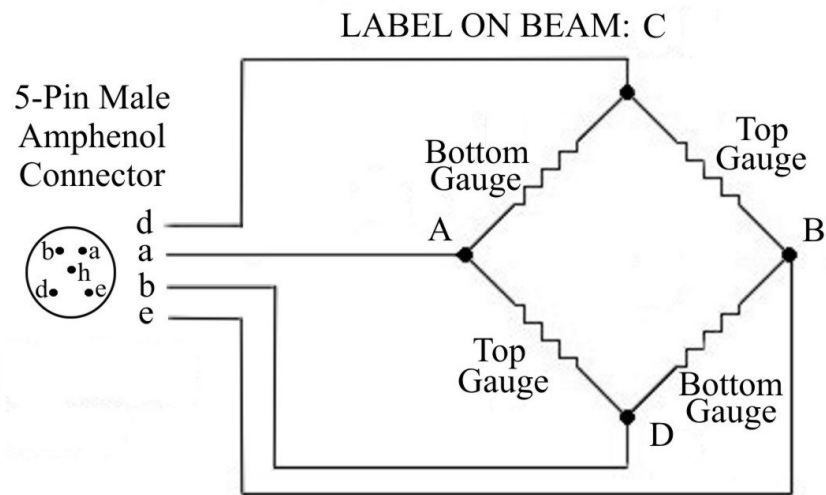


Figure 2.13. Wheatstone Bridge circuit and connector pin assignment of each load cell.

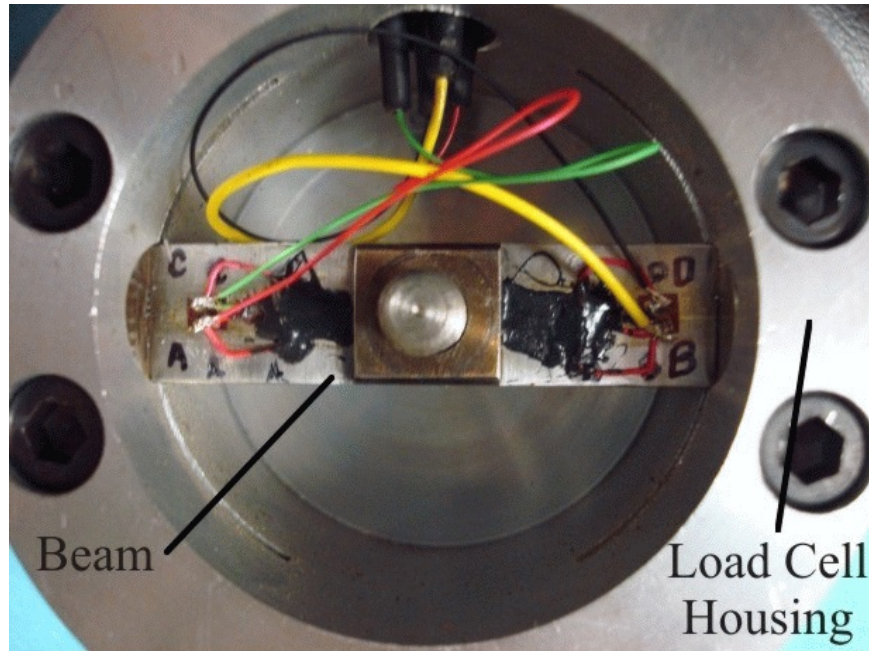


Figure 2.14. Load cell with completed beam, strain gauges and wiring.

Before installing the completed load cells in the aerodynamic balance, preliminary tests were performed by hanging weights directly from each cell. All load cells showed the expected linear behaviour for strain measurements at increasing applied loads. An appropriate and optimised strain gauge excitation level of 4.5V was determined in accordance with Vishay Micro-Measurements Technical Note TN-502 [45]. In an unstrained state, all cells indicated small non-zero strain levels, which may be compensated by using either hardware or software offset-nulling techniques. At this stage, a new and thinner beam was ordered from Aerolab for the yaw moment channel to increase its sensitivity, as this channel originally contained a relatively thick 1.8288mm (0.076") beam. The load cells for the side force and rolling moment channels were also switched in order to provide more sensitivity for the relatively small lift forces associated

with lightly-iced conductors. As a final step before mounting the load cells in the balance, the safety stops of each cell were set using a feeler gauge according to the Aerolab's specifications listed in Table 2.1.

Table 2.1. Summary of the aerodynamic balance's load cells.

Channel Number	Description	Beam Thickness	Safety Stop Thickness
1	Horizontal Side Force	0.032"	0.010"
2	Drag Force	0.076"	0.005"
3	Pitching Moment	0.032"	0.010"
4	Vertical Lift Force	0.076"	0.005"
5	Yaw Moment	0.028"	0.010"
6	Rolling Moment	0.076"	0.005"
-	Spare (Gauged Beam Only)	0.076"	0.010"

2.4.3 Automation and Motion Control

The previous drive train for the aerodynamic balance had spur and worm gears driven by a 35oz-in stepper motor. The system could not be used for forced-vibration studies, but it offered a high reduction ratio and very accurate static positioning in a relatively compact space. The presence of the worm gear also allowed the system to be driven only by the motor, so that a large aerodynamic moment acting on the model could not overpower the stepper motor. The deficiencies of the system were the mounting position of the motor on the balance's outer cradle and its open-loop configuration. With the motor mounted to the balance's outer cradle and the gearing attached to the centre spider hub, aerodynamic moments can be transferred through the stepper motor to the loads cells used to measure the aerodynamic forces. Such a configuration is undesirable and defeats the purpose of the balance's flexure rods that support the centre spider hub. The new automation system installed in the balance addresses these deficiencies without compromising the advantages of the previous configuration.

The new drive train for the aerodynamic balance consists of an AT5 synchronous belt and pulleys which provide a 1:2 reduction. Unlike the previous automated controls, the new configuration can be used for accurate model positioning in both stationary and dynamic testing. The system is closed-loop, with a positional accuracy of about $\pm 0.2^\circ$. The new automation hardware has a 262 oz-in stepper motor, a Parker Zeta microstepping driver, a 1000 line incremental encoder, and an Oregon Microsystems (OMS) MaxP8000 motion controller card. The stepper motor is mounted directly to the balance's centre spider hub, as shown in Figure 2.15. The system allows a wind tunnel

model to be indexed to a preset home position by using the index pulse of the incremental encoder as a home switch*. The new automation system's gear reduction can also be changed in the future by installing different diameter pulleys.

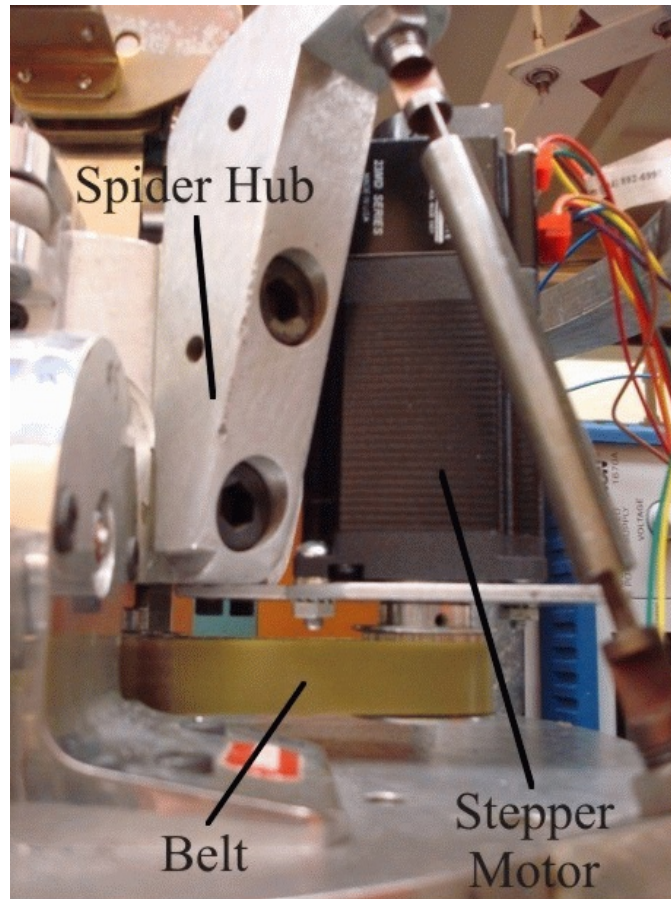


Figure 2.15. The new stepper motor mounted to the aerodynamic balance's spider hub.

* The homing procedure and index pulse only function in the counter-clockwise direction and at low rotational speeds, typically 100 (encoder) pulses per second. A typical homing command for the MaxP controller is “AS;AC1000;VL100;HTH;HR0;MA0;GO;ID;”.

2.4.4 Procedure to Align Balance

The purpose of aligning a pyramidal aerodynamic balance is to minimise the interactions and mechanical cross-coupling between load cells. With present-day personal computers, strict machining tolerances originally required to completely eliminate mechanical interactions are not needed [40]. Rather than remachining parts and tediously realigning components, a more time and cost-effective approach is to align the balance as closely as possible. Then calibration data and simple post-processing techniques are used to remove interactions [40]. In the present situation, the alignment procedure involves carefully adjusting 19 linkages so that the balance's outer cradle and spider hub are level and plumb, and the balance's flexure rods align to a common point above the centre spindle. When the flexure rods are extended with alignment rods and intersect at a common point, the intersection point is called the balance's virtual centre [40]. For the University of Manitoba's Aerolab balance, the virtual centre is located 25.88cm (10.19") above the centre spindle.

Figure 2.16 shows the Aerolab pyramidal balance, where the linkages are labelled according to their adjustment order in the alignment process. Before starting the alignment process be aware that, during the final tightening of any link, a link's turnbuckle may rotate slightly. To avoid this unwanted rotation, a simple piece of tape can be used to mark the desired orientation of link's turnbuckle before tightening. Such a marker allows any unwanted rotation to be seen and counteracted when tightening the link's lock nuts with wrenches. Starting with the aerodynamic balance's base in a level orientation, the following six steps outline the alignment process.

1. The link at the vertical lift cell is adjusted so that the pivot bar above the lift cell is level.
2. The matching pair of links connecting the pivot bar to the counterweights below the outer cradle are adjusted, ensuring there is adequate clearance for the counterweights to deflect under loading in both the upward and downward directions.
3. The links for the side force and drag load cells are adjusted so that the four vertical links supporting the outer cradle are plumb. The length of the four vertical links are also adjusted so that the links for the side force and drag load cells as well as the outer cradle's ring are level.
4. The flexure rods supporting the centre spider hub are adjusted until the centre spider hub is level and the space underneath the hub is 1.52" to the cradle. A fixed depth gauge machined from a bolt can be used to measure the space underneath the centre spider hub.
5. The centre alignment rod is mounted in the balance ensuring that the alignment rod is plumb and its tip is 25.88cm (10.19") above the centre spindle. The balance's four alignment fixtures and rods are attached to the flexure rods supporting the centre spider hub.
6. The five links for the yaw, pitch, and rolling moment channels are adjusted so that the side plate and small pivot for the yaw channel are both level, and the five alignment rods meet as closely as possible to their common point at the balance's virtual centre. Figures 2.17 and 2.18 show an example of

the alignment rods meeting as closely as possible to a common, virtual centre point. Note that the adjustment of certain combinations of moment channel links may either open, close, rotate, or translate the “pyramid” formed by the alignment rods. Future operators are advised strongly to first experiment with the moment links in order to gain an understanding of how each link changes the alignment rods’ positions.

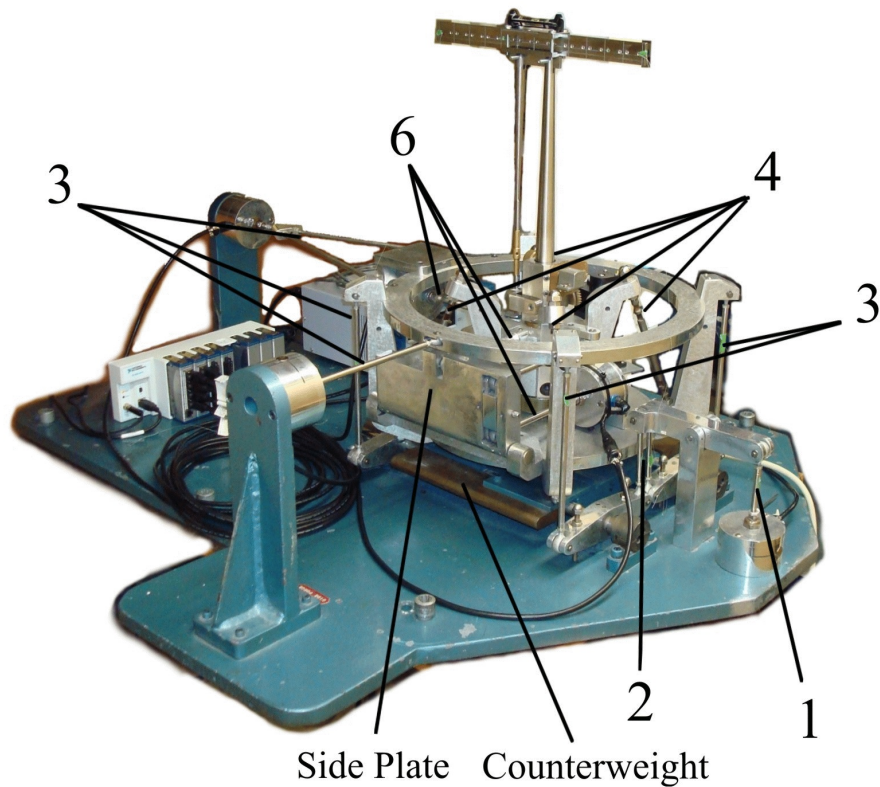


Figure 2.16. Aerolab pyramidal balance with labels showing the order of the linkage adjustments in the alignment procedure.

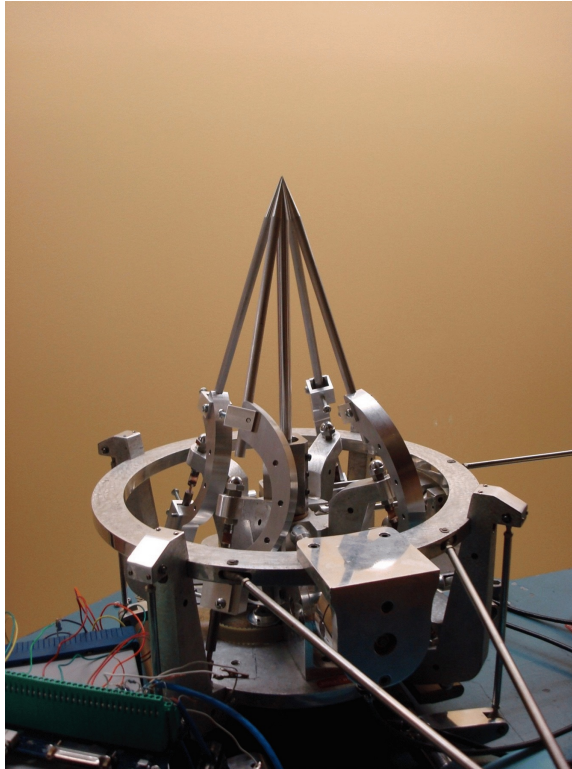


Figure 2.17. Alignment rods and fixtures of the pyramidal balance.

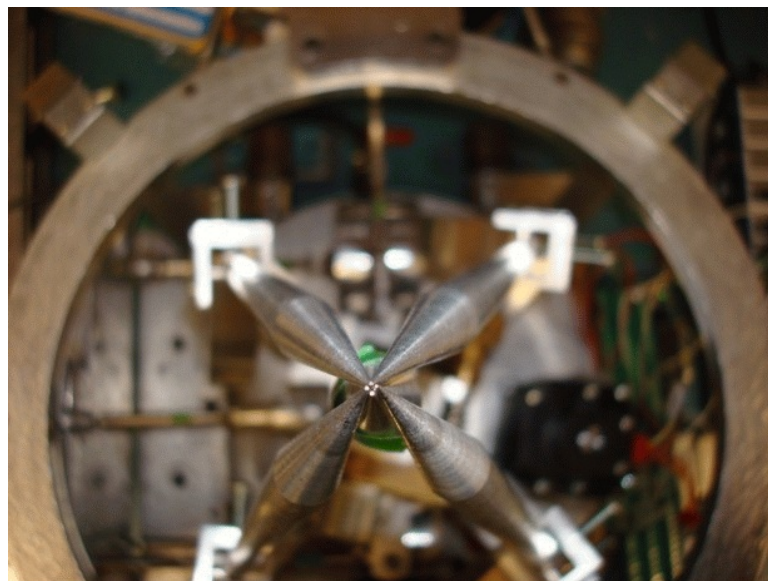


Figure 2.18. Detail of the virtual centre with the aligned pyramid.

2.4.5 Balance Calibration

The basic calibration procedure for an aerodynamic balance involves loading the balance with known weights and measuring the balance's output signals. In the present case a preliminary calibration was done outside the wind tunnel, with loads applied at the height of the balance's virtual centre. These preliminary tests confirmed that the balance performed in a repeatable and linear manner, with relatively small linear interactions between channels. Small linear interactions between load cells indicate that the balance is aligned adequately. Following this preliminary calibration, the aerodynamic balance was rolled underneath the wind tunnel's lower test section using wooden dowels. Strings and a plumb bob were used to carefully align the balance with the test section's walls and floor.

With the aerodynamic balance aligned properly with the wind tunnel test section, new bolts for adjusting the height of the balance's base were used to level and raise the balance to its proper height. Recall that the height of the balance's virtual centre above the main spindle was 25.88cm (10.19"), but the inner test section height was measured at 53.24cm (21"). Ideally, the balance's virtual centre should coincide with the midpoint of the test section, so that side and drag forces do not induce rolling and pitching moments, respectively. Such an arrangement was not possible due to the limited inner height of the test section and the thickness of the wind tunnel floor. The best possible arrangement was to raise the balance as high as possible so that the main spindle was about 0.32cm (1/8") below the lower surface of the wind tunnel floor. This approach was taken and it resulted in the balance's virtual centre being about 3cm (1 1/8") below the midpoint of

the wind tunnel's test section.

The calibration process was repeated with loads applied at the centre height of wind tunnel test section. Figure 2.19 shows the right-hand coordinate system used throughout the final calibration procedure. Note that, the alignment and level of the calibration bar and line can be checked much more accurately by calibrating the balance inside the wind tunnel and using the wind tunnel walls and floor as fixed references. The calibration results are presented in Figures 2.20 through 2.24.

A slight asymmetry is seen in Figure 2.21(e) for the yaw channel interaction that is caused by an applied side force. This asymmetry may be due to a slight curvature of some of the flexure rods observed when installing the centre spider hub. These curved flexure rods were not replaced before aligning the balance, as the geometry of the hub was thought to force the rods to assume a straight orientation. In the next major alignment and calibration process, these curved flexure rods should be replaced to remove any unnecessary cross-coupling effects due to a curved flexure rod. Until the next alignment process is performed, the post-processing techniques described in Appendix A can be used to account for the measured asymmetries in the calibration process.

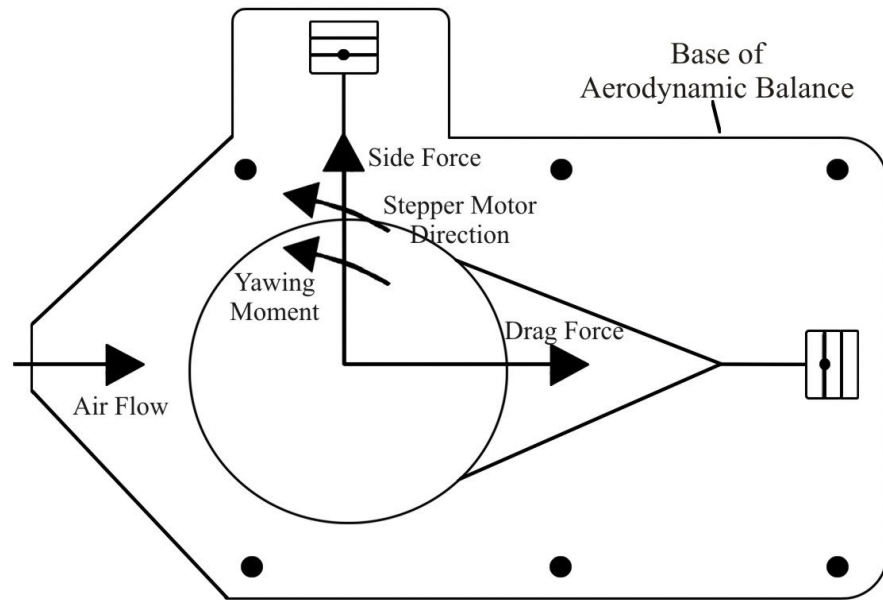


Figure 2.19. Right hand coordinate system used for the final calibration procedure.

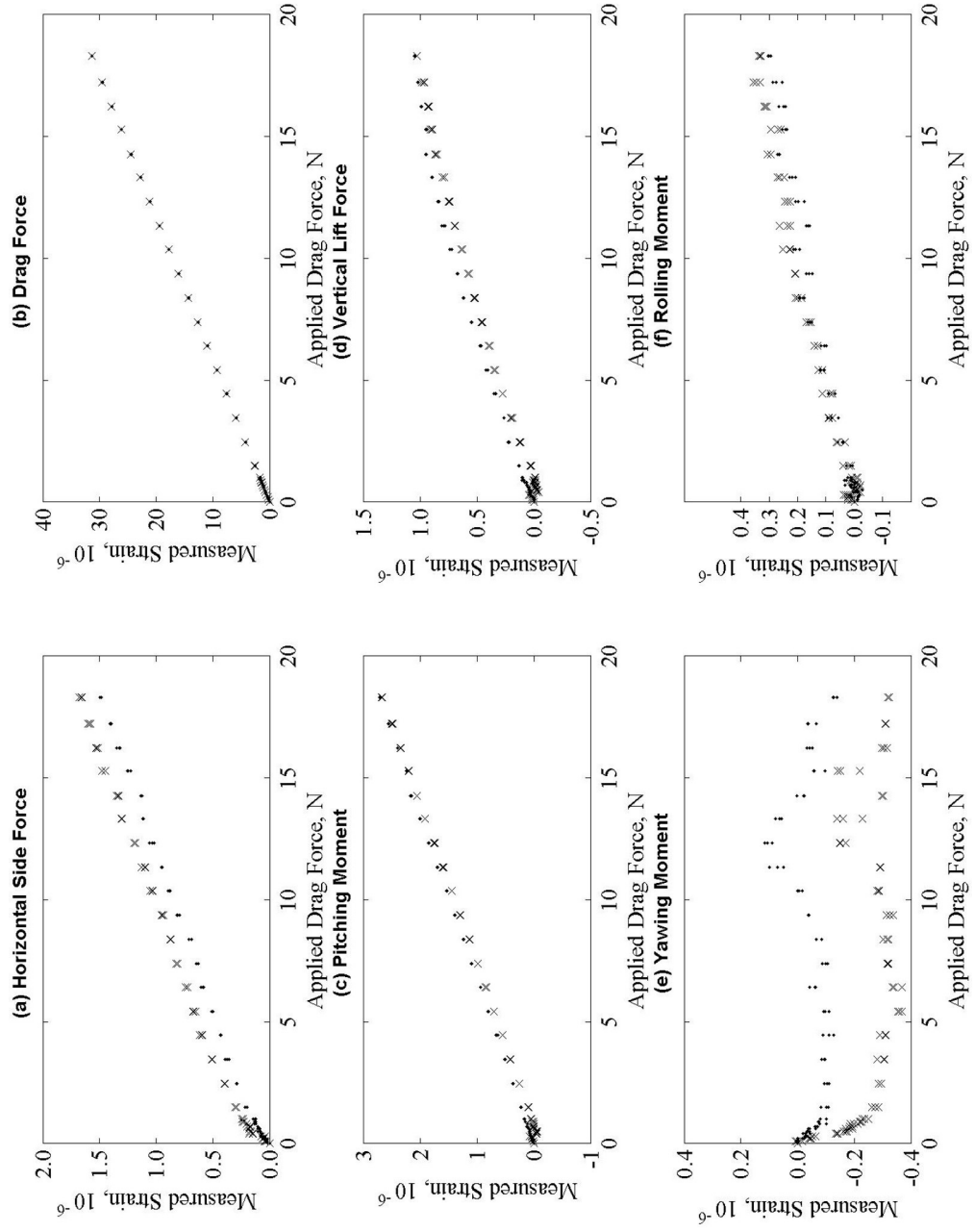


Figure 2.20. Final calibration results from applying a positive drag force.

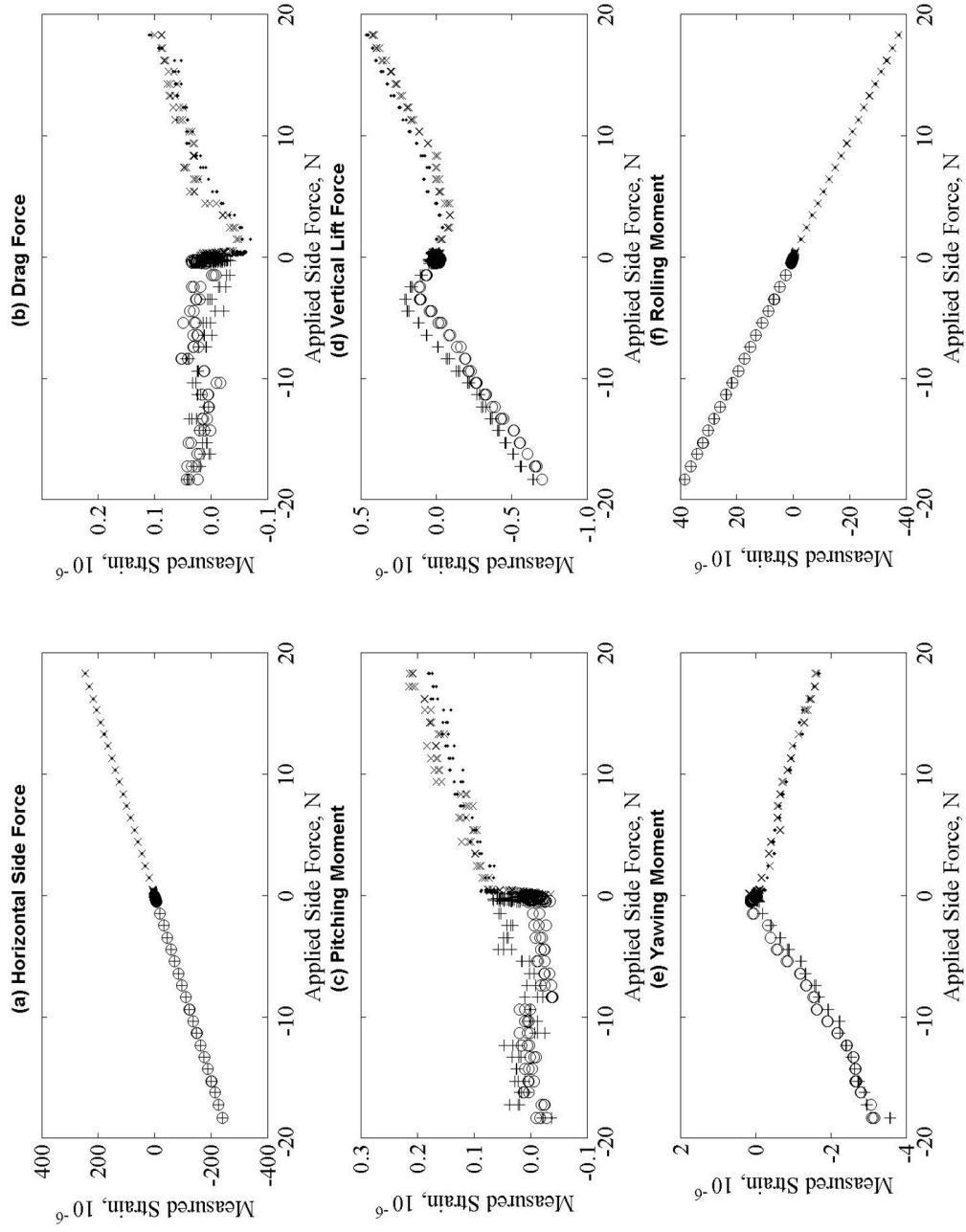


Figure 2.21. Final calibration results from applying a positive and negative side force.

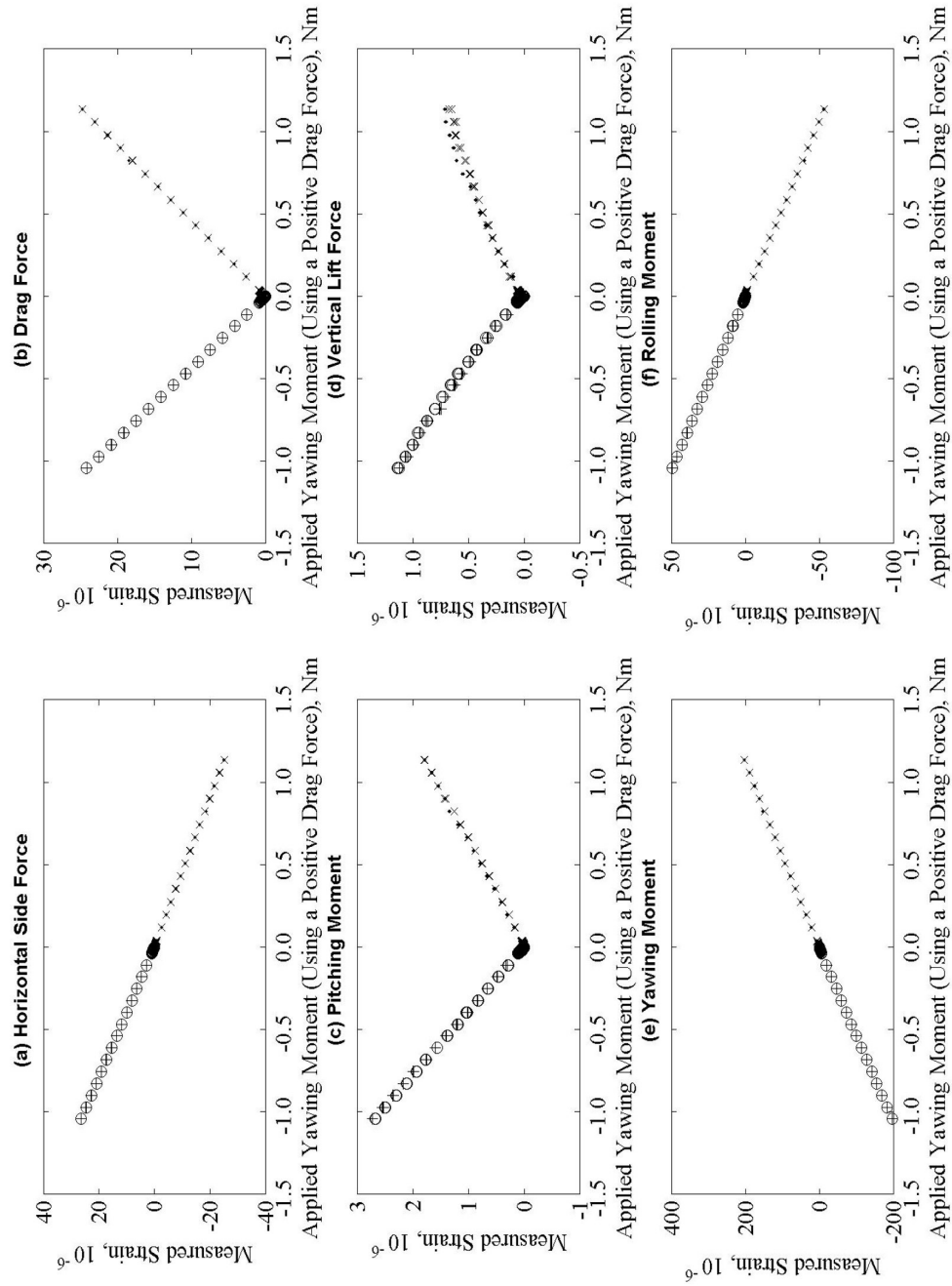


Figure 2.22. Final calibration results from applying a yawing moment by using an off-set, positive drag force.

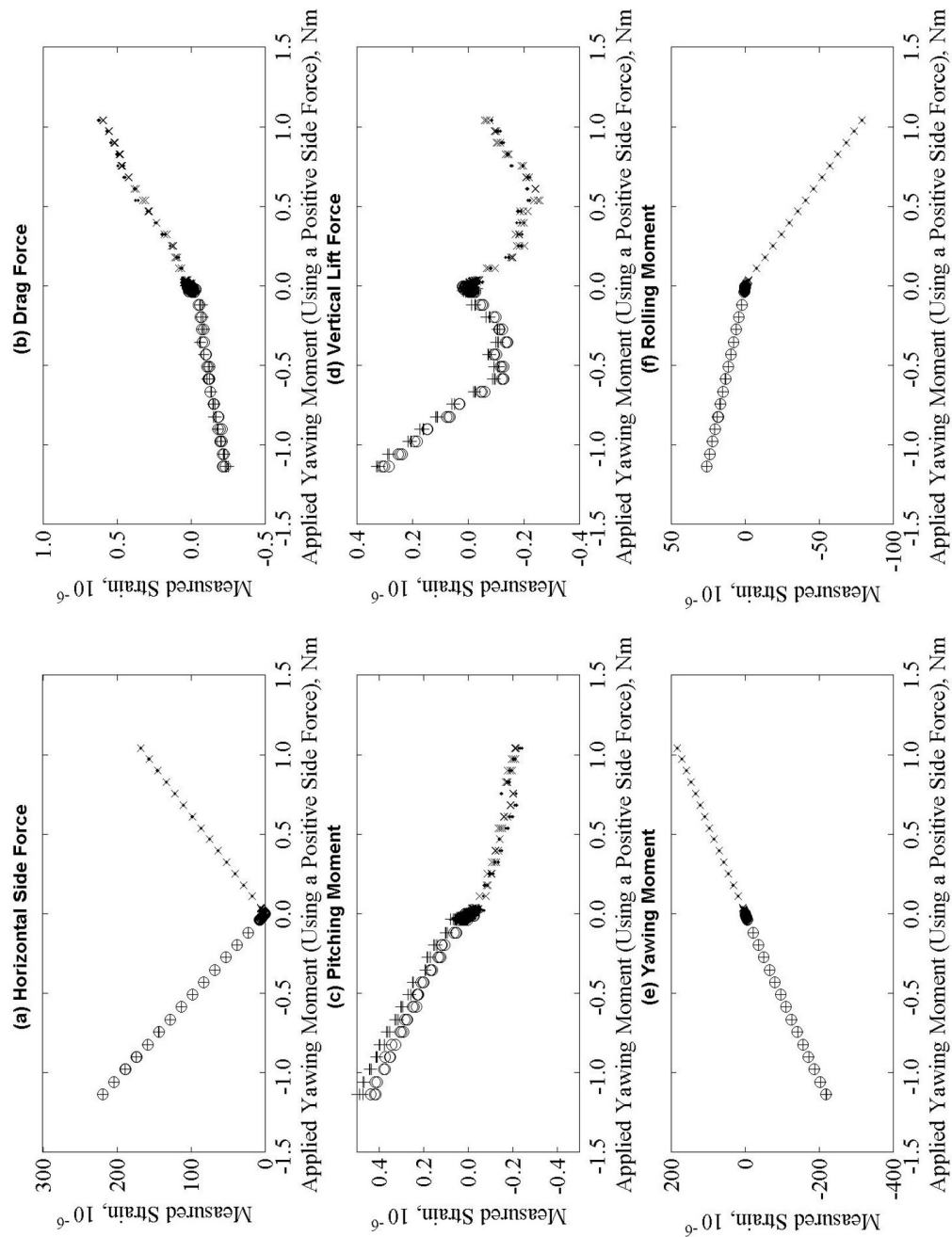


Figure 2.2.23. Final calibration results from applying a yawing moment by using an off-set, positive side force.

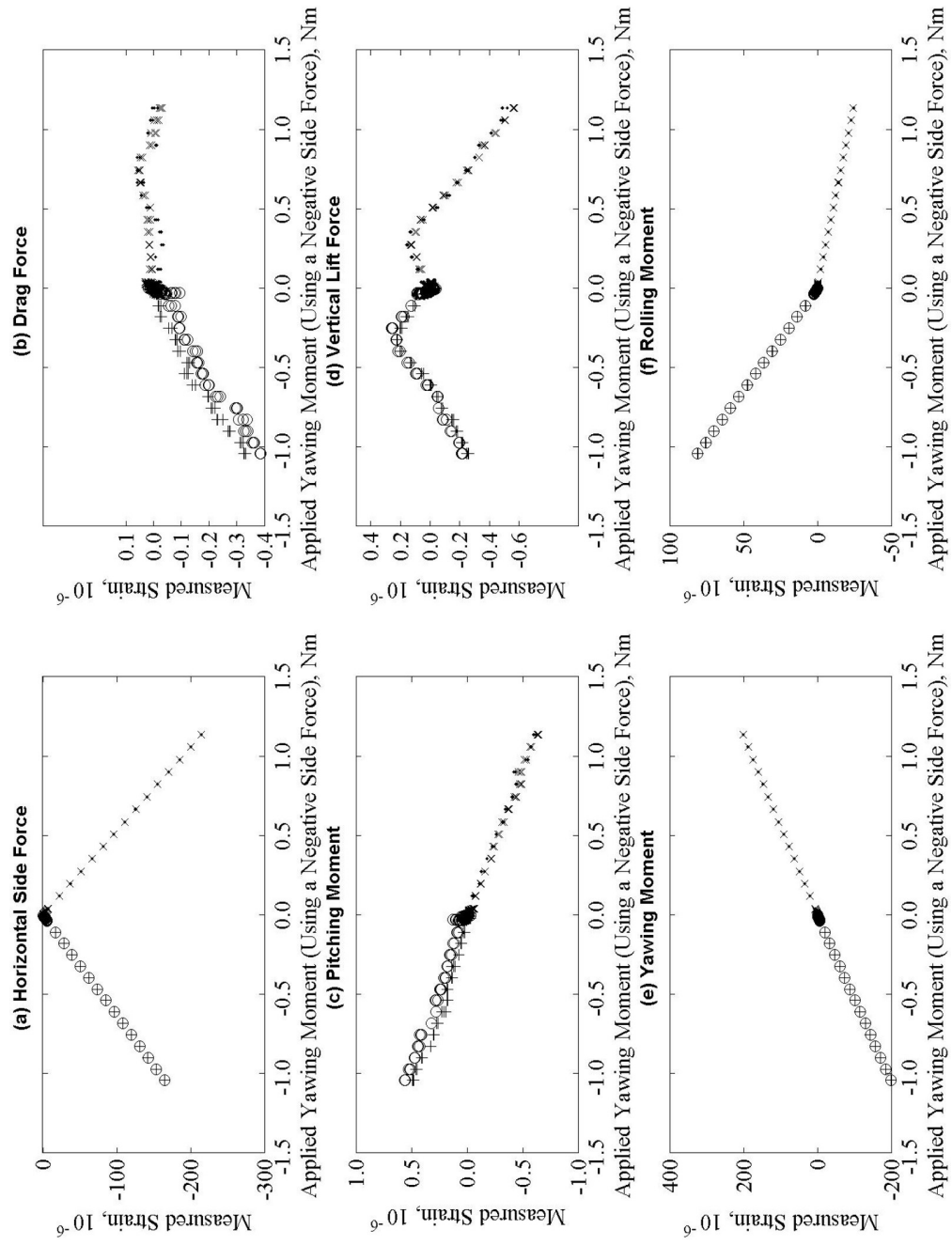


Figure 2.24. Final calibration results from applying a yawing moment by using an offset, negative side force.

2.4.6 Benchmark Trial

The results for a final benchmark trial involving the C11 ice shape [22] are presented in Figure 2.25, where the aerodynamic lift, drag, and moment are plotted as normalised coefficients versus the angle of attack. Figure 2.25 also shows the aerodynamic measurements of the C11 ice shape taken independently in the Canadian National Research Council's (NRC) 2x3m wind tunnel [46]. Both tests used a full-scale cross-sectional geometry of C11, where the model's cross-section is shown orientated at 0° in Figure 2.25. However the model in the NRC test had a length to diameter or aspect ratio of about 50:1 [46]. End plates were also used in the NRC test due to concerns about end effects and the two-dimensionality of the flow [46]. In the benchmark test conducted at the University of Manitoba, the model's aspect ratio was about 20:1 with no end plates. The agreement seen between the two separate tests shows that the present calibration results can be used to collect meaningful aerodynamic data. The agreement with the NRC test, involving a significantly longer model fitted with end plates, also indicates little difference in using a shorter 20:1 model without end plates in the planned experiments.

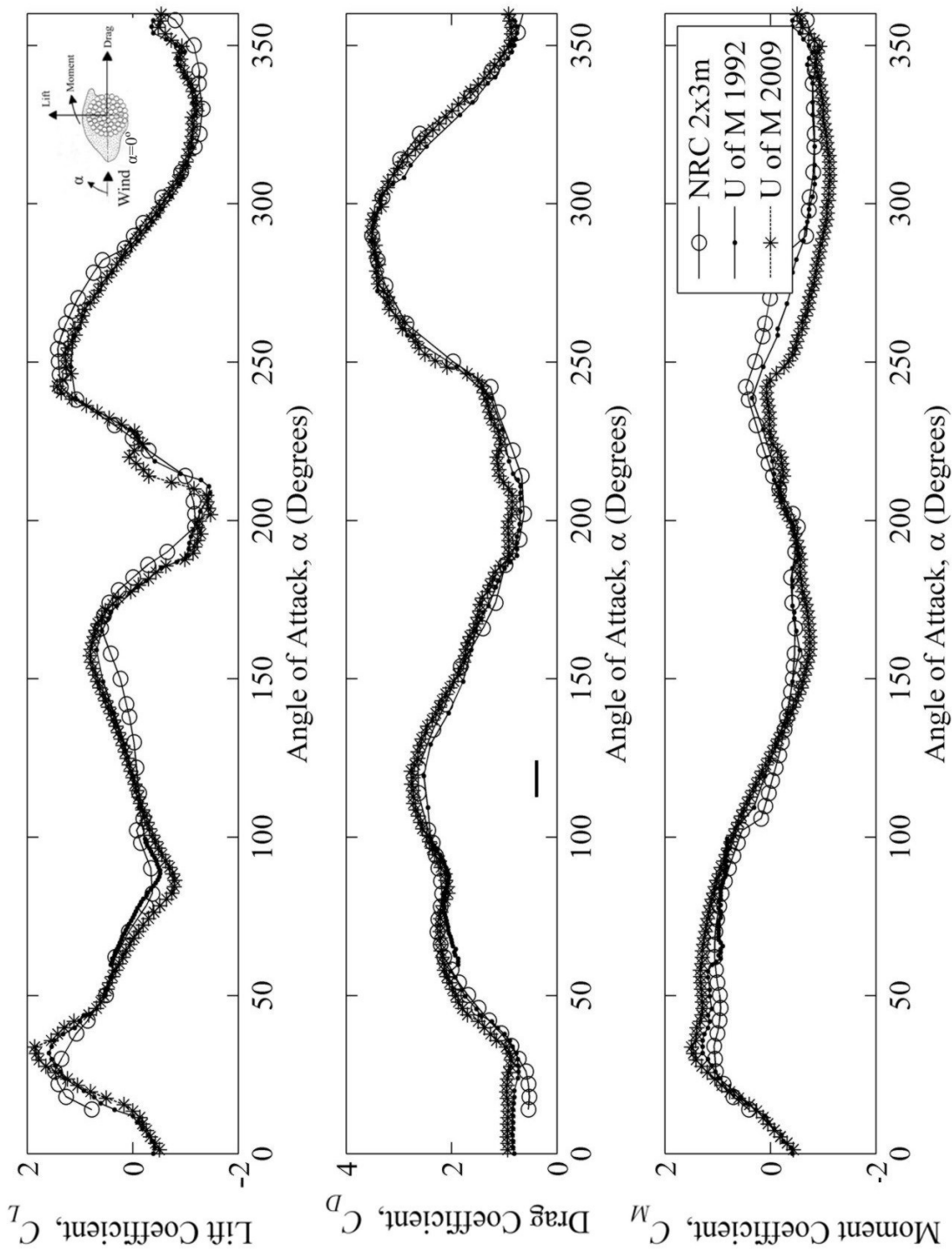


Figure 2.25. Benchmark results for a moderately-iced conductor model.

2.5 Visualisation System

A laser-based visualisation system was developed and implemented in 2009 for the planned experiments. The visualisation system was expected to help with interpreting data from the aerodynamic balance. For example, previous studies on galloping [28], flutter [35], and rotationally-oscillating cylinders [47], have all used a smoke-wire visualisation system to qualitatively study the near-wake region and flow separation points around bluff wind tunnel models. However, these previous studies used wind speeds of about 1-3m/s. Indeed, at realistic galloping wind speeds of about 10m/s, the smoke-wire technique is not feasible [28,47]. Hence, an alternative visualisation system based on readily available components for particle-image velocimetry (PIV) was implemented. Such a system involves injecting tracer particles into the wind tunnel flow, illuminating the particles with a sheet of light from a laser, and tracking the particle motion with a high-speed camera. These aspects are discussed separately next.

2.5.1 Selection of Seeding Particles

For both a PIV and a laser-based visualisation system, appropriate tracer or seeding particles are needed that closely follow the fluctuations in the airflow. Providing the particle density, ρ_p , is significantly higher than the surrounding fluid's density, ρ_f , ($\rho_p / \rho_f \gg 1$), and the particles are sufficiently small in diameter ($d_p \sim 1\mu\text{m}$), the ability of an aerosol particle to closely follow its surrounding fluid is often described by the Stokes Number, Sk [48]. For the conditions stated above, the Stokes Number is simply the ratio of the particle response time, τ_p , and the characteristic time scale of the flow, τ_f . Hence

$$Sk = \frac{\tau_p}{\tau_f}. \quad (2.1)$$

A conservative approach [48] used to estimate the Stokes Number for a given flow is to take the smallest turbulence time scale, or the Kolmogorov Scale τ_K , as the characteristic time scale of the flow. This conservative approach is followed here and it is described in detail below.

One of the most common seeding particles for wind tunnel studies is di-ethyl hexyl sebacate (DEHS, CAS122-62-3) oil droplets [49]. The density of DEHS oil is about 917kg/m³ [50]. Moreover non-toxic, monodispersed particles around 1µm in diameter can be generated easily using a Laskin-nozzle device. The particles also completely evaporate after about 5 hours [49]. This characteristic significantly minimises cleaning duties in a recirculating-type wind tunnel. For small DEHS oil droplets in an airflow

($\rho_p / \rho_f \gg 1$ and $d_p \sim 1\mu\text{m}$), the particle response time can be calculated reasonably [48] as

$$\tau_p \approx \frac{\rho_p d_p^2}{18\pi} \approx \frac{(917)(1 \times 10^{-6})^2}{18\pi} \approx 2 \times 10^{-11} \text{ s}. \quad (2.2)$$

The Kolmogorov time scale for the flow around a model can be estimated [51] to be

$$\tau_K = \left(\frac{\nu}{\varepsilon} \right)^{\frac{1}{2}} \approx \left(\frac{\nu}{\frac{(0.1V)^3}{d}} \right)^{\frac{1}{2}} \approx \left(\frac{(1.5 \times 10^{-5})(0.032)}{(0.1 \cdot 10)^3} \right)^{\frac{1}{2}} \approx 7 \times 10^{-4} \text{ s}, \quad (2.3)$$

where ν is the kinematic viscosity of air, ε is the turbulence dissipation rate, d is a characteristic length scale of the model's cross-section, and V is the free-stream wind speed. Note that the relative (or normalised) turbulence intensity is taken conservatively

as 0.1 in equation (2.3) [52]. Substituting the results from equations (2.2) and (2.3) into equation (2.1) shows that $Sk \ll 1$, so that $1\mu\text{m}$ DEHS particles are able to closely follow many types of airflow, including a 10m/s wind flowing around a lightly-iced conductor or cylinder. Due to its benefits, DEHS oil is used in the new visualisation system, along with two Laskin-nozzle droplet generators.

2.5.2 Seeding Particle Generators and Dispensers

Generating and introducing seeding particles into the wind tunnel's airflow with minimal disturbance requires careful consideration. Based on the visualisation experiments reported by Meijia and Jorgens [49], two Laskin-nozzle droplet generators, each having six nozzles, were estimated to produce enough tracer particles for visualisation experiments up to about 30m/s in the lower test section. Two such generators were built. They followed the design of Meijia and Jorgens [49] and the nozzle design principles of Kahler et al. [53,54]. Figure 2.26 shows the seeding particle generators. Although the particle size distribution of these custom-built seeding particle generators has not been tested, their nozzle dimensions match those tested and reported already [49,54]. Using an inlet air pressure to the generators of about 1Bar , the generators were able to produce monodispersed tracer particles with a mean diameter of about $1\mu\text{m}$ [54].

Preliminary experiments were conducted during the summer of 2008 to determine the best location for dispensing seeding particles in the wind tunnel. These experiments showed that the best location was directly upstream of the wind tunnel's screens and

contraction cone. Moving further upstream in the upper breather section resulted in a visibly less controlled distribution of particles in the lower test section. Dispensing the particles further downstream, directly into the contraction cone, created a large flow disturbance in the lower test section. The introduction of the seeding particles just upstream of the screens and contraction cone was the closest point to the lower test section where the dispensers could be placed without disturbing the tunnel's airflow. Dispensing tubes spanning the wind tunnel, which are illustrated in Figure 2.27, were built and installed just upstream of the screens and contraction cone. A Pitot-static tube was used to visually confirm that the mean flow uniformity in the lower test section was unaffected by the dispensers.



Figure 2.26. Custom-built particle generators for seeding.

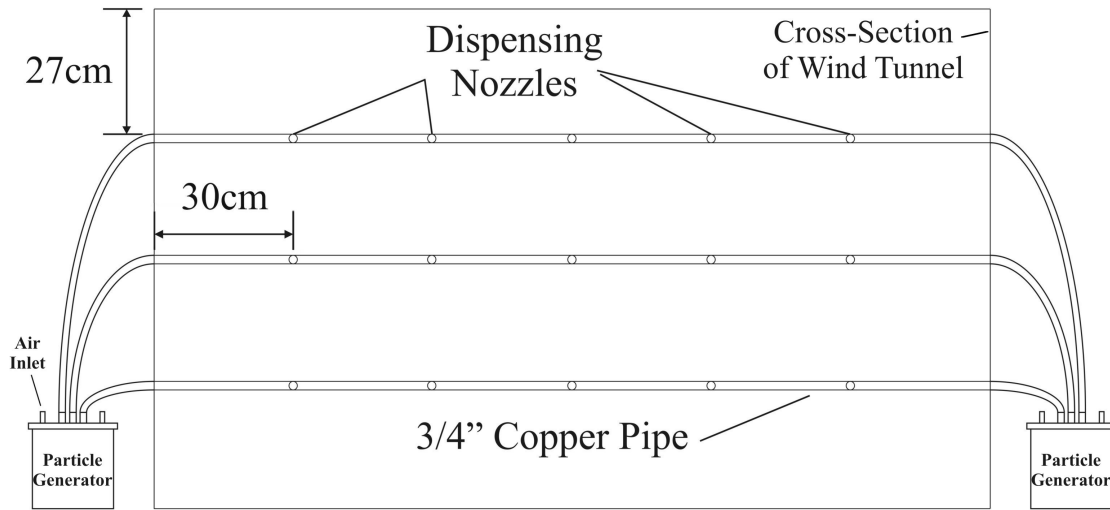


Figure 2.27. Dispensing apparatus for particle seeding generators.

2.5.3 Laser

A continuous 5W Coherent Innova 70C Argon:Ion laser was moved to the low-speed wind tunnel laboratory in the fall of 2008. Once moved into the wind tunnel laboratory, the laser's rear mirror was aligned following the manufacturer's guidelines [55]. A mirror provided by Mr. P. Kreuger and sheet-forming optics provided by Dr. M. Birouk were mounted temporarily between the laser and the test section. Slight difficulty was encountered in aligning the optics due to the temporary mounting plate used, and streaks were noticed throughout the laser-light sheet. However, the same general streak pattern was also present when the optics were tested using an alternative Coherent Innova 70C laser. The source of this unwanted pattern in the laser-light sheet was determined to originate in the laser itself. It may be corrected in the future by using a homogenising lens.

2.5.4 Camera

A Dantec Dynamics NanoSense MKIII high-speed digital camera was used for the new visualisation system. The camera featured a 1280x1024 pixel 8-bit CMOS sensor with a Nikon AF Micro-Nikkor 60mm f/2.8D lens. The camera was designed for timed-resolved PIV studies and high-speed imaging with sampling rates up to 1040 frames per second. For PIV studies with a Nd:Yag laser, the camera can be equipped with a 532nm band-pass filter to eliminate all light sources except that of the laser. Such a filter, however, does not provide significant protection from damaging the camera's CMOS sensor due to direct exposure to the laser or even direct reflections. Therefore care needs to be taken by future operators to ensure that the camera sensor is not exposed to the laser beam, especially when aligning the laser. During preliminary experiments, it was found that a simple way to safeguard the camera sensor, when aligning the laser beam, was to always keep the camera lens covered. This precaution was possible because the camera is not needed in the laser alignment process.

2.5.5 Wind Tunnel's Exhaust System

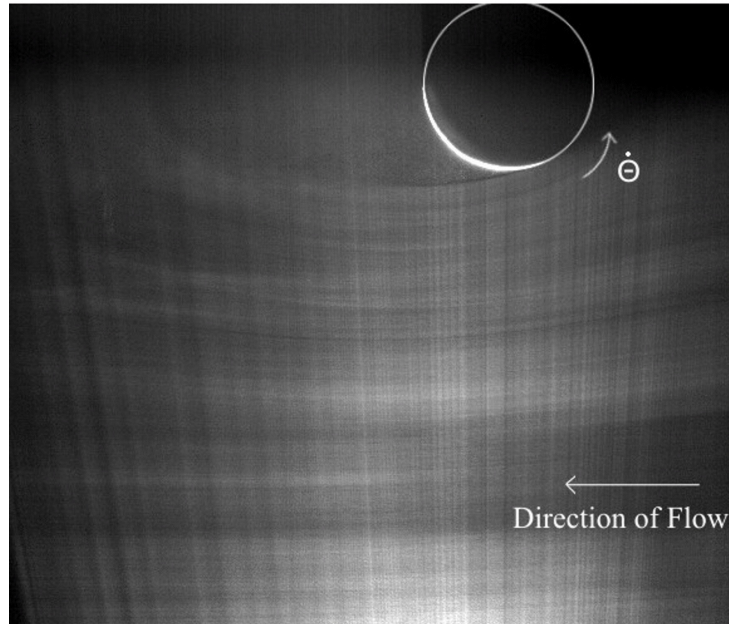
After completing preliminary seeding experiments, a wind tunnel exhaust system was found to be needed to control unwanted particles from escaping into the ambient laboratory air. Note that, although DEHS particles are non-toxic, they are also non-soluble in water and therefore should not be inhaled [49]. In the fall of 2008, a simple exhaust system was installed at the wind tunnel's breather section to remove seeding particles after fully completing an experiment. The system had two six-inch diameter

ducts with inline exhaust fans that drew seeding particles in a contained and controlled manner from the wind tunnel and into the laboratory's main exhaust system. The system was tested successfully in the winter of 2009.

2.5.6 Preliminary Flow Visualisation Around a Rotationally-Oscillating Cylinder

A set of preliminary experiments was conducted in the summer of 2009 to demonstrate the performance and usefulness of the visualisation system. These experiments involved a smooth 25.4mm diameter cylinder mounted vertically in the aerodynamic balance underneath the lower test section. New automated controls were used to rotationally-oscillate the cylinder in a steady manner, with an oscillation amplitude of $\pm 30^\circ$ and a frequency of 3Hz. The wind speed for these visualisation experiments was 10m/s. Figure 2.28 shows the 1mm thick laser sheet forming a horizontal plane at the mid-height of the test section, where the flow was visualised around the rotationally-oscillating cylinder at the instants of the cylinder's maximum angular speed. Note that this laser sheet thickness is commonly used in visualisation studies [49]. These preliminary results demonstrated that the new visualisation system can be used to qualitatively study features such as an airflow's separation at the surface of a structure. Furthermore, for the case of a smooth circular cylinder, Figure 2.28 shows that rotational motions involved in an oscillation of $\pm 30^\circ$ at 3Hz have little effect on a separation point. This observation agrees with the findings of previous studies [47,56], although a lightly-iced conductor model is expected to yield more interesting results due to its irregular geometry.

(a)



(b)

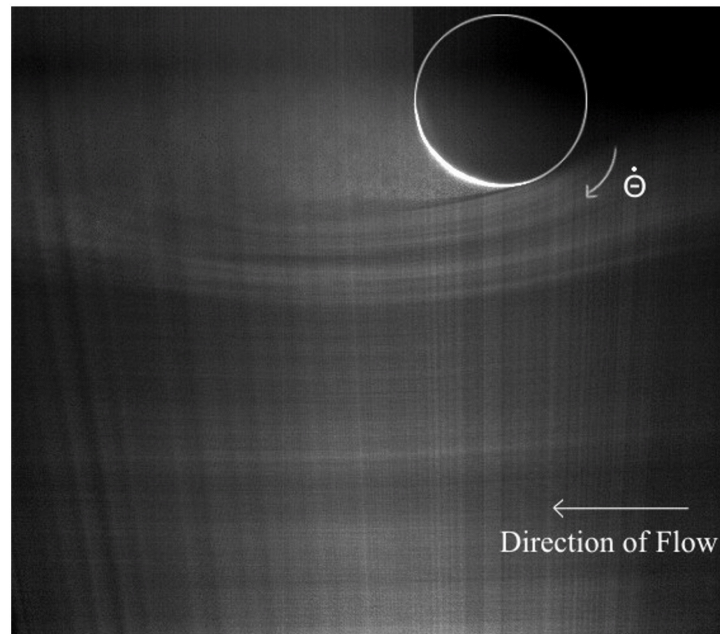


Figure 2.28. Preliminary visualisations of the airflow around a smooth, rotationally oscillating prismatic cylinder undergoing (a) counter-clockwise and (b) clockwise motion.

3 EXPERIMENTAL DETAILS AND PROCEDURE

3.1 Introduction

The present experiments are organised into three types: quasi-steady testing involving a stationary model, dynamic testing involving a rotationally-oscillating model, and flow visualisation tests. The details of each type of experiment are discussed in this chapter. For example, the motion control for the dynamic tests in addition to post-processing of the data involved in both the stationary and dynamic tests, are described and explained. Emphasis is given on the challenges and solutions that arose during these experiments.

3.2 Quasi-Steady Experiments

Quasi-steady or stationary testing involves characterising the aerodynamics resulting from the airflow around a static model. The model's angle of attack is generally varied incrementally and, at each angle of attack considered, the aerodynamic loads acting on the static model are measured. In previous quasi-steady experiments involving iced-conductor models, relatively high wind speeds of 30m/s were used [22]. Although such high wind speeds are not realistic for galloping transmission lines, this approach was taken to increase the signal-to-noise ratios involved in the aerodynamic load measurements. Furthermore, for moderately iced conductors, experiments conducted at 10 and 30m/s gave fairly similar results for the normalised aerodynamic lift, drag, and moment coefficients, C_L , C_D , and C_M , respectively [57]. The normalised aerodynamic coefficients are defined as

$$C_L = \frac{L}{\frac{1}{2}\rho V^2 dl}, \quad (3.1)$$

$$C_D = \frac{D}{\frac{1}{2}\rho V^2 dl}, \quad (3.2)$$

$$C_M = \frac{M}{\frac{1}{2}\rho V^2 d^2 l}, \quad (3.3)$$

where L , D , and M are the aerodynamic lift, drag, and moment, respectively, ρ is the density of air, V is the free-stream wind speed, and d and l are the model's bare conductor diameter and length. Note that the product of d and l is the projected area of the bare conductor normal to the airflow's direction. Results from the quasi-steady experiments were plotted as aerodynamic coefficients versus angle of attack curves.

In the present quasi-steady experiments, wind speeds of 10, 14, and 18m/s are examined. The angle of attack is varied from 0 to 360° for each wind speed in increments of 2°. At each angle of attack, 2000 data samples of the aerodynamic loads are recorded at a sampling rate of 2000 samples per second, and the average of the 2000 samples is computed. Longer sample durations of 5s were considered in preliminary trials, but yielded similar averages compared to those from data collected over 1s. The closed-loop motion control hardware as well as the wind speed controller are used to automatically position the model and maintain a constant wind speed. The ability to test at these much lower and realistic wind speeds for galloping is possible due to the newly purchased 24-bit data-acquisition hardware and the more sensitive loads cells installed in the aerodynamic balance. (See Section 2.4.2.) All the quasi-steady experiments are

repeated in still air to eliminate any minor effects caused by the centre of mass of the wind tunnel model being offset from the virtual centre of the aerodynamic balance.

Figure 3.1 shows a cross-section of the C14 lightly-iced conductor model [22] and the coordinate system used for the quasi-steady experiments.

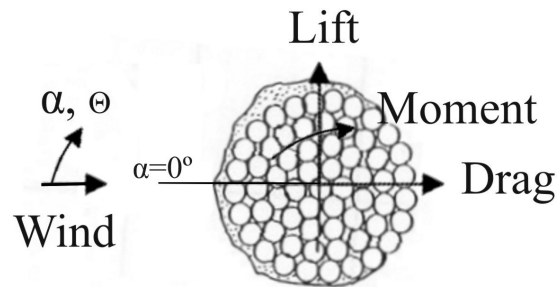


Figure 3.1. Cross-sectional shape of the C14 lightly-iced conductor and the coordinate system for the quasi-steady tests.

The wind tunnel model was orientated vertically in both the aerodynamic balance and wind tunnel test section for the quasi-steady testing. The model's projected area resulted in a solid blockage of the wind tunnel of about 4% of the wind tunnel's cross-sectional area. Using this vertical orientation, the aerodynamic balance's side force, drag, and yawing moment correspond to the lift, drag, and moment, respectively, of the iced-conductor model. However, during initial quasi-steady testing, a noticeable vibration in a cantilevered mode was exhibited by the plaster model even at the lowest wind speed of 10m/s. The natural frequency of the model in a cantilevered mode was about 12Hz, and the alternating side force caused by vortex-shedding was suspected to be responsible for the vibration. These cantilevered vibrations must have been undetected or were ignored

previously. As quasi-steady testing required a nominally stationary model, a new plaster model was cast on a much stiffer 3/4" copper pipe core using the existing C14 ice shape mould [22]. The new model showed no noticeable cantilevered motions during subsequent wind tunnel tests.

3.3 Forced-Vibration Experiments

Forced-vibration experiments involve the wind tunnel model undergoing controlled, steady rotational oscillations. As the model oscillates, the unsteady aerodynamic loads acting on the model are measured by employing the aerodynamic balance's load cells. The same model and vertical orientation as that for the quasi-steady testing are used in these dynamic tests. Therefore the quasi-steady aerodynamic measurements serve as a baseline for the results from the forced-vibration tests. In the latter, the automation equipment provides arbitrary control of the model's initial angle of attack, as well as its angular position, velocity and acceleration. The key parameters varied during the dynamic experiments are the model's initial angle of attack, and the steady-state amplitude and frequency of the model's rotational oscillations. A comparison of the static and dynamic test data is useful for assessing aerodynamic coupling effects and the validity of using a characteristic radius approach for transmission line galloping.

3.3.1 Motion Control

Unlike quasi-steady testing, which involves only point-to-point control, forced-

vibration tests require a path-controlled motion. For example, given an initial angle of attack, α_0 , oscillation amplitude, A_θ , and frequency, f_θ , the angular position of the model, θ , at any instant, t , is given by

$$\theta = \alpha_0 + A_\theta \cos(2\pi f_\theta t). \quad (3.4)$$

Note that the angular position of the model is equivalent to the angle of attack, α , for a purely rotational motion. The model's angular speed and acceleration are determined by differentiating equation (3.4) with respect to time so that

$$\frac{d\theta}{dt} = \dot{\theta} = A_\theta 2\pi f_\theta \sin(2\pi f_\theta t), \quad (3.5)$$

and

$$\frac{d\dot{\theta}}{dt} = \ddot{\theta} = A_\theta (2\pi f_\theta)^2 \cos(2\pi f_\theta t). \quad (3.6)$$

However, the analytical expressions given in equations (3.4) to (3.6), must be discretised before they can be used by the OMS MaxP8000 motion controller card. In addition, each discrete interval in the path-controlled motion must have a constant acceleration due to the limitations of this controller card [58]. Equation (3.4) is discretised using 41 points for one complete cycle of the cosine motion. Figure 3.2(a) shows the discretised angular position of the wind tunnel model.

The discrete angular speed and acceleration profiles, on the other hand, are computed from the kinematic equations,

$$\theta_{i+1} = \theta_i + \dot{\theta}_i \Delta t + \frac{1}{2} \ddot{\theta}_i \Delta t^2, \quad (3.7)$$

and

$$\dot{\theta}_{i+1} = \dot{\theta}_i + \ddot{\theta}_i \Delta t, \quad (3.8)$$

where θ_i and θ_{i+1} are the i^{th} and $(i+1)^{\text{th}}$ discrete angular positions, respectively. The $\dot{\theta}_i$, $\ddot{\theta}_i$ and $\ddot{\theta}_{i+1}$, are the corresponding angular speed and accelerations, respectively.

Rearranging equation (3.7) gives $\ddot{\theta}_i$ as

$$\ddot{\theta}_i = \frac{(\theta_{i+1} - \theta_i) - \dot{\theta}_i \Delta t}{\frac{1}{2} \Delta t^2}. \quad (3.9)$$

Consequently θ_i and θ_{i+1} are known from equation (3.4), and $\dot{\theta}_0=0$ (when $t=0$).

Substituting these quantities into equation (3.9) gives $\ddot{\theta}_1$ directly. The $\dot{\theta}_1$ is solved subsequently by employing equation (3.8). This process is repeated for each point in one cycle to give the discrete velocity and acceleration profiles shown in Figures 3.2(b) and (c). As an illustrative example, the choice of 35° , 20° , and 3.2Hz for α_0 , A_θ , and f_θ , respectively, results in the model's repeatable motion shown in Figure 3.3. The angles are measured directly by utilising the stepper motor's incremental encoder.

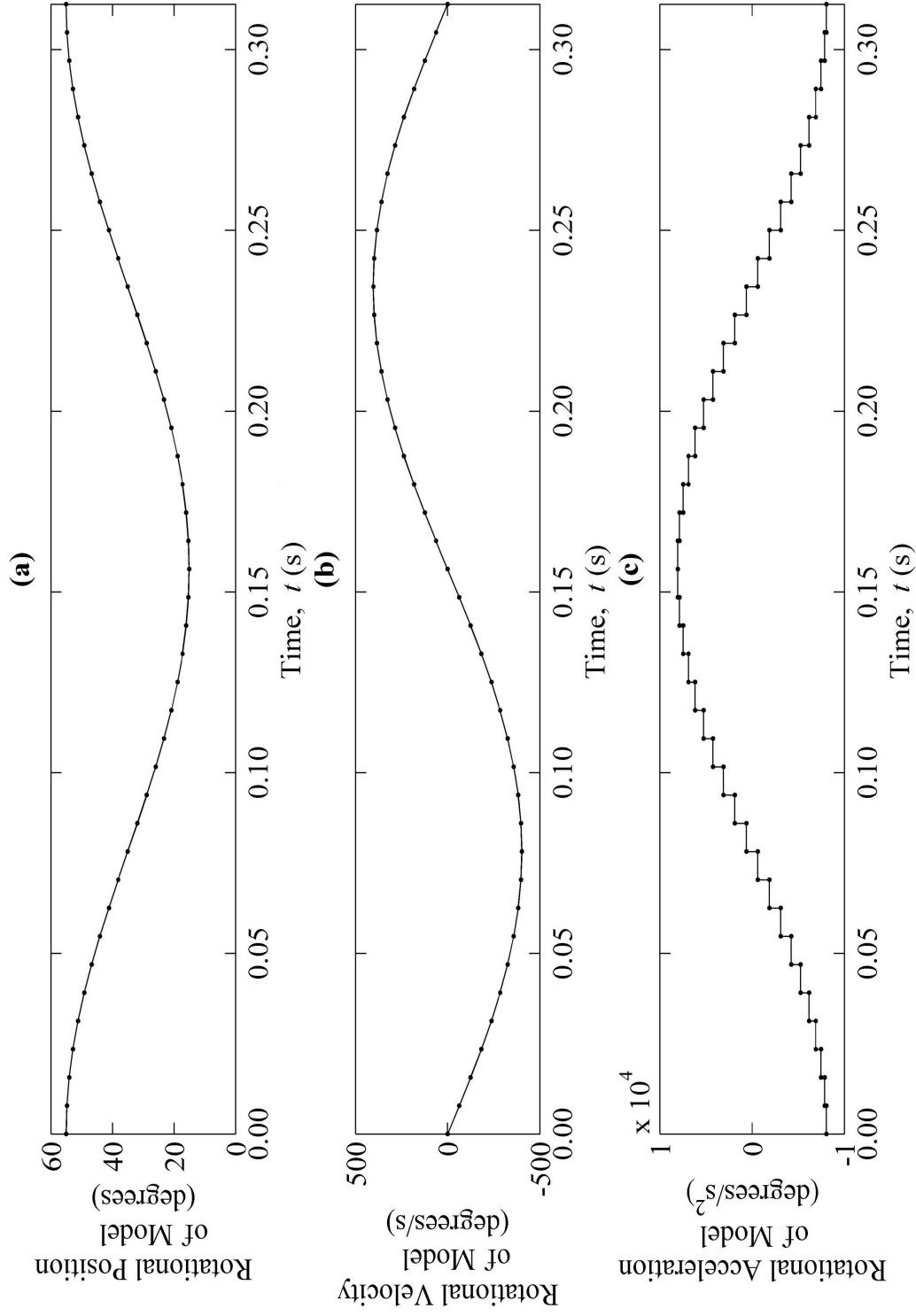


Figure 3.2. Example of the discretised rotational (a) position, (b) velocity, and (c) acceleration for the motion control of the model.

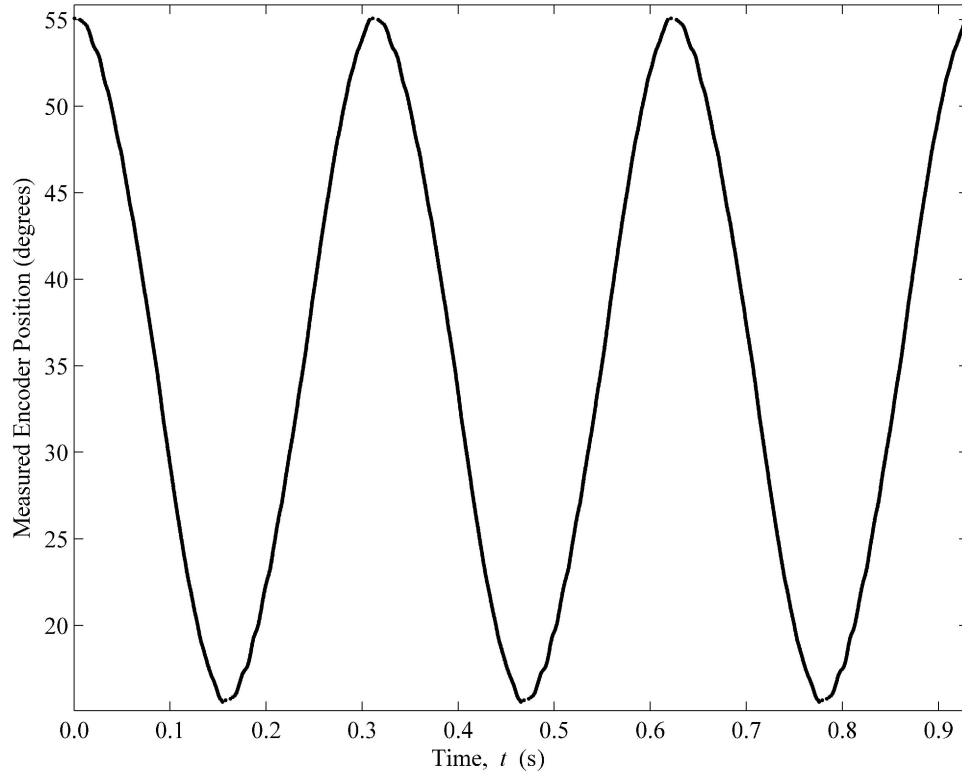


Figure 3.3. Repeatable motion of the wind tunnel model measured using an incremental encoder.

3.3.2 Removal of Inertial Loads

One of the challenges in conducting aerodynamic measurements for a dynamic model is removing the inertial component from the measured loads. For example, with instrumentation involving strain gauges, such as the present aerodynamic balance, measurements are a combination of the aerodynamic loads acting on the model and the inertial loads associated with the motion of the balance's centre spindle to which the model is attached. Various arrangements have been used previously to remove the inertial loads. For example, a second identical “dummy” model, with its own strain gauges, may be attached to the wind tunnel model but outside the airflow. The “wind-

off” inertial loads measured from the “dummy” model are removed from the corresponding measurements on the “wind-on” model in the wind tunnel [18,29]. Alternatively, a wind tunnel model incorporating an array of pressure taps may be used, where the discrete pressure readings are summed to yield the overall aerodynamic lift, drag, and moment [19,47]. These previous arrangements are costly, they require significant time to setup and, in the case of pressure taps, are not well suited for a realistic iced-conductor model whose exposed surface is irregular.

An alternative and simple strategy is taken in the present work. As Figure 3.3 shows that the automated motion controls can repeat a motion of the wind tunnel model, each dynamic test is conducted first in still air. Then the test is repeated at the desired wind speed. The still air measurements involve only inertial loads, while a “wind-on” test includes both the inertial loads and the aerodynamic loads acting on the dynamic model. As for the previous studies which used a “dummy” model, the difference between the measurements from the two tests yields just the aerodynamic loads of interest.

3.3.3 Post-Processing

Preliminary forced-vibration experiments showed that the not entirely rigid aerodynamic balance’s structural vibration response was present in the measured load cell data. Therefore a straightforward modal analysis [40] was performed on the balance to determine its natural frequencies and develop an appropriate strategy to remove the balance’s contribution in the aerodynamic data. An impact hammer was used to excite the balance’s side force, drag, and yawing moment channels, and the free-vibration

response after the impact was measured directly by using the balance's load cells.

Figures 3.4 through 3.6 show the free-vibration time histories and corresponding frequency spectra for the side force, drag, and moment channels, respectively. These frequency spectra were obtained by using Matlab's Discrete Fourier Transform function which is based upon the Fast Fourier Transform (FFT) algorithm [59].

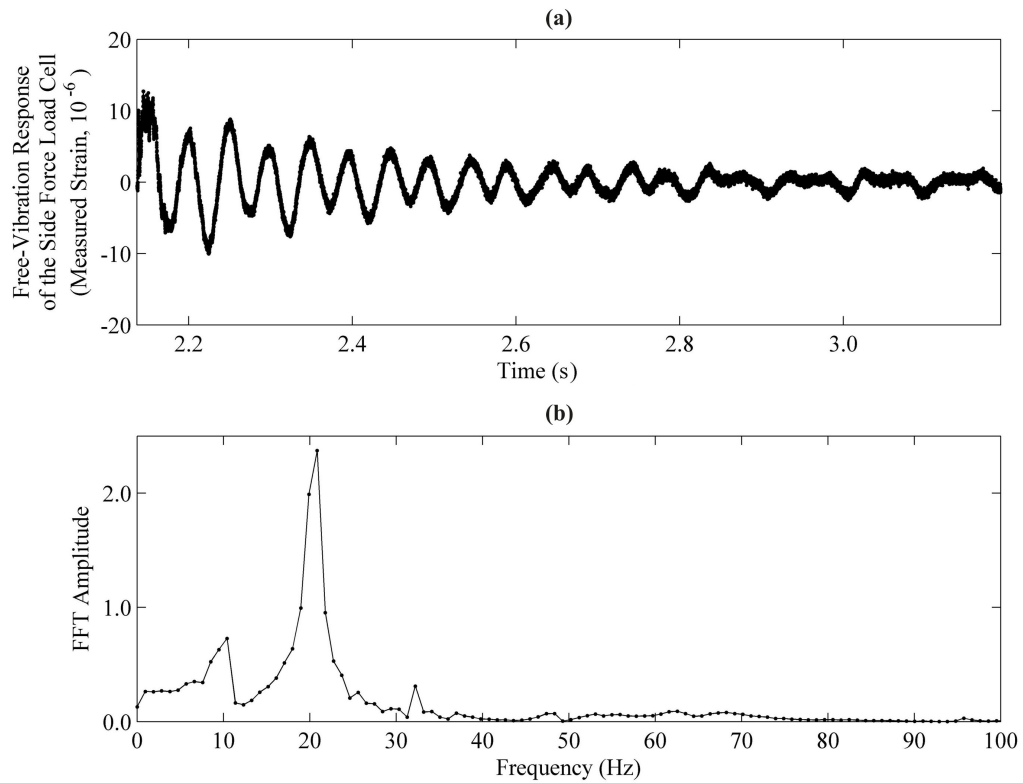


Figure 3.4. Free-vibration behaviour in (a) time response and (b) frequency spectrum of the side force data.

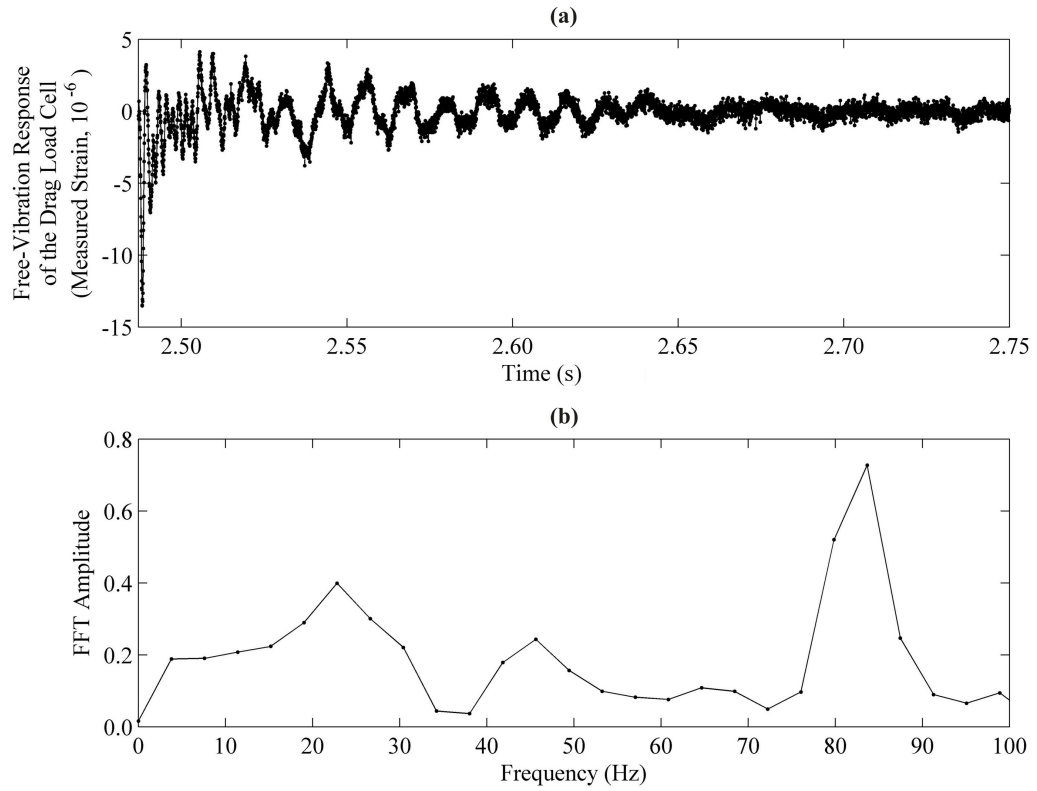


Figure 3.5. Free-vibration behaviour in (a) time response and (b) frequency spectrum of the drag data.

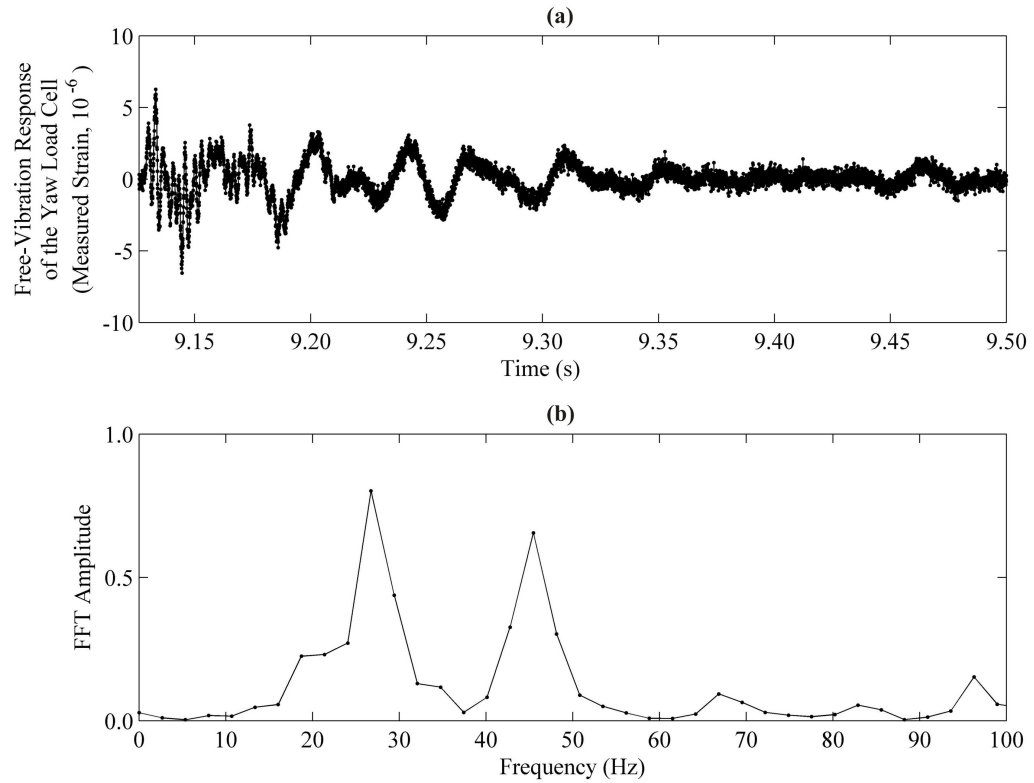


Figure 3.6. Free-vibration behaviour in (a) time response and (b) frequency spectrum of the yawing moment data.

The frequency spectra shown in Figures 3.4 through 3.6 indicate that the balance's natural frequencies are surprisingly lower than expected for a general purpose instrument; approximately 83Hz for the drag, 26Hz for the moment, and 21 and 11Hz for the side force channels. The relatively higher natural frequency in the drag direction is expected due to the corresponding cell's much thicker beam and higher stiffness than those for the side force and yawing moment channels. On the other hand, the 11Hz frequency component in the side force and its source are unclear. However, as the frequency range of interest in the present experiments is about 5Hz and lower, a digital low-pass filter is used in the post-processing stage to remove essentially all effects of the balance's structural vibrations. A suitable digital low-pass filter with a roll-off region in the range of 5-10Hz is required.

Before a suitable digital low-pass filter can be “constructed”, the sampling rate of the data must be decreased from 2000Hz to 80Hz by using Matlab's Decimate function [60]. Note that before the data are decimated, Matlab's Decimate function applies a low-pass filter to avoid aliasing the signals. The decimation process is needed as the roll-off region of a digital filter is specified by frequencies normalised by the Nyquist frequency, f_N . For example, at a sampling rate of 2000Hz, the Nyquist frequency is 2000/2 or 1000Hz. Consequently, the ideal roll-off region is defined by the narrow and unfeasible normalised frequency range of 5/1000 to 10/1000. However, after decimating the signals so that the Nyquist frequency is effectively 40Hz, the Matlab filter-design toolbox can easily produce a stable, finite-duration impulse response (FIR) low-pass filter, having a normalised transition region between 5/40 and 10/40. Figure 3.7 shows the resulting

digital low-pass filter's frequency response. Matlab's Filtfilt function is used with this filter to ensure zero-phase distortion [61].

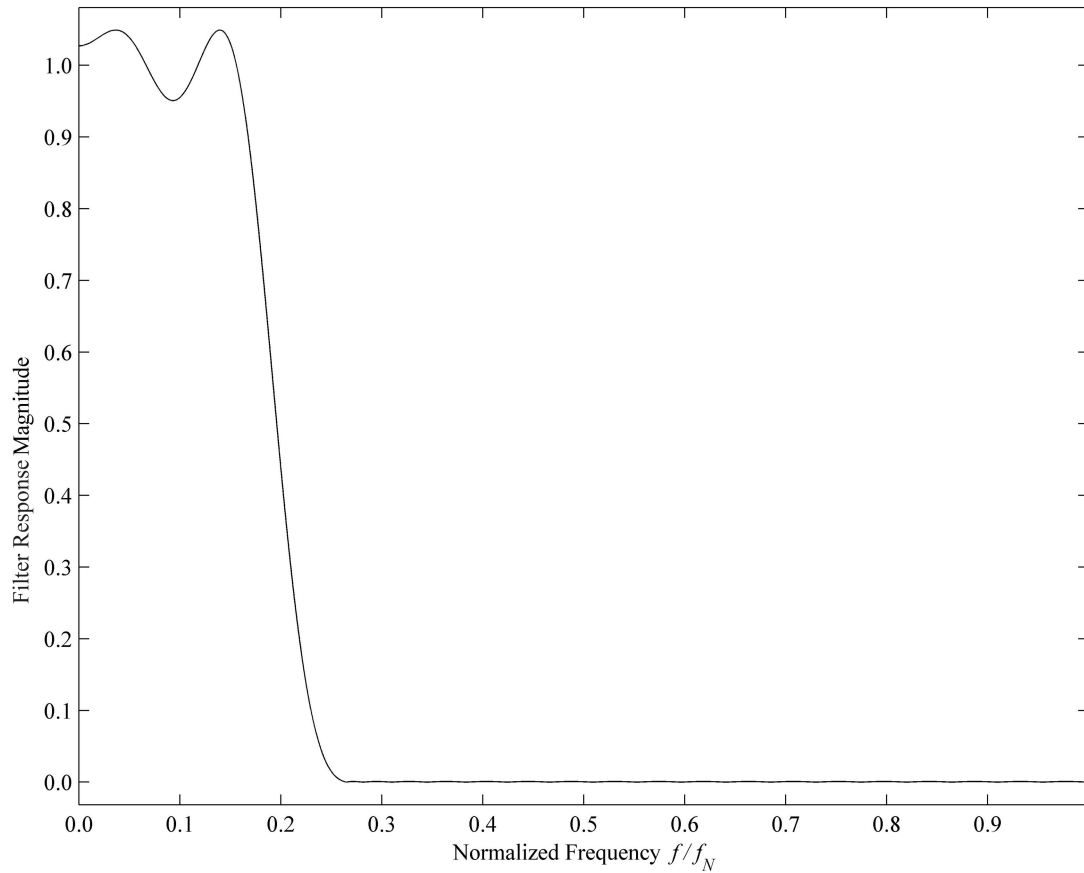


Figure 3.7. Frequency response of the low-pass digital filter used to remove the balance's structural vibrations.

In addition to the structural response of the aerodynamic balance, preliminary data showed unwanted harmonic components introduced from the stepper motor. For example, the aerodynamic data from a 1Hz forced-vibration test contained not only the expected 1Hz frequency component, but also minor harmonic components at 2, 3, 4Hz etc. The discrete nature of the operation of the balance's stepper motor caused these

harmonics to appear in the load cell measurements. To remove them in order to reduce experimental error in the aerodynamic data, a motor isolation strategy and replacement servomotor were considered. These solutions were not implemented, however, due to the significant time needed to realign and recalibrate the aerodynamic balance after making all the necessary adjustments. Instead, a simple averaging scheme was developed to minimise the effect of the stepper motor's harmonics on the aerodynamic data.

Due to the repeatable nature of the balance's automated controls, the cosine motion of the wind tunnel model can be repeated over an arbitrary number of cycles. In the post-processing stage, the measured aerodynamic loads over multiple cycles can be summed and then divided by the number of cycles to yield an average result. Figure 3.8, in which the aerodynamic loads are plotted versus the normalised time, shows the superimposed aerodynamic data from about 40 cycles of a 3.2Hz, $\pm 20^\circ$ steady-state cosine motion. (Note that the time is simply normalised by the periodicity of the motion, T_0 .) The average results and approximate distributions of the experimental data are also shown for comparison. As the distributions are approximately normal, the averaging strategy appears to be fairly reasonable, and the average aerodynamic loads seem to be representative of the overall collection of data from multiple cycles.

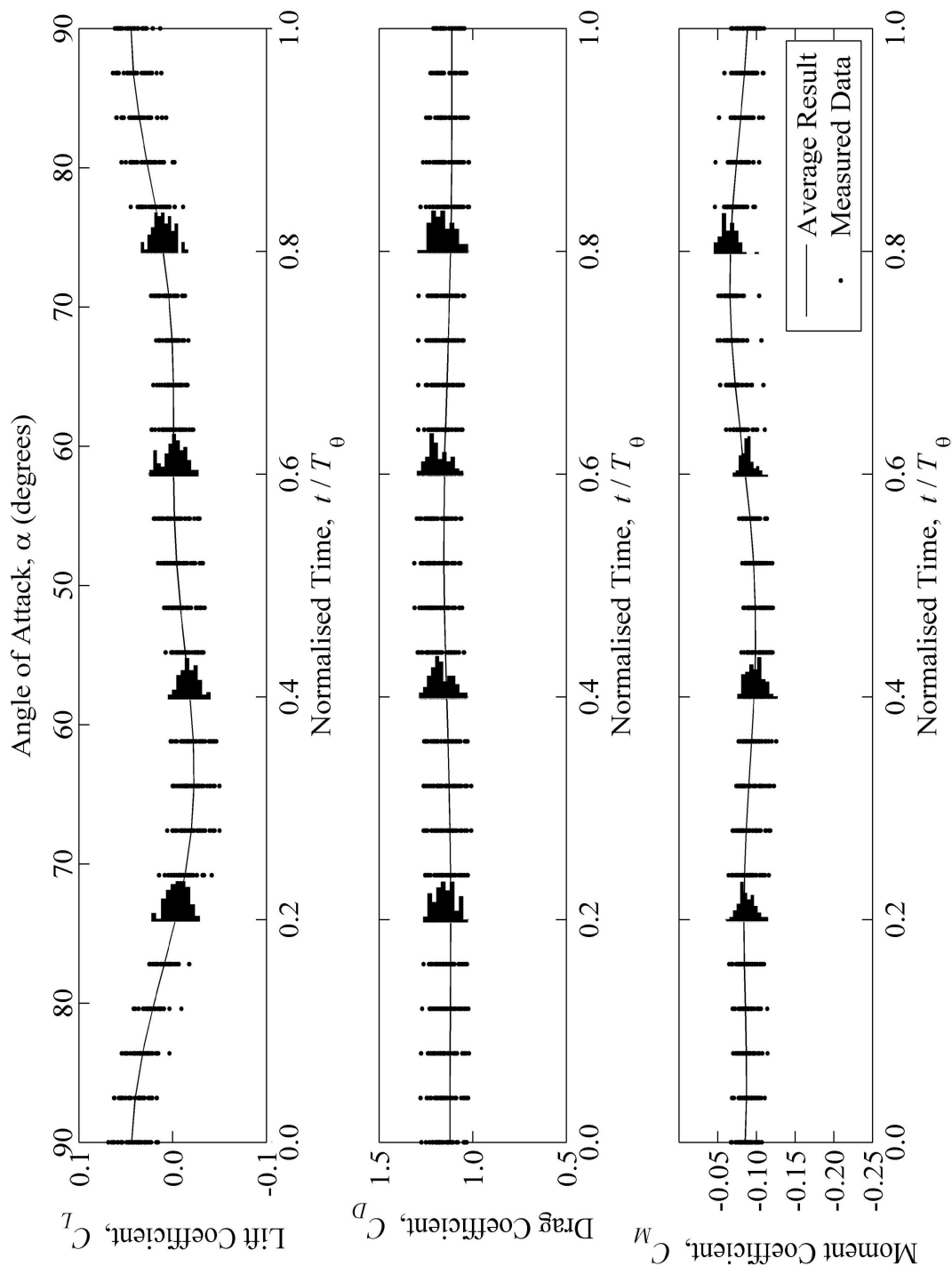


Figure 3.8. Superimposed aerodynamic data, approximate measurement distributions, and averaged results for a 3.2Hz, $\pm 20^\circ$ steady-state cosine motion.

The forced-vibration test associated with Figure 3.8 was repeated to confirm the repeatability of the previously averaged aerodynamic results. Figure 3.9 shows the new results from about 40 cycles of motion together with the averaged aerodynamic loads. Figure 3.10 superimposes the separately averaged aerodynamic loads from Figures 3.8 and 3.9. From Figure 3.10, the average lift and drag coefficients from the two individual tests indicate reasonably good agreement, i.e., within less than 0.01 and 0.02, respectively. These levels of agreement should be sufficient for identifying trends and perform relative comparisons between tests for the lift and drag coefficients. On the other hand, the average moment results given in Figure 3.10 have a significant discrepancy between the two nominally identical tests. Figures 3.8 and 3.9 show that the averaging process is not the cause of this discrepancy. Rather, the actual aerodynamic data from each test are following distinctly different trends. Although the motion of the wind tunnel model in both tests is accurate to $\pm 0.2^\circ$, even such small variation may likely be the cause of the distinctly different aerodynamic moments. This comparison demonstrates the sensitive nature of the aerodynamic moment and the experimental difficulties in its measurement. Although the aerodynamic moments are not repeatable, they are included subsequently for completeness.

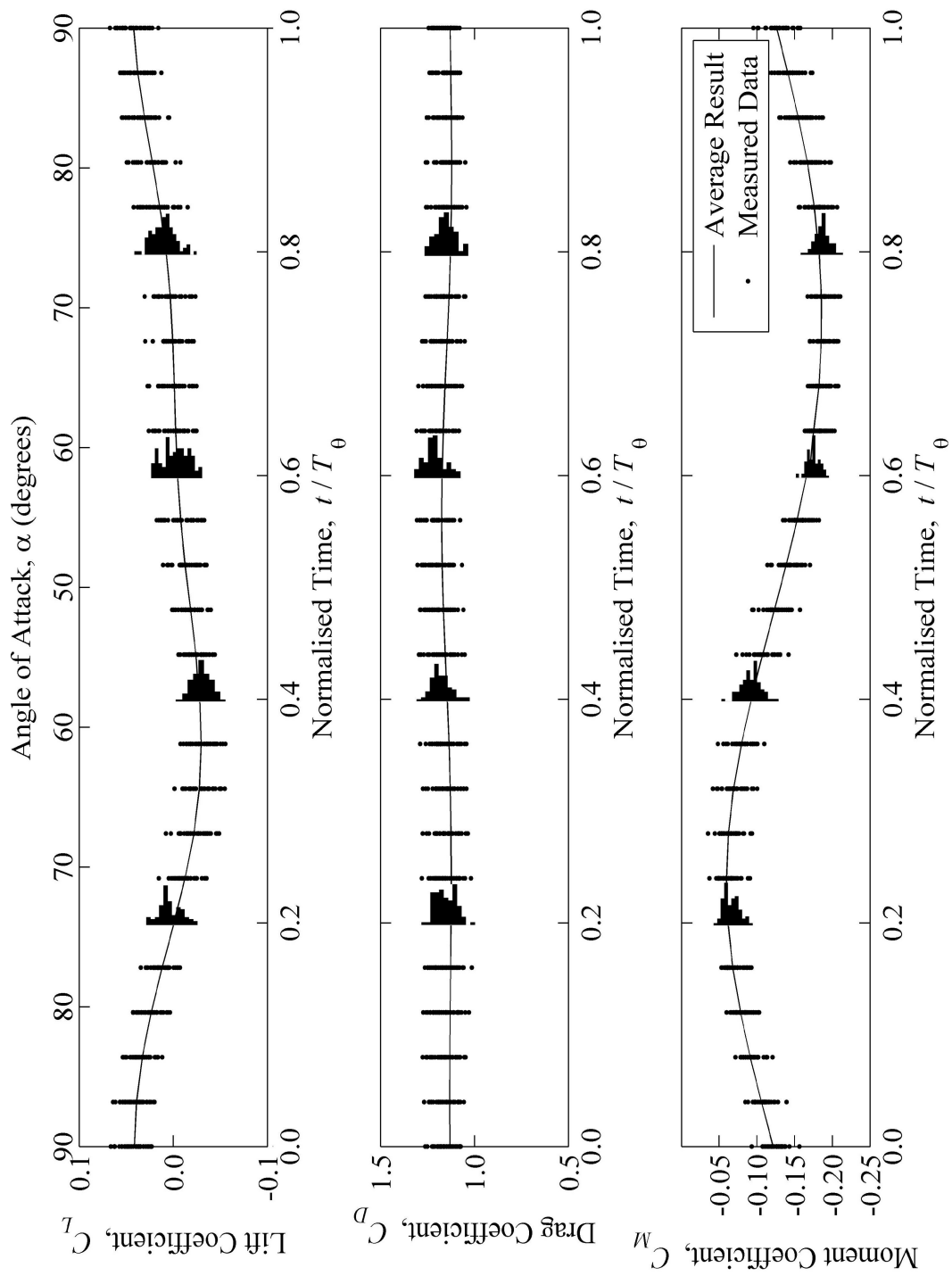


Figure 3.9. Superimposed aerodynamic data, approximate measurement distributions, and averaged results from a second test involving a 3.2Hz, $\pm 20^\circ$ steady-state cosine motion.

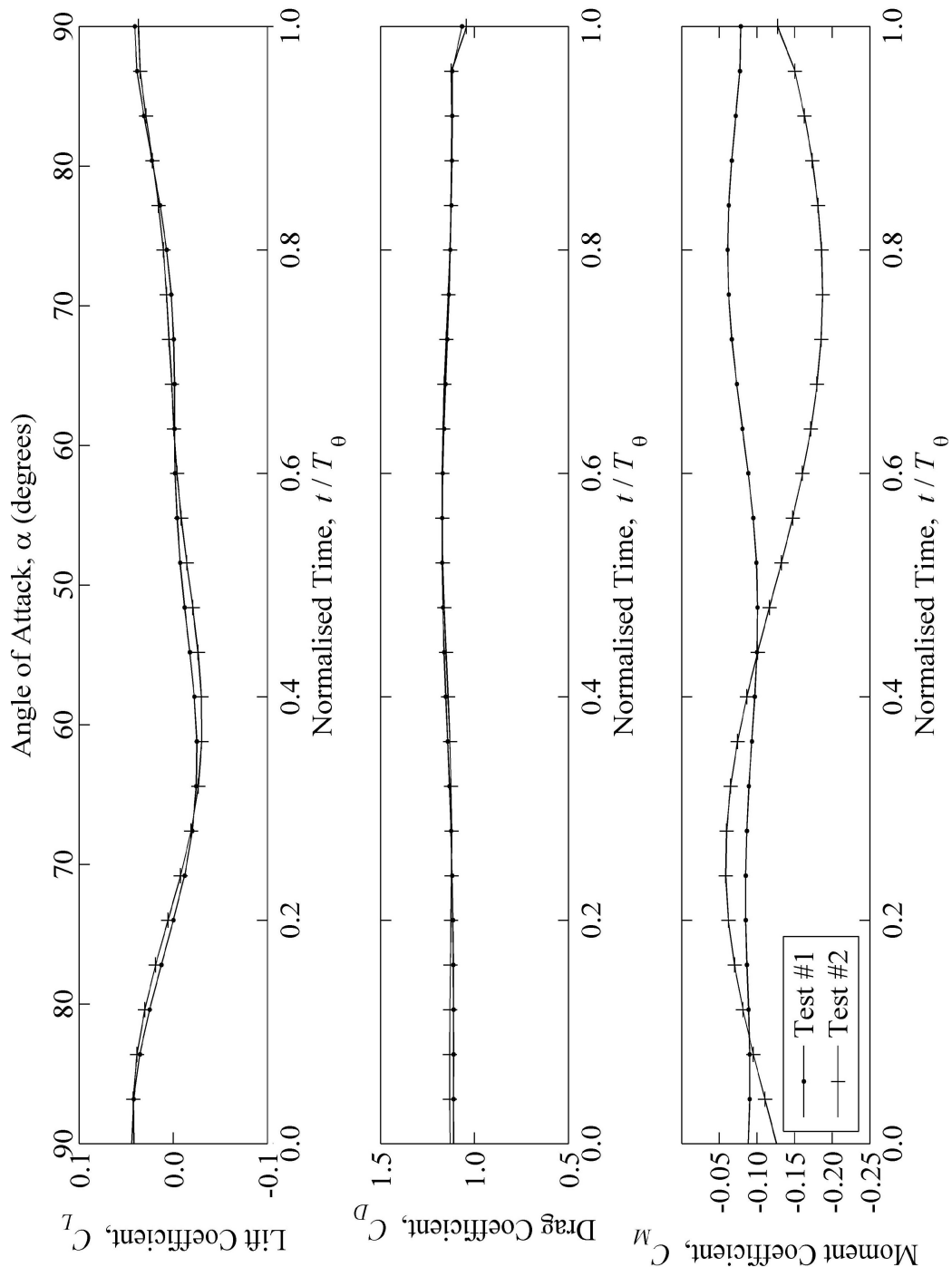


Figure 3.10. Comparison of the average results from the repeated forced-vibration tests associated with Figures 3.8 and 3.9.

3.4 Visualisation Experiments

Visualisation experiments are described next for the previous quasi-steady and forced-vibration tests. Only the most significant results are given because of the large number of images produced by the visualisation technique.

3.4.1 Camera and Laser Configuration

The camera and laser are arranged for the present visualisations in the same manner as the preliminary tests leading to Figure 2.28. The laser's 1mm thick light sheet forms a horizontal plane at the mid-height of the model and test section again. Based on initial testing, the best visualisations are achieved by setting the camera's gain to two, with the camera sensor's lower 8 bits being sampled at 200 frames per second. The laser's output power is set to about 4W throughout. Slightly higher power levels are expected to produce somewhat clearer images. Furthermore, the laser's internal optics require careful cleaning and realignment. These maintenance activities should be performed in the near future.

3.4.2 Visualisation of Separation Points

Due to the limitations of the present visualisation equipment, the wind tunnel model's two separation points cannot be visualised simultaneously. The model's second separation point, which is located opposite to the laser, is not illuminated due to the model blocking the laser sheet. This situation is illustrated in Figure 2.28. To simultaneously capture both separation points, two cameras are needed as well as costly

additions to the laser's optics so that multiple light-sheets can be projected. As the updated automated controls provide repeatable motions, an alternative approach is to conduct two separate visualisation experiments. The first experiment would capture one of the model's two separation points, as now. In the second experiment, the model would undergo the same motion, but it is rotated to a new initial angle of attack to enable the second separation point to be illuminated by the laser. Figure 3.11 illustrates the methodology of these two experiments. As lightly-iced conductor models are nearly symmetrical, and the flow in the wind tunnel is uniform, conducting these two separate visualisation experiments is expected to produce comparable results to simultaneously capturing both separation points.

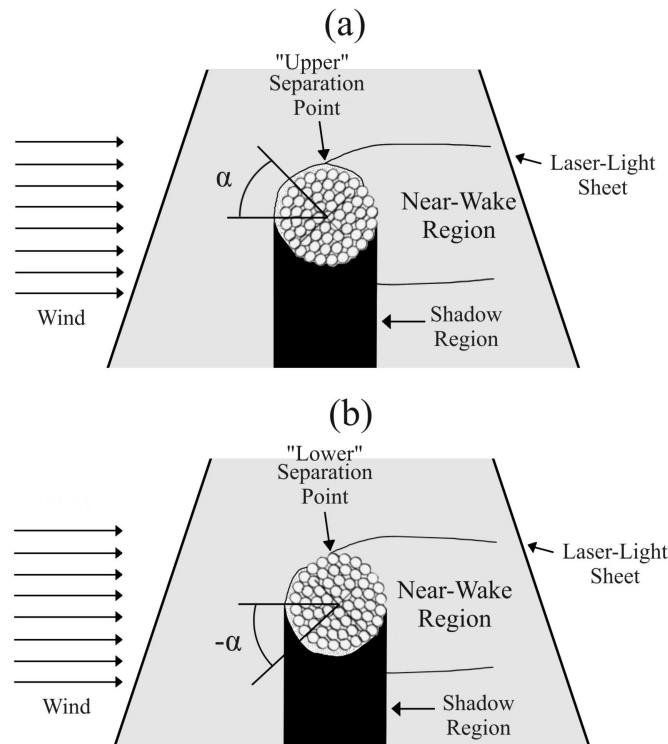


Figure 3.11. Illustration of experiments for visualising the (a) “upper” and (b) “lower” separation points.

4 EXPERIMENTAL RESULTS AND DISCUSSION

4.1 Quasi-Steady Results

The results from the quasi-steady testing are presented in Figure 4.1, where the normalised aerodynamic loads are plotted versus angle of attack for wind speeds of 10, 14, and 18m/s. For simplicity, the wind's direction is always normal to the conductor sample. Taking the bare conductor diameter, d , of 28.6mm as the characteristic length, these wind speeds correspond to Reynolds Numbers, Re , of about 18200, 25500, and 32700, respectively. Note that, although the typical wind speeds associated with galloping are between about 5 and 15m/s [7], the aerodynamic lift and moment generated at 5m/s are too small to measure with a general-purpose aerodynamic balance. The 18m/s test is included for comparison with a previous study [15] which claims that lightly-iced conductor profiles satisfy the den Hartog criterion for vertical galloping instability.

For the left-hand coordinate system (shown in Figure 3.1), the normalised den Hartog coefficient is simply

$$\frac{\partial C_L}{\partial \alpha} + C_D. \quad (4.1)$$

A negative of this coefficient implies that vertical galloping may be initiated from rest [21], or from a prescribed velocity (soft and hard galloping, respectively [2]). The den Hartog coefficient is computed by fitting the experimental lift and drag data given in Figure 4.1 with piece-wise cubic polynomials [32]. Although Parkinson and Smith [24] used a 7th order polynomial approximation, a fairly reasonable fit is achieved by using cubic polynomials over contiguous intervals of 30°. The experimental lift, drag and their

cubic fits, as well as the corresponding values of the den Hartog coefficient, are presented in Figures 4.2 through 4.4 for the three wind speeds.

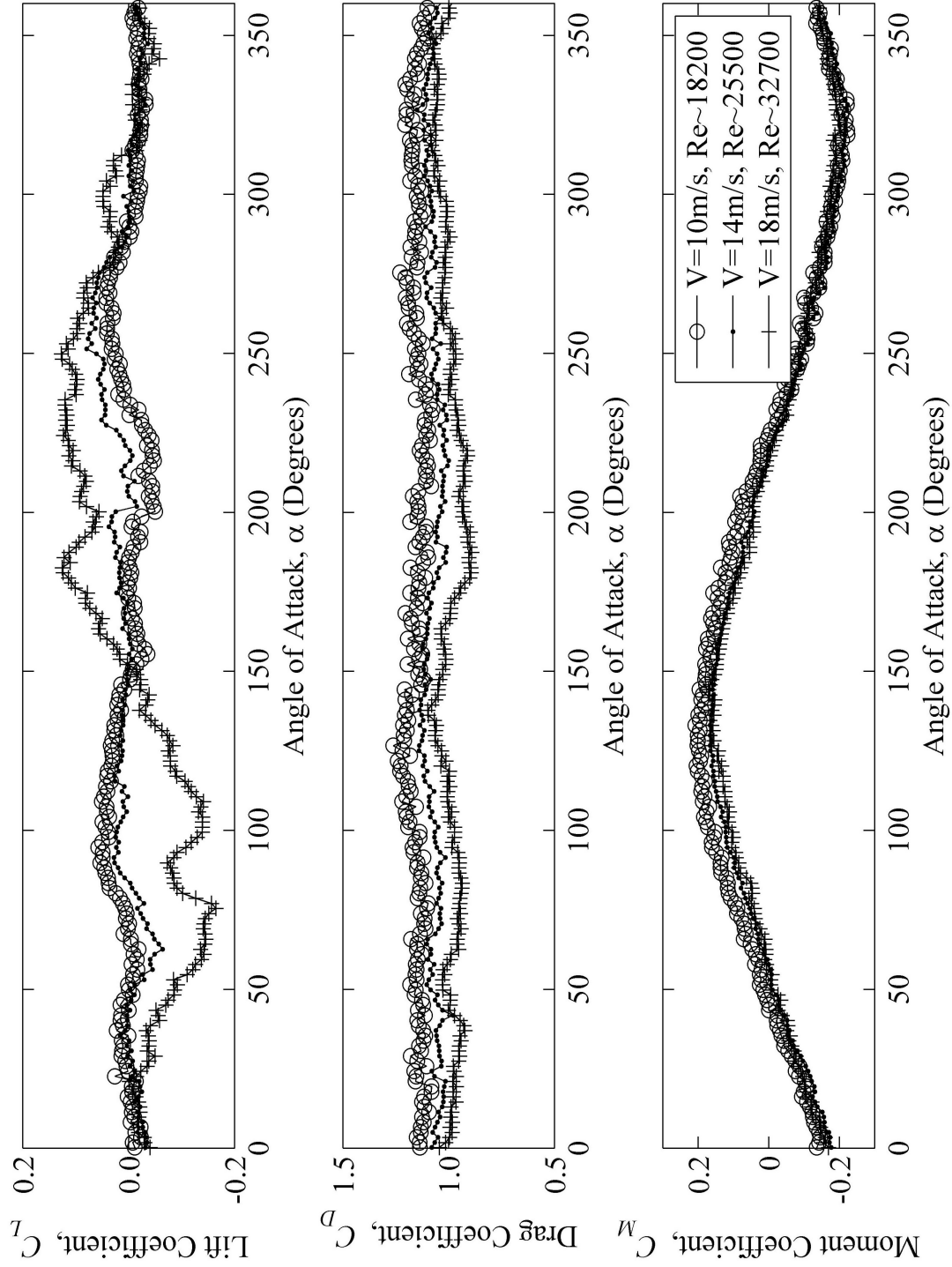


Figure 4.1. Quasi-steady aerodynamic results for wind speeds of 10, 14, and 18m/s.

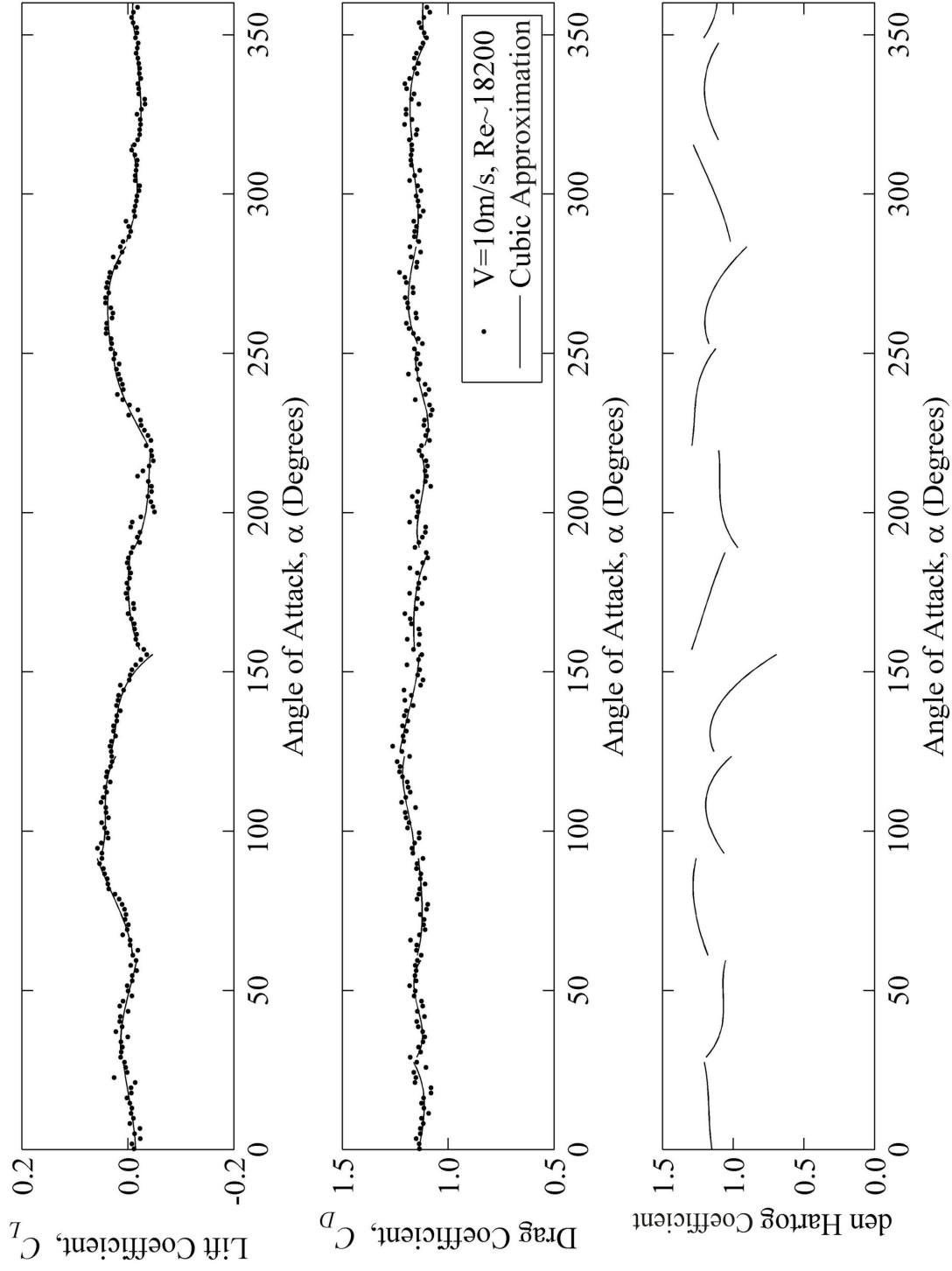


Figure 4.2. Quasi-steady lift, drag, and den Hartog results measured at 10m/s.

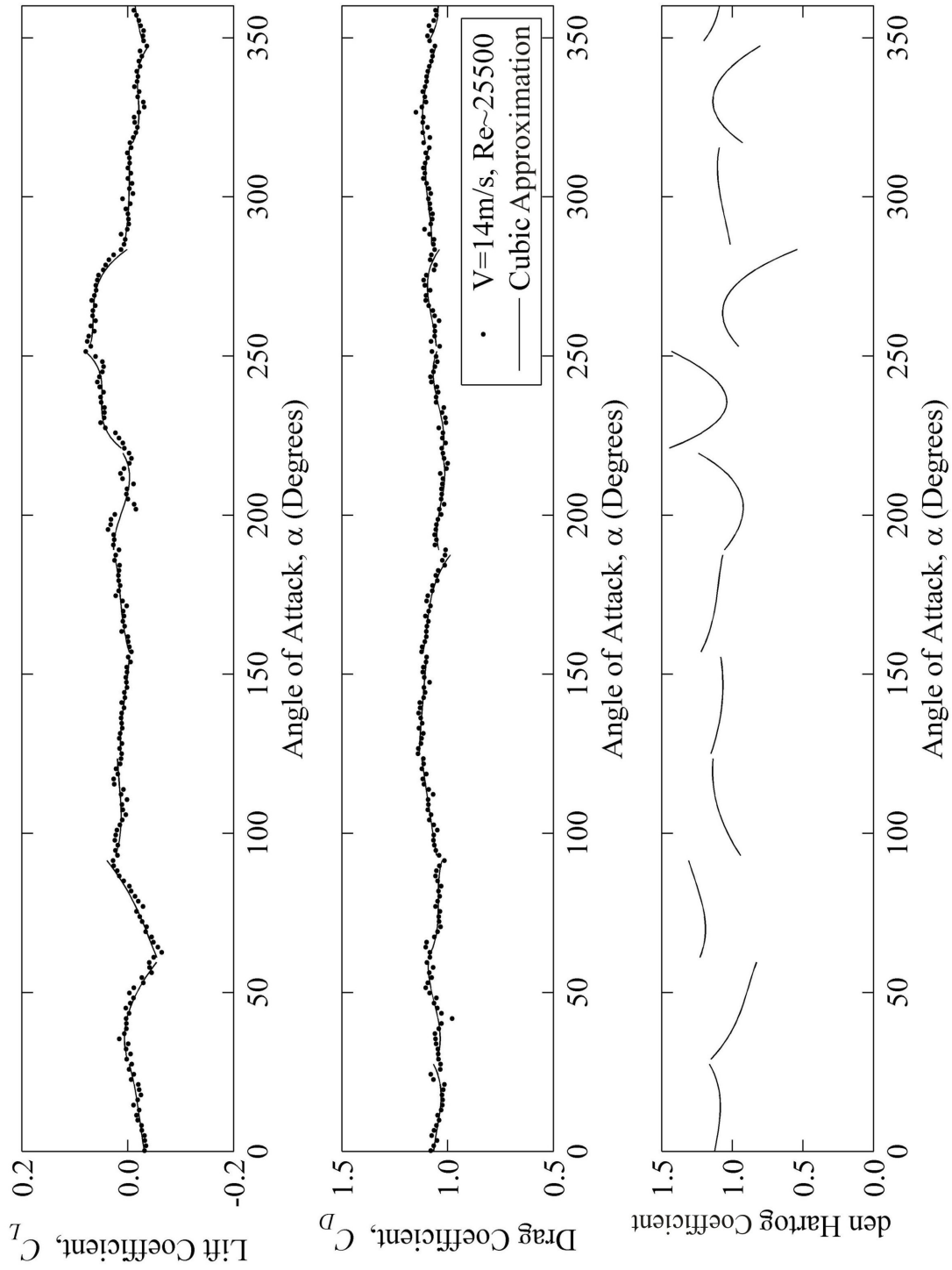


Figure 4.3. Quasi-steady lift, drag, and den Hartog results measured at 14m/s.

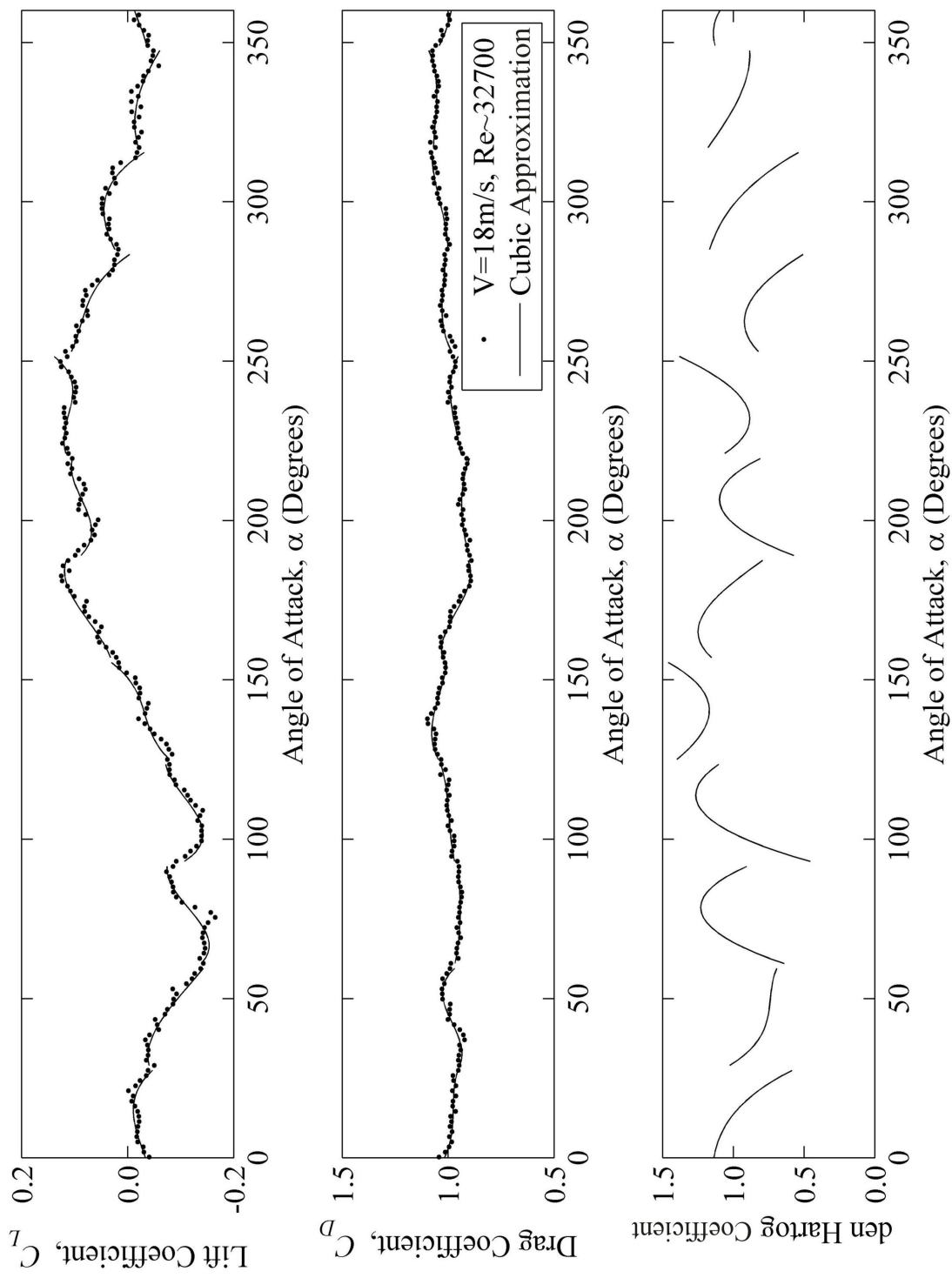


Figure 4.4. Quasi-steady lift, drag, and den Hartog results measured at 18m/s.

The effects of wind speed are seen in the quasi-steady lift and drag results presented in Figure 4.1. As the wind speed increases from 10 to 18m/s, the peak-to-peak level and slopes of the quasi-steady lift curve become larger, while the drag curve decreases from about 1.2 to 1.0. These changes generally lead to a greater possibility of satisfying the den Hartog criterion for vertical galloping. The wind speed or Reynolds Number effects also help explain the disagreement in the literature regarding lightly-iced conductor profiles and the den Hartog criterion. For example, Tunstall's study [15], which was conducted at about 18m/s with a slightly thicker, crescent ice accretion, showed that lightly-iced conductors satisfy the den Hartog criterion. In contrast, Ontario Hydro's tests [10] were conducted at somewhat lower and more representative wind speeds of 8 and 12m/s. They showed that the den Hartog criterion is not satisfied at any angle of attack, and that a spring-supported iced-conductor is stable in the purely vertical direction [10]. The present aerodynamic data and the resulting den Hartog coefficients, which are shown in Figures 4.2 through 4.4 for steady wind speeds between 10 and 18m/s, support the conclusion of Ontario Hydro. Therefore additional mechanisms must be involved in the galloping of lightly-iced transmission lines observed in the field.

In contrast to the quasi-steady lift and drag results, the aerodynamic moment data given in Figure 4.1 appear to show surprisingly little or no change with increasing wind speed. An explanation for this trend is unclear without information on the pressure distribution around the iced-conductor model. However the aerodynamic moment's positive slope, which is seen from 0° to 120° at all three wind speeds, may indicate rotational instability. Note that a positive slope for the quasi-steady moment implies that,

as the cross-section rotates to higher angles of attack, the aerodynamic moment also increases. The rotational motion and aerodynamic moment may reinforce one another to create an unstable situation. Additional experiments with a spring-supported, iced-conductor model are needed to test the hypothesis that self-excited rotational instability may occur in the region of 0° to 120° .

4.2 Forced-Vibration Results

Preliminary forced-vibration tests are conducted at 18m/s to further assess whether the post-processing strategies and wind tunnel instrumentation provide meaningful lift and drag data. For these preliminary dynamic tests, the model's initial angle of attack and amplitude of motion are kept constant at 35° and $\pm 20^\circ$, respectively. The frequency of the model's cosine motion is changed from 1.0Hz, 1.9Hz and finally to 3.2Hz. A steady wind speed of 18m/s is retained for all three tests. The aerodynamic loads measured in each dynamic test are superimposed in Figure 4.5, with the quasi-steady aerodynamic loads measured over the same $(35 \pm 20^\circ)$ angles of attack.

Figure 4.5 shows that, regardless of the rotational frequency, the dynamic tests produce virtually identical lift and drag results as the quasi-steady measurements at a wind speed of 18m/s. Therefore, the dynamic lift and drag results are at least credible.

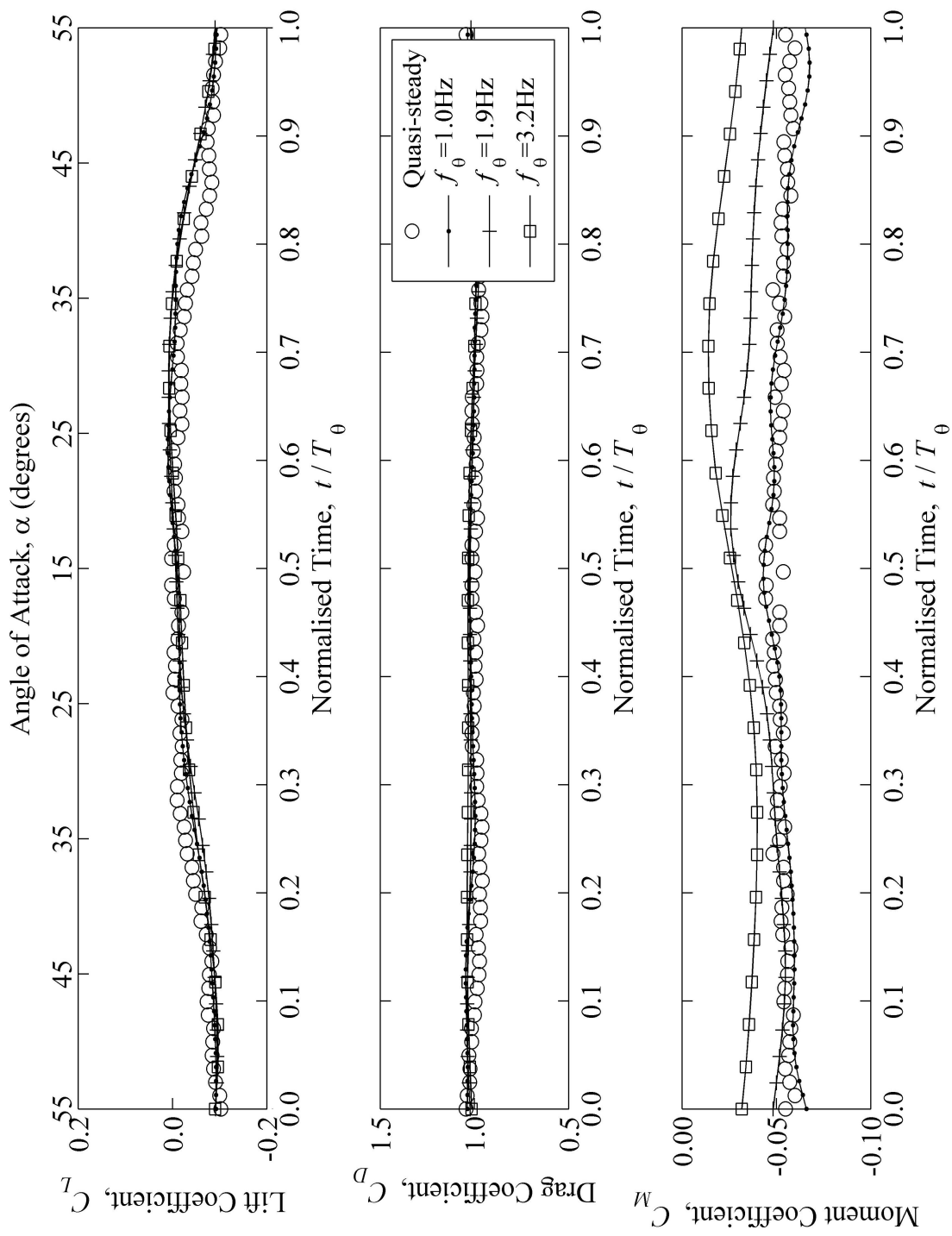


Figure 4.5. Quasi-steady and forced-vibration results measured at 18m/s, when $\alpha \neq 35^\circ$ and $A \neq 20^\circ$.

Dynamic tests are repeated at a more realistic wind speed of 10m/s. The model's initial angle of attack and amplitude of motion are kept constant at 35° and $\pm 20^\circ$, again but the frequency of the model's cosine motion is varied between the separate tests from 0.6 to 3.0Hz. The quasi-steady aerodynamic loads at 10m/s are compared in Figure 4.6 with the dynamic results for different rotational frequencies. The effect of changing the rotational amplitude to $\pm 10^\circ$ and $\pm 5^\circ$, on the other hand, is presented in Figures 4.7 and 4.8, respectively, where various rotational frequencies are considered at each rotational amplitude. In Figures 4.6 through 4.8, the initial angle of attack is always kept constant at 35° . Finally, Figures 4.9 and 4.10 show the effects of changing the initial angle of attack to 70° and 120° , respectively, for a rotational amplitude of $\pm 20^\circ$ and various rotational frequencies. A discussion of these results is given next.

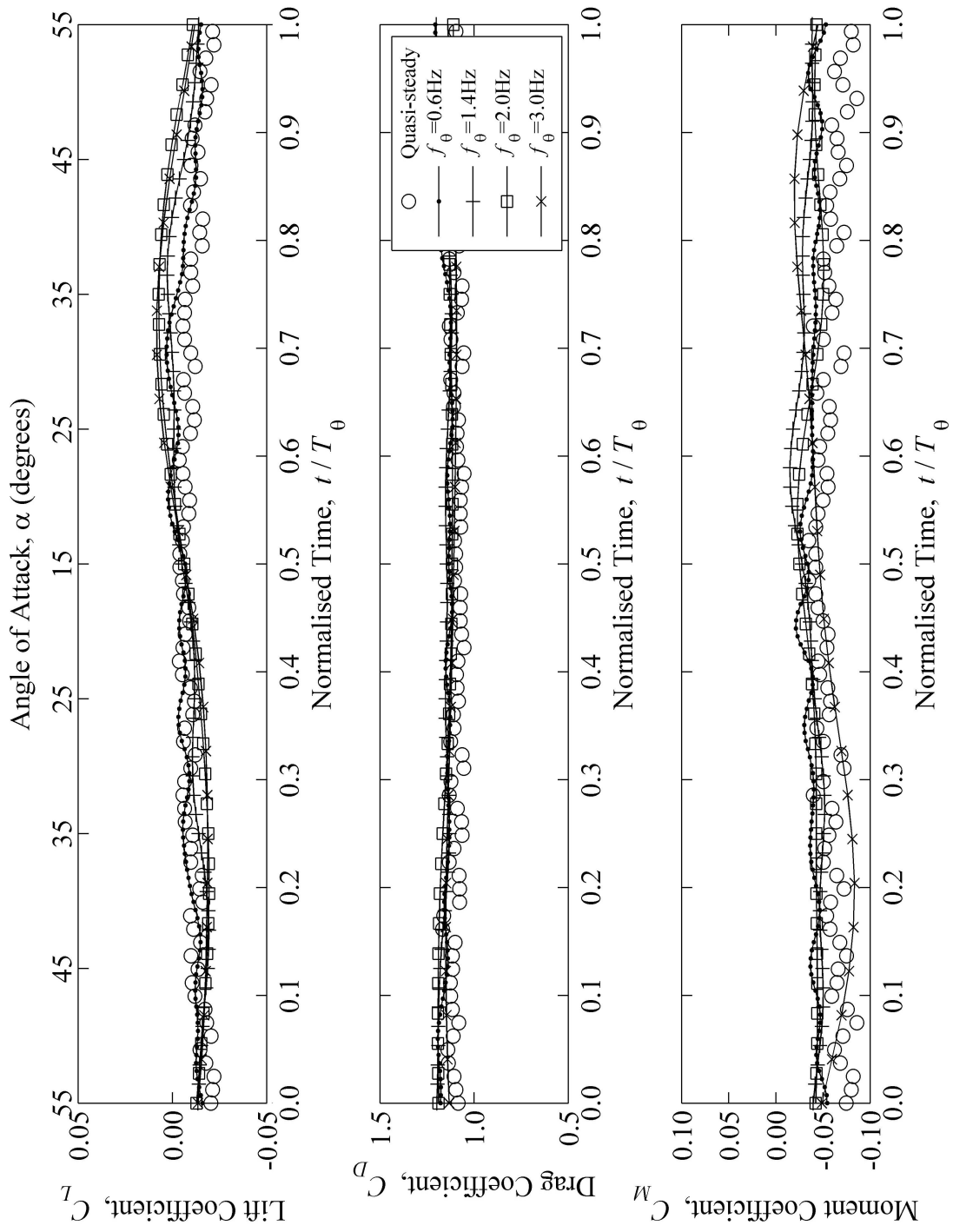


Figure 4.6. Quasi-steady and forced-vibration results measured at 10m/s, when $\alpha=35^\circ$ and $A=20^\circ$.

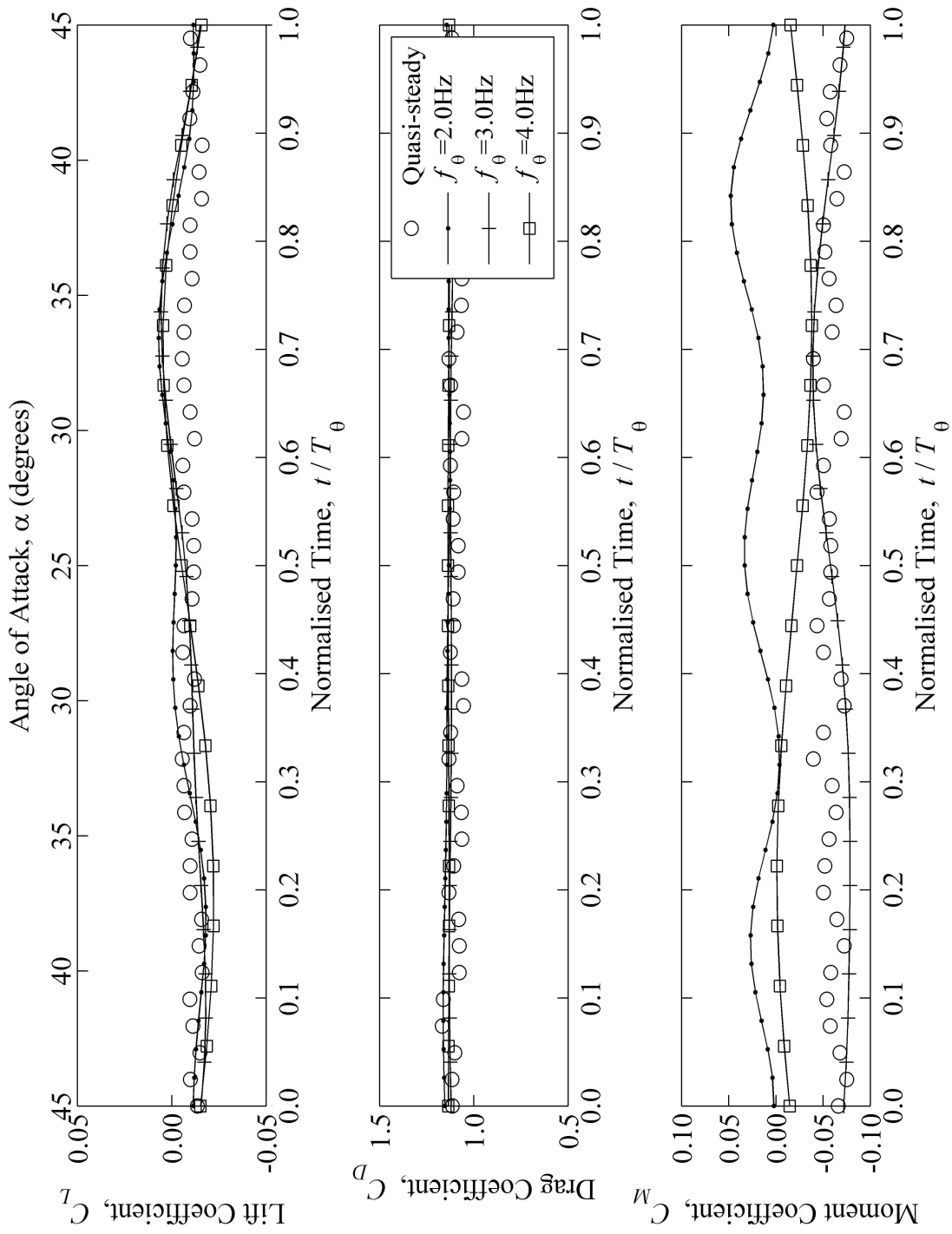


Figure 4.7. Quasi-steady and forced-vibration results measured at 10m/s, when $\alpha = 35^\circ$ and $A = 10^\circ$.

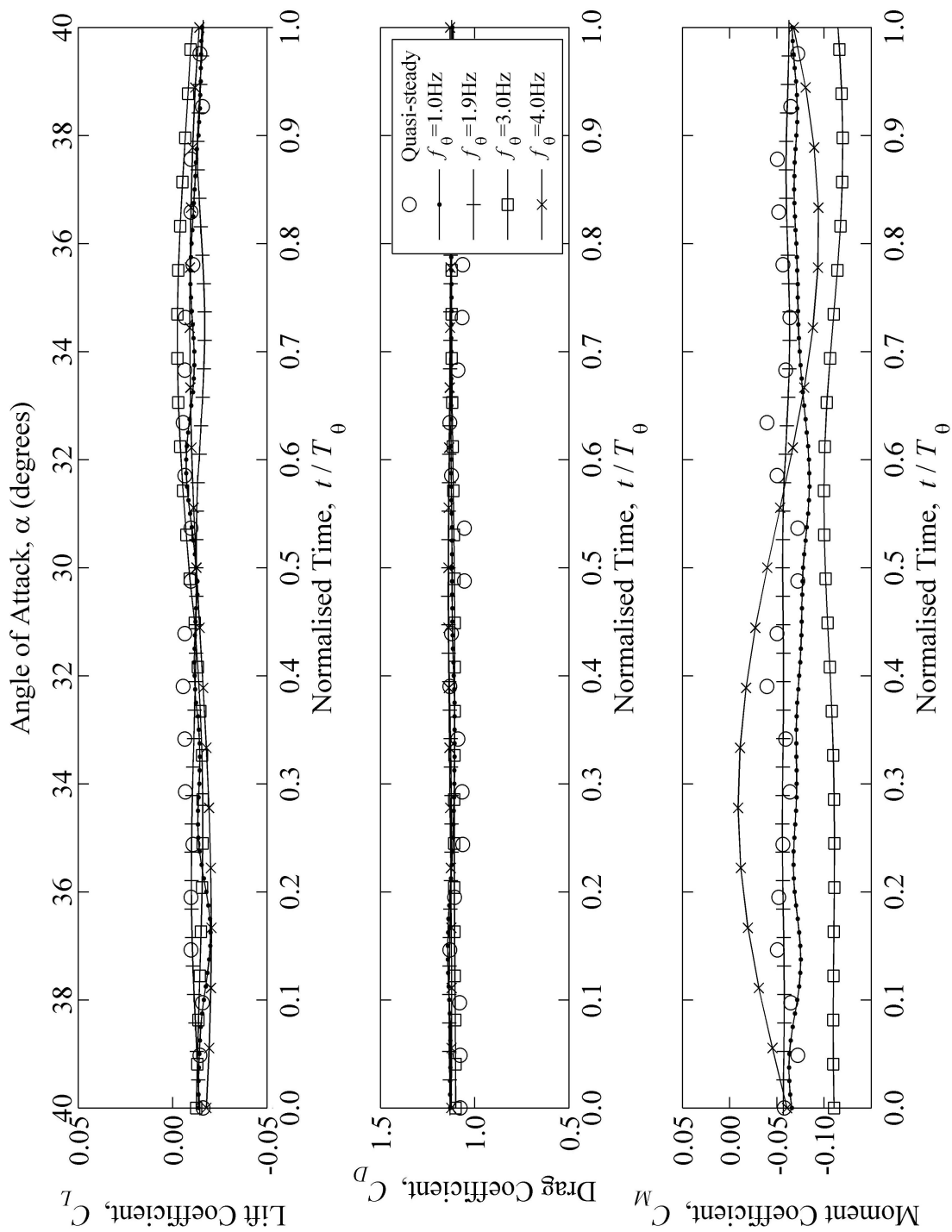


Figure 4.8. Quasi-steady and forced-vibration results measured at 10m/s , when $\alpha = 35^\circ$ and $\alpha = 5^\circ$.

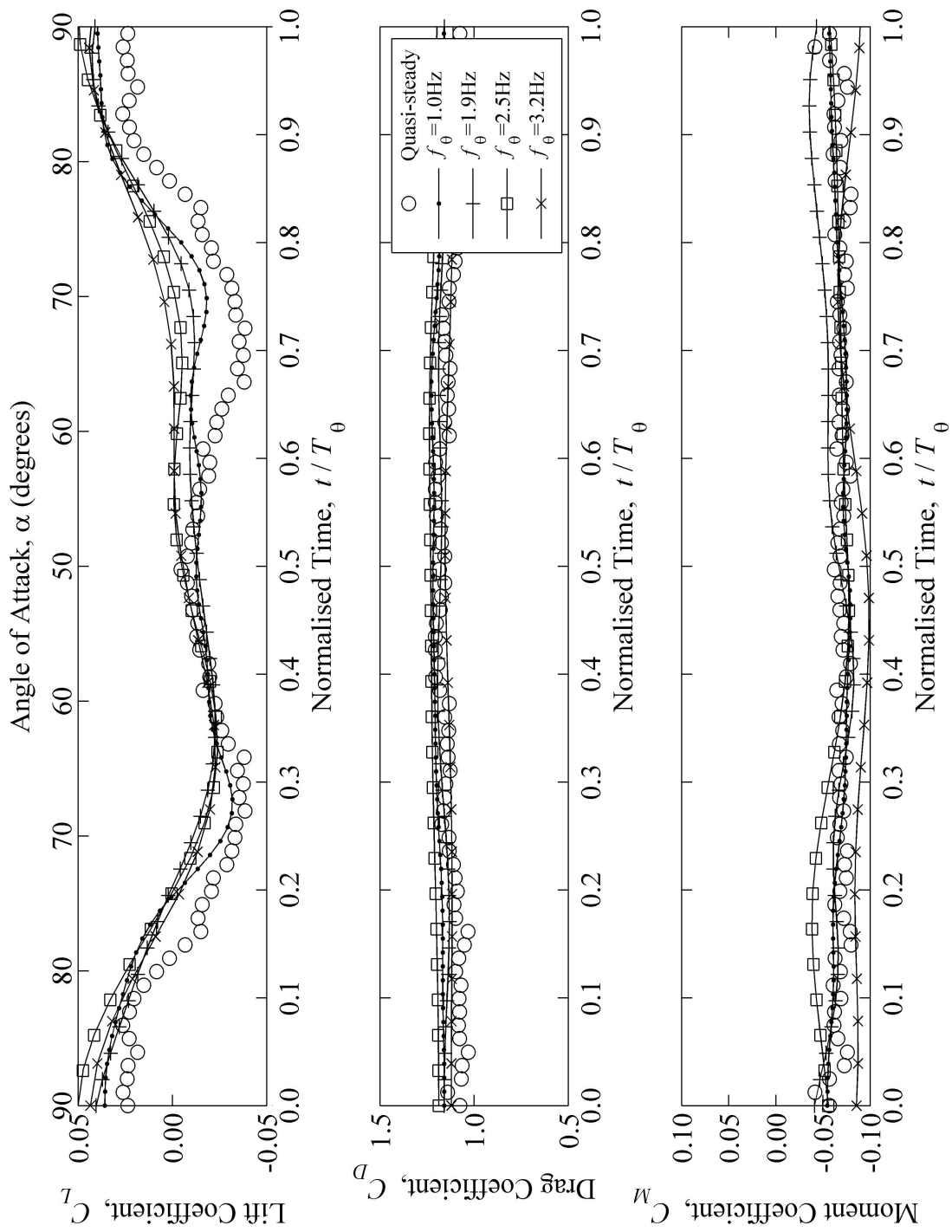


Figure 4.9. Quasi-steady and forced-vibration results measured at 10m/s, when $\alpha = 70^\circ$ and $A = 20^\circ$.

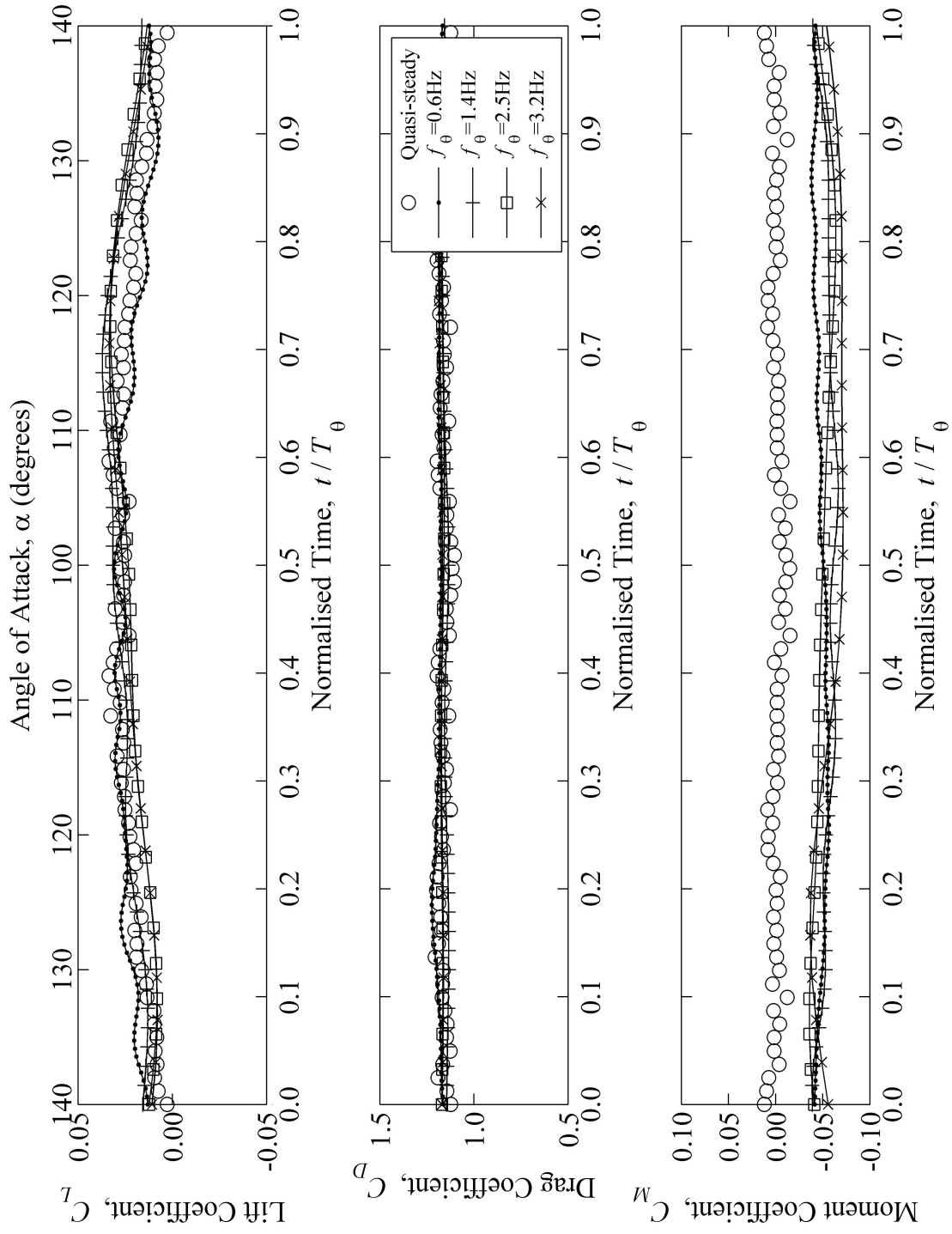


Figure 4.10. Quasi-steady and forced-vibration results measured at 10m/s, when $\alpha = 120^\circ$ and $A = 20^\circ$.

4.2.1 Effects of Rotational Amplitude

A comparison of the 3.0Hz dynamic test results given in Figures 4.6 through 4.8 shows that, as the rotational amplitude increases from $\pm 5^\circ$ to $\pm 20^\circ$, deviations from the quasi-steady lift become slightly more evident. Although these changes are subtle and within the uncertainty of the measurements before averaging, they are intuitively. To illustrate this assertion, note that the model's maximum angular speed is simply proportional to the product of the rotation's frequency and greatest amplitude. At low rotational amplitudes, such as the $\pm 5^\circ$ data presented in Figure 4.6, relatively good agreement is seen between the quasi-steady lift and drag and the corresponding dynamic results. These data suggest that the use of quasi-steady theory around the onset of galloping is appropriate. However, as galloping motions grow and rotations become larger in amplitude, corresponding, say, to the $\pm 20^\circ$ data shown in Figure 4.8, the quasi-steady theory and characteristic radius approach are unable to account for changes in the lift arising from the rotational motion. The significance of this additional rotation-induced lift force is examined more closely next.

4.2.2 Effects of Rotational Frequency

Figures 4.6, 4.9, and 4.10 show that as the rotational frequency increases, with a constant rotational amplitude of $\pm 20^\circ$, the drag always remains consistent with its corresponding quasi-steady values. On the other hand, the dynamic lift tends to deviate from the quasi-steady values. These deviations are likely induced by the rotational motion. As the model's motion is a periodic cosine profile, the rotation is initially

counter-clockwise, but clockwise in the first and second half of the cycle, respectively. Figure 4.6 shows that, when the quasi-steady lift is practically constant or insensitive to changes in the angle of attack, the additional lift caused by the counter-clockwise motion is negative in the first half of the cycle, but positive in the second half cycle. These results agree with the classical theory for spinning cylinders, in which clockwise and counter-clockwise rotations produce positive and negative lift forces, respectively [38].

Figures 4.6 and 4.9 further show that the dynamic lift deviates from the quasi-steady values more noticeably in the second half of the cosine motion. This result is most likely caused by a short-term fluid-memory effect similar to that observed by Nakamura [18]. The airflow surrounding the model begins to compensate the counter-clockwise rotation but it is then perturbed further by the immediately following clockwise motion. However, because the model is forced by the automated controls to remain in a repeatable steady-state motion, any possible fluid-memory effects are not allowed to buildup progressively in subsequent cycles of the oscillation. A spring-supported model may be useful to examine such fluid-memory effects more closely. Such models can allow the rotational motion to buildup slowly under unstable conditions, thereby allowing any fluid-memory effects to also buildup with increasing rotational amplitude.

The relatively low 0.6Hz rotational test results given in Figure 4.6 show reasonable agreement with the quasi-steady lift and drag. However, the lowest natural torsional frequency of a typical lightly-iced transmission is usually several times higher than 0.6Hz [4]. Torsional frequency may gradually decrease to around 0.6Hz with increasing icing and time [2]. The dynamic results suggest that a rotational oscillation at

these initially higher frequencies may produce a periodic lift which is superimposed on the quasi-steady lift. This type of aerodynamic coupling phenomenon may be an important aerodynamic mechanism acting in the galloping of lightly-iced transmission lines. Such a linking of the rotational and vertical DOFs has been suspected in previous studies although no experimental testing was performed [37]. Note that the repeatability of the present results has been confirmed when averaging over about 40 cycles of motion. However, due to the relatively high levels of uncertainty in the basic measurement data, additional experiments should be performed to establish more confidence in the present findings. A simple but custom-built aerodynamic balance which is designed for more sensitive lift measurements is needed.

4.2.3 Effects of Initial Angle of Attack

A comparison of Figures 4.6, 4.9, and 4.10 shows that both the quasi-steady and dynamic lift results depend strongly on the initial angle of attack. For example, in the case when α_0 is 70° , as shown in Figure 4.9, the quasi-steady lift exhibits a relatively steep positive slope. This behaviour suggests that the airflow's separation points and the ensuing flow field around the iced conductor are quite sensitive to rotations and changes in the model's angle of attack. This behaviour is confirmed in subsequent visualisations. The increased sensitivity of the flow field in this region results in higher deviations from the quasi-steady lift found in Figure 4.9. In regions where the lift curve is relatively flat, such as in Figures 4.6 and 4.10, a rotation-induced lift is still seen in the dynamic results, although not to the same extent as that observed in Figure 4.9. In such cases, the flow

field is likely less sensitive to changes in the angle of attack and the rotational motion. These results have important implications as the den Hartog explanation of iced-conductor galloping is based upon regions where the quasi-steady lift curve is steep and negative. The present results suggest that such regions, as well as those featuring positive lift slopes, exhibit the most noticeable deviations from the quasi-steady theory. These deviations may lead to stronger galloping behaviour due to the increased coupling between the rotational and vertical DOFs.

The case of $\alpha_0=120^\circ$ shown in Figure 4.10 represents a 180° wind reversal after the icing of conductor. Thus, the majority of the ice is located in the near-wake region. Although only a few wind-reversal cases have been noted in the literature [2], they are discussed briefly here for completeness.

A comparison of Figures 4.6 and 4.10, where in the former case α_0 equals 35° rather than 120° , shows that a wind reversal yields very similar results to the non-reversed case. However, a comparison of Figures 4.9 and 4.10, where α_0 equals 70° and 120° , respectively, the dynamic results show that the wind-reversal case leads to less severe deviations from the quasi-steady theory. The contradictory results from these two comparisons suggest that the icing's geometry and the slope of the quasi-steady lift curve are more influential than a simple wind reversal. However, Figure 4.2 shows that the steepest slope on the quasi-steady lift curve occurs at about $\alpha_0=150^\circ$. A wind-reversal is needed to achieve such a large initial angle of attack, as icing typically grows into the wind near the upper portion of the conductor. Due to the steep lift slope in this region, Figure 4.9 suggests that significant aerodynamic coupling and deviations from the quasi-

steady theory are to be expected. Before further testing is conducted to assess the importance of a wind reversal, more evidence from the field is needed to support the claim that a wind reversal is encountered commonly before galloping is observed.

4.2.4 Effects of Inertial and Aerodynamic Coupling

The previous discussions emphasise the effects of rotation-induced lift and the resulting aerodynamic-coupling effects seen in the dynamic results. These dynamic tests, however, include only aerodynamic forces. Recall that significant efforts have been made to remove the unwanted inertial loads associated with the aerodynamic balance and the wind tunnel model. (See Section 3.3.2.) However, due to the slightly eccentric nature of a lightly-iced conductor, a small inertia-coupling effect, which is proportional to rotational acceleration, is present between the rotational and vertical DOFs. On the other hand, the rotation-induced lift seen in the aerodynamic tests is related to the rotational speed. As galloping characteristically involves simple harmonic motion [2], both the vertical inertial force and the aerodynamic lift caused by rotations are periodic but they do not cancel. They may actually reinforce one another to yield greater coupling and instability between the rotational and vertical DOFs. Any inertia-coupling effects, however, depend strongly on the amplitude and phase of the vertical and rotational motions, the eccentricity of the iced-conductor's cross-section, and the initial angle of attack. Additional experiments, which also include vertical motion, are needed to better understand the complex relationship between an inertia and aerodynamic coupling. The previous recommendation for further experiments with a spring-supported model could

also include the effects of inertia-coupling providing the model is designed appropriately.

4.3 Visualisation Results

Visualisations from the quasi-steady testing are presented in Figures 4.11 and 4.12. The static angle of attack in these figures is changed from 90° to 50° in increments of 10° . This range of angles coincides with the relatively steep quasi-steady lift curve given in Figure 4.9. (A steep lift curve implies that the lift is sensitive to small changes in the angle of attack.) The most significant deviations from the quasi-steady lift values are also seen in this range, as illustrated by the 3.2Hz dynamic test data shown in Figure 4.9. Figure 4.13 summarises the quasi-static visualisations. It incorporates the images of Figures 4.11 and 4.12 for easier comparisons. Figure 4.13 also labels the different separation points associated with each angle of attack considered.

Visualisations from the 3.2Hz dynamic test, which correspond to the data of Figure 4.9, are shown in Figures 4.14 through and 4.17. They identify the instants when the model undergoes a counter-clockwise (CCW) and a clockwise (CW) motion. Figure 4.18 summarises the dynamic visualisation results at instants corresponding to 50° , 70° , and 90° angles of attack. The flow separation points' locations from the dynamic and corresponding quasi-steady visualisations are also labelled in Figure 4.18 for comparison. A discussion of these comparisons is given next.

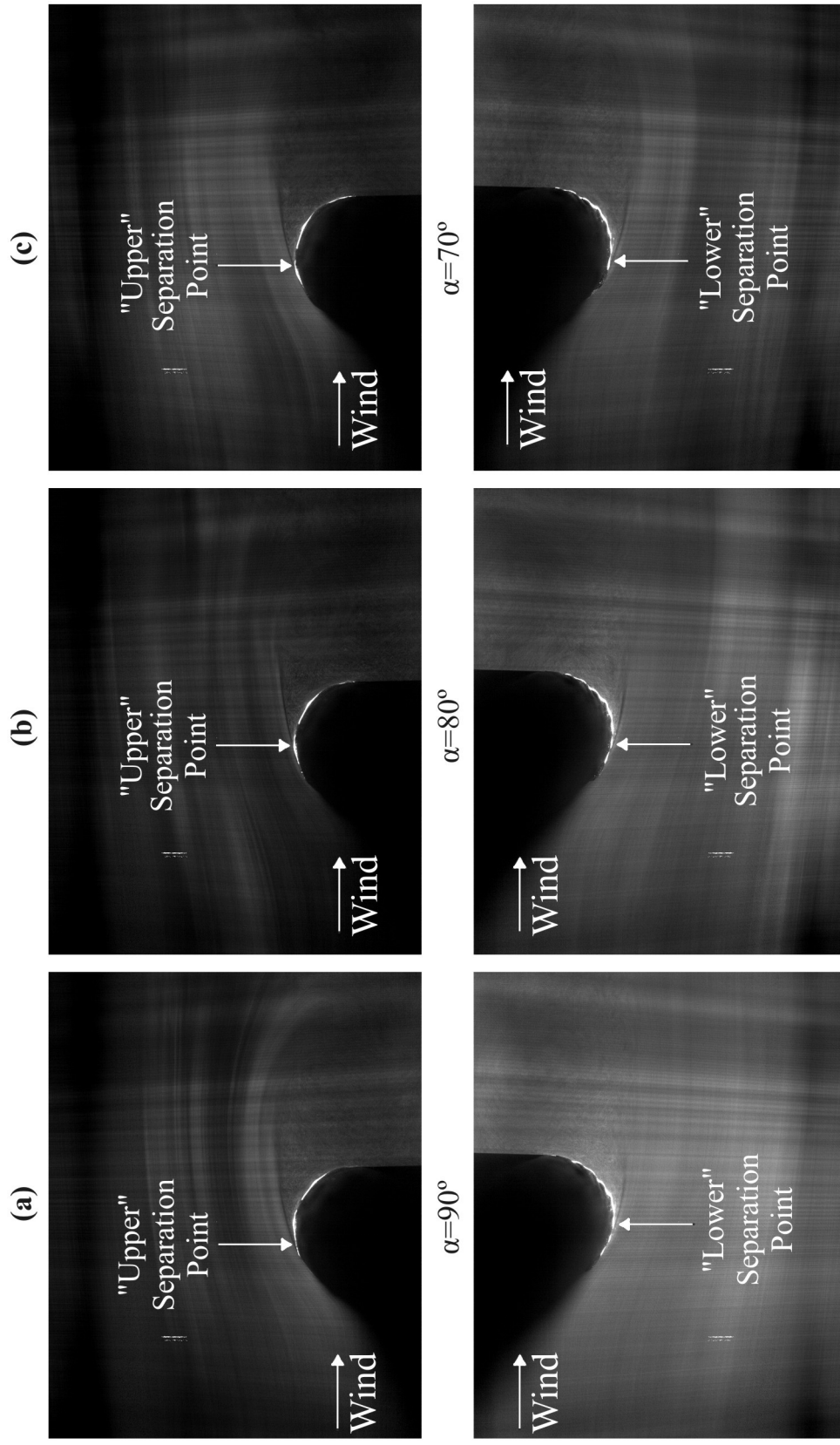


Figure 4.11. Visualisations of quasi-steady results at (a) 90° , (b) 80° and (c) 70° angles of attack.

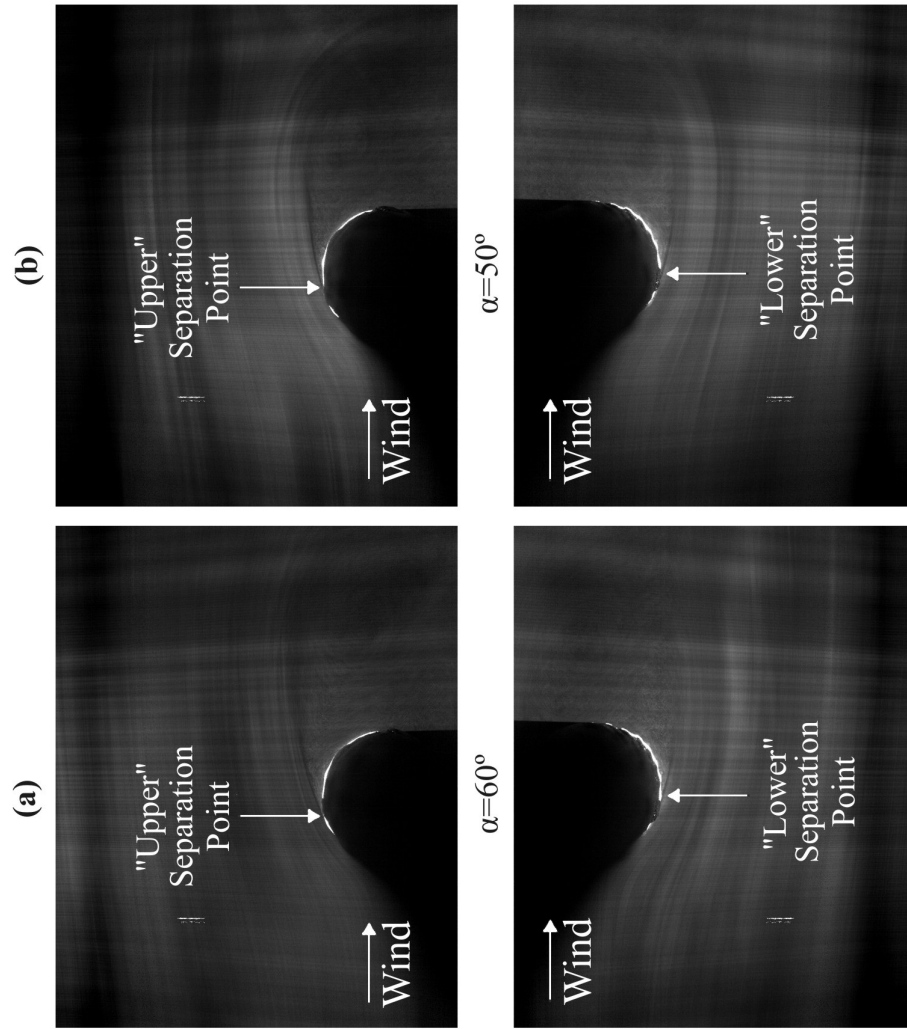
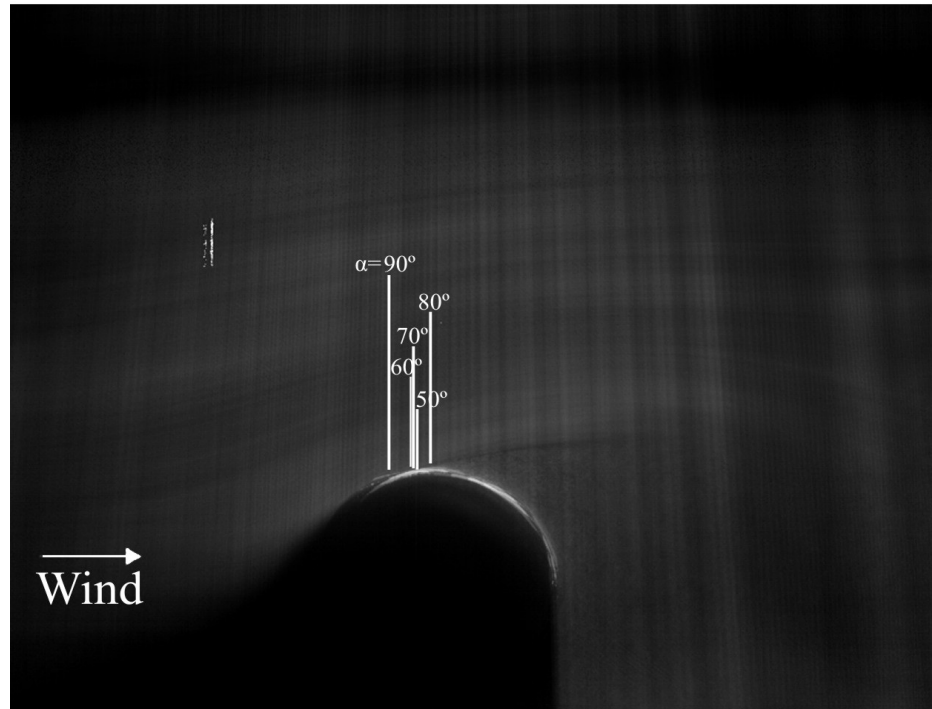


Figure 4.12. Visualisations of quasi-steady results at (a) 60° and (b) 50° angles of attack.

(a)



(b)

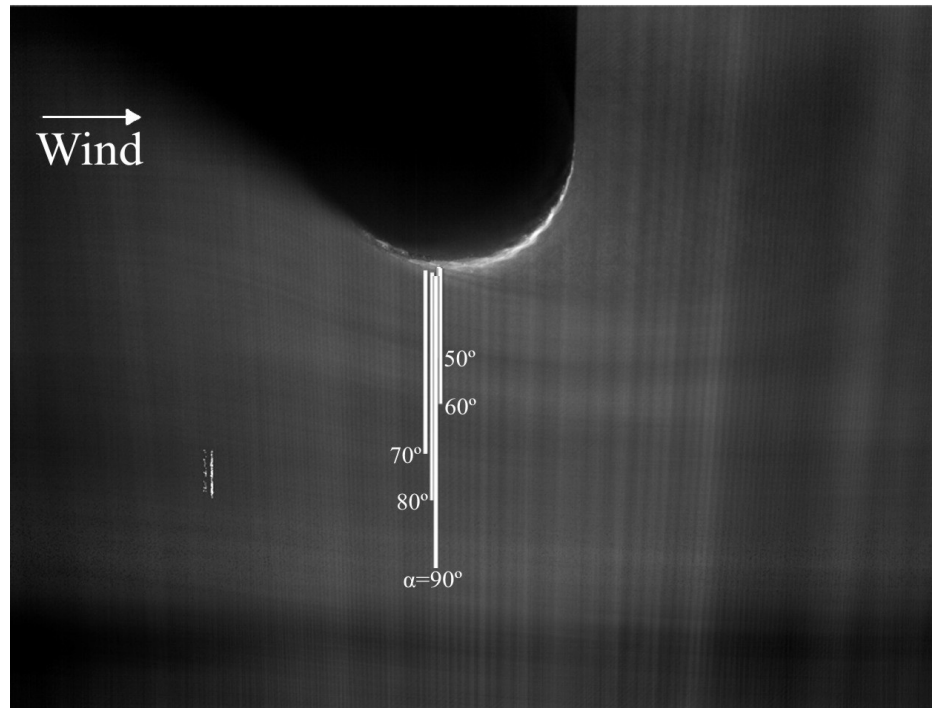


Figure 4.13. Superimposed quasi-steady visualisation results from Figures 4.11 and 4.12 for the (a) “upper” and (b) “lower” separation points.

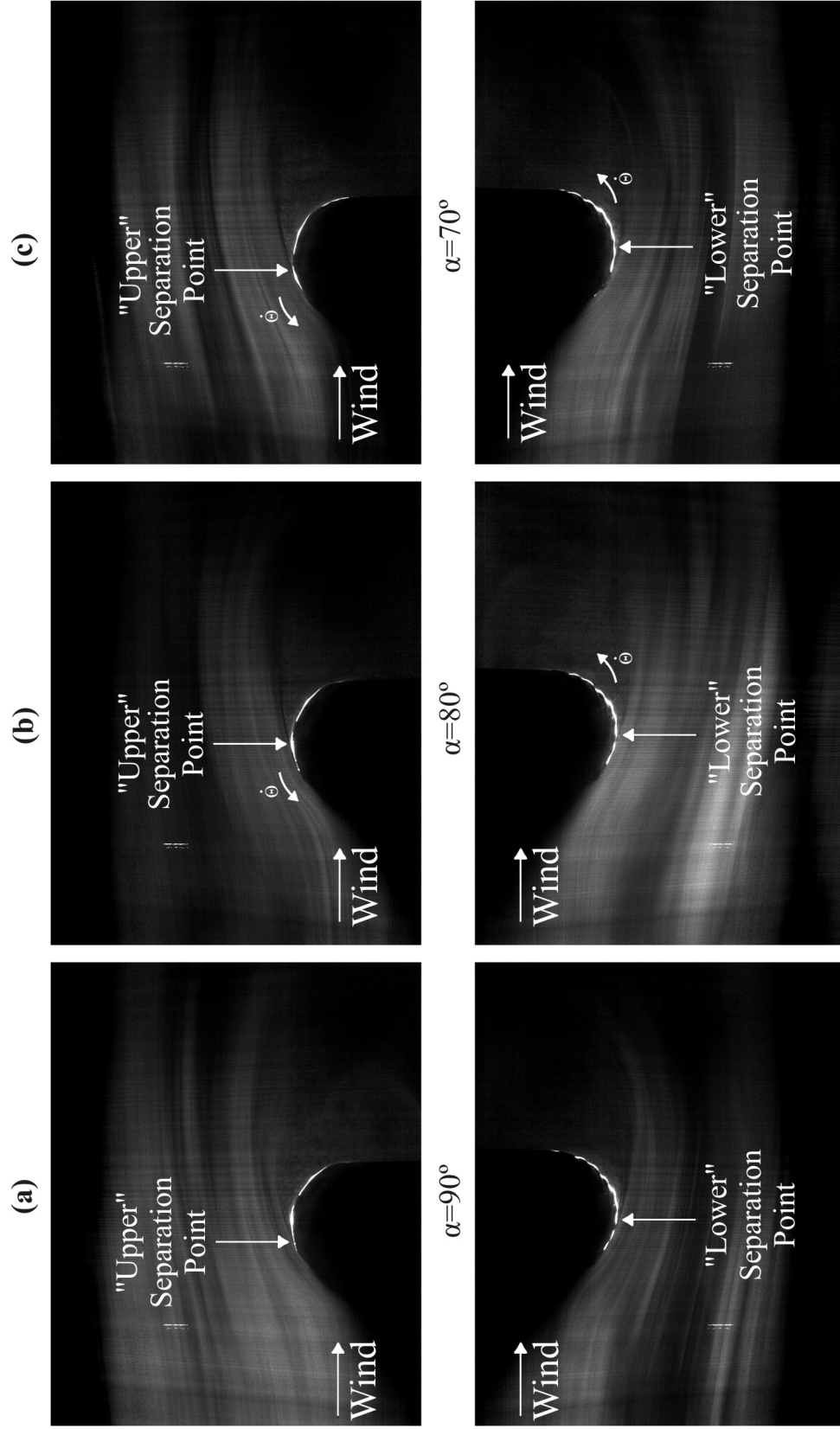


Figure 4.14. Dynamic visualisation results for counter-clockwise motion at (a) 90° , (b) 80° and (c) 70° angles of attack.

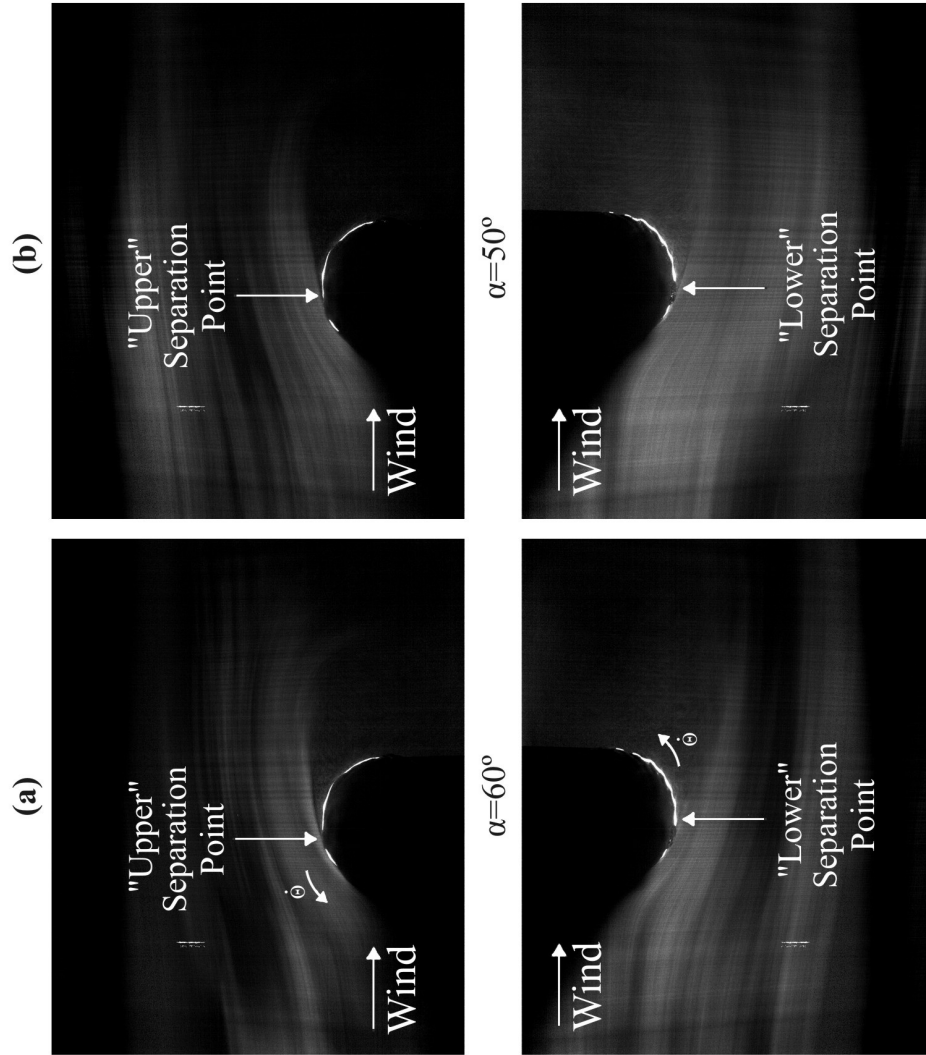


Figure 4.15. Dynamic visualisation results for counter-clockwise motion at (a) 60° and (b) 50° angles of attack.

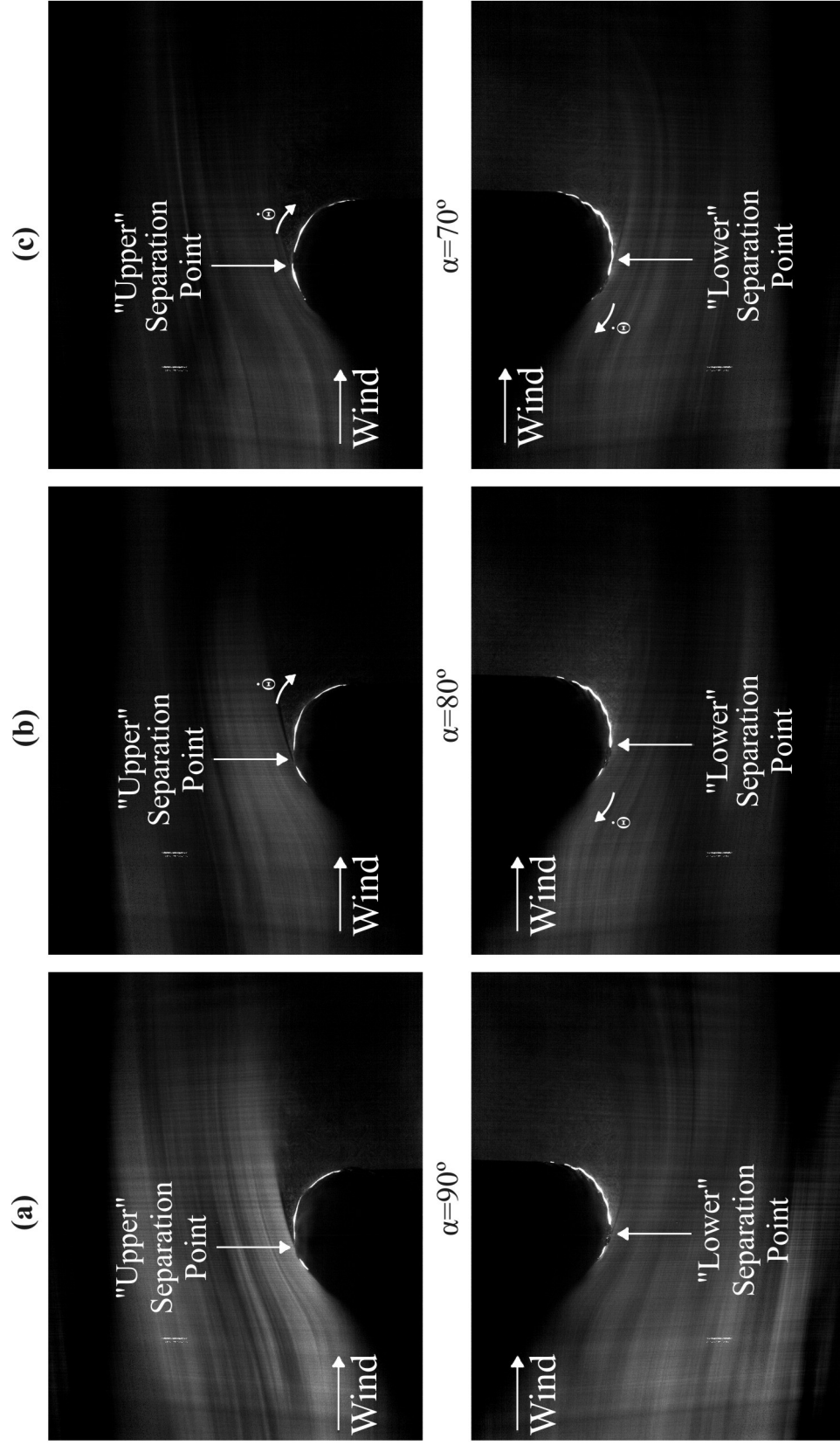


Figure 4.16. Dynamic visualisation results for clockwise motion at (a) 50° , (b) 60° and (c) 70° angles of attack.

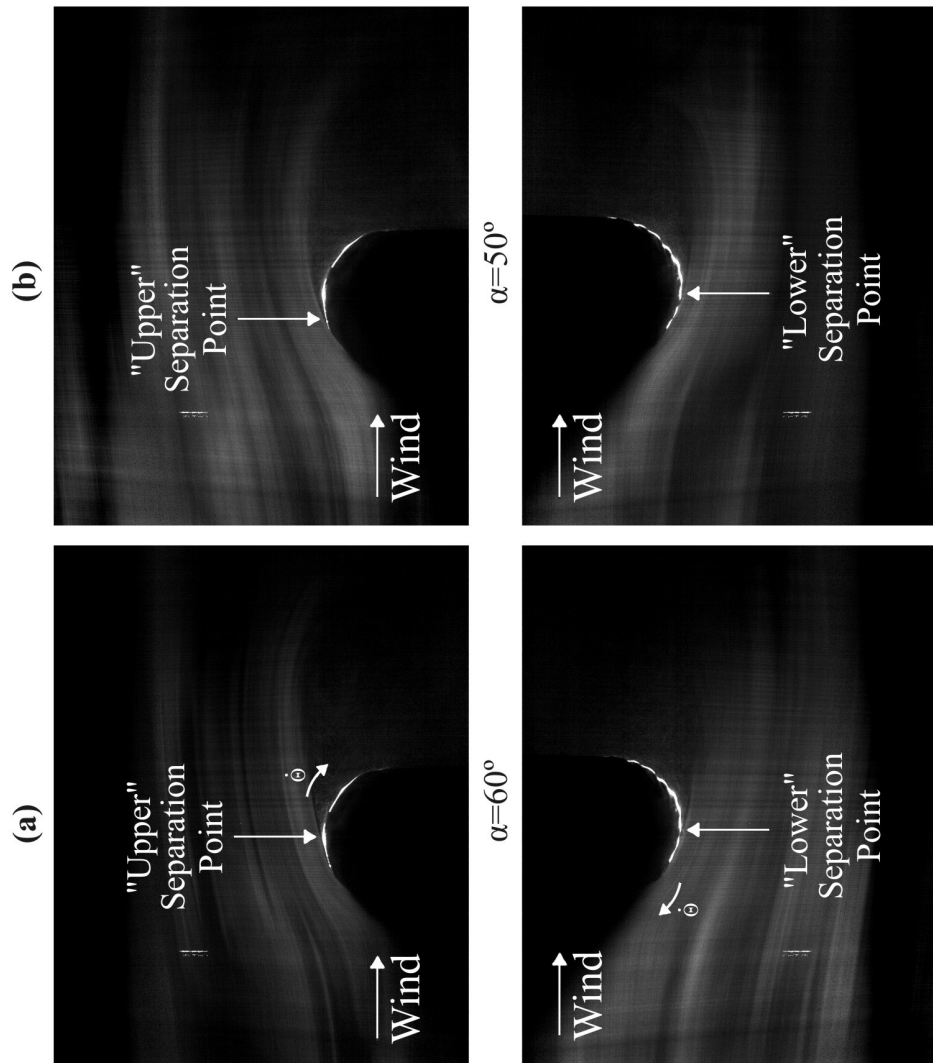


Figure 4.17. Dynamic visualisation results for clockwise motion at (a) 80° and (b) 90° angles of attack.

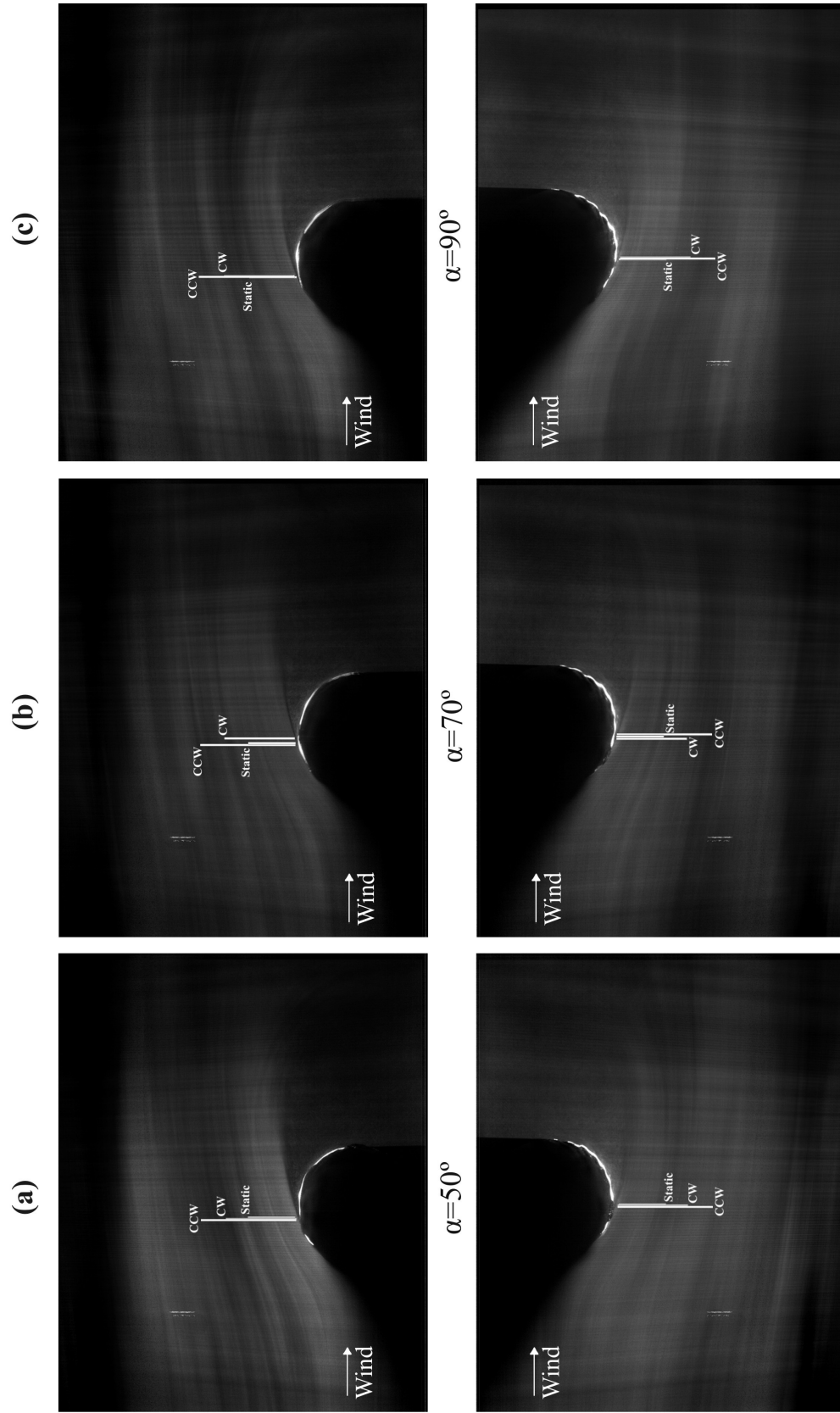


Figure 4.18. Superimposed dynamic visualisation results at (a) 50°, (b) 70° and (c) 90° angles of attack.

The visualisations of the quasi-steady results, which are superimposed in Figure 4.13, indicate that relatively small surface irregularities of an icing have a surprisingly noticeable effect on the conductor's "upper" separation point. However, on the "lower" stranded surface of the conductor where little to no icing is present, the "lower" separation point's location hardly changes over the entire 40° range considered. These visualisations strongly support Tunstall's position [15] that previously used, smooth, ideal models are unrepresentative of "naturally" iced conductors. In the future, a 3-dimensional digital scan of the iced-conductor sample may usefully quantify its surface condition and relate this information to changes in the airflow's separation points. The results of a digital scan would also be helpful in future numerical studies.

The quasi-steady and dynamic visualisation results that are superimposed in Figure 4.18 show relatively good agreement at the 50° and 90° angles of attack. This agreement is expected, as the model's angular speed goes to zero at these angles before a change of direction occurs. At the point of maximum angular speed, when α is 70° , the effects of the rotational motion are seen in both the lower and upper separation points. For a clockwise rotation, the upper separation point shifts leeward while the lower separation point moves not only slightly windward. For a counter-clockwise motion, conversely, both the upper and lower separation points shift only slightly in the windward and leeward directions, respectively. The more noticeable shift in the upper separation point's location during clockwise motion supports the previous aerodynamic data in which clockwise motions result in greater deviations from the quasi-steady values. These visualisations suggest that both the irregular surface geometry of the ice as well as a

rotational motion contribute to the aerodynamic coupling effects observed in the aerodynamic balance measurements. However, further visualisations and aerodynamic measurements are needed to corroborate the present results and conjectures.

5 CONCLUSIONS

The objective of this thesis is to examine the effect and role of rotations in lightly-iced transmission line galloping. It has been achieved by conducting wind tunnel experiments. Quasi-steady testing has supported the previous contention that the well-established den Hartog criterion for vertical galloping predicts stable conditions at all angles of attack for a typical, lightly-iced conductor. Additional mechanisms are likely involved in the galloping of lightly-iced transmission lines observed in the field.

Forced-vibration testing, which involved a lightly-iced conductor model undergoing prescribed rotational oscillations, has shown that the vertical lift is affected by a rotational motion but the drag characteristics are essentially identical to their quasi-steady counterparts. The aerodynamic moment was also measured in these tests but the data lacked consistent repeatability. Differences in the dynamic lift measurements from their quasi-steady values were more noticeable at higher frequencies and amplitudes of rotation. The rotation-induced lift observed in the dynamic tests is associated most likely with fluid-memory effects.

Visualisation images showed that the location of the airflow's separation point on the upper iced portion of a conductor is affected by minor surface irregularities in the ice. The separation point's position on the lower bare portion of a stranded conductor, however, showed noticeably less variation with changes in the angle of attack and rotational motion. These results support the contention that the surface irregularities of ice and a rotational motion jointly affect the locations of the flow's separation points. The changes result in the rotation-induced lift forces observed in the aerodynamic balance

measurements. They are neglected in quasi-steady based numerical models for predicting transmission line galloping which, therefore, need refining.

6 RECOMMENDATIONS

Several experimental parameters in the present wind tunnel studies, including the rotational amplitude and initial angle of attack, were assumed based on general field observations. A detailed and complete full-scale field study, in collaboration with a leading electrical utility, is recommended to better define the characteristics of the common problem of lightly-iced transmission line galloping. Such field studies would require actual or simulated freezing rain conditions or, alternatively, artificial aerofoils that closely resemble light ice accretions.

A 3-dimensional scan of the lightly-iced conductor model used in the present work is recommended to quantify the model's surface geometry. Once known, it may be possible to relate the model's surface geometry to the observed changes in the locations of the airflow's separation points. The data from a 3-dimensional scan would also serve as a starting point for future numerical studies.

Additional studies involving a spring-supported model are recommended to investigate the rotational stability of lightly-iced conductors. The model used in such studies would need to exhibit a critical damping ratio of about 0.1% or less [24,32], so that air bearings are recommended. The use of air bearings with an electromagnetic damper could also provide controllable damping levels as demonstrated in [24]. Then damping levels needed to suppress rotational oscillations and galloping could be established.

References

- [1] Electric Power Research Institute (EPRI), *Transmission Line Reference Book, Wind-Induced Conductor Motion*, Palo Alto, California, 1979.
- [2] Cigré Task Force B2.11.06, *State of the Art of Conductor Galloping*, Hydro Quebec - IREQ, Quebec, Canada, 2005.
- [3] Edwards, A.T. and Madeyski, A. "Progress Report on the Investigation of Galloping Transmission Line Conductors," *Tr. AIEE*, Vol. 75, p.666-686, 1956.
- [4] Nigol, O. and Clarke, G.J. "Conductor Galloping and Control Based on Torsional Mechanism," *IEEE Pwer Engng. Society Meeting*, Paper No. C74 016-2, New York, 1974.
- [5] Wolfs, M. et al. "Review of Galloping Control Methods," *Electra*, No. 191, p.44-61, 2000.
- [6] Southwire Inc., "Motion-Resistant Conductor," [Online], 2010. Available: <http://www.southwire.com/products/MotionResistantConductor.htm> [April 2010].
- [7] Lilien, J.L. and Havard, D.G. "Galloping Data Base on Single and Bundle Conductors. Prediction of Maximum Amplitudes." *IEEE Trans on Power Delivery*. Vol. 15 (2), p.670-674, 2000.
- [8] Van Dyke, P. and Laneville, A. "Galloping of a Single Conductor Covered with a D-Section on a High-Voltage Overhead Test Line," *Journal of Wind Engineering and Industrial Aerodynamics*, Vol. 96, p.1141-1151, 2008.
- [9] Chadha, J. "A Dynamic Model Investigation of Conductor Galloping," *IEEE Winter Power Meeting*, Paper 74 59-2, 1974.
- [10] Nigol, O. and Buchan, P.G. "Conductor Galloping Part I - Den Hartog Mechanism," *IEEE Trans. PAS*, Vol. 96 (2), p.699-707, 1981.
- [11] Nigol, O. and Buchan, P.G. "Conductor Galloping Part II - Torsional Mechanism," *IEEE Trans. PAS*, Vol. 96 (2), p.708-720, 1981.
- [12] Chabert, O. and Lilien, J.L. "Galloping of Electrical Lines in Wind Tunnel Facilities," *Journal of Wind Engineering*, Vol. 74-76, p.967-976, 1998.
- [13] Stumpf, P. "Determination of Aerodynamic Forces for Iced Single and Twin-Bundled Conductors," *M.Sc. Thesis*, University of Manitoba, Winnipeg, Canada, 1994.

- [14] Ohya, Y. "Drag of Circular Cylinders in the Atmospheric Turbulence," *Fluid Dynamics Research*, Vol. 34, p.134-144, 2004.
- [15] Tunstall M.J. "Accretion of Ice and Aerodynamic Coefficients," *Presentation to the AIM Study Day on Galloping*, University of Liege, 1989.
- [16] Novak, M. "Aeroelastic Galloping of Prismatic Bodies," *Journal of the Engineering Mechanics Division*, Vol. 95 (EM1), p.115-142, 1969.
- [17] Bearman, P.W. and Obasaju, E.D. "An Experimental Study of Pressure-Fluctuations on Fixed and Oscillating Square-Section Cylinders," *Journal of Fluid Mechanics*, Vol. 119, p.297-321, 1982.
- [18] Nakamura, Y., "On the Aerodynamic Mechanism of Torsional Flutter of Bluff Structures," *Journal of Sound and Vibration*, Vol. 67, p.163-177, 1979.
- [19] Matsumoto, M., Daito, Y. Yoshizumi, F., Ichikawa Y., and Yabutani, T. "Torsional Flutter of Bluff Bodies," *Journal of Wind Engineering and Industrial Aerodynamics*, Vol. 69-71, p.871-882, 1997.
- [20] den Hartog, J.P. "Transmission Line Vibration Due to Sleet," *Trans AIEE*, Vol. 51, Part 4, p.1074-1086, 1932.
- [21] den Hartog, J.P. *Mechanics of Vibrations*, McGraw-Hill Book Company Inc., New York, NY, 1947.
- [22] Stumpf, P. and Ng, H.C.M. "Investigation of Aerodynamic Stability for Selected Inclined Cables and Conductor Cables," *B.Sc. Thesis*, University of Manitoba, Winnipeg, Canada, 1990.
- [23] Richardson, A.S. "Dynamic Analysis of Lightly Iced Conductor Galloping in Two Degrees of Freedom," *Proc. IEEE*, Vol. 128 (Pt. C) (4), p.211-218, 1981.
- [24] Parkinson, G.V. and Smith, J.D. "The Square Prism as an Aeroelastic Non-linear Oscillator," *Quarterly Journal of Mechanical and Applied Mathematics*. Vol. XVII, Pt. 2, 1964.
- [25] Nakamura, Y. and Mizota, T. "Unsteady Lifts and Wakes of Oscillating Rectangular Prisms," *Journal of the Engineering Mechanics Division*, Vol. 101 (EM6), p.855-871, 1975.
- [26] Washizu, K., Ohya, A., Otsuki, Y., Fuji, K., "Aeroelastic Instability of Rectangular Cylinders in a Heaving Mode," *Journal of Sound and Vibration*, Vol. 59, p.195-210, 1978.

- [27] Bearman, P.W. and Currie, I.G. "Pressure-Fluctuation Measurements on an Oscillating Circular-Cylinder," *Journal of Fluid Mechanics*, Vol. 91, p.661-677, 1979.
- [28] Zdero, R., Turan, O.F., and Harvard, D.G., "Toward Understanding Galloping: Near-Wake Study of Oscillating Smooth and Stranded Circular Cylinders in Forced Motion," *Experimental Thermal and Fluid Science*, Vol. 10 (1), p.28-43, 1995.
- [29] Washizu, K., Ohya, A., Otsuki, Y., and Fujii, K. "Aeroelastic Instability of Rectangular Cylinders in a Torsional Mode due to a Transverse Wind," *Journal of Sound and Vibration*, Vol. 72 (4), p.507-521, 1980.
- [30] Cheers, F. "A Note on Galloping Conductors," *Report MT-14*, National Research Council, Ottawa, 1950.
- [31] Blevins, R.D. *Flow-Induced Vibration (2nd Edition)*. Van Nostrand Reinhold Co., New York, 1990.
- [32] Chan, J.K., Shah, A.H., and Popplewell, N. *Modelling of Conductor Galloping*. Canadian Electrical Association, Project 321 T 672, 1992.
- [33] Wang, J. and Lilien, J.L., "Overhead Electrical Transmission Line Galloping: A Full Multi-Span 3-DOF Model, Some Applications, and Design Recommendations," *IEEE Transactions on Power Delivery*, Vol. 13, p.909-916, 1998.
- [34] Rawlins, C.B. "Numerical Studies of the Galloping Instability of Single Conductors," *Technical Paper No. 30*, Alcoa Conductor Products Company Report, 1993.
- [35] Nakamura, Y. and Mizota, T. "Torsional Flutter of Rectangular Prisms," *Journal of the Engineering Mechanics Division*, Vol. 101 (2), p.125-142, 1975.
- [36] Waris, M.B., Ishihara, T., and Sarwar M.W., "Galloping Response Prediction of Ice-Accreted Transmission Lines," *The 4th International Conference on Advances in Wind and Structures*, Korea, 2008.
- [37] McComber, P., and Paradis, A., "A Cable Galloping Model for Thin Ice Accretions," *Journal of Atmospheric Research*, Vol. 46, p.13-25, 1998.
- [38] Goldstein S., *Modern Developments in Fluid Dynamics, Vol. II*. Dover Publications, New York, 1956.

- [39] Scanlan, R.H. and Tomko, J.J. "Airfoil and Bridge Deck Flutter Derivatives," *Journal of the Engineering Mechanics Division*, Vol. 97 (EM6), p.1717-1737, 1971.
- [40] Barlow, J.B., Rae, W.H. Jr. and Pope, A., *Low-Speed Wind Tunnel Testing Third Edition*, John Wiley & Sons, Toronto, 1999.
- [41] Vishay Micro-Measurements, "Strain Gage Selection: Criteria, Procedures, Recommendations," *Tech Note TN-505-4*, [Online], 1997. Available: <http://www.vishay.com/docs/11055/tn505.pdf> [Sept. 2009].
- [42] Rummage, T., Senior Applications Engineer, Vishay Micro-Measurements (private communication: (919) 365-3800 x142, tom.rummage@vishay.com), 2009.
- [43] Vishay Micro-Measurements, "Surface Preparation for Strain Gage Bonding," *Application Note B-129-8*, [Online], 2009. Available: http://www.vishay.com/docs/11129/11129_b1.pdf [Sept. 2009].
- [44] Vishay Micro-Measurements, "Strain Gage Installations with M-Bond 43-B, 600, and 610 Adhesive Systems," *Instruction Bulletin B-130*, [Online], 2009. Available: <http://www.vishay.com/docs/11130/b-130.pdf> [Sept. 2009].
- [45] Vishay Micro-Measurements, "Optimizing Strain Gage Excitation Levels," *Tech Note TN-502*, [Online], 1997. Available: <http://www.vishay.com/docs/11052/tn502.pdf> [Sept. 2009].
- [46] Zan, S.J., Wardlaw, R.L., and Stumpf, P., "Wind Tunnel Tests of an Iced Bundled- Conductor Model," *Proceedings of the 14th Canadian Congress on Applied Mechanics*, 1993.
- [47] Fujisawa, N., Ikemoto, K., and Nagaya, K., "Vortex Shedding Resonance from a Rotationally Oscillating Cylinder," *Journal of Fluids and Structures*, Vol. 12, p.1041-1023, 1998.
- [48] Roth, K.W., "Deposition and Dispersion of Inertial Aerosols in Secondary and Turbulent Flow Structures," *Ph.D. Thesis*, Massachusetts Institute of Technology, Cambridge, 1995.
- [49] Mejia, K.M. and Jorgens, C.J. "Conceptual Design of a Laser Light Sheet Flow Visualization System for a Pressurized, Low Speed Wind Tunnel," *22nd International Congress on Instrumentation in Aerospace Simulation Facilities*, p.1-13, 2007.

- [50] Alfa Aesar, “*Material Safety Data Sheet*,” [Online], 2008. Available: http://www.alfa.com/MSDSPDF/English_/A16049.pdf [January 12, 2008].
- [51] Tennekes, H. and Lumley, J.L., *A First Course in Turbulence*, The MIT Press, Cambridge, 1972.
- [52] Adaramola, M.S., Akinlade, O.G., Sumner, D., Bergstrom, D.J., and Schenstead, A.J., “Turbulent wake of a finite circular cylinder of small aspect ratio,” *Journal of Fluids and Structures*, Vol. 22, p.919-928, 2006.
- [53] Kähler, C.J., “General design and operating rules for seeding atomisers,” *5th International Symposium on Particle Image Velocimetry*, Paper 3207, Busan, Korea, September, 2003.
- [54] Kähler, C.J., Sammler, B., and Kompenhans, J., “Generation and control of tracer particles for optical flow investigations in air,” *Experiments in Fluids*, Vol. 33, p.736-742, 2002.
- [55] Coherent Inc., “*Operators Manual INNOVA® 70C Series Ion Laser*,” Santa Clara, 1999.
- [56] Tokumaru, P.T. and Dimotakis, P.E., "Rotary Oscillation Control of a Cylinder Wake," *Journal of Fluid Mechanics*, Vol. 224, p.77-90, 1991.
- [57] Akbel, V.D. “Aerodynamic Data for Iced Power Line Samples,” *M. Eng. Report*, University of Manitoba, Winnipeg, Canada, 2005.
- [58] Pro-Dex Inc. - Oregon Micro Systems, *Command Reference Manual Max Family Intelligent Motion Controllers*, Pro-Dex Inc. - Oregon Micro Systems, Beaverton, 2007.
- [59] The MathWorks Inc., “*Discrete Fourier transform*.” [Online], 2010. Available: <http://www.mathworks.com/access/helpdesk/help/toolbox/signal/decimate.html> [September, 2009].
- [60] The MathWorks Inc., “*Decimation - decrease sampling rate*.” [Online], 2010. Available: <http://www.mathworks.com/access/helpdesk/help/toolbox/signal/decimate.html> [September, 2009].
- [61] The MathWorks Inc., “*Zero-phase digital filtering*.” [Online], 2010. Available: <http://www.mathworks.com/access/helpdesk/help/toolbox/signal/filtfilt.html> [September, 2009].

Appendix A: Removal of Mechanical Interactions in Aerodynamic Balance

The calibration results presented in Figures 2.20 to 2.24 of the main text are useful for determining the compensation matrix, $[K]$, shown in equation (A.1). This matrix allows the measured extraneous interactions between different load cells to be removed in the post-processing stage. Now

$$\{F\} = [K]^{-1} \{F'\}, \quad (\text{A.1})$$

where $\{F\}$ is a vector containing the compensated aerodynamic loads, K_{ij} is the representative element of the square compensation matrix, $[K]$, and $\{F'\}$ is a vector containing the measured strains from the aerodynamic balance [40]. Only the horizontal side force, drag, and yawing moment channels are needed in the present case. Then $\{F\}$ and $\{F'\}$ take the forms

$$\{F\} = \begin{Bmatrix} SF \\ D \\ M \end{Bmatrix}, \text{ and } \{F'\} = \begin{Bmatrix} SF' \\ D' \\ M' \end{Bmatrix}, \quad (\text{A.2})$$

where SF' , D' , and M' are the strain measurements from the side force, drag, and yawing moment channels, respectively. SF , D , and M are the corrected values for the aerodynamic side force, drag, and yawing moment, respectively. The compensation matrix takes the form

$$[K] = \begin{bmatrix} k_{SF,SF'} & k_{SF,D'} & k_{SF,M'} \\ k_{D,SF'} & k_{D,D'} & k_{D,M'} \\ k_{M,SF'} & k_{M,D'} & k_{M,M'} \end{bmatrix}, \quad (\text{A.3})$$

where the diagonal terms correspond to the direct loading of a load cell and the off-

diagonal terms are the cross-couplings or interactions between various cells.

All the terms in the compensation matrix $[K]$ are found by measuring the slopes of the calibration data presented in Figures 2.20 through 2.24. For example, the $k_{SF, SF}$ term corresponds to the slope of the calibration data given in Figure 2.21(a). It is calculated to be $13.195 \cdot 10^{-6}/N$. Similarly, Figure 2.20(b) yields a value of $1.713 \cdot 10^{-6}/N$ for the $k_{D, D}$ term while Figure 2.21(e) gives $180.203 \cdot 10^{-6}/Nm$ for $k_{M, M}$. The off-diagonal terms require special treatment due to the minor asymmetries observed in the calibration data for the yawing moment channel. As seen in Figures 2.21(e) and 2.23(b), different slopes and corresponding off-diagonal terms are needed depending on the directions of the applied side force and yawing moment. In reference to the coordinate system shown in Figure 3.1 of the main text, Table A.1 summarises the various loading conditions employed here and the corresponding nomenclature needed for the different compensation matrices. The complete set of compensation matrices K_{PP} , K_{PN} , K_{NP} , and K_{NN} for the various loading conditions are given in equations (A.1) through (A.4). The inverse of each matrix is computed using Matlab and the results are presented in equations (A.5) through (A.8).

Table A.1. Summary of the sign convention and nomenclature for the compensation matrices.

Sign of the Side Force Strain Reading	Sign of the Yawing Moment Strain Reading	Compensation Matrix Nomenclature
Positive	Positive	K_{PP}
Positive	Negative	K_{PN}
Negative	Positive	K_{NP}
Negative	Negative	K_{NN}

$$[K_{PP}] = \begin{bmatrix} 13.195 & 0.079 & -25.212 \\ 0.005 & 1.713 & 0.206 \\ -0.086 & 0 & 180.203 \end{bmatrix}, \quad (\text{A.4})$$

$$[K_{PN}] = \begin{bmatrix} 13.195 & 0.079 & -21.212 \\ 0.005 & 1.713 & 0.206 \\ -0.086 & 0 & 180.203 \end{bmatrix}, \quad (\text{A.5})$$

$$[K_{NP}] = \begin{bmatrix} 13.195 & 0.079 & -22.117 \\ 0 & 1.713 & 0 \\ 0.188 & 0 & 180.203 \end{bmatrix}, \quad (\text{A.6})$$

$$[K_{NN}] = \begin{bmatrix} 13.195 & 0.079 & -25.212 \\ 0 & 1.713 & 0.324 \\ 0.188 & 0 & 180.203 \end{bmatrix}, \quad (\text{A.7})$$

$$[K_{PP}]^{-1} = \begin{bmatrix} 0.0759 & -0.0035 & 0.0106 \\ -0.0002 & 0.5838 & -0.0007 \\ 0.0000 & 0.0000 & 0.0056 \end{bmatrix}, \quad (\text{A.8})$$

$$[K_{PN}]^{-1} = \begin{bmatrix} 0.0758 & -0.0035 & 0.0089 \\ -0.0002 & 0.5838 & -0.0007 \\ 0.0000 & 0.0000 & 0.0056 \end{bmatrix}, \quad (\text{A.9})$$

$$[K_{NP}]^{-1} = \begin{bmatrix} 0.0757 & -0.0035 & 0.0093 \\ 0.0000 & 0.5838 & 0.0000 \\ -0.0001 & 0.0000 & 0.0055 \end{bmatrix}, \quad (\text{A.10})$$

$$[K_{NN}]^{-1} = \begin{bmatrix} 0.0756 & -0.0035 & 0.0106 \\ 0.0000 & 0.5838 & -0.0010 \\ -0.0001 & 0.0000 & 0.0055 \end{bmatrix}, \quad (\text{A.11})$$

Trials involving known combinations of loading conditions were performed to assess the uncertainty of the components of the compensation matrices given in equations (A.4) to (A.7). Although a proper error analysis is appropriate, such an analysis cannot be conducted as the deflections of the balance were not considered and the alignment of the string through which the loads were applied was not recorded during the calibrations. These deficiencies should be rectified in the next calibration procedure.

For the combined loading trials, the off-set calibration bar was rotated 45.0° from the positive drag direction so that an applied load simultaneously created a yawing moment as well as drag and side forces. Taking the applied loads as a reference, comparisons were made with the (compensated) measurements derived from the aerodynamic balance. The compensated results are given in Tables A.1 through A.4. They show that the largest percentage errors, which correspond to the bolded values, were about 2% for the side force and drag and 4% for the yawing moment.

Table A.1. Percentage errors from the combined load trials which incorporated the K_{pp} compensation matrix.

Side Force			Drag Force			Yawing Moment		
Applied (N)	Measured (N)	Percent Error (%)	Applied (N)	Measured (N)	Percent Error (%)	Applied (Nm)	Measured (Nm)	Percent Error (%)
0.354	0.347	-1.96	0.354	0.351	-0.92	0.0398	0.0386	-3.00
1.061	1.048	-1.22	1.061	1.049	-1.13	0.1192	0.1166	-2.19
1.741	1.751	0.60	1.741	1.750	0.52	0.1955	0.1952	-0.13
3.824	3.868	1.15	3.824	3.872	1.26	0.4294	0.4267	-0.34
6.626	6.718	1.39	6.626	6.715	1.34	0.7440	0.7321	-1.61

Table A.2. Percentage errors from the combined load trials which incorporated the K_{PN} compensation matrix.

Side Force			Drag Force			Yawing Moment		
Applied (N)	Measured (N)	Percent Error (%)	Applied (N)	Measured (N)	Percent Error (%)	Applied (Nm)	Measured (Nm)	Percent Error (%)
0.354	0.348	-1.74	0.354	0.351	-0.96	-0.0365	-0.0357	-2.21
1.061	1.049	-1.12	1.061	1.039	-2.05	-0.1096	-0.1087	-0.78
1.741	1.756	0.88	1.741	1.733	-0.44	-0.1797	-0.1816	1.05
3.824	3.888	1.66	3.824	3.855	0.81	-0.3949	-0.4028	2.03
6.626	6.739	1.71	6.626	6.683	0.86	-0.6841	-0.7014	2.53

Table A.3. Percentage errors from the combined load trials which incorporated the K_{NP} compensation matrix.

Side Force			Drag Force			Yawing Moment		
Applied (N)	Measured (N)	Percent Error (%)	Applied (N)	Measured (N)	Percent Error (%)	Applied (Nm)	Measured (Nm)	Percent Error (%)
-0.354	-0.351	-0.87	0.354	0.351	-0.75	0.0398	0.0397	-0.10
-1.061	-1.057	-0.40	1.061	1.040	-2.00	0.1192	0.1198	0.55
-1.741	-1.765	1.38	1.741	1.730	-0.60	0.1955	0.1994	2.02
-3.824	-3.890	1.72	3.824	3.794	-0.78	0.4294	0.4367	1.70
-6.626	-6.753	1.91	6.626	6.582	-0.67	0.7440	0.7508	0.91

Table A.4. Percentage errors from the combined load trials which incorporated the K_{NN} compensation matrix.

Side Force			Drag Force			Yawing Moment		
Applied (N)	Measured (N)	Percent Error (%)	Applied (N)	Measured (N)	Percent Error (%)	Applied (Nm)	Measured (Nm)	Percent Error (%)
-0.354	-0.352	-0.47	0.354	0.351	-0.94	-0.0365	-0.0351	-4.00
-1.061	-1.058	-0.27	1.061	1.033	-2.65	-0.1096	-0.1063	-2.95
-1.741	-1.769	1.64	1.741	1.724	-0.96	-0.1797	-0.1778	-1.04
-3.824	-3.910	2.25	3.824	3.791	-0.87	-0.3949	-0.3941	-0.19
-6.626	-6.773	2.23	6.626	6.587	-0.59	-0.6841	-0.6883	0.62