

ABSTRACT

Title of thesis: **DESIGN AND PERFORMANCE
OF A DUCTED COAXIAL ROTOR
IN HOVER AND FORWARD FLIGHT**

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A ducted contra-rotating coaxial rotor system was designed and tested to assess its potential use as a micro aerial vehicle (MAV). Performance measurements (thrust and power) of the system in hover and forward flight were obtained. The influence of several design parameters (rotor spacing, duct inlet shape, position of rotors within the duct, and tip clearance) on performance was determined. Performance measurements of the unducted coaxial rotor, as well as the unducted/ducted single rotor configurations, were also obtained to give a performance baseline for the ducted coaxial rotor. The aerodynamic characteristics of the isolated duct were assessed from loads measurement and surface flow visualization. While the net system performance of operating the coaxial rotor within the confines of a duct was not always improved, the ducted coaxial rotor concept is still attractive for a MAV based on total attainable thrust for a given rotor size and other operational benefits.

DESIGN AND PERFORMANCE
OF A DUCTED COAXIAL ROTOR
IN HOVER AND FORWARD FLIGHT

by

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Preface

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Table of Contents

List of Tables	viii
List of Figures	ix
Nomenclature	xiii
1 Introduction	1
1.1 Background	1
1.2 The Ducted Coaxial Rotor System	3
1.3 Previous Investigations of Ducted Rotary Systems	5
1.4 Objectives of Thesis	7
1.5 Organization of Thesis	8
2 Design of the Ducted Coaxial Rotor Model and Test Stand	9
2.1 Introduction	9
2.2 Coaxial Rotor Specifications	9
2.2.1 Determination of RP Material for Rotor Fabrication	14
2.2.1.1 Tensile Testing of RP Blade Specimens	16
2.2.1.2 Bending Testing of RP Blade Specimens	20
2.2.1.3 Surface Finish of RP Blade Specimens	23
2.2.1.4 Summary of RP Blade Specimen Testing	23
2.3 Rotor Test Stand and Sting Assembly Structural Design	24
2.3.1 Rotor Test Stand	25
2.3.2 Rotor Sting Assembly	26
2.3.3 Proximity of Model and Aerodynamic Interference	28
2.4 Duct Specifications	29
2.4.1 Derivation of Duct Airfoils	29
2.4.1.1 Cambered Leading Edge	30
2.4.1.2 Symmetric Leading Edge	31
2.4.1.3 NACA Trailing Edge	32
2.4.1.4 Summary	34
2.4.2 Chordwise Pressure Tap Locations	34
2.5 Duct Assembly Structural Design	35
2.5.1 Duct Design	37
2.5.1.1 Duct Panels	38
2.5.1.2 Duct Interior	39
2.5.1.3 Pressure Tap Specifications	40
2.5.2 Duct Base Design	43
2.5.2.1 Mounting Position of Duct Assembly	43
2.5.3 Duct Support Rods	44
2.5.3.1 Rotor Position Within the Duct	45
2.5.3.2 Euler Beam Bending Model	45
2.5.3.3 Beam Loading from Duct Assembly Weights	48

2.5.3.4	Beam Loading from Duct Assembly Drag	50
2.5.3.5	Summary of Results	52
2.6	Summary	53
3	Description of the Experiments	54
3.1	Introduction	54
3.2	Operation of the Rotors	55
3.3	Specifications of Loads Measurement	57
3.3.1	Signal Conditioning	57
3.3.2	Testing Software	59
3.3.2.1	Motor Speed Controller	59
3.3.2.2	Data Acquisition	60
3.3.3	Determination of Power	60
3.4	Experimental Setup and Testing Procedures	61
3.4.1	Test Parameters	64
3.4.2	Hover Performance Testing	65
3.4.3	Forward Flight Performance Testing	66
3.4.4	Isolated Duct Loads Testing	67
3.4.5	Flow Visualization on the Duct	68
3.5	Catalog of Tests	69
3.6	Summary	71
4	Results and Discussion	72
4.1	Introduction	72
4.2	Hover Performance	72
4.2.1	Introduction	72
4.2.2	Isolated Single Rotor Performance in Hover	73
4.2.3	Ducted Single Rotor Performance in Hover	78
4.2.3.1	Rotor/Duct Position	78
4.2.3.2	Duct Shape	81
4.2.3.3	Loads Decomposition	86
4.2.3.4	Tip Clearance	88
4.2.4	Isolated Coaxial Rotor Performance in Hover	89
4.2.4.1	Comparison of Isolated Single Rotors and Isolated Coaxial Rotor	91
4.2.5	Ducted Coaxial Rotor Performance in Hover	95
4.2.5.1	Rotor Spacing and Rotor/Duct Position	95
4.2.5.2	Duct Shape	98
4.2.5.3	Tip Clearance	100
4.3	Axial Forward Flight Performance	102
4.3.1	Introduction	102
4.3.2	Isolated Single Rotor Performance in Axial Flight	103
4.3.3	Ducted Single Rotor Performance in Axial Flight	107
4.3.4	Isolated Coaxial Rotor Performance in Axial Flight	109
4.3.5	Ducted Coaxial Rotor Performance in Axial Flight	109

4.4	Edgewise Forward Flight Performance	113
4.4.1	Introduction	113
4.4.2	Ducted Single Rotor Performance in Edgewise Flight	115
4.4.3	Ducted Coaxial Rotor Performance in Edgewise Flight	115
4.5	Isolated Duct Testing: Loads Measurement and Flow Visualization	122
4.5.1	Introduction	122
4.5.2	Isolated Duct Loads	125
4.5.3	Duct Surface Flow Visualization	130
4.6	Summary of Experimental Results	131
5	Concluding Remarks	134
5.1	Conclusions	134
5.2	Suggestions for Future Work	137
5.2.1	Improved Coaxial Rotor	138
5.2.2	Independent Measurement of Rotor Performance	139
5.2.3	Measurement of Pitching Moment in Edgewise Flight	140
5.2.4	Surface Pressure Measurements and Flow Field Measurements . .	140
A	Catalog of Laboratory Performance Tests	141
B	Catalog of Wind Tunnel Performance Tests	143
C	Catalog of Isolated Duct Testing	158
	Bibliography	160

List of Tables

2.1	Blade aerodynamic properties.	12
2.2	Results of RP specimen testing.	23
2.3	Chordwise pressure tap locations on the duct, normalized by duct chord. .	35
3.1	Shorthand notation of test parameters.	70
A.1	Catalog of Laboratory Performance Tests.	141
B.1	Catalog of Wind Tunnel Performance Tests.	144
C.1	Catalog of Duct Loads Tests.	158
C.2	Catalog of Duct Surface Flow Visualization Tests.	159

List of Figures

2.1	Thickness-to-chord distribution of the rotors used in the MAV model. . . .	10
2.2	Fixed pitch distribution of the rotors used in the MAV model.	11
2.3	Operational flow characteristics in terms of Reynolds and Mach number, calculated at sea level ISA conditions.	13
2.4	Pro/ENGINEER model of the coaxial rotor (0.15 <i>R</i> rotor-to-rotor separation).	15
2.5	Pro/ENGINEER model of the blade test specimen.	15
2.6	The three RP blade specimens for tensile, bending, and surface finish assessment.	16
2.7	Tensile testing of the Accura 50 blade specimen.	18
2.8	Results of tensile testing of the three RP blade specimens (only 30% of the data shown).	19
2.9	Prediction of stress from centrifugal forces along the rotor blade at 9,000 rpm, normalized by the ultimate tensile strength of each material.	21
2.10	Results of bending testing of the three RP blade specimens.	22
2.11	The rotor test stand, shown (a) as Pro/ENGINEER model and (b) as manufactured.	24
2.12	Complete variation of test stand pitch.	26
2.13	The structural components of the rotor sting assembly, as drawn with Pro/ENGINEER.	27
2.14	The cambered and symmetric duct airfoils, with placement of the 22 chordwise pressure taps.	36
2.15	The structural components of the duct and the duct base used in total forming the duct assembly.	37
2.16	Interior of the duct cross-section, showing the internal placement of the lower duct ring.	40
2.17	Cross-sectional interior of the duct.	41
2.18	Implementation of pressure measurement.	42

2.19	Mounting positions for the duct assembly.	44
2.20	Displacement of the duct support beam bending model.	53
3.1	Prediction of balance stress based on the test article configuration, flight phase, and angular state.	58
3.2	Correlation between electric power to the motors and aerodynamic power of the test article, with linear best-fit curve shown.	62
3.3	Experimental setup of the ducted coaxial rotor, shown in the wind tunnel.	63
3.4	The test parameters and flight conditions of the experiments and their possible settings.	65
4.1	Hover performance of the isolated upper and lower rotors.	74
4.2	Hover performance of the isolated upper and lower rotors, shown in power polar form.	77
4.3	Hover performance of the isolated upper and lower rotors, in nondimensional form.	79
4.4	Hover performance of the ducted single rotors with variation in rotor/duct position.	80
4.5	Hover performance of the ducted single rotors with variation in duct shape.	82
4.6	Hover performance of the ducted single rotors expressed in terms of power loading.	83
4.7	Hover performance of the ducted single rotors with variation in rotor position at a given thrust or power.	85
4.8	Hover performance of the ducted upper rotor, comparing ducted and isolated loads.	87
4.9	Hover performance of the ducted upper rotor with variation in tip clearance.	88
4.10	Hover performance of the isolated coaxial rotor, with variation in rotor spacing and theory predictions.	90
4.11	Hover performance of the isolated coaxial rotor, compared to the isolated single rotors.	93

4.12	Power sharing ratio of the isolated coaxial rotor as compared to the aggregate single rotors. Wind tunnel measurements.	95
4.13	Hover performance of the isolated coaxial rotor as compared to the aggregate single rotors and theory predictions.	96
4.14	Hover performance of the ducted coaxial rotor at various combinations of rotor spacing and rotor/duct position.	97
4.15	Upper and lower rotor power for various ducted coaxial rotor configurations.	99
4.16	Hover performance of the ducted coaxial rotor, expressed in terms of power loading. Wind tunnel measurements.	100
4.17	Hover performance of the ducted coaxial rotor with variation in duct shape.	101
4.18	Hover performance of the ducted coaxial rotor with variation in tip clearance.	102
4.19	Axial flight performance of the isolated upper and lower rotors.	104
4.20	Axial flight performance of the isolated upper and lower rotors, shown in power polar form.	106
4.21	Composite efficiency of the isolated single rotors in axial flight.	107
4.22	Composite efficiency of the ducted single rotors in axial flight at P1 and P3 positions.	108
4.23	Axial performance of the isolated coaxial rotor (S3 spacing) compared to the aggregate single rotors case.	110
4.24	Axial performance of the isolated coaxial rotor (S3 spacing), shown in terms of composite efficiency.	111
4.25	Axial flight performance of the ducted coaxial rotor, shown in terms of composite efficiency.	112
4.26	Axial flight performance of the S1/P3 ducted coaxial rotor with varying duct shape, shown in terms of composite efficiency.	114
4.27	Axial flight performance of the S1/C/P3 ducted coaxial rotor with varying tip clearance, shown in terms of composite efficiency.	114
4.28	Edgewise flight performance of the ducted single rotor for rotor/duct positions P1 and P3.	116

4.29	Edgewise flight performance of the ducted coaxial rotor for configurations S1/C/P1 and S1/C/P3.	118
4.30	Edgewise flight performance of the ducted coaxial rotor for a constant vertical force.	119
4.31	Edgewise flight performance of the ducted coaxial rotor, with variation in duct shape.	119
4.32	Edgewise flight performance of the ducted coaxial rotor at various stand pitch angles.	120
4.33	Power required to achieve a constant vertical force in edgewise flight for various shaft angles.	121
4.34	Total and rotor-only loads of the S1/C/P3 ducted coaxial rotor in edgewise flight for varying tip clearance.	123
4.35	Edgewise flight performance of the S1/C/P3 ducted coaxial rotor with nominal and increased tip clearance.	124
4.36	Loads of the cambered duct in isolation.	127
4.37	Loads of the symmetric duct in isolation.	128
4.38	Oil flow visualization of the (a) cambered and (b) symmetric duct shapes, both at 15° shaft angle.	131
5.1	A Kevlar sleeve on each rotor blade may increase rotor flexure strength on existing (or new) rotor sets.	139

Nomenclature

A	duct cross-sectional area
A	NACA airfoil constant
A	rotor blade cross-section
A	rotor disk area, $= \pi R^2$
A_{eff}	effective rotor disk area
a	speed of sound
B	NACA airfoil constant
b	linear constant
C	integration constant
C	NACA airfoil constant
C_D	drag coefficient, $= D/qA$
C_L	lift coefficient, $= L/qA$
C_P	rotor power coefficient, $= P/\rho A \Omega^3 R^3$
C_Q	rotor torque coefficient, $= Q/\rho A \Omega^2 R^3$
C_T	rotor thrust coefficient, $= T/\rho A \Omega^2 R^2$
C_T/σ	blade loading coefficient
c	blade chord
c_d	duct chord
D	NACA airfoil constant
D_d	duct drag force
d	beam cross-section centroid
d	distributed drag load
d	duct diameter
d	rod diameter
E	modulus of elasticity
E	NACA airfoil constant
FM	figure of merit
F_{CF}	centrifugal force
I	cross-section second moment of area
I	current
i	blade element number
K	controller gain
$K_{1..4}$	Küchemann constants
L	duct length
L	duct lift force
L_d	offset of duct center of mass from beam tip
M	Mach number, $= \Omega r R/a$
M	moment
m	linear slope
m	rotor mass-per-length
N_b	number of blades
n	number of blade elements
P	applied load

P	rotor power
PL	power loading, T/P
P_{aero}	aerodynamic power
P_{elec}	electric power
Q	rotor torque
q	dynamic pressure, $= \frac{1}{2}\rho V_{\infty}^2$
R	duct radius
R	rotor radius
R^2	coefficient of determination
Re_c	Reynolds number (chordwise), $= c\Omega rR/\nu$
r	distance from the beam cross-section centroid to the rod centroid
r	nondimensional radial position, $= y/R$
S_u	ultimate tensile strength
SF	safety factor
T	rotor thrust
T	transformation function
t	rotor blade thickness
t_d	duct thickness
V	voltage
V_{∞}	freestream velocity
$\bar{v}_i$	average induced velocity
W_d	duct weight
w	distributed weight load
x	Cartesian coordinate
y	Cartesian coordinate
y	dimensional radial position, $= rR$
z	Cartesian coordinate
α	pitch angle
δ	beam deflection
δ	tip displacement
ε	tensile strain
η	composite efficiency
θ	beam slope
θ	blade pitch
ν	kinematic viscosity
ρ	air density
ρ	beam density
ρ	RP material density
σ	rotor solidity, $= N_b c / \pi R$
σ	tensile stress
σ_{CF}	axial stress from centrifugal force loading
ψ	yaw angle
Ω	rotor rotational speed
ℓ	beam length

Subscripts and Superscripts

<i>D</i>	refers to loading by drag force
<i>FP</i>	refers to the flat-plate (symmetric) duct leading edge
<i>i</i>	refers to the duct interior
<i>i</i>	refers to the rod interior
<i>K</i>	refers to the Küchemann (cambered) duct leading edge
<i>l</i>	refers to the lower rotor
<i>m</i>	refers to the duct exterior
<i>o</i>	refers to the rod exterior
<i>TE</i>	refers to the duct trailing edge
<i>u</i>	refers to the upper rotor
<i>W</i>	refers to loading by weight
<i>0</i>	refers to the duct centerline

Abbreviations

AC	alternating current
AFDD	Aeroflightdynamics Directorate
AHS	American Helicopter Society
AIAA	American Institute of Aeronautics and Astronautics
A/D	analog-to-digital
CAD	computed-aided design
COTS	commercial off-the-shelf
DAQ	data acquisition
DC	direct current
ESC	electronic speed controller
GLMWT	Glenn L. Martin Wind Tunnel
HDSL	high definition stereolithography
IR	infrared
ISA	International Standard Atmosphere
LED	light emitting diode
MAV	micro aerial vehicle
NACA	National Advisory Committee for Aeronautics
NASA	National Aeronautics and Space Administration
PWM	pulse width modulation
RC	resistor-capacitor
RP	rapid prototyping
SLA	stereolithography
SLS	selective laser sintering
TTL	transistor-transistor logic
UAV	uninhabited aerial vehicle

Test Parameter Nomenclature

C	cambered duct
c	increased tip clearance rotor(s) (subscript)
L	lower rotor
P1	upper-most rotor at position of $0.10c_d$ from duct inlet
P2	upper-most rotor at position of $0.33c_d$ from duct inlet
P3	lower-most rotor at position of $0.67c_d$ from duct inlet
P4	lower-most rotor at position of $0.90c_d$ from duct inlet
R	rotor-only loads
S	symmetric duct
S1	$0.15R$ inter-rotor spacing
S2	$0.20R$ inter-rotor spacing
S3	$0.30R$ inter-rotor spacing
T	total loads
U	upper rotor

Chapter 1

Introduction

1.1 Background

Micro aerial vehicles (MAVs) are compact flight vehicles that are a subset of a broader category of uninhabited aerial vehicles (UAVs). MAVs provide unique capability not provided by manned aircraft for both civil and military applications [1, 2]. They are particularly useful in the battlefield, where intelligence, surveillance, and reconnaissance, which are critical towards successful operations, may be gathered using the MAV with little risk to the human operator. Generally, MAVs are intended be relatively small, lightweight, and robust enough to be operated by an individual soldier. Ultimately, realizing the full potential of MAVs offers the modern military increased situational awareness, yielding greater combat effectiveness to meet the desired objective at lower cost.

Serious technical and engineering efforts to develop UAVs began in the 1970s, motivated by military interest in autonomous (i.e., without human intervention) or semi-autonomous flight vehicles that could perform various high-risk missions in difficult environments. The term “dull, dirty or dangerous” or D³ has often been used to classify such environments. Recent development in MAVs over the last decade have improved their capabilities significantly, with gains in size reduction, lower structural weight fraction, and some improvement in aerodynamic efficiency [3, 4]. One particular challenge in furthering the capability of MAVs is improving their aerodynamic performance at this

small scale, where low Reynolds number flow tends to increase viscous losses and adversely affect lifting surface behavior. As a result, the aerodynamic efficiency of MAVs is generally far lower than that of larger flight vehicles [5, 6]. As MAVs mature from experimental to operational vehicles, other design constraints may arise, such as powerplant efficiency and integration at small scales [7]. Other operational constraints may also be imposed. For example, the need for a single soldier to carry and operate a MAV limits its practical size and weight.

MAVs that are capable of hover and low-speed loiter are attractive as they can “perch and observe” to obtain certain kinds of intelligence, conducting missions that fixed-wing MAVs generally cannot accomplish. Hovering capability may also be desirable from a stealth standpoint, as a hovering MAV at a distance may be less noticeable than a loitering fixed-wing MAV. Beyond hovering performance, MAVs also need efficient capability in forward flight (specifically, in axial climb and edgewise flight) to be fully effective. Several hover-capable, rotating-wing concepts have already been tested at MAV scale, including conventional rotor, coaxial rotor, cyclorotor, and ducted rotor systems [4].

A major barrier in improving the performance of such small flight vehicles lies in overcoming the various aerodynamic problems associated with low Reynolds number flight (often below 10^5 based on characteristic chord dimensions), which generally manifest as higher relative viscous losses, lower efficiencies, and higher power requirements compared to what might be expected by simply scaling-down a full-scale flight vehicle. The compromised levels of performance of these concepts suggest that the fundamental aerodynamic issues at MAV scale are not fully understood. This problem limits confi-

dence levels when designing MAVs to meet specific mission requirements, i.e., selection of design parameters for the best range, endurance, climb rate, and so forth.

1.2 The Ducted Coaxial Rotor System

One such hover-capable MAV concept that offers unique advantages over other designs is that of a ducted (shrouded) coaxial rotor. This concept is a synthesis of the ducted rotor and coaxial rotor platforms, and each is attractive for different design, operation, and performance reasons.

The benefits of ducting a single rotor are generally well understood at larger scale. The hovering ducted rotor system is generally more aerodynamically efficient than a single rotor alone because duct forces augment the performance of the rotor system. The operational advantages of ducting a rotor system (single or coaxial) include system ruggedization and rotor protection. This benefit is also useful in the operation of ducted rotary systems in dense urban environments to reduce the possibility of the rotors striking objects or personnel. The noise signature of the rotor system may be reduced and/or its directionality changed, although noise issues are not well understood.

Perhaps the largest disadvantage to the use of a duct on a rotating-wing platform is the additional weight of the duct itself. The exact installation of the duct may also be challenging from an integration standpoint to achieve the desired clearance between the rotor and the duct wall without possibility of blade strikes. From an aerodynamic standpoint, the aerodynamic benefits of ducting, which are greatest in hover, may not translate to improvements in forward flight.

The coaxial rotor also provides its own separate advantages. Most notably, the counter-rotating coaxial rotor platform requires no separate means of anti-torque because the rotor torques oppose each other. At full-scale, this advantage allows for coaxial rotorcraft to generally have a smaller footprint than comparable conventional rotorcraft that rely on a tail rotor for anti-torque. For the same vehicle weight and disk loading, the rotor area of each rotor in a coaxial system may be much smaller than that of a single rotor system, which has advantages in terms of vehicle compactness.

However, the disadvantages of a coaxial rotor system are greater losses when compared to a single rotor system at the same disk loading. The rotor-on-rotor aerodynamic interference tends to drive up net power requirements, requiring a greater powerplant to achieve the same levels of thrust, which reduces available payload and therefore range, endurance, and overall capability for the same vehicle weight. The mechanical rotor hub is also more complex in coaxial platforms, which is a further source of increased weight or decreased capability for a given weight, and may also drive up maintenance costs.

It is clear that the potential benefits of ducting a coaxial rotor are attractive on many levels, including greater vehicle ruggedness and compactness. However, their separate design disadvantages, most notably decreased capability from higher structural weight requirements, must be traded against the aerodynamic efficiency of such a concept to determine its suitability for its proposed end use as a MAV.

1.3 Previous Investigations of Ducted Rotary Systems

A greater understanding of ducts at larger scale can be attributed to ducted propeller and ducted fan research completed as early as six decades ago by Stipa [8], Krüger [9], and Sacks and Burnell [10].

More research in full-scale ducted propellers was conducted in the 1960s and published through the National Aeronautics and Space Administration (NASA). A series of experiments at NASA Ames Research Center were conducted on the Doak VZ-4DA ducted propeller (diameter of 4 ft) by Yaggy and Mort (1961) [11], Yaggy and Goodson (1961) [12], Mort and Yaggy (1962) [13], and Mort (1965) [14]. The research focus was on measuring the performance of the ducted propeller at various incident angles to the free-stream in forward flight, including studies of duct lip stall effects. The maximum figure of merit was determined to be 0.78, with a maximum propulsive efficiency of about 0.60. An additional study by Mort and Gamse [15] on the Bell X-22A ducted propeller (diameter of 7 ft) was also conducted, finding a maximum figure of merit of 0.81 and a maximum propulsive efficiency of 0.74. It was noted that the propulsive efficiency of this ducted propeller could be “significantly higher” if the duct was designed for high free-stream velocities, i.e., produced less drag, instead of designing for static conditions. In this way, Mort and Gamse acknowledged that the design constraints of a ducted propeller in static conditions (hover) and in axial forward flight may conflict. Additional research during this time period was also done by Turner [16] and Black and Wainauski [17].

More recent experimental investigations of ducted rotor systems at smaller scale include studies by Martin and Tung [18] and Pereira and Chopra [19–21]. Martin and

Tung [18] conducted performance and flow field measurements of a ducted single rotor (10 inch diameter), with several noteworthy findings. It was found that at low rotor speeds the duct contributed a net download on the system, which was determined to be a result of internal flow losses within the duct at these low Reynolds numbers. Increasing the blade/duct clearance was also shown to significantly degrade system efficiency, with the figure of merit approaching the value of the isolated rotor (0.44) when the tip gap was large (greater than 4% rotor radius).

Pereira and Chopra [19–21] conducted thorough investigations of the performance of a 16 cm (6.3 inches) diameter ducted single rotor MAV for a wide variety of design parameters, including duct inlet lip radius, tip clearance, duct diffuser length, and duct diffuser angle. In the study, significant performance gains in hover (on the order of 50% power reduction for the same thrust) were possible at this scale with optimal settings for the design parameters. Further research work into ducted rotors include experimental, computational, and control system studies of the ducted rotor concept [22–30].

In general, the performance of a ducted rotor system at this scale is influenced by several factors: (1) The performance of the rotor system itself at MAV scale, where blade chord Reynolds numbers are relatively low such that viscous losses are greater and aerodynamic efficiencies are lower [31]; (2) The design of the duct; (3) The aerodynamic interaction between the rotor and duct at MAV scale as the rotor affects flow through the duct, especially when the boundary layers are relatively thicker to the point that they may affect system performance differently at this scale; and (4) The duct inlet conditions in hover and forward flight as they impact rotor performance, where three-dimensional flow separation and long laminar separation bubbles may alter the rotor and net system perfor-

mance. A more thorough understanding of these factors may give greater confidence in the design of ducted rotor MAVs.

1.4 Objectives of Thesis

The primary objectives of the present work were twofold: (1) To investigate the performance of a ducted coaxial rotor system at MAV scale so that the aerodynamic effects of a coaxial rotor and the influence of the duct may be better understood; and (2) To obtain specific types of experimental data to support the validation of aerodynamic models for MAV performance and for the design of more efficient MAVs. To satisfy these objectives, the present work was divided into two phases.

The first phase of the study was to obtain performance measurements of a ducted coaxial rotor in hover and forward flight in terms of thrust, torque, and power. The forward flight experiments consisted of measuring levels of performance in axial (propeller mode) and edgewise flight. Several configurations were examined, including variations in rotor separation distance, duct shape, position of the rotors within the duct, and tip clearance, so as to infer the aerodynamic impact of these parameters on the performance of a ducted coaxial rotor system. Performance measurements of the rotor loads and system (duct and rotor) loads were made separately so that the duct loads could be obtained. To place the performance of the ducted coaxial rotor into proper perspective, performance measurements of the isolated coaxial rotor, the isolated single rotor, and ducted single rotor systems were also obtained.

The second phase of the study was to investigate the isolated duct used in rotor

system performance testing. This work was done in both quantitative and qualitative terms through the independent measurement of the duct forces and by means of duct surface flow visualization.

1.5 Organization of Thesis

To explain the performance of the ducted coaxial rotor system, a background of both ducted rotors and the aerodynamic challenges faced by MAVs at lower Reynolds numbers is presented in Chapter 1. Chapters 2 and 3 discuss the design and setup of the experiments, respectively. Chapter 4 presents the results in four sections: (1) Hover performance, (2) Axial flight performance, (3) Edgewise flight performance, and (4) Isolated duct results. Chapter 5 concludes the thesis by examining the implications of the results and suggesting future research goals to further the understanding of ducted coaxial rotor systems at MAV scale.

Chapter 2

Design of the Ducted Coaxial Rotor Model and Test Stand

2.1 Introduction

From prior investigations of the aerodynamics of ducted rotor systems as presented in Chapter 1, several important factors have emerged that may affect their overall performance. These parameters include rotor position within the duct, duct inlet shape, clearance between the blade tip and duct, and in the case of coaxial rotors the rotor-to-rotor spacing. To explore the performance of a ducted coaxial rotor operating at MAV scale, and to determine if any specific optimum configuration exists, a ducted coaxial rotor model was designed to permit changes in these parameters to assess their sensitivities. To this end, a test stand was also designed for hover and forward flight performance testing.

2.2 Coaxial Rotor Specifications

The coaxial rotor of the MAV model was a downscaled replica of the U.S. Army Aeroflightdynamics Directorate (AFDD) coaxial rotor system [32]. Each rotor of this system had three blades, a diameter of 4.05 ft, and a chord of 2 inches. For the coaxial rotor designed and tested in the present work, a diameter of 14 inches was selected, yielding a reduction in scale of 0.288 and a blade chord of 0.575 inches. As with the AFDD coaxial rotor, the chord inboard of $0.305R$ was increased by 20% to 0.693 inches at $0.167R$ to

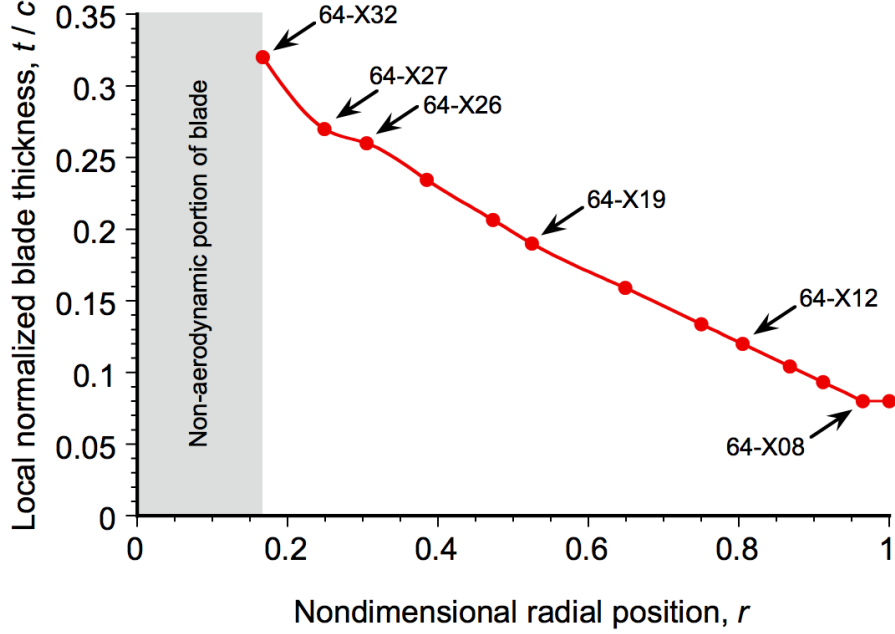


Figure 2.1: Thickness-to-chord distribution of the rotors used in the MAV model.

strengthen the attachment of the aerodynamic portion of the rotor blade to the hub. The thrust weighted solidity of each rotor was 0.078. When considering the operation of both rotors, the coaxial rotor had a thrust weighted solidity (2σ) of 0.157.

The planform used for the rotor was a close variant of the Bell XV-15 tilt-rotor planform [33–35]. Six NACA 64-series airfoils were used with nearly linearly decreasing thickness from $0.32 t/c$ to $0.08 t/c$ (NACA 64-X32, 64-X27, 64-X26, 64-X19, 64-X12, and 64-X08), with seven other stations using airfoils determined through linear interpolation. The normalized thickness distribution of the blade is summarized in Table 2.1 and Fig. 2.1, which denotes the defined blade stations.

The rotor blades were highly twisted (37.4° from root to tip). The pitch distribution, which closely resembles a double linear approximation to the ideal, hyperbolic twist for a hovering rotor [36], is summarized in Table 2.1. In this experiment, the blades had fixed

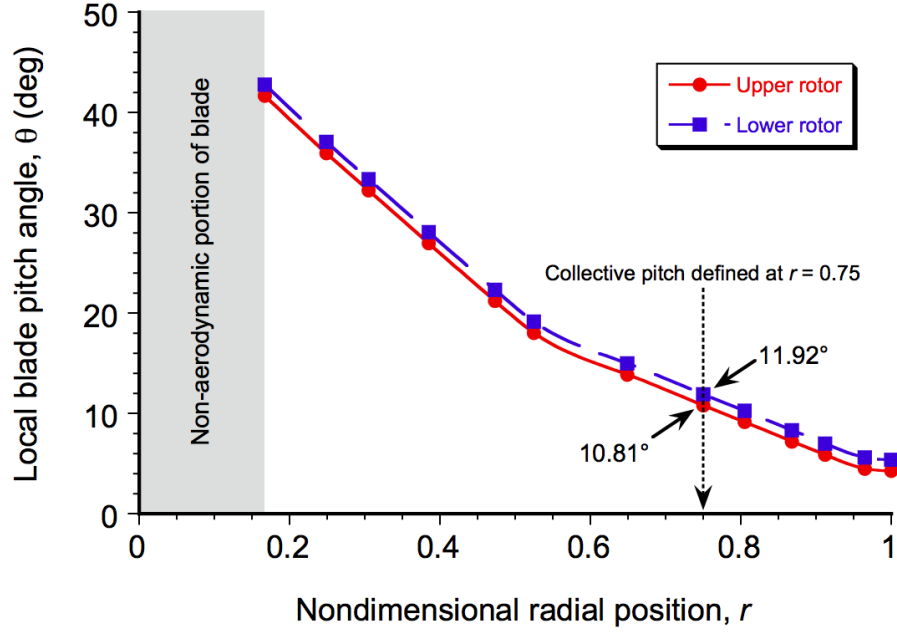


Figure 2.2: Fixed pitch distribution of the rotors used in the MAV model.

pitch.

The nominal collective pitch settings on the rotors were based on two conditions:

- (1) A desired blade loading coefficient of $C_T/\sigma = 0.06$ at the design rotor speed of 9,000 rpm for each rotor in isolation; and
- (2) Achieving a torque balance for the coaxial rotor system with each rotor operating at 9,000 rpm.

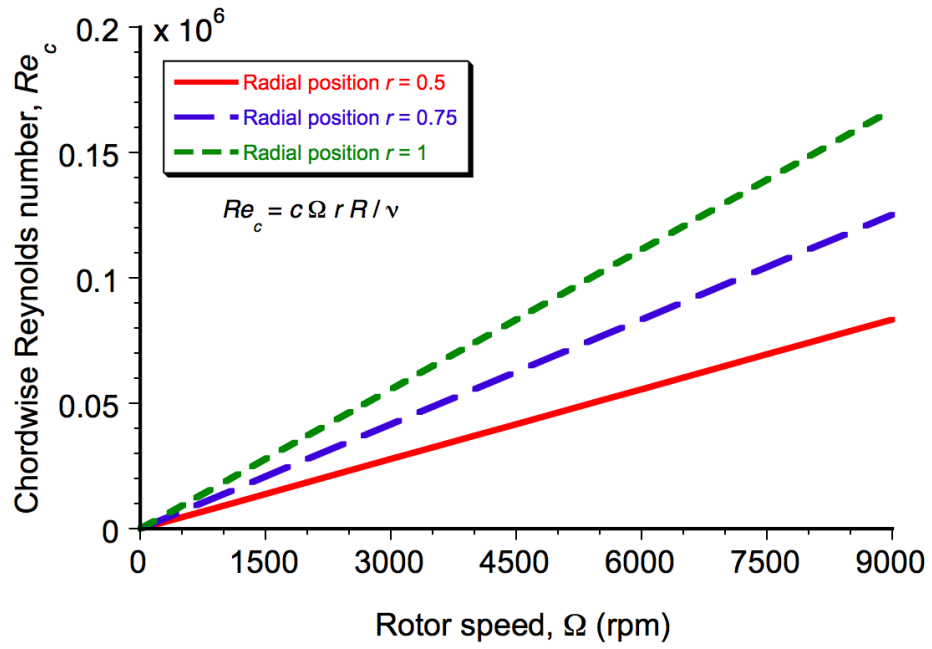
To meet these conditions, a blade element momentum theory analysis [36] was used to predict the collective pitch setting of each rotor, where the collective was defined at the $r = 0.75$ blade span location. From the analysis, an upper rotor blade pitch setting of 10.81° and a lower rotor blade pitch setting of 11.92° were used to meet the two design conditions. The pitch distribution of the upper and lower rotors is shown in Fig. 2.2. Because the lower rotor operates in the wake of the upper rotor, the pitch of the lower rotor was slightly greater to enable a torque balance.

Table 2.1: Blade aerodynamic properties.

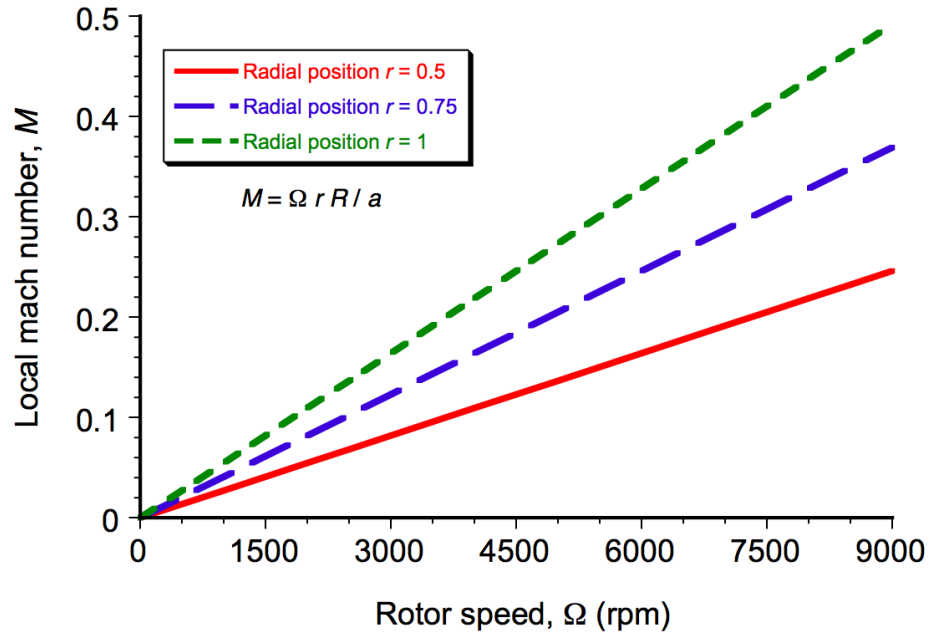
Nondimensional Radial Position	Nondimensional Thickness Distribution	Pitch Distribution
0.167	0.320	30.871°
0.249	0.270	25.149°
0.305	0.260	21.441°
0.385	0.235	16.158°
0.473	0.207	10.418°
0.525	0.190	7.234°
0.649	0.159	3.078°
0.750	0.134	0.000°
0.805	0.120	-1.655°
0.868	0.104	-3.592°
0.912	0.093	-4.922°
0.965	0.080	-6.307°
1.000	0.080	-6.531°

For the operational ranges of rotor speed (up to 9,000 rpm), the rotors operated in relatively low Reynolds number flow (i.e., below 200,000), as shown in Fig. 2.3(a). Under International Standard Atmosphere (ISA) conditions at sea level, the chordwise Reynolds number at the 0.75 radial station was 70,000 at 5,000 rpm, 100,000 at 7,500 rpm, and 125,000 at 9,000 rpm. Some influence of Reynolds number was expected on the rotor loads for all rotor speeds, but most notably below 5,000 rpm. The maximum tip speed for the rotors was $549.8 \text{ ft}\cdot\text{s}^{-1}$ at 9,000 rpm, which equated to a tip Mach number of 0.49 at sea level ISA conditions — see Fig. 2.3(b).

Three rotor-to-rotor spacings for the coaxial rotor were selected for the present



(a) Chordwise Reynolds number.



(b) Mach number.

Figure 2.3: Operational flow characteristics in terms of Reynolds and Mach number, calculated at sea level ISA conditions.

investigation: $0.15R$, $0.20R$, and $0.30R$. This range of separation between the rotors meant that various wake contractions from the upper rotor were obtained, thereby changing the fraction of the lower rotor disk that operated in the wake from the upper rotor. This effect produced varying levels of aerodynamic interference in the coaxial system.

2.2.1 Determination of RP Material for Rotor Fabrication

To manufacture the complex rotor geometry, the rotors were fabricated through a rapid prototyping (RP) process that created the rotors from a computer-aided design (CAD) model. Figure 2.4 shows the rotor CAD model, as drawn in Pro/ENGINEER. The blades had a fixed pitch at the settings estimated to achieve the specified blade loading and torque-balanced conditions at the design rotor speed. The aerodynamic portions of the blades were identical in planform, but the hub of each rotor was different. The design of the lower rotor hub was a hollow cylinder that was compatible with the concentric shaft of the upper rotor. The lower rotor hub was flanged to allow direct mounting to the motor without the need for a shaft. The lower rotor blades were attached to the external hub wall. The upper rotor hub was a cylinder with three counterbores to permit attachment to the motor shaft using screws.

Because many RP materials are not isotropic (their mechanical properties are geometrically dependent), blade specimens of various RP materials were manufactured and subjected to tensile and bending testing to determine the most suitable material for the rotor blades. Tensile testing simulated centrifugal loading and determined the safe operational rotor speed for each RP material. The surface finish of the RP specimens was also

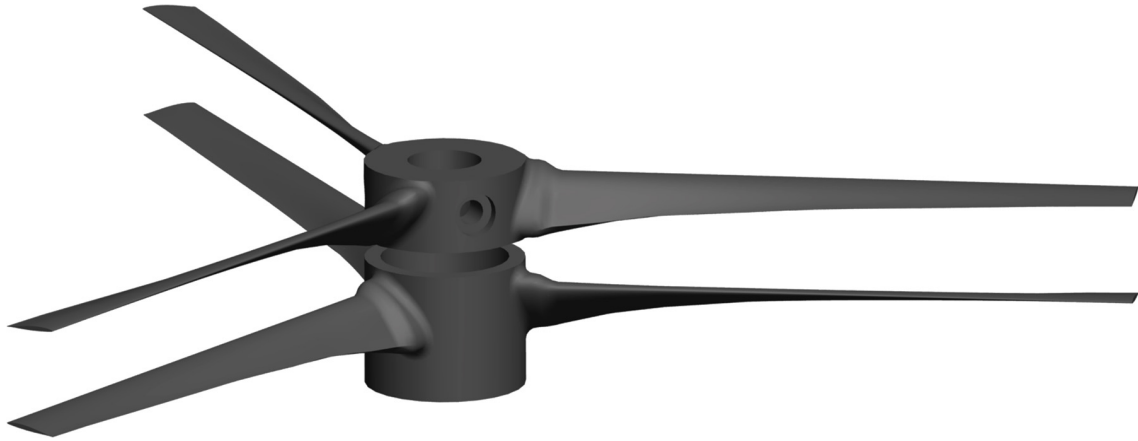


Figure 2.4: Pro/ENGINEER model of the coaxial rotor ($0.15R$ rotor-to-rotor separation).



Figure 2.5: Pro/ENGINEER model of the blade test specimen.

evaluated because a smooth surface finish is aerodynamically desirable. The specimens consisted of a rotor blade 75% of normal length with another hub added at the blade tip, as shown in Fig. 2.5, so the specimen could be mounted in a tensile testing machine. Notice that these specimens did not have the pitch distribution of the rotor blades. The specimens had no twist and had a pitch setting of 30.87° (the local pitch angle at the blade root for a rotor with a 0° collective pitch setting).

Three RP materials were investigated as possible candidate materials for the rotors: 1) Alumide, an aluminum-based compound made by selective laser sintering (SLS); 2) NyTek 1200CF, a carbon fiber-based compound also made by SLS; and 3) Accura 50, a plastic resin made by stereolithography (SLA). In the SLS process, objects are created



Figure 2.6: The three RP blade specimens for tensile, bending, and surface finish assessment.

one cross-section at a time with a laser, fusing powder of a specified material into the desired geometry. Stereolithography (SLA) is similar in that it also builds objects by cross-sectional layers, but the base material is a liquid resin. In particular, the specific SLA process for Accura 50 is considered “high definition stereolithography (HDSL)” because the build layers are approximately 57% of the normal SLA build layer thickness (0.004 inches versus 0.007 inches)¹. The three RP specimens are shown in Fig. 2.6.

2.2.1.1 Tensile Testing of RP Blade Specimens

To determine their tensile strength, the RP specimens were loaded into a tensile testing machine (Fig. 2.7) that applied increasing tension and, in doing so, simulated

¹Solid Concepts, Inc. (<http://www.solidconcepts.com/>)

loading from centrifugal forces in the rotor environment. The tensile testing machine modulated the applied tension to achieve a constant displacement rate of 0.02 in-min^{-1} .

The ultimate tensile strength S_u of each specimen was determined from the maximum sustained tensile stress. The results of the tensile testing are shown in Fig. 2.8. The ultimate tensile strength of the Alumide specimen was $2,286 \text{ lb-in}^{-2}$, which was markedly lower than its nominal (quoted) value of $6,672 \text{ lb-in}^{-2}$ (a 66% difference). The NyTek 1200CF specimen was stronger than the Alumide specimen at $5,563 \text{ lb-in}^{-2}$, but this too was a large difference (41%) from its quoted strength of $9,500 \text{ lb-in}^{-2}$. The Accura 50 specimen was the strongest with a maximum strength of $6,840 \text{ lb-in}^{-2}$. This result was only a 5% difference from the quoted value of $7,200 \text{ lb-in}^{-2}$. While some differences from the quoted values were expected because of material anisotropy, the mechanical properties of the SLS materials in particular exhibited larger than expected differences between quoted and tested strengths.

With the actual tensile strengths of the RP materials obtained, a blade element analysis was developed to predict the tensile stress from centrifugal forces at the design rotational speed Ω of 9,000 rpm for each RP material. With the ultimate tensile strength of each RP specimen known, the operational capability of a rotor fabricated from each RP material at 9,000 rpm could be assessed. The tensile stress from centrifugal loading was influenced by the mass-per-length m of the rotor blade, which was a function of blade cross-section A and RP material density ρ . For this analysis, the material density was computed using the mass of each blade specimen, which was measured with a mass scale to an accuracy of 0.1 g, and the volume of the specimen CAD geometry. Only the Alumide specimen had a notably different density from its quoted value (26% less dense).



Figure 2.7: Tensile testing of the Accura 50 blade specimen.

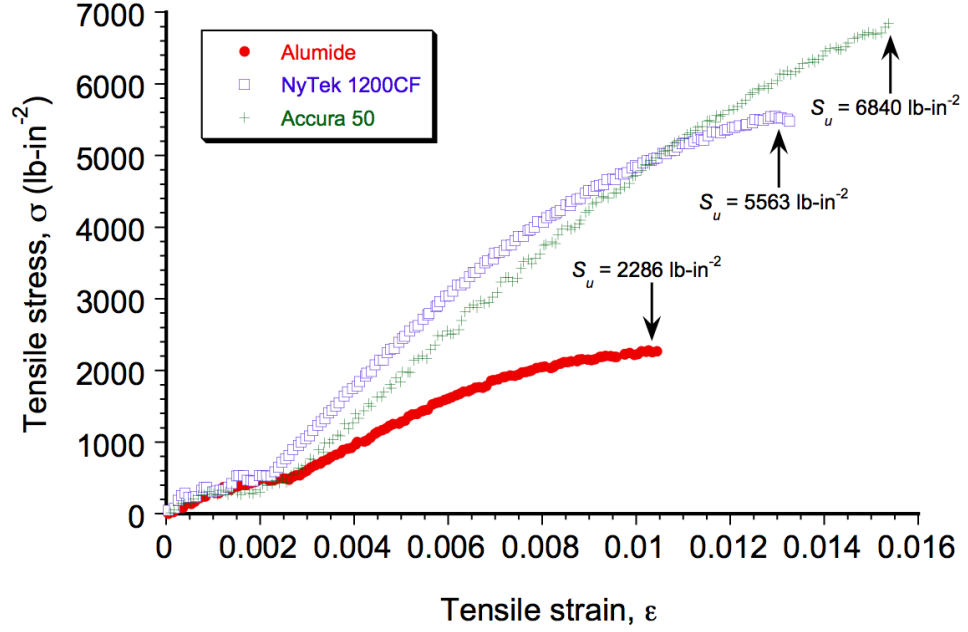


Figure 2.8: Results of tensile testing of the three RP blade specimens (only 30% of the data shown).

NyTek 1200CF and Accura 50 were both within 2% of the quoted value (1% more dense and 2% less dense, respectively).

Consider a rotor blade with mass-per-length m , which is determined by the rotor material. A small blade element of span dy has mass mdy and rotates at an angular velocity (rotor speed) of Ω , experiencing a centrifugal force dF_{CF} equal to

$$dF_{CF} = (mdy)y\Omega^2 = m\Omega^2 ydy$$

The blade element model discretized the rotor blade into n such blade elements from $y = 0$ to $y = R$, each of width Δy . For this analysis, $n = 100$ blade elements were used. In discretized form, the centrifugal force acting on the i -th blade element is expressed as

$$\Delta F_{CF}(i) = m(i)\Omega^2 y(i)\Delta y$$

The mass-per-length m of the rotor blade varies with the cross-section of each element, assuming a constant material density. Therefore, $m(i) = \rho A(i)\Delta y$. The total centrifugal force acting on any element of the rotor blade is the sum of the centrifugal force contributed by all outboard elements (i -th to n -th element, inclusive), i.e.,

$$F_{CF}(i) = \sum_i^n m(i)\Omega^2 y(i)\Delta y$$

The predicted tensile stress from centrifugal force loading at any element along the rotor blade is then

$$\sigma_{CF}(i) = \frac{F_{CF}(i)}{A(i)}$$

The predicted stress from the blade element analysis was normalized by the material ultimate tensile strength obtained by tensile testing. This normalized stress is the inverse of the safety factor (SF); smaller normalized stress suggests a greater margin of safety. These results are shown in Fig. 2.9, whereby a direct comparison of the relative strengths of each material can be made. The maximum stress of each specimen was estimated to be located at $r = 0.335$, which was just outboard of the structural increase in chord. At the design condition of 9,000 rpm, it was evident that Alumide was the weakest in tension, offering a safety factor of only 2.145. The results were more favorable for Nytek 1200CF and Accura 50, which had respective safety factors of 4.865 and 5.463. These results are also summarized in Table 2.2.

2.2.1.2 Bending Testing of RP Blade Specimens

While rotor blades must be able to withstand great centrifugal forces while in normal operation, they must also be resistant to bending loads, which primarily manifest

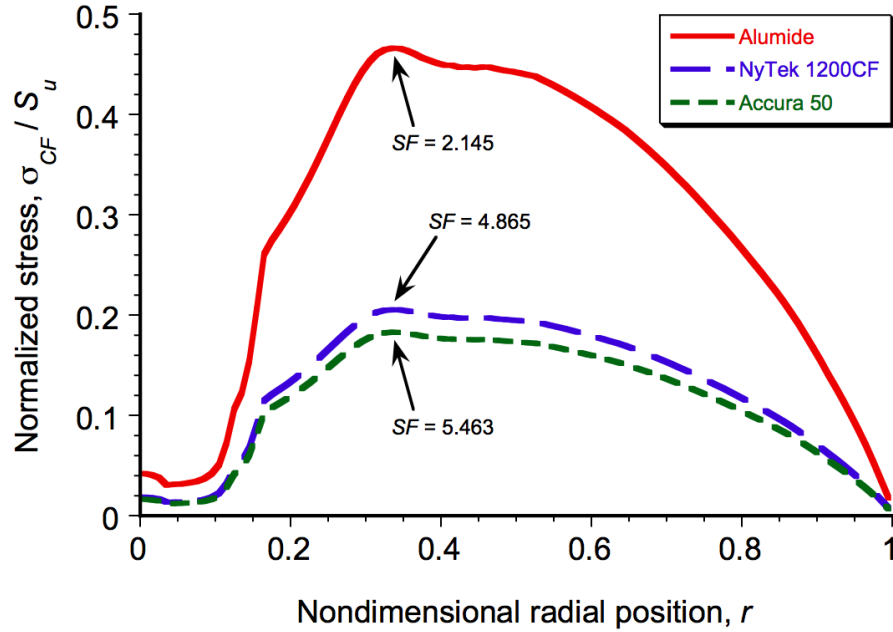


Figure 2.9: Prediction of stress from centrifugal forces along the rotor blade at 9,000 rpm, normalized by the ultimate tensile strength of each material.

from lift forces. Therefore, good bending stiffness was also desirable in the RP material used for the present rotor. To assess the bending stiffnesses of the three RP materials, a bending test was conducted on the blade specimens. This evaluation consisted of loading each specimen with increasing mass and measuring the resulting tip deflection with a scale to an accuracy of 0.020 inches (0.5 mm). The loading point was 3 inches from the edge of the rotor hub, where the aerodynamic portion of the blade began. It should be noted that bending testing was conducted after tensile testing, so the blade specimens lacked the second hub, which separated upon tensile failure. Prior to bending testing, the specimens were shortened to the same length by grinding down the blade tip.

The testing results are shown in Fig. 2.10 and Table 2.2. The bending stiffness of each material was estimated from the slope of the fitted linear curve, assuming a linear

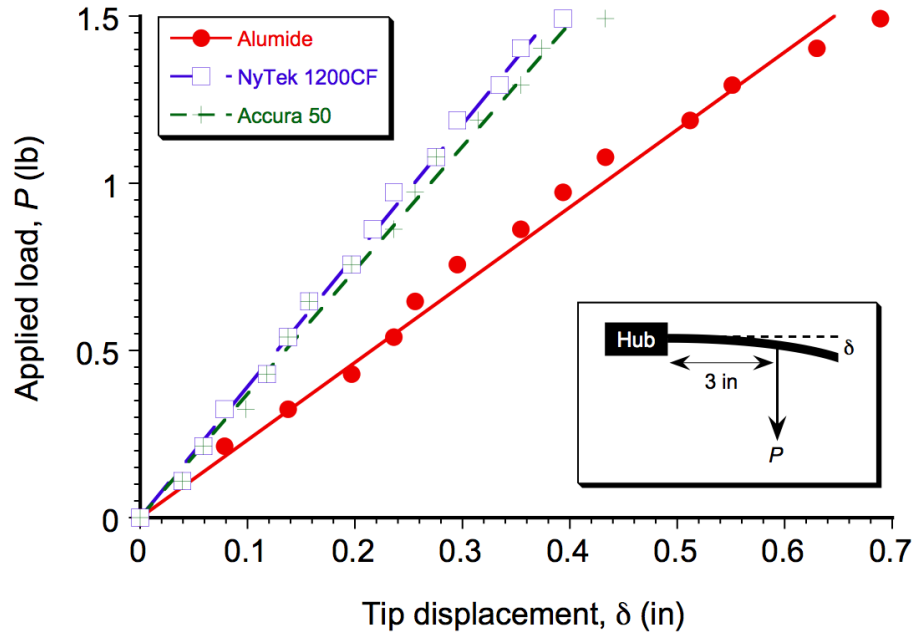


Figure 2.10: Results of bending testing of the three RP blade specimens.

stress and strain relationship during bending. In Fig. 2.10, a steeper slope indicates a greater bending stiffness, thereby needing a greater applied load P to produce the same tip displacement δ .

The fitted curve slopes of the three specimens were normalized with respect to the fitted curve slope of Accura 50 to indicate the relative bending stiffness of all specimens. This value for Alumide and NyTek 1200CF was 0.628 and 1.061, respectively. Therefore, the NyTek 1200CF blade specimen had marginally better bending stiffness than the Accura 50 specimen. The Accura 50 specimen was by far the most compliant of the RP specimens.

2.2.1.3 Surface Finish of RP Blade Specimens

Finally, the surface finish of each blade specimen was evaluated. The results are presented in Table 2.2. A smooth surface finish is aerodynamically desirable as it minimizes profile drag and reduces rotor power. The qualitative assessment of surface finish was done on a five-point scale ranging from bad (1), poor (2), fair (3), good (4), to superior (5). Materials with surface finishes less than superior (5) would require some surface alteration (by sanding or other appropriate methods) to improve the smoothness. The Alumide specimen was coarse, earning a poor (2) rating. NyTek 1200CF was good (4) as it was not entirely smooth, but it had a much improved finish than Alumide. Accura 50 was smooth without any irregularities, earning a superior (5) rating.

2.2.1.4 Summary of RP Blade Specimen Testing

Because of its strong tensile strength, good bending stiffness, and superior surface finish, Accura 50 was selected as the best RP material for fabricating the rotors for use in the present experiments. The results of all RP testing are summarized in Table 2.2.

Table 2.2: Results of RP specimen testing.

RP Material	RP Type	Safety Factor at 9,000 rpm	Relative Bending Stiffness	Surface Finish
Alumide	Aluminum SLS	2.145	0.628	Poor (2)
NyTek 1200CF	Carbon fiber SLS	4.865	1.061	Good (4)
Accura 50	Resin SLA	5.463	1.000	Superior (5)

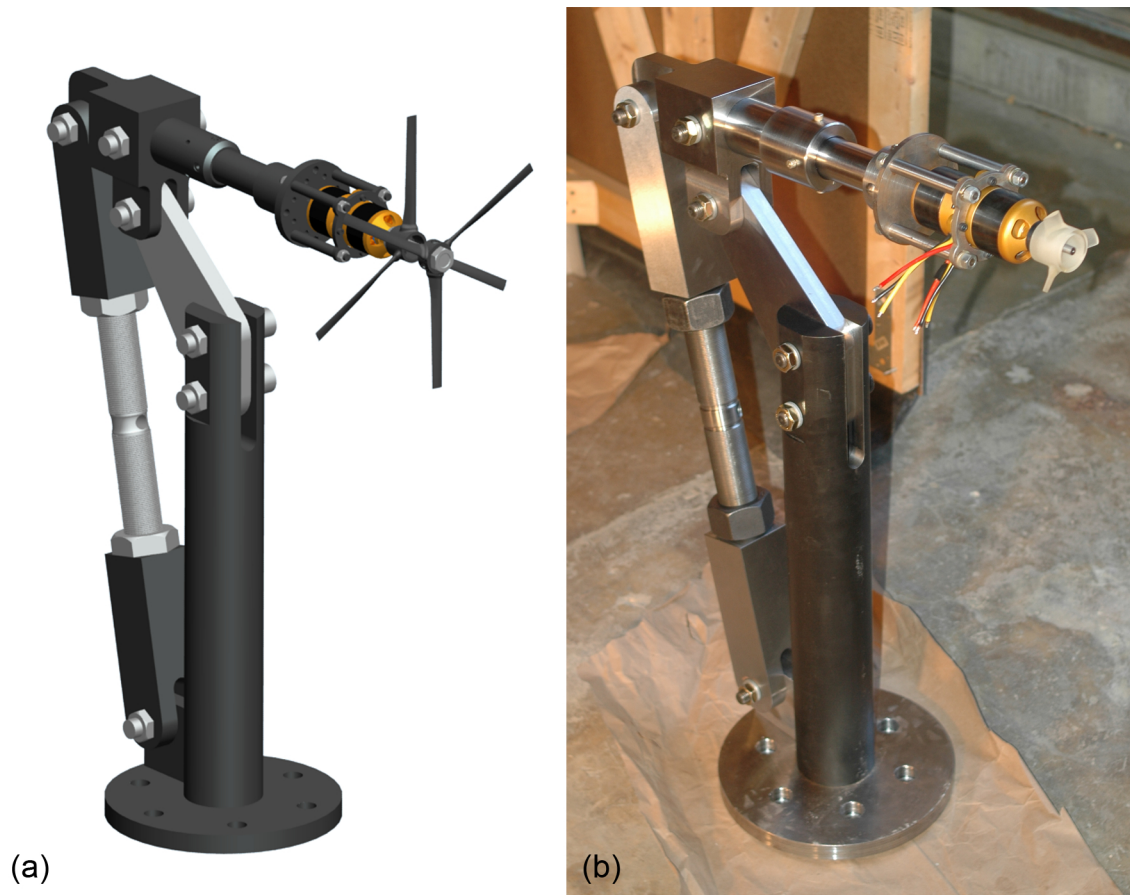


Figure 2.11: The rotor test stand, shown (a) as Pro/ENGINEER model and (b) as manufactured.

2.3 Rotor Test Stand and Sting Assembly Structural Design

To conduct performance testing of the MAV model in hover and forward flight, a test stand was designed and fabricated. The test article connected to the test stand via the sting assembly, a column of structural elements that enclosed the load-sensing balance. Figure 2.11 compares the CAD design to the manufactured product.

2.3.1 Rotor Test Stand

The primarily structural member of the test stand was the cylindrical vertical mast, which had a 4 inch outer diameter, 2 inch inner diameter, and truncated in a 1 inch thick circular flange base. The flange had a six bolt pattern attachment to interface with the support post and balance of the Glenn L. Martin Wind Tunnel, where most performance testing was conducted. This part, along with all other components of the stand, was fabricated from steel.

The test stand was designed with two innovative features: (1) A threaded diagonal support post and (2) An offset hinge. Together these features enabled the pitch of the test article to be changed from 0° (axial flow orientation) to 90° (edgewise flow orientation), as illustrated in Fig. 2.12. The maximum attainable pitch was 100° . The support post had left- and right-handed threads on each side; rotation of the support post simultaneously contracted or expanded the post relative to the adjoining post connectors that had compatible internal threads. In this way, the functionality was analogous to a turnbuckle. However, the post was anchored at the bottom to the vertical mast so contraction or expansion was only one-sided. This effect produced rotation of the sting assembly about the offset hinge formed by the diagonal connecting plate, thereby setting the pitch angle of the test article. The threads of the support post were $12 \text{ thread-in}^{-1}$, allowing smooth continuous pitch modulation without removal of the model from the stand.

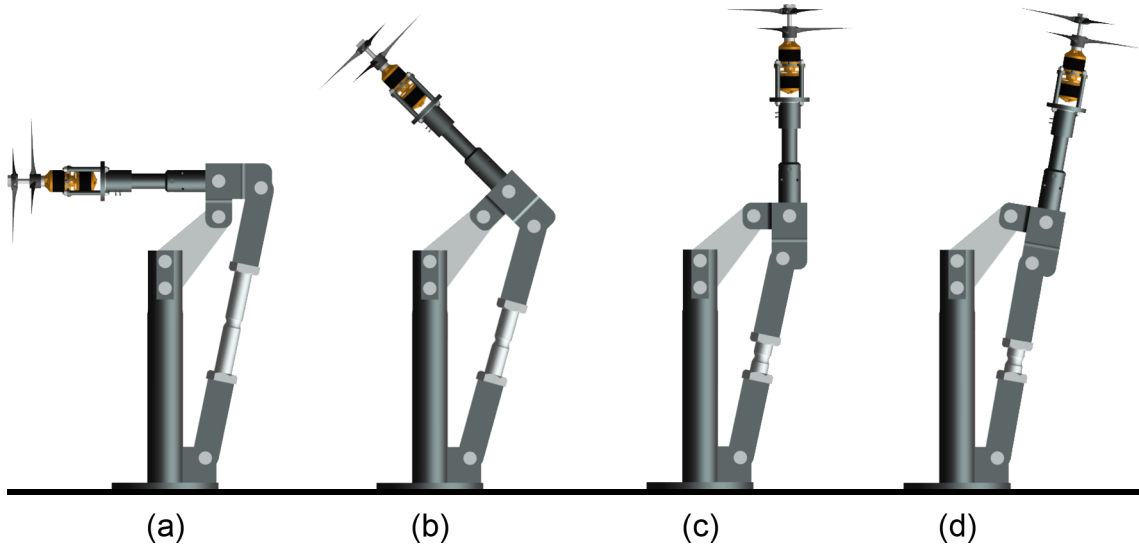


Figure 2.12: Complete variation of test article pitch as drawn with Pro/ENGINEER, showing (a) 0° (axial), (b) 45° , (c) 90° (edgewise), and (d) 100° orientations.

2.3.2 Rotor Sting Assembly

The rotor sting assembly was designed to secure the test article to the test stand and also functioned as the load path for the balance. This assembly is illustrated in Fig. 2.13. To limit weight, all structural elements of the sting assembly were manufactured from aluminum.

The upper and lower rotors of the MAV model were separated by a shaft adapter to achieve a desired rotor spacing. Specifically, three shaft adapters of varying length were fabricated to achieve rotor-to-rotor separations of $0.15R$, $0.20R$, and $0.30R$. The lower rotor attached axially to the upper motor directly (without a shaft). The concentric shaft of the lower motor attached to the shaft adapter of the upper rotor by two set screws. The coaxial motor was secured by the motor halo. Four spacing rods separated the motor halo from the motor baseplate to allow ample space for the lower motor to rotate. Connected

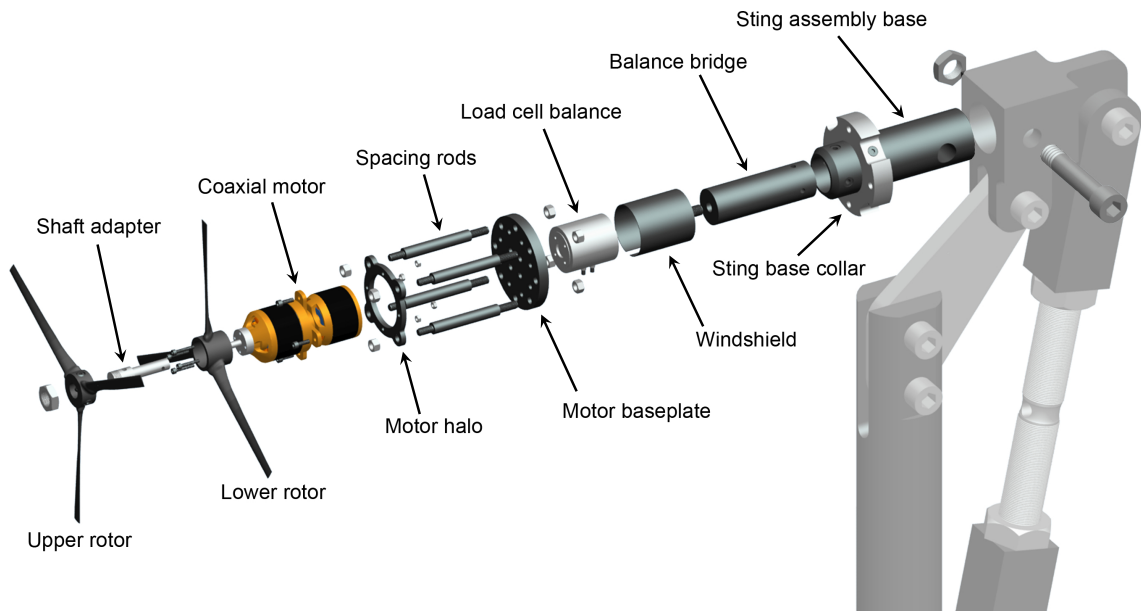


Figure 2.13: The structural components of the rotor sting assembly, as drawn with Pro/ENGINEER.

to the baseplate was the load cell balance, which was located as close to the model as possible to reduce the extraneous moment loading from the weight of the model.

The balance connected to the inside base of the cylindrical windshield, which had a wall thickness of $1/32$ (0.031) inches; this shield blocked the free-stream from interfering with the balance when the model was pitched or yawed in forward flight. The windshield had an external threaded stud (diameter of 0.5 inches) that connected to the balance bridge, whose primary role was to separate the MAV model from the test stand. The balance bridge, in turn, interfaced to the sting assembly base by means of a cylindrical slip-fit connection and four set screws. The cylindrical sting base was secured to the rotor test stand by another cylindrical slip-fit and locking shoulder bolt with a 1 inch diameter. The sting collar, which attached to the sting base, served as the mounting location for the duct.

2.3.3 Proximity of Model and Aerodynamic Interference

In the performance testing of powered rotors, the proximity of the test article to the ground or similar boundary is a critical factor. Ideally, the model should be as far away as possible from such boundaries to eliminate aerodynamic interference effects, which would otherwise introduce bias into the performance measurements. A general rule to mitigate aerodynamic interference is to ensure that the proximity of the rotor to any boundary is at least two rotor diameters (in the present case, 28 inches). Ground effect, a common source of aerodynamic interference, is of primary concern as it may arise in either hover or forward flight performance testing. In forward flight testing, ideally the rotor must lie along the vertical and horizontal centerlines of the wind tunnel test section for the model to operate in the most uniform flow.

The dimensions of the test stand and rotor sting assembly, therefore, were not trivial decisions because this affected the proximity of the test article to the floor or walls of the wind tunnel. Forward flight testing was conducted in the Glenn L. Martin Wind Tunnel, which has a test section that is 7.75 feet high by 11.04 feet wide. When the test article was vertical (edgewise flight testing), the distance of the upper rotor at maximum rotor spacing was approximately 54 inches from the ground and approximately 39 inches from the ceiling of the test section. When testing in propeller mode (axial flight), the rotor sting assembly centerline was approximately 35 inches from the ground. When at full yaw angle (i.e., free-stream orthogonal to the test article), the upper rotor at maximum rotor spacing was approximately 49 inches from the nearest test section wall. Therefore, in all pitch or yaw orientations, aerodynamic interference (in the form of ground effect or

otherwise) was minimized to the best extent possible.

2.4 Duct Specifications

Because a duct is an annular airfoil, the duct for the present work was designed by first deriving its cross-sectional airfoil shape. Two such airfoils were investigated with different amounts of camber at the leading edge. Various chordwise locations along these airfoils were identified for pressure tap placement so the pressure distribution on the duct could be measured. Pressure measurements, however, were not obtained in the present study.

2.4.1 Derivation of Duct Airfoils

Two types of leading edges, symmetric and cambered, were selected to evaluate the effects of the duct inlet shape on performance. The trailing edge was derived from a NACA symmetric airfoil and was common between the two duct airfoils. Excluding the leading edge, the lower surface of the airfoils was flat to achieve the same tip clearance at any rotor position within the duct. The symmetric duct airfoil had a circular leading edge equal to one-half of the duct thickness, $\frac{t_d}{2}$, so this duct shape was also referred to as the “flat-plate” duct. The cambered airfoil was defined using a series of equations that were derived by Küchemann and Weber [37]. This so-called “Küchemann airfoil” was specifically intended to investigate the effects of camber on the performance of ducted test articles.

Portions of the duct airfoil equations were derived nondimensionally in terms of

the duct chord c_d . In these cases, the abscissa and ordinate were given by $\bar{x} = x/c_d$ and $\bar{y} = y/c_d$. While standard airfoils such as the NACA series are defined by an upper and lower thickness distribution relative to the mean camber line, the duct airfoils in the present work were each defined in two segments: a leading edge and a trailing edge. Each of these segments was defined by their outer and inner surfaces, so a total of four equations were used to completely define each duct airfoil. A maximum duct thickness t_d of 1 inch was selected to allow sufficient space for internal load-bearing structure. The duct inner diameter d_i was 14.14 inches to achieve a nominal tip clearance of $0.01R$ (0.07 inches). To maintain a desired duct inner diameter to duct chord ratio of 1.733, the duct chord c_d was 8.078 inches.

2.4.1.1 Cambered Leading Edge

The Küchemann leading edge is a pair of elliptical shapes that form the inner and outer portions of a duct inlet. The shape is defined in terms of four constants K_1 , K_2 , K_3 , and K_4 , and an area ratio A_i/A_m , which is the ratio between the circular cross-sectional areas of the interior and exterior of the duct. Varying these parameters alters the geometry, such as the roundness of the leading edge nose or the size of the inner or outer lengths. The area ratio was determined from an inner radius R_i of $1.01R$ (7.07 inches) and a duct thickness t_d of 1 inch. Therefore, $R_m = R_i + t_d$ (8.07 inches) and $A_i/A_m = R_i^2/R_m^2 = R_i^2/(R_i + t_d)^2 = 0.768$. The Küchemann constants were then obtained by selecting appropriate values such that a proper cambered airfoil shape was constructed

using the equations

$$y_{K_m} = R_0 + (R_m - R_0) \sqrt{1 - (1 - x/L_m)^2} \quad 0 \leq x \leq L_m \quad (2.1)$$

$$y_{K_i} = \begin{cases} R_0 - (R_0 - R_i) \sqrt{1 - (1 - x/L_i)^2} & 0 \leq x \leq L_i \\ R_i & L_i \leq x \leq L_m \end{cases} \quad (2.2)$$

which are given in terms of parameters that define the leading edge geometry, namely the center radius R_0 , outer length L_m , and inner length L_i . These parameters are, in turn, defined by Eqs. (2.3)–(2.5), which utilize the Küchemann constants

$$R_0 = K_1 R_i \quad (2.3)$$

$$L_m = \frac{R_m}{K_2 + K_3 \left(\frac{A_i}{A_m} \right)^3} \quad (2.4)$$

$$L_i = K_4 (K_1 - 1) R_i \quad (2.5)$$

In the present design, the values of these constants were found to be $K_1 = 1.05$, $K_2 = 0.2$, $K_3 = 8.58$, and $K_4 = 1.5$. Notice that the outer length L_m dictated the overall length of the leading edge segment for both the Küchemann and flat-plate geometries. Therefore, the NACA trailing edge began at the end of the outer curved length, $x = L_m = 0.245c_d$ (1.978 inches).

2.4.1.2 Symmetric Leading Edge

The flat-plate leading edge is a circular shape symmetric about the line $y = \frac{t_d}{2}$. The upper and lower surfaces are derived from the equation of a circle with the circular nose centered at $\left(\frac{t_d}{2}, \frac{t_d}{2} \right)$, i.e.,

$$y = \pm \sqrt{\left(\frac{t_d}{2} \right)^2 - \left(x - \frac{t_d}{2} \right)^2} + \frac{t_d}{2} \quad (2.6)$$

Equation (2.6) was separated into two equations for the outer and inner surfaces.

At $x = \frac{t_d}{2}$, the leading edge nose terminates, and the leading edge then continues tangent to the semicircle until the trailing edge begins at $x = L_m = 0.245c_d$ (for this leading edge, $L_m = L_i$), i.e.,

$$y_{FPm} = \begin{cases} \sqrt{\left(\frac{t_d}{2}\right)^2 - \left(x - \frac{t_d}{2}\right)^2} + \frac{t_d}{2} & 0 \leq x \leq \frac{t_d}{2} \\ \frac{t_d}{2} & \frac{t_d}{2} \leq x \leq L_m \end{cases} \quad (2.7)$$

$$y_{FPi} = \begin{cases} -\sqrt{\left(\frac{t_d}{2}\right)^2 - \left(x - \frac{t_d}{2}\right)^2} + \frac{t_d}{2} & 0 \leq x \leq \frac{t_d}{2} \\ 0 & \frac{t_d}{2} \leq x \leq L_m \end{cases} \quad (2.8)$$

2.4.1.3 NACA Trailing Edge

Both duct airfoils utilized a common trailing edge based on a NACA symmetric airfoil. The upper and lower surfaces of this airfoil type are defined nondimensionally as

$$\bar{y}_u = 5\bar{t} \left(A\sqrt{\bar{x}} + B\bar{x} + C\bar{x}^2 + D\bar{x}^3 + E\bar{x}^4 \right) \quad (2.9)$$

$$\bar{y}_l = -5\bar{t} \left(A\sqrt{\bar{x}} + B\bar{x} + C\bar{x}^2 + D\bar{x}^3 + E\bar{x}^4 \right) \quad (2.10)$$

where $\bar{x}$, $\bar{y}$, and $\bar{t}$ are normalized by the airfoil chord, and the constants are equal to $A = 0.2969$, $B = -0.1260$, $C = -0.3516$, $D = 0.2843$, and $E = -0.1015$ [36].

To be used as a duct airfoil, Eq. (2.9) was multiplied by two, and Eq. (2.10) was equated to zero, so the same thickness distribution was retained while achieving the necessary flat inner surface. The airfoil chord used for normalization was the duct chord c_d , i.e.,

$$\bar{y}_{TEu} = 10\bar{t}_d \left(A\sqrt{\bar{x}} + B\bar{x} + C\bar{x}^2 + D\bar{x}^3 + E\bar{x}^4 \right) \quad (2.11)$$

$$\bar{y}_{TEl} = 0 \quad (2.12)$$

To find the location of maximum thickness, and therefore the crossover point between the leading edge and trailing edge, the derivative of Equation (2.11) was solved, i.e.,

$$\frac{\partial \bar{y}_{TEu}}{\partial \bar{x}} = 10\bar{t}_d \left(A\sqrt{\bar{x}} + B\bar{x} + C\bar{x}^2 + D\bar{x}^3 + E\bar{x}^4 \right) = 0 \quad (2.13)$$

Equation (2.13) does not have an analytic solution. However, the numeric solution determined using MATLAB was $\bar{x}_{NACA} = 0.300$.

Because this crossover point is not equal to the outer length of the K uchemann airfoil leading edge ($L_m/c_d = \bar{L}_m = 0.245$), the NACA trailing edge was geometrically scaled such that these two points were congruent. This transformation $\bar{x} \Rightarrow \bar{x}'$ was performed using the function $\bar{x}'(\bar{x}) = \bar{x} + T(\bar{x})$. The transformation function $T(\bar{x})$ is linear and equal to $m\bar{x} + b$. The constants m and b were determined from two known conditions. The scaling was about $\bar{x} = 1$, so $\bar{x}'(\bar{x} = 1) = 1$. It follows that $m + b = 0$. The other condition was developed from scaling the trailing edge in $\bar{x}$ from $\bar{x}_{NACA}$ to $\bar{L}_m$. Therefore, $\bar{x}'(\bar{x} = \bar{x}_{NACA}) = \bar{L}_m$, and it follows that $m\bar{x}_{NACA} + b = \bar{L}_m - \bar{x}_{NACA}$. From these two equations, the two parameters of the transformation function m and b can be solved, i.e.,

$$\begin{aligned} \begin{bmatrix} 1 & 1 \\ \bar{x}_{NACA} & 1 \end{bmatrix} \begin{bmatrix} m \\ b \end{bmatrix} &= \begin{bmatrix} 0 \\ \bar{L}_m - \bar{x}_{NACA} \end{bmatrix} \\ \begin{bmatrix} m \\ b \end{bmatrix} &= \begin{bmatrix} 1 & 1 \\ \bar{x}_{NACA} & 1 \end{bmatrix}^{-1} \begin{bmatrix} 0 \\ \bar{L}_m - \bar{x}_{NACA} \end{bmatrix} = \begin{bmatrix} \frac{\bar{x}_{NACA} - \bar{L}_m}{1 - \bar{x}_{NACA}} \\ \frac{\bar{L}_m - \bar{x}_{NACA}}{1 - \bar{x}_{NACA}} \end{bmatrix} \\ T(\bar{x}) &= \frac{\bar{x}_{NACA} - \bar{L}_m}{1 - \bar{x}_{NACA}} (\bar{x} - 1) \end{aligned} \quad (2.14)$$

Equations (2.11) and (2.12) are transformed such that $\bar{x} \Rightarrow \bar{x}'$. Because $\bar{x}' = \bar{x} + T(\bar{x})$, the equations for the duct trailing edge become

$$y_{TEm} = 10t_d \left(A \sqrt{\frac{x}{c_d} + T\left(\frac{x}{c_d}\right)} + B \left(\frac{x}{c_d} + T\left(\frac{x}{c_d}\right)\right) + C \left(\frac{x}{c_d} + T\left(\frac{x}{c_d}\right)\right)^2 + D \left(\frac{x}{c_d} + T\left(\frac{x}{c_d}\right)\right)^3 + E \left(\frac{x}{c_d} + T\left(\frac{x}{c_d}\right)\right)^4 \right) \quad L_m \leq x \leq c_d \quad (2.15)$$

$$y_{TEi} = 0 \quad L_m \leq x \leq c_d \quad (2.16)$$

2.4.1.4 Summary

With the common NACA trailing edge solved in Eqs. (2.15) and (2.16), the total cambered duct airfoil is shown in Fig. 2.14(a) by incorporating the Küchemann leading edge derived in Eqs. (2.1) and (2.2). Similarly, the symmetric duct airfoil shown in Fig. 2.14(b) is derived from the synthesis of the flat-plate leading edge equations (Eqs. (2.7) and (2.8)) and the NACA trailing edge (Eqs. (2.15) and (2.16)).

2.4.2 Chordwise Pressure Tap Locations

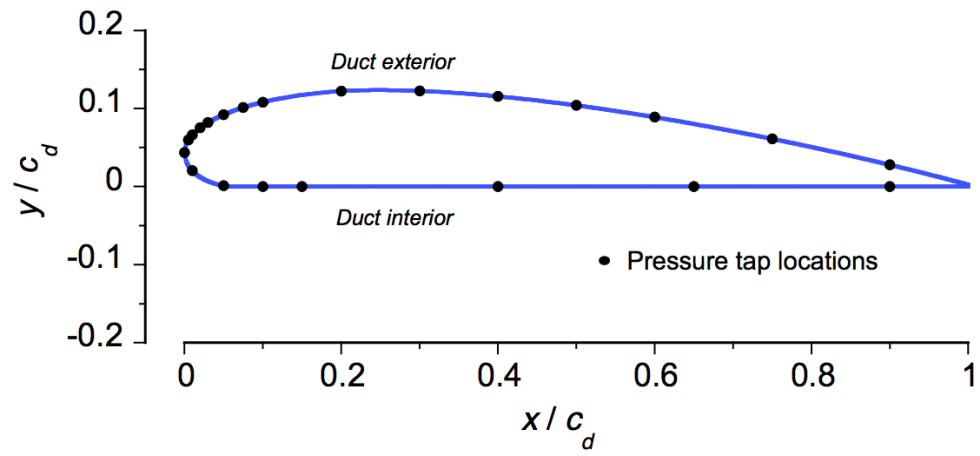
Twenty-two locations along the duct airfoils were identified for pressure tap placement; these locations are given in Table 2.3. Relatively more taps were placed at the leading edge to capture the suction peak. The chordwise placement of these pressure tap locations on the duct airfoils is shown in Fig. 2.14.

Table 2.3: Chordwise pressure tap locations on the duct, normalized by duct chord.

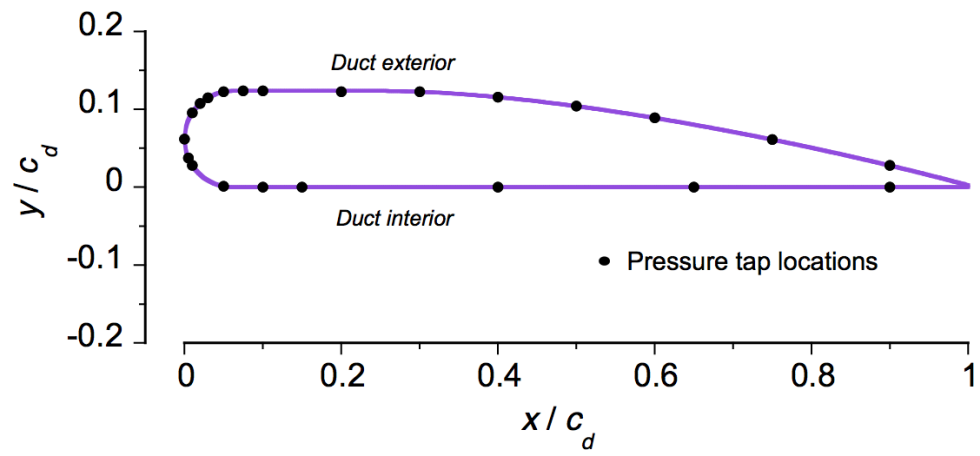
Inner Surface	Outer Surface
0.000	
0.010	0.005
0.050	0.010
0.100	0.020
0.150	0.030
0.400	0.050
0.650	0.075
0.900	0.100
	0.200
	0.300
	0.400
	0.500
	0.600
	0.750
	0.900

2.5 Duct Assembly Structural Design

The duct assembly was an assortment of components that formed two major assemblies: (1) The duct, including internal load-bearing components; and (2) The duct base, which secured the duct and attached to the rotor sting assembly. The assembly is detailed in Fig. 2.15.



(a) Cambered duct airfoil.



(b) Symmetric duct airfoil.

Figure 2.14: The cambered and symmetric duct airfoils, with placement of the 22 chord-wise pressure taps.

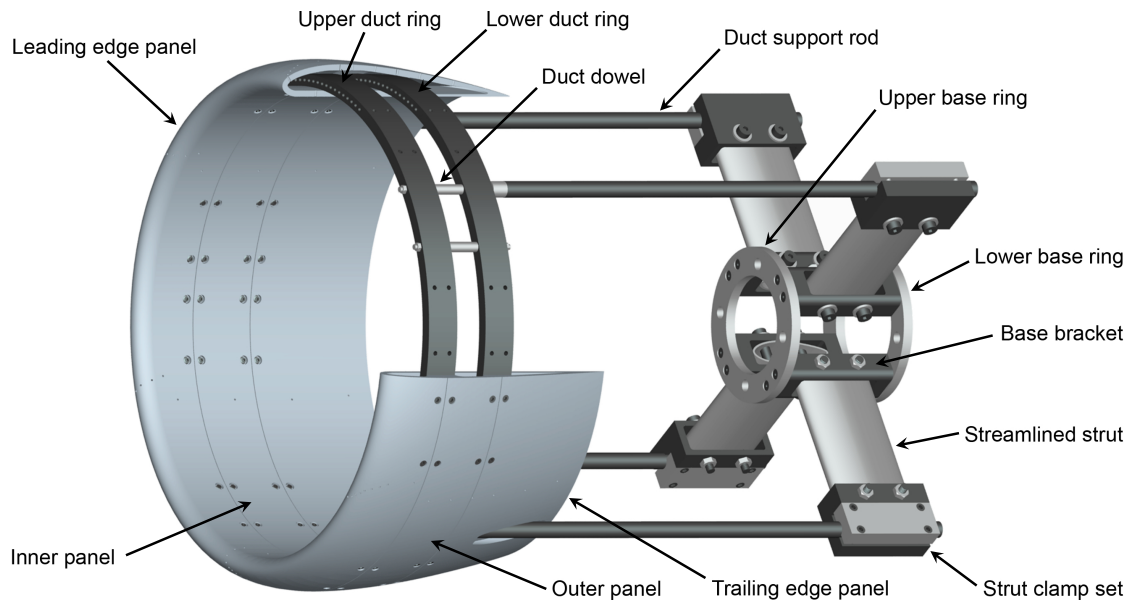


Figure 2.15: The structural components of the duct and the duct base used in total forming the duct assembly.

2.5.1 Duct Design

To achieve the necessary structural integrity (in terms of strength and stiffness) for testing, the duct was designed as a cage, utilizing inner load-bearing rings and dowels and external sectioned panels that gave the duct its aerodynamic shape. The duct panels were fabricated through rapid prototyping (RP) and attached radially to the duct rings, forming a shell that covered the internal cage. Another method of duct fabrication, creating the duct entirely from machinable polyurethane foam, was considered, as it had been utilized in experiments using a 16 cm (6.3 inches) diameter MAV scale rotor [19–21]. However, this shell-and-cage method of duct fabrication was ultimately used because it provided a much greater robust and rugged test article for the Glenn L. Martin Wind Tunnel, while still having the ability to accurately control the duct geometry.

2.5.1.1 Duct Panels

The duct panels were drawn using Pro/ENGINEER and were fabricated by rapid prototyping using the high-definition SLA Accura 50 material, which was the same material used for the rotors. Previous experience with rapid prototyping suggested that specimens tended to be brittle in locations with little material (e.g., rotor blade trailing edge), so a nominal panel thickness of 0.188 (3/16) inches was selected. This thickness also provided the panels with limited load-bearing capability as a precaution; the internal cage structure was designed to be the duct load path.

The duct panels mounted to the duct rings radially through a series of screws with the counterbores drawn in the CAD models. When attached, the screw head sat flush with the surface of the duct panel, and putty was used to fill in the exposed remainder of the counterbore for a smooth transition, limiting boundary layer disturbances as much as possible. To accommodate the screw attachment to the duct, the height of the duct rings was selected to be 1 inch, which allowed sufficient clearance for two duct panels to attach axially to both sides of a ring. The thickness of the duct rings was determined to be 0.375 (3/8) inches, which provided the necessary thickness for the screws to have sufficient threading on both the interior and exterior of the duct rings.

In total, six panels were used to define the duct shape. The trailing edge panel attached entirely to the bottom of the lower ring. The inner and outer panels created the shape of the middle of the duct. Each of these panels only covered half of the duct, so two inner and two outer panels were required to fully enclose the duct. Each panel attached to the upper and lower ring. Lastly, the leading edge panel mounted to the top of the upper

ring. Two leading edge panels were fabricated, one for each leading edge. The leading edge panels were interchangeable, so exchanging this one part could change the duct type. One set of an inner and outer panel could potentially be removed to allow visibility into the duct interior while the rotors were operational for optical flow measurements (flow visualization, PIV, etc).

2.5.1.2 Duct Interior

The internal design of the duct structure consisted of sizing and placing the duct rings within the duct chordwise cross-section. The width and thickness of the duct rings were determined from design of the duct panels. The upper duct ring was positioned at the junction between the leading and trailing edges so that changing the leading edge panel could reconfigure the duct type. Figure 2.16 shows the four constraints that bounded the placement of the lower ring:

1. The minimum panel thickness precluded the lowest 0.188 (3/16) inches of the duct cross-section from use.
2. The thickness of the duct rings bounded the placement of the lower duct ring to be in-line with the upper ring.
3. The placement of the upper ring centerline at the intersection of the leading and trailing edges bounded the forward chordwise placement.
4. The minimum panel thickness on the exterior side of the duct rings bounded the aft chordwise placement.

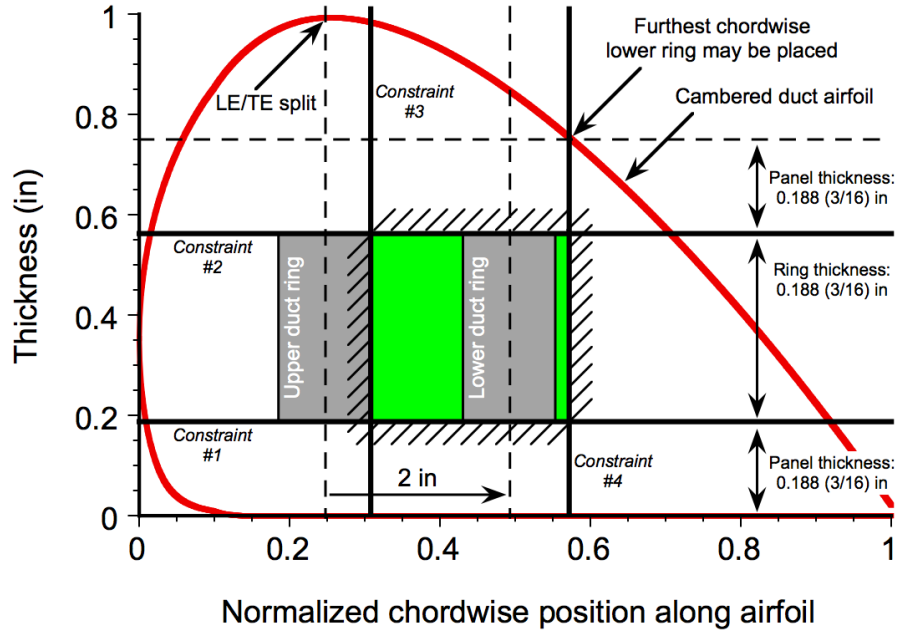


Figure 2.16: Interior of the duct cross-section, showing the internal placement of the lower duct ring.

The lower ring was located 2 inches aft chordwise of the upper ring. Eight dowels rigidly secured the duct rings to each other. Aluminum was used for the duct rings and the dowels so that a lightweight yet robust load-bearing structure was obtained.

The final layout of the duct interior cross-section is shown in Fig. 2.17. Both leading edge panels are shown. Notice that the thickness of the panels on the duct exterior increased to form a solid attachment point for each duct ring.

2.5.1.3 Pressure Tap Specifications

To permit measurement of duct pressure, pressure taps were drawn directly into the CAD model, as shown in Fig. 2.18, in the 22 chordwise locations identified in Table 2.3. Pressure taps around the leading edge from $0.100c_d$ on the inner surface to $0.200c_d$ on the

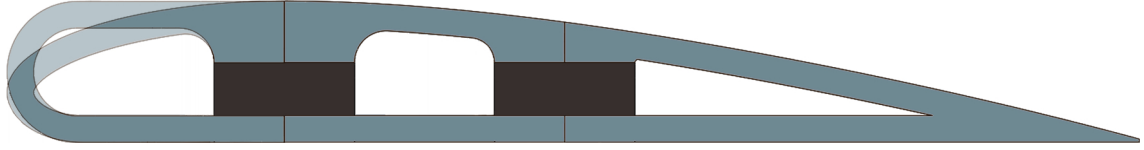


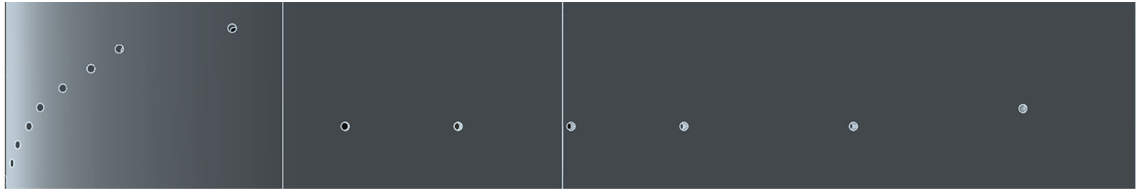
Figure 2.17: Cross-sectional interior of the duct, with both leading edges panels superimposed.

outer surface were consecutively offset by 1° . Staggering the pressure taps in this manner was an attempt to minimize the interference effects of disturbing the boundary layer over one pressure tap on the remainder immediately downstream.

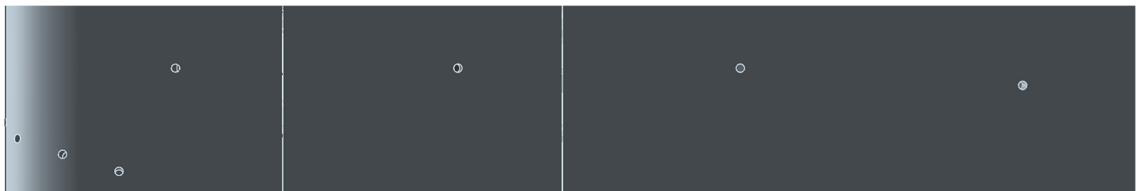
The chordwise distribution of pressure taps (Fig. 2.18(a)–(b)) were placed at six azimuthal stations, spaced equally around the duct in 60° increments. In total, 132 taps were available for pressure measurement.

The diameter of the pressure taps at the surface was 0.055 inches. Inside each pressure tap, the diameter was increased to 0.070 inches. This change in diameter formed a flat surface into which a hypodermic needle tube (or similar) could be inserted from the duct interior, thereby completing the pressure tap. Pressure tubing could then be easily connected to the pressure taps of interest.

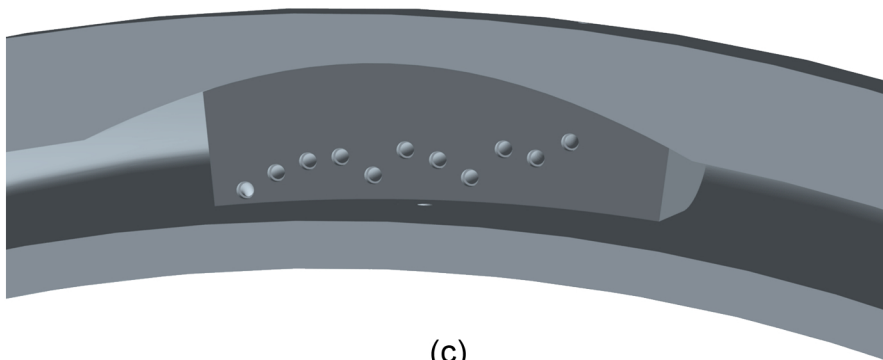
Pressure taps at the leading and trailing edge internally terminated into a common “mounting block” so the pressure taps could be more easily instrumented — see Fig. 2.18(c). The pressure tubing was routed through the duct by means of clearance holes in the duct rings, and exited the duct through the trailing edge panel. Because of the lack of interior space, the two $0.900c_d$ pressure taps on each azimuthal station were offset 1° azimuthally in opposite directions so they would not internally intersect.



(a)



(b)



(c)

Figure 2.18: Implementation of pressure measurement: Contours of the (a) outer surface and (b) inner surface of the cambered duct. (c) The mounting block on the interior of the leading edge panel, to which pressure tubing could be affixed.

2.5.2 Duct Base Design

The duct base held the duct assembly in place, and its primary components are identified in Fig. 2.15. All parts of the duct base were manufactured from aluminum to reduce weight. The base partly consisted of two large base rings through which the rotor sting assembly passed without physical contact to avoid spoiling the load path. Connecting the two base rings were the base brackets, which held each adjoining streamlined strut by a rectangular press fit and two shoulder bolts. The streamlined struts ran perpendicular to the rotor sting assembly within the downstream rotor wake. The strut cross-section was a symmetric airfoil, so the drag force from the rotor wake impinging on the struts was reduced. The strut cross-section had a 2.653 inch chord and a 0.875 inch thickness, so this symmetric airfoil had a nondimensional thickness of $0.33 t/c$.

Attached to the streamlined struts were the strut clamps, which were a rectangular pair of components that held the duct support rods. Each strut interfaced with a rectangular slot on the master clamp; it was secured with a press fit and two shoulder bolts. The smaller clamp part was joined to the master clamp with four screws. When connected, their inner surfaces formed the circular surface that secured the support rods, setting the position of the duct relative to the rotors. This clamping surface, which was the nominal outer diameter of the support rods, was slightly undersized to provide a tight fit.

2.5.2.1 Mounting Position of Duct Assembly

To obtain separate measurements of the rotor and duct, the load path through the balance was manipulated by mounting the duct either before the balance (metric side) or

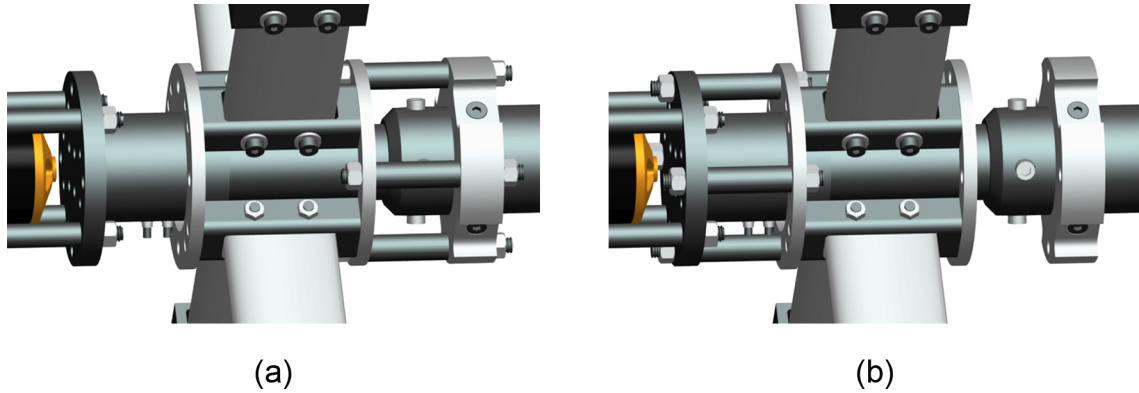


Figure 2.19: Mounting positions for the duct assembly causing the balance to sense (a) rotor-only loads or (b) total (rotor and duct) loads.

after the balance (non-metric side) to measure either the total (rotor and duct) loads or the rotor-only loads, respectively. Figure 2.19 shows this concept. In theory, the duct-only loads may be extracted from the total and rotor loads measurements. To minimize differences between mounting configurations, the rods used to mount the duct to the assembly were of the same length in each case.

For measurement of rotor loads, the mounting location of the duct base was the stinging base collar, which was on the non-metric side of the balance. This part was always attached to the assembly, regardless of mounting configuration. For total loads measurement, the duct base was mounted to the motor baseplate.

2.5.3 Duct Support Rods

A $0.01R$ (0.07 inches) tip clearance specification necessitated a sound method of rigidly securing the duct to avoid blade strikes, yet was lightweight enough to avoid an unduly extraneous bending moment on the balance. The design for attaching the duct to

the duct base comprised of four support rods that held the duct from the lower structural ring. Each support rod had a male thread at the tip that fastened to the lower ring.

2.5.3.1 Rotor Position Within the Duct

The position of the duct relative to the rotors could be set by sliding the duct support rods through the strut clamps to the desired position. In this way, the position of the rotors within the duct could be set to any location as long as the support rods had sufficient length. A rod length of 17.5 inches allowed the lower rotor to be set $0.1c_d$ from the duct trailing edge. Shortening the length of the rods by clamping them farther up moved the duct closer to the stand, effectively moving the rotors forward and closer to the duct inlet. The closest distance from the upper rotor to the duct inlet was also selected as $0.1c_d$.

2.5.3.2 Euler Beam Bending Model

To size the cross-section of the duct support rods so the $0.01R$ tip clearance requirement could be met, a Euler beam bending model was developed to predict the displacement of the duct. The duct support rods were modeled as one cantilever beam. For simplicity, the duct itself was not part of the bending model, but it was accounted for in the loading conditions. The threaded connections between the male threads of the support rods and the lower duct ring affected the normal behavior of a cantilever beam by providing an internal restoring moment at this interface. This restoring moment was assumed to cause a zero beam slope at the tip, thereby limiting the beam displacement as compared to a standard cantilever beam without this condition.

The coordinate system origin for the model was located at the tip of the cantilever beam, with the x -axis in the direction towards the rigid support (strut clamps). The y -axis was oriented vertically, with gravity and weight forces acting in the negative y direction. The direction of the z -axis was determined through coordinate system orthogonality; see Fig. 2.20.

Note that the design point for the bending model was the duct at a horizontal position, which maximizes the bending moment about the z -axis from weight, and perpendicular to the free-stream, which maximizes bending about the y -axis from drag forces. This condition represents the most unfavorable loading condition. The weight and drag loads on the beam in this configuration were decomposed into standard loading classifications whose moment distributions have already been derived [38]. By the principle of superposition, the individual moment distributions were summed. From Euler beam theory, $M = EI\delta''$ so the aggregate moment distribution was integrated twice to obtain the beam deflection δ .

The nominal geometry of the rods was determined from COTS (commercial off-the-shelf) hollow rods to evaluate if they could sufficiently hold the duct without the need for machined components. The hollow rods were aluminum, so $\rho = 0.098 \text{ lb-in}^{-3}$ and $E = 10.2 \times 10^6 \text{ lb-in}^{-2}$. The rod length ℓ was 17.5 inches, with an outer diameter d_o of 0.5 (1/2) inches and an inner diameter d_i of 0.370 inches. This inner diameter size was selected for the support rods to have the capability of internally carrying pressure tubing. Note that, for the hollow rod to connect to the lower duct ring, an adapting part of aluminum was fabricated which was press fitted into one end of each rod; the other end had a male thread which fastened to the lower ring. For simplicity, this geometry (which

was not hollow) was not accounted for in the model.

With the cross-section geometry obtained, the second moment of area I of the beam was computed by superposition and the parallel axis theorem. Consider first the geometry of one hollow rod cross-section. The parallel axis theorem states that the second moment of area I of any cross-sectional geometry is a function of its centroidal second moment of area $\bar{I}$, cross-sectional area A , and distance from the origin to the cross-section centroid d , i.e.,

$$I = \bar{I} + Ad^2$$

Note that this cross-section was symmetric about both the y - and z -axes so $I = I_y = I_z$.

The centroidal second moment of area $\bar{I}$ for the cross-section was

$$\bar{I} = \frac{\pi}{64} (d_o^4 - d_i^4)$$

which was determined from the general expression $\bar{I} = \int_A y^2 dA = \int_A z^2 dA$ [39]. The distance from the origin to the centroid d was $r/\sqrt{2}$, where r is the distance from the center of the duct to the rod centroid in the yz -plane, which was 7.445 inches. Therefore, the second moment of area of the total cross-section was

$$I = \frac{\pi}{16} (d_o^4 - d_i^4) + \frac{\pi}{2} (d_o^2 - d_i^2) r^2 \quad (2.17)$$

which, by superposition, accounted for all four hollow rods by incorporating a factor of four. Equation (2.17) evaluated to 9.856 in^4 . Notice that this second moment of area was far greater than the second moment of area for the four centroidal cross-sections (0.009 in^4) because of the relatively large offset of the elastic axis from the centroid of each rod cross-section.

2.5.3.3 Beam Loading from Duct Assembly Weights

The beam bending about the z -axis from the weights of the duct and the support rods was decomposed into the following loads:

1. The weight of the duct, W_d , which was estimated from the weight of the constituent parts using known densities (aluminum and Accura 50) and estimating volumes from the respective CAD models. The duct panels contributed 6.4 lb; the symmetric duct inlet was used in this calculation instead of the cambered inlet because of its slightly greater mass. The duct rings added 3.3 lb for a total weight of 9.7 lb. To account for the weight of miscellaneous parts, such as screws, washers, nuts, and pressure tubing, the duct weight was then conservatively estimated as $W_d = 12$ lb.

Therefore,

$$M = -W_d x$$

2. The moment caused by the offset of the duct center of mass (and thus weight) from the tip of the beam by a distance L_d of 0.972 inches, as determined from CAD analysis. This was a fixed quantity equal to

$$M = -W_d L_d$$

3. The distributed loading of the weight of the support rods w_r , driven by the density of aluminum and the nominal rod geometry, simplified to

$$w_r = -\frac{W_r}{\ell} = \frac{\pi}{4} \rho_{Al} (d_o^2 - d_i^2)$$

and the moment associated with this distributed load was

$$M = -\frac{1}{2}w_r x^2$$

4. The restoring moment M_{RW} induced by the interface between the support rods and the lower duct ring, which caused a zero slope at the tip of the beam.

In total, the moment equation for the bending about the z -axis from the weight of the duct and rods was

$$M_W = \sum M = -\frac{1}{2}w_r x^2 - W_d x - W_d L_d + M_{RW} \quad (2.18)$$

with the following boundary conditions:

$$\delta_W(\ell) = 0 \quad \text{Cantilever beam}$$

$$\theta_W(\ell) = 0 \quad \text{Cantilever beam}$$

$$\theta_W(0) = 0 \quad \text{Restoring moment}$$

It followed that

$$\theta_W = \frac{1}{EI} \left(-\frac{1}{6}w_r x^3 - \frac{1}{2}W_d x^2 - W_d L_d x + M_{RW} x \right) \quad (2.19)$$

noting that the integration constant was zero because of the $\theta_W(0) = 0$ restoring moment condition. At this point, the restoring moment was explicitly solved from the cantilever beam condition $\theta_W(\ell) = 0$, i.e.,

$$M_{RW} = \frac{1}{6}w_r \ell^2 + \frac{1}{2}W_d \ell + W_d L_d$$

Integration yielded the displacement of the beam, i.e.,

$$\delta_W = \frac{1}{EI} \left(-\frac{1}{24}w_r x^4 - \frac{1}{6}W_d x^3 - \frac{1}{2}W_d L_d x^2 + \frac{1}{2}M_{RW} x^2 + C_W \right) \quad (2.20)$$

where the integration constant C_W was determined from the cantilever beam condition

$$\delta(\ell) = 0:$$

$$C_W = \frac{1}{24}w_r\ell^4 + \frac{1}{6}W_d\ell^3 + \frac{1}{2}W_dL_d\ell^2 - \frac{1}{2}M_{RW}\ell^2$$

2.5.3.4 Beam Loading from Duct Assembly Drag

The drag on the duct and support rods, which contributed to bending about the y-axis, was decomposed into the following loads:

1. The drag on the duct, D_d , which was determined from the standard drag equation, $D = \frac{1}{2}\rho V_\infty^2 C_D A$. When perpendicular to the incoming wind, the duct was assumed to behave like a cylinder (drag coefficient $C_D = 1.2$) with cross-sectional area A based on a diameter of $2.02R + 2t_d$, which was a slightly conservative estimate. A drag force D_d of 5.6 lb was estimated using sea level density ρ and a free-stream velocity V_∞ of 20 ms^{-1} . The moment it contributed to y-axis bending was

$$M = -D_d x$$

2. The moment caused by the duct drag, which was assumed to act at the duct center of mass that was offset from the tip of the beam by a distance L_d , i.e.,

$$M = -D_d L_d$$

3. The distributed loading of the duct along the beam, which was obtained from the standard drag equation with $A = d_o \ell$ and $C_D = 1.2$ as before. Accounting for all four support rods, the distributed drag load was simplified to

$$d_r = -\frac{D_r}{\ell} = -2\rho V_\infty^2 C_D d_o$$

The moment that results from this distributed drag load was

$$M = -\frac{1}{2}d_r x^2$$

4. The restoring moment M_{RD} induced by the lower ring and beam interface, as described previously.

The same integration was repeated for the bending model about the y-axis from the duct drag, i.e.,

$$M_D = \sum M = -\frac{1}{2}d_r x^2 - D_d x - D_d L_d + M_{RD} \quad (2.21)$$

with the same boundary conditions as before

$$\delta_D(\ell) = 0 \quad \text{Cantilever beam}$$

$$\theta_D(\ell) = 0 \quad \text{Cantilever beam}$$

$$\theta_D(0) = 0 \quad \text{Restoring moment}$$

Integrating the moment distribution yielded

$$\theta_D = \frac{1}{EI} \left(-\frac{1}{6}d_r x^3 - \frac{1}{2}D_d x^2 - D_d L_d x + M_{RD} x \right) \quad (2.22)$$

with no constant of integration because of the restoring moment condition $\theta_D(0) = 0$.

From the cantilever beam condition of $\theta_D(\ell) = 0$, the restoring moment from the duct drag was solved as

$$M_{RD} = \frac{1}{6}d_r \ell^2 + \frac{1}{2}D_d \ell + D_d L_d$$

Integrating the slope equation to determine the beam displacement from duct drag resulted in

$$\delta_D = \frac{1}{EI} \left(-\frac{1}{24}d_r x^4 - \frac{1}{6}D_d x^3 - \frac{1}{2}D_d L_d x^2 + \frac{1}{2}M_{RD} x^2 + C_D \right) \quad (2.23)$$

where the constant of integration C_D was found from the condition $\delta_D(\ell) = 0$:

$$C_D = \frac{1}{24}d_r\ell^4 + \frac{1}{6}D_d\ell^3 + \frac{1}{2}D_dL_d\ell^2 - \frac{1}{2}M_{RD}\ell^2 \quad (2.24)$$

2.5.3.5 Summary of Results

The results of the bending model are presented in Fig. 2.20, which shows the displacement of the beam from weight forces (Eq. (2.20)) and drag forces (Eq. (2.23)). The weight forces imposed greater moment along the beam than the drag forces, causing greater displacement. However, the total displacement is of greater importance; it was calculated as

$$\delta_{total} = \sqrt{\delta_w^2 + \delta_D^2} \quad (2.25)$$

Equation (2.25) is also shown in Fig. 2.20. The maximum displacement was significantly less than the nominal tip clearance of 0.07 inches. The nominal geometry for the support rods, therefore, was sufficiently rigid to properly secure the duct under the predicted bending loads.

It should be noted, however, that deficiencies in Euler bending models exist with beams of low length-to-thickness ratio (short beams). These deficiencies occur because the transverse shear strain in beams of this type is sufficiently large and cannot be neglected, as it is in Euler theory. The result is an underprediction of beam displacement. While the cantilever beam bending model in the present work fell under this short beam classification, the predicted displacement was so small that accounting for Euler bending model discrepancies was unnecessary.

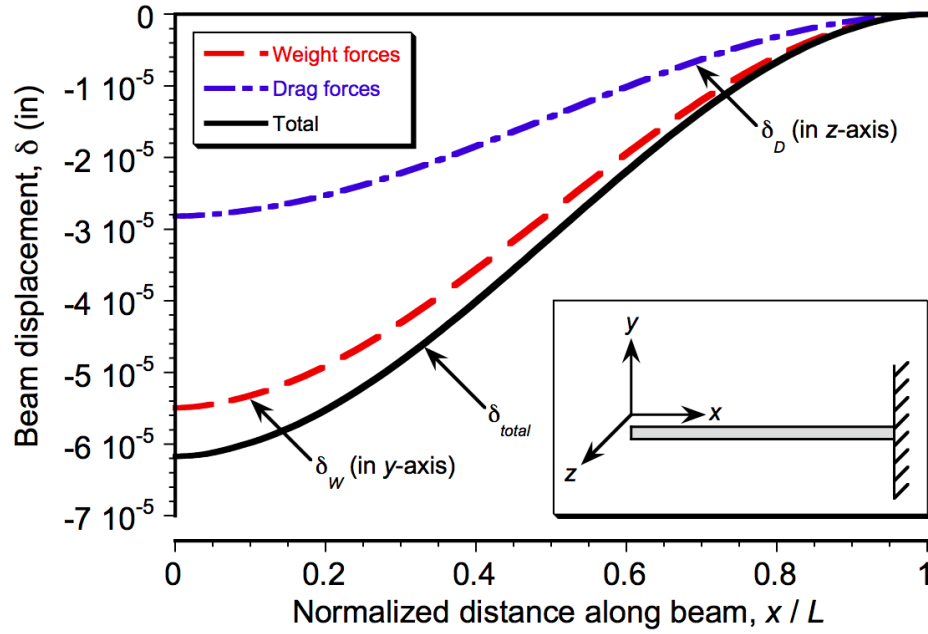


Figure 2.20: Displacement of the duct support beam bending model.

2.6 Summary

The aerodynamic and structural design of the ducted coaxial rotor that was used for the experiments has been presented in this chapter. The coaxial rotor was developed using an existing rotor design and modified for the present application. The duct was designed to have alternative inlet shapes (either cambered and symmetric). Structural elements, including a test stand and the internal components of the duct, were also designed to conduct performance experiments. The rig was designed to test different configurations though variations in rotor-to-rotor spacing, duct shape, rotor position relative to the duct, and tip clearance so that the influence of these design parameters on hover and forward flight performance may be determined.

Chapter 3

Description of the Experiments

3.1 Introduction

The following chapter describes the setup and testing procedures of the experiments conducted in the present work. Catalogs of all tests completed in the experiments are also provided in Appendices A, B, and C.

The primary objectives of the experiments were two-fold: (1) To investigate the performance capability of a ducted coaxial rotor at micro aerial vehicle (MAV) scale, and (2) To acquire a body of experimental work so that predictive methods used to design more efficient MAVs may be validated. The experiments comprised two phases: (1) Rotor system performance testing, and (2) Investigation of the isolated duct. The first phase determined the effect of various design parameters, including rotor-to-rotor spacing, rotor/duct position, duct shape, and tip clearance, on ducted coaxial rotor performance in hover and forward flight. To support this work, the performance of other MAV configuration types, including isolated single rotor, isolated coaxial rotor, and ducted single rotor, was also explored. The second phase aimed to provide a quantitative and qualitative understanding of the duct used in rotor system performance testing by measuring the isolated duct loads in forward flight and conducting duct surface flow visualization.

3.2 Operation of the Rotors

The test article was driven using an AXi Double 5330/20 three-phase alternating current (AC) brushless motor, which comprised two connected contra-rotating motors. The power to the motors was supplied by a Sorensen DCS40-75E power supply, which provided a constant direct current (DC) voltage of 41.6 Volts to an electric speed controller (ESC) hardware unit (one per motor). The power supply had a readout to monitor the amount of current supplied to the motors. The ESC converted the DC voltage from the power supply to AC voltage, which was then supplied to the corresponding motor.

The amount of converted power (throttle setting) was determined by pulse width modulation (PWM) of a square wave signal sent to the ESC, where a 1 ms high time (pulse width) was 0% throttle and a 2 ms pulse width was 100% throttle. An in-house motor speed control circuit was built to manually control the motors when necessary (e.g., troubleshooting) that used a potentiometer to vary the width of the square wave and, therefore, the desired motor throttle setting. In performance testing, the motor speed was controlled by software that was developed in-house. Heat sinks were installed on the ESCs to dissipate the significant heat they produced.

Measurement of rotor rotational speed was obtained using an optical tachometer system. A reflective object sensor, which is the combination of an infrared (IR) light emitting diode (LED) and a phototransistor, was mounted in close proximity to each motor. A white mark was placed on each motor, and as the mark passed the sensor it reflected relatively more IR light back to the phototransistor, changing the output voltage signal of the sensor. This signal was sent to a voltage comparator circuit that compared the sen-

sor output voltage to a pre-determined reference voltage so the output of the comparator would be high (nominally 5 Volts) when the white mark was in front of the sensor, and low (< 1 Volts) when it was not. This reference voltage was manually set by a potentiometer by means of calibration of the tachometer system. The output signal from the voltage comparator was a TTL (transistor-transistor logic) compatible square wave, so this once-per-revolution signal could be read by either an oscilloscope or data acquisition software.

ITT Cannon CB-series wire connectors were used to splice the wiring of the major electrical components. These military-grade connectors were ruggedized and featured a reverse bayonet interface and crimped pin and socket contacts, ruling out the possibility of broken electrical connections during testing.

The motors produced significant heat during operation that partially transferred to the test rig and the load cell balance, which had a thermal sensitivity. A thermocouple was installed on the outer casing of the balance so the temperature may be monitored to ensure it stayed within normal testing limits. A thermocouple was also installed on the motor casing to monitor the motor temperature. A third thermocouple was used to monitor the ambient temperature. The thermocouples were connected to an in-house thermocouple calibration unit (Type J calibration) that had readouts that displayed the temperature of each thermocouple.

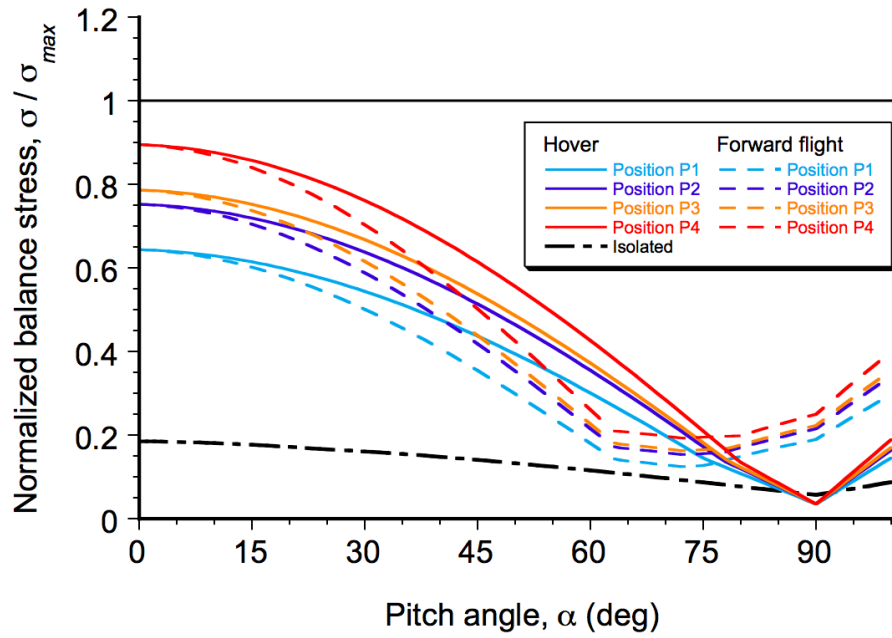
3.3 Specifications of Loads Measurement

All performance measurements were obtained from the balance, a FUTEK bi-axial load cell (model MBA500), which measured thrust and torque of the test article. The load cell was rated in thrust and torque to 500 lb and 500 in-lb, respectively. This capacity was much larger than the expected maximum loads because of the requirement to fully cantilever the duct assembly in forward flight testing, necessitating a stronger and more robust balance. As shown in Fig. 3.1, a prediction of balance stress based on the expected loads of the experiment suggests that the balance was indeed strong enough for a full sweep in pitch angle. However, a full sweep in yaw angle was not possible, as the maximum balance stress rating could be exceeded in certain configurations. For this reason, performance testing was only conducted at a 0° yaw angle.

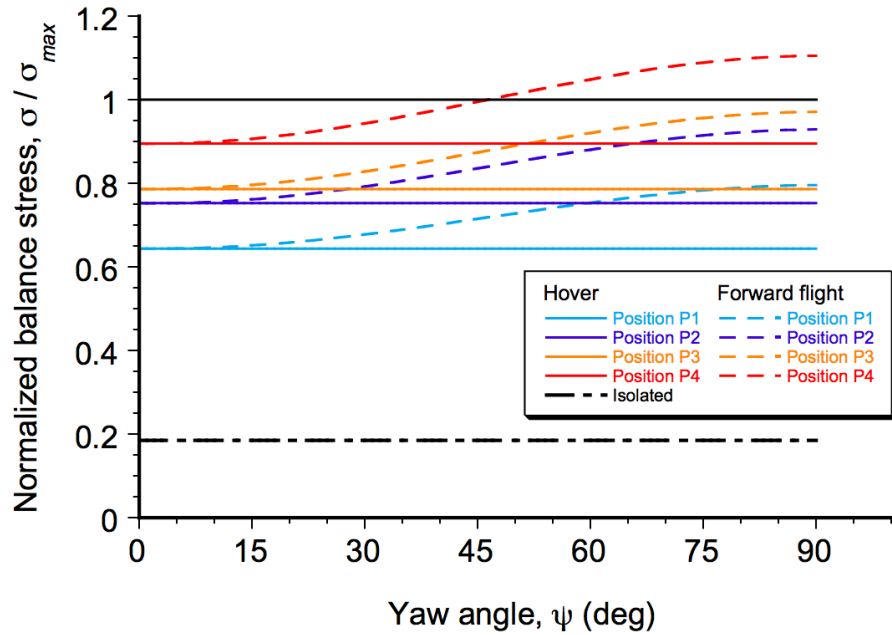
The load cell signal output was significantly conditioned so that good measurement resolution was obtained for the range of expected loads. It should be noted that the torque measured by the balance when testing a coaxial rotor configuration was the net torque of the rotors because they were contra-rotating.

3.3.1 Signal Conditioning

For the hover performance measurements obtained in the laboratory, the load cell was excited at 10.0 Volts, and the balance output signals of thrust and torque were amplified 5,000 times by an amplifier and filtered with a low pass resistor-capacitor (RC) filter with a cutoff frequency of 0.1 Hz. For the forward flight measurements obtained in the University of Maryland Glenn L. Martin Wind Tunnel (GLMWT), the balance signals



(a) Pitch angle sweep at 0° yaw angle.



(b) Yaw angle sweep at 0° pitch angle.

Figure 3.1: Prediction of balance stress based on the test article configuration, flight phase, and angular state.

were conditioned by a Measurements Group Model 2401 hardware unit that provided an excitation of 15.75 Volts, 3,000 times amplification, and a 1 Hz low pass filter. After being conditioned, the balance output signals were read by a Measurement Computing 16-bit analog-to-digital (A/D) data acquisition (DAQ) board. When operating in ± 10 Volts mode, the board resolved the balance signal down to 0.0003 Volts. The voltage read by this data acquisition system (i.e., after signal conditioning) was calibrated against known loads to obtain calibration constants of 5.6774 lb-V^{-1} for thrust and $5.9807 \text{ (in-lb)-V}^{-1}$ for torque. Therefore, the sensitivity of the data acquisition system in engineering units was 0.0017 lb and 0.0018 in-lb.

3.3.2 Testing Software

Testing software written in C++ was developed that simultaneously provided motor speed control and data acquisition. The software read in four signals from the DAQ board: upper rotor speed, lower rotor speed, thrust, and torque. The upper and lower rotor speed signals were provided to the DAQ board by the rotor optical tachometer system.

3.3.2.1 Motor Speed Controller

The software controlled the operating state of a rotor by allowing the user to specify the desired speed of the upper rotor, lower rotor, or both rotors. Incremental control was also available to change the speed of the rotor(s) in increments of 10 rpm. A proportional control system was implemented to command the throttle setting of a motor based on the measured speed (sensed by the optical tachometer) and the user-determined desired

speed, i.e.,

$$\text{Throttle}_f = \text{Throttle}_i + K (\Omega_{desired} - \Omega_{measured})$$

where the units of throttle setting and rotor speed Ω are unity and Hz, respectively. The value of the controller gain was determined through control system calibration to be $K = 2.5 \times 10^{-4} \text{ Hz}^{-1}$. The new throttle setting was converted into a PWM signal by a Measurement Computing USB-4301 counter/timer device. This signal was sent to the ESC of the appropriate motor, which in turn adjusted the AC voltage output to the motor, changing its speed based on an internal control scheme. This closed-loop speed control system generally allowed the rotor speed to be tuned to within $\pm 1\%$ while operating essentially in real-time.

3.3.2.2 Data Acquisition

The data acquisition functionality of the testing software continually sampled the 4 channels of the DAQ board at 20 kHz. Therefore, each channel was effectively sampled at a rate of 5 kHz. When prompted by the user, a data point for each channel was computed from a time average of 6,250 samples collected over 1.25 s.

3.3.3 Determination of Power

The disadvantage of measuring the net torque of a coaxial rotor is that total rotor power cannot be calculated directly from net torque measurements. To directly calculate the power of each rotor in a coaxial system, independent measurement of the upper and lower rotor torque would instead be needed. However, because the electric power con-

sumed by the motors was proportional to the required aerodynamic power of the rotors, a relationship between these two types of power was established. Electric power was calculated using Ohm's law from voltage and current, $P_{elec} = IV$, which were recorded for each test point. The voltage supplied to the motors was constant at 41.6 Volts. The relationship between powers was calibrated against the isolated single rotor tests, for which both aerodynamic power and electrical power were known; see Fig. 3.2. A linear best-fit curve (in a least-squares sense) of $P_{aero} = 0.5744P_{elec} - 0.0266$ (defined in units of horsepower) was established, with good agreement between the isolated upper rotor and isolated lower rotor tests. The coefficient of determination R^2 of the best-fit curve was 0.9824, indicating good confidence in predicting the aerodynamic power from the electrical power. The coefficients of the best-fit curve suggest that the transfer from electric to aerodynamic power was approximately 57% efficient, and that approximately 0.83 A was required to overcome losses in the system.

3.4 Experimental Setup and Testing Procedures

Performance testing of the ducted coaxial rotor, and other configurations, was conducted in a laboratory and the Glenn L. Martin Wind Tunnel (GLMWT). The testing procedure was generally consistent between locations. The setup of the test article in the wind tunnel, shown in the ducted coaxial rotor configuration, is shown in Fig. 3.3.

Prior to any performance testing, motor speed sweeps were conducted without the rotors to establish the motor tares. A best-fit curve with motor speed was calculated for each of the three rotor configurations: upper rotor (lower motor), lower rotor (upper mo-

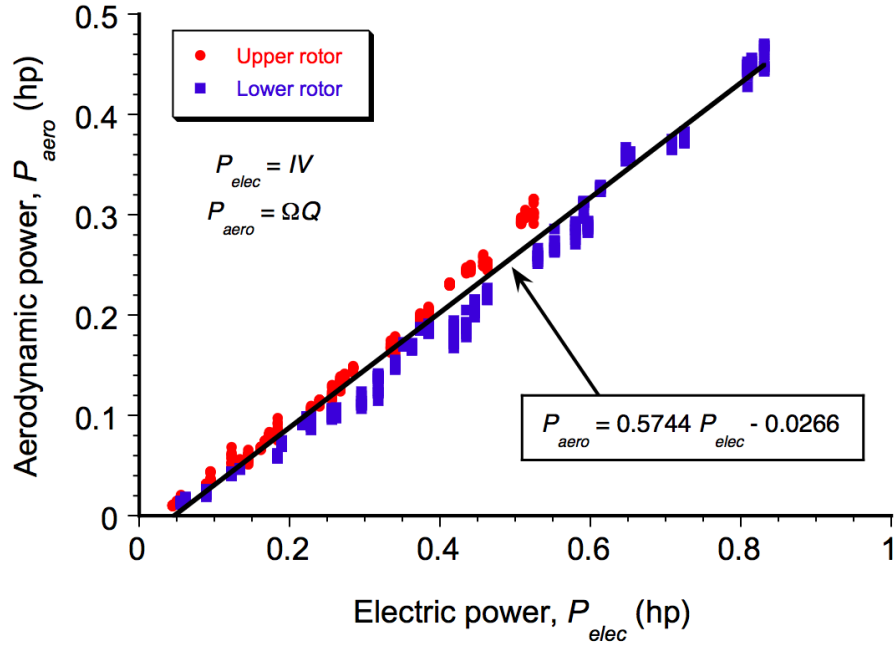


Figure 3.2: Correlation between electric power to the motors and aerodynamic power of the test article, with linear best-fit curve shown.

tor) and the coaxial rotor (both motors). Based on which motor was operating, the testing software used the appropriate best-fit curve and speed of the motor(s) to automatically remove the motor tare. These motor tare values were stored in the raw data files so that they may be recovered later. It should be noted that the motor tares were accounted for in the relationship between electric and aerodynamic power.

Before a test run was conducted, the voltage from the balance was zeroed to remove the gravity tare from the measurements. For forward flight tests, the desired wind speed was then commanded. Each test run consisted of a rotor speed sweep, bringing up the rotor(s) up from idle to nominally 5,000 rpm. The sweep continued upward to 6,000, 7,000, and 7,500 rpm. The sweep decreased from 7,500 rpm to 7,000, 6,000, 5,000, 4,000, 3,000, and 2,750 rpm. At each rotor speed along this sweep, 10 data points were



Figure 3.3: Experimental setup of the ducted coaxial rotor, shown in the wind tunnel.

recorded (each of which was the average of 6,250 samples taken at 5,000 Hz). In total, for the rotor speeds that were tested twice (5,000, 6,000, 7,000, and 7,500 rpm), 125,000 samples were collected; 62,500 samples were collected for the rotor speeds below 5,000 rpm. Voltages were also recorded after the wind tunnel was brought to idle. The voltages before the initial zero and after the test (when the rotors were idle) were also logged and stored in the raw data files.

The density during performance testing was also calculated and logged based on laboratory and wind tunnel conditions. The day-to-day variation in density was very small and had no impact on the conclusions of the present work. The performance measurements in the processed data files were normalized to International Standard Atmosphere (ISA) conditions at sea level.

3.4.1 Test Parameters

Many unique configurations of the test article were possible given the available values of the identified test parameters: configuration of the rotor(s) (including varying tip clearance), duct shape, rotor/duct position, and loads measurement type. The flight condition was set by the wind speed and relative wind angle, which was determined by the pitch angle. In this way, the rotor system could be operated in hover or in forward flight at a variety of flight conditions; axial climb and edgewise flight were the primary focus of the forward flight testing. Figure 3.4 summarizes these test parameters. Not all possible configurations and flight conditions were tested because of practical limitations of testing such a system with many degrees of freedom. Detailed summaries of the test

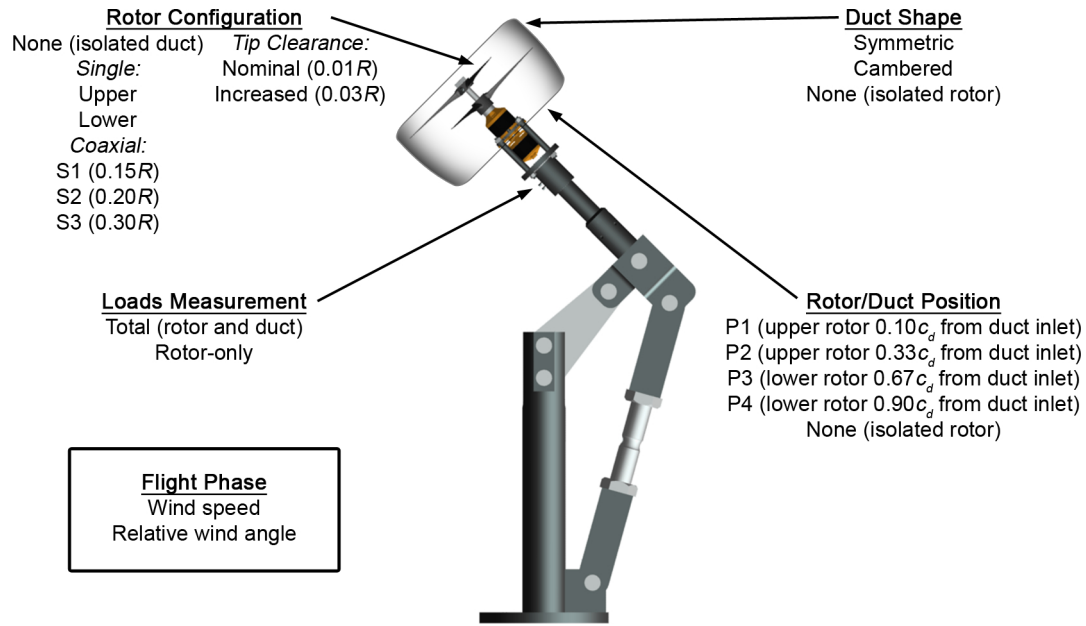


Figure 3.4: The test parameters and flight conditions of the experiments and their possible settings.

parameters for all of the tests are catalogued in Appendices A, B, and C.

3.4.2 Hover Performance Testing

Preliminary performance testing was conducted in a laboratory setting and consisted entirely of hover performance tests. These tests took place within the partial confines of a honeycomb test cell, which helped control the quality of the flow environment. The test article was always in the upright, (90°) orientation, so the rotors operated approximately 3.75 rotor diameters from the ground (see Section 2.3.3) and, therefore, out of direct ground effect. However, evidence of some flow recirculation and turbulence was observed by the somewhat higher fluctuations of the measured loads compared to those ultimately found in the wind tunnel. The laboratory tests were conducted from July 16 to

July 20, 2009. A list of all of the laboratory tests is presented in Appendix A.

3.4.3 Forward Flight Performance Testing

For some forward flight tests in the GLMWT, a static test was also conducted by operating the model while the wind tunnel was idle. For these hover cases in the wind tunnel, the model was mounted at 0° pitch angle, and the wake from the model produced a small flow through the wind tunnel, causing the model to operate in a shallow climb.

The angle of the relative wind to the test article was determined by the pitch angle. For the ducted coaxial rotor configurations, a wide sweep of pitch angles was investigated: 0° (axial climb or “propeller mode”), 15° , 60° , 75° , 90° (edgewise flight), and 100° . The ducted single rotor configurations were only tested at the two most important orientations of 0° and 90° . The isolated rotors were only tested at 0° (propeller mode) to protect the blades from unsteady aerodynamic loading in edgewise flow, which was less of an issue when the rotors were ducted. While the design of the MAV and test stand included the possibility of yaw angle, the rated capacity of the load cell balance would be exceeded beyond small yaw angles (see Fig. 3.1(b)). For the present experiments, therefore, only the pitch angle determined the angle to the relative wind, and the yaw angle was fixed at 0° .

At each pitch angle, a wind speed sweep was performed of 5, 10, 15, and 20 ms^{-1} . Static runs (0 ms^{-1}) were conducted for all propeller mode (0° pitch angle) tests. For some ducted configurations in edgewise flight, the maximum wind speed was limited by vibrations on the stand, so 20 ms^{-1} tests were mostly, but not always, conducted.

The nominal rotor speed sweep also depended on the testing conditions. For example, during axial climb testing, negative thrust was generally avoided so as not to over-stress the rotors, and this constraint prevented operation at lower rotor speeds. In edgewise flight testing, the rotor speed was limited above 5,000 rpm to keep the rotor blades from encountering excessive bending loads.

A few ducted coaxial rotor tests were also conducted at a torque-balanced rotor sweep. The upper and lower rotor speeds were differentially changed until a torque balance was achieved at (or near) the same average rotor speed as in the nominal rotor speed sweep. At this point, a sweep was performed by increasing the speed of both rotors by 1,000 rpm and then differentially trimming to again obtain a torque balance. The maximum rotor speed imposed on testing was 7,500 rpm, so a torque-balanced rotor speed sweep had fewer data points than the nominal rotor speed sweep.

Several cross-checks of the measured loads were conducted between the load cell and the GLMWT balance, though it lacked the sensitivity to resolve the relatively small forces being sensed by the load cell. Wind tunnel tests were conducted from July 22 to August 4, 2009 and are documented in Appendix B.

3.4.4 Isolated Duct Loads Testing

To supplement the ducted single and ducted coaxial rotor performance measurements, the loads on the isolated duct were also measured. For these tests, the balance configuration was set to make total loads measurement, so the duct load path passed through the load cell (Section 2.5.2.1). The rotors and coaxial motor were removed from

the assembly, so the balance only sensed the loads on the duct (and structural assembly). The testing procedure consisted of a sweep in wind speed from 0 ms^{-1} to 30 ms^{-1} in 5 ms^{-1} increments, then decreasing to 15 ms^{-1} and then to 0 ms^{-1} . The maximum wind speed was higher for duct loads testing than rotor system performance testing, as the wind speed limit was a rotor-specific constraint. This wind sweep was conducted for the usual range of pitch angles of 0° , 15° , 60° , 75° , 90° , and 100° . This testing procedure was followed for each of the two duct shapes, cambered and symmetric. Testing of the isolated duct was conducted in the wind tunnel on July 27, 2009; see Appendix C for the catalog of isolated duct loads tests.

3.4.5 Flow Visualization on the Duct

To investigate the aerodynamics of the surface of the isolated duct, surface flow visualization was performed for both the cambered and symmetric duct shapes for the usual range of pitch angles (0° , 15° , 60° , 75° , 90° , and 100°) and also at 30° and 45° . This qualitative investigation complemented the quantitative measurement of the isolated duct loads.

The surface flow visualization procedure was to paint a mineral oil and titanium dioxide mixture onto the duct prior to increasing the wind speed to 30 ms^{-1} . This speed was held for several minutes as the mixture collected in localized areas of low surface velocity and, therefore, low surface shear. The mixture was shed from the duct surface in areas with high local surface velocities, i.e., where there was an attached boundary layer. The remaining mixture formed surface flow patterns that provided insight into the

aerodynamic characteristics of the duct.

These tests were conducted in the wind tunnel on August 5, 2009. A list of these tests is presented in Appendix C. It should be noted that duct surface flow visualization was not conducted while the rotors were operating. This was because the motors could not be sufficiently protected while operational, and equipment damage could occur if the oil mixture contacted the powered motors.

3.5 Catalog of Tests

Appendices A, B, and C contain the configuration details of all tests that were conducted in the present experiments. Table 3.1 explains the shorthand notation used in these appendices to present test information in an abbreviated fashion. The hover performance tests completed in the laboratory are catalogued in Appendix A. Tests completed in the wind tunnel, which included both hover and forward flight performance tests, are detailed in Appendix B. Appendix C lists the isolated duct tests, which included duct loads testing and surface flow visualization. In total, over 300 tests were conducted in the experiments of the present work.

Table 3.1: Shorthand notation of test parameters.

Rotor Configuration	U	Upper rotor
	L	Lower rotor
	S1	Coaxial rotor, $0.15R$ rotor-to-rotor spacing
	S2	Coaxial rotor, $0.20R$ rotor-to-rotor spacing
	S3	Coaxial rotor, $0.30R$ rotor-to-rotor spacing
	c	Increased tip clearance rotor(s) (subscript)
Duct Shape	C	Cambered
	S	Symmetric
	n/a	Unducted
Rotor/Duct Position	P1	Upper-most rotor $0.10c_d$ from duct inlet
	P2	Upper-most rotor $0.33c_d$ from duct inlet
	P3	Lower-most rotor $0.67c_d$ from duct inlet
	P4	Lower-most rotor $0.90c_d$ from duct inlet
	n/a	Unducted
Loads Measurement	T	Total loads (rotor and duct)
	R	Rotor-only loads

3.6 Summary

The operation and testing procedures of the experiments conducted in the present work have been explained in this chapter. The experiments consisted of: (1) Performance testing of a ducted coaxial rotor in hover and forward flight with variation in selected test parameters; and (2) Isolated duct testing, which consisted of duct loads testing and surface flow visualization. To assess the performance of a ducted coaxial rotor relative to other platforms, testing of isolated single rotor, isolated coaxial rotor, and ducted single rotor platforms were also conducted. Performance was measured in terms of thrust and torque using a load cell balance. Power was obtained from calibration between electric and aerodynamic power using the measured voltage and current supplied to the coaxial motors. Performance measurements were obtained using testing software, which also controlled rotational speeds of the rotor. The testing procedures have also been detailed, which were mostly consistent between the two testing locations of the laboratory (hover performance) and the Glenn L. Martin Wind Tunnel (hover and forward flight performance). The configuration details of all tests conducted during the course of the present work have also been presented.

Chapter 4

Results and Discussion

4.1 Introduction

This chapter presents the results of the experiments and is presented in two sections. First, performance measurements of the various single rotor, coaxial rotor, ducted single, and ducted coaxial rotor systems were used to determine the influence of the test parameters (in terms of rotor spacing, duct shape, rotor position within the duct, and tip clearance) on performance in hover, axial flight, and edgewise flight. Second, the isolated duct tests, which consisted of loads measurements and surface oil flow visualization, were used to characterize the aerodynamics of the isolated duct in both quantitative and qualitative terms.

4.2 Hover Performance

4.2.1 Introduction

The following section presents the hover performance of the various rotor system models. As previously discussed, hover testing was conducted in a laboratory and wind tunnel setting. The model was tested either as a single or coaxial rotor and operated in isolation or within a duct, with the variations of the test parameters being identified in Chapter 1. Performance was measured in terms of rotor thrust and rotor torque. For

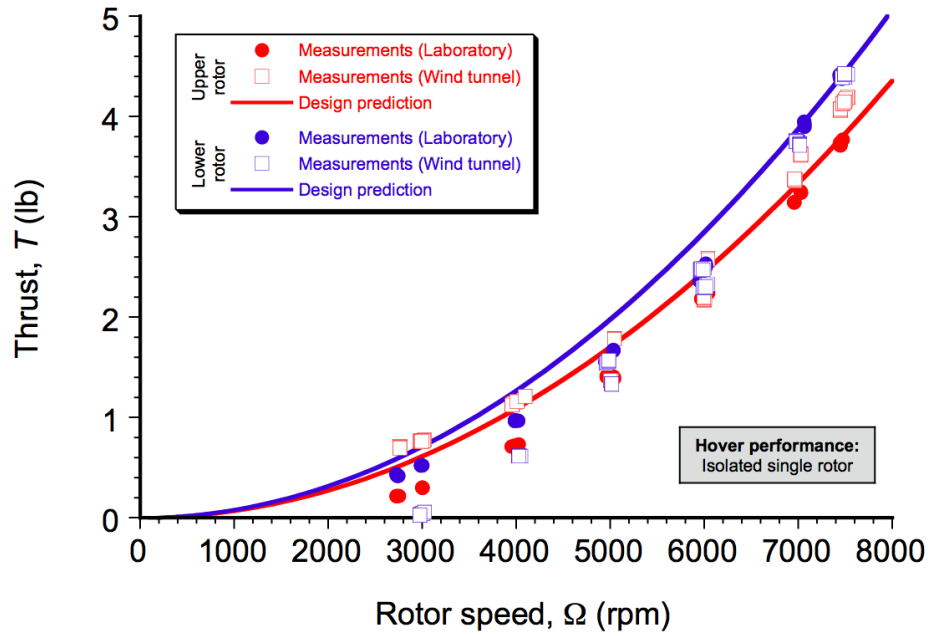
coaxial operation, the thrust of both rotors and the net rotor torque was obtained (contra-rotating rotors with opposing torques). Power was obtained either from direct calculation using the measured rotor speed and torque, as was the case for the isolated single rotors and ducted single rotors in the laboratory setting, or from the correlation between electric power and aerodynamic power as described in Section 3.3.3 (coaxial rotors and ducted single rotors in the wind tunnel). Hover testing was necessary to identify the configurations that most influenced performance so they may be studied further in forward flight.

The performance measurements were normalized to International Standard Atmosphere (ISA) conditions at sea level in the processed data files; the experimental data are presented in this chapter as measured (not normalized). As discussed in Chapter 3, the density throughout performance testing was calculated and recorded. Because the variation in density was very small, the conclusions of the present work are unaffected.

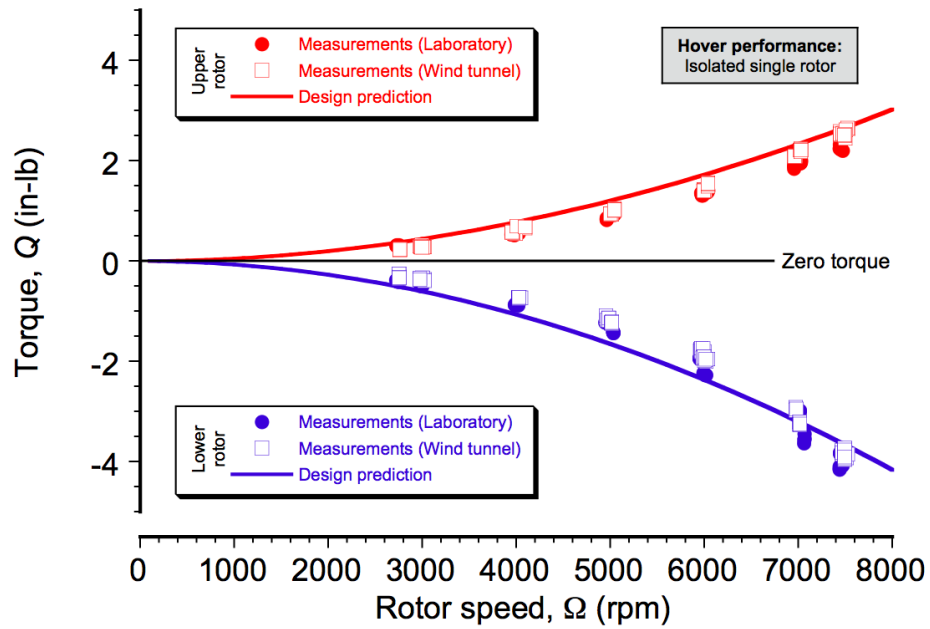
4.2.2 Isolated Single Rotor Performance in Hover

The hover performance of the isolated upper and lower rotors in terms of thrust, torque, and power is presented in Fig. 4.1(a)–(c). Performance measurements obtained in laboratory testing and wind tunnel testing are compared to the performance prediction that was used to design the rotors. Notice the sign difference in the torque measurements shown in Fig. 4.1(b), which was a result of the balance orientation. In these experiments, positive torque was associated with the upper rotor, whereas negative torque was associated with the lower rotor.

In general, the results were consistent with expectations based on the collective

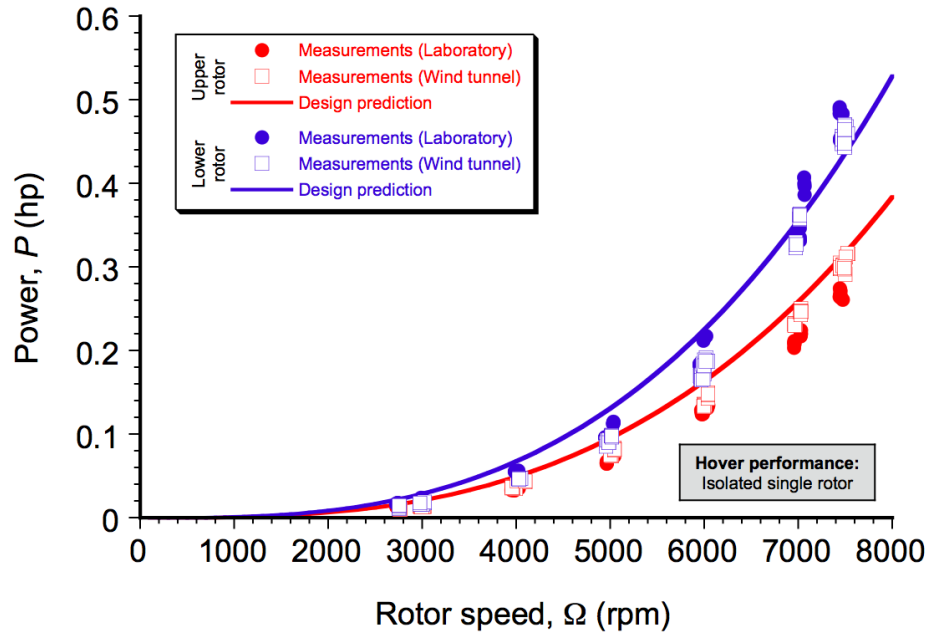


(a) Thrust.



(b) Torque.

Figure 4.1: Hover performance of the isolated upper and lower rotors, comparing loads measurements obtained in the laboratory and wind tunnel against design prediction.



(c) Power.

Figure 4.1: Hover performance of the isolated upper and lower rotors, comparing loads measurements obtained in the laboratory and wind tunnel against design prediction.

(cont)

pitch setting of each rotor. The lower rotor had a 1.11° higher collective pitch setting than the upper rotor (11.92° versus 10.81°), which consequentially was predicted to produce slightly greater thrust at the cost of increased torque and power. This outcome was evident in the laboratory measurements, where the lower rotor loads were always greater than the upper rotor loads. However, in the wind tunnel, the lower rotor only produced more thrust than the upper rotor at higher rotor speeds (above 5,000 rpm). This behavior was possibly a result of stall on the inboard portion of the lower rotor because of greater local angles of attack, a consequence of high collective pitch angles and low rotor speeds.

The measurements of thrust on the upper rotor that were obtained in the laboratory

agreed reasonably well with the design prediction above 5,000 rpm, with the least error at the highest rotor speeds. In the wind tunnel, the measurements for the upper rotor agreed somewhat more with the predictions, with the best agreement being between 5,000 and 6,000 rpm. Measurements of thrust on the upper rotor were higher when the rotor was tested in the wind tunnel instead of the laboratory. For the lower rotor, the measured thrust in both the laboratory and the wind tunnel was notably lower than the predictions, except at the highest rotor speeds where better agreement was evident. It is worth noting that the laboratory and wind tunnel were two different aerodynamic environments. Specifically, for hover testing in the wind tunnel, the rotors systems actually operated in a shallow rate of climb.

The torque measurements of the upper and lower rotor were consistent between the laboratory and wind tunnel data sets, with some small differences occurring at higher rotor speeds. On one hand, the prediction of torque on the upper rotor showed reasonably good agreement to the measurements, with a slight overprediction through the range of rotor speeds. On the other hand, measurements of torque on the lower rotor suggested a greater sensitivity to rotor speed than was predicted, causing a small overprediction at lower rotor speeds and a small underprediction at higher rotor speeds. These trends were also evident in the power measurements.

Figure 4.2 shows the hover performance in power polar form. This presentation of the measurements is preferred because the relationship between thrust and power is more evident, which, for the present experiments, is more critical than the relationship of thrust and power to rotor speed. This presentation also allows for direct comparison of thrust at a constant power, or power at a constant thrust, so that system efficiency may be inferred.

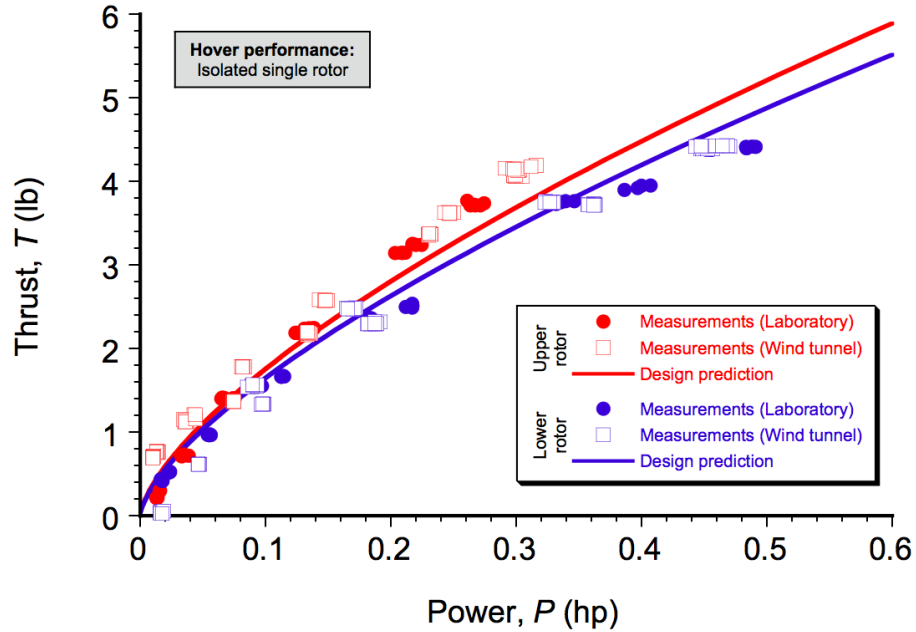


Figure 4.2: Hover performance of the isolated upper and lower rotors, shown in power polar form.

From Fig. 4.2, the lower rotor required more power than the upper rotor to produce the same level of thrust, suggesting that the lower rotor was less efficient.

In general, the measurements indicated that the performance prediction had reasonable accuracy. The differences were likely attributed to accurately predicting the effect of low Reynolds number scale on the rotor aerodynamics. As shown in Fig. 2.3, the test article was operated in the 10^4 – 10^5 Reynolds number range, so any Reynolds number effects will manifest more at the low rotor speeds, especially in thrust because blade element lift is highly sensitive to Reynolds number.

Such Reynolds number effects may also be the source of the measured behavior of the rotor loads with rotor speed. Using simple momentum theory, thrust and torque varies by the square of the rotor speed, and power varies by the cube of the rotor speed.

However, when the previous measurements were shown in their nondimensional form (see Fig. 4.3) significant sensitivity to rotor speed was found. The measurements also showed evidence of aerodynamic hysteresis in some cases, most notably in thrust. This behavior again indicated the influence of low Reynolds number aerodynamics on the performance of rotor systems at this scale.

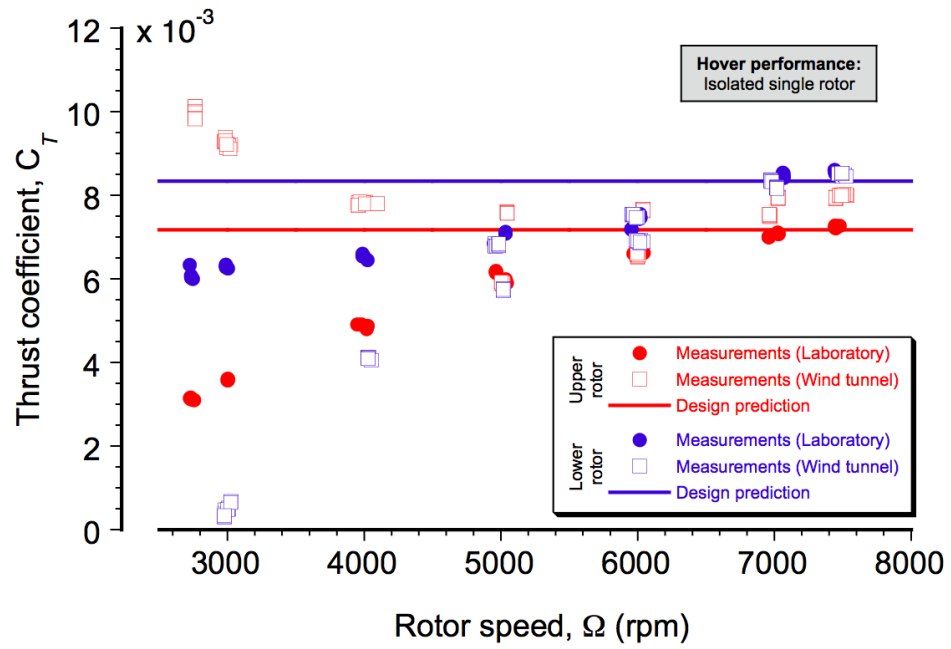
4.2.3 Ducted Single Rotor Performance in Hover

4.2.3.1 Rotor/Duct Position

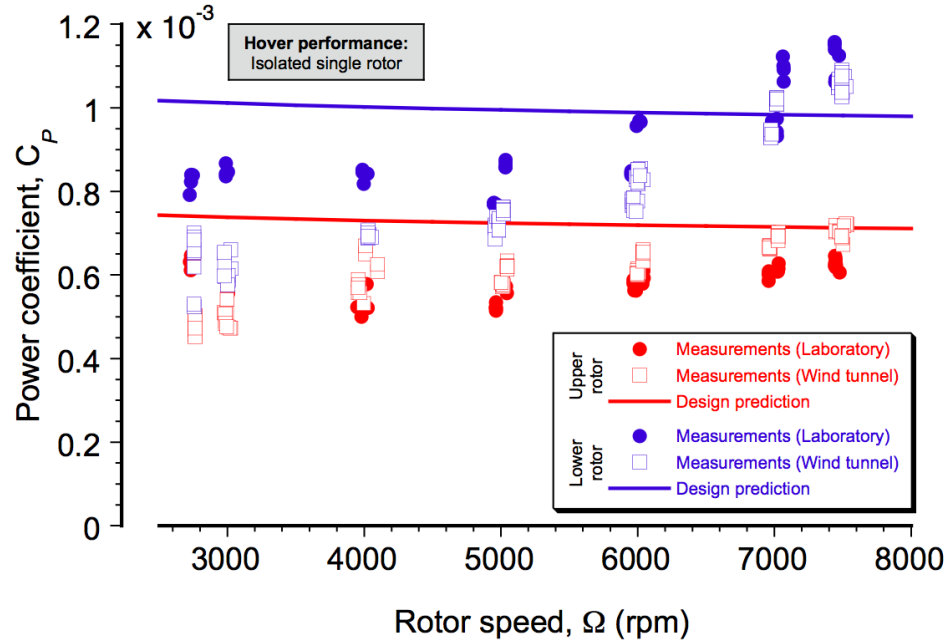
The consequences of operating a single rotor in a duct, which were discussed in Chapter 1, are an increase in system thrust and reduction in system power, causing a net improvement in system efficiency. These effects were observed in the present work in Fig. 4.4(a) for the ducted upper rotor and Fig. 4.4(b) for the ducted lower rotor, which are compared to the cases of the respective single rotor operating in isolation. The position of the rotor within the duct was noted to have an effect on system performance. The cambered duct was used for these measurements.

For the upper rotor, the effect of the duct was shown to cause a slight performance improvement. Some sensitivity to duct position was evident, with the greatest improvement in system improvement occurring when the rotor was closest to the duct inlet at the P1 position. The P2 and P3 positions gave nearly the same performance improvement. The P4 position, however, did not appear to alter the performance of the rotor.

Interestingly, operating the lower rotor in the duct caused a relatively greater performance improvement than for the upper rotor. This behavior was true even for the P4

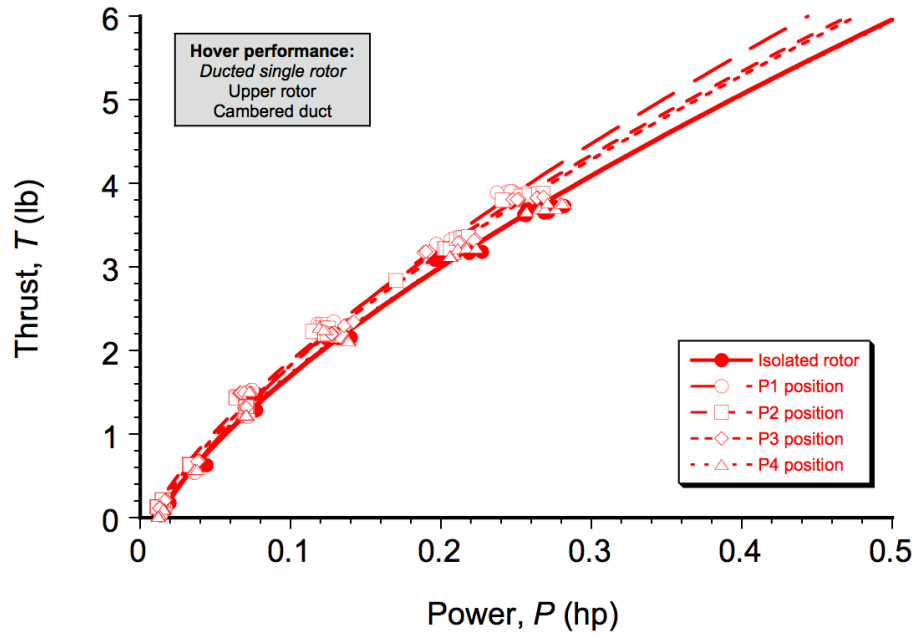


(a) Thrust measurements, presented nondimensionally.

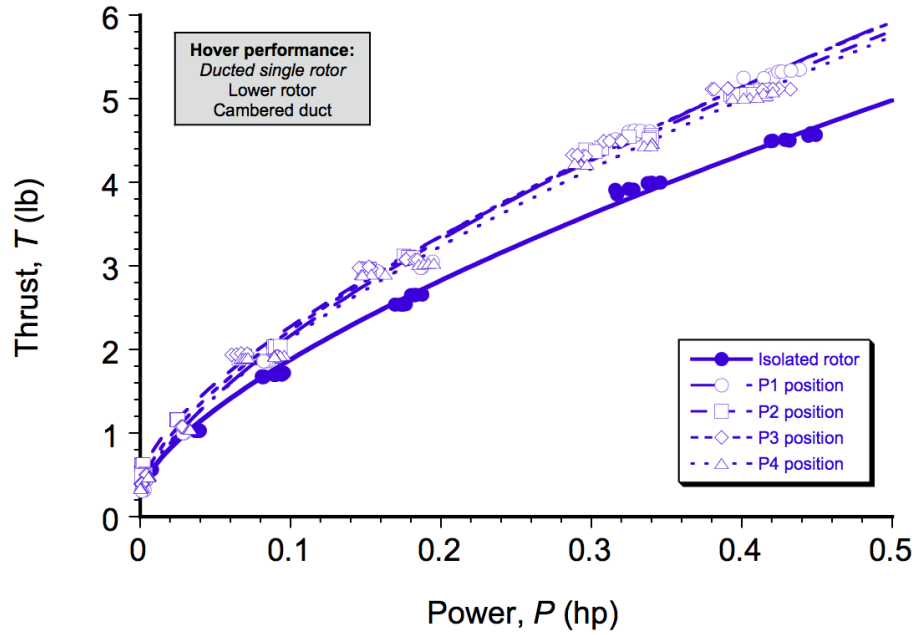


(b) Power measurements, presented nondimensionally.

Figure 4.3: Hover performance of the isolated upper and lower rotors, in nondimensional form: (a) thrust coefficient and (b) power coefficient.



(a) Upper rotor.



(b) Lower rotor.

Figure 4.4: Hover performance of the ducted single rotors with variation in rotor/duct position: (a) upper rotor and (b) lower rotor. Laboratory measurements.

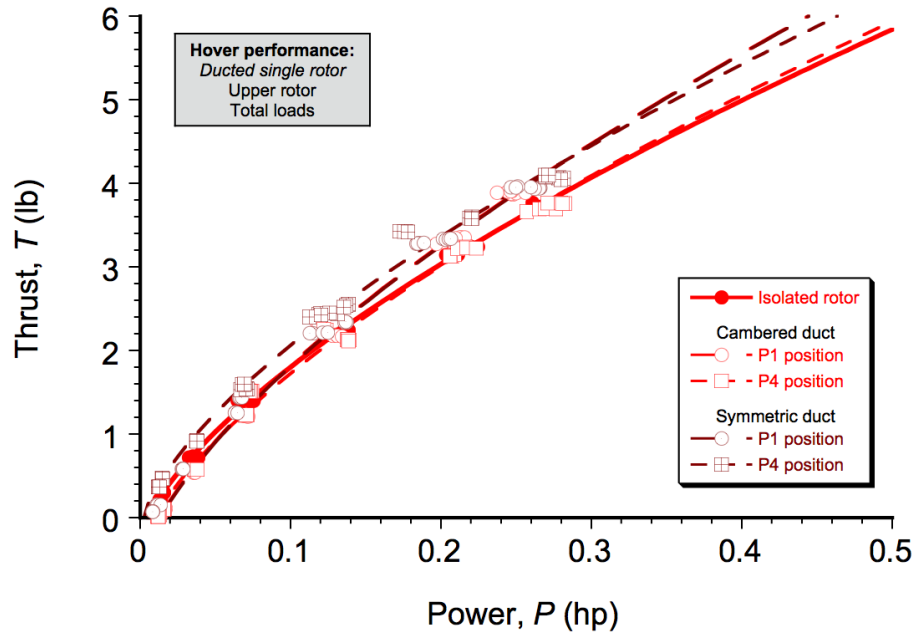
position, which was a position that did not provide significant performance enhancement for the upper rotor. Some sensitivity to duct position was evident (the best position was also P1) though the trend was less obvious than for the upper rotor.

4.2.3.2 Duct Shape

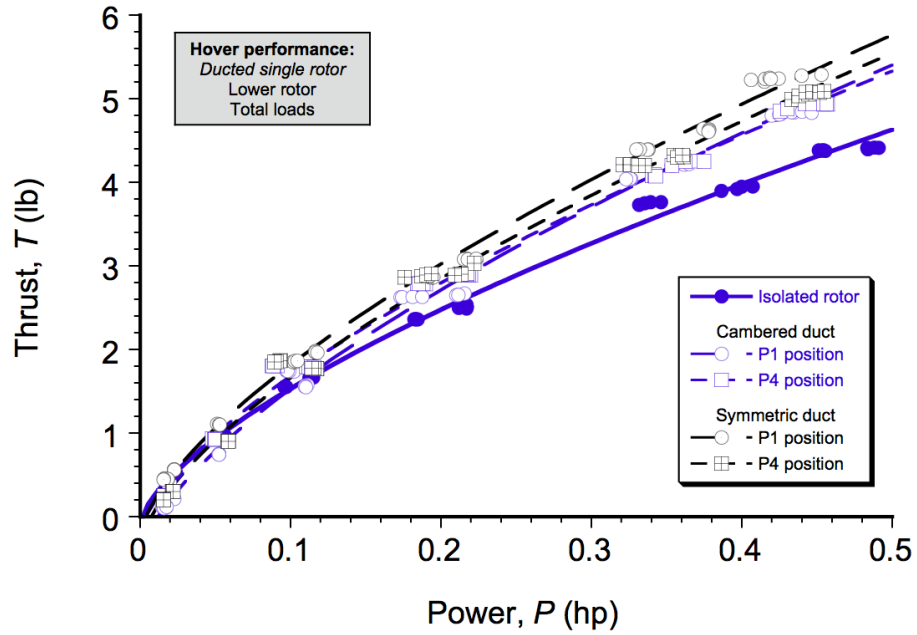
The effect of duct shape is shown in Fig. 4.5. For both the upper and lower rotors, the symmetric duct provided greater performance augmentation than for the cambered duct. The P2 and P3 rotor positions in the symmetric duct were not tested, but the P1 position provided a greater performance enhancement than for the P4 position. As evidenced previously by the cambered duct measurements, better performance was obtained when operating the rotor closer to the duct inlet (noting that this trend for the symmetric duct was based on only two positions).

The gains in efficiency of ducted rotors is also shown using power loading, which is defined as T/P , i.e., the amount of thrust produced per unit power consumed. The power loading of the upper and lower rotors is shown in Fig. 4.6. Clearly, for both rotors the effect of the duct caused an increase in power loading, which was on average 20% better for the upper rotor and 30% better for the lower rotor. The symmetric duct gave greater power loading improvements than for the cambered duct, and for the same duct shape the P1 position was slightly more efficient (higher power loading). Also notice that the isolated upper rotor was about 30% more efficient (in terms of power loading) than the isolated lower rotor.

Another way to compare the effects of both the rotor position and duct shape is



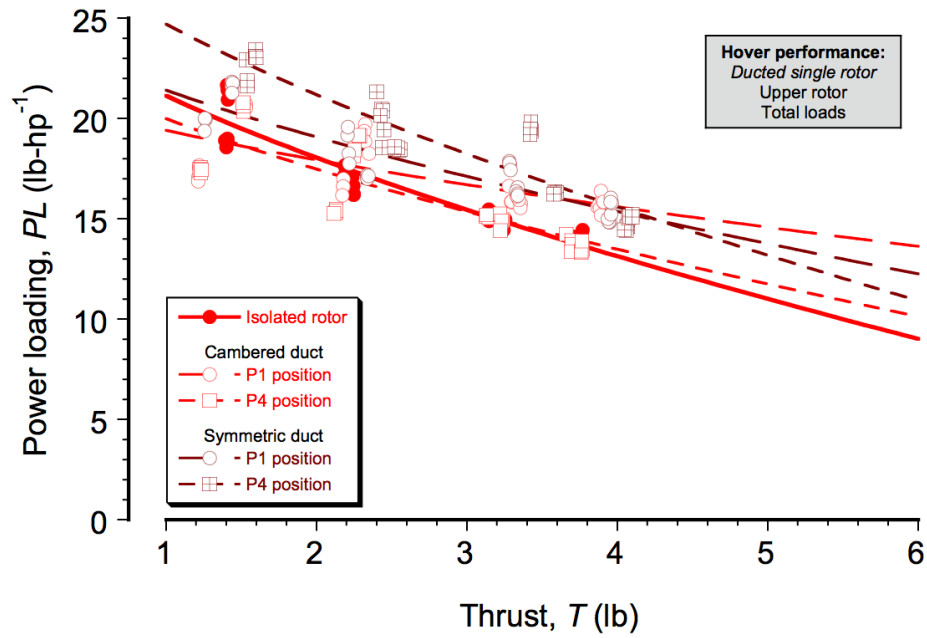
(a) Upper rotor.



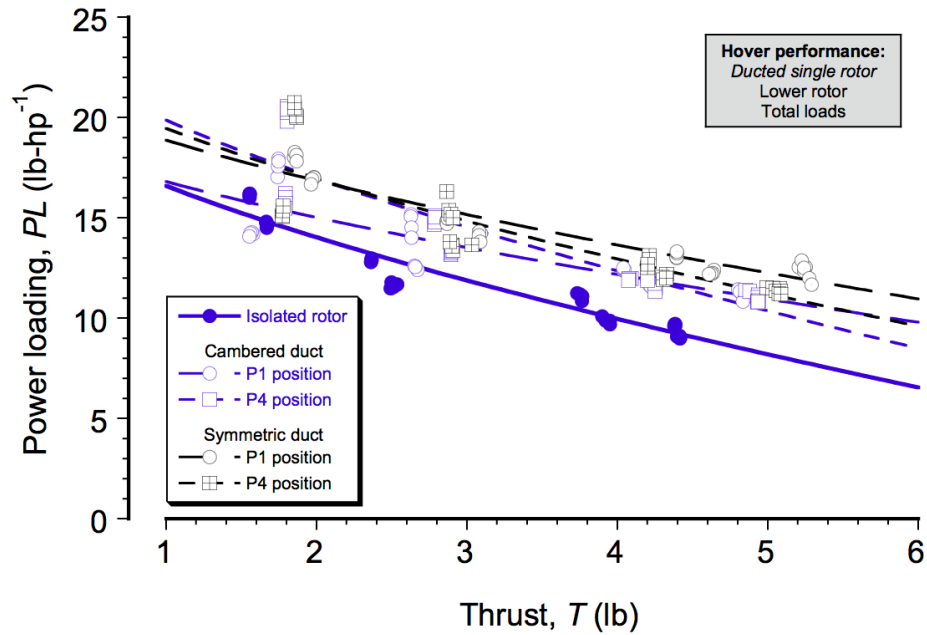
(b) Lower rotor.

Figure 4.5: Hover performance of the ducted single rotors with variation in duct shape:

(a) upper rotor and (b) lower rotor. Laboratory measurements.



(a) Upper rotor.



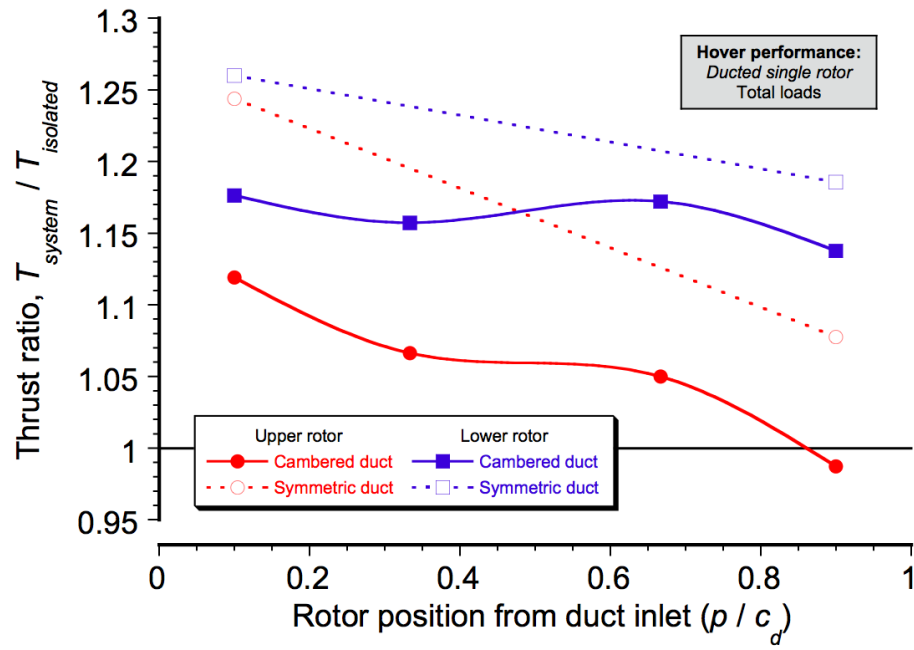
(b) Lower rotor.

Figure 4.6: Hover performance of the ducted single rotors expressed in terms of power loading: (a) upper lower and (b). Laboratory measurements.

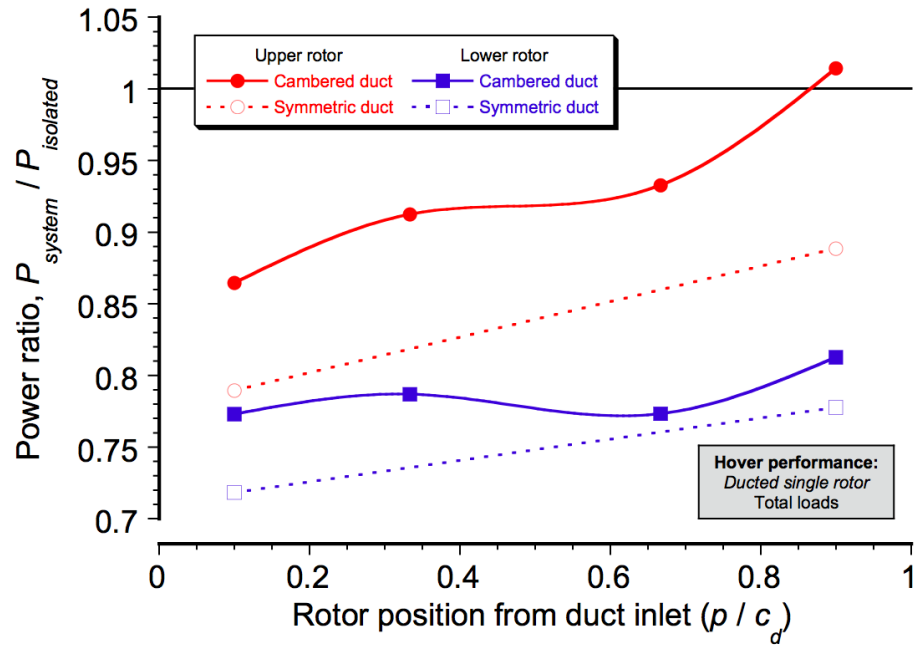
by calculating the thrust required for a given power, and the power required for a given thrust, and normalizing these values by the performance of the respective isolated rotor. Therefore, the net effect of the duct may be easily evaluated. Figures 4.7(a) and 4.7(b) present this analysis for a constant power of 0.5 hp and a constant thrust of 5 lb, respectively. A greater thrust ratio suggests relatively higher duct thrust and a corresponding off-loading of the rotor. These results used data above 5,000 rpm to reduce the impact of Reynolds number.

The performance sensitivity of duct position showed that for the single rotors, rotor placement close to the inlet was the most beneficial, with mostly decreasing benefits as the rotor was moved further into the duct. For the cambered ducted upper rotor at the P4 position, a slight performance degradation was also noted. The advantage of the symmetric duct was also evident; the symmetric duct had a greater leading edge radius, suggesting that this geometry induced stronger suction pressure at the duct lip and caused greater duct lift. This outcome was true for both the upper and lower rotors. Positioning the rotor further back in the duct decreased any augmentation of system performance, which likely was caused by a combination of two factors that are more pronounced at the positions furthest in the duct: (1) Altered inflow into the duct, leading to reduced duct forces; and (2) Ingestion of the boundary layer on the duct wall by the rotor, increasing rotor power requirements. If these two factors were sufficiently large, the net effect could be a system performance degradation, as seen for the ducted upper rotor at the P4 rotor position.

While it was shown that, in isolation, the upper rotor produced better performance levels than the lower rotor, the performance of the lower rotor was more improved by the



(a) Thrust ratio at a constant power of 0.5 hp.



(b) Power ratio at constant thrust of 5 lb.

Figure 4.7: Hover performance of the ducted single rotors with variation in rotor position at a given thrust or power. Laboratory measurements.

presence of the duct. Because of the greater collective pitch setting on the lower rotor, for the same rotor power, the upper rotor produced more thrust than the lower rotor. With all other factors being equal, this result implied that (in theory) the inflow at the upper rotor was smaller than that for the lower rotor. While it is desirable for an isolated rotor to have a low induced velocity for the same thrust, a greater mass flow rate suggests greater duct inlet suction pressures and a higher duct thrust. In other words, the relatively greater mass flow rate of the lower rotor caused greater duct forces than for the upper rotor.

4.2.3.3 Loads Decomposition

Swapping the load path of the balance allowed for both the total and rotor-only loads to be measured. Figure 4.8 shows the loads of the ducted upper rotor system, as well as the rotor itself, for the P1 and P3 rotor positions. The cambered duct was used in this case.

These measurements were conducted in the wind tunnel, which showed that the P1 rotor position caused a performance improvement for all levels of thrust. Interestingly, the performance of the P3 position only exceeded that of the isolated rotor at higher thrusts in this environment. This result may be explainable by the ingestion of the boundary layer from the duct wall at this position, which was relatively further back in the duct than the P1 position. This behavior, which was also observed in the laboratory measurements (Fig. 4.7), drove up rotor power requirements, and the ducted system performance only exceeded the isolated case at higher thrusts when the rotor inflow was higher and the duct thrust was greater.

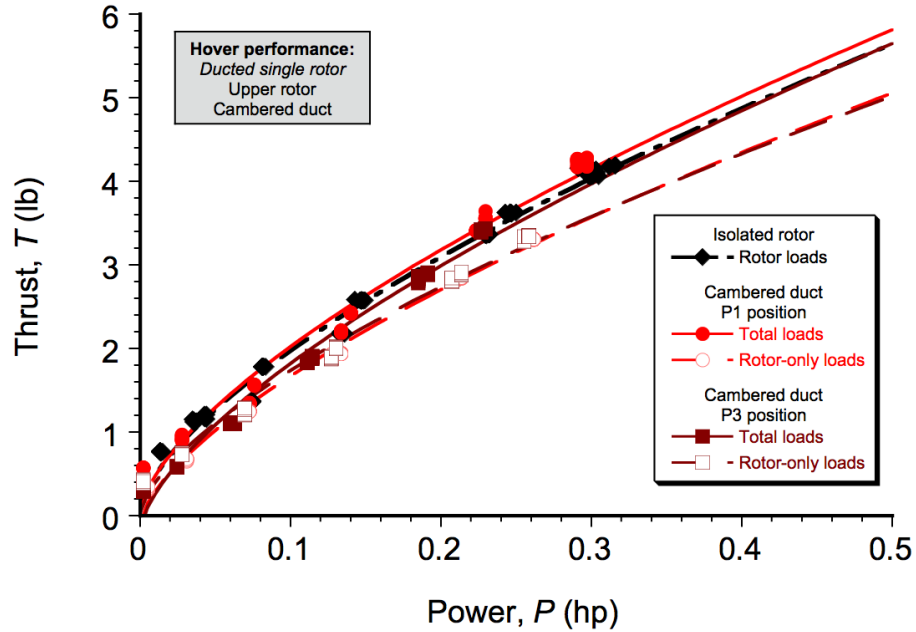


Figure 4.8: Hover performance of the ducted upper rotor, comparing the total and rotor-only loads for positions P1 and P3 against the isolated loads. Wind tunnel measurements.

When compared at the same power, the thrust produced by the isolated rotor was notably greater than for the rotor operating within the duct. This outcome was partially attributed to the offloading of the rotor itself; as duct lift increased a corresponding decrease in thrust produced by the rotor was expected, though obviously at a lower rate, so the net effect was an improvement in thrust at the same power. By this argument, the expectation would be, at the same power, the thrust of the rotor at the P3 position would be greater than that of the rotor at the P1 position, because the duct produced more lift at the P1 position. However, the measurements suggested that the thrust of the rotor was nearly the same at the P1 and P3 positions. This result may be caused by the aerodynamic interference of the duct on the rotor at the P3 position, driving up power requirements, which was not shown for the P1 position.

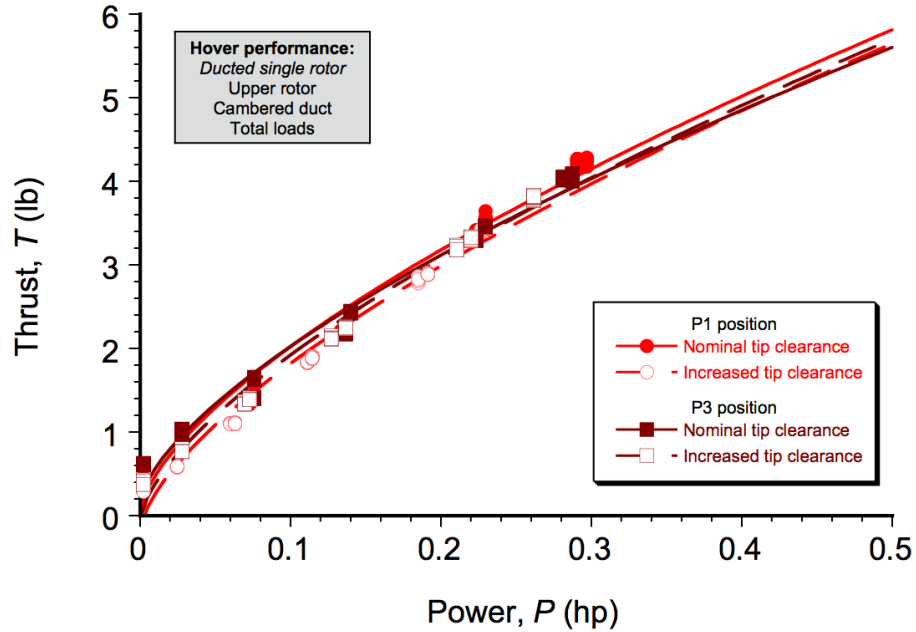


Figure 4.9: Hover performance of the ducted upper rotor with variation in tip clearance for the P1 and P3 rotor/duct positions. Wind tunnel measurements.

4.2.3.4 Tip Clearance

The influence of varying the tip clearance on performance is shown in Fig. 4.9. For this investigation, the upper rotor was used with the cambered duct. Two rotor positions were used, P1 and P3, to determine if the effect of increasing the tip clearance was also influenced by the rotor position within the duct.

When the rotor was operated at the P1 position, the performance of the system was slightly degraded when the tip clearance was increased, which was expected because the gains from lower tip losses with a smaller tip clearance were now lost. However, increasing the tip clearance when the rotor was operated at the P3 position showed different results. In this case, the system performance was barely improved with a larger tip gap. Based on previous measurements of the ducted rotor at this position, the outcome

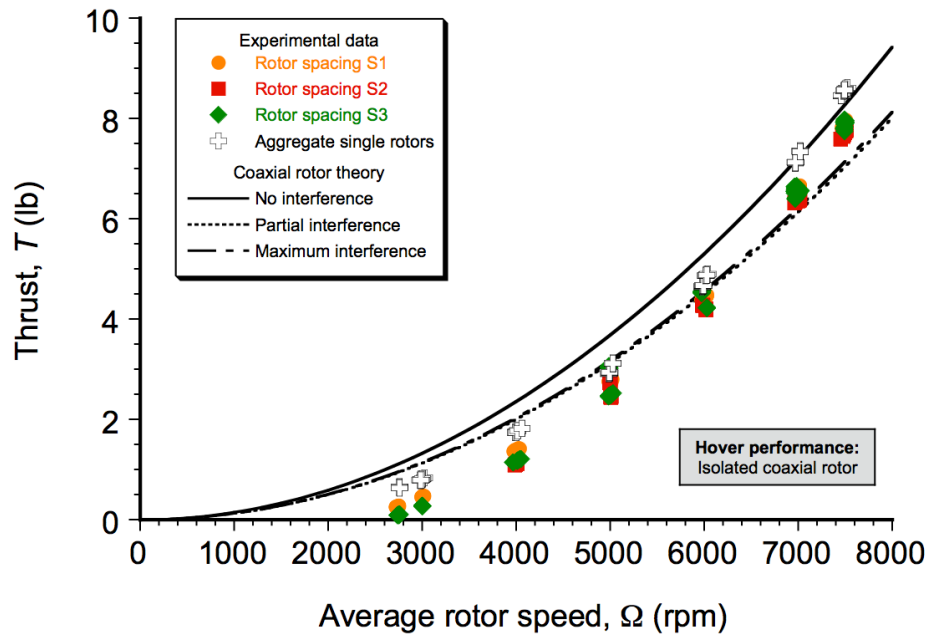
of increased tip clearance improving performance was consistent with allowing the thick boundary layer of the duct wall to influence less of the rotor at this position.

4.2.4 Isolated Coaxial Rotor Performance in Hover

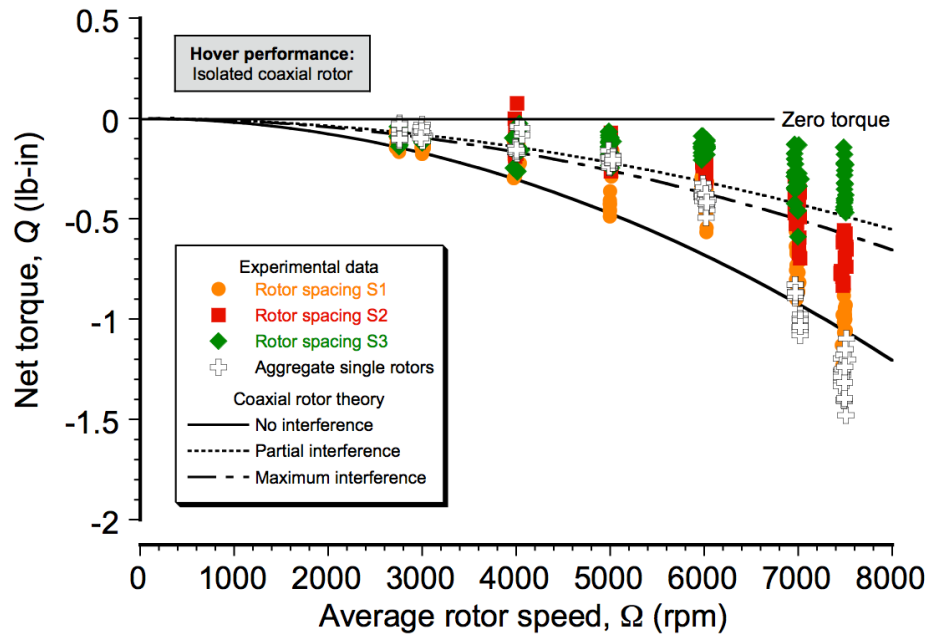
Figure 4.10 shows the measurements of total thrust and net torque of the isolated coaxial rotor for rotor spacings of S1 ($0.15R$), S2 ($0.20R$), and S3 ($0.30R$). No apparent sensitivity of thrust to rotor spacing was evident. However, the torque measurements show a negative shift in net torque for more closely spaced rotors. This result implied that for closer rotor spacings the lower rotor required more torque for the same (average) rotor speed. This outcome was most likely caused by the upper rotor wake as it impinged on a greater fraction of the lower rotor disk. Notice that the exact effect of rotor spacing on power and, therefore, system efficiency cannot be inferred from Fig. 4.10 because power measurements were not obtained for these cases, and total power cannot be calculated from net rotor torque alone.

Figure 4.10 also shows the sum of the performance of the upper and lower rotors when they were tested separately. These measurements indicate that the rotor-rotor aerodynamic interference in the coaxial rotor system appeared to affect both the thrust and net torque as compared to when the rotors were operated separately.

Also shown in Fig. 4.10 are predictions based on coaxial rotor theory [40]. These predictions were calculated for several levels of aerodynamic interference: (1) No interference (the rotor flows are decoupled and the rotors operate in aerodynamic isolation); (2) Partial interference, where the lower rotor operates in the contracted upper rotor wake



(a) Thrust measurements.



(b) Net torque measurements.

Figure 4.10: Hover performance of the isolated coaxial rotor, with variation in rotor spacing and theory predictions: (a) thrust and (b) torque. Wind tunnel measurements.

(in this case, the rotor wake area contraction is 0.82, which was the assumption used for the selection of the blade pitch); and (3) Maximum interference, where the lower rotor operates fully within the non-contracted wake of the upper rotor (infinitely close rotor spacing). The predicted results suggested some sensitivity of rotor-to-rotor spacing on performance, but less than the sensitivity in the performance measurements.

4.2.4.1 Comparison of Isolated Single Rotors and Isolated Coaxial Rotor

While individual torque measurements of the upper and lower rotor were not obtained (only the net rotor torque was measured), the total rotor power, as obtained from the correlation between electric power and aerodynamic power, allowed for the individual torques of the coaxial rotor to be determined.

Upper rotor torque Q_u and lower rotor torque Q_l was determined from the net torque Q_N and total power P_T , i.e.,

$$\text{Net torque: } Q_u + Q_l = Q_N \quad (4.1)$$

$$\text{Total power: } \Omega_u Q_u + \Omega_l Q_l = P_u + P_l = P_T \quad (4.2)$$

The equations for net torque of the system (Eq. (4.1)) and total power of the system (Eq. (4.2)) were expressed in terms of a matrix equation, i.e.,

$$\begin{bmatrix} 1 & 1 \\ \Omega_u & \Omega_l \end{bmatrix} \begin{bmatrix} Q_u \\ Q_l \end{bmatrix} = \begin{bmatrix} Q_N \\ P_T \end{bmatrix} \quad (4.3)$$

which can be rewritten as

$$\begin{bmatrix} Q_u \\ Q_l \end{bmatrix} = \begin{bmatrix} 1 & 1 \\ \Omega_u & \Omega_l \end{bmatrix}^{-1} \begin{bmatrix} Q_N \\ P_T \end{bmatrix} \quad (4.4)$$

Solving for the upper and lower rotor torque, and also the upper and lower rotor power, from Eq. (4.3) and Eq. (4.4) gives

$$Q_u = \frac{P_T - \Omega_l Q_N}{\Omega_u - \Omega_l} \quad (4.5)$$

$$Q_l = \frac{-P_T + \Omega_u Q_N}{\Omega_u - \Omega_l} \quad (4.6)$$

and

$$P_u = \Omega_u Q_u = \Omega_u \left(\frac{P_T - \Omega_l Q_N}{\Omega_u - \Omega_l} \right) \quad (4.7)$$

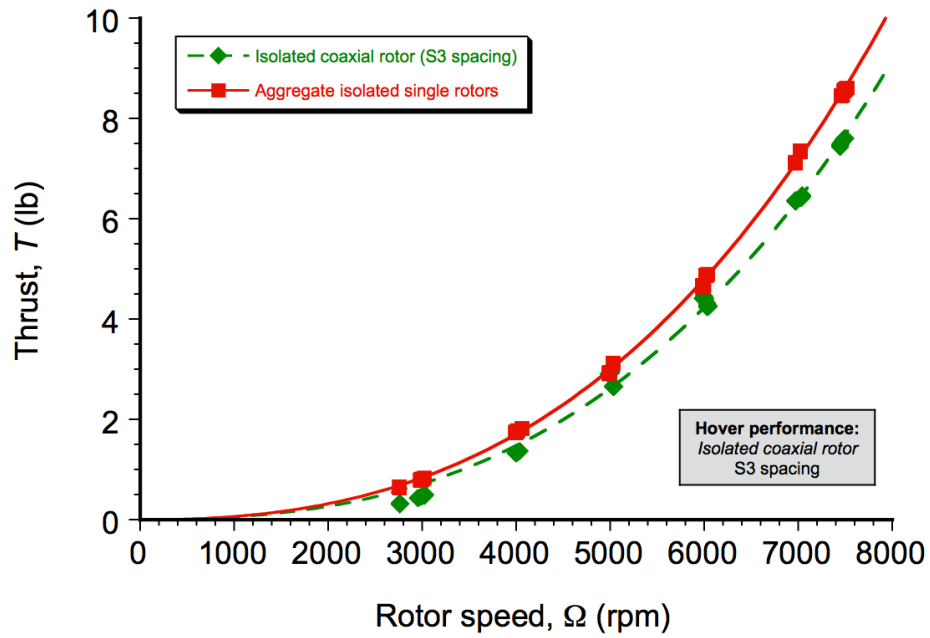
$$P_l = \Omega_l Q_l = \Omega_l \left(\frac{-P_T + \Omega_u Q_N}{\Omega_u - \Omega_l} \right) \quad (4.8)$$

In this case, the upper rotor speed Ω_u and torque Q_u were defined to be nominally positive, and the lower rotor speed Ω_l and torque Q_l were defined to be nominally negative (contra-rotating rotors).

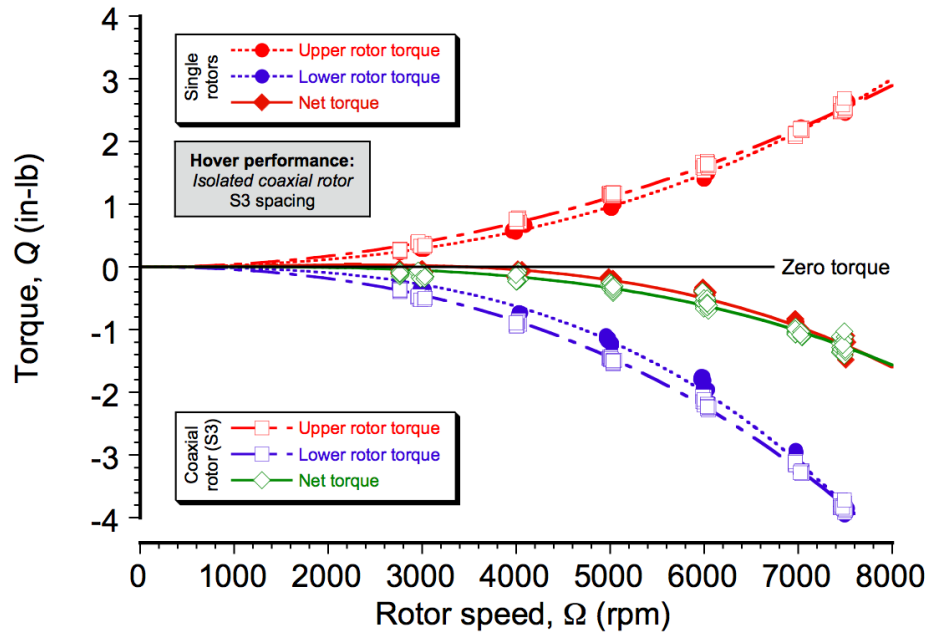
In using these equations, two rotors operating simultaneously (as a coaxial system) can be compared to two rotors operating separately. For this analysis, a baseline performance case for the isolated coaxial rotor was determined by summing the thrust, torque, and power of the upper and lower rotors to obtain the aggregate performance. This baseline case allowed for comparison of the coaxial rotor to the sum of its separate parts. Figure 4.11 shows the loads for these cases, where the coaxial rotor was operated at the S3 (0.30R) rotor spacing.

The thrust produced by the coaxial rotor could not be decomposed into the thrust produced by the individual rotors, but overall the coaxial rotor was found to produce less thrust at a given rotor speed than the sum of the isolated rotor thrusts.

The torque of each rotor when operated coaxially was greater than that of the iso-

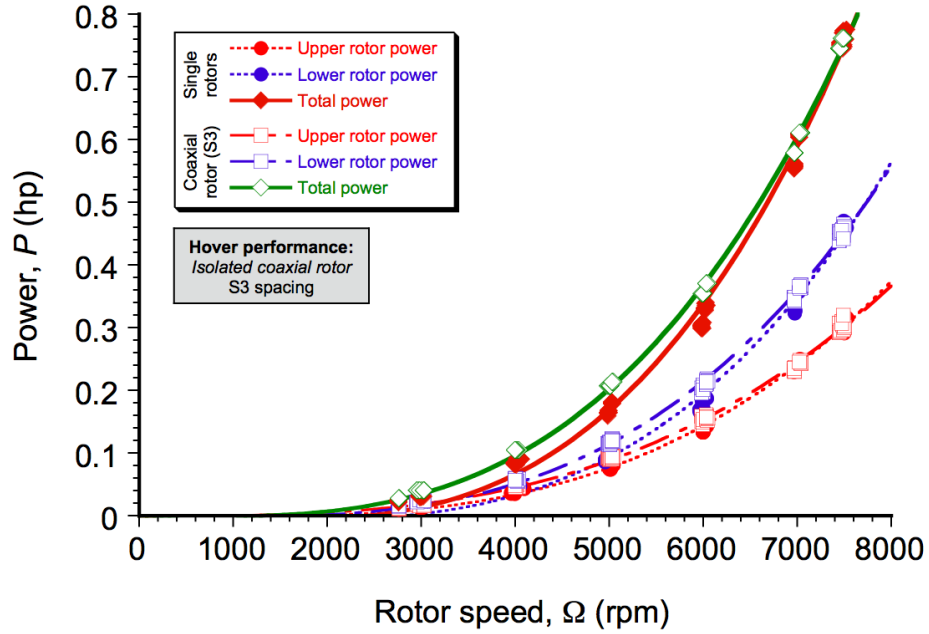


(a) Thrust.



(b) Torque.

Figure 4.11: Hover performance of the isolated coaxial rotor, compared to the isolated single rotors in terms of (a) thrust, (b) torque, and (c) power. Wind tunnel measurements.



(c) Power.

Figure 4.11: Hover performance of the isolated coaxial rotor, compared to the isolated single rotors in terms of (a) thrust, (b) torque, and (c) power. Wind tunnel measurements.

(cont)

lated case. The net effect was found to be biased towards the lower rotor, which was expected because of the wake impingement of the upper rotor on the lower rotor. This behavior increased the power requirements of the lower rotor compared to the theoretical (aggregate) single rotor case. The power sharing of the coaxial rotor then was more biased towards the lower rotor; see Fig. 4.12.

It is interesting to also note that in the coaxial system the upper rotor torque and power also increased when it was operated above the lower rotor. That is, the presence of the lower rotor also affected the upper rotor in these experiments.

The power polar from the measurements shown in Fig. 4.11 is presented in Fig. 4.13.

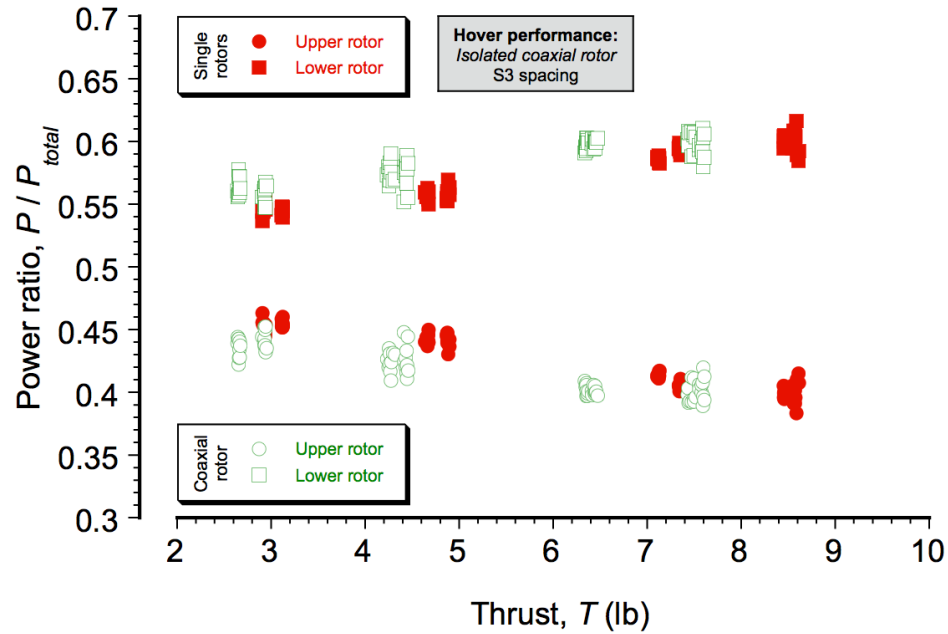


Figure 4.12: Power sharing ratio of the isolated coaxial rotor as compared to the aggregate single rotors. Wind tunnel measurements.

It was shown that to produce the same thrust the coaxial rotor had greater power requirements, which was a direct result of the aerodynamic interference between the two rotors operating as a coaxial system. While this interference slightly drove up the power requirements for the upper rotor, the lower rotor mostly incurred the penalty. In total, this penalty was on the order of 25% for 5,000 rpm and higher.

4.2.5 Ducted Coaxial Rotor Performance in Hover

4.2.5.1 Rotor Spacing and Rotor/Duct Position

Interestingly, the trends observed for a ducted single rotor did not always hold for the ducted coaxial rotor. Figure 4.14 shows the performance of the ducted coaxial rotor,

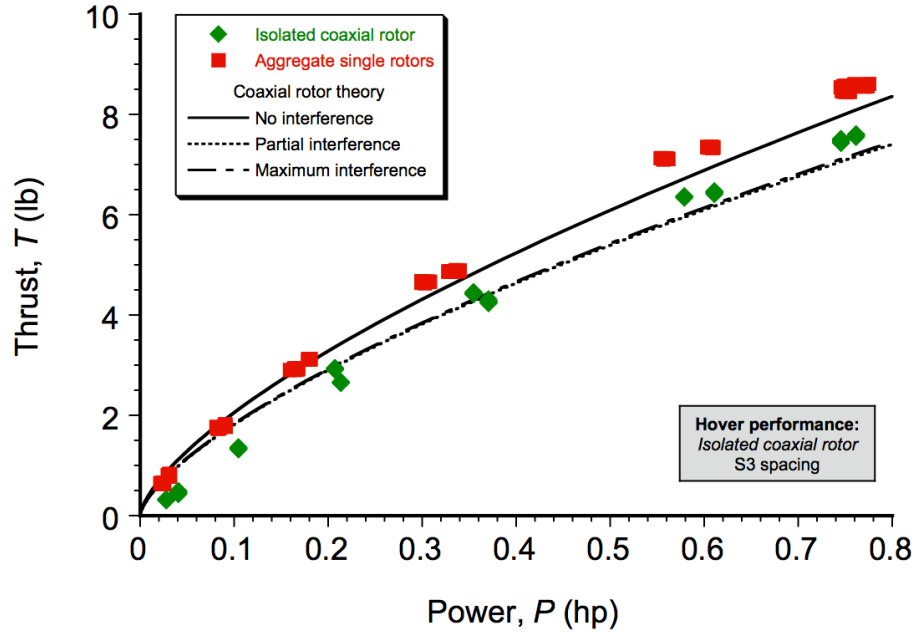


Figure 4.13: Hover performance of the isolated coaxial rotor as compared to the aggregate single rotors and theory predictions. Wind tunnel measurements.

using the cambered duct shape, at three combinations of rotor spacings and rotor/duct positions: (1) S1/C/P1, (2) S1/C/P3, and (3) S3/C/P3. Notice that power measurements of the ducted coaxial rotor with S3 rotor spacing, cambered duct inlet, and P1 rotor position (S3/C/P1) were not obtained, so the performance of this configuration could not be assessed.

Unlike ducting the single rotor, which almost always improved system performance and efficiency relative to the isolated single rotor, only the S1/C/P3 ducted coaxial rotor configuration operated at high levels of thrust improved upon the performance of the isolated coaxial rotor. The other configurations, S1/C/P1 and S3/C/P3, both suffered serious performance degradations when they were operated in the cambered duct. This result is particularly intriguing for the case of the S1/C/P1 configuration, because the

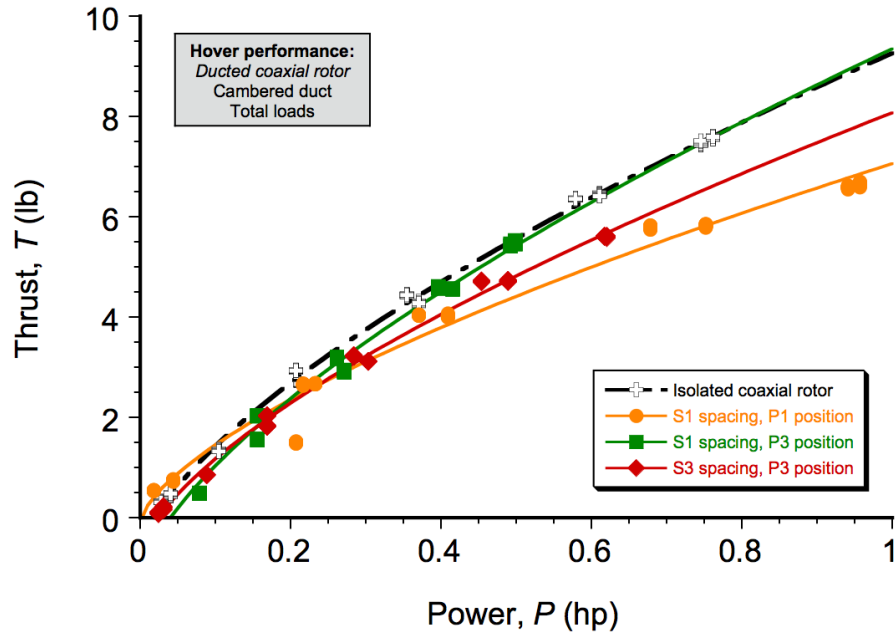


Figure 4.14: Hover performance of the ducted coaxial rotor at various combinations of rotor spacing and rotor/duct position. Wind tunnel measurements.

ducted single rotor tests suggested that the P1 rotor position was the most beneficial when, for the ducted coaxial rotor, it was found to be the least.

A better picture of the performance of these configurations becomes clear when investigating the individual rotor powers, as shown in Fig. 4.15. For all configurations, a much greater power consumption by the lower rotor in any ducted coaxial rotor configuration was evident from the notably decreased power ratio for the upper rotor; that is, the lower rotor consumed a relatively greater fraction of the total power. This effect is likely attributed to the presence of the duct. The normal contraction of the upper rotor wake was constrained, and the wake impinged on a relatively greater fraction of the lower rotor disk area, thereby further increasing the power requirements of the lower rotor of the ducted coaxial system than when it was isolated.

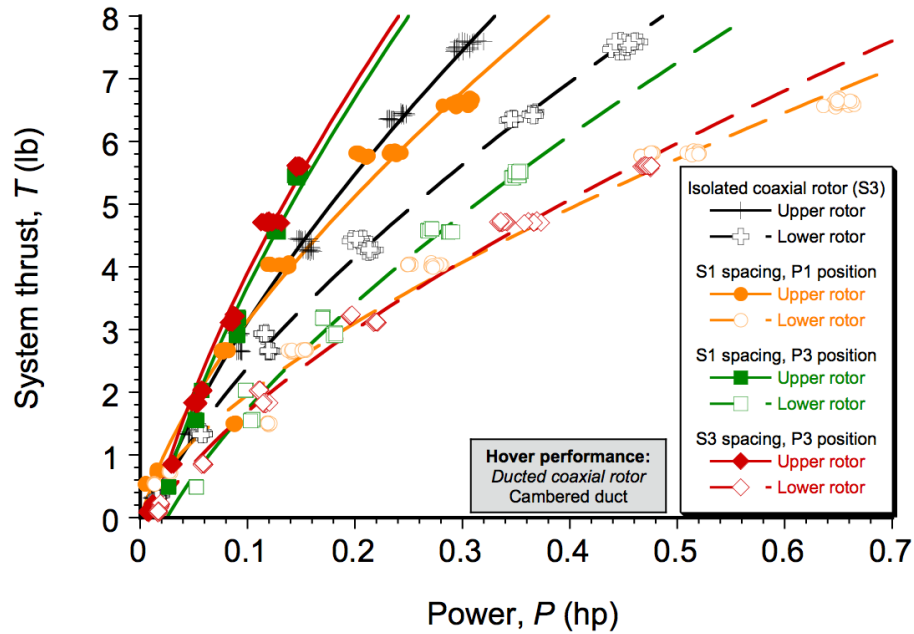
The reasons for the relative performance merits of the various ducted coaxial rotor configurations cannot be fully explained using the performance measurements alone. For the S1/C/P3 and S3/C/P3 configurations, the power requirements of the upper rotor were decreased relative to the isolated upper rotor, which was consistent with the duct offloading the rotor. However, the power requirements for the upper rotor in the S1/C/P1 configuration were increased instead.

The profound impact on efficiency (in terms of power loading) of the coaxial rotor when ducted is shown in Fig. 4.16. The power loading was substantially degraded for the ducted coaxial rotor configurations (S1/C/P1 and S3/C/P3) as compared to the isolated coaxial rotor (a reduction of approximately 25%). However, ducting a coaxial rotor improved efficiency over the isolated case for the S1/C/P3 configuration, but only at high levels of thrust.

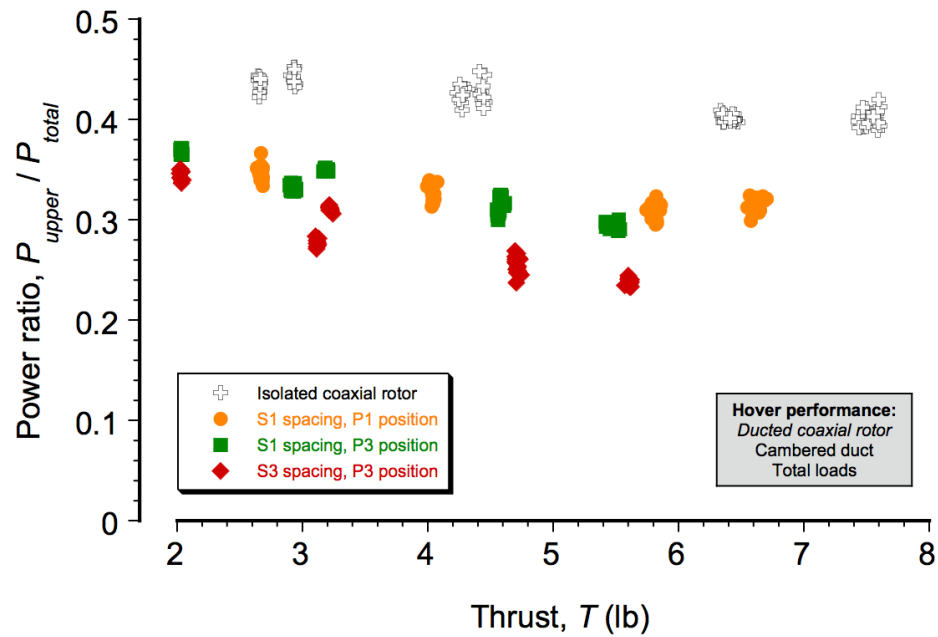
Relative to the isolated upper rotor at the same disk loading, the power loading of the isolated coaxial rotor was found to be 20% lower (a result caused by rotor-to-rotor aerodynamic interference), and the power loading of the ducted coaxial rotor (S1/C/P3) was overall 15% lower than for the ducted upper rotor at the same disk loading.

4.2.5.2 Duct Shape

Unlike the effects of rotor spacing and rotor/duct position on performance, which were not consistent with the ducted single rotor measurements, a modest improvement in performance was observed using the symmetric duct instead of the cambered duct; see Fig. 4.17. In this case for the S1 rotor spacing and P3 rotor/duct position, operating the



(a) Upper and lower rotor power.



(b) Upper rotor power fraction.

Figure 4.15: Upper and lower rotor power for various ducted coaxial rotor configurations:

(a) expressed in magnitude and (b) expressed in terms of the upper rotor power fraction.

Wind tunnel measurements.

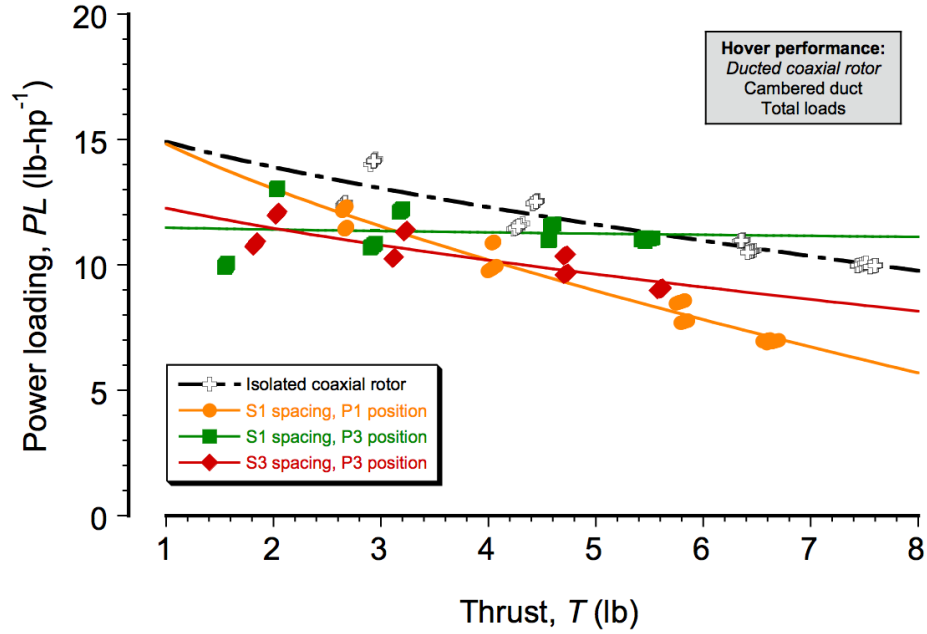


Figure 4.16: Hover performance of the ducted coaxial rotor, expressed in terms of power loading. Wind tunnel measurements.

coaxial rotor in the symmetric duct produced a net improvement in system performance over the cambered duct. The performance improvement caused by this duct shape further suggests that a more rounded inlet is preferable for both single and coaxial rotors in hover because it produced greater suction pressures.

4.2.5.3 Tip Clearance

The effect of tip clearance on the performance of the ducted coaxial rotor is shown in Fig. 4.18. For this investigation, the S1/C/P3 configuration was used. Figure 4.18 shows that increasing the tip clearance reduced the performance of the system as well as the performance of the coaxial rotor itself, a performance characteristic that was consistent with the ducted single rotor at the P1 rotor position (Fig. 4.9). However, for the

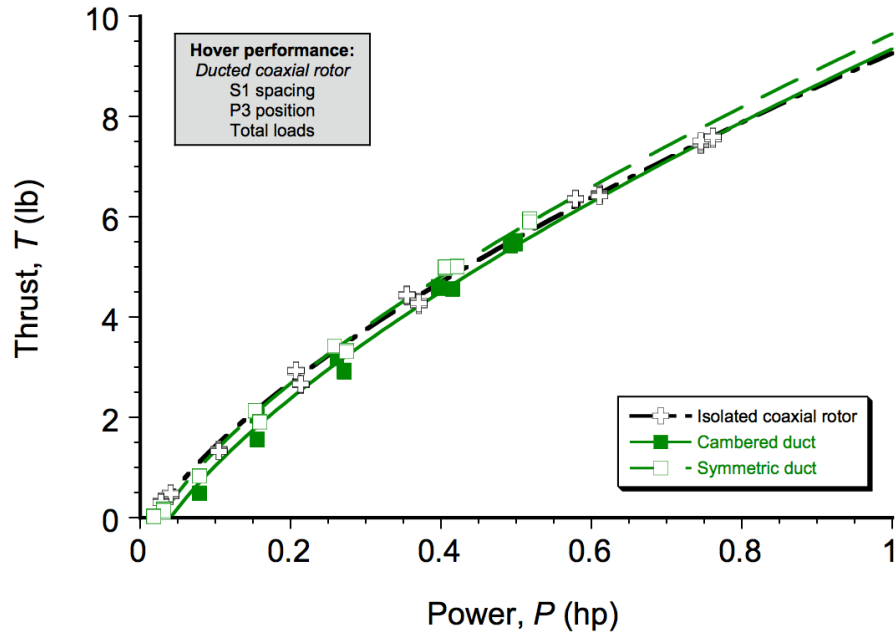


Figure 4.17: Hover performance of the ducted coaxial rotor with variation in duct shape.

Wind tunnel measurements.

ducted single rotor at the same position (P3), opening the tip gap appeared to slightly improve performance, whereas for the coaxial rotor at this same position, a degradation of performance was instead observed. This outcome may be explained by the advantage of an increased tip gap at the P3 position being outweighed by greater tip losses from the coaxial rotors, which would incur a higher penalty compared to a ducted single rotor system.

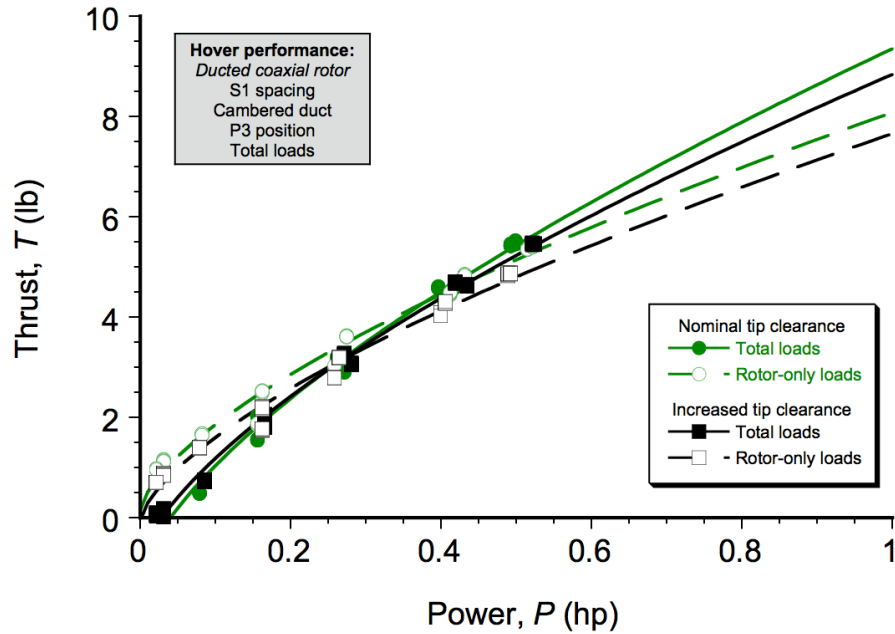


Figure 4.18: Hover performance of the ducted coaxial rotor with variation in tip clearance.

Wind tunnel measurements.

4.3 Axial Forward Flight Performance

4.3.1 Introduction

The various rotor systems were tested in the wind tunnel in axial climbing flight or “propeller mode,” where the pitch of the test article was 0° . In this operating condition, the rotors essentially behaved as fixed-pitch propellers and so, in general, fairly classic propeller characteristics were observed. Assessing the axial flight performance of ducted rotor systems for use as a MAV was important for two reasons: (1) Efficient climb performance requires less power and, therefore, a lighter powerplant may be used; and 2) The duct forces may improve system performance in this flight condition.

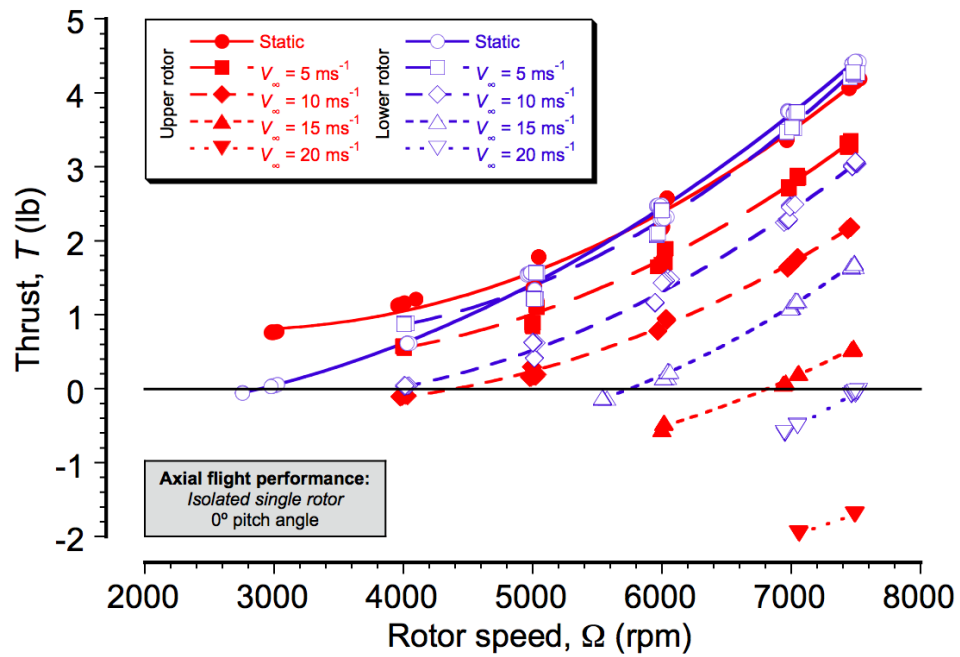
4.3.2 Isolated Single Rotor Performance in Axial Flight

Shown in Figs. 4.19(a)–(c) is the axial flight performance (in terms of thrust, torque, and power) of the upper rotor and lower rotor operating in isolation (no duct). Axial flow through the rotor increased the inflow angle and, because the blade pitch angle was fixed, the angle of attack of the blade sections was reduced. This effect reduced the thrust of the rotor, decreasing by approximately the square of the wind speed (see Fig. 4.19(a)). Lower levels of thrust also decreased rotor torque and power accordingly (see Figs. 4.19(b) and 4.19(c)).

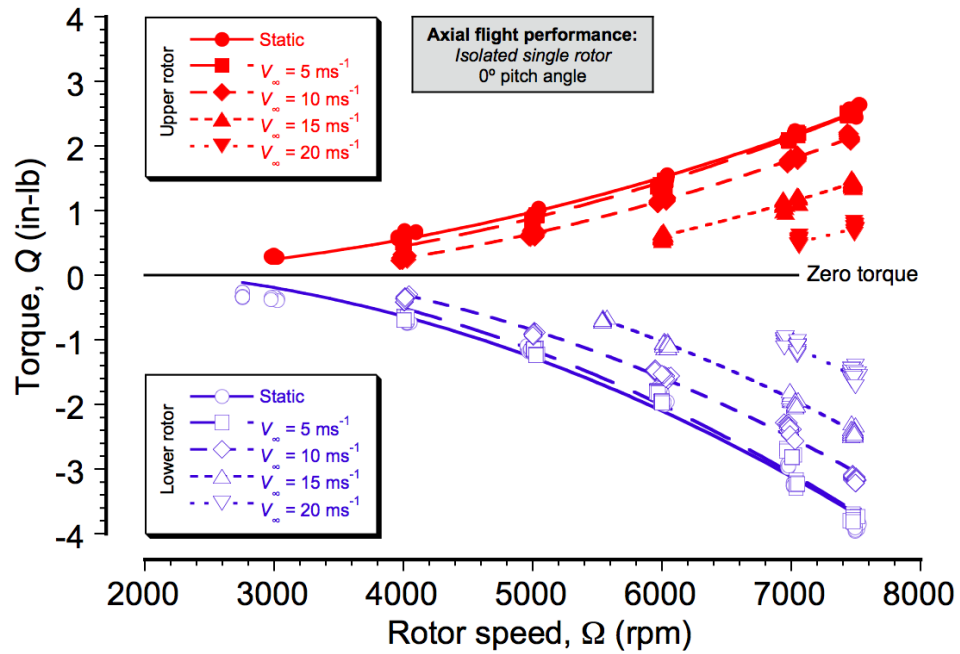
In general, if the blade section angle of attack is reduced to zero (inflow angle and blade section pitch angle are equal), no lift is produced by that blade section. The point at which the rotor produces zero thrust is the beginning of the brake state. In this state, the blades carry both regions of positive and negative lift, and the rotor flow state begins to exhibit recirculation with upstream and downstream flows.

For a given wind speed, the lower rotor entered the brake state at a lower rotor speed than the upper rotor. For example, while operating in a 10 ms^{-1} climb, the brake state was reached at 4,400 rpm for the upper rotor and 3,900 rpm for the lower rotor. The slightly higher collective pitch setting of the lower rotor was the cause of this behavior.

To directly compare the axial flight performance of the isolated single rotors, the power polar of each rotor is given in Fig. 4.20. In hover, the upper rotor was clearly more efficient; to achieve the same amount of thrust, less power was required for the upper rotor than for the lower rotor. As the axial climb speed increased, to produce the same thrust the upper rotor was operated at a relatively higher rotor speed, requiring more torque and

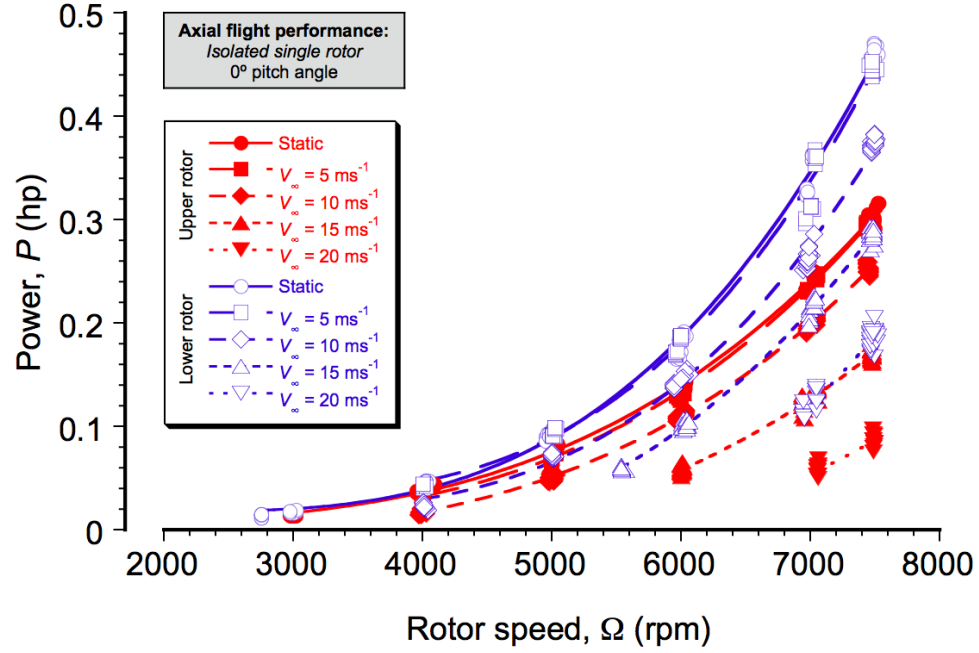


(a) Thrust.



(b) Torque.

Figure 4.19: Axial flight performance of the isolated upper and lower rotors: (a) thrust, (b) torque, (c) power.



(c) Power.

Figure 4.19: Axial flight performance of the isolated upper and lower rotors: (a) thrust, (b) torque, (c) power. (cont)

power.

The results may be presented in terms of the composite efficiency η , which is given by

$$\eta = T(V_{\infty} + \bar{v}_i) / P \quad (4.9)$$

where the average induced velocity $\bar{v}_i$ was determined from simple momentum theory, i.e.,

$$\bar{v}_i = -\frac{V_{\infty}}{2} + \sqrt{\left(\frac{V_{\infty}}{2}\right)^2 + \frac{T}{2\rho A_{eff}}} \quad (4.10)$$

For single rotors, $A_{eff} = A$, the rotor disk area, and for coaxial rotors, $A_{eff} = 2A$ (twice the disk area of the single rotor). Notice that in hover $V_{\infty} = 0$, and the definition for the

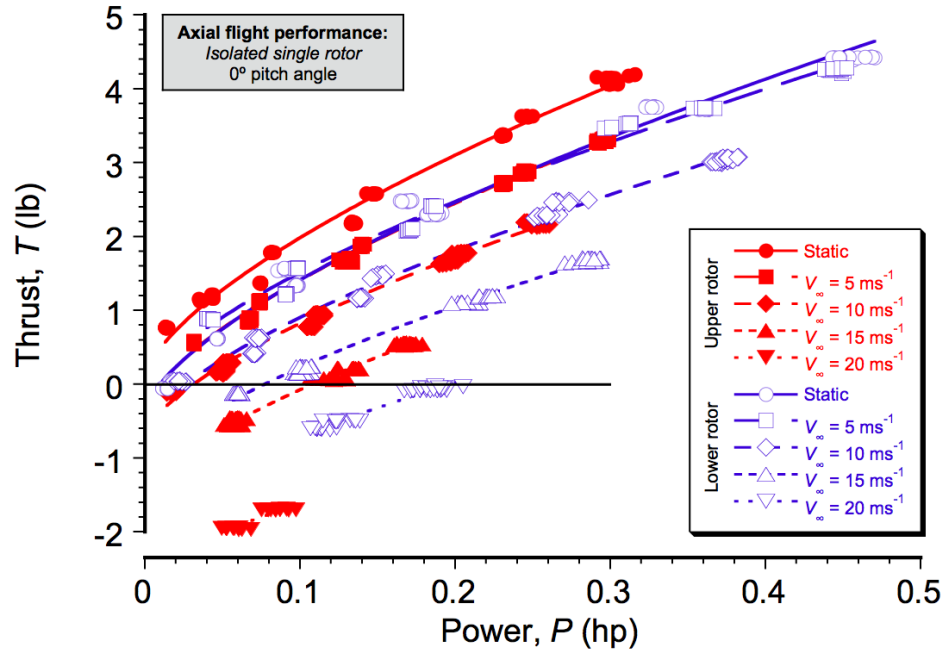


Figure 4.20: Axial flight performance of the isolated upper and lower rotors, shown in power polar form.

average induced velocity simplifies to the definition given from momentum theory. Therefore, the definition of composite efficiency in hover is reduced to the classical definition of figure of merit. When $V_\infty \gg \bar{v}_i$, the definition of composite efficiency approaches the definition of propulsive efficiency.

The composite efficiency of the single rotors is presented in Fig. 4.21. In hover, the figures of merit for the upper and lower rotors were approximately 0.7 and 0.55, respectively, which was rather good for rotors operating in low Reynolds number flow at this scale. The maximum propulsive efficiency of 0.75 was seen around $10\text{--}15\text{ ms}^{-1}$. In general, the efficiency of the upper rotor was slightly greater than that of the lower rotor.

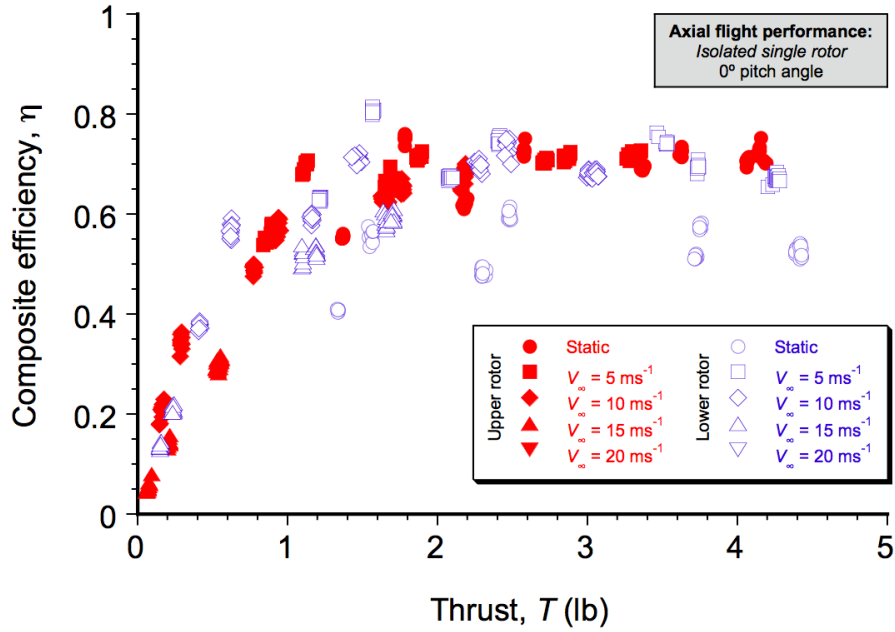
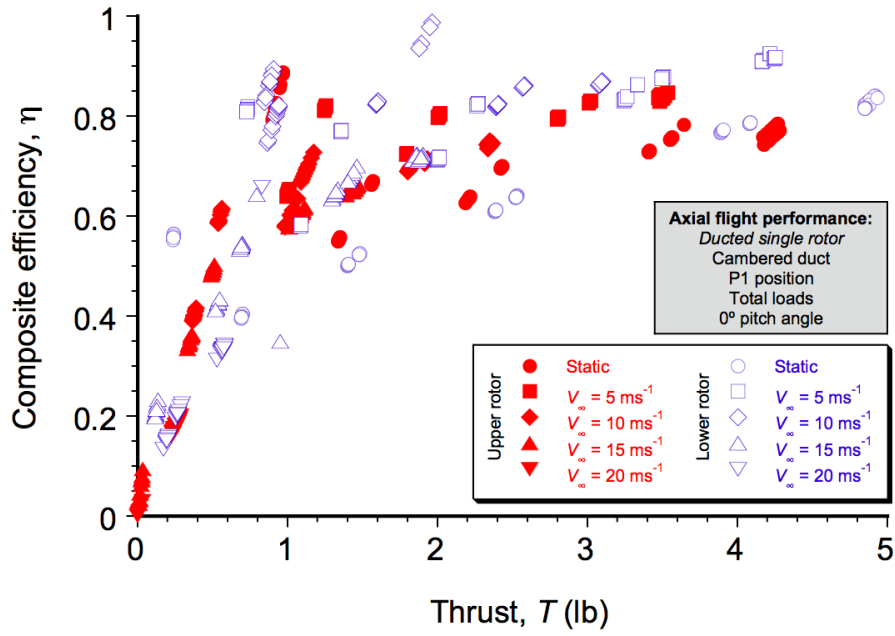


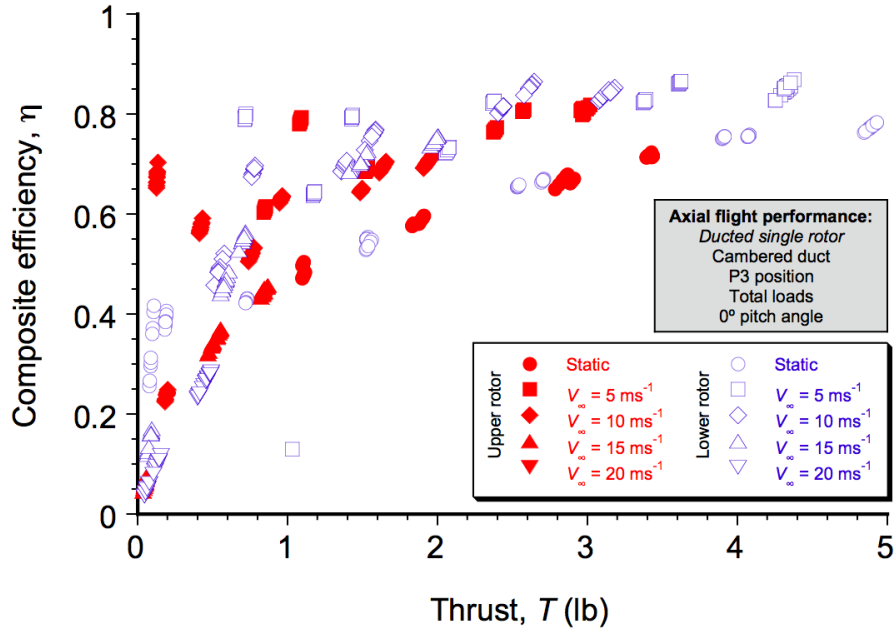
Figure 4.21: Composite efficiency of the isolated single rotors in axial flight.

4.3.3 Ducted Single Rotor Performance in Axial Flight

Figure 4.22 shows that when the single rotors were ducted their efficiency improved. The maximum hovering efficiency (i.e., figure of merit) of both the ducted upper and ducted lower rotors was about 0.75, which for the isolated lower rotor was a significant improvement from a figure of merit of 0.5. The maximum propulsive efficiency of both rotors was between 0.8–0.85, which was achieved at low axial flight speeds. While some small differences existed between rotor positions at low levels of thrust, in general the axial flight performance seemed insensitive to where the single rotors were operated in the duct.



(a) P1 rotor/duct position ($p/c_d = 0.1$).



(b) P3 rotor/duct position ($p/c_d = 0.67$).

Figure 4.22: Composite efficiency of the ducted upper and lower rotors in axial flight at

(a) P1 rotor/duct position and (b) P3 rotor/duct position.

4.3.4 Isolated Coaxial Rotor Performance in Axial Flight

The axial flight performance of the isolated coaxial rotor with the S3 rotor spacing is shown in Fig. 4.23. From hover to moderate forward flight speeds (10 ms^{-1}), the power requirements for the isolated coaxial rotor were higher than that of the aggregate of the single rotors, which was a direct result of rotor-on-rotor aerodynamic interference (Fig. 4.13). At high forward flight speeds (15 ms^{-1}), the performance of the isolated coaxial rotor and the aggregate of the single rotors were nearly identical, which suggests the aerodynamic interference between the coaxial rotors was reduced. This outcome may be a result of the upper rotor wake contracting less as the rotor operating state approached the brake state, causing the lower rotor to operate in a more uniform flow. As the upper rotor in the aggregate case entered the brake state around 20 ms^{-1} , the aggregate performance was substantially degraded. In general, the convention that the hovering performance of a coaxial rotor is equal to the performance of two separate (isolated) rotors plus an interference penalty appears to hold in axial flight, at least up to moderate flight speeds until the brake state is reached.

4.3.5 Ducted Coaxial Rotor Performance in Axial Flight

A small loss of hovering efficiency was seen when operating the coaxial rotor in the duct. As shown in Figs. 4.24 and 4.25, the figure of merit of the isolated coaxial rotor with S3 spacing was 0.5, which was slightly greater than the ducted coaxial rotor with the S1/C/P3 configuration. However, the propulsive efficiency of the S1/C/P3 ducted coaxial rotor, which was on average 0.6, was slightly better than for the isolated coaxial rotor. The

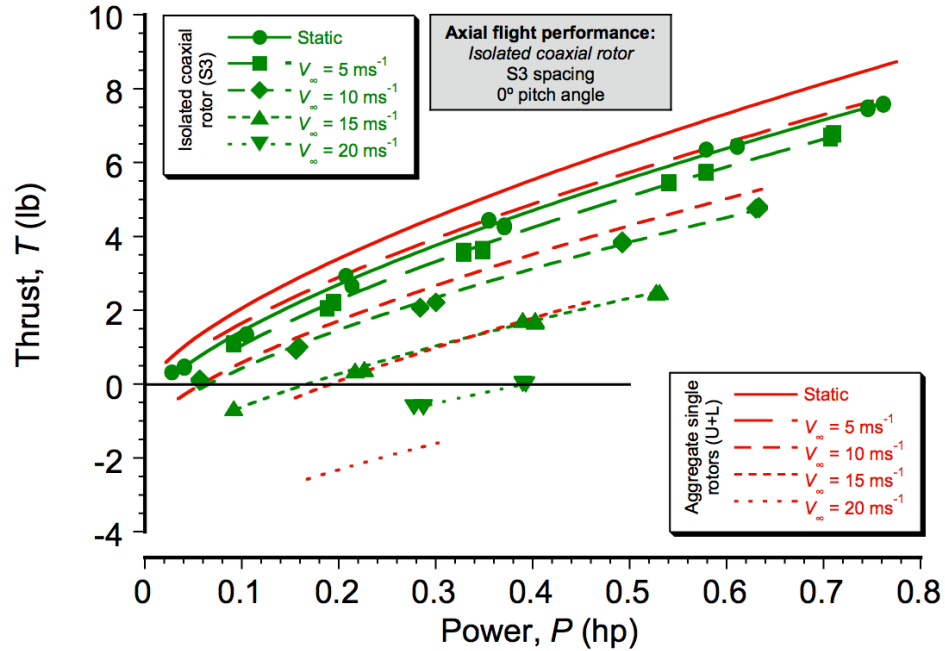


Figure 4.23: Axial performance of the isolated coaxial rotor (S3 spacing) compared to the aggregate single rotors case.

configurations of the ducted coaxial rotor that did not improve upon isolated coaxial rotor performance in hover (S1/C/P1 and S3/C/P3) also had poor axial flight performance. In general, the hover performance trends for rotor spacing and rotor position of the ducted coaxial rotor appeared to hold for axial flight performance. That is, the S1/C/P3 configuration improved on axial flight performance of the isolated coaxial rotor slightly, whereas a large performance degradation was noted for the S1/C/P1 and S3/C/P3 configurations.

The influence of duct shape for the S1/P3 ducted coaxial rotor is shown in Fig. 4.26. The symmetric duct gave slightly better levels of performance to the system when operated in the static condition. However, these advantages were quickly lost in axial flight, and the cambered duct was shown to give better performance than for the symmetric duct. In axial flight, the more rounded inlet shape of the symmetric duct, while good in hover,

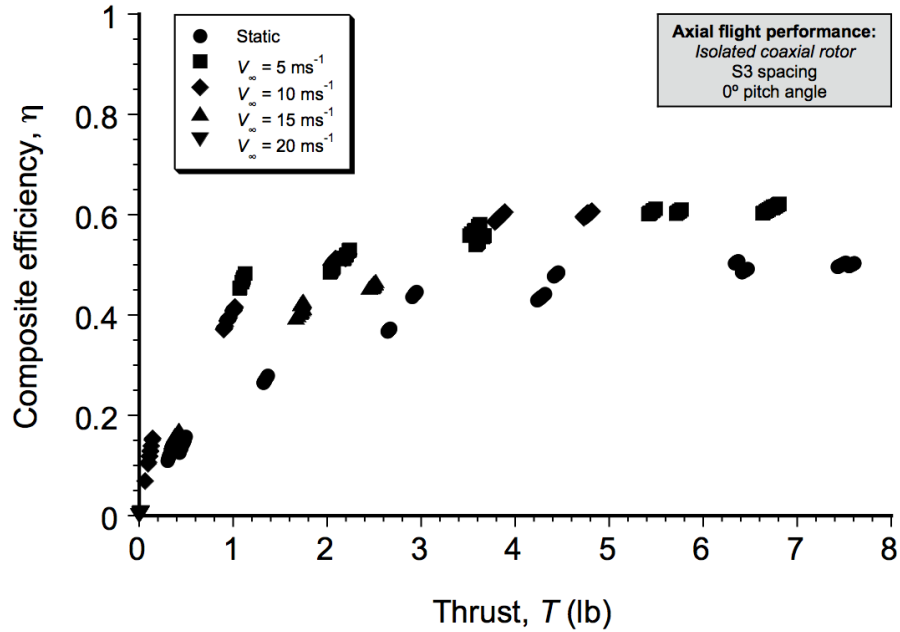
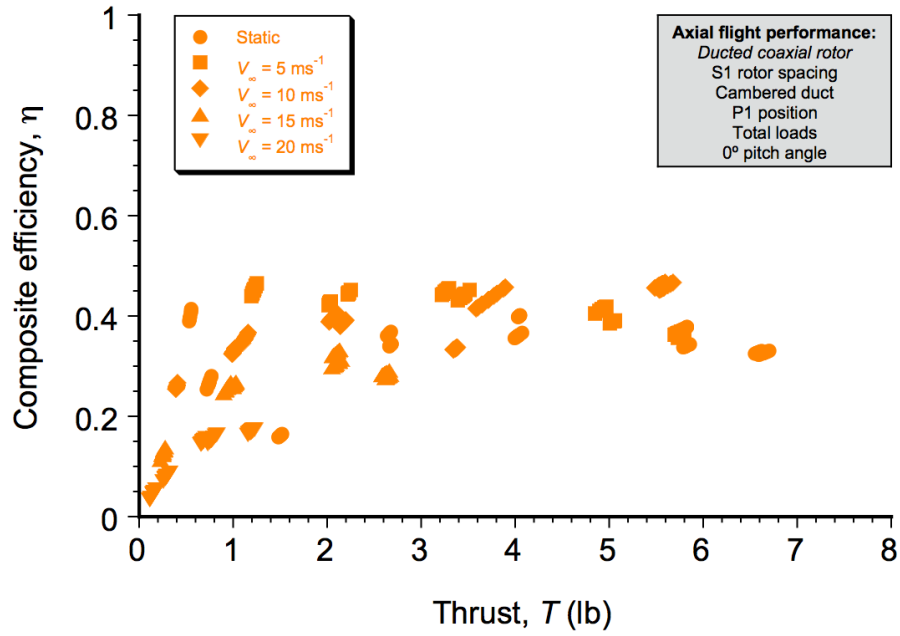


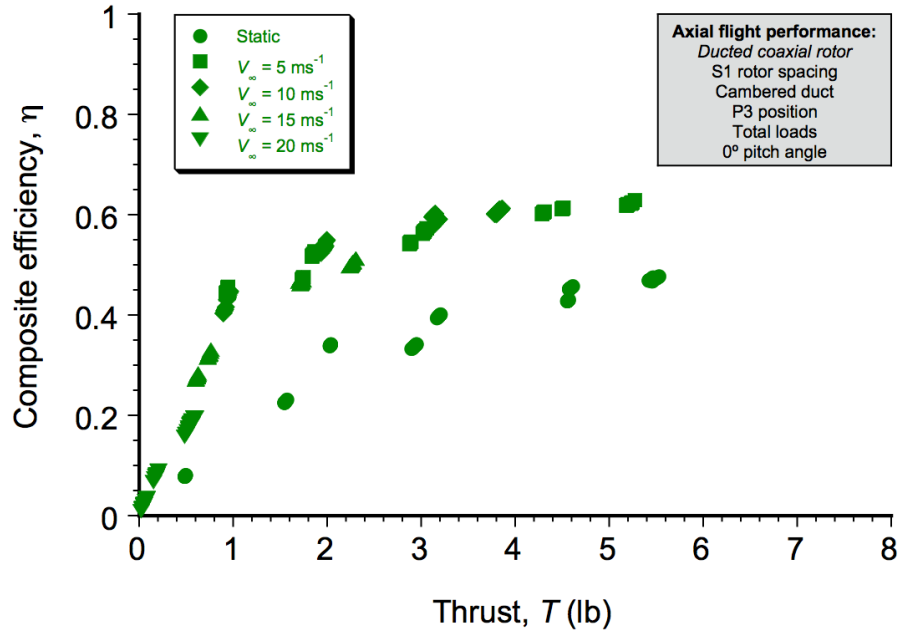
Figure 4.24: Axial performance of the isolated coaxial rotor (S3 spacing), shown in terms of composite efficiency.

likely produced more drag than the cambered duct, which had a smaller leading edge radius.

The effect of increasing the blade tip clearance of the ducted system is shown in Fig. 4.27 in terms of composite efficiency. In the static condition, increasing the tip gap appeared to degrade the system performance slightly, yielding a slightly lower figure of merit. This difference was more evident at low flight speeds, where the composite efficiency of the baseline system was better than in the static case. For moderate-to-high axial flight speeds, the effect of tip clearance did not substantially change system performance, except for the 20 ms^{-1} case where a larger tip gap was shown to give better performance.

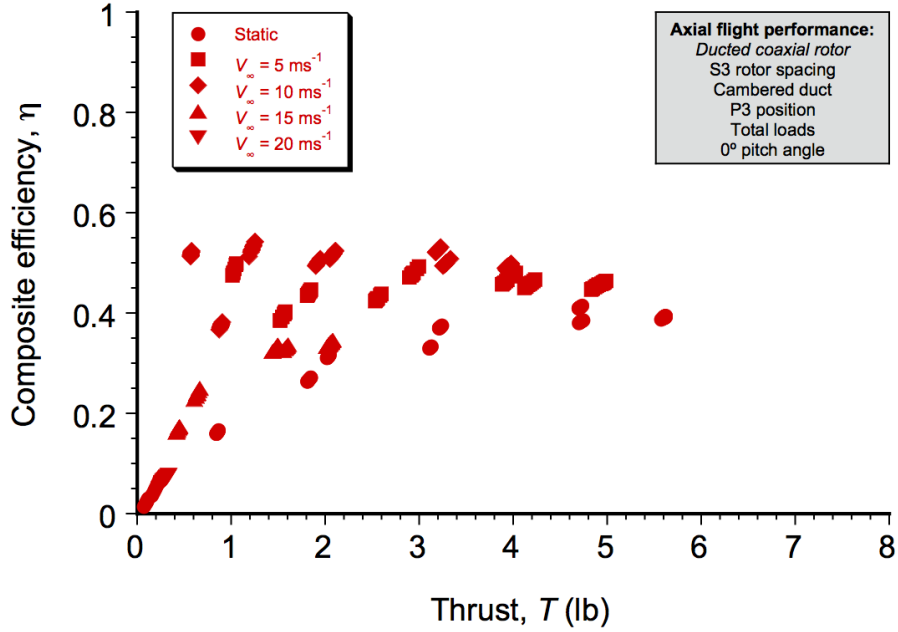


(a) S1 spacing, cambered duct, P1 position.



(b) S1 spacing, cambered duct, P3 position.

Figure 4.25: Axial flight performance of the ducted coaxial rotor, shown in terms of composite efficiency: (a) S1/C/P1, (b) S1/C/P3, (c) S3/C/P3.



(c) S3 spacing, cambered duct, P3 position.

Figure 4.25: Axial flight performance of the ducted coaxial rotor, shown in terms of composite efficiency: (a) S1/C/P1, (b) S1/C/P3, (c) S3/C/P3.

4.4 Edgewise Forward Flight Performance

4.4.1 Introduction

Edgewise flight testing (pitch angle at or near 90°) was only conducted for ducted configurations, as the rotors were potentially shielded from the edgewise component of the incoming wind, which limited the unsteady aerodynamic loading on the blades. For the ducted single rotor configurations, the system performance was compared against the rotor/duct position (P1 or P3), and the influence of tip clearance was investigated. The ducted coaxial rotor tests in edgewise flight consisted of variation in rotor spacing and rotor/duct position. In addition, the variation in duct shape was also studied. The perfor-

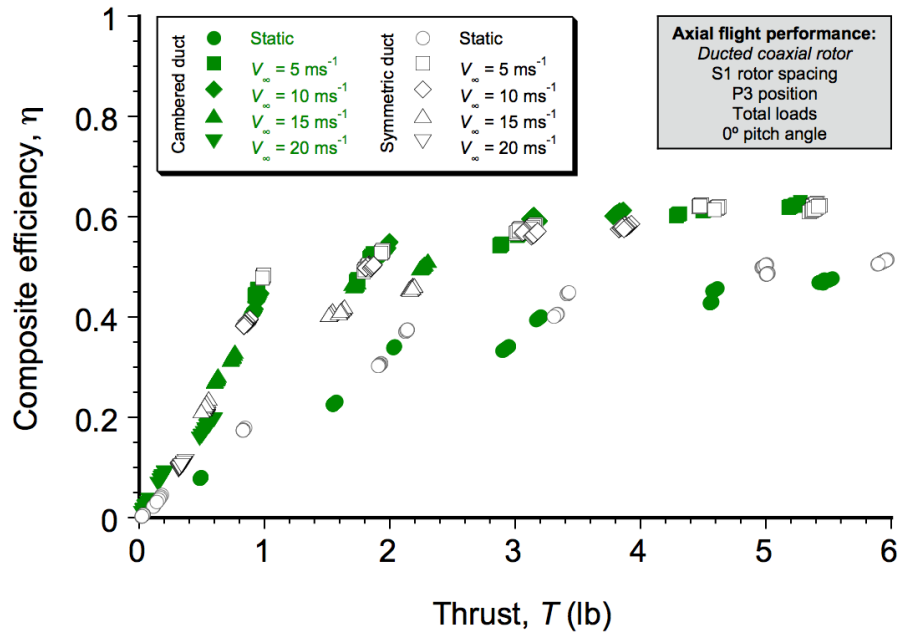


Figure 4.26: Axial flight performance of the S1/P3 ducted coaxial rotor with varying duct shape, shown in terms of composite efficiency.

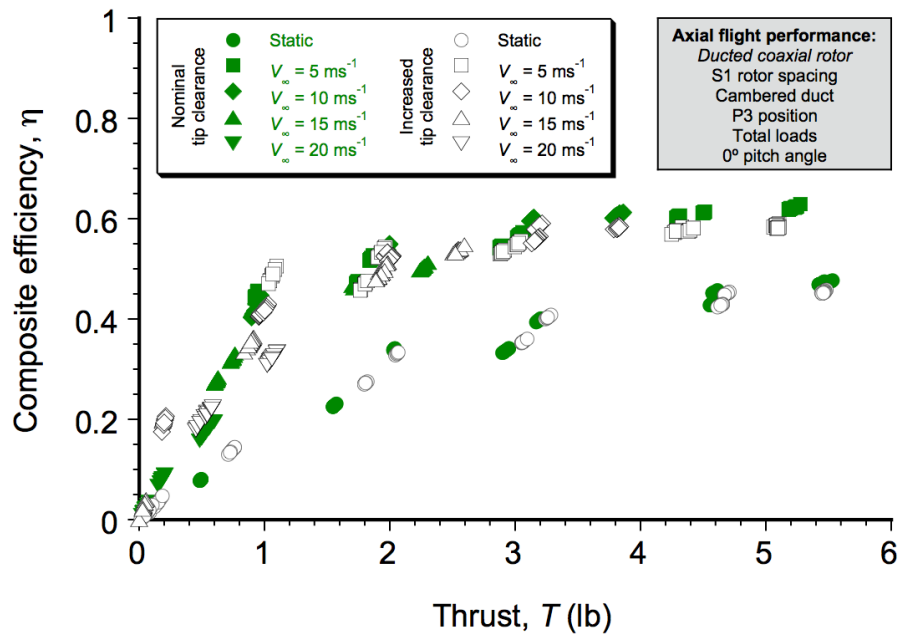


Figure 4.27: Axial flight performance of the S1/C/P3 ducted coaxial rotor with varying tip clearance, shown in terms of composite efficiency.

mance at intermediate pitch angles (i.e., pitched increasingly forward into the incoming flow) was measured. The effects of blade tip clearance on the loads of the rotor and duct was also investigated.

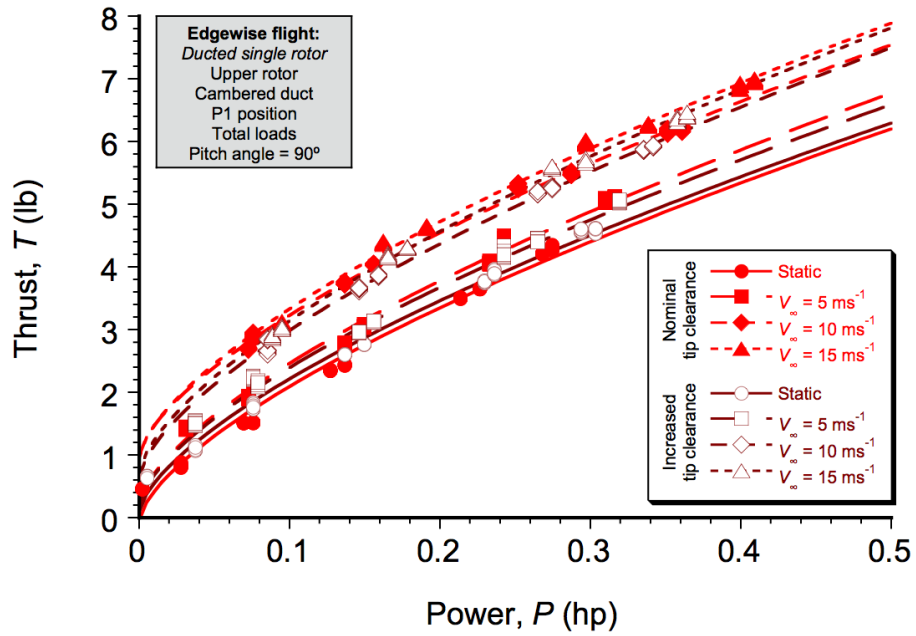
4.4.2 Ducted Single Rotor Performance in Edgewise Flight

Figure 4.28 shows the edgewise flight performance of the ducted single rotor at rotor/duct positions of P1 and P3 with the cambered duct installed. As the forward flight speed was increased, the thrust produced by the system for a given power quickly increased. Likewise, the power requirements were decreased to achieve a given level of thrust at higher free-stream velocities. This behavior was characteristic of most rotors in edgewise flow, where the power requirements decrease with increasing airspeed because of a reduction in induced losses. Of the two rotor/duct positions, the P3 position provided slightly greater performance.

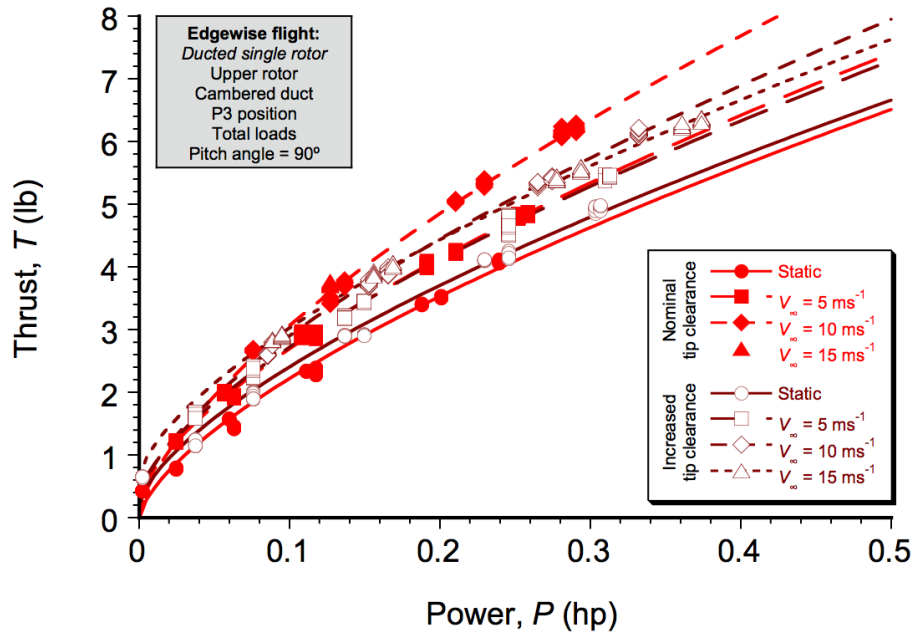
The effects of variations in blade tip clearance is also shown in Fig. 4.28. Interestingly, a small performance improvement was noted in the static condition for both the P1 and P3 positions. This improvement was lost as the system was exposed to edgewise flow, where a smaller tip gap gave better levels of performance.

4.4.3 Ducted Coaxial Rotor Performance in Edgewise Flight

The edgewise flight performance of the ducted coaxial rotor configurations is presented in Fig. 4.29. The general characteristics for the ducted single rotor were also observed here, i.e., thrust produced for a given power increased as edgewise flight speed



(a) P1 rotor/duct position ($p/c_d = 0.10$).



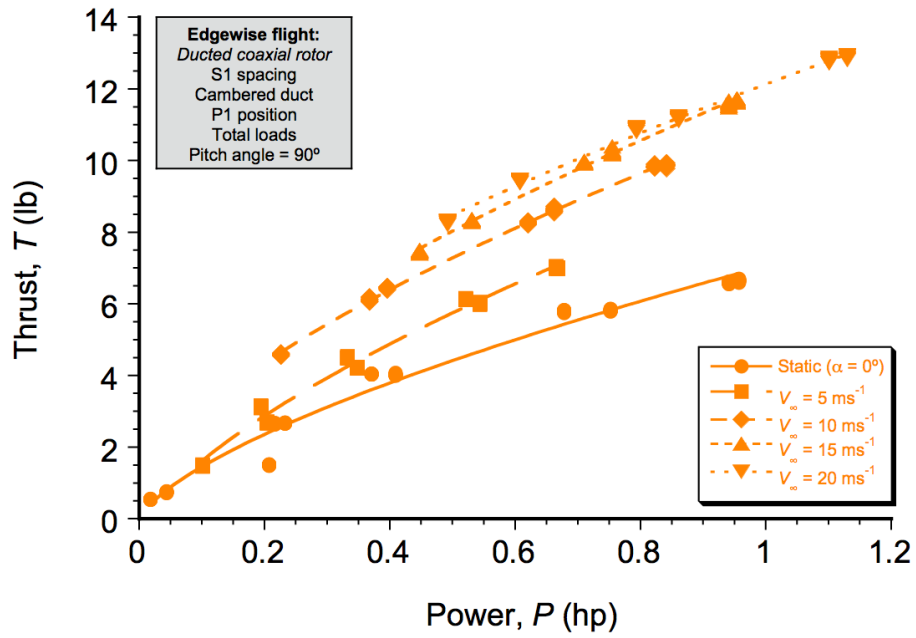
(b) P3 rotor/duct position ($p/c_d = 0.67$).

Figure 4.28: Edgewise flight performance of the ducted single rotor for (a) rotor/duct position P1 and (b) position P3. Variation with blade tip clearance also noted.

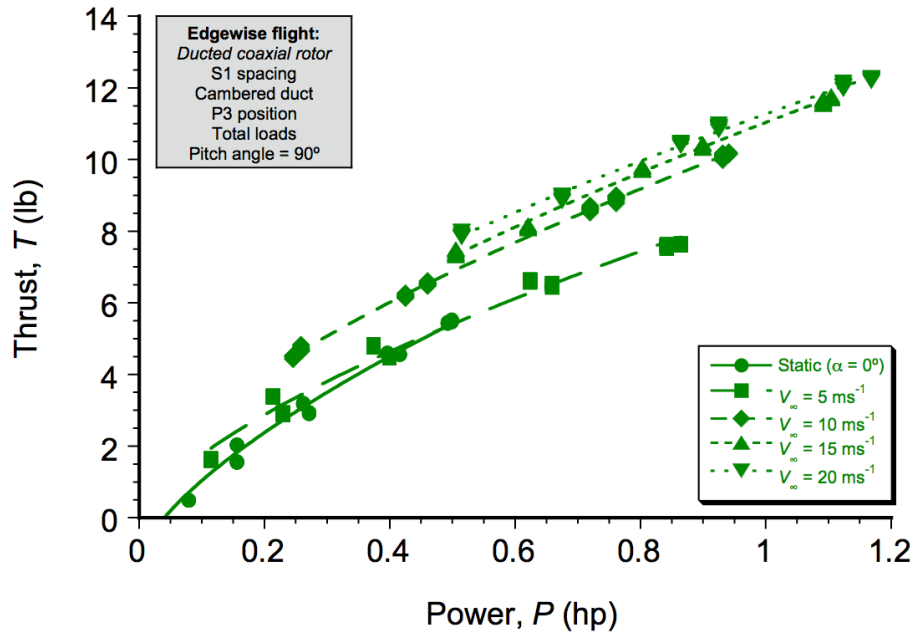
increased. Note that the relative performance gains decreased as forward velocity increased. This behavior was evident when comparing the power required to achieve a particular level of thrust, as shown in Fig. 4.30. Clearly, there was some performance sensitivity between these two configurations in hover and low edgewise flight speeds, where the power requirements were much greater when placing the coaxial rotor further inside the duct (at the P3 position). However, for high forward flight speeds the power requirements became less sensitive to the position of the duct. Because the duct shields the rotors from the incoming flow (to a degree), the profile power requirements should be relatively insensitive, but not immune, to the effects of forward speed.

A sensitivity to duct shape was evident in the results shown in Fig. 4.31 for the S1 rotor spacing and P3 rotor/duct position. The symmetric duct performed significantly better than the cambered duct, which was consistent with measurements in hover for the ducted single and ducted coaxial rotors (see Figs. 4.5 and 4.17). The relative performance improvement was greater as the free-stream velocity was increased.

Performance measurements were also obtained at intermediate shaft pitch angles; Fig. 4.32 shows representative measurements for pitch angles of 75° and 60° for the S1/C/P1 configuration, respectively. As the system was pitched forward, the thrust produced for a given power diminished quickly; a reduction of 20% relative to the vertical case occurred when pitched forward to only 75° . For the 60° shaft angle case, the power polars for increasing wind speed were nearly identical. Notice that in these intermediate pitch angles, the balance was also measuring components of the duct forces that were parallel to the axis of the balance, but these components were small relative to the rotor forces (see later).



(a) S1 rotor spacing, P1 rotor position.



(b) S1 rotor spacing, S3 rotor position.

Figure 4.29: Edgewise flight performance of the ducted single rotor for (a) S1 rotor spacing and P1 rotor position and (b) S1 rotor spacing and P3 rotor position.

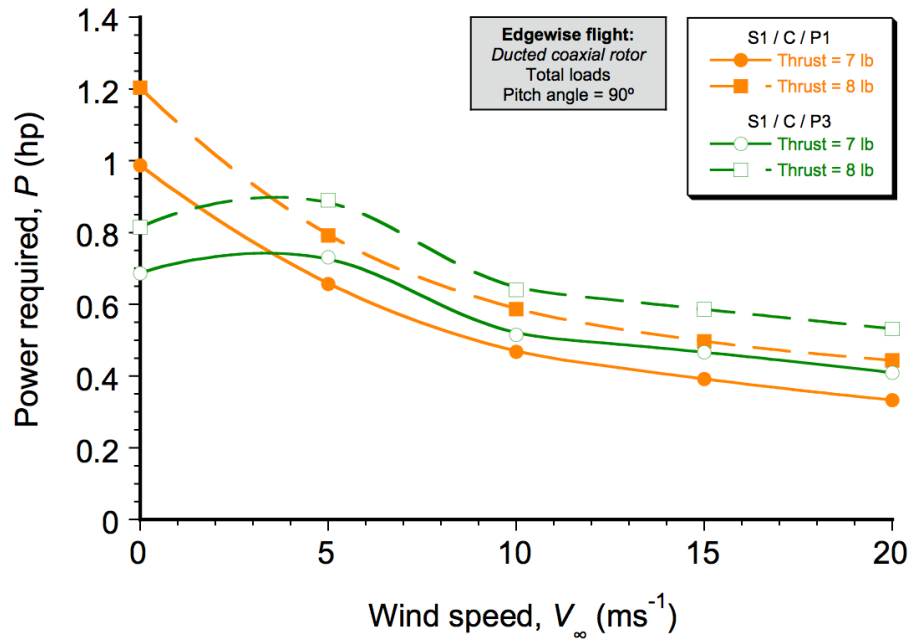


Figure 4.30: Edgewise flight performance of the ducted coaxial rotor for a constant vertical force.

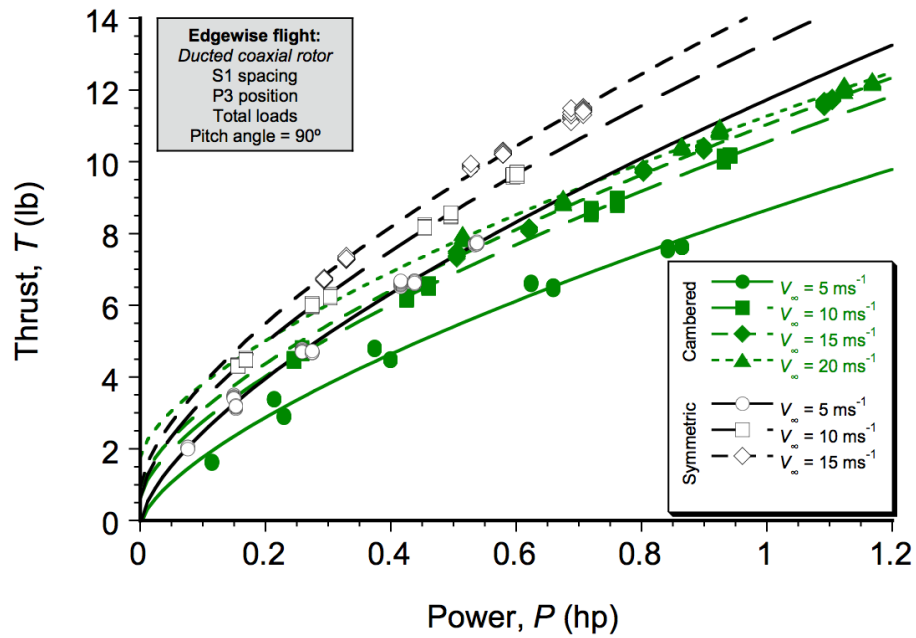
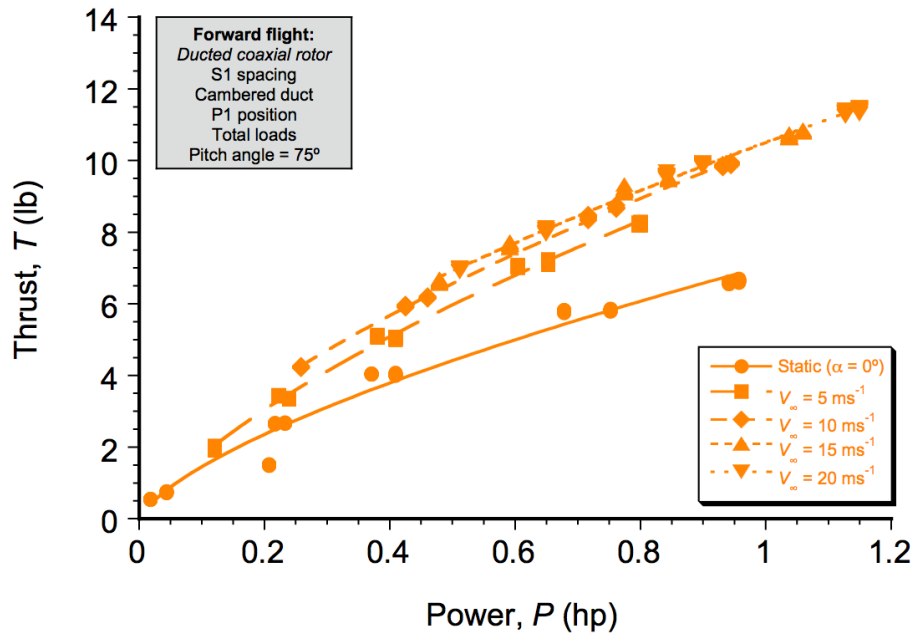
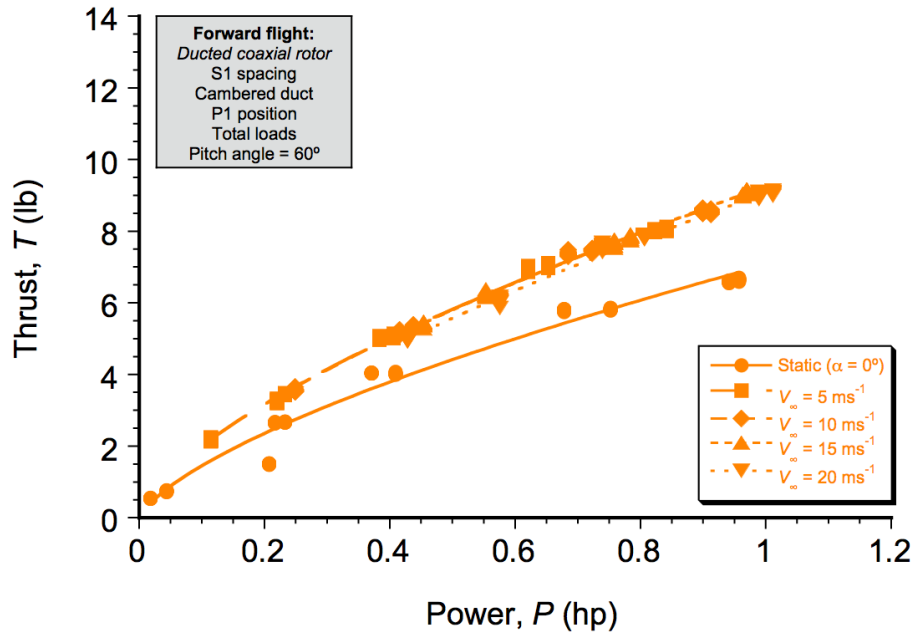


Figure 4.31: Edgewise flight performance of the ducted coaxial rotor, with variation in duct shape.



(a) Stand pitch angle: $\alpha = 75^\circ$



(b) Stand pitch angle: $\alpha = 60^\circ$

Figure 4.32: Edgewise flight performance of the ducted coaxial rotor at various stand pitch angles: (a) $\alpha = 75^\circ$ (15° pitched forward), (b) $\alpha = 60^\circ$ (30° pitched forward).

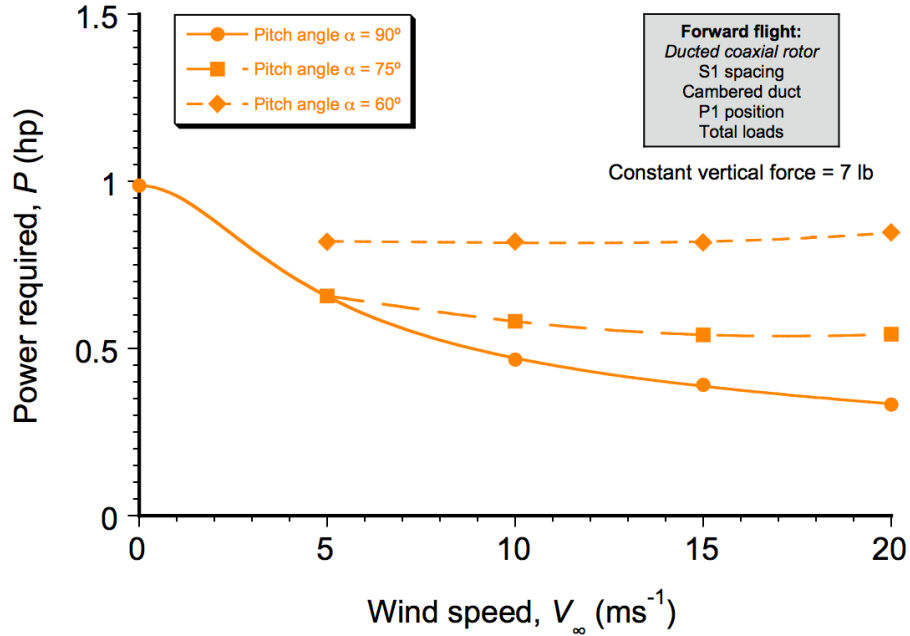


Figure 4.33: Power required to achieve a constant vertical force in edgewise flight for various shaft angles.

The power required to achieve a constant vertical force at shaft angles of vertical (90°), 75° , and 60° is shown in Fig. 4.33 for the S1/C/P1 configuration. The power requirements were increased as the shaft angle was pitched increasingly forward. This outcome was expected because of the increasingly large axial component of the incoming flow through the rotor as the system was pitched forward. Thrust was reduced at the blade element level, and an increase in power must occur to compensate and maintain the desired vertical force. Notice that as the system did no work to overcome parasitic drag, these measurements did not include the contribution of parasitic power.

The loads of the system were decomposed into their constituent parts (total loads and rotor-only loads) in Fig. 4.34. The configuration in this case for the ducted coaxial rotor was the S1 spacing, cambered duct, and P3 rotor position. Interestingly, in

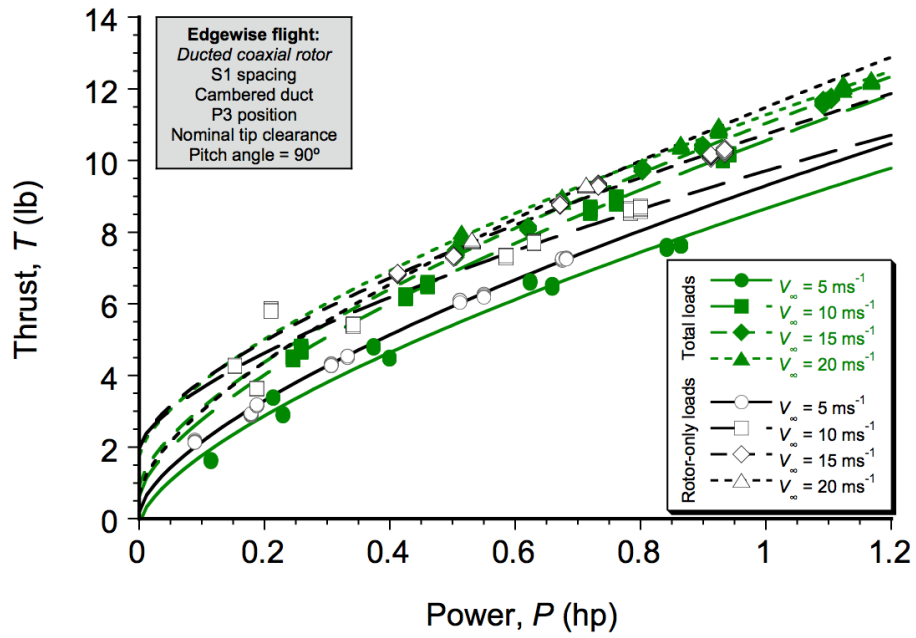
Fig. 4.34(a) the rotor loads were found to be greater than the system loads in low forward flight speeds, indicating that aerodynamic interference from the duct in this case caused a serious performance degradation. At higher free-stream velocities, the adverse effect of duct interference decreased. However, when the tip clearance was increased, the system loads were instead greater than the rotor-only loads, and the duct increased system performance, instead of decreasing it; see Fig. 4.34(b). The aerodynamic interference with the duct that was present with the nominal tip clearance was alleviated when the blade tip gap was increased. The performance of the system and rotor also appear to be more sensitive to increasing edgewise flight speed than the case with the nominal tip clearance.

Comparing the system loads of the nominal and increased tip clearance, as shown in Fig. 4.35(a), makes clear the system improvement that occurred when increasing the tip clearance. Figure 4.35(b) suggests that the rotor performance was degraded when the tip clearance was reduced, which was the expected behavior. In this case, the power requirements to produce a given thrust were increased by 20%, and a 10% decrease in attainable thrust was expected for a given power.

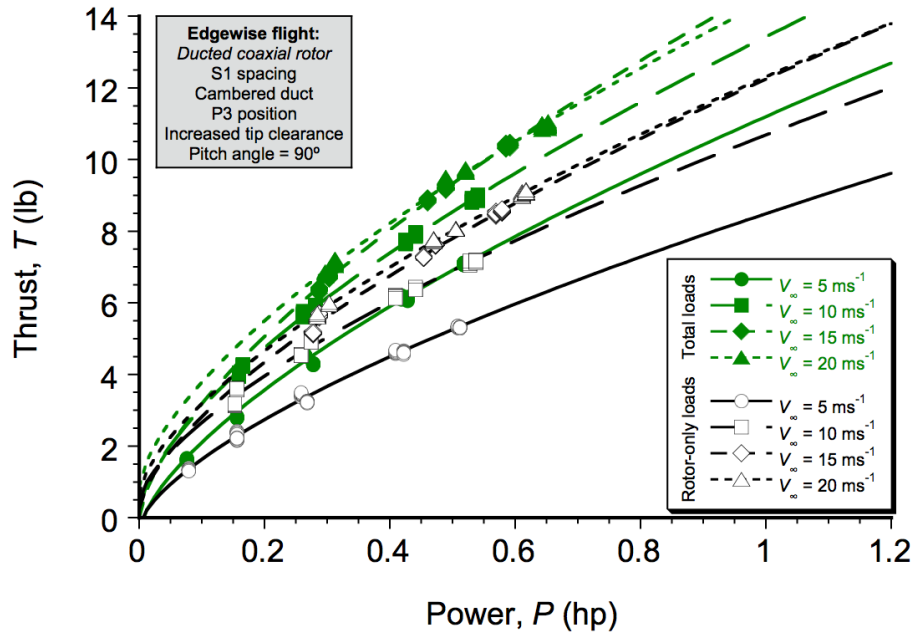
4.5 Isolated Duct Testing: Loads Measurement and Flow Visualization

4.5.1 Introduction

The following section contains the results from isolated duct testing, which consisted of two parts: (1) Measurement of thrust and torque produced by the duct for a parametric sweep in wind speed and pitch angle, and (2) Duct surface flow visualization. The purpose of studying the duct without the rotors was to supplement the ducted single

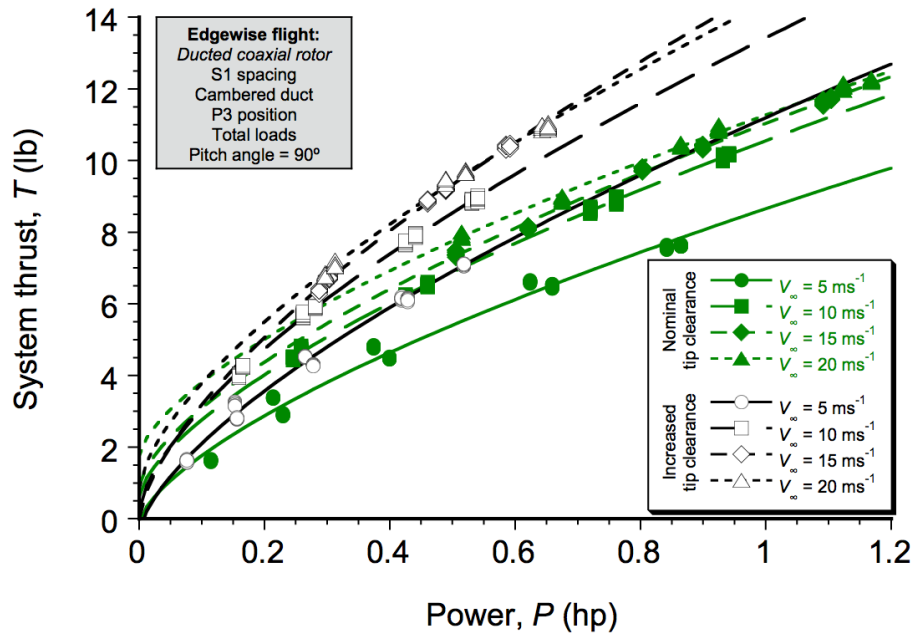


(a) Nominal tip clearance.

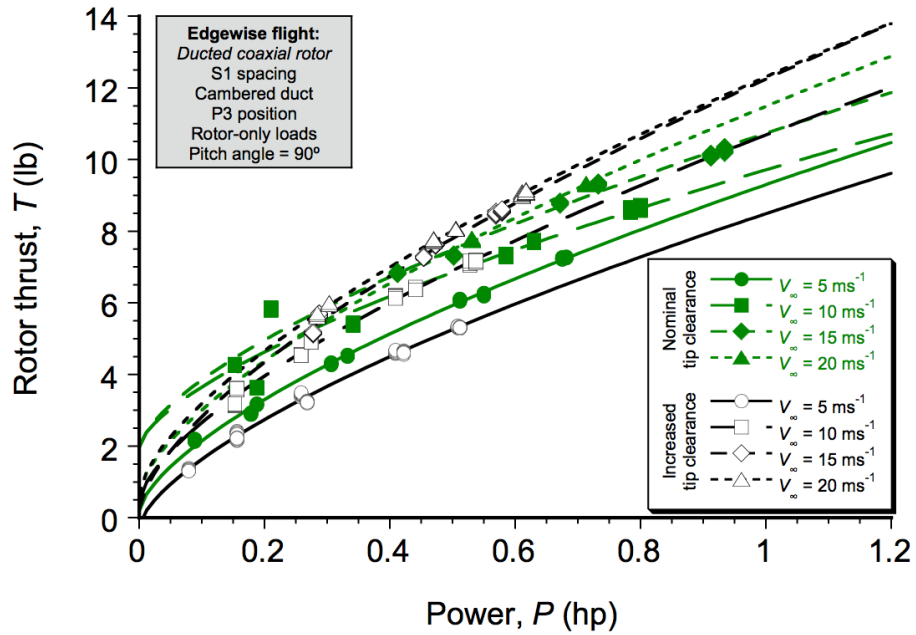


(b) Increased tip clearance.

Figure 4.34: Total and rotor-only loads of S1/P3 ducted coaxial rotor in edgewise flight for varying tip clearance: (a) nominal tip clearance, (b) increased tip clearance.



(a) System performance.



(b) Rotor performance.

Figure 4.35: Edgewise flight performance of the S1/C/P3 ducted coaxial rotor with varying tip clearance: (a) total performance and (b) rotor performance.

and ducted coaxial rotor performance measurements, and to assist in validation of numerical simulation models of the isolated duct (e.g., for computational fluid dynamics) as a first step to simulating the aerodynamics of the more complex ducted rotor system, noting however that the rotor itself affects the aerodynamics of the duct. Flow visualization was not conducted while the rotors were operating in the duct because the coaxial motor and test equipment could not be entirely protected from the oil mixture when the motors were powered.

Both duct shapes, cambered and symmetric, were investigated. An interchangeable inlet with the appropriate geometry, as explained in Chapter 2, set the shape of each duct.

4.5.2 Isolated Duct Loads

To obtain duct-only loads, the balance configuration was set to total loads measurement, so the duct load path passed through the load cell (see Section 2.5.2.1). The rotors and coaxial motor were removed from the assembly, and therefore the balance only sensed loads on the duct (and duct structural assembly). The testing procedure consisted of a parametric sweep in wind speed from static conditions to 30 ms^{-1} for the full range of shaft angles, which was 0° to 100° (horizontal to slightly beyond vertical). The maximum wind speed was higher for duct testing than rotor testing, as the upper wind speed limit was a rotor-specific constraint.

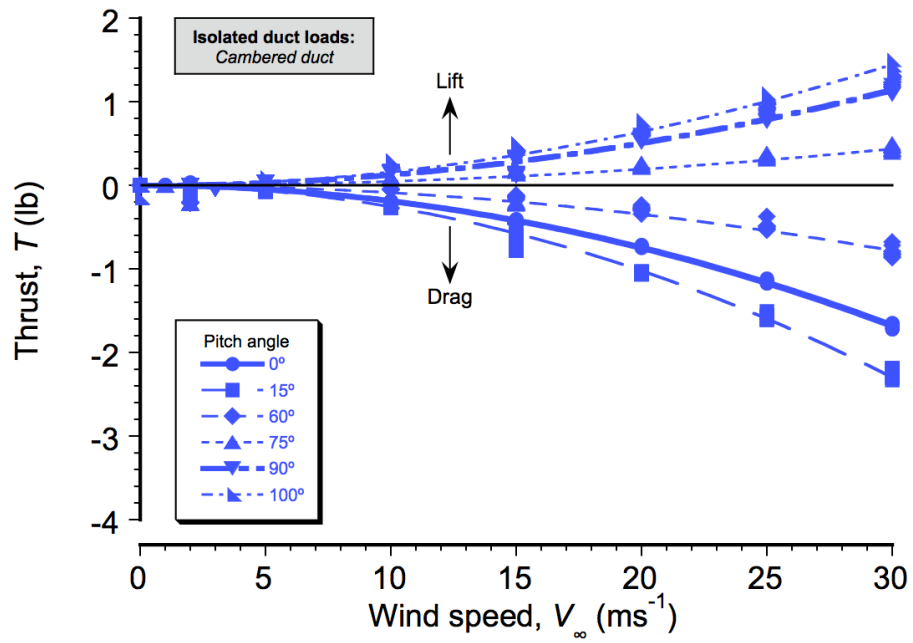
It should be noted that the balance sensed the residual force along the axis of the duct, implying that the measured load was the resultant force of the components of duct lift and drag that were parallel to this axis (the other components were extraneous and

were not sensed by the balance). In propeller mode, the balance measured the drag on the duct; when the duct was vertical, the balance measured duct lift. This result occurred because lift and drag are defined in the wind reference frame, which did not change, yet the measurement of loads was made in the body (duct) reference frame, which varied with the pitch angle. The duct torque, however, was a body reference frame load so changing the shaft angle had no effect on this particular measurement.

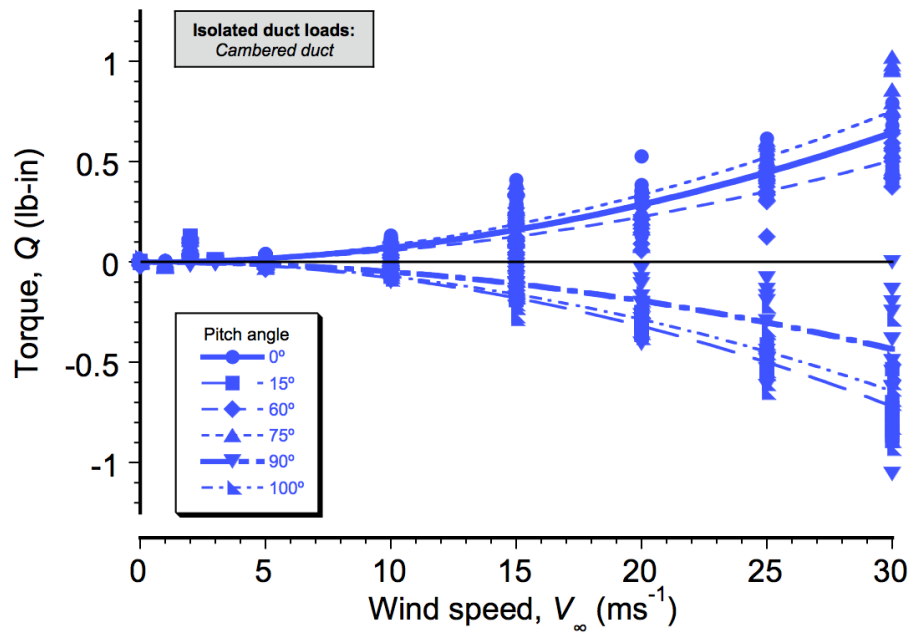
The cambered duct loads are presented in Fig. 4.36. The same loads are presented for the symmetric duct in Figs. 4.37. In all cases, the loads proportionally increased with dynamic pressure $q = \frac{1}{2}\rho V_\infty^2$, which was expected. Figures 4.36(a) and 4.37(a) indicate that the cambered duct produced slightly less lift than the symmetric inlet in edgewise flight and significantly less drag in axial climb. The somewhat modest duct lift (approximately 10% of the duct weight at 30 ms^{-1}) was caused by a chordwise pressure gradient, which was likely strongest at the duct inlet.

Surprisingly, each duct produced a small torque that seemed only somewhat sensitive to the duct inlet. Other sources of torque may exist aside from duct forces at the inlet, such as the duct trailing edge or other duct structural assembly components. Significant spread was observed in the torque measurements, especially at the vertical orientation. It is suggested, then, that this torque was caused, at least in part, by unsteady flow separation around the duct. Indeed, the vertical orientation should have more torque scatter; greater flow separation was expected to occur when the duct was vertical, where the incident duct cross-section was much less streamlined than in the horizontal, propeller mode case.

Notice that, because the measurement frame of reference changed relative to the wind, the measured duct loads at angles other than horizontal and vertical had components

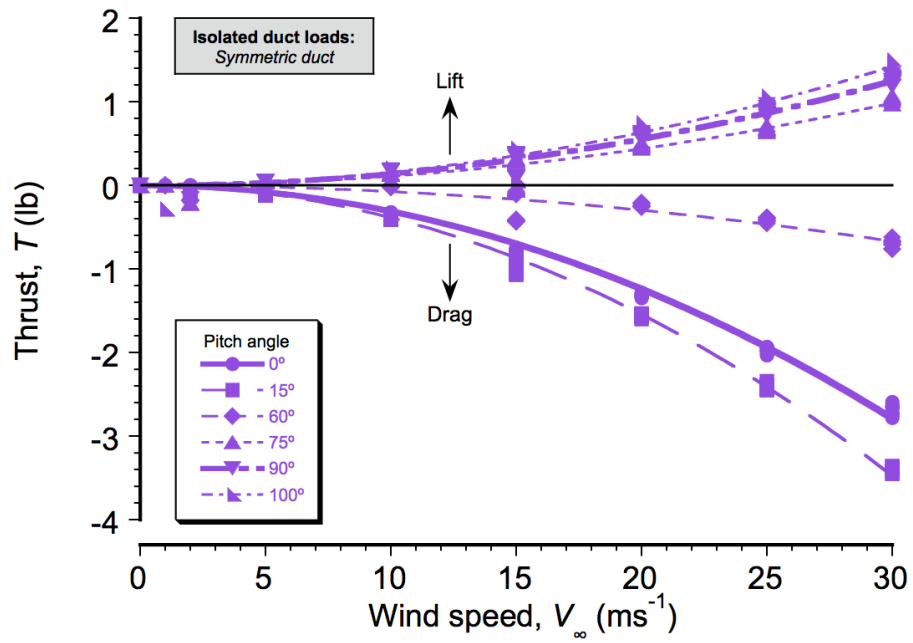


(a) Duct lift and drag.

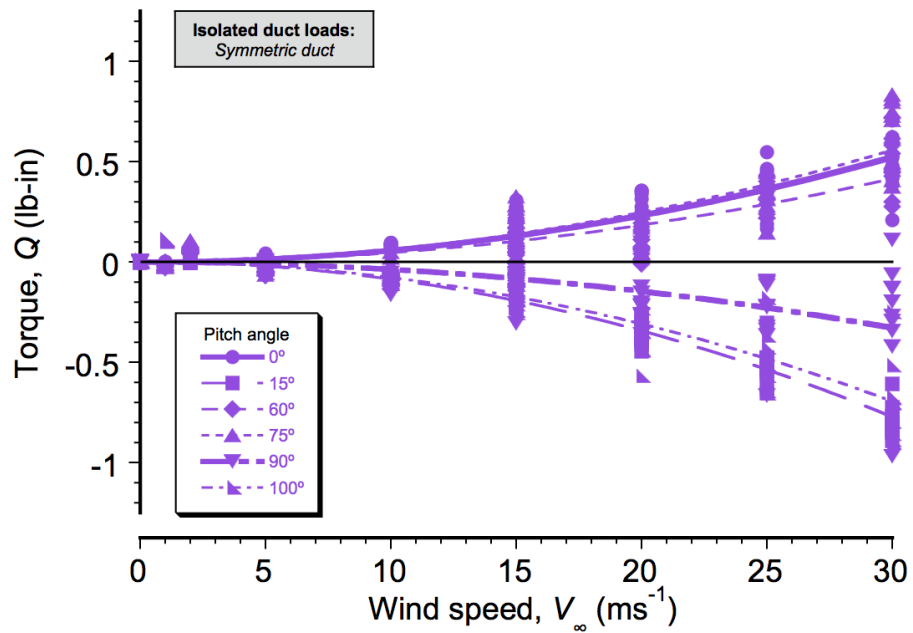


(b) Duct torque.

Figure 4.36: Loads of the cambered duct in isolation: (a) thrust (lift/drag) and (b) torque.



(a) Duct lift and drag.



(b) Duct torque.

Figure 4.37: Loads of the symmetric duct in isolation: (a) thrust (lift/drag) and (b) torque.

of both lift and drag in the thrust measurement. For example, the thrust at 100° shaft angle was measured to be greater than at 90° ; in this case, a small component of duct drag was sensed by the balance.

To assess the lift and drag characteristics of each duct shape independent of dynamic pressure q , the loads were nondimensionalized to the standard lift and drag coefficients, i.e.,

$$C_L = \frac{L}{qA}$$

$$C_D = \frac{D}{qA}$$

The area A used in the calculation of these parameters was the area of the cross-sectional duct annulus with an inner radius of $1.01R$ and an outer radius of $1.01R + t_d$. These coefficients were calculated for every data point above 10 ms^{-1} to obtain a single value. The wind tunnel speed was not entirely stable below 10 ms^{-1} , so these measurements were not included in this average. The lift coefficient of the cambered duct was calculated as 0.34, whereas this value was 0.38 for the symmetric duct. The difference in the drag coefficients, however, was significantly greater: 0.45 for the cambered duct versus 0.78 for the symmetric duct.

The relative values of the lift and drag coefficients agree with the performance measurements of the ducted single and ducted coaxial rotor. Ducted rotor systems with the symmetric duct were shown to produce greater thrust for a given power in hover and edgewise flight than for the cambered duct, which clearly was a result of the better lifting capability of this inlet shape (higher lift coefficient). In axial flight, the cambered duct was shown to better improve system performance of the ducted coaxial rotor, which in-

dicates that the system must overcome less drag than for the symmetric duct (lower drag coefficient).

4.5.3 Duct Surface Flow Visualization

To investigate the surface aerodynamics of the duct in isolation, oil flow visualization was performed for both the cambered and symmetric duct shapes for the usual range of shaft angles between pitch angles of 0° and 100° . This qualitative investigation complemented the quantitative measurement of isolated duct loads. A mineral oil and titanium dioxide mixture was painted onto the duct prior to increasing the wind speed to 30 ms^{-1} . This speed was held for several minutes. The mixture collected in localized areas of low surface shear, leaving behind visible surface flow patterns. Areas that were partially covered with the mixture, or not covered at all, indicated high local skin friction, e.g., an attached boundary layer, which caused the mixture to be shed from the duct surface.

Shown in Fig. 4.38 is the flow visualization of both duct shapes, set to a 15° shaft angle. Complex flow patterns were observed in each case, suggesting that the duct aerodynamics were three-dimensional in nature, even for a seemingly simple system as an isolated duct in forward flight. Flow attachment was seen around the inlet of both ducts, and the localized pool of mixture further chordwise on the duct exterior surface suggests that flow separation immediately occurred after the inlet in some areas. However, in other areas, the boundary layer remained attached over the outer surface of the duct. The surface flow patterns also suggest that more flow separation occurred on the symmetric duct.

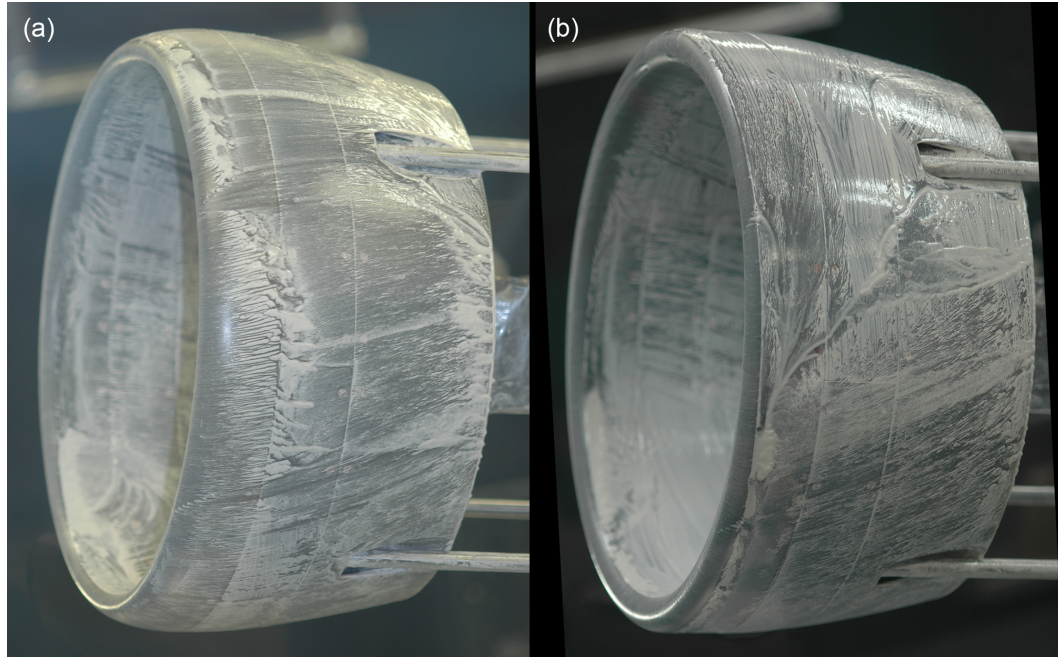


Figure 4.38: Oil flow visualization of the (a) cambered and (b) symmetric duct shapes, both at 15° shaft angle.

In the duct interior, the patterns suggest that flow separation occurred over the interior lip at this pitch angle (15°) for both ducts. More reattachment was observed for the cambered duct than for the symmetric duct.

4.6 Summary of Experimental Results

It has been shown that the test parameters that were examined in these experiments (i.e., rotor/duct position, duct type, tip clearance, and for coaxial systems the rotor spacing) all affected performance to varying degrees. While ducting a single rotor was shown to almost always improve system performance, only in one configuration (i.e., the S1 rotor spacing, cambered duct, P3 position) was the ducted coaxial rotor shown to have better performance than the isolated coaxial rotor.

For ducted single systems in hover, rotor positions closest to the duct inlet have been observed to be more favorable, most likely a consequence higher suction pressures induced by the rotors and less influence from the impingement of the duct wall boundary layer that occurred at positions further in the duct. For both the ducted single and ducted coaxial systems it has been observed that the symmetric duct inlet (which featured a larger leading edge radius) improved system performance in hover over that than the cambered inlet. Increasing tip clearance, in general, has been shown to degrade performance, but in some cases improvements in performance were obtained.

In axial flight, it has been shown that ducting the single rotors gave then better propulsive efficiency, with no apparent sensitivity to rotor position within the duct. A small decrease in hover efficiency was noted when the coaxial rotor was ducted, but the propulsive efficiency of the ducted coaxial rotor was slightly better than that of the isolated coaxial rotor. Measurements in axial flight have also shown that the symmetric duct gives less favorable effects on performance.

In edgewise forward flight, it has been observed that the sensitivity to rotor position for the ducted single rotor systems is greater than for axial flight, and that operating the rotors further down inside the duct gave greater levels of thrust for the same power. It has been noted that performance is improved with greater blade tip clearances in the coaxial rotor system, which suggests there may be substantial aerodynamic interference effects between the duct and the rotor system.

The present results are mostly consistent with those seen previously with single rotors operated in a duct, i.e., a gain in thrust for a given power, mainly because of the additional forces produced on the duct and the improved performance of the rotor within

the duct. For ducted coaxial rotor configurations, performance was not always improved. Clearly, the reasons for the sensitivity to the ducted coaxial system to test parameters cannot be fully established from performance measurements alone. While it can be appreciated that a coaxial rotor operating within a duct encounters a more complex flow, further types of measurements will be required to fully understand the issues.

Chapter 5

Concluding Remarks

5.1 Conclusions

Experiments were conducted to measure the performance of a ducted coaxial rotor system at micro air vehicle (MAV) scale. Several test parameters, such as rotor/duct position, rotor spacing, duct shape, and tip clearance, were varied to determine the sensitivity of the performance of the ducted coaxial rotor system to these parameters. Comparisons were made (where possible) to the performance of the isolated coaxial rotor to determine the effects of operating a coaxial rotor within a duct at this small scale. The performance of each of the isolated single rotors of the coaxial rotor was also obtained, as well as their performance when contained within the duct. Measurements were obtained as a function of rotor speed (rotational speed) in hover, axial flight, and edgewise forward flight.

The following conclusions have been drawn from the experiments that were conducted in this research:

1. The performance of the ducted coaxial rotor was not always improved over that of the isolated coaxial rotor. However, for the ducted coaxial rotor with S1 ($0.15R$) rotor-to-rotor spacing and the P3 rotor position (i.e., positioning the rotors further down inside the duct), the system performance was slightly improved over that of the isolated coaxial rotor when it was operated at higher levels of thrust. This outcome was different to that seen with the ducted single rotors, which almost always

exceeded performance levels of the isolated single rotor (i.e., they produced more thrust for a given power and higher power loading). The ducted coaxial rotor system also had lower levels of power loading than for the ducted single rotor system. Nonetheless, the ducted coaxial rotor concept is still attractive on the basis of other factors, such as available thrust for a given vehicle size, vehicle ruggedness, operator safety, the elimination of a separate anti-torque system, and also in terms of overall efficiency relative to other competing vehicles at MAV scale.

2. For the hovering ducted single rotor, the rotor positions closest to the duct inlet were found to be the most beneficial. However, the highest levels of performance from the ducted coaxial rotor were achieved when the rotors were operated further down within the duct. Rotor position within the duct had a stronger influence on system performance than rotor spacing (i.e., the spacing between the upper and lower rotors), with the best performance being obtained when using smaller rotor spacing. For all ducted rotor systems, the symmetric duct (which had a larger leading edge radius) gave improved hovering performance compared to the cambered duct. In general, increasing the blade tip clearance to the duct (i.e., tip gap) reduced system performance, but in certain conditions a larger tip gap actually improved performance. This latter effect was believed to be associated with the less adverse aerodynamic interference of the duct wall boundary layer on the aerodynamics of the blade tip region, which improved rotor performance. The ducted coaxial rotor configuration with the best overall performance was with the S1 spacing, the symmetric duct, and the P3 rotor position.

3. In axial flight, the duct improved the propulsive efficiency of both the single rotor and coaxial rotors. For the ducted single rotor, the axial flight performance was insensitive to the position of the rotor within the duct. For the ducted coaxial rotor, the trends of rotor spacing and rotor position measured in hover were essentially the same as those seen in for flight; the ducted coaxial rotor with S1 spacing and P3 position showed a slightly better axial flight performance than the isolated coaxial rotor, whereas other configurations (i.e., S1/P1 and S3/P3) gave a lower performance than for the isolated case. The cambered duct was shown to provide better levels of performance than the symmetric duct because it produced less drag, requiring less power from the system to achieve a certain thrust. In general, a smaller blade tip clearance was preferred, at least until higher free-stream velocities were reached.
4. In edgewise forward flight, the ducted single rotor produced greater thrust for a given power with rotor positions further down inside the duct. Increasing the tip gap was shown to generally decrease system performance. For the ducted coaxial rotor, operating the rotors at the inlet produced greater thrust for a certain power. Significant levels of aerodynamic interference between the rotor system and duct were found in edgewise flight for the ducted coaxial rotor. It was found that increasing the blade tip clearance generally alleviated this interference. Again, this observation suggests that the duct wall boundary layer was most likely influencing the rotor performance at the blade tip region.
5. While flow visualization with the rotors operating was not performed, surface oil

flow visualization of the isolated duct showed regions of local flow separation and three-dimensional laminar separation bubbles around the inlet to the duct. This observation suggests that the flow conditions at the duct inlet were rather complex, which will have effects on rotor performance. Larger regions of flow separation over the interior region of the duct was evident when using the symmetric leading edge, which was also found to produce greater drag than for the duct with the cambered leading edge.

6. Overall, the results obtained during this research suggested that the effects of the various design parameters on the performance of the ducted single rotors and ducted coaxial rotors are different, and not always consistent with expected behavior. Future work must be performed to more precisely identify the mechanisms that are responsible.

5.2 Suggestions for Future Work

The results obtained in the present work expose several important characteristics about ducted coaxial rotors at MAV scale. Several suggestions are offered for improved future research, including: (1) An alternative coaxial rotor system with greater solidity, as well as using blades that are stronger and more robust, (2) Independent measurements of thrust and torque, (3) Measurement of the pitching moment in edgewise flight, (4) Surface pressure measurements, and (5) Flow field (i.e., off-surface) measurements.

5.2.1 Improved Coaxial Rotor

The rotors using in the present experiments were downsized versions of an existing rotor design. However, they were not optimum for the use in this experiment. Specifically, the relatively low solidity of the rotors resulted in lower values of thrust that would be desired for a MAV of this scale. Furthermore, because of the small blade chord, there were clearly low Reynolds number effects present in the results, which led to lower levels of performance than what otherwise might have been obtained. Redesigning the blades and rotor system to match the duct and power available from the motors is recommended in any future work with this rig.

The small chords of the blades also led to other issues, primarily a lack of blade stiffness and overall robustness. While the blades of the present coaxial rotor were structurally sufficient for axisymmetric flow conditions, the rotor bending stiffness would have to be increased to expand the envelope in forward flight. Increasing the bending stiffness would decrease the bending stresses caused by cyclic loading in edgewise flow conditions, increase the number of cycles before fatigue failure, hence improving the longevity of the rotors. Manufacturing the rotors from a stronger material (such as a stronger rapid prototyping material, or out of polycarbonate, or making the rotors through a carbon fiber layup procedure) would address this concern. As an alternative method of improving flexure strength using the existing rotors, Kevlar sleeves could be applied using a vacuum bagging procedure. An example of such a sleeve is shown in Fig. 5.1. This exploratory attempt was found to significantly improve blade stiffness.



Figure 5.1: A Kevlar sleeve on each rotor blade may increase rotor flexure strength on existing (or new) rotor sets.

5.2.2 Independent Measurement of Rotor Performance

While measuring the net thrust and torque of the coaxial rotor system offers advantages in terms of lower instrumentation complexity, the net power in this case can only be estimated using a relationship between the electric power and aerodynamic power. While using this approach was shown to be satisfactory for the present experiments, measuring the thrust and torque independently on each rotor is obviously a much more desirable option. In this case, the thrust sharing between the upper and lower rotors of the coaxial would be known, and the separate power requirements for the upper and lower rotors could also be obtained directly. However, altering the coaxial motor in the present experiments to obtain independent torque measurements is not a simple task. In this case, the upper and lower rotors cannot be coupled to each other in any way so the load path for each remains completely isolated. Two load cells (one for each rotor) will also be needed.

5.2.3 Measurement of Pitching Moment in Edgewise Flight

In edgewise forward flight, ducted rotor systems are known to experience adverse pitching moments, which can make the control of such MAVs challenging. While measurements of thrust, torque, and power were sufficient to identify those design parameters that provided the best performance in hover and axial flight, measurement of the pitching moment in edgewise flight would more completely expose the effect of these parameters in this flight regime. For example, Martin and Tung [18] observed that a duct with a less rounded (smaller leading edge radius) served to reduce this type of pitching moment.

5.2.4 Surface Pressure Measurements and Flow Field Measurements

The experimental results suggested that in certain operating conditions, significant aerodynamic interference occurred between the duct and the rotor system, e.g., for the ducted coaxial rotor with the S1 spacing, cambered duct, and P1 rotor position in hover. Surface pressure measurements and flow field measurements (such as those obtained from particle image velocimetry) could better explain the aerodynamic interactions in this system and give a more complete understanding of the performance of ducted coaxial rotors. Flow field measurements at the inlet to the duct would also help to further explain the inlet flow conditions and how they ultimately affect the rotor system. To allow some optical access to the interior of the duct when the rotors are operating, the current setup was designed to so that a set of inner and outer duct panels may be removed and replaced with panels that allow for optical access.

Appendix A

Catalog of Laboratory Performance Tests

The performance tests conducted in the laboratory are listed in Table A.1. Table 3.1 explains the shorthand notation used to present the specifications of these tests in an abbreviated fashion.

Table A.1: Catalog of Laboratory Performance Tests.

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measured
Isolated Single Rotor					
1.1	Isolated upper rotor	U	n/a	n/a	R
2.1	Isolated lower rotor	L	n/a	n/a	R
Ducted Single Rotor					
1.2	Ducted upper rotor	U	C & S	P1	T
1.3	Ducted upper rotor	U	C	P2	T
1.4	Ducted upper rotor	U	C	P3	T
1.5	Ducted upper rotor	U	C & S	P4	T
2.2	Ducted lower rotor	L	C & S	P1	T
2.3	Ducted lower rotor	L	C	P2	T
2.4	Ducted lower rotor	L	C	P3	T
2.5	Ducted lower rotor	L	C & S	P4	T
Isolated Coaxial Rotor					
3.1	Isolated coaxial rotor	S1	n/a	n/a	R
3.2	Isolated coaxial rotor	S2	n/a	n/a	R

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Table A.1 — *Continued*

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measured
3_3	Isolated coaxial rotor	S3	n/a	n/a	R
Ducted Coaxial Rotor					
4.1	Ducted coaxial rotor	S1	S	P1	T
4.4	Ducted coaxial rotor	S1	S	P4	T
4.5	Ducted coaxial rotor	S2	S	P1	T
4.8	Ducted coaxial rotor	S2	S	P4	T
4.9	Ducted coaxial rotor	S3	S	P1	T
4_12	Ducted coaxial rotor	S3	S	P4	T
5.1	Ducted coaxial rotor	S1	C	P1	T & R
5.2	Ducted coaxial rotor	S1	C	P2	T & R
5.3	Ducted coaxial rotor	S1	C	P3	T & R
5.4	Ducted coaxial rotor	S1	C	P4	T & R
5.5	Ducted coaxial rotor	S2	C	P1	T & R
5.6	Ducted coaxial rotor	S2	C	P2	T & R
5.7	Ducted coaxial rotor	S2	C	P3	T & R
5.8	Ducted coaxial rotor	S2	C	P4	T & R
5.9	Ducted coaxial rotor	S3	C	P1	T & R
5_10	Ducted coaxial rotor	S3	C	P2	T & R
5_11	Ducted coaxial rotor	S3	C	P3	T & R
5_12	Ducted coaxial rotor	S3	C	P4	T & R

Appendix B

Catalog of Wind Tunnel Performance Tests

The performance tests conducted in the Glenn L. Martin Wind Tunnel are listed in Table B.1. Table 3.1 explains the shorthand notation used to present the specifications of these tests in an abbreviated fashion.

Table B.1: Catalog of Wind Tunnel Performance Tests.

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
Isolated Single Rotor							
1_10	Isolated upper rotor	U	n/a	n/a	R	0	0
1_11	Isolated upper rotor	U	n/a	n/a	R	0	5
1_12	Isolated upper rotor	U	n/a	n/a	R	0	10
1_13	Isolated upper rotor	U	n/a	n/a	R	0	15
1_14	Isolated upper rotor	U	n/a	n/a	R	0	20
2_10	Isolated lower rotor	L	n/a	n/a	R	0	0
2_11	Isolated lower rotor	L	n/a	n/a	R	0	5
2_12	Isolated lower rotor	L	n/a	n/a	R	0	10
2_13	Isolated lower rotor	L	n/a	n/a	R	0	15
2_14	Isolated lower rotor	L	n/a	n/a	R	0	20

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Table B.1 — Continued

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
Ducted Single Rotor							
1_20	Ducted upper rotor	U & U _C	C	P3	T & R	0	0
1_21	Ducted upper rotor	U & U _C	C	P3	T & R	0	5
1_22	Ducted upper rotor	U & U _C	C	P3	T & R	0	10
1_23	Ducted upper rotor	U & U _C	C	P3	T & R	0	15
1_24	Ducted upper rotor	U & U _C	C	P3	T & R	0	20
1_30	Ducted upper rotor	U & U _C	C	P3	T & R	90	0
1_31	Ducted upper rotor	U & U _C	C	P3	T & R	90	5
1_32	Ducted upper rotor	U & U _C	C	P3	T & R	90	10
1_33*	Ducted upper rotor	U & U _C	C	P3	T & R	90	15
1_34†	Ducted upper rotor	U & U _C	C	P3	T & R	90	20
1_40	Ducted upper rotor	U & U _C	C	P1	T & R	0	0
1_41	Ducted upper rotor	U & U _C	C	P1	T & R	0	5
1_42	Ducted upper rotor	U & U _C	C	P1	T & R	0	10
1_43	Ducted upper rotor	U & U _C	C	P1	T & R	0	15
1_44	Ducted upper rotor	U & U _C	C	P1	T & R	0	20

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Table B.1.1 — *Continued*

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
1_50	Ducted upper rotor	U & U _C	C	P1	T & R	90	0
1_51	Ducted upper rotor	U & U _C	C	P1	T & R	90	5
1_52	Ducted upper rotor	U & U _C	C	P1	T & R	90	10
1_53	Ducted upper rotor	U & U _C	C	P1	T & R	90	15
1_54 [†]	Ducted upper rotor	U & U _C	C	P1	T & R	90	20
2_20	Ducted lower rotor	L	C	P1	T	0	0
2_21	Ducted lower rotor	L	C	P1	T	0	5
2_22	Ducted lower rotor	L	C	P1	T	0	10
2_23	Ducted lower rotor	L	C	P1	T	0	15
2_24	Ducted lower rotor	L	C	P1	T	0	20
2_30	Ducted lower rotor	L	C	P1	T	90	0
2_31	Ducted lower rotor	L	C	P1	T	90	5
2_32	Ducted lower rotor	L	C	P1	T	90	10
2_33	Ducted lower rotor	L	C	P1	T	90	15
2_34 [†]	Ducted lower rotor	L	C	P1	T	90	20

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Table B.1 — *Continued*

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
2_40	Ducted lower rotor	L	C	P3	T	0	0
2_41	Ducted lower rotor	L	C	P3	T	0	5
2_42	Ducted lower rotor	L	C	P3	T	0	10
2_43	Ducted lower rotor	L	C	P3	T	0	15
2_44	Ducted lower rotor	L	C	P3	T	0	20
2_50	Ducted lower rotor	L	C	P3	T	90	0
2_51	Ducted lower rotor	L	C	P3	T	90	5
2_52	Ducted lower rotor	L	C	P3	T	90	10
2_53	Ducted lower rotor	L	C	P3	T	90	15
2_54 [†]	Ducted lower rotor	L	C	P3	T	90	20

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Table B.1 — *Continued*

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
Isolated Coaxial Rotor							
3_10	Isolated coaxial rotor	S1	n/a	n/a	R	0	0
3_11	Isolated coaxial rotor	S1	n/a	n/a	R	0	5
3_12	Isolated coaxial rotor	S1	n/a	n/a	R	0	10
3_13	Isolated coaxial rotor	S1	n/a	n/a	R	0	15
3_14	Isolated coaxial rotor	S1	n/a	n/a	R	0	20
3_16	Isolated coaxial rotor	S2	n/a	n/a	R	0	0
3_17	Isolated coaxial rotor	S2	n/a	n/a	R	0	5
3_18	Isolated coaxial rotor	S2	n/a	n/a	R	0	10
3_19	Isolated coaxial rotor	S2	n/a	n/a	R	0	15
3_20	Isolated coaxial rotor	S2	n/a	n/a	R	0	20
3_22	Isolated coaxial rotor	S3	n/a	n/a	R	0	0
3_23	Isolated coaxial rotor	S3	n/a	n/a	R	0	5
3_24	Isolated coaxial rotor	S3	n/a	n/a	R	0	10
3_25	Isolated coaxial rotor	S3	n/a	n/a	R	0	15
3_26	Isolated coaxial rotor	S3	n/a	n/a	R	0	20

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Table B.1 — Continued

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
Ducted Coaxial Rotor							
4_21	Ducted coaxial rotor	S1	S	P3	T	0	0
4_22	Ducted coaxial rotor	S1	S	P3	T	0	5
4_23	Ducted coaxial rotor	S1	S	P3	T	0	10
4_24	Ducted coaxial rotor	S1	S	P3	T	0	15
4_25	Ducted coaxial rotor	S1	S	P3	T	0	20
4_46	Ducted coaxial rotor	S1	S	P3	T	90	5
4_47	Ducted coaxial rotor	S1	S	P3	T	90	10
4_48	Ducted coaxial rotor	S1	S	P3	T	90	15
4_49 [†]	Ducted coaxial rotor	S1	S	P3	T	90	20
5_21	Ducted coaxial rotor	S1 & S1 _C	C	P3	T & R	0	0
5_22	Ducted coaxial rotor	S1 & S1 _C	C	P3	T & R	0	5
5_23	Ducted coaxial rotor	S1 & S1 _C	C	P3	T & R	0	10
5_24	Ducted coaxial rotor	S1 & S1 _C	C	P3	T & R	0	15
5_25	Ducted coaxial rotor	S1 & S1 _C	C	P3	T & R	0	20

Continued on Next Page

Table B.1 — *Continued*

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
5_28	Ducted coaxial rotor	S1	C	P3	T	10	5
5_29	Ducted coaxial rotor	S1	C	P3	T	10	10
5_30	Ducted coaxial rotor	S1	C	P3	T	10	15
5_31	Ducted coaxial rotor	S1	C	P3	T	10	20
5_206	Ducted coaxial rotor	S1	C	P3	T	15	5
5_207	Ducted coaxial rotor	S1	C	P3	T	15	10
5_208	Ducted coaxial rotor	S1	C	P3	T	15	15
5_209	Ducted coaxial rotor	S1	C	P3	T	15	20
5_34	Ducted coaxial rotor	S1	C	P3	T	45	5
5_35	Ducted coaxial rotor	S1	C	P3	T	45	10
5_36	Ducted coaxial rotor	S1	C	P3	T	45	15
5_37	Ducted coaxial rotor	S1	C	P3	T	45	20
5_40 [‡]	Ducted coaxial rotor	S1	C	P3	T	60	5
5_41 [‡]	Ducted coaxial rotor	S1	C	P3	T	60	10
5_42 [‡]	Ducted coaxial rotor	S1	C	P3	T	60	15
5_43 [‡]	Ducted coaxial rotor	S1	C	P3	T	60	20

Continued on Next Page

Table B.1 — Continued

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
5_201 [§]	Ducted coaxial rotor	S1	C	P3	T	60	5
5_202 [‡]	Ducted coaxial rotor	S1	C	P3	T	60	10
5_203 [‡]	Ducted coaxial rotor	S1	C	P3	T	60	15
5_204 [‡]	Ducted coaxial rotor	S1	C	P3	T	60	20
5_46	Ducted coaxial rotor	S1 & S1 _C	C	P3	T & R	90	5
5_47	Ducted coaxial rotor	S1 & S1 _C	C	P3	T & R	90	10
5_48	Ducted coaxial rotor	S1 & S1 _C	C	P3	T & R	90	15
5_49 [¶]	Ducted coaxial rotor	S1 & S1 _C	C	P3	T & R	90	20
5_52	Ducted coaxial rotor	S1	C	P3	T	100	5
5_53	Ducted coaxial rotor	S1	C	P3	T	100	10
5_54	Ducted coaxial rotor	S1	C	P3	T	100	15
5_55	Ducted coaxial rotor	S1	C	P3	T	100	20

Continued on Next Page

Table B.1 — Continued

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
5_210	Ducted coaxial rotor	S1	C	P3	T	90	0
5_211	Ducted coaxial rotor	S1	C	P3	T	90	5
5_212	Ducted coaxial rotor	S1	C	P3	T	90	10
5_213	Ducted coaxial rotor	S1	C	P3	T	90	15
5_214 ^{**}	Ducted coaxial rotor	S1	C	P3	T	90	20
5_58	Ducted coaxial rotor	S3	C	P1	T & R	0	0
5_59	Ducted coaxial rotor	S3	C	P1	T & R	0	5
5_60	Ducted coaxial rotor	S3	C	P1	T & R	0	10
5_61	Ducted coaxial rotor	S3	C	P1	T & R	0	15
5_62	Ducted coaxial rotor	S3	C	P1	T & R	0	20
5_65	Ducted coaxial rotor	S3	C	P1	T	15	5
5_66	Ducted coaxial rotor	S3	C	P1	T	15	10
5_67	Ducted coaxial rotor	S3	C	P1	T	15	15
5_68	Ducted coaxial rotor	S3	C	P1	T	15	20

Continued on Next Page

Table B.1 — *Continued*

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
5_71	Ducted coaxial rotor	S3	C	P1	T	60	5
5_72	Ducted coaxial rotor	S3	C	P1	T	60	10
5_73	Ducted coaxial rotor	S3	C	P1	T	60	15
5_74	Ducted coaxial rotor	S3	C	P1	T	60	20
5_77	Ducted coaxial rotor	S3	C	P1	T	75	5
5_78	Ducted coaxial rotor	S3	C	P1	T	75	10
5_79	Ducted coaxial rotor	S3	C	P1	T	75	15
5_80	Ducted coaxial rotor	S3	C	P1	T	75	20
5_82	Ducted coaxial rotor	S3	C	P1	R	90	0
5_83	Ducted coaxial rotor	S3	C	P1	T & R	90	5
5_84	Ducted coaxial rotor	S3	C	P1	T & R	90	10
5_85	Ducted coaxial rotor	S3	C	P1	T & R	90	15
5_86	Ducted coaxial rotor	S3	C	P1	T & R	90	20

Continued on Next Page

Table B.1 — *Continued*

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
5_89	Ducted coaxial rotor	S3	C	P1	T	100	5
5_90	Ducted coaxial rotor	S3	C	P1	T	100	10
5_91	Ducted coaxial rotor	S3	C	P1	T	100	15
5_92	Ducted coaxial rotor	S3	C	P1	T	100	20
5_101	Ducted coaxial rotor	S3	C	P3	T & R	0	0
5_102	Ducted coaxial rotor	S3	C	P3	T & R	0	5
5_103	Ducted coaxial rotor	S3	C	P3	T & R	0	10
5_104	Ducted coaxial rotor	S3	C	P3	T & R	0	15
5_105	Ducted coaxial rotor	S3	C	P3	T & R	0	20
5_108	Ducted coaxial rotor	S3	C	P3	T	15	5
5_109	Ducted coaxial rotor	S3	C	P3	T	15	10
5_110	Ducted coaxial rotor	S3	C	P3	T	15	15
5_111	Ducted coaxial rotor	S3	C	P3	T	15	20

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Table B.1 — *Continued*

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
5_114	Ducted coaxial rotor	S3	C	P3	T	60	5
5_115	Ducted coaxial rotor	S3	C	P3	T	60	10
5_116	Ducted coaxial rotor	S3	C	P3	T	60	15
5_117	Ducted coaxial rotor	S3	C	P3	T	60	20
5_120	Ducted coaxial rotor	S3	C	P3	T	75	5
5_121	Ducted coaxial rotor	S3	C	P3	T	75	10
5_122	Ducted coaxial rotor	S3	C	P3	T	75	15
5_123	Ducted coaxial rotor	S3	C	P3	T	75	20
5_126	Ducted coaxial rotor	S3	C	P3	T & R	90	5
5_127	Ducted coaxial rotor	S3	C	P3	T & R	90	10
5_128	Ducted coaxial rotor	S3	C	P3	T & R	90	15
5_129	Ducted coaxial rotor	S3	C	P3	T & R	90	20
5_132	Ducted coaxial rotor	S3	C	P3	T	100	5
5_133	Ducted coaxial rotor	S3	C	P3	T	100	10
5_134	Ducted coaxial rotor	S3	C	P3	T	100	15
5_135	Ducted coaxial rotor	S3	C	P3	T	100	20

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Table B.1 — *Continued*

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
5_151	Ducted coaxial rotor	S1	C	P1	T & R	0	0
5_152	Ducted coaxial rotor	S1	C	P1	T & R	0	5
5_153	Ducted coaxial rotor	S1	C	P1	T & R	0	10
5_154	Ducted coaxial rotor	S1	C	P1	T & R	0	15
5_155	Ducted coaxial rotor	S1	C	P1	T & R	0	20
5_158	Ducted coaxial rotor	S1	C	P1	T	15	5
5_159	Ducted coaxial rotor	S1	C	P1	T	15	10
5_160	Ducted coaxial rotor	S1	C	P1	T	15	15
5_161	Ducted coaxial rotor	S1	C	P1	T	15	20
5_164	Ducted coaxial rotor	S1	C	P1	T	60	5
5_165	Ducted coaxial rotor	S1	C	P1	T	60	10
5_166	Ducted coaxial rotor	S1	C	P1	T	60	15
5_167	Ducted coaxial rotor	S1	C	P1	T	60	20

Continued on Next Page

Table B.1 — Continued

Test Number	Test Description	Rotor Configuration	Duct Shape	Rotor/Duct Position	Loads Measurement	Pitch Angle (deg)	Wind Speed (ms^{-1})
5_170	Ducted coaxial rotor	S1	C	P1	T	75	5
5_171	Ducted coaxial rotor	S1	C	P1	T	75	10
5_172	Ducted coaxial rotor	S1	C	P1	T	75	15
5_173	Ducted coaxial rotor	S1	C	P1	T	75	20
5_176	Ducted coaxial rotor	S1	C	P1	T & R	90	5
5_177	Ducted coaxial rotor	S1	C	P1	T & R	90	10
5_178	Ducted coaxial rotor	S1	C	P1	T & R	90	15
5_179	Ducted coaxial rotor	S1	C	P1	T & R	90	20
5_182	Ducted coaxial rotor	S1	C	P1	T	100	5
5_183	Ducted coaxial rotor	S1	C	P1	T	100	10
5_184	Ducted coaxial rotor	S1	C	P1	T	100	15
5_185	Ducted coaxial rotor	S1	C	P1	T	100	20

* Test 1_33 (nominal tip clearance and total loads) was incomplete due to high stand vibrations.

† Test (or test suite) was not conducted because of high stand vibrations.

‡ Test was not conducted because of high failure rate of rotors.

§ Incomplete test due to rotor failure.

¶ Test 5_49_R (nominal tip clearance and rotor-only loads) was incomplete due to rotor failure.

|| Torque-balanced test.

** Test was not conducted because of rotor speeds over allowable.

Appendix C

Catalog of Isolated Duct Testing

The two isolated duct experiments conducted in the Glenn L. Martin Wind Tunnel — loads measurements and surface flow visualization — are listed in Table C.1 and Table C.2, respectively. Table 3.1 explains the shorthand notation used to present the specifications of these tests in an abbreviated fashion.

Table C.1: Catalog of Duct Loads Tests.

Test Number	Duct Shape	Pitch Angle (deg)
6_1	Cambered	0
6_2	Cambered	15
6_3	Cambered	60
6_4	Cambered	75
6_5	Cambered	90
6_6	Cambered	100
6_10	Symmetric	0
6_11	Symmetric	15
6_12	Symmetric	60
6_13	Symmetric	75
6_14	Symmetric	90
6_15	Symmetric	100

Table C.2: Catalog of Duct Surface Flow Visualization Tests.

Test Number	Duct Shape	Pitch Angle (deg)
C_0	Cambered	0
C_15	Cambered	15
C_30	Cambered	30
C_45	Cambered	45
C_60	Cambered	60
C_75	Cambered	75
C_90	Cambered	90
C_100	Cambered	100
S_0	Symmetric	0
S_15	Symmetric	15
S_30	Symmetric	30
S_45	Symmetric	45
S_60	Symmetric	60
S_75	Symmetric	75
S_90	Symmetric	90
S_100	Symmetric	100

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