**Experimental and Numerical Studies of Aerodynamic Performance and
Near-Wake Propagation from a Novel Vertical Axis Wind Turbine**

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Abstract

With the world's growing energy demand, the need for offshore wind turbine installations in water depths greater than 50 meters has become a strategic priority for the wind energy market. In deep water sites, Vertical Axis Wind Turbines (VAWTs), particularly with curved rotor blades, may provide more than a 20% reduction in costs (i.e. installation and maintenance) when compared to standard HAWTs (Horizontal Axis Wind Turbines). However, VAWTs have suffered from an intrinsic deficiency rooted in the complex aerodynamic behaviour of the turbine due to Blade-Vortex Interactions (BVIs) during normal turbine operation. To overcome this performance limitation, a novel VAWT design that decreases the BVI effects is proposed. The key concept behind this design focused on vertically shifting the position of one blade with respect to the second blade, herein named Shifted Troposkien Shape VAWT (STS-VAWT).

To validate this novel design, a series of experiments were conducted in the Atmospheric Boundary Layer (ABL) wind tunnel to compare the aerodynamic performance (power coefficient) of the novel design (STS-VAWT) with that of a conventional design (based on troposkien shape VAWTs from Sandia National Laboratory). In this study, two sets of prototype turbines were designed, built, and tested. The first set consisted of three different configurations of VAWT (conventional, novel 50%, and novel 100% STS-VAWT) designed to operate in a medium Tip Speed Ratio (TSR) regime ($4 < \lambda < 6$), where the BVI effect is strong. TSR is the ratio of the blade velocity (at the equator) to the wind velocity. Whereas, the second set of two prototype turbines, which consisted of the conventional and 50% STS models, were designed to run at low TSR ($\lambda < 4$), where the BVI effect is minimal. To maintain consistent comparisons between the prototypes, the turbines of each set were fabricated using the same rotor parameters (i.e. overall height,

radius, NACA airfoil, and blade swept area). Comparison of wind tunnel results showed that the 50% STS model had a superior aerodynamic performance when compared with the conventional troposkien shape, particularly at the medium tip speed regime.

To confirm that the innovative concept (50% STS-VAWT) had performed better than other models, the aerodynamic performance of the three different configurations were also investigated numerically. An in-house MATLAB code, which combined the Blade Element Theory (BET) theory and the Vortex Filament Method (VFM), was developed and used. Simulations results (power coefficients) were verified and validated against experimental data having three diameters (2-m, 5-m, and 17-m conventional turbines from Sandia National Laboratories). The comparison results showed that the 50% STS-VAWT outperformed the conventional VAWT as well as the 100% STS-VAWT by up to 14% (peak power), within the range of rotation and turbine sizes that were investigated in the present work.

Following the aerodynamic performance study, it was essential to investigate the flow at the wake of each turbine model. Therefore, detailed wake measurements downstream of the novel turbine (50% STS-VAWT) were performed experimentally in comparison to those of a conventional design. Velocity deficit and turbulence intensity at the wake were measured in the wind tunnel at low and high tip speed ratios using a hot-wire anemometer technique. The measurements were taken from the near rotor plane to more than three rotor diameters (3D) downwind of each turbine at both lateral (horizontal) and vertical mid-planes. The experimental results revealed that the velocity deficit was significantly higher at the centre of the wake (where most of the power was generated) and gradually diminished as the wake progressed downstream. Furthermore, the wake behind the 50% STS model was narrow and shorter than the conventional turbine, when both turbines were spinning at a medium tip speed ratio.

I dedicate this work to my parents and brother, and am sorry that they
have not lived to see me graduate.
I will be forever grateful for their assistance

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List of Symbols

Symbols	Description	Units
A	Area	$[m^2]$
A_s	Rotor swept area	$[m^2]$
AR	Blade aspect ratio	$[-]$
a	Half the distance between both ends of troposkien blades on the axis of rotation	$[m]$
b	Blade semichord	$[m]$
BN	Blade number	$[-]$
c	Elliptic integral parameter	$[-]$
C	Chord length	$[m]$
C_D	Drag coefficient	$[-]$
C_L	Lift coefficient	$[-]$
C_n	Normal force coefficient	$[-]$
C_t	Tangential force coefficient	$[-]$
C_p	Power coefficient	$[-]$
C_Q	Torque coefficient	$[-]$
D	Diameter	$[m]$
$E(\pi/2; k)$	Elliptic integral of the second kind	$[-]$
F_N	Normal force generated by the aerodynamic blade element	$[N]$
F_T	Tangential force generated by the aerodynamic blade element	$[N]$

$F(\pi/2; k)$	Elliptic integral of the first kind	$[-]$
H	Height of the blade	$[m]$
$\mathbf{i}, \mathbf{j}, \mathbf{k}$	Unit triad defining the inertial reference frame	$[-]$
l	Blade length	$[m]$
NB	Number of blades	$[-]$
NE	Number of element	$[-]$
ω	Rotational speed	$[rad\ s^{-1}]$
P	Power	$[W]$
Q	Torque	$[N\ m]$
r	Radius of the blade section from the vertical axis of the wind turbine	$[m]$
R_e	Reynolds number	$[-]$
$\mathbf{s}, \mathbf{c}, \mathbf{n}$	Unit triad defining the aerodynamic blade element	$[-]$
T_u	Turbulence intensity	$[-]$
U, V, W	Components of the vortex induced velocity	$[m\ s^{-1}]$
U_R	Total velocity assigned to each aerodynamic blade element	$[m\ s^{-1}]$
U_∞	Speed in freestream conditions	$[m\ s^{-1}]$
U_t	Blade velocities	$[m\ s^{-1}]$
V	Voltage	$[Volt]$

Greek-Symbols

α	Angle of attack	$[rad]$
β	Ratio of the equator radius to half of the height of the wind turbine	$[-]$
$\Delta x, \Delta y, \Delta z$	Components of the vortex displacement	$[m]$
Γ_b	Bound vortex strength	$[m^2\ s]$

Γ_s	Spanwise vortex strength	$[m^2 s]$
Γ_t	Trailing tip vortex strength	$[m^2 s]$
θ	Azimuth angle	$[rad]$
θ_b	Blade azimuth angle	$[rad]$
ϕ	Local angle of the troposkien	$[rad]$
λ	Tip speed ratio	$[-]$
ρ	Fluid density	$[kg m^{-3}]$

Acronyms

ABL	Atmospheric Boundary Layer
BR	Blockage Ratio
BEM	Blade Element Momentum
BVI	Blade Vortex Interaction
CFD	Computational Fluid Dynamics
DMS	Double Multi-Streamtube
DAQ	Data Acquisition System
GW	Gigawatt
HAWT	Horizontal Axis Wind Turbine
HWA	Hot-wire Anemometry
MW	Megawatt
TSR	Tip Speed Ratio
NI	National Instruments
PIV	Particle Image Velocimetry
VAWT	Vertical Axis Wind Turbine
RSS	Root-sum square

Chapter 1

Introduction

The International Energy Agency (IEA) concluded in the International Energy Outlook (IEO, 2017) that the world energy consumption will increase by 28% during the next two decades, mainly due to the economical growth of emerging national economies (including India and China) [1]. At least 31% of the total increase will come from renewable sources (including wind, solar, hydropower, geothermal, and others). Among these sources, wind power is currently the fastest growing source, accounting for 55% of the total renewable power capacity installation in 2017 [2]. This is mainly due to the advances made to wind energy technologies that altered how the world is balancing its environmental responsibilities with its energy needs. Wind turbines are usually categorized into Horizontal Axis Wind Turbines (HAWTs) and the Vertical Axis Wind Turbines (VAWTs) depending on the orientation of the main axis of rotation, as shown in Figure 1.1a and 1.1b [3]. Both types of turbines are often grouped into wind farms (whether onshore or offshore wind farms, as shown in Figures 1.1c and 1.1d, to produce electricity at a cost competitive with other forms of electricity generation, i.e., fossil fuels or solar energy). The maximum power coefficient C_p (which used to describe the

efficiency of any wind turbine) can be approached by HAWTs is 0.45, i.e., less than the Betz limit which 0.59. However, a recent analysis [4] has shown that the power coefficient of VAWT is approximately 60% higher than the Betz limit, because of its aerodynamic behaviour [5]. Although VAWTs are competitive with HAWTs and even better in some applications, [3, 5, 6], HAWTs are the most commercially used wind turbine, particularly for onshore wind farm applications. One of the main reasons why HAWTs became more prominent is because several generations of HAWTs have evolved as a result of extensive academic research and commercialization [5].

Figure 1.1: Types of wind turbine : (a) Vertical axis wind turbine (b) Horizontal axis wind turbine (c) Onshore wind farms, (adapted from [7]) (d) Offshore wind farms, (adapted from [8]).

Recently, offshore wind energy has seen an increase in interest (30% increase in 2017 as shown in Figure 1.2) and is expected to be responsible for more than half of the total wind energy growth by 2040 [1]. Presently, most offshore wind farms operate in shallow waters up to 30 meters deep and many countries (Norway, USA, China, and Japan) are shifting towards water depths greater than 40 meters, where the strong, less turbulent, and consistent winds are present [9–11]. Although the majority of turbines in an onshore wind farm are HAWTs, for depths greater than 40 meters, HAWTs display a number of limitations including: (i) a higher center of gravity, (ii) massive tower construction, and (iii) high cost of energy (i.e. installation, maintenance, and operation costs, among others) [12–15]. VAWTs, on the other hand, are the most promising alternative for deep-water installations because they reduce the cost of energy by 20% [16].

Figure 1.2: Global cumulative offshore wind capacity in 2017.

1.1 Vertical Axis Wind Turbine (VAWT)

VAWTs are usually categorized into three categories: Darrieus troposkien, H-Darrieus and Savonius (see Figure 1.3). These turbines consist of vertical blades (normally 2 or

3) connected to a rotating vertical shaft at both ends while driving a generator. Since this shaft is installed vertically, the power equipment (the gearbox and the generator) is placed on the ground, thus reducing complexity and costs [3, 6, 17].

Figure 1.3: Vertical axis wind turbines: (a) Darrieus troposkien (b) H-Darrieus rotor (c) Savonius rotor.

The Darrieus troposkien design (Figure 1.3 a) was invented in the United States by the French aeronautical engineer (Georges Darrieus) in 1931 and refined by the National Research Council of Canada (NRC) from the 1970s [3]. These turbines were designed with two symmetrical curved blades (symmetrical in cross-section) attached at two points (top and bottom) on the vertical shaft [18]. The advantages of these curved blades included their ability to capture energy from every wind direction, minimizing internal bending stresses along the blade during rotation, and generating aerodynamic lifting forces that allow the blades to move faster than the wind (at the equator of the turbine). The troposkien turbine was studied extensively during the 1960s and 1970s [14, 19, 20] and eventually reached commercialization in the 1980s (for example, by FloWind Corporation, which installed more than 500 turbines in California). The installed Darrieus troposkien VAWTs were affected by cyclic fatigue weaknesses at the joints between the extruded aluminum blades and gradually vanished from the mainstream market [21, 22].

Despite their drawbacks, there has been a recent revival of the Darrieus troposkien VAWTs, due to significant advantages that make them attractive for offshore (deep waters) and urban applications including: 1) Their ability to harvest the wind from any direction and attain a very high speed without a yaw mechanism guiding the blade [13, 23]; 2) Easier and safer maintenance as the generator and the gearbox of the turbine can be installed on the ground (or sea level); 3) A more inherently stable system when afloat since the center of gravity of the turbine is closer to the ground [24]; 4) Reduced blade structural requirements since the blade is curved and attaches to the tower at two points. This leads to reduced machine complexity and better scalability to very large sizes for both offshore and urban applications [25, 26]; 5) Friendlier to wildlife than HAWTs because they can be placed closer to the ground than traditional HAWTs, which should make them easier for birds and bats to see [27]; 6) Easier fabrication as the troposkien design typically has a constant airfoil shape along the length of its blades [28]; 7) Greater energy generation as the troposkien turbine’s extracted power per unit ground area is approximately ten times bigger than HAWTs [29, 30]; 8) Faster wake dissipation as the wake generated downstream of the turbine can be dissipated quicker than the wake downstream of the HAWTs [31]; and 9) The ability to produce power consistently at high and low wind speeds. In residential and urban landscapes, where the average winds range between 10 and 15 km/h, VAWTs can produce power consistently at both high and low wind speeds while producing relatively low acoustic noise levels [32, 33]. HAWTs, on the other hand, only produce power consistently at a high wind speed.

Even though troposkein VAWTs have the aforementioned advantages, the design of troposkien VAWT suffers from an intrinsic deficiency rooted in the complex aerodynamic behaviour of the turbine due to Blade-Vortex Interactions (BVIs). The BVI phenomenon results from the interaction between the downwind blades with the wake vortices generated

by the upwind blades during normal turbine operation. This, in turn, has two adverse impacts on the performance of the turbine: 1) from an aerodynamic perspective, it diminishes the generated lift and subsequently the generated torque, and 2) from a structural point of view, BVIs create fluctuations in the aerodynamic loading that reduce the life of components due to fatigue. The BVI effects strongly depend on the Tip Speed Ratio (TSR, ratio of the blade velocity at the equator to the wind velocity). This has been verified experimentally by Fujisawa and Shibuya [34], who described the shedding of two pairs of vortices that leave the vicinity of the blades for different tip speed ratios.

1.2 The Diversity of Vertical Axis Wind Turbines

With a focus on improving VAWTs' performance including their ability or inability to self-start, a number of researchers attempted to come up with a suitable design that would enable the turbines to mitigate their issues. The majority of these attempts had an interest in changing the geometric configuration of the turbine to find a balance between cost and better performance, disregarding the structural perspective associated with these configurations. Therefore, many hybrid configurations of the VAWT have emerged, as shown in Figure 1.4. As can be seen in this figure, many of the later designs were hybrids of the original three configurations of VAWTs (i.e. Savonius, H-Darrieus, and troposkien Darrieus designs), and all of these variations consisted of vertically oriented blades which were mounted on a vertical shaft. For example, the combinations of H-Darrieus with the Savonius design (see Figure 1.4e) as well as troposkien Darrieus with Savonius (see Figure 1.4d) were suggested. Each of these hybrids attempted to exploit the best features of each design. For instance, the troposkien Darrieus is not a self-starting turbine (due to a band of negative torque at low tip speed range,

$\lambda < 1.6$) but it is more efficient than the others, whereas H-Darrieus has the capability to self-start. The Savonius turbine, which was invented in 1922 by the Finnish engineer Sigurd Johannes Savonius, has curved shape (rotating cylindrically) and because of that, it experiences less drag when it moves against the wind instead of when moving with the wind [6]. Although the Savonius turbine has not been successful (it has low speed, weight, and lower aerodynamic performance), it has been combined with other types due to its positional torque, i.e., it has a higher torque that can be used to pump water or for grain grinding. Additionally, the Savonius turbine is self-starting as well.

Figure 1.4: Basic VAWT configurations: (a) Troposkien Darrieus, (b) H-Darrieus, (c) Savonius VAWT, (d) Combined Troposkien with Savonius VAWT, (e) Combined H-Darrieus with Savonius VAWT, and (f) Helical VAWT.

The most notable design that improved the aerodynamic performance of Darrieus turbine was proposed by Alexander Gorlov [35], which was patented in the 1990s for hydro applications. This was accomplished by using blades that were helically twisted around the rotor axis, as shown in the Figure 1.4f. This shape recognized the following benefits: a reduction of the variation in aerodynamic loads and an improvement of the dynamic behavior because the blades encounter higher angle of attacks during all

azimuth angle positions. In addition, the blade shape allowed the turbine to rotate continuously at any wind direction, thus producing much higher lift forces.

However, as stated earlier, all vertical axis turbines(including the helical version) suffered from an intrinsic deficiency in its performance due to the BVI effect on the generated torque, when the wake vortices released from the advancing blades interact with the following blades. These vortices induce a sharp change in the effective angle of attack observed by the interacting blade (the downwind blade) leading to a sudden change in pressure on the blade surface. This rapid pressure change reduces the lift forces, thus decreasing the overall efficiency of the turbine. The VAWTs seem to be in need of a new design philosophy that would diminish the negative impact of BVIs. This need has been fulfilled in the current thesis by a novel design, which was able to improve the performance in addition to lower production costs, as will become clear in Subsection 3.2.1.1.

1.3 Motivation for Improving the Performance of Troposkien Turbine

The motivation for the current study was to examine the effects of BVIs on the aerodynamic performance (C_p vs. λ , or Power Coefficient vs. Tip Speed Ratio, both which will be defined later) and additionally to study the wake development from troposkien turbines. For vertical turbines, the BVI effects strongly depend on the Tip Speed Ratio (TSR).

Figure 1.5: Formation and development of vortices: (a) $\lambda = 1$ (b) $\lambda = 3$ (adapted from Fujisawa and Shibuya [34]).

Figure 1.5 indicates that the propagation of vortices and their influence on the blades are different at each distinct TSR or λ . At $\lambda = 1$ (shown in Figure 1.5a), the enclosed vortices quickly leave the vicinity of the blades and travel downstream of the wind turbine. Conversely, as shown in Figures 1.5b (i.e., $\lambda = 3$), while following identical blade revolution as Figure 1.5a, the vortices are either trapped inside the oval volume within the wind turbine or they were outside but very close to the blades (Figure 1.5b). Notice that the behaviour of vortices at different TSRs is expected, since for a given wind turbine rotational speed, a higher wind velocity gives a smaller TSR. Hence, the vortices will travel faster with a higher wind velocity at a lower TSR. However, this behaviour can considerably affect the wind turbine performance. As observed in this figure, the induced velocity caused by the vortices acts in the opposite direction of the flow, i.e., they induce impedance (resistance) against the motion of the wind. This effect is more pronounced at the equator of the wind turbine where most of the torque is generated. Therefore, for high TSRs, since the vortices stay longer in the neighborhood

of the blades, they cause a larger impedance, resulting in a reduced wind velocity close to the blades. As the result of this, the angle of attack between the reduced velocity and the tangential velocity of each blade is decreased, thus leading to a reduction in lift and torque values. In case of high TSRs, the impedance could be very large such that an oval shape vortex structure is created around the wind turbine resisting the incoming air from going through the wind turbine (refer to [36]).

Wind farms have suffered production losses (i.e. performance losses) mainly due to the wake effects. Since the upstream turbines extracted kinetic energy from the incoming wind to generate electricity, the wind leaving the upstream turbines, therefore, must have a lower energy (velocity) than the incoming wind (following linear momentum conservation). In addition to the significant deficit in the wind velocity, the wake downstream of the wind turbine will also have a higher level of turbulence. Therefore, the output power of downstream turbines is normally reduced. It is common procedure to let the wind “recover” and place downstream rows of turbines at a certain distance from the upstream rows (i.e. through proper spacing). This reduction in the performance of the downstream turbines of a wind farm is referred to as the “wake effect”. This wake effect strongly affects not only the performance of the wind farm but also the lifespan of individual turbines by inducing variable forces on the blades [37].

For HAWT farms, the power losses can vary between 5% and 40% depending on the wind farm layout [38–41]. Much effort has been made to address these losses, including separating turbines laterally (spanwise) by 3 to 5 rotor diameters and longitudinally (streamwise) by 6 to 10 rotor diameters [42–44]. Although proper spacing allows for a recovery of the velocity deficit for the downwind turbines, it strongly affects the total wind power extracted per ground surface area (i.e. large separation distances between turbines) [45]. Evidence shows that VAWTs can be spaced more tightly and extract

more power than HAWTs for the same wind farm area [29, 30]. This improvement in performance resulted from unique advantages such as, faster wake dissipation [31, 41], insensitivity to the wind direction, and higher power density (i.e. power per unit ground area) when compared to HAWTs [29]. However, the wake structure downstream the vertical turbines, particularly the troposkien design, has received much less attention than the horizontal turbines in literature and the understanding of the near-wake structure (up to 3 rotor diameters) remains under investigation.

1.4 Summary of Objectives

During the next decades, offshore wind turbines will likely be installed more than any other form of renewable power generation, particularly when water depths are greater than 40 meters, comprising more than half of the new wind power capacity. In deep-water sites, VAWTs offer both the performance (which can reach similar level as those of horizontal turbines) as well as an overall cost of energy reduction of more than 20% [6, 16]. The “cost of energy” involves capital and operational expenditures, annual energy production, and the fixed charge rate as defined by NREL (National Renewable Energy Laboratory, U.S. Department of Energy). This explains the greater interest in VAWT development recently, which led to several different designs, all attempting to provide more efficient and cost-effective turbines [14, 16, 24, 46–50]. Therefore, the current thesis is aiming at further investigation of opportunities to increase the performance of troposkien turbines by decreasing the effects of blade-vortex interactions. The objectives of the current thesis were defined as follows:

1. The first objective of this thesis research was to create a novel design where its performance would be more efficient than the previous conventional design. This

could be accomplished by diminishing the interaction between generated vortices and the oncoming rotor blade. If the novel design were successful as intended (that is, having the resulting performance being better than the conventional model), then, this new design should address some of the other disadvantages of the early conventional troposkien design as follows:

- (a) One shortcoming of the troposkien turbine was that its blades had to be twice as long as those of a HAWT, thus increasing the cost of the blade [17, 51]. Therefore, reducing the troposkien blade size was crucial when this project was undertaken. The impact of this change was that the mass of materials required to build a new blade could be decreased, and as a consequence, the aerodynamic and gravity loads on the blades would also be limited. This, in turn, offered further benefits for future turbine designs, i.e, multi-megawatt sizes with better scalability.
- (b) When the blade size is decreased, the production costs associated with the troposkien turbine will be lowered. As a consequence, the cost of energy reduction can be raised beyond 20%. For large offshore turbines, the decrease of blade size can also lower maintenance and operations costs.
- (c) A troposkien turbine is normally not self-starting and this is considered to be one of its shortcomings. Although many efforts have been made to improve the self-starting behaviour of VAWTs in the past [52–56], significant improvements have not been achieved and there are still many difficulties that need to be addressed. In this thesis, the weight reduction of the novel turbine, together with reduction of the BVI effects on its blades may become one of the key factors to mitigate self-starting issues.

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2. The second objective of this study was to compare the near-wake downstream of the new design against the conventional one (based on Sandia VAWTs). The velocity deficit and turbulence intensity in the wake of both turbines were mapped using a hot-wire anemometer at low and medium tip speed ratios. The measurements were taken from the near rotor plane to more than three rotor diameters (D) downwind of each turbine at both lateral and vertical planes.
 3. The last objective of this research was to develop an accurate and efficient aerodynamic code to predict the aerodynamic performance of both models (the conventional and the new design) and then to validate results with available experimental data for larger turbines from the literature. Here, an in-house MATLAB code, based on Blade Element Theory (BET) coupled with the Vortex Filament Method (VFM), was developed and used for the first time to compute the aerodynamic loads on the current novel turbine.

1.5 Structure of the Thesis

The thesis is structured into five chapters. Chapter 2 presents a chronological review of the troposkien Darrieus turbine, an overview of the aerodynamic models, and a literature survey with respect to certain techniques used in VAWT studies. Subsequently, Chapter 3 describes the design process of the prototype turbine models, the experimental setups of both performance and wake measurements (which were performed in the ABL (Atmospheric Boundary Layer) wind tunnel), along with the theory of the MATLAB code. Here, the code was used to predict and analyze the aerodynamic performance of different turbine configurations. Chapter 4 demonstrates the experimental results along with the wake measurements and the numerical analysis of the turbine models. Finally,

Chapter 5 presents the overall comparison of both results (experimental and numerical), all conclusions of the research made throughout this thesis, and recommendations for future work.

Chapter 2

Background and Literature Review

2.1 Introduction

This chapter details a comprehensive overview of the curved blade turbine. First, a chronological review of Darrieus troposkien vertical axis wind turbine is defined in Section 2.2, beginning from when the Darrieus troposkien research program started at NASA/DOE and continued for over 20 years. Section 2.2.1 offers a general discussion concerning the aerodynamics behaviour around the turbine blades and how it affects the turbine performance. Additionally, the background material, relating to the aerodynamic models that were used to predict the VAWT performance, is described in Section 2.2.2. Finally, Section 2.3 briefly describes the background and literature review related to the VAWTs including H-Darrieus.

2.2 A Chronological Review of the Darrieus Troposkien Turbine

Wind turbines were first conceptualized when humankind began using windmills for irrigation and other mechanical applications. For instant, in the tenth century VAWTs were used to pump water or to grind wheat in Afghanistan and Persia. During the 19th century, windmills became more common around the world, particularly in Northwest Europe, i.e., the Dutch Windmill [6]. At the end of the the 19th century, windmills were used in North America to generate electricity (i.e. Brush windmill built in 1888 in Cleveland, Ohio), and they became more frequent by the 1930's [6, 57]. In the same period (1931), the Darrieus turbine (i.e. both curved-blade configuration and straight-blade configuration) was invented in the United States by the French aeronautical engineer (Georges Darrieus) and refined by the National Research Council of Canada (NRC) from the 1970s [3, 51].

The Darrieus troposkien turbine (i.e. curved-blade configuration) was also studied extensively during the 1970s by SNL (Sandia National Laboratories) [14, 19, 20], eventually reaching commercialization in the 1980s; and gradually vanishing from the mainstream market in the 1990s [3, 21]. In the early 1970s, Sandia started their theoretical and experimental studies on the curved-blade turbine and eventually built a 5-m VAWT with two and three curved-blades. The blades were made from extruded aluminum and were bolted to the upper and lower central column (main shaft). The performance of the turbine was found to be 0.39 (i.e. maximum power coefficient) when the turbine was spinning at a rotational speed of 150 rpm with three blades. Conversely, the maximum power coefficient with two blades was lower than the three-bladed due to lower solidity

of the turbine with two blades. Solidity represents the percentage of the circumference of the rotor that contains material instead of air. The relative success of the 5-m turbine indicated that larger blades would produce more power [17, 22, 58]. In the years immediately following 1977, a 17-m VAWT was designed and built, with two curved blades and aspect ratio (H/D) of 1:1, at the Sandia National Laboratories. The turbine blades were fabricated from extruded aluminum with a NACA 0015 airfoil, producing a rated power of 60 kW [59]. After a number of successful years of operation (i.e., the Sandia 17-m turbine ran continuously for years with excellent power generation), the turbine was commercialized by several companies (for example, by FloWind Corporation). Later, in 1980, Sandia continued their work on this type of turbine and built one of the largest turbines in the U.S., which had a diameter of 34 meters with a swept area of 955 square meters. The swept area is the projection of the revolving turbine on a wind cross section. The turbine aspect ratio was 1.25 (height over diameter), producing a power of 625-kW at a wind speed of 12.5 m/s [60].

Darrieus troposkien turbines were also developed by FloWind Corporation throughout the 1980s. Using a two-curved blade configuration, FloWind developed and built several versions with different rotor diameters (i.e. 17-m, 19-m, 34-m, and 38-m). FloWind also launched a commercial 3-bladed troposkien rotor with an extended height-to-diameter ratio to decrease the aerodynamic forces acting on the blade while increasing its efficiency. Over the 1970s and 1980s, more than 600 commercial VAWTs of the Darrieus configuration were installed in the United States by several manufacturers including FloWind Corporation and Vawtpower [6, 17, 21, 61].

A Darrieus troposkien turbine of 24.4-m was installed in 1977 on Magdalen Island, Quebec. Although the turbine operated efficiently during the first year of operation, maintenance errors caused the generator to be disconnected and the turbine was forced

to be halted. Later, Indal Technologies Inc. fixed the turbine and installed two other prototypes of the same size, reaching a power level up to 500-kW. The first megawatt-class prototype of a troposkien turbine (i.e. the 4-MW) named “Eolé”, was installed in early 1987 at Cap-Chat, Quebec from a joint project between NRC and Hydro Quebec. With a 64-m diameter, 96-m height, and 4,000 square meters of swept area, the Eolé turbine is considered to be the largest VAWT ever built. After six years of operation, the turbine was shut down due to damage to its bearings [6]. At the same period, DAF-Indal Technologies in Canada designed and built three intermediate sized Darrieus troposkien turbines with different output power capacities ranging between 14 to 50 kW. Later, four Darrieus models (5.5-m, 12.8-m, 17-m, and 25-m in diameter) were built and tested by Alcoa Corporation at different wind speeds (up to 54 m/s). Although the troposkien turbines experienced a remarkable and rapid growth at the onset of their commercialization, overall interest declined considerably over the years, and as a consequence, R & D at certain institutes and companies (including NRC, FloWind, DAF Indal, and U.S. Windpower) came to a halt due to lack of support from their governments (the U.S. and Canadian federal agencies).

Presently, the offshore wind power generation is becoming a promising alternative to onshore installation due to strong and consistent winds, as well as other related factors [9–11]. Hence, a greater interest in developing VAWTs arose due to its potential in achieving more than 20% cost of energy reduction compared to the standard HAWTs. This especially apparent in deep-waters that are greater than 40 meters, as reported in the literature [62].

2.2.1 Aerodynamics Analysis of Troposkien VAWT

The velocity triangle (as seen in the following figure) around a VAWT blade consists of the rotational velocity vector, U , combined with the wind velocity vector, V , to form the final fluid velocity vector as seen by the blade, W , which has an angle of attack, α , with the blade, thus producing lift, L , and drag, D , forces.

The rotational velocity (as seen by the blade) is given by the following vector cross product

$$U = -\omega \times R \quad (2.1)$$

Figure 2.1: VAWT velocity triangle : (a) Isometric view (b) Top view.

The combination of lift and drag forces, when projected to the tangential direction (blade radius of rotation), results in a tangential force, which will produce torque and power. Similarly, the lift and drag forces also produce a normal force in the radial (normal) direction.

As a consequence of the change in angle of attack at each blade location during a

revolution, the lift and drag forces (or tangential and normal forces) are accordingly changed. Three flow regimes are formed around the blade, i.e., 1) attached regime, where the angles of attack vary between -15° to $+15^\circ$; 2) high-lift/stall-development regime, where the angles of attack vary between 15° and 30° ; and 3) deep-stall regime with attack angles between 30° and 90° degrees.

The integration of tangential forces over a revolution will create torque and power that will sustain the rotation. The power coefficient (C_p) is expressed as follows

$$C_p = \frac{P}{\frac{1}{2} \rho A_s U_\infty^3} = \frac{Q \omega}{\frac{1}{2} \rho A_s U_\infty^3} \quad (2.2)$$

where Q is the torque, ω is the angular velocity, V is the wind velocity, and A_s is the swept area, and P is the power generated. It is worth defining the tip speed ratio (ratio of the blade velocity at the equator to the wind velocity)

$$TSR = \lambda = \frac{\omega R}{V}. \quad (2.3)$$

2.2.2 Overview of the Aerodynamic Models of Darrieus Turbine

Numerical performance prediction of VAWTs (whether troposkien or H-Darrieus) is a challenging issue because the blades operate parallel to the flow, resulting in very complex flow phenomena around the turbine blades (airfoil stall, vortex shedding, and BVIs) [6]. Several aerodynamic models have been developed since the Sandia National Laboratories and NRC started their VAWT research programs. As shown in Figure 2.2, these models were divided into three major categories as presented by Islam [28]. Each model has its own characteristics, issues, and attractiveness.

Figure 2.2: Structure of the Darrieus aerodynamic predictions models.

Blade Element/Momentum model (BEM), determined the forces acting on the turbine blades and turbine performance based on the conservation of linear and angular momentum (i.e. equating the streamwise aerodynamic force on the blades to the rate of change of momentum of the air). Conversely, whereas Blade Element Theory (BET) measured the forces at a section of the blade, as a function of blade geometry (i.e. calculates the relative flow at each section of the blade and then integrating along the span to determines the overall performance) [28, 63]. One of the early models which utilized this concept were single and multiple stream tube (SST and MST) models [64, 65]. The central idea behind these models (single and multiple stream tube) relied on the calculation of the drag forces based on a constant induced velocity inside streamlines that covered the swept area of the rotor (whether single or multiple streamlines). Although each of these models has its own ability to predict the performance of the turbines in an acceptable manner, they still present a number of limitations, as reviewed by [28]. A more sophisticated model (Double-Multiple Stream Tube, DMST) proposed by Paraschivoiu [22] appeared later;

however, some discrepancies were present in the output results, particularly at the high tip-speed ratio or high solidity conditions. Moreover, for these models (SST, MST, and DMST) the interactions between the vortices and the turbine blades were not included in the code, and thus, any information in the near wake structure was not computed. These initial models lead to the development of the vortex filament method by Strickland [66] to simulate the complex interactions between the three-dimensional wake flow and the turbine blades. Here, the VAWT performance was simulated by dividing the blade into several spanwise elements, each element replaced by a lifting line with a bound circulation. The bound vortex strength at each element was evaluated in time using empirical aerodynamic information at a certain angle of attack (blade element theory), which was determined from the blade rotation, free stream velocity, and wake vortex filaments that were shed and tracked in time (Vortex Filament Method). Aerodynamic forces acting on the blade were then obtained by using the Kutta-Joukowski theorem. Scheurich and co-workers [67, 68] adopted this approach and predicted the performance of three different configurations of VAWT: H-Darrieus, troposkien Darrieus and a helical structure, under steady and unsteady wind conditions. Their simulation results exhibited reasonable agreement with the experimental data and provided a valuable representation of the near wake profile.

2.3 A Review of the Literature

As stated earlier, the Darrieus troposkien turbines were studied intensively for two decades (1970s and 1980s), and then, commercialization and research gradually vanished from the 1990s. The consequence was that only relatively limited experimental or numerical contributions regarding the Darrieus troposkien turbine have appeared in the

literature recently, whereas the majority of the academic work has been focused on the H-Darrieus turbines. A general review of both experimental and numerical efforts that have been attempted to characterise various aspects of Darrieus VAWT performance (whether troposkien or H-Darrieus) was summarized in this section. The review showed the major aspects of the flow around VAWTs, such as blade vortex interactions, dynamic stall phenomena, and detailed wake measurements downstream of the turbine.

One of the first measurements of the aerodynamic performance of a troposkien turbine was conducted in 1976, by Muraca et al.[69]. A 4.267-m troposkien turbine with an average blade chord of 0.142-m was designed and then tested in a wind tunnel (i.e. Langley Research Center wind tunnel). The performance ($C_p - \lambda$) was obtained under two different wind tunnel velocities: 6.92 m/sec and 8.32 m/sec. The experimental results showed a good agreement when compared with analytically predicted data. Muraca et al. also investigated the near wake structure downstream of the rotor by using a Pitot tube. The authors showed only the wake velocity deficit at 0.7-m downstream of the turbine, whereas, the effects of turbulence fluctuations were absent due to the technique that was used (i.e. Pitot tube). Later, in 1979, Vermeulen [70] conducted wake measurements behind a full-scale troposkien turbine (Fokker turbine having 5-m diameter with two blades) and reported only the wake velocity deficit without turbulence intensity levels due to their type of equipment, i.e., a wind meter. The measured power coefficient of the Fokker 5-m was plotted against the tip-speed ratio ($C_p - \lambda$), reaching a peak power of 0.33. Vermeulen also made a full comparison of the results with Boschloo's results (Boschloo studied the wake of a small-scale wind turbine in a wind tunnel [71]) and concluded that no conclusive results could be obtained for the full-scale turbine at high tip speed ratios.

With the emergence of hot-wire anemometry as a technique for measuring flow

velocities, the near flow field around the troposkien turbine with 1.7 m diameter was mapped by Bergeles [72]. The mean velocity and turbulence intensity were determined from 0.5D upstream to 1D downstream of the turbine, for different tip speed ratios in the horizontal plane. The experimental data showed that the flow field downstream of the rotor was asymmetric, and the edges of the wake were affected by increasing the tip speed ratio (i.e., the edges of the wake were not affected at a TSR of 5.4) whilst small effects were present at a tip speed ratio of 7. Whale et al. [73] experimentally examined the wake structure of a small-scale wind turbine in a water tunnel using PIV (Particle Image Velocimetry). This technique has proved to be accurate and flexible, allowing the acquisition of detailed velocity data very close to the wind turbine. PIV results showed a series of helical vortices embedded in diffused vorticity behind the rotor. Furthermore, PIV revealed separate regions of concentrated vorticity at the wake located approximately at the mid-span of the turbine.

A detailed study of the flow field near the troposkien Darrieus turbine was also carried out by Marini et al. [74], using two different aerodynamic models: the single streamtube momentum model and the free wake vortex model. The comparison of the power coefficient between vortex model and SMM calculations showed good agreement in some cases (i.e. for low solidity within the whole λ range) but worse agreement when the λ was small ($\lambda < 4$) for high solidity. In other words, the single streamtube momentum model was more suitable (i.e. guaranteeing the good accuracy within short CPU time) for predicting the overall performance of the troposkien Darrieus turbine for low solidity, while it is unreliable if solidity is high. Vortex wake theories, on the other hand, have shown the best accuracy and can be used for wind turbine design.

The dynamic stall for stationary and rotating Darrieus airfoil blade operating at low tip-speed ratios was investigated by Fujisawa et al. [34, 75] using flow visualizations

and PIV measurements. From this study, two separate pairs of counter-rotating vortices were formed and travelled through the wake. One group moved downstream through the turbine swept area (closer to the center of the second turbine’s blade) and interacted with the downstream blade, while the other originated along the inner surface and towards the lower part of the blade. The development and formation of these vortices depended on the tip speed ratio (i.e. as the tip speed ratio was decreased, the blade angle increased and the vortices became larger).

Ferreira et al.[76] categorized the wake and induced incoming flow for non-skewed and skewed flow over the H-Darrieus turbine blade using the hotwire anemometry technique. During this research, the evolution of tip vortices roll-up from the upwind blade interacted with the downwind blade. Ferreira’s research formed an important benchmark for the numerous studies that followed and covered many aspects of H-Darrieus turbine wake (Wind Energy Research Group of Delft University of Technology, (DUWIND) [77–80]. Leu et al. [81] measured the vortex structures from the NACA 0015 airfoil of a VAWT with and without an upstream turbulence generator. Leu benefited from the advances in PIV (Particle Image Velocimetry) using hollow glass microsphere particles (13 μm diameter) as tracers to acquire high-resolution digital images. They showed a significant impact of stream turbulence intensity on the dynamic stall process. The dynamic stall occurred approximately from $\alpha = 17^\circ$ to $\alpha = 30^\circ$ when the turbulence generator was not employed, while dynamic stall happened from $\alpha = 20^\circ$ to $\alpha = 25^\circ$ with the turbulence generator placed upstream. The review also described many of other experimental efforts [82–89] that have attempted to characterise various aspects of vertical axis wind turbine performance, particularly H-Darrieus, including dynamic stall and vortex structures at the wake.

Apart from experimental studies, various aerodynamic models have been developed

and used to predict the aerodynamic loads and performance of wind turbines [22, 63, 64, 66]. Brahim et al. [90] developed a new code to predict aerodynamic loads for the Sandia-17m rotor and compared the numerical results with both the CARDAAV code and available experimental data. The comparison results showed good agreement with the experimental data. Rosen et al. [91] carried out a detailed study on the structure of both rotating straight or curved turbine blades by a nonlinear structural model. Besides dealing with nonlinear boundary conditions, the model was also used to address some of the aeroelastic aspects in the analysis of VAWTs. The aerodynamic loads (forces and moments) along the turbine blades were calculated using two techniques: the first was based on differentiation of the displacements along the blade, while the second was based on integration of the loads along the blade. The model was also tested against experimental data.

Through a dynamic simulation model, Wakui et al. [92] analyzed theoretically wind turbine generator systems for three types of wind turbine configuration (i.e. the Darrieus-Savounis hybrid wind turbine, the Darrieus wind turbine, and the propeller turbine). In this study, the authors examined the three different turbine configurations with two different methods of an operating system. The first was the constant tip speed ratio operation for stand-alone power systems, while the second was the synchronous operation for utility power systems. They concluded that each wind turbine configuration should have its own operating system in particular wind conditions, for example, Darrieus turbine has little power extraction under constant tip speed ratio operation for stand-alone power systems.

An extensive series of numerical and experimental studies dealing with VAWTs were published by Ferreira et al. [93–95]. One of these studies aim was to demonstrate the suitability of using PIV data for validation of the numerical models, as well as assessing

which turbulent model and grid refinement method should be used. The work observed the effect of the dynamic stall in a 2D single-bladed H-Darrieus and compared results between two turbulence models: URANS (Unsteady Reynolds Averaged Navier-Stokes) and LES (Large Eddy Simulation). These transient models showed good agreement when compared against the PIV experimental data that was mentioned earlier. The unsteady flow behaviour of a straight bladed Darrieus VAWT was a focus of Ferreira et al. [96] who applied a 3D unsteady potential-flow vortex model.

The unsteady flow around the airfoil of a VAWT blade was numerically studied by Asress [97]. The main focus of this study was to analyze 2D unsteady flow using ANSYS FLUENT along with RANS equations as well as using DMST (Double Multiple Stream Tube) around the VAWT model. The author showed that both RANS and DMST models of the turbine under-predicted torque for lower tip speed ratios when compared against experimental data. Rashedul et al. [98] numerically studied the 2-D aerodynamics performance at different values of tip speed ratios around a straight blade fixed-pitch VAWT. The turbine had 3 blades based on the NACA0018 symmetric airfoil. The studies showed the vortex structures in the wake of a Darrieus wind turbine using ROVLM (Rotor Vortex Lattice Method) model and how the numerical results compared against PIV data that was obtained earlier by Whale [73].

By turning attention to the literature related to VAWT, the numerical and experimental academic contributions regarding the troposkien VAWT type have been relatively limited and the majority of the projects were focused on H-Darrieus. Therefore, the present work aims at filling the gap and provide experimental data with particular emphasis on the aerodynamic performance of novel and conventional troposkien turbines. Moreover, this thesis presents an experimental analysis of the wake structure (velocity deficit and turbulent intensity), downstream of the three different turbine configurations.

Chapter 3

Experimental and Computational Methods

3.1 Introduction

One goal of this thesis was to investigate and characterize, experimentally and numerically, the aerodynamic performance of the conventional VAWT design (curved blades) and compare it to the novel designs. Additionally, to studies of the wake structure at multiple locations downstream of each turbine configuration was studied. This chapter begins with an emphasis on the design of three different turbine configurations: conventional design, novel 50% STS-VAWT, and 100% STS-VAWT. These turbines have been designed, built, and tested in the department. The next section (i.e., Section 3.2.2) discusses the aerodynamic turbine wake and how it may affect the performance of the turbines placed downstream (i.e., wake losses in wind farms). Next, Subsection 3.2.2.2 briefly explains the hot-wire anemometry technique and how it was used in the current work to map

the velocity deficit and turbulence intensity levels in the lateral and vertical planes downstream of the three turbine models. Finally, the implementation of the numerical code that were used to predict the turbine performances is outlined in Section 3.3.

3.2 Wind Tunnel Experiments

3.2.1 Power Measurement

Wind tunnel experiments were conducted to evaluate the aerodynamic performance of two sets of prototype turbines. The first set consisted of three different turbine configurations: conventional (based on Sandia VAWTs), novel 50% STS-VAWT (50% Shifted Troposkien Shaped-Vertical Axis Wind Turbine), and novel 100% STS-VAWT. Whereas the second set consisted of only two different models: conventional and 50% STS-VAWT. In the following subsections, the specifications of both sets of turbines are described.

3.2.1.1 Troposkien Geometry

In order to accommodate the vertical shift in the STS-VAWT prototypes, each configuration requires its own specific blade design, i.e, the design of a troposkien shape blade with a specific diameter to height ratio (β). The troposkien shape can be derived from the balance of forces of a uniform skipping rope rotating vertically at a constant velocity. Blackwell and Reis [18] obtained the following solution (follow notation from Figure 3.1): [18]

$$\frac{z}{a} = 1 - \frac{F(\phi, k)}{F(\frac{\pi}{2}, k)}, \quad (3.1)$$

where z is the vertical position, a is the half height of the blade, and F is the complete elliptical integral of the first kind with a parameter k , defined as

$$F(\phi, k) = \int_0^\phi \frac{d\theta}{\sqrt{1 - k^2 \sin^2 \theta}}. \quad (3.2)$$

The parameter ϕ is given by

$$\phi = \sin^{-1} \left[\frac{1}{\beta} \left(\frac{r}{a} \right) \right], \quad (3.3)$$

Figure 3.1: Sketch of the conventional troposkien turbine, showing some of the geometry parameters.

where r is the blade radius at a particular height (b or R is the maximum radius at the equator of the blade) and β is the diameter to height ratio as discussed before. β is

correlated to the parameter k by

$$\beta = \frac{2k}{(1 - k^2)F(\frac{\pi}{2}; k)}, \quad (3.4)$$

The integrals can be obtained numerically through iterative determination of k as function of β . Finally, the troposkien blade length, S , and the swept area, A_s , can be determined by

$$\frac{S}{2a} = \frac{2}{1 - k^2} \frac{E(\frac{\pi}{2}, k)}{F(\frac{\pi}{2}, k)} - 1, \quad (3.5)$$

$$\frac{A_s}{4a^2} = \frac{\ln\left(\frac{1+k}{1-k}\right)}{(1 - k^2)F^2(\frac{\pi}{2}, k)}, \quad (3.6)$$

where $E(\frac{\pi}{2}, k)$ is the complete elliptical integral of the second kind with parameter k , defined as

$$E(\phi, k) = \int_0^\phi \sqrt{1 - k^2 \sin^2 \theta} d\theta. \quad (3.7)$$

Due to the fact that any point on a perfect troposkien shape has its own local curvature, fabricating such blades demands high-quality machining, which is not cost-effective. Conversely, conformity with the troposkien shape is particularly important to ensure that the developed bending stress along the blade is minimal. Hence, wind turbine manufacturing companies turned to design and build straight/arc approximations of the troposkien shape configurations. Such straight/arc approximations consist of a middle circular segment that is connected to the supports by two straight parts. The same

strategy is followed here for designing the blades.

3.2.1.2 Design and Prototyping

The central idea behind the proposed configuration (STS-VAWT) relies on the reduction of the BVI (Blade Vortex Interaction) effect. Therefore, it is imperative to design prototypes that experience the BVI effect under normal operation conditions. Figure 3.2 shows the power coefficient as function of tip speed ratio ($\text{TSR} = \omega R / U_\infty$, i.e. the ratio between the maximum rotational blade velocity and the wind velocity) of a typical VAWT [3]. As depicted in this figure, a wind turbine typically operates at three different zones. At low Tip Speed Ratio (TSR), lower than 4, dynamic stall plays a major role in the performance of the turbine. Conversely, at high TSR, greater than 6, it is the secondary effects (BVI, rotating tower, or the presence of struts) dictate and degrade the performance. Between TSRs 4 and 6, there is a transition zone where the effects of both zones contribute to the efficiency of the turbine.

Figure 3.2: Darrieus turbine performance characteristics (adapted from [3]).

In this work, two sets of prototype turbines were designed, built, and then tested in the wind tunnel. The geometry of the first set (set I) of turbines was determined such

that they operate in the transition zone (TSR between 4 and 6), where the effect of the BVI could be evaluated in the STS configurations. Although, the ideal situation would have been to design a turbine that could operate in the high TSR zone, the constraints of the available wind tunnel (such as the size and wind speed) did not allow exploring this option due to a much higher TSR. However, to ensure that the discrepancies between the performance of the set I conventional shape and STS models were solely due to the BVI effect, the second set of the prototype turbines (set II, which had a smaller radius of rotation than set I) was designed to run at low TSR (i.e. lower than 4), where the BVI effect was minimal. In the following two sections, the specifications of both sets of turbines are described. It is also noteworthy that the turbine blades were all made from ABS plastic material using a rapid prototyping technique, and the vertical shaft was made of a 6061 aluminum round tube with a diameter of 0.019 m.

3.2.1.2.1 Set I Prototype Turbines: Significant BVI Effect (Transition Zone)

Set I of the prototype turbines consisted of three models: a conventional troposkien shape VAWT, a 50% STS-VAWT, and a 100% STS-VAWT as shown in Figure 3.3.

Figure 3.3: Geometries used in the current work: (a) Conventional (b) 50% STS (c) 100% STS.

The first step in the geometrical design was referencing the airfoil coordinates database

file from [99] the SolidWorks software. With the airfoil's shape defined, the process of building the blade began by manipulating the coordinates along the length of the blade to produce the desired height and chord lengths. The curved blade's design was essentially created by shifting the airfoil element as a straight line at both ends (i.e. the top and the bottom part), and adding an arc line at the equator of the blade. The second turbine blade was designed in a similar manner. These curved blades, which had an oval shape, would offer minimum or no bending stresses along its length. In addition to this, these blades were manufactured with a NACA 0015 airfoil cross-section that can generate sufficient aerodynamic lifting forces at certain angles of attack. These lifting forces allow the turbine blade to rotate around its vertical shaft faster than the wind, thereby optimizing power generation. Table 3.1 presents the specifications of these turbines.

Table 3.1: Specifications of Set I Prototype Turbines

Parameters of the models
Number of blades (N)	2	2	2
Turbine radius (b) [m]	0.375	0.375	0.38
Turbine height (H) [m]	0.75	0.75	0.75
Blade height ($h = 2a$) [m]	0.75	0.50	0.375
Swept area (A_s) [m ²]	0.36	0.36	0.36
Chord length (c) [m]	0.1	0.1	0.1
Solidity ($\sigma = NcS/A_s$)	0.59	0.50	0.46
Blade weight (kg)	0.345	0.290	0.284
Blade length (l) [m]	1.10	0.93	0.855
Blade weight/length ratio	0.313	0.311	0.33
Blade airfoil	NACA 0015	NACA 0015	NACA 0015

In order to make sure that any difference in the performance of these turbines was caused by its specific configuration, all the turbines were made with the same number of blades (2), turbine radius, turbine overall height, swept area, chord length, and a NACA0015 airfoil cross section (see Table 3.1). In addition to this, the solidity (the

ratio of the total blade area to the turbine swept area ($\sigma = \frac{N c l}{A_s}$) [3]) of the offset design (50% STS) decreased by decreasing the blade length.

The present novel model (see Figures 3.3b and 3.3c) consisted of two curved blades, wherein one blade was longitudinally shifted with respect to the other blade. In other words, the attachment point of one blade was halfway from the attachment points of the other blade. This turbine was named the “Shifted Troposkein Shape-Vertical Axis Wind Turbine” (STS-VAWT), as discussed previously. As shown in Figure 3.4a, vortices shed at the equator (middle height) of the 50% STS upwind blade (higher advancing blade; left side in the figure) will interact horizontally with the top portion of the downwind blade, causing less BVI effects. Conversely, for the conventional turbine (see Figure 3.4b), the generated vortices will interact with the second blade entirely, thus maximizing the BVI, especially at the power-producing equator section.

To determine the effect of vertical blade shifting on the turbine aerodynamic efficiency, the blade was also shifted 100% of the height from the other blade (100% STS-VAWT). Here, the BVI effects could be reduced even more than the 50% due to the fact that the blade paths would not overlap (see Figure 3.5). Moreover, the design of each blade followed the straight/arc approximation of the troposkien shape as shown in Figure 3.6. As evident in this figure, the straight/arc approximation closely matched the troposkien shape for all the β values. Here, β is the ratio of the maximum blade displacement from the axis of rotation to one-half of the spacing between the attachment points on the axis of rotation [18].

Figure 3.4: Blade path profiles of both conventional and 50% STS turbine models: (a) 50% STS (b) Conventional.

Figure 3.5: The blades path prole of 100% STS turbine models.

Figure 3.6: Set I Prototype Turbines: Comparison between Troposkien and Straight/Arc Blades Shapes.

3.2.1.2.2 Set II Prototype Turbines: Minimal BVI Effect (Dynamic Stall Zone)

The set II prototype wind turbines consisted of two models: the conventional troposkien shape and the 50% STS model. The 100% STS model was not built for this set. The specifications of the set II prototypes are presented in Table 3.2. Note that the height of the turbine and its blade were chosen the same as the corresponding model in set I; however, the smaller radius of the turbine in set II resulted in a smaller swept area and blade length. This resulted in a completely different behaviour of the turbine, such that at 600 and 700 rpm under the wind tunnel speed range, it operated in a TSR (Tip Speed Ratio) range of 2 to 3. This meant that due to the fact that the speed of the turbine was only 2 to 3 times higher than the vortices, by the time the downwind blade enter the BVI space, the vortices generated by the upwind blade would have already left the inner

region of the turbine. This ensured that the BVI effect would have minimum influence on the performance of the turbine. Notice again that similar to the set I turbines, the blades of set II were fabricated using an arc/straight approximation of the troposkien shape. This is shown in Figure 3.7, where a close match between the approximation and the troposkien shape was observed.

Table 3.2: Specifications of Set II Prototype Turbines

Parameters of the models
Number of blades (N)	2	2
Turbine radius (b) [m]	0.25	0.25
Turbine height (H) [m]	0.75	0.75
Blade height ($h = 2a$) [m]	0.75	0.50
Swept area (A_s) [m ²]	0.24	0.24
Chord length (c) [m]	0.1	0.1
Solidity ($\sigma = Ncl/A_s$)	0.625	0.416
Blade weight (kg)	1.055	0.891
Blade length (S) [m]	0.928	0.733
Blade weight/length ratio	1.13	1.21
Blade airfoil	NACA 0015	NACA 0015

Figure 3.7: Set II Prototype Turbines: Comparison between Troposkien and Straight/Arc Blades Shapes.

3.2.1.3 Wind tunnel

The wind tunnel experiments were carried out in the Atmospheric Boundary Layer (ABL) wind tunnel. The ABL wind tunnel is a low-speed open-circuit wind tunnel with two test sections (i.e. upstream and downstream test section) for aerodynamic and boundary layer tests. Both test sections are 2.44 m (96.06 in) long and the cross-section is 1.68 m (66.14 in) wide and 1.12 m (44.09 in) high. The contraction ratio at the inlet of the tunnel is 7.1:1 followed by a fetch length distance of 9.76 m (see Figure 3.8). The maximum wind speed in the wind tunnel is 62 km/h and the flow turbulence intensity is less than 0.42%.

Figure 3.8: 3D geometry of the ABL (Atmospheric Boundary Layer) wind tunnel (flow from right to left).

The wind tunnel air flow is directed through flow straighteners (honeycomb structure) at the inlet (right side in Figure 3.8, followed by the contraction nozzle, upstream test section, downstream test section, and axial fan (1.67 m diameter), which is powered by a 30 kW motor. The air from the outlet is then recirculated to the inlet section through the open co-axial space inside the room of the wind tunnel. The wind velocity can be adjusted by controlling the fan frequency.

3.2.1.4 Experimental Setup and Power Measurements

The VAWT models were placed vertically at the center of the upstream test section (close to the contraction nozzle) and mounted on a small foundation installed on the floor of the wind tunnel (see Figure 3.9). The foundation was made from aluminum and had two bearings at the upper and lower ends. The upper end had a tapered roller bearing to support both the radial and the axial loads. The lower end had a ball bearing with an internal smooth surface to allow the turbine's shaft to rotate with minimum friction.

(a) Conventional Rotor Setup

(b) 50%-STS Rotor Setup

(c) 100%-STS Rotor Setup

(d) Conventional Rotor Setup

(e) 50%-STS Rotor Setup

(f) 100%-STS Rotor Setup

Figure 3.9: Vertical Axis Wind Turbines Models in the ABL Wind Tunnel.

Two pulleys(one attached to the turbine shaft and the other to a DC motor) were connected using a belt as shown in Figure 3.10. The motor (DAYTON TENX 3XE19, 1/44 hp or 17 W, 1800 RPM) was located 30 cm from the center of the wind tunnel and it was connected to two separate digital multi-meters (Mastech MS8268 Digital AC/DC) in order to measure the voltage and current produced by the turbine. A rheostat (Rheostat, NBR series, Wirewound 50Ω 25W) was used as a power control device to adjust the current in order to maintain a constant rotational speed of the turbine. The rotational speed of the turbine models was measured using a tachometer (DT-2234C+). In this study, power curves were obtained for different tip speed ratios at rotational speeds of 600 and 700 RPM, respectively. The wind tunnel speed varied from 11 to 35 km/h throughout the entire experimental work, and three measurements were taken for each rotational speed.

Figure 3.10: Experimental setup of all turbine models.

3.2.2 Wake Measurements

3.2.2.1 Wake Regions

The wake downstream from the turbine (whether HAWT or VAWT) is normally divided into 1) the near wake region (i.e. starting right after the turbine and extending to $5D$) and 2) the far-wake region (i.e. from $15D$ to $20D$), as shown in Figure 3.11 [44]. The flow field in the near-wake region has non-uniform velocity with higher turbulence levels than the free stream. This has typically been attributed to the effect of rotor configuration, blade aerodynamics, and the vortices shed from the blades which increase the turbulence intensity level.

Figure 3.11: Schematic of the near and far wake behind the Darrieus turbine.

3.2.2.2 Constant Temperature Hot-Wire Anemometry (HWA)

The HWA technique is considered to be a useful for measuring mean and fluctuating velocity components instantaneously at a point in the flow field with good spatial and temporal resolution. This technique consists of a small platinum or tungsten wire,

typically of 5 μm diameter and 1 mm length, mounted on two stainless steel prongs and exposed against the movement of the fluid (see Figure 3.12). The working principle of the technique is based on electrical circuits, heat transfer, and fluid dynamics concepts combined to measure the fluid speed [100–105].

Figure 3.12: Schematic of a typical Hot-Wire Anemometer.

The wire is heated and maintained at a desired temperature and inserted into the air flow. Any change in fluid velocity will change the resistance (R_2) of the wire due to the heat transfer variation. Through a Wheatstone bridge (see in Figure 3.13), the wire (resistance R_2) is connected to other resistances (R_1 , R_3 , and R_4), which will become unbalanced when R_2 is changed. A differential feedback amplifier detects the imbalance and quickly adjusts the current flowing through the sensor while keeping the temperature constant (thermal inertia) and returning the bridge to a balanced mode. The output voltage across the bridge is proportional to the fluid velocity. HWA can also operate in constant current or constant voltage modes [100–102].

The magnitude of the power generated by the current along the wire is equal to the heat dissipated by convection.

$$P = I^2 R_w = h A_w (T_w - T_f), \quad (3.8)$$

Figure 3.13: CTA bridge circuit.

$$R_w = R_{Ref}[1 + \alpha(T_w - T_{Ref})], \quad (3.9)$$

where I is the input current, T_w and T_f are the temperatures of the wire and fluid, respectively, R_w is the resistance of the wire and a function of temperature, α is the thermal coefficient of resistance, R_{Ref} is the resistance at the reference temperature T_{Ref} , A_w is the projected wire surface area, and h is the heat transfer coefficient of the wire.

According to King's law, h is a function of fluid velocity V_f

$$h = a + b.(V_f)^c \quad (3.10)$$

where a and b are coefficients obtained from calibration, c depends on the hot wire structure and is approximately equal to 0.5.

By substituting the values from the Eq.3.9 and Eq.3.8 into the equation Eq.3.10, can be re-written as:

$$V_f = \left(\frac{\left[\frac{I^2 \cdot R_{Ref} [1 + \alpha(T_w - T_{Ref})]}{A_w (T_w - T_f)} - a \right]}{b} \right)^{\frac{1}{c}} \quad (3.11)$$

3.2.2.3 Instrumentation and data acquisition system

For the wake, a hot-wire probe connected to an anemometer unit (AN-1005 hot-wire anemometer, fabricated by A.A. Lab Systems Ltd) had been used. The anemometer was operated on CTA (Constant Temperature Anemometry) mode, where the wire was placed in a Wheatstone bridge with a constant temperature. The probe was calibrated against a Pitot-static tube, which was connected with a pressure transducer (i.e. PX653 pressure transducer manufactured by Omega) to a DAQ acquisition (i.e. NI-9205 from National Instruments). A PC equipped with the data acquisition card was employed to receive the output signal from the anemometer. To measure the instantaneous wind velocity at all designated points in the wake, the probe was mounted to a traversing arm. The output voltage of the probe was recorded with the sampling rate (SR) of 2.8 kHz, and for 30 seconds at each measurement location. The separation distance between the measurement locations of $\Delta x = 3$ cm and $\Delta y = 3$ cm were chosen as a compromise between overall mapping accuracy and computing post-processing time. Here, up to 2000 desired locations in the wake were recorded at each vertical and lateral planes (see Figure 3.14) for two different tip speed ratio regions: low region ($\lambda = 2.5$) and high region ($\lambda = 5.06$).

Figure 3.14: Schematic of the wake measurements: (a) lateral plane (b) vertical plane.

3.3 Aerodynamic Models of a Darrieus VAWT

The lift driven VAWTs (either straight or curved blades) have significant challenges because their blades spin parallel to the flow, creating very complex flow phenomena around the turbine blades (i.e stall flow, vortex shedding, and BVIs). Although this flow behaviour made the VAWT aerodynamic modelling difficult, many models have appeared in the literature for both H-Darrieus [54, 64, 106–109] and troposkien turbines [22, 63, 66, 110]. The most noticeable was the Vortex filament method proposed by Strickland [66]. This model has considered the unsteady effects of the vortices (that shed in the wake), and then, the velocity field induced by the vorticity including the velocity at the blades can be calculated. Thus, the model was able to predict the aerodynamic loads more accurately, even, when the turbines were operate in a medium ($4 < \lambda < 6$), where the BVI effect is strong. More recently, Fereidooni [110] combined this code with structural dynamic equations proposed by [111] to study the aeroelastic

behaviour of the 17-meter DOE-Sandia VAWT. In the current study, the same procedure of [66, 110] was used for developing an in-house aerodynamic code (using MATLAB) to predict the performance of the 50% and 100% STS-turbine and compare the performance to conventional predictions. This study attempted to assess the impact of blade vortex interaction on the aerodynamic performance of three troposkien VAWT (2-m, 5-m, and 17-m). One of the reason why the simulations were performed against the Sandia turbines (2-m, 5-m, and 17-m) instead of the obtained (wind tunnel) experimental data was due to unavailability of airfoil experimental data for low Reynolds number. This study was needed to investigate the scaling effects on the performance of the novel turbines. In the following subsections, the implementation of the MATLAB code is described.

3.4 Implementation of the Present In-house MATLAB Code

The following steps outline the numerical procedures based on [66] and [110] which were implemented into the in-house code. In the first step, the turbine blades were divided into a number of spanwise elements, "i" , as shown in Figure 3.15a (the element at the equator of a blade is enlarged for the sake of clarity; separate verification analysis indicated that 23 uniform elements with same length provided discretization convergence when power coefficients were concerned in the current simulations). A local coordinate system is defined and placed at the aerodynamic center of the airfoil (Figure 3.15b). Each element was associated to a single bound vortex strength, " Γ_b ", at a particular time step "j" and defined as

Figure 3.15: Blade elements and vortex filaments along with (a) Global coordinate system, and (b) local coordinate system.

$$\Gamma_{b(i,j)} = \frac{1}{2} C_{l(i,j)} c (|U_{R(i,j)}| + |U_{R(i+1,j)}|) / 2 \quad (3.12)$$

where C_l is the lift coefficient per unit span ($C_l = L / (0.5 \rho U_\infty^2 A)$), c is the airfoil chord length, and U_R (m/s) is magnitude of the relative flow velocity around the airfoil (taken as the average velocity between the values at the two spanwise ends of the element). The blade section lift C_l (airfoil lift coefficient) can be obtained from experimental data at different Reynolds numbers and angles of attack [112].

The relative velocity, $U_{R(i,j)}$, as seen by each element is given by the following relation:

$$\mathbf{U}_{R(i,j)} = (\mathbf{U} + U_\infty + U_t \cos \theta) \mathbf{i} + \mathbf{V} \mathbf{j} + (\mathbf{W} - U_t \sin \theta) \mathbf{k}, \quad (3.13)$$

where (U, V, W) is the velocity induced by the wake behind the blades (obtained from the contribution of all vortex filaments that were shed and being tracked), $U_t = R_i \omega$ is the element rotational velocity, and U_∞ is the freestream (wind) velocity. As a

consequence of changing the angle of attack during a cycle, the lift and the bound vortex strength also change accordingly. This change in the bound vortex strength results in the shedding of a spanwise vortex at the trailing edge of the airfoil at a certain time step, while keeping the change of the total circulation equal to zero (Kelvin's circulation theorem). The shed bound vortex strength is given by

$$\Gamma_{s(i,j)} = \Gamma_{b(i,j)} - \Gamma_{b(i,j-1)} \quad (3.14)$$

In order to numerically represent the change in bound vortex strength along the blade, trailing vortices perpendicular to the trailing edge must also be shed

$$\Gamma_{t(i,j)} = \Gamma_{b(i+1,j)} - \Gamma_{b(i,j)} \quad (3.15)$$

The shedding of vortex filaments was also depicted in Figure 3.15. The Adams-Bashforth integration method was used to calculate and update the positions of these vortex filaments in time. In the second step, the aerodynamic forces (tangential and normal) which act on the each element of the blade are determined by

$$F_{t(i,j)} = \frac{1}{2} C_{t(i,j)} \rho c l_i U_{R(i,j)}^2 \quad (3.16)$$

$$F_{n(i,j)} = \frac{1}{2} C_{n(i,j)} \rho c l_i U_{R(i,j)}^2 \quad (3.17)$$

where l_i is the spanwise length of each blade element and c is the element chord length. The normal and tangential coefficients can be obtained by

$$C_{t(i,j)} = C_{l(i,j)} \sin \alpha - C_{d(i,j)} \cos \alpha \quad (3.18)$$

$$C_{n(i,j)} = C_{l(i,j)} \cos \alpha + C_{d(i,j)} \sin \alpha \quad (3.19)$$

The dimensionless power coefficient for a single blade can be obtained from the summation of the contributions of all elements along the blade in time using

$$C_p = \frac{1}{NT} \sum_{j=1}^{NT} \sum_{i=1}^{NE} \frac{Q_{(i,j)} \omega}{\frac{1}{2} A_s \rho U_\infty^3} \quad (3.20)$$

where $Q_{(i,j)}$ is the torque generated by a blade element, ρ is the fluid density, A_s is the swept area (total frontal area of the revolving turbine), NT is the number of the time steps per revolution, NE is the number of blade elements, and ω is the blade angular velocity.

Here, the aerodynamic performance of the three configurations (conventional, 50%, and 100% STS-VAWT) were numerically investigated. In the first part of the work presented in Subsection 3.4.1, the performance ($C_p - \lambda$) of 2-m, 5-m, and 17-m Sandia turbines were investigated at low turbine aspect ratio ($AR = 1$; the ratio between the turbine height and its diameter). Whereas, the second part of the work (Subsection 3.4.2) were at high aspect ratio ($AR = 1.5$). The two main purposes of these investigations were; first, to perform larger-scale aerodynamic predictions of the novel turbines (50% and 100% STS-VAWT) and compare results to conventional turbine calculations; secondly to evaluate the effect of the aspect ratio (i.e. low and high aspect ratio) on the performance of the turbines. To ensure a consistent comparison, the geometry of each

configuration was modelled having two curved blades with a constant NACA airfoil along the blade. Due to their novelty, the 50% and 100% STS-VAWTs have not been examined previously with any modelling techniques. Several aspects of each rotor configurations are presented in the following subsections.

3.4.1 At Low Aspect Ratio: Constant Turbine Height with Various Blade Lengths

One of the main purpose of the current aerodynamic code was to determine the link between the aspect ratio of the three different configurations and their performance (power coefficient). Therefore, the aerodynamic performance of three different configurations were investigated numerically with two different aspect ratios ($AR = 1$ and $AR = 1.5$). In this set (low aspect ratio), the turbine height-to-diameter ratio of each configuration was selected to be one, i.e., the three different configurations of each turbine model (conventional, novel shape 50% STS, or 100% STS) was modeled having the same parameters as shown in Figure 3.16.

Figure 3.16: Geometry of the troposkien VAWT configurations with low aspect ratio: (a) Conventional (b) 50% STS (c) 100% STS.

3.4.1.1 Conventional Model Configurations

Figure 3.17: Conventional VAWT Configurations for: (a) 2 meters (b) 5 meters (c) 17 meters.

Table 3.3: Specifications of the conventional configurations for 2-m, 5-m, and 17-m DOE-Sandia VAWTs

Parameters	2-meter	5-meter	17-meter
Number of blades (N)	2	2	2
Turbine radius (r) [m]	1	2.55	8.275
Turbine height (H) [m]	2	5.10	16.55
Blade height (h) [m]	2	5.10	16.55
Blade length (l) [m]	2.97	7.5300	24.273
Swept area (A_s) [m ²]	2.59	16.99	175
Chord length (c) [m]	0.08815	0.1524	0.61
Solidity, σ	0.202	0.135	0.169
l/R ratio	2.97	2.95	2.93
c/R ratio	0.08815	0.059	0.073
AR ratio	1	1	1
c/L ratio	0.029	0.020	0.025
h/R ratio	2	2	2
β_t ratio	1	1	1
β_b ratio	1	1	1
Blade airfoil (NACA)	0012	0015	0015

Based on Sandia experimental data [22, 113, 114], the conventional shape of each of the three diameters is illustrated in Figure 3.17. Table 3.3 summarizes several parameters that describe the shape of the conventional VAWT, including the rotor solidity $\sigma =$

Ncl/A_s , the chord-to-radius ratio, c/R , the chord-to-length ratio, c/l , β_t is the turbine ratio (see the Subsection 3.2.1.1), and the rotor aspect ratio $AR = H/D$.

Another ratio defined in this work was the blade β_b ratio (which is the maximum deflection of the turbine blade from the axis of rotation (R) to half of the blade height ($h/2$), as shown in Figure 3.16). Regardless of the rotor size, both β_b and β_t ratios were the same for the conventional troposkien turbine, since the turbine's height was the same as the blade's height. In contrast, both values β_b and β_t ratios were different for the shifted troposkien shape turbines, i.e., 50% or 100% STS.

3.4.1.2 50% STS Model Configurations

Table 3.4 and Figure 3.18 illustrate the details of the 50% STS-VAWT geometry of the 2-m, 5-m, and 17-m turbines. The main modifications that were made to the 50% STS-VAWT configurations were as follows:

- Turbine blade heights were decreased by 33.3% with respect to the conventional one.
- Turbine blade lengths of 2-m, 5-m, and 17-m turbines were decreased by 12.79%, 15.67% and 15.7%, respectively.
- β_b ratios were increased from 1 to 1.5.
- l/R ratios of the 2-m, 5-m, and 17-m turbines were decreased by 13.7%, 14.8% and 14.8%, respectively.

Figure 3.18: 50% STS VAWT Configurations for: (a) 2-m (b) 5-m (c) 17-m DOE-Sandia VAWTs.

Table 3.4: Specifications of the 50%STS configurations for 2-m, 5-m, and 17-m DOE-Sandia VAWTs.

Parameters	2-meter	5-meter	17-meter
Number of blades (N)	2	2	2
Turbine radius (r) [m]	1	2.55	8.275
Turbine height (H) [m]	2	5.10	16.55
Blade height (h) [m]	1.3347	3.4	11.036
Blade length (l) [m]	2.59	6.35	20.46
Swept area (A_s) [m ²]	2.59	16.99	175
Chord length (c) [m]	0.08815	0.1524	0.61
Solidity, σ	0.17	0.11	0.14
l/R ratio	2.59	2.49	2.47
c/R ratio	0.08815	0.059	0.073
AR ratio	1	1	1
c/L ratio	0.034	0.024	0.029
h/R ratio	1.33	1.33	1.33
β_t ratio	1	1	1
β_b ratio	1.5	1.5	1.5
Blade airfoil (NACA)	0012	0015	0015

3.4.1.3 100% STS Model Configurations

Table 3.5 and Figure 3.19 show the details of the 100% STS-VAWT geometry used here.

The main modifications that were made to the 100%STS-VAWT configurations for the

three diameters were as follows:

Table 3.5: Specifications of the 100%STS configurations for 2-m, 5-m, and 17-m DOE-Sandia VAWTs.

Parameters	2-meter	5-meter	17-meter
Number of blades (N)	2	2	2
Turbine radius (r) [m]	1	2.55	8.275
Turbine height (H) [m]	2	5.10	16.55
Blade height (h) [m]	1	2.55	8.275
Blade length (l) [m]	2	5.82	18.86
Swept area (A_s) [m ²]	2.59	16.99	175
Chord length (c) [m]	0.08815	0.1524	0.61
Solidity, σ	0.136	0.104	0.131
l/R ratio	2.00	2.28	2.28
c/R ratio	0.08815	0.059	0.073
AR ratio	1	1	1
c/L ratio	0.044	0.026	0.032
h/R ratio	1	1	1
β_t ratio	1	1	1
β_b ratio	2	2	2
Blade airfoil (NACA)	0012	0015	0015

Figure 3.19: 100% STS VAWTs Configuration for: (a) 2-m (b) 5-m (c) 17-m DOE-Sandia VAWTs.

- Turbine blade heights were decreased by 50% with respect to the conventional one.
- Turbine blade lengths of 2-m, 5-m, and 17-m turbines were decreased by 32.6%, 22.7% and 22.3%, respectively.
- β_b ratios were increased from 1 to 2 compared to the conventional design.

- l/R ratio of 2-m, 5-m, and 17-m turbines were decreased by 31.03%, 21.3% and 21.3%, respectively.

3.4.2 At High Aspect Ratio: Various Turbine Heights with Constant Blade Length

Figure 3.20: Geometry of the troposkien VAWT configurations with high aspect ratio: (a) Conventional (b) 50% STS (c) 100% STS.

The purpose of this study was to analyse the link between the aspect ratio of the troposkien turbine (whether the conventional or novel designs) and its performance (power coefficient). Since the effects on the VAWT's performance depended on the rotor solidity, blade number, rotor shape, and the blade area (length or chord), the turbine heights of the novel 50% and 100% STS were increased (see Figure 3.20), while the turbine radius was kept the same as in the Subsection 3.4.1. Notice that the height and the length of the novel turbine blades (50% and 100% STS) were kept the same as that of the conventional models, while the opposite was in set I (see Subsection 3.4.1).

3.4.2.1 Conventional Model Configurations

The aspect ratio of the conventional troposkien in this set (high aspect ratio) was kept the same as the corresponding model in set I (AR=1, see Figure 3.20).

3.4.2.2 50% STS Model Configurations

Table 3.6 summarizes the main geometrical features of the 50% STS-VAWT configuration for the three different turbine diameters. The most noticeable changes in this table were that the aspect ratio of the novel turbine was increased (AR=1.5) compared to the corresponding model in set I (AR=1), whereas, the rotor solidity was decreased by 33%.

Table 3.6: Specifications of the 50%STS configurations for 2-m, 5-m, and 17-m DOE-Sandia VAWTs.

Parameters	2-meter	5-meter	17-meter
Number of blades (N)	2	2	2
Turbine radius (r) [m]	1	2.55	8.275
Turbine height (H) [m]	3	7.5	24.825
Blade height (h) [m]	2	5.1	16.55
Blade length (l) [m]	2.97	7.5300	24.273
Swept area (A_s) [m ²]	4.43	29.08	299.55
Chord length (c) [m]	0.08815	0.1524	0.61
Solidity, σ	0.118	0.078	0.098
l/R ratio	2.97	2.95	2.93
c/R ratio	0.08815	0.059	0.073
AR ratio	1.5	1.5	1.5
c/L ratio	0.029	0.020	0.025
h/R ratio	2	2	2
β_t ratio	0.667	0.667	0.667
β_b ratio	1	1	1
Blade airfoil (NACA)	0012	0015	0015

3.4.2.3 100% STS Model Configurations

The 100% STS configurations of the three turbines (2-m, 5-m, and 17 m) have employed aspect ratios of 2 (see Table 3.7).

Table 3.7: Specifications of the 100%STS configurations for 2-m, 5-m, and 17-m DOE-Sandia VAWTs.

Parameters	2-meter	5-meter	17-meter
Number of blades (N)	2	2	2
Turbine radius (r) [m]	1	2.55	8.275
Turbine height (H) [m]	4	10.1	33.1
Blade height (h) [m]	2	5.1	16.55
Blade length (l) [m]	2.97	7.5300	24.273
Swept area (A_s) [m ²]	5.18	33.98	350.82
Chord length (c) [m]	0.08815	0.1524	0.61
Solidity, σ	0.101	0.066	0.086
l/R ratio	2.97	2.95	2.93
c/R ratio	0.08815	0.059	0.073
AR ratio	2	2	2
c/L ratio	0.029	0.020	0.025
h/R ratio	2	2	2
β_t ratio	0.5	0.5	0.5
β_b ratio	1	1	1
Blade airfoil (NACA)	0012	0015	0015

Chapter 4

Experimental and Numerical Results

The objective of this chapter is to present the results obtained from this research. The results and comparisons of the conventional model versus the 50% STS and the 100% STS models were divided into two sets: The first set of experiments were performed for the turbines operating in the transition zone (tip speed ratio, TSR, between 4 and 6), and the second set was done for the turbines running at low TSR (lower than 4). Initially, the results and comparisons of the aerodynamic performances of conventional model versus the 50% STS and the 100 % STS models are illustrated in the figures of this chapter. It is important to note that the curves represented not only the average values but also include the standard deviation of three measurements taken for each prototype. Secondly, both lateral and vertical wake profiles of velocities and turbulence intensities through the center of the turbines were obtained for both sets. The free stream velocity of the wind tunnel flowed from the left side of the figures with $U = 5.72$ m/s throughout the entire experiment of “set I”, while the turbine was rotating in the counter-clockwise direction (bird’s eye view). Thus, the Reynolds number based on the tip speed and the chord length was $Re = 2.3 \times 10^5$. The measurements of the second set, however, were

performed at a tip speed ratio of $\lambda = 2.5$.

4.1 Aerodynamic Performance of the Turbine Models

4.1.1 Set I Prototype Turbines: Significant BVI Effect (Transition Zone)

Wind Tunnel Blockage Corrections:

The blockage ratio (BR, the ratio between the turbine swept area, A_s , and the test-sectional area, A_t), for the Set I prototype turbines was 19.1%. In wind turbine experimental studies, the results must be corrected if the blockage ratio is more than 10% [115–121]. This value (19.1%) is considered a high blockage ratio which causes the air to flow faster through the gap between the turbine and the tunnel side walls, pushing the turbine to produce more power when compared to what the turbine would produce in reality (unbounded flow). The reason behind designing a prototype turbine with this high of a swept area (i.e. $BR = A_s/A_t$) was to ensure that the turbine would operate in high enough TSR, where the BVI could be examined for the different configurations. Thus, correcting this higher blockage effect was particularly important to make sure that the experimental results resembled those obtained in unbounded free-stream conditions. Ideally, the usage of a larger wind tunnel (unavailable at the time of the experiments) would be more advantageous to reduce BR. However, this will be the subject of future work.

For VAWTs, Sandia Laboratories adopted a correction proposed by Pope and Harper [122] as the standard method for their wind tunnel blockage corrections. This method

was used also by several other authors [116, 120]. In this method, the “blocked” wind tunnel values of power coefficient, C_P , tip speed ratio, λ , and velocity, U , should be correlated to unconfined corrected values, C_{Pc} , λ_c , and U_c as follows

$$C_{Pc} = C_p \left(\frac{U}{U_c} \right)^3 \quad (4.1)$$

and

$$\lambda_c = \lambda \left(\frac{U}{U_c} \right). \quad (4.2)$$

The power coefficient is defined by

$$C_p = \frac{P}{\frac{1}{2} \rho U_\infty^3 A_s}. \quad (4.3)$$

where P is the power produced by the turbine, ρ is the air density, A_s is the turbine swept area, and U is the wind speed. The tip speed ratio is defined as

$$\lambda = \frac{\omega R}{U}. \quad (4.4)$$

Pope and Harper [122] proposed the following blockage correction equation for unusually shaped objects

$$\epsilon_{wt} = \frac{1}{4} \frac{A_f}{A_t} \quad (4.5)$$

where the frontal area, A_f , includes the turbine swept area and the rig frontal area

(turbine supports and exposed experimental components) and A_t is the wind tunnel cross section area (1.68 m $\times$ 1.12 m). The corrected velocity becomes

$$U_c = U (1 + \epsilon_t) \quad (4.6)$$

When taking into account the conservation of mass before and around the blockage

$$\rho U A_t = \rho U_{side} (A_t - A_f), \quad (4.7)$$

the correlation could be rewritten as

$$\epsilon_t = \frac{1}{4} \frac{A_f}{A_t} = \frac{1}{4} \left(1 - \frac{U}{U_{side}} \right). \quad (4.8)$$

If the area ratio is used, a single correction is obtained ($\epsilon_t=0.0489$ for set I). If velocity measurements are available, the velocity ratio could also be used to estimate the correction factor, ϵ_t . In this study, the velocities were measured at two different locations by using a hot-wire anemometer technique (AN-1005, A.A. Lab Systems Ltd), as shown in Figure 4.1. The first location was at 5D upwind of the rotor (i.e. before the turbine, U), while the second location was between the turbine and the tunnel wall during the rotation of the turbines (i.e. where the greatest blockage effects occurred, U_{side}). Therefore, the power output and tip-speed ratio of turbine models were corrected by modifying the approach used by Pope and Harper [122]. This correction was considered to better represent the blockage effects due to turbine rotation and operating conditions (tip speed ratio). It should be noted that, the velocities are the average reading obtained from three measurements, where each measurement is calculated for 30 second samples

under the same operating conditions.

Figure 4.1: A schematic of the velocity measurement locations at the ABL (Atmospheric Boundary Layer) wind tunnel: (a) Hot-wire anemometer technique setup (b) Top view of the rotor inserted into the ABL wind tunnel where U and U_{side} are velocities, A_T , A_s are areas for the tunnel and the turbine (c) Front view of the velocity measurement locations for the three different configurations.

The results are shown in Figure 4.2, which illustrate the velocity ratio (U/U_{side}) comparison of the set I prototype turbines as a function of TSR and wind velocity for both 600 rpm and 700 rpm , respectively. As shown in Figure 4.2c and Figure 4.2d, the velocity ratio increases as the free-stream velocity is increased regardless of the model rotors. Conversely, the velocity ratio decreases when the tip speed ratio is increased (see Figure 4.2a and 4.2b). Additionally, these figures also indicate that the U/U_{side} vs. λ and U/U_{side} vs. U curves of the three turbines are fundamentally different, although all the turbines have the same swept area. This is because the blockage ratio is not only based on the turbine swept area and the test-sectional area but also critically dependent upon the rotational aerodynamic differences between the three geometry designs.

Figure 4.2: Set I Prototype Turbines: velocity ratio as a function of TSR and free stream velocity at 600 *rpm* and 700 *rpm*.

Figures 4.3 and 4.4 show the uncorrected power coefficient results (conventional troposkien VAWT) as function of the tip speed ratio in comparison to the two corrected values using the standard Pope and Harper ([122]) method, Eq.4.5, giving a constant correction based on area blockage ratio, and the modified Pope and Harper method proposed here using hot-wire anemometry (HWA) velocity information, Eq.4.8. As expected, the corrected curves show smaller values when compared against the uncorrected curve. The modified Pope and Harper method provides a more aggressive correction than the standard one, but takes into account effects of rotation, geometry, and flow speed.

Figure 4.3: Set I Prototype Turbines: Comparison of the Pope & Harper corrections with the experimental wind tunnel corrections for the conventional rotor at *600 rpm*.

Figure 4.4: Set I Prototype Turbines: Comparison of the Pope & Harper corrections with the experimental wind tunnel corrections for the conventional rotor at *700 rpm*.

Power Coefficient Curves:

As mentioned previously, the set I prototype turbines operate at higher TSR where the BVI effect could be examined over different turbine configurations. The measured performance curves (C_P versus λ) of the turbines with the modified Pope and Harper

blockage effect corrections are depicted in Figures 4.5 and 4.6 for both 600 rpm and 700 rpm, respectively. The curves present the average and the standard deviation of three measurements taken for each prototype.

Figure 4.5 shows the power coefficient measurement results at 600 rpm for the conventional VAWT (black line), the 50% STS-VAWT (light gray line) and the 100% STS-VAWT (dotted gray line), clearly indicating a superior performance of the 50% STS model. The highest C_p value for the 50% STS model was 0.24 when λ was 3.13, while the highest C_p for the conventional model was only 0.206 when λ was 3. Conversely, the maximum C_p value of 100% STS model was obtained at 0.18 corresponding to 3.15 of λ , which was the lowest value compared with the other models. From this figure, it is observed that when λ is increased, the 50% STS model outperforms the conventional one by greater margins.

Figure 4.5: Set I Prototype Turbines: Power Coefficient (C_p) vs. TSR at at 600 rpm.

When λ increased, as shown in Figure 4.6 (at 700 rpm), the 50% STS-VAWT model still had the largest peak value $C_p = 0.23$ corresponding to $\lambda = 3.2$ compared to the second largest peak value $C_p = 0.2$ for the 100% STS-VAWT model (i.e. the 100%

STS-VAWT model had a significant improvement when compared to the lower RPM test and it reached a peak above the conventional model). Although the 50% STS-VAWT model has the lower rotor solidities (which greatly affects the performance of the 50% STS such as power coefficient, rotor shaft torque and power extracted by the wind turbine) compared to the other two troposkien designs, it outperformed regardless of the turbine rotational speeds that were investigated. The reason behind this is that the BVI effect was partially mitigated by the 50% STS model design when compared to the conventional model. It is important to point out that the power coefficient of 50% STS model can be increased further with increasing the rotor solidity (by changing the chord) to match solidity of the conventional design. The 100% STS model also had a greater BVI effect reduction, demonstrated through a large drop in the peak power coefficient mainly due to the smaller blade and arc length (less power generation when compared to the other two turbines).

Figure 4.6: Set I Prototype Turbines: Power Coefficient (C_p) vs. TSR at at 700 rpm.

4.1.2 Set II Prototype Turbines: Minimal BVI Effect (Dynamic Stall Zone)

To verify that the increase in performance of the 50% STS model compared to the conventional model was because of reduced BVI, at second set was made. In this set, only two different turbine models (i.e. the conventional and 50% STS model) were built and tested under the same conditions. The turbine height, blade height, number of blade and chord length of these models were kept the same as in Table 3.2, however, a smaller radius of 0.25 m (9.84 in) was tested. These two rotor models were modeled to generate tip speed ratios (λ) that were in the dynamic stall region (see Figure 3.2) where BVI was expected to have a smaller effect.

Wind Tunnel Blockage Corrections:

The blockage effects of the set II prototype turbines were reduced to up to 12.8% due to a decrease in the radius of rotation. The purpose of this set was to allow the turbines to operate at a lower range of TSR (between 2 to 3), where the BVI effect is normally negligible. Although this ratio was smaller than Set I, it is still considered a relatively high blockage ratio (bigger than 10) and should be corrected. Figure 4.7 shows the measured velocity ratio U/U_{side} vs. TSR for the set II turbines (conventional and 50% STS-VAWT only) at 600 and 700 rpm, having similar distributions as set I (see again Figure 4.2) but increased values due to the difference in blockage ratio between the two sets.

Figure 4.7: Set II Prototype Turbines: velocity ratio as a function of TSR and free stream velocity at **600 rpm** and **700 rpm**.

The measured power coefficient (C_p) of the set II prototype turbines (with the modified Poper and Harper correction) as a function of TSR for 600 and 700 rpm is shown in Figures 4.8 and 4.9, respectively. According to these figures, three observations were made: 1) Over a significant portion of the TSR range (between 2 and 3), the 50% STS model showed a lower performance when compared to the conventional model. 2) The peak of the power coefficient (C_p) occurred at a higher TSR for the 50% STS model. 3) As the TSR was increased, the gap between the performance of the two models narrowed down such that for the TSRs greater than 3 (for the 700 rpm plot), the 50% STS model slightly outperformed the conventional troposkien shape turbine. The above

experimental results were summarized by taking the C_p peak values corresponding of λ of each turbine models for both BVI regions and presented in the Table 4.1. The better performance of the conventional turbine was expected since it produces more power (larger blades than the 50% STS model) while the BVI effect is not dominant and does not reduce its performance for this lower TSR operational range.

Figure 4.8: Set II Prototype Turbines: Power Coefficient (C_p) vs. TSR at at 600 *rpm*.

Figure 4.9: Set II Prototype Turbines: Power Coefficient (C_p) vs. TSR at at 700 *rpm*.

Table 4.1: A summary of the overall performance of all turbine models.

Tip speed ratio regions	RPM	Conventional		50% STS		100% STS	
		λ	C_p	λ	C_p	λ	C_p
Transition region	600	3.71	0.25	4.13	0.29	4.13	0.23
	700	4.23	0.247	4.41	0.276	4.41	0.24
Dynamic stall region	600	2.5	0.24	2.61	0.21	—	—
	700	2.5	0.25	2.5	0.22	—	—

4.2 Wake Measurements of the Turbine Models

Results indicated that the power coefficient differences between the tested models were not affected by the blockage ratio corrections, as depicted in the figures of the previous sections. Based on these results, it is assumed herein that any difference in the wake downstream of these turbine models is caused by the differences in their design geometry. However, the blockage only affected the recovery rate of the wake downstream from all of the models due to the fact that all turbine models have the same swept area.

4.2.1 Set I Prototype Turbines: Significant BVI Effect (Transition Zone)

4.2.1.1 Streamwise Velocity Deficit

Experiments of the near-wake downstream were performed at a tip speed ratio $\lambda = 5.06$, as shown in Figure 4.10, for each turbine (conventional, 50% STS, and 100% STS) for vertical and lateral (horizontal) mid-planes (see Figure 3.14 for the measurement plane configurations). Regardless of the turbine models, the velocity deficit was significantly higher at the center of the wake (see Figures 4.10a, 4.10c, and 4.10e) where most of the

power is generated. However, the deficit gradually diminished, producing a wedge shaped region as the wake progressed downstream. Although some similarities in the overall shape of this region have been observed between the 50% STS and the conventional model, the differences in their wake length and width were clearly distinguishable (see Figures 4.10a and 4.10c). The wake length and width of the 50% STS turbine decreased by approximately 10% and 16% respectively, when compared to what the conventional produced. These differences were explained by the fact that the 50% STS model has less blade area (i.e. height and length) interacting with the wind, resulting in less BVIs especially at the center of the turbine where one blade was shifted vertically with respect to the other blade. On the other hand, the 100% STS model, demonstrated two velocity deficit regions: (i) a region where vortices moved quickly inwards (due to the rotation of the rotor), and (ii) a region where the deficit was narrower (see Figure 4.10e). This was due to the unique configurations of this model (i.e. shorter blade length).

By comparing the lateral velocity profile, shown in Figures 4.10b and 4.10d, the conventional wake was flat with a sharper region at the upper side of the figure due to the turbine rotation direction (counter-clockwise from the top view). In other words, the upwind blade vortices were released earlier and then moved downstream throughout the wake. The lateral velocity contours of the 100% STS model (4.10f) demonstrated a narrow lateral wake expansion because the measurements were performed through the center (horizontal mid-plane) of the turbine.

Figure 4.10: Contour of velocity deficit proles at both vertical (left column) and lateral (right column) planes (Set I): (a) Conventional, (b) 50% STS, (c) 100% STS.

In Figure 4.11, vertical crosswind profiles of the velocity deficits were compared between the conventional and 50% STS model at various downstream locations (i.e. 1D, 2D, 3D, and 3.5D). The expansion shapes of both turbine wakes were quite similar, but

the wake from 50% STS model recovered (x-axis values going towards zero) more rapidly than the conventional wake. Note that the gap differences between the two wake models increased as the wake expansion downstream of the rotor, i.e., three rotor diameters downwind of the turbine. At 3.5 rotor diameter downstream of the turbine, the velocity deficit differences increased (around 35%), compared to values at 1 D.

Figure 4.11: Comparison of vertical crosswind profiles of normalized streamwise velocity deficit in the wake, between conventional model and 50% STS-VAWT model at various downstream locations: (a) at 1D (b) at 2D (c) at 3D (d) at 3.5D.

4.2.1.2 Wake Turbulence Intensity

The effect of the turbulence intensities (TIs, i.e. the ratio of the turbulence root mean square velocity intensity to the steady wind speed upwind of the rotor) for each turbine model is shown in Figure 4.12 at both vertical and lateral profiles. Both plane results

have indicated a highly turbulent region occurred in the center of the wake (mainly due to the reasons stated earlier), but it gradually decreased as the wake expanded further downstream. Whereas, very low levels of turbulence was observed behind both sides of the rotor. Also seen in Figure 4.12 (a, b, c, and d), the turbulence of the 50% STS modeled not drastically increase (being smoothly distributed along the wake), and was much less than the level observed from the conventional model. The benefit of this was expected to be more important for a turbine placed within an array (i.e. installed at the wind farm) through an increase of its fatigue life, reduction of its blade vibrations, and improvement of its performance. The contours of the turbulence intensity profile of the 100% STS model, shown in Figure 4.12e and 4.12f, demonstrated that the turbulence intensity of the top blade was stronger than the lower one. However, significant turbulence was not observed in the lateral measurement plane downstream of this rotor (Figure 4.12f). This behavior was expected since the lateral profiles of turbulence were obtained through the center of the turbines, i.e., between the rotor blades.

Figure 4.12: Contour of turbulence intensity profiles at vertical plane (Set I): (a) Conventional (b) 50% STS (c) 100% STS.

4.2.2 Set II Prototype Turbines: Minimal BVI Effect (Dynamic Stall Zone)

4.2.2.1 Streamwise Velocity Deficit

In Figures 4.13a, 4.13b, 4.13c and 4.13d, two-dimensional contours of the streamwise velocity for the conventional and 50% STS models are presented in the both vertical and lateral planes. Although, the deficit was roughly the same as what was observed in set I (i.e. it decreased as the wake moves downstream), the conventional induced wake was recovered faster than the wake behind the 50% STS model, which was contrary to the results of set I. This behaviour was expected, as previous power measurements (see Figures 4.13c and 4.13d) have demonstrated that the performance of the conventional model was better than the 50% STS, particularly at this region of TSR (i.e., $\lambda_c < 4$). This was assumed to be due to less interaction between the vortices and the downstream half of the rotor (i.e. reduced BVI effect).

Figure 4.13: Contour of velocity deficit profiles at both vertical and lateral planes (Set II): (a) Conventional (b) 50% STS (c) Conventional (d) 50% STS.

4.2.2.2 Wake Turbulence Intensity

In Figure 4.14, the turbulence intensity levels were generally quite high in the core of the wake, however outside this core, the level of turbulence diminished rapidly. The maximum level of turbulence intensity behind the 50% STS rotor was slightly higher (i.e. at 2.4D downwind) than the maximum level obtained behind the conventional model.

Figure 4.14: Contour of turbulence intensity profiles at vertical plane (Set II): (a) Conventional, (b) 50% STS, (c) Conventional, (d) 50% STS.

4.3 Numerical Analysis of The Turbine Models

4.3.1 Numerical Validation

Performance ($C_p - \lambda$) simulation predictions (i.e. in-house MATLAB code) of three conventional VAWT configurations (2-m, 5-m, and 17-m) were validated with test data available in the literature [26, 58, 59] and other well-known aerodynamic code results. The experimental power coefficient of Sandia 2-meter was plotted versus tip-speed ratio ($C_p - \lambda$) in Figure 4.15a, in addition to current simulation predictions results. As illustrated it can be seen that the current aerodynamic model showed good agreement,

particularly at a TSR beyond the peak ($\lambda = 4.6$). Conversely some discrepancies were observed in the lower tip speed regions ($2.5 < \lambda < 4.5$).

Figure 4.15: Comparison of power coefficients between experimental data and different aerodynamic code results including the present models: (a) Sandia 2-m at 267 rpm (b) Sandia 5-m at 162.5 rpm (c) Sandia 17-m at 50.6 rpm.

This could be justified by the fact that the airfoil coefficient (C_l and C_D) values obtained from the experimental database were less accurate at low Reynolds number (due to liner interpolation and data sparsity). In the Figure 4.15b, the power coefficient curve of Sandia 5-m VAWTs was predicted by the DMST (Double-Multiple Stream Tube) model, MST (Multiple Stream Tube) model, and the present model. The differences between the predictions of the two previous models and the experimental data were significant, particularly at high range of TSR ($\lambda \geq 4.5$), whereas the results of the present

model agreed well with the experimental data over a wide range of tip speed ratios. Figure 4.15c depicts the final comparison between the present code (in-house MATLAB code) and the Sandia 17-meter experimental data, however, they were compared with predictions made with three well-known aerodynamic codes, as reported by Touryan [21]. As the scale of the turbine increased (turbine size), the Reynolds numbers increased and led to improvement of the blade performance in general. Here, all models were in good agreement with experimental data over most of the TSR values. However, despite over prediction, the present simulation results seem to be in overall good agreement in contrast to the experimental data over a wide range of tip-speed ratios.

4.3.2 Convergence Analysis

Prior to the numerical analysis of the performance of the VAWTs, it was vitally important to verify how accurate the performance of the conventional rotor was predicted and if the simulation was fully converged. Thus, it was essential to investigate the effects of the azimuth angle increment (smaller increment indicating smaller time step) on the resulted maximum power coefficient of each rotor configuration. Furthermore, the number of cyclic revolutions were varied until the convergence was obtained. The variation in an increment was plotted against the power peak value as shown in Figure 4.16a. From the results, there was no considerable improvement when decreasing the angle increments below 15 degrees. Thus, an angle increment of 15° was determined to evaluate and estimate the power coefficient value adequately.

Figure 4.16: Effects of azimuth angle increment and number of revolutions on maximum power coefficient: (a) angle increment conditions (at peak power) (b) number of revolutions of Sandia 17-Revolutionsm at 50.6 rpm.

As shown in Figure 4.16b, the performance of the turbine converged as the number of turbine revolutions increased (after about 15 revolutions). To reduce the computational run-time and the complexity of the code implementation, all of the simulations were conducted using 15 turbine revolutions. The speed of the turbine was kept at a constant rotational speed for the entire simulation.

4.3.3 Aerodynamic Performance Prediction of the Turbine Models

4.3.3.1 Low Aspect Ratio: Constant Turbine Height with Various Blade Lengths

A comparison of the power coefficients as a function of the tip speed ratio between simulation results for the 2-m conventional VAWT (light line), 2-m 50% STS-VAWT (black line), 2-m 100% STS (gray line) and the Sandia experimental data (circle symbols) is presented in Figure 4.17. It is clearly demonstrated that the predicted performance using MATLAB analysis was in good agreement with experimental findings however,

some discrepancies were noticed at the lower tip speed region due to the low accuracy in 2D airfoil data at low Reynolds number. At 267 rpm (see Figures 4.17a), the highest power coefficient was obtained by the 50% STS- VAWT ($C_P = 0.34$ at $\lambda = 5.8$), followed by the conventional VAWT ($C_P = 0.3$ at $\lambda = 4.85$), and the 100% STS-VAWT ($C_P = 0.2$ at $\lambda = 5.3$). The superior performance of the 50% STS-VAWT in contrast to the other two configuration data was expected due to less interaction between the wake of a blade and the power producing section of the second blade, as discussed before. When the turbine rotation speed was increased from 267 rpm to 400 rpm, the BVI became stronger because the blade wake stayed in the blade's path for several blade revolutions. Thus the generated aerodynamic performance of the 50% STS model was higher and peaked at higher tip speed ratios of $\lambda = 7.6$ (see Figures 4.17b). The 100% STS model had even less interactions when compared to the 50% STS or conventional model, however, produced the least power of both sets (267 and 400 rpm). This was mainly due to the geometry constrictions such as lower solidity σ , higher β ratio and smaller blade height compared to the 50% STA-VAWT. These aspects were known to have negative impacts on the aerodynamic performance as reported before [18].

Figure 4.17: Comparison of power coefficient of Sandia 2-m between experimental data and the present models results: (a) Sandia 2-m at 267 rpm (b) Sandia 2-m at 400 rpm.

The performance simulation results of Sandia 5-m VAWT at 162.5 and 175 rpm are shown in Figures 4.18 a and 4.18 b, respectively. At low TSR region (2 to 4.5) where the BVI effect is negligible, the performance of the conventional turbine was marginally better than the other two configurations. When the TSR increased, the BVI effects became stronger, because the blade wake stayed in the blade's path for several blade revolutions. The 50% STS-VAWT model outperformed the other configurations and reached a peak power coefficient of 0.392 at 162.5 rpm. However, 100% STS-VAWT also showed a good coefficient at λ beyond 8.2. As shown in Figures 4.18b at a rotational speed of 175 rpm, the 50% STS-VAWT produced higher power coefficient values (especially at higher tip speed ratios). Thereby, the maximum power coefficient of the 50% STS-VAWT was 0.384, while the power coefficient of the conventional VAWT and the 100% STS-VAWT were $C_p = 0.34$ and 0.275, respectively. This performance improvement was expected because of the BVI mitigation.

Figure 4.18: Comparison of power coefficient of Sandia 5-m between experimental data and the present models results: (a) Sandia 5-m at 162.5 rpm (b) Sandia 5-m at 175 rpm.

Larger VAWT (17-m) simulation results indicated that 50% STS-VAWT have higher peak power coefficients than the other two turbines tested (conventional and 100% STS-VAWT) for both 42.7 rpm (Figure 4.19a) and 50.6 rpm (see Figure 4.19b). However,

note that the conventional rotor had the best performance for lower tip speed ratios (approximately, $\lambda < 6$). Conversely, while 100% STS power curve had lower performance than the others in both conditions (42.7 and 50.6 rpm) for most of the tip speed ratio range.

Figure 4.19: Comparison of power coefficient of Sandia 17-m between experimental data and the present models results: (a) Sandia 17-m at 42.7 rpm (b) Sandia 17-m at 50.6 rpm.

As shown previously, the 50% STS performed better in terms of power coefficients than the conventional and the 100% STS configuration within the range of rotation and turbine sizes. This performance was due to the difference in the geometry design of 50% STS turbine compared with the conventional. As the 50% STS turbine blade traveled along its circular path, only the lower section of the shifted blade interacted with the second blade wake. Thus, two distinct regions were generated: (i) a region where the BVI effect was significant which occurred close to the turbine center (Figure 4.20a, dots representing the centroids of vortex filaments, and colour indicating time), and (ii) a region where the vortices moved quickly downwind of the rotor (minimal BVI effects). Conversely, for the conventional turbine, (see Figure 4.20b), the generated vortices impeded the second blade entirely, thus maximizing the BVI, especially at the

producing section (equator). This interaction influences not only the power production of the turbine but also the lifespan of the blades. Although the 100% STS rotor based design was quite promising and indeed seems to greatly diminish the BVI effects, it displayed a performance smaller than the other two configurations (Figure 4.20c). This was due to the smaller blade length of the 100% STS model, causing a drop in the generated lift and, subsequently the generated torque by the turbine blade.

Figure 4.20: Wake structure : (a) 50% STS-VAWT configuration (b) conventional VAWT configuration (c) 100% STS-VAWT configuration.

Therefore, the 50% STS generated less blade vortex interactions which produced more power when compared with the conventional and the 100% STS-VAWT configurations. It was concluded that although the three different configurations have the same rotor parameters (i.e. overall height, radius, NACA airfoil, and swept area), the difference in

their aerodynamic performance (in terms of power coefficients) was due to differences in their design geometry. In other words, the aerodynamic performance of VAWTs, regardless of the size and configuration of the turbine, was related to the blade-vortex interaction effects.

4.3.3.2 High Aspect Ratio: Various Turbine Heights with Constant Blade Length

The effect of aspect ratio (Figure 4.21) on the power coefficient was evaluated numerically and plotted in Figures 4.22, 4.23, and 4.24. As can be seen in these figures, the power coefficient curves were significantly affected when the aspect ratio (turbine's height divided by its maximum diameter of rotation) was increased.

Figure 4.21: Geometry of the troposkien VAWT configurations with high aspect ratio: (a) Conventional (b) 50% STS (c) 100% STS.

Although 100% STS would produce more power than the 50% STS as well as the conventional turbine due to more swept area, the “power coefficient” (indicating power per swept area) showed similar levels when operating at 267 rpm (Figure 4.22a). The

conventional turbine had a higher peak performance ($C_P = 0.3$ at $\lambda = 4.8$) than both the 50% and 100% STS turbines, which showed better performance than the conventional turbine at higher TSRs. 100% STS turbine outperformed the 50% STS for lower TSRs at this rpm. For a higher rotational speed (400 rpm), as shown in Figure 4.22b, BVI effects reduced the overall efficiency of the conventional turbine (light grey curve), while improving 50% STS's performance when compared to the 100% STS turbine for higher TSRs.

Figure 4.22: comparison of power coefficient between experimental data and the present models results with aspect ratio of 1.5: (a) Sandia 2-m blade height at 267 rpm (b) Sandia 2-m blade height at 400 rpm. Turbines have different swept areas.

Figure 4.23: Comparison of power coefficients of Sandia 5-m between experimental data and the present models results with aspect ratio of 1.5: (a) Sandia 5-m blade height at 162 rpm (b) Sandia 5-m blade height at 175 rpm. Turbines have different swept areas.

Figure 4.24: Comparison of power coefficient of Sandia 17-m between experimental data and the present models results with aspect ratio of 1.5: (a) Sandia 17-m at 42.7 rpm (b) Sandia 17-m at 50.6 rpm.

As shown in Figures 4.23 and 4.24, the power curves of the 50% STS and 100% STS models are similar to each other, however, are both wider and lower than the conventional model due to a reduction in rotor solidity and an increase in swept area. It has been proved that [108, 123–125] the turbine performance will normally decrease when the solidity is decreased (by increasing the aspect ratio).

4.3.3.3 Performance Comparisons of 50% STS between Low and High Aspect Ratio.

To determine how the power coefficient of the novel design (50% STS turbine) is affected by the aspect ratio, the performance comparisons for the 50% STS turbines with high ($AR=1.5$) and low ($AR=1.0$) aspect ratios are plotted in Figure 4.25. Clearly, the effects were quite significant regardless of the turbine size and its rotational speed. As expected, this figure indicated much lower performance for the turbine with high aspect ratio when compared to the one with low aspect ratio. The decreased percentage of the peak power coefficient curves ranged from 11.2 to 14.6%. As stated earlier, the aspect ratio has a

direct proportional relationship to the turbine's swept area, in addition to an inverse relationship to the performance.

Figure 4.25: Comparison of power coefficient of the three different turbine diameters, between high and low aspect ratio (50% STS VAWT).

Based on the literature, the influence of the aspect ratio and rotor solidity on

the performance of troposkien VAWTs (whether the conventional or the new modified configurations 50% and 100% STS-VAWT) were investigated in this study for the first time.

4.4 Production Costs

Financially, the curved blade turbines (i.e., the troposkien VAWT) offered a real opportunity to reduce the cost of energy (COE) by 20% when compared with HAWTs. This is especially apparent in deep-water locations, as reported previously [62]. However, for a given swept area, these curved blades have a surface area greater (by a factor of 2 to 3) than that of the propeller turbine (i.e., HAWT turbines) [17, 51]. The large blade area (defined by the combination of the blade length and chord) was indeed one of the reasons leading to a low blade speed during normal operation and consequently to poor turbine efficiency. More importantly, as the blades were made larger (i.e., for example, Sandia 17-m), the amount of materials required to build the blade increased, resulting in larger turbine weight (i.e. larger rotor weight per kilowatt-hour). This, in turn, led to higher costs, and to the design of larger powertrain components, such as gearbox shafts, bearings, and gears. Therefore, many investigators have been attempting to invent new geometry designs that would reduce turbine size (i.e. blade height and length) and costs (i.e. fewer materials to lower cost), while increasing efficiency [51].

Although the 50% STS rotor was designed purely for the purpose of maximizing the aerodynamic efficiency of the VAWTs, this higher efficiency also came with lower production costs when compared to the conventional design. Tables 3.1, 3.2, 3.4, and 3.6 presented data showing that the blade length and the blade height of the 50%STS turbine decreased by 12.8% and 33% respectively, compared to the conventional VAWT.

As a result, the mass of the materials used to construct the 50% STS turbine blade was 0.290 kg, corresponding to a 15.94% decrease in the weight of the blade when compared with the conventional model (see Tables 3.1 and 3.2). This reduction of weight would provide a lower production cost as the wind turbine blade is considered the 2nd most expensive component of a wind turbine (including material, labor, transportation, and overhead). Additionally, the materials used to construct the turbine represent the greatest contributor to the total cost, ranging between 30.7-37.9 % of the blade's aggregate cost [126, 127].

The 50% STS type blades were rugged, shorter, lighter and simplistic. This would result in a reduction of the cost of energy of the 50% STS VAWTs by greater than 20% compared with HAWTs, particularly for deep-water installations. This could also lower the bending loads (both aerodynamic and gravitational), and provide further benefits when manufacturing multi-megawatt-sizes turbines (delivering in scalability). Another benefit that is promising for reduced size and blade weight is the size of powertrain components, which are expected to be smaller and lightweight when compared to the conventional design. Therefore, the superior aerodynamic performance combined with better scalability and reliable cost projections of the 50% STS VAWTs indicate that it could become an effective choice for deep-water applications.

Chapter 5

Conclusions, Contributions, and Recommendations for Future Work

5.1 Overall Comparison and Conclusions

Since NASA, DOE, Sandia Laboratories, and the National Research Council of Canada stopped their wind turbine (i.e., troposkien VAWT) research program in 1994, there has been a lack of research related the troposkien Darrieus VAWTs. In addition to the absence of research activity, the low aerodynamic efficiency combined with larger blades (as well as component fatigue failures), resulted in a reduction of popularity of the curved blade turbines and an increase in the use of HAWTs, particularly for onshore wind farms. However, as the wind farms have been moving towards offshore installations, particularly for deep water (i.e., greater than 40 meters), the costs for a HAWT offshore wind farms are much more expensive than onshore ones. Hence, troposkien VAWTs offer a real opportunity to reduce the cost of offshore wind energy by 20% when compared to

HAWTs.

To overcome the performance limitations and the blade length of the troposkien VAWTs, this thesis presented a novel configuration where the heights of each blade were decreased and shifted vertically with respect to the other blade to mitigate the negative impact of BVIs (Blade Vortex Interactions) on the turbine blade performance. In order to evaluate the efficacy of this idea, an experiment to determine the aerodynamic performance along with the wake effects, as well as a numerical study of the novel design compared with the conventional troposkien design were undertaken. This was accomplished by designing, building and testing two sets of prototype wind turbines (i.e., at a medium and high range of TSR, tip speed ratio) in the ABL (Atmospheric Boundary Layer) wind tunnel (see Section 3.2.1.1). To ensure a consistent comparison between the prototypes, the turbines of each set are fabricated having the same NACA 0015 airfoil section and with the same overall height, radius, and swept area.

Experimental Data: Aerodynamic Performance:

The wind tunnel experimental data presented in the previous section showed that when the BVI effect was negligible, i.e., when the turbine models were operating at the low TSR range (in this case lower than 3), the 50% STS model underperformed a conventional troposkien shape with the same swept area. This behaviour was expected due to the fact that the 50% STS model has a smaller blade length, which directly resulted in a lower generated tangential force. Note that the blade length effect manifested itself in the non-dimensional parameter i.e. solidity, defined as $\sigma = NcS/A_s$, where N is the number of blades, c is the chord length, S is the blade length, and A_s is the swept area. On the contrary, at medium to high TSR (in this case 3.5 to 5.5), the 50% STS model with a shorter blade (lower solidity) outperformed the conventional troposkien

shape. This is due to the effect of the blade vertical shift. This vertical shift resulted in two significant changes in the behaviour of the 50% STS model when compared to the conventional troposkien shape:

(1) In the conventional troposkien shape, every pair of points, on one blade at the same height, followed the same circular path around the wind turbine shaft. In other words, the blades exactly followed each other's travel path. However, this was not the case in the STS model. For instant, the equator of the shifted blade was at the same height of the top attachment point of the un-shifted blade; while the former followed a circular path and the latter was stationary at one point. This improvement was particularly important to minimize BVI at medium or high TSR where the following blade was traveling fast enough to intercept the vortices released from the advancing blade.

(2) Since the blades of the 50% STS model do not follow the same path, the vortices generated from these blades were more distributed along the turbine height. This resulted in a lower concentration of cluttered vortices inside and around the wind turbine. The lower concentration of vortices consequently led to lower impedance against the incoming flow, thus increasing its performance.

It is also interesting to note that the 50% vertical shift is a better modification than the 100%. Although, according to the previous discussion, one would conjecture that the BVI effect could be even less in a 100% model than to the 50%, the lower solidity (smaller blade length in this case) of the 100% STS model causes a significant drop in the torque generation capacity of the wind turbine. Therefore, it can be concluded that the 50% STS model has better balance between minimizing the BVI effect and maximizing the solidity at a given swept area.

Experimental Data: Wake Measurements:

To better understand the near-wake behind each turbine model, the velocity deficit and turbulence intensity were measured. The flow field downstream of the turbines (i.e., in the near-wake up to 3D) was mapped using hot-wire anemometry in both the lateral and vertical planes at two tip speed ratio regions: (i) at low region ($\lambda > 4$) and (ii) high region ($4 < \lambda < 6$). In general, the experimental results showed that the wake expansion was almost symmetrical in both profiles (lateral and vertical) regardless of the turbine model. At the high TSR region ($\lambda = 5.06$), where the BVI effect is strong, the velocity deficit and turbulence regions behind the conventional model was broader and had a wider transition zone than that behind the 50% STS model. Whereas, at low λ where the BVI effect is negligible, the velocity deficit region behind the conventional model was less than that of the one behind the 50% STS model. Whereas, the overall shape of the wake behind the 100%STS model was visible in the form of two deficit regions: (i) a strong region corresponding to the upwind side of the rotor, and (ii) a weak region. Therefore, it can be concluded that the 50% STS model has an optimal balance between maximizing the performance and minimizing the wake expansion downstream of the rotor. By achieving an acceptable fatigue lifetime combined with better performance, this was expected to be more important for turbines installed in a wind farm configuration.

Numerical Analysis:

The power coefficients of conventional VAWTs having three different diameters (2-m, 5-m, and 17-m) were determined using an in-house code based on BEM (Blade Element Momentum) and the vortex filament method. Simulation results were compared against Sandia experimental data, showing an overall good agreement. This code was used to

simulate two novel geometries: (i) 50% STS-VAWT and (ii) 100% STS-VAWT for three different diameters (2-m, 5-m, and 17-m) and different rotational speeds. Simulation results indicated that 50% STS-VAWT showed superior aerodynamic performance (peak power coefficient) when compared against the conventional VAWT and the 100% STS-VAWT. In addition to this, the materials and designs that reduce the blade weight of 50% STS VAWTs could be beneficial for multi-megawatt size installations. This is due to a reduction of the COE (Cost of Energy) particularly for certain offshore (deep-water) installations. This improved performance combined with its lower production costs demonstrate that the 50% STS turbine could be an effective choice for deep-water.

5.2 Contributions

The main original contributions of this thesis can be summarized as follow:

- Invention, fabrication, and experimental investigation of a novel design of the Darrieus troposkien VAWT.

A novel troposkien VAWT design for both offshore and urban environment was launched in this thesis. In this new configuration, one blade was shifted vertically with respect to the other blade in order to diminish the negative impact of the BVI on the generated torque.

- Improvement of the aerodynamic performance of the Darrieus troposkien VAWT.

The aerodynamic performance of three different configurations of VAWTs (i.e. conventional, 50% STS-VAWT, and 100% STS-VAWT) were investigated experimentally in the ABL wind tunnel. The comparison results showed that the 50% STS model outperformed the other two models by up to 10% (peak power), within a medium range of λ where the

BVI effect was strong

- Reduction of the blade size of the Darrieus troposkien VAWT.

The size of the troposkien turbine blades (i.e., the blade length $\times$ chord) is approximately twice as long as that of a HAWT [17, 51]. Therefore, reducing the blade size of the 50% STS configuration to approximately 13% when compared with the conventional design was crucial in the troposkien turbine's design. This will reduce concerns about turbine's noise, fatigue, visual impacts, and minimize required materials to build the turbine blades (decreases the rotor weight per kW).

- Improvement of the self-starting capability of the Darrieus troposkien VAWT.

One shortcoming of the originally curved blade turbine (troposkien) was its inability to start on its own, i.e., the need for external assistance for self-start such as a motor. Although the three different turbine configurations were not totally successful in self-starting, at the very least, the new design (50% STS) was able to start up with less wind and had the ability to rotate faster than the conventional model. These results were quite promising and were considered as a good starting point for further research on more effective self-starting solution methods.

- Reducing the material production cost of the troposkien VAWT.

The wind turbine blade is considered the 2nd most expensive component of a wind turbine (including material, labor, transportation, and overhead) [126, 127]. The cost of material generally ranges between 44-51% of the blade's aggregate cost based on design specification [127–129]. In the past, Sandia has put an effort to reduce the cost of energy of VAWT through improved blade design and used different materials [130]. Although, Sandia succeeded to manufacture VAWT blades costing the same

as their HAWT counterparts [17], reducing even more the costs, should have a major role in future turbine designs. The current work has been successful in reducing the production costs to up to 13% by decreasing the turbine size (i.e. blade height and length), and improving both the self-starting behaviour and the performance of the troposkien turbine.

- Characterizing the wake structure behind the Darrieus troposkien VAWT.

The velocity deficit and turbulence intensity in the near wake (i.e. up to 3D) behind three different troposkien VAWTs were experimentally measured at low and high TSR regions using a hot-wire anemometer technique.

- Developing and validating an aerodynamic code for the Darrieus troposkien VAWT.

The vortex filament model, originally developed by Strickland [66], was implemented using MATLAB and validated by comparison of results against experimental data. In the present work, the aerodynamic performance of the three geometries (conventional VAWT, 50% STS-VAWT, and 100% STS-VAWT), were simulated for three different turbine diameters (2-m, 5-m, and 17-m). Noticeably, the new modified configurations, i.e. 50% and 100%STS-VAWT had not been examined previously with any aerodynamic modeling techniques. The present work demonstrates its novelty.

5.3 Recommendations

The following topics are recommended for future work:

- Field testing (or large wind tunnel testing) of the 50% STS-VAWT.

Although laboratory experiments are more common than field experiments, it is

highly recommended to compare the aerodynamic performance of the novel design (50% STS-VAWT) with the conventional model outdoors, i.e., under free controlled testing conditions. Alternatively, a wind tunnel with at least a variety of wind speeds and blockage ratio less than 2% is recommended. For example, this could be achieved using the icing wind tunnel facilities at the NRC.

- Designing a wind farm layout for the 50% STS-VAWT.

Evaluating the generated energy of small wind farms (i.e. those having three or four turbines) of the 50% STS-VAWT in comparison with that generated from the conventional VAWT wind farm is one of the most important recommendations for future work.

- Improving the current aerodynamic code for a floating 50% STS-VAWT.

The challenges presented by the unsteady aerodynamic behaviour of VAWTs, together with the difficulty of conducting experimental tests in offshore environments, would require further improvements on the current aerodynamic code for a floating 50% STS-VAWT. These improvement would need to take into account all other parameters such as hydrodynamics, structural dynamics, and mooring line dynamics. In addition, the code must be written in FORTRAN or another programming language using a compiler because MATLAB is mostly used for post-processing requirements such as plots, animations, and scripting.

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APPENDIX A: Uncertainty Analysis of Power Coefficient Measurements

A critical characteristic of any measurement instrument is its accuracy, in other words, how closely the instrument's reading reflects the true value being measured with a minimum uncertainty. The lower the uncertainty, the more confidence that the reading is accurate and reliable [131–136]. The current experimental measurements that were obtained from the troposkien turbine models in the wind tunnel, have some degree of measurement uncertainties. These arise as either as a consequence of random or systematic errors, as shown in Figure 5.1. Random errors are caused by unknown and unpredictable changes in the experiment. They can be determined by taking N repeated measurements, as expressed in the following equation

$$R_x = \frac{\sum (x_i - \bar{x})^2}{\sqrt{N}} \quad (5.1)$$

where $\bar{x}$ is the average value of x. To combine random measurement uncertainty with

the same units of measure, the root sum square (RSS) method is used as follows

$$R_T = [R_1^2 + R_1^2 + R_1^2 + \dots + R_k^2]^{1/2} \quad (5.2)$$

On the other hand, systematic errors are those that can not be detected in this way and are usually associated with bias in experimental data (which arises from equipment). Similarly, if the measurements are independent of each other and the errors are systematic or bias then it can be calculated as

$$B_T = [B_1^2 + B_1^2 + B_1^2 + \dots + B_k^2]^{1/2} \quad (5.3)$$

Figure 5.1: A schematic summary of measurement errors.

Thus, the total measurement uncertainty can be determined by implementing either of the two recommended uncertainty models (Additive, ADD, or Root-Sum-Square, RSS, method) [131–134].

$$U_{ADD} = B + t R_x @ 99\% \quad (5.4)$$

$$U_{RSS} = \pm [(B_T)^2 + t_{95}(S_T)^2]^{1/2} @ 95\% \quad (5.5)$$

Where U_{RSS} is the total uncertainty of the result, B_T is the total bias or systematic uncertainty, S_T is the total random uncertainty, and t_{95} is "Student's t-distribution" at 95% confidence level for the appropriate degrees of freedom (DOF).

Equation 5.5 combines the uncertainties of measured variables by a vector sum as they are assumed to be independent and uncorrelated with each other [135, 136]. In this study, the Root-Sum-Square (RSS) method has been adopted to achieve the more accurate data for power coefficient measurements. The power coefficient of the wind turbine (C_P), which is frequently non-dimensional (C_p vs. λ), is defined in Eq 5.6 and is a function of: the air density (ρ), the swept area of the rotor blades (A_s), and the free stream wind speed (U_∞).

$$C_p = \frac{P}{\frac{1}{2} \rho A_s U_\infty^3} \quad (5.6)$$

The overall uncertainty of C_p can be estimated by taking the partial derivatives with the RSS method results in the error propagation equation:

$$u_{C_p} = \left[\left(\frac{\partial C_p}{\partial P} \right)^2 u_{P^2} + \left(\frac{\partial C_p}{\partial \rho} \right)^2 u_{\rho^2} + \left(\frac{\partial C_p}{\partial A} \right)^2 u_{A^2} + \left(\frac{\partial C_p}{\partial U_\infty} \right)^2 u_{(U_\infty)^2} \right]^{1/2} \quad (5.7)$$

Here, u_P , u_ρ , u_A , and u_V denote the uncertainties of P , W , ρ , A , and V , respectively.

In the current measurements, the uncertainty due to the random errors, which are errors that affect the precision of the measurement, was reduced by repeating the measurement. The bias errors during the wind tunnel test are due to the instrumentation used to measure the relevant variables, and are discussed as follows:

Power measurement P : The turbine output power was determined by measuring the voltage and current with a digital multimeter. It is well known that these digital multimeters are subject to measurement uncertainty. The overall uncertainty of the power is a combination of estimated uncertainties of each component and can be calculated according to the following formula

$$uP = \sqrt{\left(\frac{\partial P}{\partial V}\right)^2 uV^2 + \left(\frac{\partial P}{\partial I}\right)^2 uI^2} \quad (5.8)$$

where uP , uV , and uI denote the uncertainties of P , V , and I , respectively. Errors in both multimeters were evaluated for both calibration and manufacturer specifications. Based on the data provided by the manufacturer, an accuracy of the measured voltage (V) and current (I) were estimated to be $\pm 0.5\% + 2$ counts, and $\pm 1.2\% + 3$ counts, respectively. However, a DC power supply (steady DC power supply from BK Precision, Model 1610, (S/N 145-04858)) was connected to the two multimeters (EXTECH multimeter and Mastech MS8268 multimeter) to obtain the best measurement accuracy. The measurement uncertainties were lower (i.e. $0.1\% + 2$ counts) when compared with the manufacturer's accuracy specification. Thus, the multimeters reading uncertainty will be a combination of the calibration and the manufacturer specifications. The resulting total error in power, measured by both multimeters, is presented in Table 5.1.

Velocity measurement U_∞ : Wind tunnel test section flow quality is considered an important parameter in the wind turbine power measurements (see Eq 5.6), and it must

meet high standards to obtain accurate and reliable measurement data. The tunnel speed is determined from a sensitive manometer along with a pressure transducer connected to static pressure taps on the tunnel contraction. Thus, the velocity is a function of density and differential pressure. It can be determined from

$$U_{\infty} = \left(\frac{\Delta P}{0.5 \rho} \times 0.85 \right)^{0.5} \quad (5.9)$$

where Δp is the static pressure difference between the two taps ($pc1 - pc2$), ρ is air density, and 0.85 is a tunnel calibration factor

Density was found using the ideal gas law:

$$\rho = \frac{P_a}{R T} \quad (5.10)$$

where P_a is atmospheric pressure, R is the specific gas constant for air, and T is the ambient air temperature.

The resulting equation for the propagation of error for the velocity, $u U_{\infty}$, is therefore:

$$u\rho = \sqrt{\left(\frac{\partial \rho}{\partial P_a}\right)^2 uP_a^2 + \left(\frac{\partial \rho}{\partial R}\right)^2 uR^2 + \left(\frac{\partial \rho}{\partial T}\right)^2 uT^2} \quad (5.11)$$

and

$$uU_{\infty} = 0.5 \left[\sqrt{\left(\frac{\partial U_{\infty}}{\partial \Delta P}\right)^2 u\Delta P^2 + \left(\frac{\partial U_{\infty}}{\partial \rho}\right)^2 u\rho^2} \right] \quad (5.12)$$

where $u\Delta P$ is differential pressure error, uT is temperature error and $u\rho$ is density

error. Thus, any changes in atmospheric conditions may contribute to error in the air density. Here, the term atmospheric pressure refers to the pressure generated by the weight of the air surrounding the wind tunnel, i.e., inside the wind tunnel room. The maximum deviation of atmospheric pressure is assumed to be less than 0.2% of the reading. This deviation is caused by the assumption that all airflow is flowing only through the doors. Additionally, the manufacturer claims that the maximum transducer accuracy is less than $\pm 0.25\%$ of the full-scale reading. The temperature of the incoming air (i.e. in the wind tunnel test section) was not measured, and therefore, the room temperature was used. The room temperature ranged between 21 and 22°C, which was measured by the thermocouple. According to the manufacturer's specification, the accuracy of the thermocouple is less than $\pm 0.5\%$ of the reading per 10 °C. Table 5.1 below lists the corresponding errors associated with individual variables as well as the total error in the velocity.

Area measurement A: The swept area of the troposkien turbine refers to the oval shape created by the blades as they sweep through the air, i.e., two straight parts along with a middle circular segment. This depends on both the radius and height of the blade.

$$uA = \sqrt{\left(\frac{\partial A}{\partial R}\right)^2 uR^2 + \left(\frac{\partial A}{\partial H}\right)^2 uH^2} \quad (5.13)$$

where uR , and uH are the uncertainty of R and H , respectively.

The prototype turbines were designed using Solidworks software and printed from ABS (Acrylonitrile Butadiene Styrene) plastic materials by utilizing 3D printing. 3D printing has a high and micro-resolution build for extremely accurate parts. Thus, the precision can be considered to be $\pm 0.001\%$ with a minimum of $\pm 200\mu$.

Table 5.1: Systematic errors associated with individual variables.

Variable	Symbol	Systematic	Random
Voltage	V	$\pm 0.5\%$ +2	fossilized ≈ 0
Current	I	$\pm 1.2\%$ +3	fossilized ≈ 0
Pressure	P	$\pm 0.251\%$	fossilized ≈ 0
Density	ρ	$\pm 1.2\%$	fossilized ≈ 0
Velocity	U_∞	$\pm 0.47\%$	fossilized ≈ 0
Area	A_S	$\pm 0.001\%$	fossilized ≈ 0

Random errors are not constant and are characterized via the standard deviation of the random variations. In this study, three measurements were taken for two rotational speeds of 600 and 700 RPM, respectively. Thus, the total random uncertainty of the peak power coefficient is presented in the next table.

Table 5.2: Random error of the peak power coefficient

		Test 1	Test 2	Test 3	% Error
Conventional	600 RPM	0.2527	0.2606	0.2535	1.7
	700 RPM	0.2542	0.2432	0.2280	5.4
50% STS	600 RPM	0.2931	0.2825	0.2898	1.8
	700 RPM	0.2836	0.2843	0.2883	0.88
100% STS	600 RPM	0.2275	0.2351	0.2392	2.5
	700 RPM	0.2532	0.2531	0.2478	1.22

Therefore, the total uncertainty is

Table 5.3: Total uncertainty of the peak power coefficient

		Systematic	Random	Total uncertainty
Conventional	600 RPM	0.01830	1.7	3.5% @ 95
	700 RPM	0.01830	5.4	11.2% @ 95
50% STS	600 RPM	0.01830	1.8	3.7% @ 95
	700 RPM	0.01830	0.88	1.8% @ 95
100% STS	600 RPM	0.01830	2.5	5.1% @ 95
	700 RPM	0.01830	1.22	2.5% @ 95

Uncertainty in tip-speed ratio (λ):

The tip-speed ratio (λ) expression is obtained from Ω , R and U_∞ by

$$TSR = \lambda = \frac{\omega R}{U_\infty} \quad (5.14)$$

The resulting equation for the propagation of error for the tip speed ratio, $u\lambda$, is therefore:

$$u\lambda = \left[\left(\frac{\partial \lambda}{\partial \omega} \right)^2 u(\omega)^2 + \left(\frac{\partial \lambda}{\partial R} \right)^2 uR^2 + \left(\frac{\partial \lambda}{\partial U_\infty} \right)^2 u(U_\infty)^2 \right]^{1/2} \quad (5.15)$$

where uR is radius error, $u\omega$ is the rotational speed error of the turbine.

Measurement of speed of rotation ω : The rotational speed of the turbine models was measured using a digital laser tachometer. The tachometer operates by optically counting the number of revolutions over a period of time. According to the manufacture calibration certificate, the tachometer has a high accuracy of $\pm 0.05\%$ on the output reading. The maximum uncertainty of tip speed ratio (λ) was found to be 0.47% @ 95.