A SOLUTION-BASED STALL DELAY MODEL FOR

HORIZONTAL AXIS WIND TURBINES

A Thesis in
Aerospace Engineering

by
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ABSTRACT

A comprehensive review of contemporary stall delay models and a proposed new solution-based stall delay model to predict rotational effects on horizontal-axis wind turbines are presented. In contrast to conventional stall delay models that correct sectional airfoil data prior to the solution to account for three-dimensional and rotational effects, a novel approach is proposed that corrects sectional airfoil data during a Blade Element Momentum (BEM) solution algorithm by investigating solution-dependent parameters such as the spanwise circulation distribution and the local flow velocity acting at a blade section. An iterative process is employed that successively modifies sectional lift and drag data until the blade circulation distribution is converged. Results obtained with the solution-based stall delay model show consistent agreement with measured data along the NREL Phase VI and MEXICO rotor blades at low and high wind speeds. A final application of the solution-based stall delay model to a notional 2.3-MW turbine design demonstrates the presence of rotational augmentation at inboard stations of utility-scale wind turbine rotors.
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LIST OF SYMBOLS

\( A \) = Rotor swept area \([m^2]\)

\( a \) = Axial induction factor

\( a' \) = Angular induction factor

\( \alpha \) = Local angle-of-attack \([\text{deg}]\)

\( B \) = Number of rotor blades

\( \beta \) = Local blade twist angle \([\text{deg}]\)

\( \Delta \beta \) = Total blade twist \([\text{deg}]\)

\( \text{BEM} \) = Blade Element Momentum

\( c \) = Blade chord length \([m]\)

\( c_l \) = Sectional lift coefficient

\( c_d \) = Sectional drag coefficient

\( c_n \) = Sectional normal force coefficient

\( c_t \) = Sectional tangential force coefficient

\( \Gamma \) = Blade Circulation \([m^2/s]\)

\( F_n \) = Sectional normal force per unit length \([N/m]\)

\( F_t \) = Sectional tangential force per unit length \([N/m]\)

\( \text{NREL} \) = National Renewable Energy Laboratory

\( \text{MEXICO} \) = Model Experiments in Controlled Conditions \([56]\)

\( \Omega \) = Rotor angular velocity \([\text{rad/s}]\)

\( \phi \) = Local inflow angle \([\text{deg}]\)

\( r \) = Blade radial location \([m]\)
\[ R \quad = \quad \text{Blade radius [m]} \]
\[ \text{SBSD} \quad = \quad \text{Solution-Based Stall Delay} \]
\[ V_0 \quad = \quad \text{Wind speed at hub height [m/s]} \]
\[ \lambda \quad = \quad \text{Tip speed ratio, } \frac{\omega R}{V_0} \]
\[ \lambda_r \quad = \quad \text{Local tip speed ratio, } \frac{\omega_r}{v_0} = \frac{R}{R} \]

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Chapter 1

Introduction

1.1. Introduction

Capturing energy from the wind has progressed greatly in the past century from rural small-scale windmills used for pumping water to multi-megawatt turbines utilized to create renewable electrical energy. The modern paradigm shift from fossil fuels to renewable energy alternatives has stimulated the progression of wind turbine development. Modern utility-scale turbines are approaching aerodynamic efficiencies close to the theoretical maximum dictated by the Betz limit and airfoil lift-to-drag ratios [1]. Adverse aerodynamic loads are the cause of fatigue and structural failure of mechanical components [2]. These adverse loads must be subdued to increase the operating life of wind turbines. Future alleviation of the adverse aerodynamic loads while maximizing the aerodynamic forces responsible for energy capture can be achieved through accurate aerodynamic modeling of wind turbine blade aerodynamics.

There is constantly room for improvement in the field of aerodynamic load prediction methods. Current methods lack either the fidelity or computational speed necessary to accurately and efficiently predict the various aerodynamic loads on the turbine blades. In order to get accurate values for aerodynamic loads on the complex
rotating blades a Computational Fluid Dynamics (CFD) model must be employed. These models can accurately predict the full three-dimensional aerodynamic effects on the blades, however come at a very high computational cost. As of today, wind turbine manufacturers do not have the resources to utilize CFD models during the design process, so they turn to more simplistic models. Therefore, the wind energy industry presently relies heavily on the Blade Element Momentum (BEM) theory based models to predict the blade forces and power production of wind turbine designs. This method is a simple and computationally efficient way of predicting basic aerodynamic loads. The BEM method theory is strictly based on two-dimensional flow. Three-dimensional effects are accounted for through the use of tip and root loss factors. This apparent limitation leads to a restricted predictive accuracy of aerodynamic loads on the blades. As the tip and root loss corrections still do not account for the full spectrum of three-dimensional aerodynamic effects and loads, load prediction inaccuracies necessitate the overdesign of structural and mechanical elements in order for the wind turbines to operate safely. Inaccuracies in the blade load predictions can sometimes lead to designs that fail much earlier than predicted. Ultimately, the combination of overdesign and design failures leads to higher than anticipated cost-of-energy (COE). Obtaining a better understanding of the three-dimensional aerodynamic effects on wind turbine blades can allow new models to be developed that increase the accuracy and speed of aerodynamic load predictions. Enhanced design capabilities, materials, and computational methods will provide more efficient and cost-effective turbines, thereby ensuring the continued growth and success of wind energy [2].
1.2. **Rotational Augmentation**

Blade rotation causes many complicated three-dimensional aerodynamic flow effects, some of which are not encountered in conventional fixed wing aircraft. The three-dimensional aerodynamic flow effects are one of the many challenging aspects in accurately predicting the blade loads. These three-dimensional effects caused by rotation have been found to effectively delay the conventional stall process on the blade relative to two-dimensional wind tunnel tests. The result is an augmentation of blade loading in post-stall flow conditions compared to the conventional two-dimensional flow case. This effective delay of stall is referred to as stall delay, or rotational augmentation. A complete description of the rotational augmentation phenomenon is presented in the following sections.

1.2.1. **Background**

The first record of augmentation of aerodynamic loads due to rotation was an analysis for aircraft propellers done by Himmelskamp in 1945 [3]. Figure 1-1 shows some of the results of his experiment. The data labeled ‘wind tunnel’ are two-dimensional airfoil data taken from wind tunnel measurements. The lift coefficient at each radial station was determined from pressure taps along the chord at that station.
Figure 1-1. Experimental results of Himmelskamp on the effect of rotation on a propeller blade [61].

Himmelskamp postulated that the increase in lift at the inboard blade sections was due to centrifugal and Coriolis effects acting on the flow. In 1963, Banks and Gadd [4] followed with a theoretical analysis. They concluded that the effect of rotation was to delay the laminar separation point or to prevent separation altogether due to a linear adverse external-velocity gradient. Du and Selig [5] confirm that rotation does delay the separation point, but only slightly. Similar theoretical work was conducted in the rotorcraft field by McCroskey and Yaggy [6]. One early experiment by Ronsten [7] compared a rotating and non-rotating wind turbine blade. Figure 1-2 shows the lift coefficient for a rotating and non-rotating blade at 30% of the radius. The effect of rotation clearly increases the lift coefficient significantly and effectively delays stall on the blade. Figure 1-2 shows the lift coefficient for a rotating and non-rotating blade at
30% of the radius. The rotating case has increased lift coefficient compared to the non-rotating case, especially at high angles-of-attack.

![Graph showing lift coefficient comparison](image)

Figure 1-2. Comparison of lift coefficient at 30% radius for a rotating blade (RB) and a non-rotating blade (NRB) of a STORK 5WPX wind turbine blade [7].

In 2000, the National Renewable Energy Laboratory (NREL) conducted the Unsteady Aerodynamics Experiment (UAE) in the NASA Ames wind tunnel. The UAE acquired test data from more than 1700 wind turbine test conditions with high-fidelity measurement equipment. The NREL Phase VI experiment component tested a heavily instrumented stall-regulated wind turbine equipped with the S809 airfoil at high accuracy and repeatability [8]. One of the two rotor blades was equipped with 22 pressure taps dispersed chordwise at five radial locations as well as 10 other radial locations with only two pressure taps. The angle-of-attack at each section was determined by pressure probes.
extending from the leading edge near the five pressure tap distributions. The results used in this study from the NREL Phase VI rotor use the two-bladed upwind configuration with zero cone angle and zero yaw. The rotor operates at a constant rotational speed and is stall regulated. Immediately following the tests NREL invited turbine modeling experts to blindly predict the behavior of the turbine under precisely controlled conditions. The experts were given all the necessary information to accurately model the turbine. NREL then held a meeting comparing the predictions of the different models and the actual measurements obtained from the wind tunnel measurements. The model predictions were not nearly as accurate or reliable as previously thought. This experiment has increased research in the field of aerodynamic load prediction models for wind turbines. Current research relies heavily on the large amount of data available from the UAE experiments due to its high level of accuracy and repeatability.

Many researches use normal and tangential force coefficients rather than lift and drag coefficients, due to their availability in experimentation [2,9,10]. Under experimental conditions the angle-of-attack at a wind turbine blade section is difficult to measure accurately. Figure 1-3 shows the coordinate system used for defining the different aerodynamic angles and coefficients on a wind turbine blade section. Normal and tangential force coefficients are defined relative to the chord line, while lift and drag coefficients are defined relative to the incoming local effective velocity.
Figure 1-3. Blade sectional aerodynamic angles and coefficients (⊥: normal to, //: parallel to) [1].

1.2.2. Augmentation Mechanisms and Effects

It is now known that a blade’s rotation augments the aerodynamic loads acting on that blade. Most of the current work in the area of rotational augmentation is in determining the underlying mechanisms and phenomena that are causing the augmentation as well as developing new models based on that knowledge to better predict the loads on a rotating blade. There is both consensus and debate over various mechanisms and their influence on the flow and therefore the loads acting on rotating blades.

As discussed earlier, Himmelskamp [3] hypothesized that rotation introduced centrifugal and Coriolis effects which augmented the aerodynamic loads beyond the two-dimensional airfoil capabilities. This hypothesis has largely been corroborated over the
years by many researchers [1,4,11-16]. Carcangiu et al. [11] developed a post processing tool to investigate the effects of rotation. The tool allowed them to output the magnitude of various terms in the governing equations they used. Through this process, it was shown that both the Coriolis and spanwise convection terms increased in magnitude after flow separation. Much effort has been put forth throughout the wind turbine community to further the understanding of the mechanisms at work as a consequence of the centrifugal and Coriolis effects. For example, Schreck and Robinson [17] determined that rotational augmentation is not governed by Reynolds number effects, global unsteadiness, or sweep by analyzing the NREL Phase VI [8] rotor blade in both stationary and rotating conditions at similar Reynolds numbers.

Instead, there are three primary mechanisms that govern the effects of rotation on blade flow. The first mechanism is the dynamic pressure along the blade, which increases towards the tip due to the increasing relative wind speed as radial position increases [1]. Second, the flow that rotates with the blade sections, mainly the boundary layer, is exposed to a centrifugal load that pushes the flow outboard [9]. Third, both the varying dynamic pressure and the centrifugal load generate radial flow, which induces a Coriolis force acting chordwise on the boundary-layer flow [1].
When the blade is operating under attached flow conditions, the effect of varying dynamic pressure and centrifugal loading is small. This is due to the fact that the flow remains on the blade for a very short period of time, thus allowing the flow to move only a limited distance radially [12]. However, the fact that there is some radial flow does induce a Coriolis force that acts towards the trailing edge, see fig. 1-4. The Coriolis force due to radial flow can be thought of as a favorable pressure gradient that can effectively delay the onset of separation or stall at the inboard blade sections. Even small amounts of radial flow cause the BEM theory to break down, because it only allows for two-dimensional flow as it treats the blade as a set of independent blade elements.
Figure 1-5. Theoretical image of radial flow along a rotating wind turbine blade [12].

The same mechanisms described above act very differently when the flow begins to separate. Separation occurs when the boundary layer locally reverses flow direction [1]. This allows the centrifugal force more time to act on the flow. Indeed, Lindenburg [9] suggests that the centrifugal force has a much larger impact on the separated flow than the chordwise pressure gradient. The centrifugal force acts as a pump pushing separated air further outboard. This process is known as centrifugal pumping. Rotorcraft experiments by McCroskey [18] showed centrifugal forces acting on separated flow regions and moving mass flow outboard. The centrifugal pumping also decreases the volume of the separation bubble, which leads to lower pressure in the separated volume.
The lower pressure on the suction side hence increases the normal force acting at that airfoil section. The radial motion of the fluid due to the centrifugal pumping then induces a Coriolis force on the blade, which acts towards the trailing edge. It will be shown in section 3 that the ratio of centrifugal force and Coriolis force is of importance to modeling rotational augmentation. Another phenomenon occurs due to a balance between the Coriolis force and the chordwise pressure gradient. If the two are in equilibrium, the separated flow region will not extend up to the trailing edge [19]. Instead, the separated flow region is then thought to begin rotating as shown in Fig. 1-5 due to turbulent mixing with the free stream at the upper side of the separated flow region [12]. This rotation is thought by some researchers to be a standing vortex, which adds suction to the blade surface, thereby increasing the aerodynamic forces on the blade [2,20-22]. The boundary layer flow will either separate and continue over the standing vortex or enter into the vortex through mixing as shown in Fig. 1-5 [12]. Figure 1-6 is taken from a paper by Sorensen et al. [23] who used an incompressible Navier-Stokes solver, Ellipsys3D, to compute separation streamlines on the NREL Phase VI rotor. The NREL Phase VI rotor operates at a constant rotational speed and pitch with varying wind speed.
Figure 1-6. Limiting streamlines on the suction side of the NREL phase VI blade for a) 7 m/s, b) 10 m/s, and c) 20 m/s. The vertical lines correspond to chordwise pressure taps at 30%, 47%, 63%, 80%, and 95% radius [23].

Figure 1-6 shows limiting streamlines on the blade from the NREL phase VI experiment at three different wind speeds. Schreck et al. [20] use similar results in their study and suggest that the coalescence and bending of the limiting streamlines in the 10 m/s case is indicative of a vortical structure. They then argue that the augmentation effects are proportional to the distance between the leading edge separation point and the impingement that occurs later on the blade suction surface. The boundary-layer separation and shear-layer impingement are also identified in later work by Schreck and Robinson [24-27]. Figures 1-5 and 1-6 show the intense radial flow which, Corten [12] argues, is fed at the root from both the leading and trailing edges.
Other theories do exist that differ from the idea of a standing vortex. One example is the work by Chaviaropoulos and Hansen [28]. They suggest that, when separation occurs, the Coriolis force sucks mass from the separation bubble and moves it outboard, thus leading to a reduction in the volume of the separation bubble. The reduction in the bubble volume produces a pressure drop on the suction side of the blade, which increases the blade loading. Some researchers suggest on the other hand, that the Coriolis effects on rotation are larger than the centrifugal effects [11,19]. Dumitrescu and Cardos [29-31] have conducted theoretical analyses of the effect of rotation on wind turbine blades. Their work has proposed that rotation leads to a leading-edge separation bubble rather than leading edge separation due to a reduced pressure peak at the leading edge. Their theoretical model treats the system as rotating flow over a stationary disk for the inboard section of the blade near the root. The model includes two working modes for the centrifugal effects, i.e. a sucking and pumping mode for the inner and outer parts of the blade, respectively. The sucking mode is significantly different from other models and observations, because it suggests that separated flow actually flows inboard towards the root. The model predicts a concentrated vortex within the leading-edge separation bubble with a separation line starting from a saddle point and terminating inboard at a focus location. Carcangiu et al. [11] modeled the flow field past a rotating wind turbine blade using a full three-dimensional steady Reynolds-Averaged-Navier-Stokes (RANS) approach. This approach generated fig. 1-7. The figure shows an inboard blade section of a wind turbine blade for the rotating and non-rotating case. Based on the figure the authors claim that the main effects of rotation are to stabilize vortex shedding and limit the growth of the separation cell.
The many different ideas for the cause of rotational augmentation show how divided the debate is over the true effects of rotation on blade aerodynamic loads. It is widely agreed that rotation augments the blade loading at inboard sections of the blade. However, new evidence suggests that the opposite is true near the blade tip. Lindenburg [19] uses a separate empirical technique to model the tip region. Recent work by Lindenburg [9] and Madsen et al. [32] proposed novel approaches to tip loss corrections. Sorensen et al. [20] also discuss the complicated aerodynamics near the tip and its influence on the blade loading. More detailed measurements of the tip region are required to truly understand how the aerodynamics at the tip alters the blade loads there.

The effect of blade rotation on the drag force is still relatively unknown. Some researchers have argued or assumed that rotation decreases the drag force exerted on the blade. This decrease in drag force can be seen in the work of Du and Selig [16] and Corten [12]. Du and Selig [16] base their argument on numerical predictions from...
Sorensen [33] using a viscous-inviscid interaction code. Corten [12] suggests that the flow-separation line is pushed more towards the trailing edge than in the two-dimensional case. He then conjectures that this will reduce the blade wake and leads to a drag force reduction. On the other hand, a majority of researchers argue that the drag force will increase on the rotating blade [9,11,28,34]. Lindenburg [19] also argues that, since the centrifugal pumping mechanism adds energy to the system, the drag force must increase.

There are even discrepancies when the NREL Phase VI experimental data are used to examine the effect of rotation on drag. Bak et al. [10] compare two-dimensional wind tunnel data for the S809 airfoil obtained at Ohio State University and NREL test data for the rotating case. Tangler [35] uses the NREL test data for the rotating case and compares against two-dimensional wind tunnel data for the S809 airfoil. Comparing these two analyses shows some similar trends, but also some large discrepancies. Bak et al. [10] show a general decrease in drag coefficient compared to the two-dimensional case, except for the most inboard section at high angles-of-attack. Tangler [35] shows a general decrease in drag coefficient at low angles-of-attack and a general increase in drag coefficient for post-stall angles-of-attack (except for the most outboard station), with much larger increases in drag inboard. The inconsistency in drag coefficient data is attributed to the fact that drag force data are purely based on pressure drag computed from the chordwise pressure taps on the NREL Phase VI rotor blade, and small errors in angle-of-attack can lead to large errors in the drag force [9]. Lindenburg [9] initially states that the NREL Phase VI rotor test data show a dramatic increase in drag force coefficient. He then follows by raising suspicion about the accuracy of the data obtained
in the experiment due to the relatively low accuracy of the pressure tap and angle-of-attack data. Others have noted a rapid increase in the drag force coefficient at 47% span initially and then at the 30% span location [22,35-36]. This is explained by Gerber et al. [36] as the departure of a standing vortex on the suction side.

Despite continuing controversy, a clear trend is forming that suggests that an increase in drag force is most prevalent over the regions of rotational augmentation [20]. In particular at inboard blade sections in post-stall conditions, the drag force encounters large increases in magnitude [9,11,22,28,34-36].

1.3. Blade Geometry Effects

The NREL Phase VI L–Sequence describes the parked blade configuration and investigates the three-dimensional blade response at static angles-of-attack in the absence of rotational effects [37]. In the parked configuration, the instrumented blade was locked at zero degrees azimuth and zero yaw. Through the use of this parked blade data, many researchers have studied the effect of blade geometry on the aerodynamic loads [2,7,20,22,38-39,40]. Parked blade aerodynamic loads data have been shown to improve the accuracy of stall delay models over the use of purely two-dimensional airfoil data [38]. Eggers et al. [38] suggest that the parked blade data are more relevant than the purely two-dimensional data because they account for the effects of aspect ratio, taper, and twist. Madsen and Christensen [41] found from test data that aspect ratio and radial pressure gradient were of greater significance than rotational effects.
Parked blade data are most widely used to compare against the rotating case to determine the effect of rotation on blade loads. However, the parked blade data are not often compared to the two-dimensional airfoil data. Eggers et al. [38], however, did compare the parked blade data at varying radial locations with the two-dimensional airfoil data for the NREL Phase VI blade (Fig. 1-8). By comparing two-dimensional wind tunnel data with parked blade data Fig. 1-8 shows the effect of blade geometry on the airfoil properties for the NREL Phase VI rotor. The normal force coefficient is greatly reduced at higher local geometric angles-of-attack for all radial stations of the parked blade data in comparison with the two-dimensional wind tunnel data. This is thought to be due to the flow of high pressure air from the lower surface into the low pressure deep-stalled region of the upper surface near the tip regions [38].

![Figure 1-8](image.png)

Figure 1-8. Comparison of 2D wind tunnel normal force coefficient from Colorado State University (CSU) with parked blade data at varying radial locations for the NREL Phase VI rotor [38].
Schmitz and Chattot [22] examined the spanwise circulation distribution across the parked NREL Phase VI rotor blade. They discovered a similar spanwise vortex to that observed in the rotating case. However, the spanwise vortex is more stable and further inboard than in the rotating case. Johansen et al. [39] suggest, however, that the spanwise vortex could be a property specific to the NREL Phase VI blade design. This means that other blade designs may not show this same vortical structure when operating under rotating conditions. If this is true, the use of parked blade data, whether obtained experimentally or computationally, could prove to be very valuable.

Schmitz and Chattot [22] also identify a rapid increase in the drag coefficient around the 40% radius at higher angles-of-attack that they associate with the spanwise vortex. A similar rapid increase in drag coefficient was observed for the rotating case [9,22,35-36]. Again, the blade geometry is seen to have a strong three-dimensional effect on the blade aerodynamics that would not be captured using two-dimensional polar data. This finding solidifies the argument to incorporate the use of data obtained from parked blade measurements or computation to increase the accuracy of future stall delay models.

1.4. Contemporary Stall Delay Models

Many attempts have been made to improve upon the basic BEM theory based design codes by adding three-dimensional corrections that account for the complicated three-dimensional rotational aerodynamics. When these corrections are specifically designed to account for rotational augmentation effects, they are most commonly called
Some models have been developed primarily empirically from experimental data [14-15,42]. Other models began as theoretical analyses attempting to simplify the full Navier-Stokes equations to solve the problem [9,10,13,16,33,38,43-45]. Yet, even these models generally require empirical correction factors to fit the data to experimentally obtained results. The empirical corrections are necessary, because most models were designed based on simplified assumptions or because they used a certain wind turbine as a base model. A brief description of some existing stall delay models will be given in this section.

The predominantly empirical stall delay models began with the work of Corrigan and Schillings in 1994 [14]. Their model built upon the boundary-layer equations formulated by Banks and Gadd [4], but simplified them to depend only on a single parameter. They finally decided on the angular location of the trailing edge as that parameter. They then translated this into a shift in angle-of-attack to account for the delay of stall. They used experimental helicopter data to obtain their empirical coefficients. The model by Corrigan and Schillings was later adapted and tested for use with wind turbines by Tangler and Selig in 1997 [15]. Dumitrescu et al. [42] developed a semi-empirical model to correct two-dimensional airfoil lift coefficient data based on their derivation of the three-dimensional form of the momentum-integral equations.

The stall delay models for wind turbines that are based on theory and analysis began with Snel in 1991 [43] and were continued by Snel et al. in 1992 [13]. Snel et al. used an order of magnitude analysis done on the boundary-layer equations and solved the equations to propose a simple model. The result is a model, which modifies the two-
dimensional lift coefficients. The analysis suggested a strong dependence on the ratio of local chord to local radial location, \( \frac{c}{r} \). They offered no correction for the drag coefficient. Du and Selig [16] later performed an analysis as an extension of Snel et al. based on an analysis of the three-dimensional integral boundary-layer equations. Du and Selig’s model includes a modified tip speed ratio term and more empirical factors. Their model also includes a correction for the drag coefficient. Chaviaropoulos and Hansen [28] used a quasi-three-dimensional model for their approach. The model was developed using simplified equations derived from the integration of the three-dimensional incompressible Navier-Stokes equations in the radial direction. They proposed that rotation caused an increase in pressure drag, which leads to an overall increase in blade loading. Their analysis gave them a semi-empirical stall delay model, which depends on the blade twist angle and the ratio of local chord to local radius. The model also includes many empirical factors. Corten [12] uses the work of Snel et al. as a basis and begins with the fundamental continuity equation and the Navier-Stokes equations to attempt to include the radial flow into their model. His model depends on the fraction of the chord flow, which is separated and the local chord to local radial location ratio. Eggers et al. [38] developed a model that modifies the normal and tangential force coefficients for rotational effects. They found greater accuracy using parked blade normal force coefficient data to account for blade geometry rather than two-dimensional wind tunnel data. The model relies on axial and angular induction factors, tip speed ratio, and the spanwise location where deep-stalled flow occurs. Lindenburg [9] has developed a model based on his analysis of separated flow at the trailing edge. He chose not to rely on the boundary-layer equations which break down in the separated flow region. He describes
his model as a Centrifugal Pumping model, because it accounts for the radial flow due to the centrifugal forces. The model suggests that augmentation is proportional to the size of the separated region. He begins by altering the normal and tangential force coefficients as well as shifting the angle-of-attack. This is then translated into the traditional lift and drag coefficients. The model depends on the area of separated flow, local chord to local radial position ratio, and modified local tip speed ratio. He then uses a separate model near the tip to account for the relatively unknown tip effects that govern the flow in that region. Finally, Bak et al. [35] developed a stall delay model based on an analysis of the difference in pressure distribution between a rotating and non-rotating blade using the NREL Phase VI data. Their model quantifies the rotational effects as the difference in pressure between the pressure and suction sides of the blade. Changes in normal and tangential force coefficients are then found by integrating the pressure difference over the different airfoil sections and adding those differences to the two-dimensional normal and tangential force coefficients. These are then translated into lift and drag coefficients. The model depends on normalized chordwise position, angle-of-attack, angle-of-attack just before separation, and angle-of-attack of fully separated flow.

Each stall delay model fails to obtain accurate results when compared with measurement data. Figure 1-9 shows results from a study conducted by Breton et al. [34] comparing the lift coefficient from different stall delay models using a lifting-line-prescribed wake vortex scheme. The values labeled ‘2D values’ are the measured two-dimensional data from the Delft University of Technology wind tunnel. The NREL Phase VI measurement data were adapted from the normal and tangential force coefficients to
generate the data labeled ‘From NREL Measurements’. The figure shows data from an inboard, center, and outboard section to show how each model and the effect of rotation are dependent on radial location.

The effect of rotation is clearly very large at the inboard location and then decreases as radial location increases until it appears to have the effect of decreasing the lift at the 95% radial location. The tip region obviously needs further study, as most models predict higher loads when the opposite is actually true. Some stall delay models require choosing empirical correction factors to make a specific model fit a specific wind turbine. The empirical models make it nearly impossible to have accurate predictions for new, untested wind turbines. A large variation in the corrections from the different models is seen at all three radial locations. A new model is necessary that does not necessitate adjusting parameters to fit a specific wind turbine and which can improve upon the accuracy of current models.
Figure 1-9. Comparison of contemporary stall delay models with NREL Phase VI data with two-dimensional wind tunnel data from Delft University of Technology for three radial locations; a) 30%, b) 63%, c) 95% [34]
1.5. **Objectives and Contributions of this Work**

This project is attempting to create a new stall delay model for horizontal-axis wind turbines. This work will have three primary objectives:

1. Complete a comprehensive review of contemporary stall delay models. The primary purpose of this effort is to determine similarities between the models which can be used as a foundation for the proposed new stall delay model.

2. Obtain parameters relevant to the stall delay problem for the wind turbine blade using the Blade Element Momentum (BEM) solution method in the XTurb-PSU wind turbine design and analysis software. Using this data, some of which will be solution-dependent, the aerodynamic force coefficients will be incrementally altered to account for the effects of rotational augmentation. This process will be iterated until convergence is achieved.

3. Compare the new stall delay model with horizontal-axis wind turbine experimental data from multiple experiments. Thus, the stall delay model should be able to be applied to any horizontal-axis wind turbine and provide accurate blade loading augmentation due to stall delay.

The primary contribution of this work is the solution-dependent nature of the new stall delay model which uses a combination of dimensionless groupings defined by the blade geometry, flow conditions, and the actual BEM solution to predict stall delay. The main difference compared to contemporary stall delay models is that airfoil data are corrected for rotational augmentation effects during the BEM solution process instead of solely prior to the solution.
Chapter 2

Numerical Methods

2.1. Blade Element Momentum Theory

The Blade Element Momentum (BEM) theory is the primary solution method used during the design process for horizontal-axis wind turbines [46]. BEM theory uses a combination of basic momentum theory and blade element theory.

In blade element theory, the wind turbine blade is discretized into a series of small elements called blade elements or strips, see Fig. 2-1. Each strip acts autonomously as a two-dimensional airfoil without influence from surrounding strips. The aerodynamic forces on the strip are then found based on the local flow conditions and a table-look-up of two-dimensional wind tunnel data. Finally, the loads acting on each of the blade elements are summed along the blade span to calculate the total forces and moments acting on the blade.
Momentum theory begins with the assumption that the rotor disk can be treated as an actuator disk. The work done by the airflow passing through this actuator disk can then be quantified as a loss of pressure or momentum. The loss of momentum in the flow is then used to calculate the axial and angular flow induction at the rotor plane. The blade element and momentum theories are then coupled to form BEM theory. The induced velocities affect the local flow conditions at the rotor plane which then affect the blade loads calculated by blade element theory. The coupling uses an iterative process to find the axial and angular induction factors at the rotor plane as well as the aerodynamic forces.

It is important to note the assumptions in BEM theory in order to understand the theory’s limitations. First, it is assumed that the flow is steady and purely two-dimensional. Therefore, the basic BEM method is not useful for dynamic changes in the wake or wind turbine operating conditions. The fundamental BEM theory also does not model the tip and root vortices due to the assumption of two-dimensional flow. However, the root and tip losses, which account for three-dimensional effects, can be included in

Figure 2-1. Discretization of wind turbine blade into strips [46].
the BEM solution in the form of a correction factor presented by Prandtl (see [47]) in Eqn. (1).

\[ F = F_T \times F_R \] (1a)

\[ F_T = \frac{2}{\pi} \cos^{-1} \left[ \exp \left( -\left\{ \frac{(B/2)(1-r/R)}{(r/R) \sin \phi} \right\} \right) \right] \] (1b)

\[ F_R = \frac{2}{\pi} \cos^{-1} \left[ \exp \left( -\left\{ \frac{(B/2)(r/R-(r/R)_{\text{root}})}{(r/R) \sin \phi} \right\} \right) \right] \] (1c)

Here, \( F_T \) is the tip loss factor, \( F_R \) is the root loss factor, \( B \) is the number of rotor blades, and \( \phi \) is the local inflow angle in radians. Finally, due to the assumptions of blade element theory, there is no allowance for spanwise flow along the blade. This is an important limitation for the stall delay problem which is known to be caused partly due to radial flow along the blade. The assumption also suggests that there is no spanwise pressure variation, which is especially untrue for heavily loaded rotors. This limitation is the primary motivation for appending BEM theory with a stall delay model.

BEM theory treats each blade element as a two-dimensional airfoil section as shown in Fig. 2-2. Figure 2-2a shows the flow angles and velocities needed to calculate the forces on the airfoil section. In this figure, \( V_0 \) is the wind velocity, \( V_{rel} \) is the relative flow velocity, \( \Omega \) is the rotor rotational speed, \( \alpha \) is the axial induction factor, and \( \alpha' \) is the angular induction factor. The relative flow velocity is dependent on the local inflow angle, \( \phi \), which is the sum of the angle-of-attack, \( \alpha \), and the local pitch angle, \( \beta \). The local pitch angle is determined by the blade geometry, and the angle-of-attack is then determined by the local wind velocity, rotor speed, and induced velocities. Figure 2-2b
shows the resultant forces on the airfoil section in the direction of the flow as well as normal and parallel to the rotor plane. Notice that the angle to transform from the lift and drag forces to those normal and parallel to the rotor plane is the local flow angle, $\phi$. The forces normal and parallel to the rotor plane determine the thrust and torque of the blade element, respectively.

![Diagram showing velocities and forces](image)

Figure 2-2. Wind turbine blade section a) local velocities and angles and b) local forces.

In order to calculate the angle-of-attack at the blade element, the blade inflow angle must be calculated from the local tip speed ratio, $\lambda_r$, and the induced velocity factors as shown in Eqn. (2).

$$\tan \phi = \frac{V_0(1-a)}{\Omega r (1+a')} = \frac{(1-a)}{\lambda_r(1+a')}$$

To calculate the induced velocity factors, the forces on the blade must be determined. Equation (3) is used to calculate the incremental thrust, $dT$, and torque, $dQ$, for an annular section with width $dr$ of the actuator disk using momentum theory.
To relate the induced velocity factors with the local blade forces the combined BEM theory equations are used as shown in Eqn. (4). The derivation of the combined BEM theory equations can be found in *Wind Energy Explained* by Manwell et al. [48].

\[
dT = \rho V_0^2 4a (1-a) \pi rdr \tag{3a}
\]
\[
dQ = 4a'(1-a) \rho V_0 \pi r^3 \Omega \, dr \tag{3b}
\]

In Eqn. (4), \(c\) is the chord length of the blade element. From Eqn. (4), the lift coefficient, \(c_l\), and the drag coefficient, \(c_d\), are found using a table-look-up of two-dimensional airfoil data as a function of angle-of-attack. The set of equations including Eqns. (2), (3), and (4) require an iterative process to find a solution for both the induction factors and the blade forces. The BEM iterative solution process is shown in Fig. 2-3. The final step of the BEM solution method is to calculate the total forces and moments acting on the rotor as well as the power produced by the rotor. This is accomplished by summing the \(N\) elemental forces and moments along the blade as shown in Eqn. (5) for the thrust coefficient, \(C_T\) (5a), and the power coefficient, \(C_p\) (5b). Here \(A\) is the total swept area of the rotor.

\[
C_T = \left( \Sigma^N dT \right) / \left( \frac{1}{2} \rho AV_0^2 \right) \tag{5a}
\]
\[
C_p = \left( \Sigma^N dT * \Omega \right) / \left( \frac{1}{2} \rho AV_0^3 \right) \tag{5b}
\]
2.1.1. XTurb-PSU

XTurb-PSU is the in-house wind turbine design and analysis software code [49]. The code contains functionality for use of either Blade Element Momentum (BEM) theory or Helicoidal Vortex Method (HVM). The XTurb-PSU code is being actively developed at The Pennsylvania State University as a teaching and research tool. It is capable of using a classical BEM solution algorithm based on NREL’s AeroDyn code [46] or a prescribed HVM [50] to predict wind turbine loads and performance. In this work, the XTurb-PSU solver is used exclusively in the BEM mode.

Appendix A and B show two subroutines added to the XTurb-PSU code for the purpose of this and other works. Appendix A shows the subroutine for the Viterna correction applied to airfoil data at both high and low angles-of-attack based on NREL’s AeroDyn Theory Manual [46]. Appendix B includes the subroutine for the Solution-
Based Stall Delay Model (SBSD) described in section 2.3. Finally, Appendix C includes the input files for the wind turbines described in chapter 3.

2.2. Comprehensive Review of Contemporary Stall Delay Models

This section provides an overview of some of the stall delay models described in section 1.4 above. The following three contemporary stall delay models are rooted in theoretical analyses and share a similar methodology for the correction of the 2D lift and drag coefficients described as:

\[ c_{l,3D} = c_{l,2D} + f_l \Delta c_l \]  
\[ c_{d,3D} = c_{d,2D} + f_d \Delta c_d \]  

Here \( f_l \) and \( f_d \) are functions of the specific stall delay model, \( \Delta c_l \) is the difference between the airfoil \( c_{l,2D} \) and the inviscid \( c_l \) (i.e. thin-airfoil theory with \( c_l = 2\pi(\alpha - \alpha_0) \)) where \( \alpha_0 \) is the zero-lift angle), and \( \Delta c_d \) is the difference between the airfoil \( c_{d,2D} \) and the drag coefficient at zero angle-of-attack \( c_{d,0} \), i.e. \( c_{d,0} = c_d(\alpha = 0) \). The coefficients \( c_{l,3D} \) and \( c_{d,3D} \) are the corrected lift and drag coefficients that account for 3D effects.

The method of Snel [43] and Snel et al. [13,44-45] uses an order of magnitude analysis of the boundary-layer equations to propose a simple model to correct the 2D lift coefficient based on the ratio of local chord length to local radial location. Experimental data obtained from the Aeronautical Research Institute of Sweden (FFA) 5WPX turbine is used for validation. The functional relationship for \( f_l \) and \( f_d \) is given as
\[ f_t = 3\left(\frac{C}{r}\right)^2 \] (7)

which is a sole function of the local blade chord ratio \(\frac{C}{r}\).

Du and Selig [16] performed an analysis of the 3D integral boundary layer equations as an extension of the work of Snel et al. [13,44-45] that includes a modified tip speed ratio term, some empirical factors, and a correction for the drag coefficient. Their work concluded in the following relations for the correction factors \(f_t\) and \(f_d\):

\[
f_t = \frac{1}{2\pi} \left[ \frac{1.6\left(\frac{C}{r}\right) a - \left(\frac{C}{r}\right) a_d}{0.1267} \frac{dR}{\Delta r} \right] - 1 \] (8a)

\[
f_d = -\frac{1}{2\pi} \left[ \frac{1.6\left(\frac{C}{r}\right) a - \left(\frac{C}{r}\right) a_d}{0.1267} \frac{dR}{\Delta r} \right] - 1 \] (8b)

\[
\Lambda = \frac{\Omega R}{\sqrt{V_0^2 + (\Omega R)^2}} \] (8c)

Here the additional parameters \(a, b,\) and \(d\) are suggested to be set to unity, and \(\Lambda\) is a modified tip speed ratio whose inclusion resulted in improved comparisons with experimental data from the FFA 5WPX wind turbine blade, the NREL Combined Experiment Rotor (CER), and the Aerostar 7.5-m blade. Raj [51] developed a stall delay model for use in free wake methods based on the work by Du and Selig [16].

The method of Chaviaropoulos and Hansen [28] uses a quasi-3D model based on simplified equations derived from the integration of the 3D incompressible Navier-Stokes equations in the blade spanwise direction. They proposed that rotation also causes an increase in pressure drag. Their model depends on the blade twist angle, the local blade
chord ratio, and several empirical factors. The model contains the following semi-empirical relations to correct the 2D lift and drag coefficients

\[ f_l = a(C/r)^h \cos^n(\beta) \]  \hspace{1cm} (9a)

\[ f_d = a(C/r)^h \cos^n(\beta) \]  \hspace{1cm} (9b)

where the additional empirical parameters have suggested values of \( a = 2.2, \ h = 1, \) and \( n = 4 \) based on comparisons with available data from a Bonus 300 Combi stall-regulated wind turbine. The authors also present a correction for the pitching moment coefficient, which is not described here.

The empirical stall delay model by Corrigan and Schillings [14] is based on experimental helicopter data and simplified boundary-layer equations as formulated by Banks and Gadd [4]. They formulated their model of rotational effects as a delay in the stall angle \( \Delta \alpha \) given by:

\[ \Delta \alpha = (\alpha_{c_{l,\text{max}}} - \alpha_0) \left[ \left( K \frac{C}{r}/0.136 \right)^n - 1 \right] \]  \hspace{1cm} (10a)

\[ c_{L,3D}(\alpha + \Delta \alpha) = c_{L,2D}(\alpha) + \frac{\partial c_{L,\text{pot}}}{\partial \alpha} \Delta \alpha \]  \hspace{1cm} (10b)

where \( \alpha_{c_{l,\text{max}}} \) is the 2D angle-of-attack at \( c_{l,\text{max}} \), \( \alpha_0 \) is the zero-lift angle-of-attack, \( K \) is the assumed linear adverse velocity gradient, and the remaining empirical factor is often set to \( n = 1 \). The gradient \( \frac{\partial c_{L,\text{pot}}}{\partial \alpha} \) is the lift slope in the potential region. The shift in angle-of-attack is applied to the entire airfoil table and therefore causes an intrinsic reduction of
the drag coefficient for a given angle-of-attack. The Corrigan and Schillings model was later evaluated for use with horizontal-axis wind turbines by Tangler and Selig [15].

Dumitrescu et al. [42] developed a correction model based on their findings of an inboard standing vortex using their derived three-dimensional form of the momentum-integral equations for a general rotor blade. They found that the onset of the vortex was in the $r/c$ range between 0.5 and 1.0, and if the root is larger than 1.0, both twist and taper are extrapolated to the $r/c$ value of 1.0. The following equations were developed to correct the two-dimensional lift coefficient:

\[
\alpha_1 = \tan^{-1}\left(\frac{2}{3} \frac{V_0}{\beta_0 \alpha_1}\right) - \beta_1 \tag{11a}
\]

\[
\Delta c_{l1} = 2\pi (\alpha_1 - \alpha_0) - c_{l,2D}(\alpha_1) \tag{11b}
\]

\[
c_{l,3D} = c_{l,2D} + \Delta c_{l1} \left[1 - \exp\left(-\frac{\gamma}{r/c+1}\right)\right] \tag{11c}
\]

Here $c_1$, $\alpha_1$, and $\beta_1$ are the local chord length, angle-of-attack, and twist angle, respectively, at the origin of the vortex and $\alpha_0$ is the zero-lift angle-of-attack. The vortex strength is defined as $\Delta c_{l1}$, which is the difference between the potential lift coefficient and the two-dimensional lift coefficient, $c_{l,2D}$. Finally, the corrected lift coefficient, $c_{l,3D}$, is computed assuming a viscous decay of the vortex in the spanwise direction with the value $\gamma = 1.25$.

Eggers et al. [38] developed a model to correct the normal ($c_n$) and tangential ($c_t$) force coefficients rather than the conventional lift and drag coefficients. The model relies
on axial and angular induction factors $a$ and $a'$ as well as the tip speed ratio $\lambda$. Their model is presented in the following relations

\[ c_{n,3D} = c_{n,2D} + \Delta c_{n,3D} \]  
(12a)

\[ c_{t,3D} = c_{t,2D} + 0.12 \Delta c_{n,3D} \]  
(12b)

\[ \Delta c_{n,3D} = \frac{1}{2} \frac{(r_0^2-r^2)\lambda^2}{(1-a)^2+(1-a)^2(r\lambda)^2} \]  
(12c)

where $r_0$ is the normalized outboard spanwise location where the onset of deep stalled flow occurs. The rotational effects go to zero as $r \rightarrow r_0$ and $\lambda \rightarrow 0$.

The model of Lindenburg [9] is based on an analysis of separated flow at the trailing edge. His model accounts for radial flow due to centrifugal forces. The model depends on the area of separated flow, the local blade chord ratio, and a modified tip speed ratio. The centrifugal pumping model is formulated in the following manner

\[ c_{n,3D} - c_{n,2D} = 1.6\left(\frac{C}{r}\right)(1-f)\left(\Omega r/V_{eff}\right)^2 \]  
(13a)

\[ \left(\Omega r/V_{eff}\right)^2 \approx (\cos \phi)^2 \]  
(13b)

\[ c_n = \frac{\partial c_{n,\text{pot}}}{\partial \alpha} \left(\frac{1+f}{2}\right)^2 (\alpha - \alpha_0) \]  
(13c)

where $f$ is the fraction of the blade chord over which the flow is separated, $V_{eff}$ is the effective axial wind velocity at a blade section, and $\frac{\partial c_{n,\text{pot}}}{\partial \alpha}$ is the slope of the normal force coefficient in the potential region. Equation (8c) is the relation for $f$ as a function of
the normal force coefficient and its slope. As previously stated, Lindenburg’s model also predicts a shift of the separation point towards the trailing edge, which is expressed as a shift in the angle-of-attack. The so obtained rotating angle-of-attack is given by

\[ \alpha_{3D} = \alpha_{2D} + \frac{0.25 \text{rad}}{2\pi} 1.6 \left( \frac{c}{r} \right) \left( \Omega r/V_{eff} \right)^2 \] (13d)

where the factor 1.6 is determined empirically. The rotating angle-of-attack is then used along with the normal force coefficient to geometrically obtain corrections for the lift and drag coefficients using the following relations:

\[ c_{l,3D} = c_{l,2D} + 1.6 \left( \frac{c}{r} \right) \left( \frac{\Omega r}{V_{eff}} \right)^2 \left[ (1 - f)^2 \cos(\alpha_{3D}) + 0.25 \text{rad} \cdot \cos(\alpha_{3D} - \alpha_0) \right] \] (13e)

\[ c_{d,3D} = c_{d,2D} + 1.6 \sin(\alpha_{3D})(1 - f)^2 \left( \frac{c}{r} \right) \left( \Omega r/V_{eff} \right)^2 \] (13f)

Lindenburg also found that a correction was necessary near the tip due to the complex 3D aerodynamics in that region. He proposed the following relation to be applied outboard of the 80% spanwise position

\[ c_{l,3D,tip} = c_{l,2D} - \left( \Omega r/V_{eff} \right)^2 e^{-1.5 AR_{out}} \Delta c_l(c_{l,2D}/c_{l,pot}) \] (13g)

where \( AR_{out} \) is the aspect ratio of the part of the blade outboard of the section under consideration, \( \Delta c_l \) is defined as in Eqn. (6a), and \( c_{l,pot} = 2\pi \sin(\alpha - \alpha_0) \) is the potential lift coefficient.

The model proposed by Bak et al. [10] is based on the difference between the pressure coefficient distribution, \( \Delta c_p \), of a rotating and non-rotating blade given by
where \( x \) is the chordwise position, \( \alpha_{f=1} \) and \( \alpha_{f=0} \) are the angles-of-attack where the flow around the airfoil is just about to separate and just fully separated, respectively. The pressure difference is then integrated around the airfoil geometries to find \( \Delta c_n \) and \( \Delta c_t \).

These values are added to the 2D normal and tangential force coefficients to obtain the corrected values \( c_{n,3D} \) and \( c_{t,3D} \). Finally, the sectional lift and drag coefficients are found by:

\[
\begin{align*}
\text{(14b)} \\
\Delta c_p &= \frac{\pi}{2} \left(1 - \frac{x}{c}\right)^2 \left(\frac{\alpha - \alpha_{f=1}}{\alpha_{f=0} - \alpha_{f=1}}\right)^2 \sqrt{1 + \left(\frac{R}{\tau}\right)^2 \left(\frac{c}{\tau}\right)^2 /[1 + tan^2(\alpha + \phi)]}
\end{align*}
\]

All stall delay models described above have their individual strengths and weaknesses. Some models are based on theoretical considerations, others on empiricism. It appears though that, to date, rotational augmentation or the stall delay phenomenon is incompletely characterized and understood [34]. All contemporary stall delay models share the fact that sectional airfoil data are corrected prior to the actual BEM solution process. This intrinsically hinders all models to detect blade-specific and solution-dependent effects that can otherwise only be captured via data analyses of highly resolved experiments or expensive Computational Fluid Dynamics (CFD) methods. The latest model proposed by Bak et al. [10] is a promising attempt to include these effects.
2.3. Solution-Based Stall Delay Model

The present work approaches the challenging problem of stall delay on wind turbine blades from a different perspective than those models presented in section 2.2. The basic idea is to use classical dimensional analysis to find dimensionless groupings that are relevant to the stall delay phenomenon. Some exponents for dimensional groupings are then determined by considering dependencies found in the contemporary stall delay models described above.

2.3.1. Dimensional Analysis and Buckingham Pi Theorem

To begin the dimensional analysis, all dimensional parameters are identified that are important to the stall delay phenomenon. They are shown in Table 2-1.

<table>
<thead>
<tr>
<th>Blade Geometry</th>
<th>Flow Conditions</th>
<th>Solution-dependent</th>
</tr>
</thead>
<tbody>
<tr>
<td>$R$, $r$, $c$, $\Delta \beta$, $\frac{d \beta}{dr}$</td>
<td>$V_0$, $\Omega$</td>
<td>$V_{rel}$, $\Gamma$</td>
</tr>
</tbody>
</table>

The blade geometry parameters include the blade radius $R$, the local radial station $r$, the blade chord $c$, and the blade twist distribution $\beta(r)$ through the total blade twist $\Delta \beta$ and the twist slope $d \beta / dr$. As for the flow conditions, the wind speed $V_0$ and the rotor speed $\Omega$ are considered. As far as solution-dependent dimensional parameters are concerned, consider the relative velocity $V_{rel}$ seen by a local airfoil section and being a
function of axial and angular induction factors $a$ and $a'$, see Eqn. (15), and the spanwise
distribution of blade circulation $\Gamma(r)$. Note that the blade pitch angle is not included in
the list of dimensional parameters under flow conditions as it is assumed that the blade
pitch setting indirectly affects the solution-dependent parameters.

$$V_{rel} = \sqrt{\left(V_0 (1 - a)\right)^2 + \left(\Omega r (1 + a')\right)^2}$$  \hspace{1cm} (15)

Next, there are three independent dimensions that describe the dimensional
parameters in Table 2-1: length, time, and radians. The classical Buckingham Pi Theorem
[52] states that the number of dimensionless groupings is equal to the number of
dimensional parameters subtracted by the number of independent dimensions. Therefore,
there are a total of six dimensionless groupings that are relevant to the stall delay
problem. These six dimensionless groupings shown in Table 2-2, or a combination of
them, will be used to modify the lift and drag coefficients in the solution-based stall delay
model.

<table>
<thead>
<tr>
<th>Blade Geometry</th>
<th>Flow Conditions</th>
<th>Solution-dependent</th>
</tr>
</thead>
<tbody>
<tr>
<td>$B_1 = \frac{r}{R}$</td>
<td>$B_2 = \frac{c}{R}$</td>
<td>$B_3 = \frac{d\beta}{d\Omega R}$</td>
</tr>
<tr>
<td>$\Phi_1 = \frac{\Omega R}{V_0}$</td>
<td>$\Sigma_1 = \frac{\Gamma}{V_0 R}$</td>
<td>$\Sigma_2 = \frac{V_0}{V_{rel}}$</td>
</tr>
</tbody>
</table>

Thus, 3D airfoil data become a function of the dimensionless groupings as:

$$c_{l,3D}, c_{d,3D} = F\left(B_1^{b_1}, B_2^{b_2}, B_3^{b_3}, \Phi_1^{f_1}, \Sigma_1^{s_1}, \Sigma_2^{s_2}\right)$$  \hspace{1cm} (16)
From a general fluid dynamics approach, the functional relationship $F$ along with the exponents of the dimensionless groupings is determined from a wealth of experimental data. Unfortunately, only a limited amount of data is available for rotating wind turbine blades. Therefore, the proposed solution-based stall delay model will build upon some of the previously presented contemporary stall delay models. The new model becomes solution dependent by including, in particular, the solution-dependent dimensionless groupings $\Sigma_1$ and $\Sigma_2$ in Table 2-2.

### 2.3.2. Methodology of a Solution-Based Stall Delay Model

The proposed solution-based stall delay model builds upon the well-established Du and Selig [16] stall delay model in Eqn. (8a-c) with the drag coefficient correction proposed by Eggers et al. [38] in Eqn. (12a-c). This combination is currently used as a standard in NREL’s AirfoilPrep worksheet [53]. It was observed for the NREL Phase VI and MEXICO rotors that solution-dependent effects of rotational augmentation can be related to the spanwise blade circulation distribution, a fact that had also been suggested by Tangler [35]. One has to keep in mind, though, that the spanwise circulation is not readily available in a BEM-based solver. However, the classical Kutta-Joukowski lift theorem is used to obtain the equivalent blade circulation $\Gamma'$ from Eqn. (17).

$$\Gamma' = \frac{1}{2} V_{rel} c_l c$$

(17)

As noted by Tangler [35], one of the primary effects of rotational augmentation is to increase the magnitude of the spanwise circulation $\Gamma'$, and thus the lift, at the inboard
blade sections. It was observed that this increase in magnitude tends to occur inboard of the maximum spanwise circulation value $I_{\text{max}}$. This location was chosen as the breakpoint for solution-dependent rotational corrections. Thus, all lift and drag corrections due to rotational augmentation are to be applied inboard of the maximum circulation value. The solution-based stall delay model works as follows, see Fig. 2-4: Initially, the BEM code is run for the given operating conditions of the wind turbine without the use of any stall delay model. In the following, this run will be referred to as ‘Baseline’ or ‘Iter=1’. The maximum equivalent spanwise circulation value $I_{\text{max}}$ is obtained from the ‘Baseline’ run, and its spanwise location is used later as the breakpoint for the solution-dependent rotational corrections. Following, the BEM code is rerun as ‘Iter=2’ with sectional airfoil data being modified according to the Du and Selig [16] stall delay model described in Eqn. (8a-c) with the drag correction by Eggers et al. [38] described in Eqn. (12a-c) similar to NREL’s AirfoilPrep worksheet [53]. The adjustment suggested by Hansen [53] for large angles-of-attack is used as implemented in AirfoilPrep. The differences between the corrected (Iter=2) lift and drag coefficients and the lift and drag coefficients determined by the baseline run (Iter=1) are then stored at each spanwise station as $\Delta d_{c_l}$ and $\Delta d_{c_d}$, respectively. Next, the solution-dependent process is initiated (Iter=3) as a BEM run using the following relations to correct the lift and drag coefficients

$$c_{l,\text{Iter}} = c_{l,\text{Iter-1}} + f_{\text{conv}} g_{c_l} \Delta d_{c_l} \quad (18a)$$

$$c_{d,\text{Iter}} = c_{d,\text{Iter-1}} + f_{\text{conv}} g_{c_d} \Delta d_{c_d} \quad (18b)$$
where $f_{\text{conv}}$ is a local convergence criterion based on the spanwise circulation $\Gamma$ and obtained from

$$f_{\text{conv}} = \frac{\|\Gamma_{\text{Iter-1}} - \Gamma_{\text{Iter-2}}\|}{r_{\text{max}}} \quad (19)$$

The solution-dependent process is repeated with successive application of Eqn. (18) followed by a full BEM solution until the spanwise circulation $\Gamma$ is converged to $f_{\text{conv}} \leq 5.0 \times 10^{-4}$. The results will show that this is typically achieved not later than $\text{Iter} = 5$. In Eqn. (18), $g_{c_l}$ and $g_{c_d}$ are chosen as a combination of the dimensionless groupings in Table 3-2. As far as $g_{c_l}$ is concerned, it is assumed that a solution-dependent modification to the sectional coefficient of lift $c_i$ does not depend on the blade twist $\beta$, i.e. $b_3 = 0$ in Eqn. (16). However, $g_{c_l}$ is related to other dimensionless groupings known from contemporary stall delay models described earlier. In particular, the dependence on the chord ratio as suggested by Snel et al. [13,44-45] and a modified centrifugal pumping effect as suggested by Lindenburg [7] are considered. The correction factor is written as

$$g_{c_l} = \left(\frac{\Gamma}{r}\right)^{1/2} \left(\frac{\Gamma}{\nu_{\text{rel}}^2}\right)^{1/2} = B_1^{b_1} B_2^{b_2} B_3^{b_3} F_1^{f_1} \Sigma_1^{s_1} \Sigma_2^{s_2} \quad (20a)$$

such that $b_1 = -4, b_2 = 2, b_3 = 0, f_1 = -2, s_1 = 1/2, s_2 = -3/2$. Note that $\nu_{\text{rel}}$ and $\Gamma$ are chosen at their respective radial locations and iteration $\text{Iter}$. Equation (20a) includes all dimensionless groupings from Table 2-2, in particular the solution-dependent groupings $\Sigma_1$ and $\Sigma_2$, except for the relative blade twist $B_3$. Equation (20a) was obtained empirically by comparison to available data from the NREL Phase VI rotor and MEXICO experiments as described in the ‘Results and Discussion’ section. Nevertheless,
the main idea and contribution of this work is to include solution-dependent dimensionless groupings towards a general stall delay model. As a next step, consider a solution-dependent modification $g_{c_d}$ to a sectional coefficient of drag $c_d$ and choose

$$g_{c_d} = -\frac{1}{3} \frac{R}{r} \left( \frac{e}{r} \right)^{-1} \left( \frac{d \Phi}{dr} \right)^{-1} \left( \frac{2 \psi_{rel}}{\Omega r} \right)^{-1} = -\frac{1}{3} B_1^{b_1} B_2^{b_2} B_3^{b_3} \phi_{f_1} \Sigma_1^{s_1} \Sigma_2^{s_2}$$

such that $b_1 = 3, b_2 = -1, b_3 = 1, f_1 = 1, s_1 = 0, s_2 = 1$. Again, this choice was obtained empirically by comparison against data from the NREL Phase VI and MEXICO rotors presented in the ‘Results and Discussion’ section. Furthermore, the drag coefficient correction $g_{c_d}$ is used only if there exists a local maximum in the circulation distribution $\Gamma$ inboard of the absolute maximum $\Gamma_{max}$ in the baseline BEM run (Iter=1). The existence of a local maximum inboard of $\Gamma_{max}$ suggests that the flow is separated over the blade region in between, a solution-dependent characteristic that cannot be detected in contemporary stall delay models. The solution-dependent drag correction $g_{c_d}$ is then applied exclusively between both maxima of $\Gamma$. It will be seen that this region is more pronounced for the NREL Phase VI than for the MEXICO rotor, which indicates a difference in the separation behavior between both rotor blades that has been noted predominantly in the works of Schreck et al. [2,20,40]. Figure 2-4 illustrates the methodology of the solution-based stall delay model. The BEM solver used is the XTurb-PSU design and analysis software described in section 2.1.1.
Figure 2-4. Methodology of the Solution-based Stall Delay Model (SBSD).
Chapter 3

Results and Discussion

The NREL Phase VI rotor [8] and MEXICO experiment [54] offer a wealth of measured data to study rotational augmentation for wind turbine rotors and differ substantially in blade geometry and operating conditions, see Table 3-1. The range of tip speed ratios covered by the NREL Phase VI and MEXICO turbines includes that of standard utility-scale wind turbines.

Table 3-1. NREL Phase VI and MEXICO rotor specifications.

<table>
<thead>
<tr>
<th></th>
<th>NREL Phase VI</th>
<th>MEXICO</th>
</tr>
</thead>
<tbody>
<tr>
<td>Radius [m]</td>
<td>5.029</td>
<td>2.25</td>
</tr>
<tr>
<td>RPM</td>
<td>72</td>
<td>424.5</td>
</tr>
<tr>
<td>Tip Pitch [deg]</td>
<td>3.0</td>
<td>-2.3</td>
</tr>
<tr>
<td>TSR (Data range)</td>
<td>3 – 7.5</td>
<td>4 – 10</td>
</tr>
</tbody>
</table>

In a recent investigation, Schreck et al. [40] noted that the NREL Phase VI and MEXICO experiments show surprising similarities but also some distinct differences in rotational augmentation effects. The similarities include a steepening of the normal force coefficient versus angle-of-attack curve for low to moderate angle-of-attack followed by
a delay of the stall angle-of-attack and an amplified maximum normal force coefficient. However, the two rotors have dissimilar flow separation on each blade. The NREL Phase VI blade pressure coefficient distribution implies a leading-edge separation followed by a shear layer impingement, while the MEXICO blade pressure coefficient distribution is consistent with a trailing-edge separation. In order to investigate the similarities and differences between the NREL Phase VI and MEXICO rotor blades, first consider three important dimensionless groupings that are proposed in Eqn. (20a) for $g_{s\alpha}$ and the solution-based stall delay model.

\[
\frac{c}{R} = B_2 \quad (21a)
\]

\[
\frac{r}{V_{rel}R} = \Sigma_1 \Sigma_2 \quad (21b)
\]

\[
\frac{F_{coriolis}}{F_{centrifugal}} = \frac{2\Omega V_{rel}}{\alpha^2 r} = \frac{2V_{rel}}{\alpha r} = 2B_1^{-1} \Phi_1^{-1} \Sigma_2^{-1} \quad (21c)
\]

It is known that the local blade chord ratio in Eqn. (21a) is of significant importance in the solution-based stall delay model as has been shown in many of the contemporary stall delay models [9,13-14,16,33]. Previous analyses [40,55] of measured data from the NREL Phase VI and MEXICO rotors demonstrated that stall delay associated with rotational augmentation effects are prominent down to $c/r = 0.17$ and remain detectable up to $c/r = 0.10$. Figure 3-1 shows the local blade chord ratio for the NREL Phase VI and MEXICO blades and suggests that stall delay effects are more pronounced along the NREL Phase VI than the MEXICO rotor blade. Note in Fig. 3-1
that the local chord ratio is only shown up to the inboard station where the root airfoil begins to transition into a circular cylinder.

Figure 3-1. (c/r) vs. (r/R) for NREL Phase VI and MEXICO rotors.

Next, consider the normalized circulation term in Eqn. (21b) for both rotors at $Iter = 1$, i.e. before executing the Du and Selig stall delay model with the Eggers et al. drag coefficient correction described above. Figure 3-2 illustrates the normalized circulation over the blade span for the NREL Phase VI and MEXICO rotors over a range of wind speeds from attached to separated flow conditions along the blade. Higher values for the normalized circulation are apparent for the NREL Phase VI rotor with increasing wind speed and support the fact that rotational augmentation effects are more pronounced along the blade of the former rotor.
Finally, Eqn. (21c) is a variation of the centrifugal pumping parameter that is physically significant to the stall delay problem and used in the model developed by Lindenburg [9]. This is plotted in Fig. 3-3 for the NREL Phase VI and MEXICO rotors at $Iter = 1$, i.e. just before executing the Du and Selig stall delay model with the Eggers et al. drag coefficient correction. The solution-based methodology of the present work, though, allows for using the true $V_{rel}$ term from the actual solution within the model. Figure 3-3 supports once more that stall delay (or rotational augmentation) effects are stronger for the NREL Phase VI compared to the MEXICO rotor.
In the following sections, results obtained with the new solution-based stall delay model are compared to available data from the NREL Phase VI and MEXICO experiments.

### 3.1. NREL Phase VI Rotor

The solution-based stall delay model has been applied to the NREL Phase VI rotor, see Appendix C.1, in steady and zero-yaw conditions [8]. In this work, the comparisons are focused on the dimensionless equivalent spanwise circulation distribution and normal and tangential forces as well as force coefficients. Four wind speeds (7m/s, 9m/s, 11m/s, and 13m/s) are considered ranging from fully attached to stalled flow along the NREL Phase VI rotor blade.
3.1.1. NREL Phase VI Rotor – Equivalent Blade Circulation

First, the effect of the solution-based stall delay model on the dimensionless equivalent spanwise circulation as defined in Eqn. (12), hereafter referred to as the circulation distribution, is presented. Figure 3-4 shows the circulation distribution at the four wind speeds considered in this work. The line labeled as ‘Baseline’ is the first run (Iter=1) of the BEM solver with no correction to sectional airfoil data. The dashed lines labeled ‘Iter =’ correspond to the respective iterations of the solution methodology illustrated in Fig. 2-1. The run with ‘Iter = 2’ denotes the Du and Selig stall delay model from Eqn (8a-c) used in conjunction with the Eggers et al. correction for the drag coefficient from Eqn (12a-c). The Du and Selig stall delay model with the Eggers et al. drag coefficient correction (Iter=2) will be referred to as the ‘contemporary stall delay model’ for the remainder of this discussion. The dashed iteration lines show the circulation distribution as it changes with modified sectional lift and drag coefficients. The final solution is achieved once the circulation distribution is converged as outlined in Fig. 2-1. This is denoted in the figures by the solid black line labeled ‘Final’. The data point labeled $I_{max}$ is the maximum circulation value obtained in the baseline (Iter=1) run that is used as the breakpoint for solution-based rotational corrections applied exclusively inboard of that location. Tangler [35] studied the NREL Phase VI experimental data and constructed the experimental blade spanwise circulation data, labeled as ‘Tangler’. Also included in the figures are the radial locations associated with a local blade chord ratio $c/r = 0.1$ and $c/r = 0.17$. As mentioned earlier, these locations are suggested by Schreck et al.
to be the locations inboard of which rotational augmentation is detectable \( (c/r = 0.1) \) and rotational augmentation is prominent \( (c/r = 0.17) \). At 7m/s, Fig. 3-4(a) shows little modification from the baseline as the rotor is operating in attached flow at this wind speed with sectional angles-of-attack well below the onset of separation and stall. At 9m/s, Fig. 3-4(b) shows a moderate amount of rotational augmentation correction inboard with the new solution-based model exhibiting higher values for the blade circulation inboard of \( \Gamma_{\text{max}} \) than the baseline case. The new model increases the circulation further than the contemporary stall delay model and is slightly closer to the data suggested by Tangler [19]. At 11m/s, Fig. 3-4(c) shows large increases in the spanwise circulation distribution by the stall delay model, which correlate well with the experimental data at the 30% and 47% radial locations. However, the 63% station is overpredicted by both the solution-based and contemporary stall delay models. This sudden decrease in the circulation distribution is likely due to a standing vortex on the upper surface of the blade that has been proposed by several researchers in the past [2,20-22]. Finally, at 13m/s, Fig. 3-4(d) shows a large increase in the circulation distribution for the inboard part of the blade. The solution-based stall delay model approaches the experimental value at the 30%, 47%, and 63% stations. All four figures show an overprediction of the circulation distribution near the tip, a characteristic common to BEM predictions of wind turbine loads. The classical root and tip loss factors have been used in the BEM solver based on NREL’s AeroDyn code [46] with no additional correction. It is interesting to note that both the 11m/s and 13m/s circulation distributions have an inboard maximum. This suggests that the flow is separated outboard of that location. It is also interesting to observe in Figs. 3-4(a-c) that the maximum spanwise circulation location matches quite
closely to the $c/r = 0.17$ location that is commonly associated with the spanwise location up to which rotational augmentation is prominent, see also Snel et al. [13,43-45] and Schreck et al. [56]. Outboard of $c/r = 0.1$, the effect of rotational augmentation is no longer detectable as has been suggested by other researchers.
Figure 3-4. NREL Phase VI equivalent spanwise circulation distribution for (a) $V_{\text{wind}} = 7\text{ m/s}$, (b) $V_{\text{wind}} = 9\text{ m/s}$, (c) $V_{\text{wind}} = 11\text{ m/s}$ and (d) $V_{\text{wind}} = 13\text{ m/s}$.
3.1.2. NREL Phase VI Rotor – Sectional Normal and Tangential Forces

Next, the effect of the solution-based stall delay model on the normal force coefficient along the blade is presented. Figure 3-5 shows the normal force coefficient along the blade for the four wind speeds considered. The labeling convention is the same as described above except for the experimental data. The experimental data for the normal force coefficient are available for the NREL Phase VI rotor [8] and labeled as ‘NREL’ in the figures. Figure 3-5(a) shows insignificant modification to the normal force coefficient along the blade at 7m/s attributed to the fact that the flow is attached along the entire blade. Figure 3-5(b) shows good agreement in the normal force coefficient with the experimental data at 9m/s for the ‘Final’ iteration of the solution-based stall delay model. Figure 3-5(c) illustrates that at a wind speed of 11m/s the solution-based stall delay model shows improved agreement with experimental data over the contemporary stall delay model (Iter=2). However, similar to Fig. 3-4(c), there is an overprediction of the blade loads at the 63% radial location. Finally, Fig. 3-5(d) shows improved prediction at 13 m/s near the 30% and 47% radial locations when compared to the contemporary stall delay model with a small overprediction of the normal force coefficient at the 63% radial station. Again, all four figures show an overprediction of the sectional loads at the outer 20% of blade consistent with standard BEM-type solvers.
Figure 3-5. NREL Phase VI normal force coefficient along the blade span for (a) \( V_{\text{wind}} = 7\text{m/s} \), (b) \( V_{\text{wind}} = 9\text{m/s} \), (c) \( V_{\text{wind}} = 11\text{m/s} \) and (d) \( V_{\text{wind}} = 13\text{m/s} \).
Figure 3-6 shows the tangential force coefficient along the blade span for the four wind speeds considered. Figure 3-6(a) presents insignificant modification to the tangential force coefficient along the blade, which is again attributed to attached flow conditions at 7m/s. At 9m/s, see Fig. 3-6(b), one can observe a small amount of improved agreement of the tangential force coefficient with experimental data when contrasted to the contemporary stall delay model (Iter=2). At 11m/s, Fig. 3-6(c) illustrates that by using the solution-based stall delay model an improved agreement is obtained with the experimental data when compared to the contemporary model. However, as in Figs. 3-4(c) and 3-5(c), there is an overprediction of the blade loads at the 63% radial location. Improved correction near the 30% and 47% radial locations over the contemporary stall delay model is shown in Fig. 3-6(d) for the 13m/s case. The improvement is particularly noticeable with regards to the trend of the curve when examined against the experimental data. The correction for the 11m/s and 13 m/s cases is still insufficient to match closely to experimental data, which shows a sharp decrease in the tangential force coefficient most probably due to the inboard standing vortex. One has to be aware, though, that tangential force coefficients in Fig. 3-6 are an order of magnitude smaller than the normal force coefficients in Figs. 3-5. Consequently, one can therefore expect higher discrepancies with the data. It is apparent that the new solution-based stall delay model captures the data trend quite well.
Figure 3-6. NREL Phase VI normal force coefficient along the blade span for (a) $V_{\text{wind}} = 7$ m/s, (b) $V_{\text{wind}} = 9$ m/s, (c) $V_{\text{wind}} = 11$ m/s and (d) $V_{\text{wind}} = 13$ m/s.
Figure 3-7 shows the normal and tangential forces along the NREL Phase VI blade for wind speeds of 7m/s and 13m/s. A comparison to measured normal and tangential forces assists in quantifying how well the sectional dynamic pressure, proportional to $V_{rel}^2$, is predicted by the solution-based stall delay model. Figures 3-7(a) and 3-7(b) show the normal and tangential forces, respectively, along the blade at a wind speed of 7m/s. As expected, little correction is added by the solution-based stall delay model due to attached flow conditions along the blade that results in close agreement with measured data. At 13m/s, Figs. 3-7(c) and 3-7(d) show that the solution-based stall delay model (Final) exhibits closer agreement to measured data than the contemporary stall delay model (Iter=2). Good agreement with measured data is seen in Fig. 3-7(c) with a small underprediction of the normal force at the 30% station; however the typical overprediction of the tip loads. Figure 3-7(d) shows overall good agreement with the trends of the measured tangential force along the blade with some overprediction for most of the blade.
Figure 3-7. NREL Phase VI normal forces along the blade span for (a) $V_{\text{wind}} = 7\text{m/s}$ and (c) $V_{\text{wind}} = 13\text{m/s}$ as well as the tangential forces along the blade span for (b) $V_{\text{wind}} = 7\text{m/s}$ and (d) $V_{\text{wind}} = 13\text{m/s}$. 
3.1.3. NREL Phase VI Rotor – Sectional Normal Force Coefficients vs. Angle-of-Attack

Figure 3-8 shows the normal force coefficient as a function of angle-of-attack for three selected radial stations along the NREL Phase VI rotor blade. The following figures show the baseline airfoil polar data (Baseline), the iteration using the Du and Selig model with the Eggers et al. drag coefficient correction (Iter=2), the final iteration by the solution-based stall delay model (Final), and measured normal force coefficients (NREL). At the 30% radial station in Fig. 3-8(a), a large amount of correction to the normal force coefficient is apparent for the solution-based stall delay (SBSD) model when compared to the 2D polar data. The maximum corrected normal force coefficient is more than two times greater than the 2D maximum. There is some discrepancy between the corrected normal force coefficient and the measured data. It is interesting to note that the steepening of the $C_n$ versus angle-of-attack curve for $\alpha > 15\text{deg}$ is captured quite well when using the solution-based stall delay model. It appears, though, as if NREL data are shifted to higher angles-of-attack compared to SBSD results. The reason for the discrepancy is unknown at present. At the 47% radial station in Fig. 3-8(b), some stall delay corrections are apparent compared to the 2D data. Similar trends can be observed for the SBSD computed data points and the measured NREL data. However, there is again a shift in angle-of-attack between the two data sets as had been observed at the 30% radial station in Fig. 3-8(a). It is unclear at present whether SBSD computed angle-of-attack, NREL measured angle-of-attack, or a combination of both is responsible for this observation in Figs. 3-8(a-b). At the 63% radial station in Fig. 3-8(c), some difference
between the measured (NREL) and the corrected (Final) normal force coefficients is apparent for higher angles-of-attack. The 63% radial station appears to be the outboard limit affected by stall delay, see also the $c/r=0.17$ line in Fig. 3-4. All cases show some delay of stall when using the solution-based stall delay model as well as an increase in the magnitude of the maximum normal force coefficient in comparison to the 2D polar data.

The results obtained by the solution-based stall delay model for the NREL Phase VI rotor show overall improved agreement with experimental data when compared to the existing contemporary stall delay model.
Figure 3-8. NREL Phase VI normal force coefficient vs angle-of-attack at (a) r/R = 30%, (b) r/R = 47% and (c) r/R = 63%.
3.2. MEXICO Rotor

The solution-based stall delay model was also applied to the MEXICO [54] rotor, see Appendix C.2. The results used in this study for the MEXICO rotor use the three-bladed upwind zero yaw test configuration. The rotor is operated at constant rotational speed and constant pitch for all wind speeds. This section presents results obtained by the solution-based stall delay model for the equivalent blade circulation and normal and tangential forces as well as force coefficients in comparison to measured data from the MEXICO experiment. Three wind speeds (10 m/s, 15 m/s, and 24 m/s) are considered that encompass attached as well as partially separated and stalled flow along the MEXICO rotor blade at zero yaw and a constant rotor speed of 424.5 rpm.

3.2.1. MEXICO Rotor – Equivalent Blade Circulation

Figure 3-9 shows the circulation distribution at the three wind speeds considered. The labeling convention is the same as described previously. Figure 3-9(a) shows little modification from the baseline, because the rotor is operating in attached flow at the 10 m/s wind speed. At 15 m/s, Fig. 3-9(b) shows a moderate amount of correction inboard with the solution-based model exhibiting larger values inboard of $I_{\text{max}}$ than the baseline case. Figure 3-9(c) shows significant increases in the inboard circulation distribution computed by the solution-based stall delay model at 24 m/s also compared to the contemporary stall delay model (Iter=2). The circulation distribution at 24 m/s has an inboard local maximum, and therefore the solution-based drag coefficient correction
$g_{cd}$ from Equation (20b) is used for this case. It is interesting to note that the location of $I'_{max}$ is outboard of the location of $c/r=0.1$. This differs from what has been observed for the NREL Phase VI rotor, thus providing a good means for testing the solution-based stall delay model. Rotational augmentation effects are apparent and prominent inboard of $c/r = 0.17$ for wind speeds of 15m/s and 24m/s. Furthermore, though the solution-based corrections are active everywhere inboard of the $I'_{max}$ location determined in the baseline (Iter=1) run, Fig. 3-9(c) reveals that results obtained by the solution-based stall delay model only become distinguishable from the contemporary stall delay model (Iter=2) inboard of the $c/r=0.17$ line.
Figure 3-9. MEXICO equivalent spanwise circulation distribution for (a) $V_{\text{wind}} = 10\text{m/s}$, (b) $V_{\text{wind}} = 15\text{m/s}$ and (c) $V_{\text{wind}} = 24\text{m/s}$.
3.2.2. MEXICO Rotor – Sectional Normal and Tangential Forces

Next, the effect of the solution-based stall delay model on the normal and tangential forces along the blade will be discussed. Figure 3-10 shows the normal force along the blade span for the three wind speeds considered. Measured data for the normal force are labeled as ‘MEXICO’ in the figures. Figure 3-10(a) shows good agreement with measured data and insignificant modification to the normal force along the blade. This is attributed to the fact that the flow is attached along the entire blade. Figure 3-10(b) also shows good agreement at a wind speed of 15m/s for the normal force when compared to the measured data. The contemporary (Iter=2) and solution-based (Final) stall delay models are nearly indistinguishable in Fig. 3-10(b) and show a small rotational augmentation effect when compared to the baseline (Iter=1) case. At a wind speed of 24m/s shown in Fig. 3-10(c), similar corrections near the 25% and 35% radial locations are obtained for both the contemporary and solution-based stall delay models. In comparison to the data, though, it can be seen that the solution-based stall delay model more closely matches the trend of the measured normal forces compared to the conventional stall delay model. As has been observed for the Phase VI rotor, sectional normal forces are overpredicted in the tip region consistent with BEM models that use standard tip correction factors.
Figure 3-10. MEXICO normal force along the blade span for (a) $V_{\text{wind}} = 10\text{ m/s}$, (b) $V_{\text{wind}} = 15\text{ m/s}$ and (c) $V_{\text{wind}} = 24\text{ m/s}$.
Figure 3-11 shows the tangential forces along the blade span for the three wind speeds considered. Note that the scaling of the tangential force is different for each of the three figures for better quantitative comparison. At 10m/s, Fig. 3-11(a) shows negligible modification to the tangential force along the blade due to attached flow conditions. At a wind speed of 15m/s shown in Fig. 3-11(b), a small increase in the tangential force is observed for the solution-based stall delay model (Final) close to the blade root. At a wind speed of 24m/s shown in Fig. 3-11(c), discrepancies between measured and both the contemporary (Iter=2) and solution-based (Final) stall delay models can be noted. However, it is apparent that the solution-based stall delay model better predicts the trend of the data with an underprediction of approximately 25N/m up to the c/r=0.1 location around the 62% radial station. One again should keep in mind that the tangential forces are an order of magnitude smaller than the normal forces in Fig. 3-10, and quantitative comparisons are therefore quite sensitive. The figures continue to show an overprediction of the tip loads.

The results obtained by the solution-based stall delay model show overall improved agreement with measured data over the contemporary stall delay model.
Figure 3-11. MEXICO tangential force along the blade span for (a) $V_{\text{wind}} = 10\text{m/s}$, (b) $V_{\text{wind}} = 15\text{m/s}$ and (c) $V_{\text{wind}} = 24\text{m/s}$.
3.2.3. MEXICO Rotor – Sectional Normal Force Coefficients vs. Angle-of-Attack

Figure 3-12 shows the normal force coefficient as a function of angle-of-attack for two radial stations along the MEXICO rotor blade. The figures show the baseline airfoil polar data (Baseline), the iteration using the Du and Selig model with the Eggers et al. drag coefficient correction (Iter=2), the final iteration by the solution-based stall delay model (Final), and measured normal force coefficients (MEXICO). The measured normal force coefficients are digitized from Schreck et al. [40]. At the 25% radial station in Fig. 3-12(a), a large correction to the normal force coefficient is apparent for the solution-based stall delay model when compared to the 2D polar data. The maximum corrected normal force coefficient is about two times larger than the 2D maximum. The corrected data matches very closely with the measured data. At the 60% radial station in Fig. 3-12(b), there exists a small shift in the angle-of-attack combined with a small amplification of the normal force coefficient compared to the 2D data.
Figure 3-12. MEXICO normal force coefficient vs angle-of-attack at (a) $r/R = 25\%$ and (b) $r/R = 60\%$.

### 3.3. Notional 2.3-MW Rotor

Accounting for 3D rotational effects associated with stall delay is also important for utility-scale wind turbine blades. However, documented evidence of stall delay on utility-scale wind turbine blades has only recently been presented by Schreck et al. [56] who analyzed measured surface pressures on a 2.3-MW wind turbine blade with thick flatback airfoil cross sections at the National Wind Technology Center (NWTC). The pressure instrumentation and data processing techniques used on the 2.3-MW blade are documented by Medina et al. [57]. It was found that the occurrence and intensity of rotational augmentation was consistent with previous observations. In particular, the presence of thick flatback airfoils did not impede rotational augmentation effects, and
rotationally augmented blade loads persist even in highly turbulent inflow conditions at the NWTC test site.

A schematic view of the blade planform and the spanwise locations where data were analyzed are documented [56], however no information is available concerning blade twist, airfoils, and operating conditions (rotor speed, blade pitch setting angle, etc.). A notional 2.3-MW wind turbine blade, see Appendix C.3, was therefore designed based on information contained in the study by Schreck et al. [56] and on available data in the Siemens product brochure [58] for the Siemens SWT-2.3-101 wind turbine. For this work, a 101 meter diameter notional 2.3-MW blade is equipped with publicly available Delft University of Technology airfoils [59] starting at $r/R = 0.30$ and a FB-3500-0875 flatback airfoil [60] inboard of that location. Table 3-2 shows the notional 2.3-MW blade geometry and airfoils.

Table 3-2. Notional 2.3-MW blade design.

<table>
<thead>
<tr>
<th>$r/R$</th>
<th>$c/r$</th>
<th>Twist [deg]</th>
<th>Airfoil</th>
</tr>
</thead>
<tbody>
<tr>
<td>0.14</td>
<td>0.4223</td>
<td>16.00</td>
<td>FB-3500-0875</td>
</tr>
<tr>
<td>0.25</td>
<td>0.2607</td>
<td>12.41</td>
<td>FB-3500-0875</td>
</tr>
<tr>
<td>0.30</td>
<td>0.2157</td>
<td>9.85</td>
<td>DU 00-W2-350</td>
</tr>
<tr>
<td>0.35</td>
<td>0.1828</td>
<td>7.28</td>
<td>DU 97-W-300</td>
</tr>
<tr>
<td>0.40</td>
<td>0.1517</td>
<td>5.36</td>
<td>DU 91-W2-250</td>
</tr>
<tr>
<td>0.55</td>
<td>0.0780</td>
<td>1.50</td>
<td>DU 93-W-210</td>
</tr>
<tr>
<td>0.80</td>
<td>0.0255</td>
<td>0.67</td>
<td>DU 95-W-180</td>
</tr>
<tr>
<td>1.00</td>
<td>0.0020</td>
<td>0.00</td>
<td>DU 95-W-180</td>
</tr>
</tbody>
</table>
The power curve of the Siemens 2.3-MW wind turbine was digitized from the SWT-2.3-101 brochure with an assumed mechanical/electrical loss of 6.0% for all wind speeds. The notional 2.3-MW turbine design is pitch regulated with variable speed ranging from 6 to 16 rotations per minute (RPM). Figure 3-13 shows the power curve for the notional 2.3-MW design compared to the SWT-2.3-101. Close agreement in the power is seen between both rotor designs.

Figure 3-13. Notional 2.3-MW power curve.
Rotational augmentation effects are expected to occur down to $c/r = 0.17$, i.e. $r/R \approx 0.34$ in Table 3-2. Figure 3-14 shows the normal force coefficient along the blade span at a wind speed $V_{\text{wind}} = 8\text{m/s}$. It is seen that rotational augmentation is indeed present down to that location with little difference between predictions by the contemporary stall delay model (Iter=2) and the solution-based stall delay model (Final).

Figures 3-15 and 3-16 show the solution-based dimensionless groupings from Eqns. (21a) and (21c), respectively, computed at Iter=1 of the solution method for selected wind speeds of the power curve in Figure 3-13. The solution-based dimensionless groupings show less variation between wind speeds when compared to the NREL Phase VI and MEXICO rotors in Figs. 3-2 and 3-3. This is attributed in part to the
variable pitch and speed control of the notional 2.3-MW turbine. However, the same trends that were seen in Figs. 3-2 and 3-3 are present for the utility-scale notional 2.3-MW turbine in Figs. 3-15 and 3-16. Primarily, Fig. 3-16 shows an increase in the ratio of coriolis to centrifugal forces inboard and with increasing wind speed.

Figure 3-15. \( \Gamma/(V_{rel}R) \) vs. \((r/R)\) for Notional 2.3-MW rotor (Iter=1).
3.3.1. Notional 2.3-MW Rotor – Sectional Normal Force Coefficient vs. Tangential Force Coefficient

The work of Schreck et al. [56] presented normal and tangential force coefficients at the 7m (r/R = 0.14) and the 13m (r/R = 0.26) spanwise stations. Table 3-2 reveals that both stations are located inboard of c/r = 0.17 where rotational augmentation effects are expected. Figure 3-17 shows computed normal vs. tangential force coefficients for the 17% and 28% spanwise stations along the notional 2.3-MW rotor blade. The figures show the baseline airfoil polar data (2D) of the FB-3500-0875 flatback airfoil and the final iteration by the solution-based stall delay model (SBSD Final). The data
presentation differs from that for the NREL Phase VI and MEXICO rotors; however it is consistent with how data were presented by Schreck et al. [56]. At the 17% radial station in Fig. 3-17(a) and 28% radial station in Fig. 3-17(b), rotational augmentation of the normal and tangential force coefficients is apparent when the solution-based stall delay model is used compared to the 2D polar data. The increase in both normal and tangential forces is qualitatively similar to rotational augmentation effects noted by Schreck et al. [56] on the Siemens 2.3-MW blade, while a quantitative comparison is not possible due to the unavailability of the exact turbine specifications, in particular airfoil data, and operating conditions.

Figure 3-17. Notional 2.3-MW normal force coefficient vs. tangential force coefficient at (a) r/R = 17% and (b) r/R = 28%.
Chapter 4

Summary and Conclusions

This work presented the development and testing of a solution-based stall delay model for horizontal-axis wind turbines. The classical Buckingham PI Theorem was used to identify dimensionless groupings relevant to the stall delay problem that depend on blade geometry, flow conditions, and also on the physical solution occurring at individual blade sections. A comprehensive review of state-of-the-art stall delay models showed that some of the identified dimensionless groupings are used already in current stall delay models. However, the fact that all current stall delay models correct 2D airfoil data exclusively prior to a BEM solver run hinders the inclusion of the solution-dependent dimensionless groupings. A solution-based stall delay model was then proposed that builds upon the well established contemporary stall delay model of Du and Selig [16] with the drag correction coefficient by Eggers et al.[38], which is currently NREL’s standard to correct 2D airfoil data used in BEM solvers. The methodology of the solution-based stall delay model is to add additional corrections to sectional airfoil lift and drag coefficients during the solution and to perform subsequent BEM solver runs until the spanwise circulation distribution is fully converged. This iterative process typically converges within 3-5 BEM solver runs. The solution-based corrections include dimensionless groupings that had been proposed by Snel et al.[13,43-45] and Lindenburg
[9] along with the sectional twist slope and solution-dependent parameters such as the blade circulation and the relative velocity acting at a local blade section. The methodology is easy to implement into existing BEM solvers.

The new solution-based stall delay model was applied to the NREL Phase VI and MEXICO rotors for quantitative data comparison. An interesting observation was that solution-dependent corrections to airfoil data are larger for the NREL Phase VI than for the MEXICO rotor. However, an improved agreement with measured data was found for both rotors when compared to a contemporary stall delay model. In particular, the new solution-based stall delay model gave consistent results between both rotors for sectional normal and tangential forces and force coefficients. As a final step, the solution-based stall delay model was applied to a notional design of a 2.3-MW wind turbine rotor with variable pitch and speed. The notional design was equipped with a UC Davis flatback airfoil up to the 30% radial station and DUT airfoils thereafter. While no quantitative comparison was possible against recent field measurements presented by Schreck et al. [56], rotational augmentation was clearly visible in predictions obtained from the solution-based stall delay model and showed a qualitative similar trend compared to field data on a utility-scale 2.3-MW wind turbine. As expected, and supported by distributions of solution-dependent parameters, rotational augmentation effects were less pronounced than for the NREL Phase VI and MEXICO rotors and, consequently, closer to predictions from a contemporary stall delay model. Results obtained from this work support that the new solution-based stall delay model is a promising general approach to predicting stall delay on wind turbine blades.
REFERENCES


[34] Breton SP, Coton F, Moe G. A study on rotational effects and different stall delay models using a prescribed wake vortex scheme and NREL phase VI experiment data. *Wind Energy* 2008.


Appendix A

XTurb-PSU Subroutine - Viterna Correction

SUBROUTINE ViternaCorrection(ASPECTRATIO, Name, ALPHAP, CLP, CDP, CDPRP, CMP, COUNTP, NAIRF, eps)

+++++ CORRECTING AIRFOIL DATA WITH VITERNA EQUATION ++++
**** Added by Joshua Dowler - 07-06-2011 ****
Based on ...
- Aerodyn User's Guide
- NREL/CP-500-36900 (Tangler & Kocurek)
- CM calculation based on wind tunnel trends from
OSU: available through NREL

Implicit INTEGER (i-n)
Implicit REAL*8 (a-h, o-z)
Include 'parmxturb.inc'

CHARACTER Name*30

INTEGER DUM
INTEGER COUNTP(NA)
INTEGER NAIRF

REAL*8 eps
REAL*8 ALPHAP(NN, NA), CLP(NN, NA), CDP(NN, NA)
REAL*8 CDPRP(NN, NA), CMP(NN, NA)
REAL*8 ALPHAP1(NN, NA), CLP1(NN, NA), CDP1(NN, NA)
REAL*8 CDPRP1(NN, NA), CMP1(NN, NA)
REAL*8 CDMAX, A2, B2, CMSLOPE, DCMSLOPE
REAL*8 pi

pi = 2.*asin(1.)
CMSLOPE = 0.
DCMSLOPE = 0.

DO i=1, NAIRF
   IF (ALPHAP(COUNTP(i), i).le.0) THEN
      Write(6,*)'** WARNING: Max Polar AOA must exceed zero degrees **'
      Write(6,*)'** For Polar',i,'the max AOA is',ALPHAP(COUNTP(i), i),
   END IF
END DO
\begin{verbatim}
& Write(6,*)'*
& END IF

CDMAX = 1.11+0.018*ASPECTRATIO
A2 = (CLP(COUNTP(i),i)-CDMAX*SIN(ALPHAP(COUNTP(i),i)*pi/180.)
& *COS(ALPHAP(COUNTP(i),i)*pi/180.))
& *(SIN(ALPHAP(COUNTP(i),i)*pi/180.)
& /(COS(ALPHAP(COUNTP(i),i)*pi/180.))**2)
B2 = (CDP(COUNTP(i),i)-CDMAX*(SIN(ALPHAP(COUNTP(i),i)*pi/180.))
& **2)/COS(ALPHAP(COUNTP(i),i)*pi/180.)

j = ((179+ALPHAP(1,i))/10)+1
m = (ALPHAP(COUNTP(i),i))/10
DUM = j+COUNTP(i)+18-m

IF ((m.lt.9).AND.(m.ge.0)) THEN
   CMSLOPE = -0.009+(0.009+(CMP(COUNTP(i),i)-CMP(COUNTP(i)-5,i))
 & /(ALPHAP(COUNTP(i),i)-ALPHAP(COUNTP(i)-5,i))/5.
   DCMSLOPE = 10./(110.-(m+1.)*10.)*(CMSLOPE*100.+CDMAX/3.)
ENDIF

DO n=1,COUNTP(i)
   ALPHAP1(j+n,i) = ALPHAP(n,i)
   CLP1(j+n,i) = CLP(n,i)
   CDP1(j+n,i) = CDP(n,i)
   CDPRP1(j+n,i) = CDPRP(n,i)
   CMP1(j+n,i) = CMP(n,i)
END DO

DO n=1,18-m
   ALPHAP1(j+COUNTP(i)+n,i) = (m*10)+10*(n)
END DO

DO n=1,j
   ALPHAP1(n,i) = -180.0+10.0*(n-1)
END DO

IF (m.lt.9.and.m.ge.0) THEN
   DO n=m,8
      CLP1(DUM+n-17,i) = CDMAX/2.*SIN(2.*ALPHAP1(DUM+n-17,i)
 & *pi/180.)+A2*COS(ALPHAP1(DUM+n-17,i)*pi/180.)
 & **2./SIN(ALPHAP1(DUM+n-17,i)*pi/180.)
      CDP1(DUM+n-17,i) = CDMAX*SIN(ALPHAP1(DUM+n-17,i)*pi/180.)***2
 & +B2*COS(ALPHAP1(DUM+n-17,i)*pi/180.)
      CDPRP1(DUM+n-17,i) = 0.99* CDP1(DUM+n-17,i)
   END DO
   DO n=m,10
      CMP1(DUM+n-17,i) =((CMSLOPE*100.)-DCMSLOPE*((ALPHAP1(DUM+n
 & -17,i)-(m+1.)*10.))**((3.4.))*SIN(0.65*
 & ALPHAP1(DUM+n-17,i)*pi/180.)+0.125
   END DO
   CLP1(DUM,i) = 0
   CDP1(DUM,i) = 0.01
   CDPRP1(DUM,i) = 0.005
   CMP1(DUM,i) = 0.0
\end{verbatim}
CMP1(DUM-1,i) = -CDMAX/3.*0.75
CMP1(DUM-2,i) = -CDMAX/3.*0.45

IF (j.eq.0) THEN
  CLP1(DUM,i) = CLP(1,i)
  CDP1(DUM,i) = CDP(1,i)
  CDPRP1(DUM,i) = CDPRP(1,i)
  CMP1(DUM,i) = CMP(1,i)
END IF

DO n=1,8-m
  CLP1(DUM-9+n,i) = -0.7*CLP1(DUM-9-n,i)
  CDP1(DUM-9+n,i) = CDP1(DUM-9-n,i)
  CDPRP1(DUM-9+n,i) = 0.99*CDP1(DUM-9-n,i)
END DO

DO n=1,4
  CMP1(DUM-7+n,i) = ((CMSLOPE*100.)-DCMSLOPE*((ALPHAP1(DUM-7,i)
&                      - (m+1.)*10.)/10.)**(3./4.))*SIN(0.95*
&                      ALPHAP1(DUM-7+n,i)*pi/180.)+0.125
END DO

ELSE IF (m.ge.9.and.m.lt.18) THEN
  CLP1(DUM,i) = 0
  CDP1(DUM,i) = 0.01
  CDPRP1(DUM,i) = 0.005
  CMP1(DUM,i) = 0.0
Write(6,*)
  Write(6,*)'WARNNING: Airfoil Polar Data Input Problem'
  Write(6,*)'Polar AOA must end at 89(deg) for Viterna Correction'
  Write(6,*)
END IF

ELSE IF (m.ge.9.and.m.lt.18) THEN
  CLP1(DUM,i) = 0
  CDP1(DUM,i) = 0.01
  CDPRP1(DUM,i) = 0.005
  CMP1(DUM,i) = 0.0
Write(6,*)
  Write(6,*)'WARNNING: Airfoil Polar Data Input Problem'
  Write(6,*)'Polar AOA must end at 89(deg) for Viterna Correction'
  Write(6,*)
END IF

IF (j.gt.9) THEN
  CLP1(1,i) = CLP1(DUM,i)
  CDP1(1,i) = CDP1(DUM,i)
  CDPRP1(1,i) = CDPRP1(DUM,i)
  CMP1(1,i) = CMP1(DUM,i)
DO n=2,10
  CLP1(n,i) = -CLP1(DUM-n+1,i)
  CDP1(n,i) = CDP1(DUM-n+1,i)
  CDPRP1(n,i) = 0.99*CDP1(n,i)
  CMP1(n,i) = -CMP1(DUM-n+1,i)-0.1
END DO
IF (j.ge.18-m) THEN
  CLP1(n,i) = CLP1(DUM-19+n,i)
  CDP1(n,i) = CDP1(DUM-19+n,i)
  CDPRP1(n,i) = 0.99*CDP1(n,i)
CMP1(n,i) = -CMP1(DUM+1-n,i) - 0.1
END DO
DO n=19-m,j
  CLP1(n,i) = (CLP1(18-m,i) + (ALPHAP1(n,i) - ALPHAP1(18-m,i)) &
                *(CLP1(j+1,i) - CLP1(18-m,i)) &
                /(ALPHAP1(j+1,i) - ALPHAP1(18-m,i))) / (19./20.)
  CDP1(n,i) = (CDP1(18-m,i) + (ALPHAP1(n,i) - ALPHAP1(18-m,i)) &
                *(CDP1(j+1,i) - CDP1(18-m,i)) &
                /(ALPHAP1(j+1,i) - ALPHAP1(18-m,i))) * (6./10.)
  CDPRP1(n,i) = (CDPRP1(18-m,i) + (ALPHAP1(n,i) - ALPHAP1(18-m,i)) &
                *(CDPRP1(j+1,i) - CDPRP1(18-m,i)) &
                /(ALPHAP1(j+1,i) - ALPHAP1(18-m,i))) * (6./10.)
  CMP1(n,i) = (CMP1(18-m,i) + (ALPHAP1(n,i) - ALPHAP1(18-m,i)) &
                *(CMP1(j+1,i) - CMP1(18-m,i)) &
                /(ALPHAP1(j+1,i) - ALPHAP1(18-m,i))) * (9./10.)
END DO
ELSE IF (j.lt.18-m) THEN
  DO n=11,j
    CLP1(n,i) = CLP1(DUM-19+n,i)
    CDP1(n,i) = CDP1(DUM-19+n,i)
    CDPRP1(n,i) = 0.99*CDP1(n,i)
    CMP1(n,i) = -CMP1(DUM+1-n,i) - 0.1
  END DO
END IF
ELSE IF(j.gt.0.and.j.le.9) THEN
  CLP1(1,i) = CLP1(DUM,i)
  CDP1(1,i) = CDP1(DUM,i)
  CDPRP1(1,i) = CLP1(DUM,i)
  CMP1(1,i) = CMP1(DUM,i)
  DO n=2,j
    CLP1(n,i) = -CLP1(DUM-n+1,i)
    CDP1(n,i) = CDP1(DUM-n+1,i)
    CDPRP1(n,i) = 0.99*CDP1(n,i)
    CMP1(n,i) = -CMP1(DUM-n+1,i) - 0.1
  END DO
END IF
COUNTP(i) = DUM
DO k=1,COUNTP(i)
  ALPHAP(k,i) = ALPHAP1(k,i)
  CLP(k,i) = CLP1(k,i)
  CDP(k,i) = CDP1(k,i)
  CDPRP(k,i) = CDPRP1(k,i)
  CMP(k,i) = CMP1(k,i)
END DO
END DO
END
Appendix B

XTurb-PSU Subroutine - Solution-Based Stall Delay Model

SUBROUTINE StDel(BN,JX,r,rh,c,t,s,d,polar       ! Blade Geom
& ,NAIRF,POCOUNT,ALPHAP,CLP,CDP,CDPRP,CMP        ! Polar Data
& ,KXTRMP,MXTRMP
& ,CASETSR,CASEPITCH,BRADIUS,VSECiter            ! Case Input
& ,eps,maxloc,localmaxloc,stdeliter
& ,gamma,gammaiter,DCL,DCD,CXTRMMIN)

*****************************************************************
Based on the NREL Excel Sheet "AirfoilPrep'
  - Du & Selig   (Lift)
  - Eggers       (Drag)
*****************************************************************

implicit INTEGER (i-n)
implicit REAL*8 (a-h,o-z)
include 'parmxturb.inc'

INTEGER INTMODE
INTEGER index
INTEGER NPOCOUNT(NA)
INTEGER NMXTRMP(NA)
INTEGER NKXTRMP(NN,NA)
INTEGER DUM3

INTEGER BN,JX
INTEGER polar(NY)
INTEGER NAIRF
INTEGER POCOUNT(NA)
INTEGER MXTRMP(NA)
INTEGER KXTRMP(NN,NA)
INTEGER maxloc,localmaxloc,stdeliter
REAL*8 pi,zero
REAL*8 NALPHAP(NN,NA),NCLP(NN,NA),NCDP(NN,NA)
REAL*8 NCDPRP(NN,NA),NCMP(NN,NA)
REAL*8 rmin,rmax
REAL*8 alphamin,alphamax,alphaend
REAL*8 asu,bsu,dsu
REAL*8 rst(NY)
REAL*8 MCASETSR
REAL*8 clmin,clmax
REAL*8 liftslope,alphazero,cdzero
REAL*8 ALPHADUM(NN),CLDUM(NN),CDDUM(NN)
REAL*8 MCLP,MEXP,MFL,MADJ
REAL*8 CXTRMMIN(NN)
REAL*8 Conv,Factor,minBeta,maxBeta
! List
REAL*8 r(NY),rh(NY),c(NY),t(NY),s(NY),d(NY)
REAL*8 ALPHAP(NN,NA),CLP(NN,NA),CDP(NN,NA),CDPRP(NN,NA),CMP(NN,NA)
REAL*8 CASETSR,CASEPITCH,BRADIUS,VSECiter(NY,NY)
REAL*8 eps
REAL*8 gamma(NY),gammaiter(NY,NY)
REAL*8 DCL(NN,NA),DCD(NN,NA),dBeta(NY)

pi = 2.*asin(1.)
zero = 0.00
minBeta = t(1)
maxBeta = t(1)

******************************************************************************
               rmin = r(1)       ! Lower Limit for Stall-Delay Model
               rmax = r(jx)      ! Upper Limit for Stall-Delay Model
           alphamin = -2.0     ! Min. AOA [deg] for Lift-Slope
           alphamax = +4.0     ! Max. AOA [deg] for Lift-Slope
           alphaend = 15.0     ! Adjustm. for alpha > alphaend [deg]
                asu = 1.0        ! ... may depend on r, see below
                bsu = 1.0
                dsu = 1.0
******************************************************************************

NAIRF = JX       ! As many Airfoils as Radial Stations

DO j=1,JX       ! 'Old' Polar Data
    index = polar(j)
polar(j) = j
    NPOCOUNT(j) = POCOUNT(index)
    NMXTRMP(j) = MXTRMP(index)
    DO k=1,POCOUNT(index)
        NALPHAP(k,j) = ALPHAP(k,index)
        NCLP(k,j) = CLP(k,index)
        NCDP(k,j) = CDP(k,index)
        NCDPRP(k,j) = CDPRP(k,index)
        NCMP(k,j) = CMP(k,index)
        NKXTRMP(k,j) = KXTRMP(k,index)
    END DO
    IF (t(j).lt.minBeta) THEN
        minBeta = t(j)
    ELSEIF (t(j).gt.maxBeta) THEN
        maxBeta = t(j)
    ENDIF
    IF ((j.ne.1).AND.(j.ne.jx)) THEN
        dBeta(j) = (t(j+1)-t(j-1))/(r(j+1)-r(j-1))
    ENDIF
    dBeta(1) = dBeta(2)+(dBeta(3)-dBeta(2))*
               (r(1)-r(2))/(r(3)-r(2))
    dBeta(jx) = dBeta(jx-1)+(dBeta(jx-2)-dBeta(jx-1))*
               (r(jx)-r(jx-1))/(r(jx-2)-r(jx-1))
END DO
DO j=1,JX                   ! 'New' Polar Data
  POCOUNT(j) = NPOCOUNT(j)
  MXTRMP(j) = NMXTRMP(j)       ! = Original (NOT correct)
  rst(j) = MAX(r(j),rmin)   ! Stall Delay for r >= rmin
  DUM3 = NPOCOUNT(j)
  DO k=1,NPOCOUNT(j)
    ALPHADUM(k) = NALPHAP(k,j)
    CLDUM(k) = NCLP(k,j)
    CDDUM(k) = NCDP(k,j)
  END DO
  INTMODE = 1
  CALL interp1(1,alphamin,DUM3,ALPHADUM,CLDUM,alphamin,eps,INTMODE)
  CALL interp1(1,alphamax,DUM3,ALPHADUM,CLDUM,alphamax,eps,INTMODE)
  liftslope = (clmax-clmin)/(alphamax-alphamin)      ! [1/deg]
  CALL interp1(1,zero,DUM3,CLDUM,ALPHADUM,alphazero,eps,INTMODE)
  CALL interp1(1,alphazero,DUM3,ALPHADUM,CDDUM,cdzero,eps,INTMODE)
  ! Modified CASETSR
  ! Selig & Du
  MCASETSR = CASETSR/(1.+(CASETSR*rst(j))**2.)**(0.5
  ! NREL - AirfoilPrep
  MCASETSR = CASETSR/(1.+(CASETSR*1.)**2.)**(0.5
  DO k=1,NPOCOUNT(j)
    ALPHAP(k,j) = NALPHAP(k,j)
  END DO
  c-jd Solution-Based Stall Delay Model
  c-jd - Apply stall delay model if r(j) <= maxloc
    !Apply Stall Delay Model
    IF (stdeliter.eq.1) THEN
      IF (j.lt.maxloc) THEN
        ! 3D Lift Correction
        ! - Selig & Du
        c-ss Note that orig. Selig & Du model uses 2.*pi for the Lift Slope
        MCLP = liftslope*(NALPHAP(k,j)-alphazero)
        MEXP = dsu/(MCASETSR*rst(j))
        MFL = ((1.6*(c(j)/rst(j)))/0.1267)
        & *(asu-(c(j)/rst(j))*MEXP)
        & /(bsu+(c(j)/rst(j))*MEXP) - 1. )
        & /(liftslope*180./pi)
        IF (NALPHAP(k,j).lt.alphaend) then
          CLP(k,j) = NCLP(k,j) + MFL*(MCLP-NCLP(k,j))
        Else ! Adjustm. due to C. Hansen (Windward Engineering)
          MADJ = ((90.-NALPHAP(k,j))/(90.-alphaend))**2.
          CLP(k,j) = NCLP(k,j) + MFL*(MCLP-NCLP(k,j))*MADJ
        Endif
        ! 3D Ext. Lift Correction
        ! - Schmitz
        IF (rmax.lt.r(jx)-eps) then
          IF (r(j).lt.rmax) then
            CLP(k,j) = CLP(k,j)+0.5*(CLP(k,j)-NCLP(k,j))*(1.-rmax)**0.5
Else
  CLP(k,j) = CLP(k,j) - 0.5*(CLP(k,j) - NCLP(k,j))*(1.-rmax)**0.5
Endif
Endif

! 3D Drag Correction
! - Eggers
MCLP = CLP(k,j) - NCLP(k,j)
MFL = MCLP
& *(SIN(NALPHAP(k,j)/180.*pi) - 0.12*COS(NALPHAP(k,j)/180.*pi))
& /(COS(NALPHAP(k,j)/180.*pi) + 0.12*SIN(NALPHAP(k,j)/180.*pi))
CDP(k,j) = NCDP(k,j) + MFL
CDP(k,j) = MAX(CDP(k,j),eps) ! Ensure Non-Negative Drag
! 3D Ext. Drag Correction
! - Schmitz
If (rmax.lt.r(jx)-eps) then
  If (r(j).lt.rmax) then
    CDP(k,j) = CDP(k,j) - 0.5*(CDP(k,j) - NCDP(k,j))*(1.-rmax)**0.25
  & Else
    CDP(k,j) = CDP(k,j) + 0.5*(CDP(k,j) - NCDP(k,j))*(1.-rmax)**0.25
  Endif
Endif
ELSE
  CLP(k,j) = NCLP(k,j)
  CDP(k,j) = NCDP(k,j)
ENDIF

ELSE
  Conv = (gamma(j) - gammaiter(stdeliter-1,j))/
  & gammaiter(1,maxloc)
  Factor = (2*VSECiter(1,j)/(CASETSR*r(j)))**2.*
  & (gammaiter(1,j)/VSECiter(1,j))**0.5*(c(j)/r(j))**2.
  CLP(k,j) = NCLP(k,j) + Conv * Factor * DCL(k,j)
  IF (localmaxloc.ne.maxloc) THEN
    Factor = 1/3.*dBeta(j)/((maxBeta-minBeta)/BRADIUS)/
    & (2*VSECiter(1,j)/(CASETSR*r(j)))*
    & r(j)/(c(j)/r(j))
  ELSE
    Factor = 0.
  ENDIF
  CDP(k,j) = NCDP(k,j) - Conv * Factor * DCD(k,j)
ENDIF

CDPRP(k,j) = NCDPRP(k,j) ! = Original (NOT correct)
CMP(k,j) = NCMP(k,j) ! = Original (NOT correct)
KXTRMP(k,j) = NKXTRMP(k,j) ! = Original (NOT correct)
END DO
END DO
END
Appendix C

XTurb-PSU Input Files
Appendix C.1

NREL Phase VI Rotor

NREL Phase VI Rotor XTurb-PSU Input File

&BLADE
  Name       = 'Phase VI',
  BN         = 2,
  ROOT       = 0.25,
  NTAPER     = 2,
  RTAPER     = 0.25,
              1.00,
  CTAPER     = 0.1465,
              0.0707,
  NTWIST     = 20,
  RTWIST     = 0.25,
              0.267,
              0.3,
              0.328,
              0.388,
              0.449,
              0.466,
              0.509,
              0.57,
              0.631,
              0.633,
              0.691,
              0.752,
              0.8,
              0.812,
              0.873,
              0.934,
              0.95,
0.994,
1,

DTWIST    = 20.04,
18.074,
14.292,
11.909,
7.979,
5.308,
4.715,
3.425,
2.083,
1.15,
1.115,
0.494,
0.015,
-0.381,
-0.475,
-0.92,
-1.352,
-1.469,
-1.775,
-1.983,

NAIRF    = 1,

RAIRF    = 0.25,

AIRFDATA = './S80905.polar',

BLENDAIRF = 0,
STALLDELAY = 2,
VITerna   = 1,

NSWEEP   = 2,

RSWEEP   = 0.25,
1.00,

LSWEEP   = 0.00,
0.00,

NDIHED   = 2,

RDIHED   = 0.25,
1.00,

LDIHED   = 0.00,
0.00,

&END
&OPERATION
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DESIGN   = 0,
NTSR     = 10,
BTSR     = 2,
ETSR     = 20,
NPITCH   = 2,
BPITCH   = 1.8,
EPITCH   = 3.0,

ANALYSIS = 0,
NANA     = 1,
TSRANA   = 2,
PITCHANA = 3.0,

PREDICTION = 1,
BRADIUS  = 5.029,
RHOAIR   = 1.225,
MUAIR    = 1.8E-05,
NPRE     = 1,
VWIND    = 5,
7,
9,
10,
11,
13,

RPMPRE   = 72.0,
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72.0,

PITCHPRE = 3.0,
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3.0,

&EEND
&SOLVER
  METHOD = 1,
  JX = 41,
  COSDISTR = 1,
  GNUPLOT = 2,
&EEND
&HVM
&EEND
&BEMT
&EEND
### NREL Phase VI Airfoil Data

**S80905.polar**

**XFOIL**  
Version 6.96

Calculated polar for: S809

1. Reynolds number fixed
2. Mach number fixed

\[ \text{xtrf} = 1.000 \text{ (top)} \quad \text{1.000 (bottom)} \]
\[ \text{Mach} = 0.000 \quad \text{Re} = 0.500 \times 10^6 \quad \text{Ncrit} = 9.000 \]

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Appendix C.2

MEXICO Rotor

MEXICO Rotor XTurb-PSU Input File

&BLADE
  Name       = 'MEXICO',
  BN         = 3,
  ROOT       = 0.093,
  NTAPER     = 21,
  RTAPER     = 0.093,
              0.102,
              0.104,
              0.133,
              0.167,
              0.200,
              0.300,
              0.400,
              0.456,
              0.500,
              0.544,
              0.600,
              0.656,
              0.700,
              0.744,
              0.800,
              0.900,
              0.962,
              0.975,
              0.988,
              1.000,
  CTAPER     = 0.08667,
              0.08667,
0.04000, 
0.04000, 
0.07333, 
0.10667, 
0.09200, 
0.07911, 
0.07378, 
0.07022, 
0.06667, 
0.06311, 
0.05956, 
0.05733, 
0.05467, 
0.05156, 
0.04533, 
0.04089, 
0.03658, 
0.02489, 
0.00489, 

NTWIST = 19, 

RTWIST = 0.093, 
0.133, 
0.167, 
0.200, 
0.300, 
0.400, 
0.456, 
0.500, 
0.544, 
0.600, 
0.656, 
0.700, 
0.744, 
0.800, 
0.900, 
0.962, 
0.975, 
0.988, 
1.000, 

DTWIST = 0.000, 
0.000, 
8.200, 
16.400, 
12.100, 
8.300,
7.100, 6.100, 5.500, 4.800, 4.000, 3.700, 3.200, 2.600, 1.500, 0.700, 0.469, 0.231, 0.000,

NAIRF = 7,
RAIRF = 0.093, 0.133, 0.200, 0.456, 0.544, 0.656, 0.744,


BLENDAIRF = 0,
STALLDELAY = 2,
VITerna = 1,

NSWEEP = 2,
RSWEEP = 0.25, 1.00,
LSWEEP = 0.00, 0.00,
NDIHED = 2,
RDIHED = 0.25, 1.00,
LDIHED   = 0.00,
0.00,

&END
&OPERATION
CHECK    = 0,
DESIGN   = 0,
NTSR     = 1,
BTSR     = 7,
ETSR     = 7,
NPITCH   = 1,
BPITCH   = 0.0,
EPITCH   = 0.0,

ANALYSIS = 0,
NANA     = 1,
TSRANA   = 7,
PITCHANA = 0.0,

PREDICTION = 1,
BRADIUS  = 2.25,
RHOAIR   = 1.225,
MU AIR   = 1.8E-05,
NP RE    = 1,
VWIND    = 10.0,
15.0,
24.0,
RPMPRE   = 424.5,
424.5,
424.5,
PITCHPRE = -2.3,
-2.3,
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\&END
\&SOLVER
  METHOD     = 1,
  JX         = 41,
  COSDISTR   = 1,
  GNUPLOT    = 2,
\&END
\&HVM
\&END
\&BEMT
\&END
**MEXICO Rotor Airfoil Data**

The airfoil data used for the MEXICO rotor were obtained by special permission from Gerard Schepers from ECN Wind Energy.
Appendix C.3

Notional 2.3-MW Rotor

*Notional 2.3-MW Rotor XTurb-PSU Input File*

```
&BLADE
  Name = Notional 2.3-MW',
  BN  = 3,
  ROOT = 0.05941,
  NTAPER = 14,
  RTAPER = 0.05941,
    0.09406,
    0.13465,
    0.16832,
    0.22772,
    0.28713,
    0.38614,
    0.52871,
    0.63366,
    0.77228,
    0.86139,
    0.94059,
    0.98020,
    1.00000,
  CTAPER = 0.04059,
    0.05049,
    0.06138,
    0.06436,
    0.06436,
    0.06436,
    0.06139,
    0.04455,
    0.03366,
```
0.02178, 0.01683, 0.01386, 0.00990, 0.00198,
NTWIST = 5,
RTWIST = 0.0594, 0.18, 0.375, 0.55, 1.0,
DTWIST = 16.00, 16.00, 6.00, 1.50, 0.00,
NAIRF = 7,
RAIRF = 0.05941, 0.15, 0.3, 0.35, 0.4, 0.55, 0.80,
AIRFDATA = './Cylinder04.polar', './FB_3500_0875.polar', './00W2350DUT.polar', './97W300DUT.polar', './91W2250DUT.polar', './93W210DUT.polar', './95W180DUT.polar',
BLENDAIRF = 1,
STALLDELAY = 2,
VITerna = 1,
NSWEEP = 2,
RSWEEP = 0.25, 1.00,
LSWEEP = 0.00,
0.00,
NDIHED = 2,
RDIHED = 0.25, 1.00,
LDIHED = 0.00, 0.00,

&END
&OPERATION
CHECK = 0,

DESIGN = 0,
NTSR = 11,
BTSR = 9.0,
ETSR = 9.5,
NPITCH = 11,
BPITCH = -1.5,
EPITCH = -0.5,

ANALYSIS = 0,
NANA = 1,
TSRANA = 2,
PITCHANA = 3.0,

PREDICTION = 1,
BRADIUS = 50.5,
RHOAIR = 1.225,
MUAIIR = 1.8E-05,
NPRE = 1,
VWIND = 3,
4,
5,
RPMPRE = 6.0,
7.2,
8.9,
10.7,
12.5,
14.3,
16.0,
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PITCHPRE = -1.0,
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7.76,
9.66,
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**Notional 2.3-MW Rotor Airfoil Data**

**Cylinder04.polar**

XFOIL         Version 6.96

Calculated polar for: Cylinder

1 1 Reynolds number fixed          Mach number fixed

xtrf =   1.000 (top)        1.000 (bottom)

Mach =   0.000     Re =     0.215 e 6     Ncrit =   9.000

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**FB_3500_0875.polar**

XFOIL         Version 6.96

Calculated polar for: FB-3500-0875

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Mach =   0.000     Re =     0.666 e 6     Ncrit =   9.000

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**00W2350DUT.polar**

**XFOIL Version 6.96**

Calculated polar for: DU 00-W2-350

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Mach = 0.000 Re = 3.000 e 6 Ncrit = 9.000

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### 97W300DUT.polar

**XFOIL Version 6.96**

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91W2250DUT.polar

XFOIL     Version 6.96

Calculated polar for: DU 91-W2-250

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Mach = 0.000 Re = 3.000 e 6 Ncrit = 9.000

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### 93W210DUT.polar

**XFOIL**  
Version 6.96

Calculated polar for: DU 93-W-210

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1.000 (bottom)

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95W180DUT.polar

XFOIL         Version 6.96

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