CHAPTER 4

Experimental Testing of Wind Turbines Using Wind Tunnels With an Emphasis on Small-Scale Wind Turbines Under Low Reynolds Numbers

Kenneth W. Van Treuren & Timothy A. Burdett
Department of Mechanical Engineering, Baylor University, Waco, TX, USA.

Abstract

The future of wind turbine design must include wind tunnel testing to produce high quality, experimental data used for design. These experimental data, both airfoil and overall system performance, can be used to validate and improve the design of the wind turbine blades and systems. Currently, little experimental testing of wind turbines is performed, with much of the aerodynamic design being accomplished using computational tools such as PROFIL and XFOIL. Computational Fluid Dynamics (CFD) prediction is improving and will be an outstanding tool for wind turbine blade design; however; these codes are not robust enough for predicting performance under low Reynolds numbers. Little experimental laboratory verification of CFD codes exists for wind turbines, especially for low-Reynolds numbers. Typically wind turbines are designed and built full scale. Because wind tunnel testing is often field tested to compare with the design predications. However, field testing can also be a very expensive process. This chapter will focus on the necessity of experimentally testing wind turbine blades to determine airfoil lift and drag data over typical Reynolds numbers of operation and also the testing of wind turbine systems (blades and generator) to determine overall wind turbine performance. This type of testing should be accomplished prior to building the full scale machine because a better design can be reached with wind tunnel testing. The Blade Element Momentum Theory (BEMT) is typically used in the design of small-scale wind turbines and this design method depends heavily on the use of accurate airfoil data. Thus, for small-scale wind turbines, quality experimental airfoil data taken at the appropriate Reynolds numbers are necessary for accurate design and prediction of power production. The data presented are for wind tunnel
testing in air; however, there is a growing interest in testing wind turbines in water tunnels, although not many facilities exist for this application. The discussions presented should be applicable to either air or water testing.

Keywords: Wind turbine experimental testing, wind turbine airfoil testing, wind tunnel, small-scale wind turbine, low Reynolds number wind tunnel testing.

1 Introduction

Having sufficient alternative energy resources is vital for the world’s future. In any portfolio of future energy resources, wind energy is a logical choice to supplement existing electrical power generation. Wind energy has the potential to provide a significant portion of electrical power, however; efficiencies can be improved with research and testing of new designs. These increases in power output for current and future wind turbines will specifically occur as the result of improvements in the blade design and construction of these machines. A small body of experimental data either gathered through wind tunnel testing of airfoils or from testing full-scale wind turbines exists. Currently, many wind turbine blades are designed using 2-D airfoil data generated from numerical programs. Often experimental testing of the complete wind turbine system, both rotor and generator, does not occur except on full scale, production turbines in the field [1]. Full wind turbine systems are not usually tested in wind tunnels due to their size and the difficulties that result from both geometric scaling and Reynolds number matching. The smaller scale wind tunnels typically available at most universities do not lend themselves to testing of wind turbines. In fact, Serpa states that wind tunnels have been of limited value in wind turbine rotor development except for gathering airfoil lift and drag data [1]. Challenges in scale make testing in a small wind tunnel difficult, however; much can still be learned from testing small-scale wind turbine systems in a wind tunnel. More wind tunnel experimental testing of wind turbine systems needs to be accomplished.

2 Challenges of experimental testing

2.1 Design using blade element and blade element momentum theory

To understand the experimental testing necessary for wind turbines, it is first desirable to understand the design processes most widely used in wind turbine design. Blade element theory (BET) or the blade element momentum theory (BEMT or BEM) are the most commonly used aerodynamic models in wind turbine rotor design. Most wind turbine textbooks, such as Wind Energy Explained by Manwell, McGowan and Rogers, outline both BET and BEMT theories and give the background necessary for the development of these systems of equations [2]. These design procedures are based on the development of rotor aerodynamic theory. BET uses airfoil forces and geometry to calculate the torque generated based on free stream and rotational flows as the basis for its development while BEMT adds an actuator disk and stream-tube momentum analysis to the BET development. A representation of the stream tube used in the BEMT is shown in Fig. 1.
Several assumptions form the basis for the actuator disc model (based on a momentum analysis with no wake rotation). The actuator disc model assumes a homogeneous, incompressible, steady state fluid flow, no frictional drag, an infinite number of blades, uniform thrust over the disc area, a non-rotating wake and the static pressure far upstream and downstream is at the ambient static pressure. Airflow in the streamtube expands as it approaches the actuator disc resulting in a decrease in velocity and increase in static pressure. As the airflow passes through the actuator disc, energy is extracted. Immediately downstream of the disc, the static pressure decreases as a result of the energy extraction. Extraction of energy causes the flow to continue to expand with a corresponding decrease in velocity. Downstream of the actuator disc the velocity continues to decrease, which results in an increase in static pressure to ambient.

Rankine [4] in 1865 and Froude [5] in 1878 were the first to develop models for the system, that involved the simple non-rotating actuator disc as it applied to the study of ship propellers. Joukowski [6], in 1918, included the effects of a rotating disc in the analysis with wake rotation. In 1926, Betz [7] proposed his simple model to determine the power from an ideal turbine which resulted in the ‘Betz Limit’ used to characterise the maximum power output from the stream tube, 16/27. These momentum analyses only account for general rotor parameters and not blade geometry. Both BET and BEMT include the forces developed by the blades as a result of flow over the blades. Froude, in 1878, and Drzewiecke [8], in 1892, began the study of this development, which was refined by Glaüert [9], in 1935. Glauert further refined this theory to determine the optimum design parameters to maximise power output, which was combined with the existing momentum theory [9].

In BET, the turbine blade is divided into segments of length, $\delta r$, and theoretical or experimental airfoil data are used to determine the torque and thrust on the blade. The combination of momentum theory and BET is often called BEMT or Strip Theory. The latter is named such because the momentum analysis is broken into strips that coincided with the blade segments [3]. Figure 2 illustrates both an element of a blade and a strip or annulus of the turbine rotor for that element.

_Wind Energy Explained_ outlines the difference between BET and BEMT as simply that ‘blade element theory refers to an analysis of forces at a section of the
blade, as a function of blade geometry’ and ‘momentum theory refers to a control volume analysis of the forces at the blade based on the conservation of linear and angular momentum’ [2]. However, in some sources, such as the Wind Energy Handbook, this distinction is not clear and BET is often used to describe what would be considered in other sources as BEMT [3]. In Wind Turbine Technology, BET is described merely as a theoretical step to reach a ‘modified blade element theory’ [1] or what has been commonly known as BEMT.

The main assumption in BEMT is that ‘individual streamtubes or strips can be analysed independently of the rest of the flow’ [1]. The validity of this analysis depends on the availability of accurate 2-D airfoil data for the airfoil being used for the local Reynolds number at that particular strip. Reynolds number for this condition would be based on the relative velocity to the airfoil, which is a function of axial velocity and rotational speed for the particular strip. Analysing the strips independently of each other is reasonable if there is no significant spanwise flow. No spanwise flow would allow the use of 2-D airfoil data for each strip. However, the flow is not truly 2-D, which has led to the development of several corrections to BEMT, most notably the tip-loss analysis developed by Glauert [9]. Some additional corrections include accounting for hub-loss, stall delay in 2-D airfoil data that result from 3-D flow effects, and a skewed-wake [10].

Optimisation of wind turbine blade design using either BET or BEMT can yield different results. Each design methodology will yield a different resultant design angle of attack, $\alpha$, which is directly attributable to the inclusion of the axial, $a$, and tangential, $a'$, momentum for BEMT influencing the magnitude of the relative velocity, $U_{rel}$. This difference can be seen in Fig. 3. Optimising the wind turbine design using BET involves selecting the angle of attack at which the lift divided by drag is maximised for the relative velocity, $U_{rel}$, magnitude and flow angle, $\varphi$ shown in Fig. 3a. Analysing Fig. 3b, the optimal angle of attack changes as the magnitude and direction of the relative velocity change because of the addition of the momentum terms. Since the momentum terms also change with angle of

Figure 2: Blade element section and annular ring used in BET and BEMT [3].
attack, the maximum power output for BEMT is not generally at the angle of attack for maximum L/D, as would be found for BET.

The two limiting factors to the accuracy of BET or BEMT are the validity of the assumptions made in the general wind turbine theory and the quality of the airfoil data used for design. BET is generally less accurate when predicting wind turbine performance. BEMT has been shown to be reasonably accurate under most conditions but depends largely on the quality airfoil data used in the design to include wind tunnel corrections applied for the data if it is experimentally obtained. Standard airfoil corrections need to be applied to airfoil wind tunnel data. However, the BEMT models do not fully capture the 3D flows that occur (particularly the complex flows that occur at low Reynolds numbers). When the flow is attached to the blade, the BEMT with the quasi-standard corrections applied can be reasonably accurate. However, there is no standard set of equations that accurately model the aerodynamics when the flow is not attached, a condition which occurs at high speeds on a stall-control turbine and on wind turbine operating at low Reynolds numbers. This is particularly true for small-scale stall-controlled wind turbines, because of the sensitivity of the data collected near the stall of the airfoil in the wind tunnel. Generally speaking, however, the lack of accuracy of the airfoil data has the potential to be the largest source of error, especially for the lower Reynolds number ranges, and can mask the inaccuracies of many of the assumptions in BET/BEMT even when the proper wind tunnel corrections for the airfoil data are applied.

For higher Reynolds numbers, usually taken to be greater than 500,000, often airfoil data are generated using computational models such as the PROFIL (Eppler Code) and XFOIL. Model validation, especially over the range of Reynolds numbers that the wind turbine blade will experience, is something that is needed in the industry. This is especially true for the lower Reynolds numbers. Often actual validation for many wind turbine designs is accomplished in the field using fully operational wind turbines after they are built and installed. While this procedure is not uncommon and much can be learned from testing the actual wind turbine, it highlights the lack of appropriate experimental data to use in design and also some of the aerodynamic issues that are evident in the modelling codes [12]. Not only is wind tunnel testing of airfoils necessary to achieve an accurate design, wind tunnel testing of complete wind turbine systems (generator and blade sets) is also needed to more accurately improve the design process.

More wind tunnel testing of wind turbine systems is desirable in theory; however, there are issues with both geometric and Reynolds number scaling that must
be considered. Predictions of performance from existing models can range from 25% to 175% of the measured values mostly due to the different approaches to using the 2-D airfoil data in the modelling codes [13]. This variation in performance is also dependent on the quality of the 2-D airfoil data available from either wind tunnel testing or computational design codes such as XFOIL.

While less common than theoretical analyses, several experimental studies of wind turbine systems have been performed. Blanch showed for a small HAWT, stall on the NACA 4415 blades can seem unpredictable and needs further study [12]. Sicot et al. examined rotation and turbulence effects on wind turbine blades emphasising stall mechanisms [14]. They showed an influence of turbulence on separation resulting in an improvement in lift and drag. More recently, Bowen et al. [15] tested five commercial horizontal axis small wind turbines to provide data for government agencies to consider when implementing tax incentives. They give comments on safety and function for each wind turbine system. They also report a number of data values for the turbines tested but give no insight into the aerodynamic design of the blades. More research is needed to characterise small-scale wind turbines with a focus on aerodynamic performance. NREL tested their 10-m wind turbine rotor in the NASA Ames wind tunnel and acquired data on 1,700 different test conditions [13]. These data were used to validate the predictive capabilities of various codes available to wind turbine blade designers. After conducting a ‘blind comparison’ of codes with their data, the conclusion was that the results were not favourable with wide variations between various code predictions. Clearly, more work is needed to adequately predict performance and further examine the aerodynamic behaviour of the blades. This implies that more experimental data are necessary to further the predictive nature of the wind turbine models and to improve the design process.

2.2 Parameters of interest in wind turbine aerodynamic testing

For either full-scale wind turbine testing or testing in a wind tunnel, there are several parameters that will be of interest to the wind turbine designer. For typical experimental procedures, a dimensional analysis would determine the dimensionless parameters of interest for the experiment. In wind turbine analysis, dimensionless parameters such as power coefficient, thrust coefficient, momentum coefficient, lift coefficient, drag coefficient and tip speed ratio would be important [2]. The power coefficient, $C_p$, is given in the following equation:

$$C_p = \frac{\text{Rotor Power}}{\text{Power in the Wind}} = \frac{P}{\frac{1}{2} \rho U^3 A}, \quad (1)$$

where $P$ is typically the power from the wind turbine rotor (a function of the angular velocity $\Omega$), $\rho$ is the air density, $U$ is the free stream velocity and $A$ is the wind turbine area swept by the rotating blades. If the power, $P$, is considered the system power (electrical power out of the rotor/generator set), then a system power
The thrust coefficient, $C_T$, is similarly defined by eqn (2):

$$C_T = \frac{\text{Thrust Force}}{\text{Dynamic Force}} = \frac{T}{\frac{1}{2} \rho U^2 A}, \quad (2)$$

where $T$ is the thrust force on the rotor. The momentum coefficient, $C_M$, is shown in eqn (3):

$$C_m = \frac{\text{Pitching Moment}}{\text{Dynamic Moment}} = \frac{M}{\frac{1}{2} \rho U^2 Ac}, \quad (3)$$

where $M$ is the pitching moment and $c$ is the chord of the blade. The 2-D lift and drag coefficient, $C_l$ and $C_d$, are as follows in eqns (4) and (5):

$$C_l = \frac{\text{Lift force}_{\text{unit length}}}{\text{Dynamic force}_{\text{unit length}}} = \frac{L/l}{\frac{1}{2} \rho U^2 c}, \quad (4)$$

$$C_d = \frac{\text{Drag force}_{\text{unit length}}}{\text{Dynamic force}_{\text{unit length}}} = \frac{D/l}{\frac{1}{2} \rho U^2 c}, \quad (5)$$

where $L$ is the lift, $D$ is the drag and $l$ is the unit length. The tip speed ratio, $\lambda$, is shown in eqn (6)

$$\lambda = \frac{\Omega R}{U}, \quad (6)$$

where $R$ is the radius of the wind turbine. Two other parameters that are sometimes included are the advance ratio, $J$, and the rotor speed power coefficient, $K_p$, given in eqns (7) and (8)

$$J = \left(\frac{U}{v}\right)_{\text{min}} = \frac{U}{R\Omega} = 1/\lambda, \quad (7)$$

$$K_p = \frac{P}{0.5 \rho R^3 \Omega^3 A} = C_p \lambda^3, \quad (8)$$

where $v$ is the translation (rotation) velocity. For aerodynamic testing, the Reynolds number is usually based on the aerodynamic chord of the wind turbine blade, as defined below in eqn (9)

$$Re = \frac{\rho Uc}{\mu}, \quad (9)$$

where $\mu$ is the absolute viscosity.
For testing of 2-D airfoils, the experimental process begins with measuring the lift and drag forces over a range of Reynolds numbers at a nominal angle of attack of $0.0^\circ$ as shown in Fig. 4. This does two things. First, it defines the region where the lift and drag coefficients are reasonably insensitive to Reynolds number for the airfoil being tested as well as at what Reynolds numbers airfoil data may be an issue due to separation.

Second, especially for the lower Reynolds numbers below this region of insensitivity, measuring these values for lift and drag will be more difficult as flow separation will occur. This is illustrated in Fig. 5, which shows the difficulty of making measurements in the low Reynolds number range especially when using different experimental techniques. At higher Reynolds numbers, the onset of transition can also influence the experimental data.
For an airfoil, the coefficient of lift and drag, local Reynolds number and angle of attack are important. These values are the required parameters to quantify performance of the airfoil and acquire accurate predictions of wind turbine performance using these 2-D data. To determine these parameters, lift force, drag force, angle of attack, dynamic pressure, atmospheric temperature and atmospheric pressure must all be measured. Additionally, as part of the data analysis, appropriate wind tunnel corrections must be applied.

For particular Reynolds numbers that will be experienced over the span of the wind turbine blade, it is necessary to have the correct lift and drag coefficient data for the blade elements in the design process. Typical data for such measurements are shown in Fig. 6 for a NACA 4415 2-D airfoil.

By plotting $C_L$ against $C_D$, one can determine the point of maximum $L/D$ or $(C_L/C_D)$ by drawing the tangent line from the origin as can be seen in Fig. 7.

Using the lift and drag data, one can also determine the point of maximum $L/D$ by plotting $L/D$ against angle of attack as can be seen in Fig. 8.

Thus, it is necessary to conduct an experimental airfoil test at the required Reynolds numbers over the range of angles of attack that will be experienced, including stall and beyond for stall controlled wind turbines. Local Reynolds numbers will naturally increase as the blade elements are considered from the hub to the tip of the blade for a constant chord, which might be present on small-scale wind turbines. On many larger-scale wind turbines, the chord of the blade decreases as one moves...
from the hub to the tip holding the Reynolds number constant or having it only increase slightly. The quality of the wind turbine blade design and the prediction of performance are only as good as the lift and drag data used in the design process.

3 Scaling wind turbines for wind tunnel testing

In the design of wind turbines, scaling often is a topic for discussion. Currently, wind turbine blades are designed theoretically and usually tested in full-scale turbines [15,20]. Airfoil sections are tested in the wind tunnel; however, full systems
are not usually tested in wind tunnels, especially the small-scale wind tunnels found in most universities. Challenges in scale make testing in a small wind tunnel difficult, however, much can still be learned from testing full systems in a small-scale wind tunnel. Typically, a proven design of a particular size is considered the basis for scaling to a larger size for the harvesting of more power from the wind. This would be geometric scaling. Geometric scaling has been a topic largely ignored resulting in a relatively little literature on the subject, especially as it applies to wind turbines [21]. The topic of scaling is briefly covered in most wind energy textbooks; however, scaling relationships are considered mainly for changing the geometric size of the turbine. Reynolds number matching is ignored [22]. For scaling relations, Manwell et al. [2] use the following assumptions:

1. Keep the tip speed ratio constant.
2. Keep the blade profile, the number of blades and the blade material constant.
3. Geometric similarity is maintained to the extent possible.

Table 1 summarises the relationships between quantities of interest for wind turbines based on rotor diameter as the scaling factor. Using these guidelines, if the rotational speed is halved then the radius is doubled, keeping the tip speed ratio constant. If the tip speed ratio is constant, then the power, thrust and momentum coefficient will remain constant as these are the functions of tip speed. A constant tip speed also preserves the angle of attack for the wind turbine blade resulting in the same lift and drag coefficient. The danger in using the concept of geometric

<table>
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<th>Quantity</th>
<th>Symbol</th>
<th>Relation</th>
<th>Scale dependence</th>
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<td>Power</td>
<td>( P )</td>
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<tr>
<td>Torque</td>
<td>( Q )</td>
<td>( Q_1/Q_2 = (R_1/R_2)^3 )</td>
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<tr>
<td>Thrust</td>
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<td>Rotational speed</td>
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<td>Weight</td>
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</tr>
<tr>
<td>Aerodynamic moments</td>
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<td>( M_{A1}/M_{A2} = (R_1/R_2)^3 )</td>
<td>( \sim R^3 )</td>
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<td>Centrifugal forces</td>
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<td>( F_{c1}/F_{c2} = (R_1/R_2)^2 )</td>
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<td><strong>Stresses</strong></td>
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<td>Gravitational</td>
<td>( \sigma_g )</td>
<td>( \sigma_{g1}/\sigma_{g2} = (R_1/R_2)^1 )</td>
<td>( \sim R^1 )</td>
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<tr>
<td>Aerodynamic</td>
<td>( \sigma_A )</td>
<td>( \sigma_{A1}/\sigma_{A2} = (R_1/R_2)^0 )</td>
<td>( = 1 ) ( \sim R^0 )</td>
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<tr>
<td>Centrifugal</td>
<td>( \sigma_c )</td>
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<td>( = 1 ) ( \sim R^0 )</td>
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<td>Natural frequency</td>
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<td>( (\Omega_1/\omega_{n1})/(\Omega_2/\omega_{n2}) = (R_1/R_2)^0 )</td>
<td>( = 1 ) ( \sim R^0 )</td>
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similarity is that while the volume of a wind turbine will increase as the scaling factor cubed, the power production is a factor of the scaling factor squared.

These relationships are valid providing the weight does not become the prime concern for wind turbine blades (as might be true for large turbines) and that the values of design parameters should not result in a critical Reynolds number of less than 200,000 [18]. Reynolds numbers less than 200,000 result in testing problems for small wind turbines (D < 5 m) or wind tunnel testing [23]. These relationships are also only true if the blades are scaled up or down exactly in proportion to the change in radius. Peterson states that small variations in the radius, such as extensions of the blade supports at the hub, cause the scaling projections to be inaccurate [24]. Items associated with wind turbines that are considered in scaling are the rotor, weights in the nacelle and the tower. Jackson has shown that fatigue loads are able to scale but only if dynamic and aerodynamic similarity is maintained [20]. Shrestha et al. have even determined scaling laws to estimate weights for direct drive generators of different power levels used with wind turbines [25]. Associated with the scaling would be the increased weight of the blades and the loads generated on the blades themselves. This does not always follow the same geometric similarity. Moe states, for example, that increasing the size by 20% would increase the power production by 50% while increasing the weight by 73% [22]. Increasing the size of the wind turbine using geometric scaling will usually find that cost do not scale in the same proportion and will become an increasingly larger part of the overall energy production costs [22]. It is suggested that scaling a wind turbine up past a certain size will eventually not be possible and will result in a constant response level, regardless of size [22]. Manwell et al. [2] state in their third scaling relation that ‘geometric similarity is maintained to the extent possible’ recognising there may be cases where geometric similarity is not possible.

Van Treuren and Gregg discuss the importance of dimensionless coefficients for scaling, such as power coefficient, thrust coefficient, momentum coefficient, lift coefficient, drag coefficient and tip speed ratio [26]. The other non-dimensional number of importance when considering similarity or scaling is the Reynolds number based on the chord for the wind turbine blade. Burdett and Van Treuren have looked at the case for scaling when Reynolds number matching is included [27]. Table 2 shows that values, such as the rotational velocity, scale as one over

<table>
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<td>Chord</td>
<td>C</td>
<td>( C_1/C_2 = sc )</td>
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<tr>
<td>Kinematic viscosity</td>
<td>( \nu )</td>
<td>( \nu_1/\nu_2 = sc )</td>
</tr>
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<td>Freestream velocity</td>
<td>( U )</td>
<td>( U_1/U_2 = 1/sc )</td>
</tr>
<tr>
<td>Rotational velocity</td>
<td>( \Omega )</td>
<td>( \Omega_1/\Omega_2 = 1/sc^2 )</td>
</tr>
<tr>
<td>Rotor power</td>
<td>( P_{\text{Rotor}} )</td>
<td>( P_{\text{Rotor1}}/P_{\text{Rotor2}} = 1/sc )</td>
</tr>
<tr>
<td>Torque</td>
<td>( \tau )</td>
<td>( \tau_1/\tau_2 = sc )</td>
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the radius squared instead of one over the radius as seen in Table 1 for rotational speed. As can be seen in Table 2, the freestream velocity must increase by the geometric scaling factor, and the rotational velocity must increase by the square of the scaling factor. Because of the relationships derived from Reynolds number matching, scaling in this manner can lead to impractical freestream and rotational velocities.

While matching Reynolds numbers should also occur in experimental testing, matching Reynolds numbers leads to unrealistically high free stream and tip velocities when scaling from a large turbine blade to a smaller scaled wind turbine blade for testing in a wind tunnel. The Reynolds number is also not a feasible way to scale wind tunnel turbines to a large scale. At higher Reynolds numbers, above 500,000, the aerodynamics do not change significantly with Reynolds numbers. To scale down with the Reynolds number, the wind tunnel speed would have to be unrealistically high, making testing nearly impossible. To scale up would result in low speeds and low Reynolds numbers. For example, the wind tunnel testing of a 0.508 m (20 in.) diameter fan at 64.37 kph (40 mph) when scaled to a 2.4383 m (8 foot) diameter turbine blade would have a scaled wind speed of 13.41 kph (8.33 mph). Since many of these smaller wind turbines with 2.4383 m (8 foot) diameter turbine blades are optimised for approximately 40.23 kph (25 mph), the corresponding speed for reducing the size of the turbine to that needed for testing in the wind tunnel would be unrealistically high.

The result is that to test the full-scale Reynolds number on a smaller scale, it is necessary to test the at tip speeds above the full-scale value. Wainauski tested a 1/30 scale model of a 3.5-MW wind turbine and did match Reynolds numbers. The result was test tip speeds of 183 m/s (600 ft/s). This led to the recommendation by Wainauski to keep the tip speeds below the drag rise Mach numbers of the blade airfoils to avoid compressibility losses [28]. Gao and Hu recognise that dynamic similarity (Reynolds number matching) is hard to achieve with wind turbines of different sizes. They show that the power coefficient of two wind turbines is different if the Reynolds numbers are different. If the sizes of wind turbines are different, such as is usually the case with testing a scaled wind turbine in a wind tunnel as opposed to an actual wind turbine, the Reynolds number cannot be satisfied and wind tunnel data cannot be directly applied to the original wind turbine [29]. An important impact of Reynolds number is its effect on the 2-D airfoil data used in the design of the wind turbine. Gao and Hu have found that the lift and drag coefficients changed little in the Reynolds number range of 200,000–2,500,000 [29]. Burdett et al. have also shown an effect of Reynolds number on design considerations for wind turbines using 2-D airfoil data for lower Reynolds numbers [30]. They show that at lower Reynolds numbers, the angle of attack for maximum C_l/C_D changes and this must be considered when using BET. Even more important is the onset of flow separation at Reynolds numbers below 100,000, which must be addressed by active or passive flow control devices if a reasonable power production with the wind turbine is desired.

Design procedures are important for the design of any wind turbine. Wind turbines of all sizes are available for consumer use. Large turbines with megawatt
capacity can have wind turbine blade diameters from 80 to 100 m while small, residential wind turbines of 1–2 kW can have blades with rotor diameters of 3 m or less [23]. However, it is extremely important to have design procedures to take advantage of information gathered for smaller wind turbines under laboratory conditions that can be scaled to the larger diameters. It is also advantageous to test scaled-down models in a wind tunnel to examine parameters such as torque, power and thrust characteristics planned for larger systems. This experimental testing would help to minimise risks to the maximum extent possible.

While often difficult, wind tunnel testing of wind turbines can provide very meaningful results and minimise research and development risks. For example, wind tunnel data can be used for optimising designs. Gregg et al. studied the effect of tip speed ratio ($\Omega$, defined in Eq. (8)), number of blades and surface roughness on wind turbine performance in a wind tunnel with a 2 ft (0.61 m) square cross-section [16]. Additionally, wind tunnel tests can be used to develop and validate aerodynamic modelling tools such as the Unsteady Aerodynamics Experiment (UAE) and the Model Experiments in Controlled Conditions (MEXICO) wind tunnel tests [31]. Other tests of this nature have been performed by Ronsten [32] and Snel et al. [33] to better model the stall delay phenomena.

In wind tunnel testing of wind turbines, scaling is an integral topic for discussion. For wind tunnel testing, a turbine design is typically scaled down to allow testing in a wind tunnel without significant blockage. As noted by Simms et al. in the case of the UAE and also the MEXICO tests, however, it is sometimes necessary to test a full-scale turbine because scaling is impractical [13]. Nonetheless, full-scale testing in a wind tunnel is difficult because typical wind tunnel sizes are too small to fit most wind turbines while keeping blockage minimal.

When scaling a turbine for design or wind tunnel testing, typically only geometric scaling and TSR matching are applied. Multiple studies have been performed using only these scaling criteria. For example, Cho et al. tested a 12% scaled model of the NREL Phase VI turbine and analysed the acoustic noise produced [34]. Chen and Liou tested multiple scaled models and analysed the significance of the blockage correction with wind tunnel blockage between 10.2% and 28.3% [35]. Jackson studied the scaling of fatigue loads with good correlation for most of the conditions studied [20]. Hulskamp et al. scaled trailing edge flaps to determine their ability to reduce dynamic loading [36].

Another important aspect of scaling is Reynolds number matching (Reynolds number based on chord as defined in eqn (9)). Without Reynolds number matching, the aerodynamic conditions between the two turbines are not truly identical. However, for Reynolds numbers greater than 500,000, airfoil performance does not change significantly with Reynolds number so Reynolds number matching is less significant. Most medium to large turbines (>20 m) operate at higher Reynolds numbers, and this effect does not need to be taken into account [1]. This is demonstrated by Jackson, who documents that fatigue loads for larger turbines can be scaled without Reynolds number matching and reasonable agreement can be achieved [34]. At Reynolds numbers below 500,000, however, ‘extreme and unusual behavior is caused by anomalous transition, separation and bubble formation phenomena’ [1].
Because of the unique aerodynamics at Reynolds numbers below 500,000, the flow can vary significantly with both flow and geometric parameters [17]. Burdett et al. discuss how airfoil lift and drag coefficients change with Reynolds number and the effect that change has on wind turbine design. In particular, they show that the optimal angle of attack for wind turbine design varies with Reynolds number [30]. Additionally, the flow is very sensitive to changes in freestream turbulence, surface finish and airfoil shape [37]. This sensitivity to roughness can be exploited for low Reynolds numbers to reduce the separation that occurs and likewise increases both airfoil and wind turbine performance [17,38]. Figure 9 shows how drag coefficient for the S823 airfoil can be significantly affected by roughness. In this test performed at approximately zero angle of attack ($\alpha$), the roughness applied is a trip strip with a height of 0.35% of the chord length and a width of 2.0% of chord length, located at 25% of the chord on the upper airfoil surface only. At Reynolds numbers below 125,000, the addition of the trip strip significantly reduces drag up to 36.9%. However, above this Reynolds number the drag is actually increased by up to 13% (at 275,000).

Due to the sensitivity of this flow regime, the specific Reynolds number below which the unusual flow phenomena occurs is also sensitive to similar changes. While this Reynolds number is generally taken to be 500,000 [1], Gao and Hu predicted in a computational fluid dynamics (CFD) model that this number should be closer to 200,000 and that the airfoil lift and drag coefficient changed little in the Reynolds number range of 200,000 to 2,500,000 (specific to the rotor and conditions modelled) [29].

Two situations exists when scaling down a turbine for wind tunnel testing that will result in Reynolds numbers below 500,000 and necessitate Reynolds number matching. First, if the original full-scale turbine already experiences low Reynolds numbers, Reynolds matching must be used in scaling, as in the testing of a scaled 1.15-m turbine optimised for 5 m/s by Burdett et al. [39]. Second, if scaling down the turbine results in Reynolds numbers below 500,000, either Reynolds number matching must be used or some other test parameters must be controlled such that
the Reynolds numbers on the blade remain above 500,000. One such test was performed by Wainauski in scaling a 3.5 MW turbine by a 1:30 ratio to an 8 ft (2.44 m) diameter for testing in 18 ft (5.49 m) octagonal test section. In this test, a TSR was chosen to keep the Reynolds numbers over the blade near 600,000 and the tip speed below 183 m/s. Because of this, Reynolds number matching was not necessary and compressibility effects did not need to be taken into account. However, this study was very beneficial in demonstrating the effects of scaling, surface roughness and blade shape modifications on the full-scale 3.5 MW turbine. After correcting the data for scaling effects, the wind tunnel data agreed with the full-scale annual energy production (AEP) within 1\% [28].

A few other considerations exist for scaling. An obvious parameter for scaling is mass, which is difficult to scale. For steady-state wind turbine tests in smaller wind tunnels, however, not scaling the mass will not change the measured values. Additionally, the starting torque for the full-scale generator is very difficult to scale. For this reason, cut-in speed (the wind speed at which the turbine begins to spin) cannot be scaled very easily. Peterson notes that small variations in turbine geometry, such as radius, may cause the scaling projections to be inaccurate [24].

4 Wind tunnel testing

The use of wind tunnels in testing wind turbines is highly advantageous. However, wind turbine testing of actual wind turbine systems with large blades in a wind tunnel is not usually possible because the large diameters do not physically fit into a wind tunnel or the rotor disc will cause an unacceptably large blockage in the tunnel. It is possible to test fixed, full-scale wind turbine systems at locations such as the National Renewable Energy Laboratory’s National Wind Technology Center in Colorado but this is costly [40]. Other testing of wind turbine systems outside a wind tunnel has been reported by Blanch [12]. Blanch tested a 3-m diameter wind turbine mounted to a trailer by towing the trailer along an airport runway. He noticed some variability in the data due to differing environmental conditions. In a wind tunnel, both the wind speed and quality are carefully controlled, eliminating the variability. The largest tunnel in the literature used in wind turbine testing is the NASA Ames Research Center’s 34.4 m (80 ft) × 36.6 m (120 ft) wind tunnel [13]. This tunnel enabled testing of a 10-m diameter wind turbine system (NREL Phase VI test) keeping the blockage to an acceptable level so that corrections were not necessary (<10\%). Some large wind tunnels do have dimensions of 10 m × 10 m but even this is not large enough for most of the wind turbine systems [18]. The majority of wind tunnels are much smaller and, if the blockage constraint is satisfied, this limits the size of the turbine that can be tested. The MEXICO tests were done in the DNW facility with a 4.5-m diameter turbine in a 9.5 m × 9.5 m wind tunnel test section with no corrections [31]. Sicot et al. tested a 1.3-m diameter turbine in a 4 m × 4 m test section with no blockage corrections [14]. Small wind tunnels of the sort found in research universities are also useful for wind
turbine system testing. Burdett et al. have successfully tested systems in a tunnel with dimensions of 0.6 m × 0.6 m with wind turbine diameters of 0.5 m [27,39]. For these tests, blockages corrections must be applied. Wind tunnels can still be of use to examine specific aspects of wind turbine blades to develop technologies to apply to the wind turbine blades. Usually, wind turbine tests involve the measurement of power out and how a rotor would respond to unsteady conditions [18]. For power measurements under steady conditions, the model does not have to match the material properties of the actual turbine blade; however, to match dynamic response to unsteady conditions, the material elastic properties should be similar.

Wind turbine testing involves wind tunnels in a variety of ways. Maeda et al. [41] used an actual full-sized wind turbine blade to measure the static pressure distribution over the blade. This was a 10-m diameter wind turbine rotor with the hub located 13 m above the ground. This pressure distribution was then compared with a similar airfoil in a Gottingen-type wind tunnel. Comparisons were made with good results. Butterfield et al. [42] made a similar test of airfoil cross-sections in a large-scale wind tunnel, comparing with other wind tunnel and actual $C_L$ data with good results. Currently, new wind turbine designs are typically tested in the field by measuring the power output and developing a power curve for the actual turbine. It is difficult to measure the aerodynamic characteristics of the wind turbine in the field.

Some testing has been done on small-scale models in wind tunnels of 1 m × 1 m or less [27, 35, 43–48]. Blockage is always a consideration under these small-scale conditions. Traditionally, tunnel blockages of 6%–7.5% or even up to 10% have been acceptable according to Howell et al. [46]. Scaling down rotating machinery typically leads to losses in efficiency which must be understood [49].

Evident from the small-scale models in wind tunnel experiments is the low-power output from the wind turbine systems. This is due to a number of factors. First, tip speed ratios are lower due to the smaller diameters. Attempting to match Reynolds numbers is not possible and would lead to unrealistic rotational speeds. Reynolds numbers based on the chord leads to numbers that are well below the 200,000 previously mentioned as a lower limit. Second, the smaller size of the turbine means there is less power output or power available. The result is low $C_P$ values for the tip speed ratios tested. Third, the smaller chord for the wind turbine blade also reduces the amount of lift generated by the blade and thus, the power output is lower. Fourth, finding an appropriate small-scale generator for use in the wind tunnel is difficult. Several wind tunnel experiments were presented in the literature without significant discussion of the choice of generator. Fifth, designing a fixed pitch turbine blade leads to an optimised performance for only one speed. Operating above or below this speed leads to lower power generation. Lastly, there are mechanical losses of the system, which are hard to evaluate. Often this is evident in the wind tunnel by the higher free stream airspeed necessary to start the wind turbine rotating for testing. In spite of these challenges, there is a place for measurement of full wind turbine systems in a small-scale wind tunnel.
4.1 Testing airfoils

4.1.1 Airfoil characteristics
Much of the literature concerning wind tunnel testing of wind turbine systems involves experiments to determine the lift and drag coefficients of airfoil cross-sections that will be used in wind turbine blade design. Lift and drag data for airfoils are generally found either by experimental testing or by numerical simulation. Experimental testing by NACA in the 1930s resulted in the family of airfoils used in the design of aircraft [50,51]. Early wind turbines began using these NACA airfoil sections such as the NACA 4415 airfoil [18]. These airfoils were considered appropriate because of their laminar flow characteristics. For wind turbines, it is desired to maximise lift and minimise drag for the chosen design speed. Thus, for BET design, operating at maximum L/D ratio (minimum drag for the lift produced) is desirable. Figure 6 shows data for the NACA 4415 airfoil, typical for these types of tests. These airfoils are appropriate for most conditions because of their low L/D. However, the surface finish of the blades must be very smooth as roughness can cause a laminar boundary layer to prematurely transition to turbulent flow. Additionally, an increase in the freestream turbulence can have a similar effect. For this reason, differences in surface roughness of airfoil models and wind tunnel freestream turbulence between experiments can cause differences in data. Additionally, the blockage corrections applied and the measurement methods can affect the data [12]. From the plot of $C_L$ vs. $C_D$ shown in Figs. 7 and 8, it is possible to determine the maximum L/D ratio directly.

Wind tunnel testing of airfoil cross-sections will always have its place in the design of wind turbines. While NACA airfoil cross-sections are still being used on wind turbines, attention is being given to designing airfoils that are more appropriate for wind turbines. Figure 10 shows data for a laminar flow LS-1 airfoil. Because the local Reynolds number over the blade is a function of the blade radius, data must be collected for the entire range of Reynolds numbers expected. The National Renewable Energy Laboratory (NREL) has supported research in this area of airfoil design [52]. Other researchers are developing alternative families of airfoils and testing NREL airfoils for wind turbine use [19,53–56]. Future work with wind turbine airfoils will include modification of existing airfoils for better performance [57,58].

Another area which is critical for wind turbine performance is flow separation, which can occur under low Reynolds number conditions below 500,000. Modern computational design programs are questionable in the low Reynolds number regime and tend to under predict the laminar separation bubble drag [53]. Also, the effects of leading edge roughness is another area that needs further research.

Several studies have been accomplished to gather experimental data for low Reynolds numbers and compare to simulations. McGhee et al. studied the Eppler 387 airfoil at Reynolds numbers from 60,000 to 460,000 and presented both experimental and simulation data using PROFIL, a numerical design code developed by Prof. Richard Eppler [60]. A good comparison was found between the theoretical and experimental results for the higher Reynolds numbers, but at lower Reynolds numbers PROFIL was unable to accurately predict the increase
in drag resulting from a laminar separation bubble. They concluded that the simulation would predict a lower drag coefficient than was measured experimentally. The laminar separation on low Reynolds numbers airfoils was studied by Hu and Yang [61]. They document the behaviour of this separation bubble for a Reynolds number of 70,000 using both PIV and surface pressure measurements. It was found that the separation bubble moves forward as the angle of attack is increased. Being able to predict this separation bubble and its movement is critical to predicting airfoil performance for small-scale wind turbines. A number of studies have been done on airfoils for small-scale wind application [62–69]. A wide range of Reynolds numbers have been tested and/or simulated with these studies. For Reynolds numbers above 200,000, agreement between experimental data and PROFIL was generally good. However, at 100,000 and below, where laminar separation can dominate the flow over an airfoil, the agreement was considered fair [64]. Selig and McGranahan [69] have shown that drag behaviour is highly dependent on Reynolds number, especially in low Reynolds number regimes. Thus, there is a need for additional airfoil analysis, both experimental and simulated, at the low operational Reynolds numbers for small-scale wind turbines to further refine blade design.

Two notable sources of airfoils designed for the low Reynolds numbers that small-scale wind turbines experience are the National Renewable Energy Laboratory (NREL) as well as Dr. Michael Selig at the University of Illinois, Urbana-Champaign.
NREL has developed two families of airfoils (each family is for a specific diameter turbine) for turbines under 10 m in diameter. The S833–S835 have been designed for 1–3 m turbines [70], and the S822–S823 have been designed for 3–10 m turbines [71]. These airfoils have been designed theoretically using PROFIL in the Reynolds numbers range of 150,000–800,000. Among other design criteria, these airfoils were designed to minimise the effect of leading-edge roughness by simulating the flow transition that occurs under those conditions.

Additional airfoils have been designed by Selig et al., and these as well as other low Reynolds numbers airfoils have been tested experimentally over Reynolds numbers range of 30,000–500,00 [72–75]. In sources [73] and [74], the same airfoils were tested with both normal flow conditions and tripped flow conditions (to simulate leading edge roughness). For most airfoils testing, however, data were only taken at a Reynolds number as low as 100,000. Additionally, the data were never taken post stall, which is necessary for the design of stall-regulated wind turbines.

If lower Reynolds number or post-stall data were desired, experimental testing must be performed because neither PROFIL nor XFOIL can accurately predict these conditions on all airfoils due to flow separation over most of the airfoil surface under these conditions. While CFD could be an alternative to experimental testing, it must first be validated experimentally before the theoretical model can be extrapolated to conditions it cannot currently predict. CFD does not presently model transition from laminar to turbulent well, which is a critical phenomenon in a separation bubble as it causes reattachment. Since an accurate model does not exist, experimental testing is still required.

4.1.2 Airfoil testing in a wind tunnel

While airfoil testing has become relatively standardised, many differences exist in wind tunnel sizes that have been used to measure airfoil data. Large tunnels, such as the tunnel at the University of Illinois Urbana Champlain (UIUC), have test sections as large as 3 ft × 4 ft. These types of tunnels employ several different techniques to measure the forces on the airfoil. A common method used is the force balance, or sting, mount which measures lift and drag directly. Some tunnels do measure lift and drag using a side mount system which essentially isolates the flow to be 2-D. Other experiments measure drag downstream of the model by characterising the wake and using the momentum deficit approach. What is evident is that experimental data gathered are highly dependent on the wind tunnel and the method used to measure the forces. Experiments do not usually report the turbulence intensity of the flow, which also can have a large impact on the separation characteristics of the airfoil.

A typical small laboratory wind tunnel, such as seen in Fig. 11, is an open-circuit tunnel with a 24-in × 24-in × 48-in test section. The Model 406 Wind Tunnel was manufactured by Engineering Laboratory Design, Inc. and has a 40-hp electric motor that drives a constant pitch fan. Varying the RPM gives a low velocity of 0.1 m/s to an upper tunnel velocity greater than 50 m/s. Tunnel velocity variation over the test section is less than ±1%. The contraction ratio from the inlet
Figure 11: Typical small-scale wind tunnel [27].

Figure 12: ACP Spyderfoam Airfoil [76].

is 6.25:1. The contraction ratio, a precision honeycomb inlet and three graduated, high-porosity screens provide a clean inlet turbulence intensity of approximately 0.2%. Access to the tunnel test section is provided through a removable floor and ceiling.

An S823 airfoil is shown in Fig. 12 and is typical of airfoils tested in this wind tunnel. The test article is 24 in. in span with a 6-in chord, and is manufactured out of ACP SpyderFoam. A hole was cut through the centre of the airfoil, which spans the airfoil at the quarter-chord for a 3/16-in. stiffener rod. The groove on the top of the airfoil is remaining from cutting the hole for the stiffener rod and was shown not to affect the airfoil performance. The airfoil also has a groove cut chordwise on the airfoil centreline on both the top and bottom surfaces to allow the mounting system to be flush with the airfoil (Fig. 13) and mounts directly onto the Engineering Laboratory Design (ELD) force balance (Fig. 14). The airfoil spans the test section to minimise and 3-D flow effects.
The lift and drag were measured using the ELD dynamometer. Its DC voltage output was connected to a National Instruments BNC 2110 and recorded using Labview. The dynamic pressure was measured using a pitot-static tube upstream of the airfoil. The angle of attack was measured using a digital torpedo level, accurate to 0.1°.

4.2 Testing wind turbine systems

Full wind turbine systems tests can occur in many size wind tunnels. The purpose of each of these tests is to gather experimental data under reasonable conditions that can be compared with CFD or be scaled up (or down) to appropriate size turbines. Small-scale wind turbine tests using wind tunnels of the size depicted in Figs. 11 and 14 are of value in determining wind turbine system characteristics. Burdett et al. have successfully used this is size wind tunnel to compare BET and BEMT design methods as well as geometric and Reynolds number scaling [39,27]. Other tests can be conducted on tip speed ratios, number of blades, optimisation of twist, effects of roughness, etc. such as was studied by Gregg and Van Treuren and Gregg et al. [77,16]. Typical setup for the wind turbine system in the wind tunnel is shown in Fig. 15. Blades for these tests were designed in Solidworks and printed on an OBJET 30 3-D printer and can be seen in Fig. 16.

The results of these experimental tests provide valuable information. Figs. 17–19 show the results of some these testing. The graphs illustrate a comparison between tested parameters, where $C_{P_S}$ is the wind turbine system electrical power output normalised by the power available in the air. From these tests, it is easy to compare the effects of tip speed ratio, number of blades and the addition of roughness to the blades on power output for a wind turbine rotor/generator combination.

Figure 20 shows the comparison of the rotor power coefficient of two-rotor blade sets designed using either BET or BEMT (seen in Fig. 16b). Figure 21 shows the rotor power coefficient when applying geometric and Reynolds number scaling of the wind turbine blades shown in Fig. 16a. Obviously, wind tunnel testing of full-wind turbine systems provide insight into the design of wind turbine rotors.
Figure 14: Airfoil and test section with force balance [76].

Figure 15: Dimensions of turbine and wind tunnel test section [39]

Figure 16: Printed wind turbine blades used in scaling tests with dovetail hub and showing different angles of twist for BET and BEMT blades [27,39].
Figure 17: S818 two blade tests [16].

Figure 18: S818 performance for different blade sets at a constant TSR of 3 [16].

Figure 19: S818 data comparing a 4-blade smooth configuration with a 3-blade ridged configuration at a TSR of 3 [16].
Corrections for wind tunnel testing

With experimental testing of airfoils, the flow conditions in the wind tunnel are not identical to the ideal 2-D flow that an airfoil shape would experience in an open system. Because of this, multiple wind tunnel corrections have been published to account for the confinement of the wind tunnel. Barlow et al. provide a fairly thorough summary of wind tunnel corrections for 2-D models [78]. In the testing
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of six low Reynolds number airfoils, Selig \textit{et al.} use similar correction factors but apply them in the final correction differently than Barlow \textit{et al.} [69]. In the analysis presented in this paper, both corrections are used and compared to determine the effect of the different methods.

5.1 2-D Airfoil flow considerations

In all branches of aerodynamics that use airfoils, accurate performance lift, drag and moment coefficient data are absolutely necessary for quality design and correct analysis for predictive models. For higher velocities and subsequently higher chord-based Reynolds numbers (but not supersonic), numerical simulations such as XFOIL and PROFIL are probably adequate for use in design methods. However, for low Reynolds numbers (roughly 200,000 and below – the limit is different for every airfoil) and post-stall conditions, better predictive tools are necessary. Figure 22 shows how airfoil performance can drastically change with decreasing

Figure 22: Lift and drag curves over a range of Reynolds numbers [78].
Reynolds numbers. In this figure, both the lift and drag coefficients (defined in eqns (6) and (7)) are plotted versus angle of attack. While numerous low-Reynolds numbers airfoils have been designed, only a fraction of those have been developed for small-scale wind turbines. Airfoils for small-scale wind turbines have unique design requirements. Some of the notable ones are structural requirements for airfoil shapes to be used at the root of the blade and the leading-edge roughness insensitivity necessary to minimise the loss of performance with deposition from bugs and other particulates [70].

All airfoils will experience low Reynolds numbers in low wind conditions or under design wind conditions near the hub where the rotational velocity is the lowest. Because of the rotational effects, the root is where the lowest relative velocities occur on the blade, thus the lowest Reynolds numbers. However, airfoil performance must be known at the specific Reynolds numbers under which the turbine blades will operate to design these blades properly. In addition, post stall data are necessary in the design of stall-regulated wind turbines, which neither PROFIL nor XFOIL can accurately predict due to flow separation and turbulent flow over most of the airfoil under these conditions.

For higher chord-based Reynolds numbers (greater than approximately 200,000) where separation bubbles are minimal and full separation only occurs at high angles of attack (greater than approximately 12°), accurate data can generally be simulated using current software like PROFIL and XFOIL. However, PROFIL does not accurately predict the drag curve for low Reynolds numbers because it does not account for the added drag due to the separation bubble as shown by McGhee et al. [60]. As a result, the simulated drag curve can be lower than what is experimentally measured. Additionally, the inaccurate drag curve can lead to an inaccurate prediction of the angle at which the maximum L/D occurs. XFOIL, on the other hand, was developed to account for separation bubble losses and simulates low Reynolds number flow, as shown by Drela, for the E387 airfoil [79]. Nonetheless, this code is not always accurate for other airfoils analysed at low Reynolds numbers. In particular, it does not always predict the location of turbulent separation accurately as shown by McGranahan and Selig, which can result in significant discrepancies in drag prediction [80]. Additionally, neither of these codes predicts post-stall performance accurately, which is extremely important for stall-controlled blades. Because of the limitations of the current predictive codes, taking experimental data for low Reynolds numbers is necessary to ensure accurate data are obtained for blade design.

### 5.2 2-D Airfoil wind tunnel corrections

Multiple corrections were applied to the data to account for the increased drag due to the airfoil mount as well as the flow conditions altered by the wind tunnel walls. Perhaps, one of the first corrections that should be considered when collecting airfoil data is the drag tare associated with the force balance. This must be accomplished with the model off the support or sting. With the model off, the wind tunnel needs to be run at the operating conditions and, depending on the type of sting, angles of attack. To account for the increased drag due to the airfoil mount,
the bottom bracket of the mount (detached from the airfoil) was installed in the wind tunnel and secured at an angle of 0.0°. Subsequently, the wind tunnel was run at the test Reynolds numbers, and, using eqn (1), the measured drag was converted to a coefficient. This value was then subtracted off the measured values for drag at the corresponding Reynolds numbers. Measurements of drag should be made at the operating conditions and a table or graph for correction made. These values would then be subtracted from the total drag recorded to get the drag of the model alone. More ways to estimate drag added by the tare can be found in Barlow et al. [78].

The wind tunnel walls affect the 2-D flow so that it is not the same as an airfoil performing in the open air. The effects of the wind tunnel walls can be divided into three categories: blockage, streamline curvature and buoyancy (as described by Barlow et al. [78]).

The first type of blockage is solid blockage, $\varepsilon_{sb}$, which is caused by the presence of a model within the wind tunnel test section. This decreases the effective area that the air is flowing through. Applying Bernoulli’s equation shows that the air velocity must increase when it flows through the decreased area, effectively increasing all aerodynamic forces and moments for that angle of attack. As seen in eqn (10), the solid blockage correction factor is merely a function of model volume, $M_v$, and wind tunnel cross-sectional area, $A_{ts}$. In calculating this equation, a value of 0.74 was used for per the recommendation by Barlow et al. for a wing spanning the tunnel. In calculating, the simple cross-sectional area was used since the displacement thickness at the location of the model is not currently known. Notably, accounting for only the model volume does not account for the change that occurs with airfoil orientation. However, since the airfoil will be operating over a relatively small range of angles, this correction factor is sufficient

$$
\varepsilon_{sb} = \frac{K_v M_v}{A_{ts}^{3/2}}. \tag{10}
$$

The second type of blockage is wake blockage, $\varepsilon_{wb}$, which is caused by a decreased velocity in the airfoil wake. According to the continuity equation for closed test sections, the freestream velocity must increase. The effect of the blockage is proportional to the size of the wake; thus, it is proportional to the measured value for drag. The constant of proportionality only depends on the chord of the airfoil and the height of the test section, $h_{ts}$, as seen in eqn (11), where $c_{dH}$ is the uncorrected drag coefficient

$$
\varepsilon_{wb} = \left(\frac{c}{2h_{ts}}\right) c_{dH}. \tag{11}
$$

The second effect that is caused by the wind tunnel walls is streamline curvature. This correction occurs because the physical boundaries of the walls interfere with the natural curvature of the free air over the model, effectively increasing the airfoil effective camber and a subsequent increase in lift, moment and angle of attack. However, the drag is not affected by this phenomenon. Equation (12) documents the streamline correction factor
Buoyancy, or boundary layer growth, is the third notable effect of the wind tunnel walls. As the air moves through the wind tunnel, the boundary layer will grow increasingly large and the static pressure will decrease. Since the freestream velocity is measured upstream of the model, the actual freestream velocity at the model is higher. This correction factor must be determined experimentally for each unique wind tunnel. Equation (13) shows the form of the correction factor, and eqn (14) shows the correction factor equation used by Selig et al. [69]. Figure 23 shows the variation of the velocity correction factor with Reynolds number. While this correction is insignificant for Reynolds numbers above 400,000, the correction factor increases significantly as Reynolds number decreases, suggesting that such a correlation should be determined for low Reynolds number testing and should be used to correct the data.

\[ K_{vel} = f(V_u), \]  

\[ K_{vel} = 1.015755 - 0.0002391V_u + 0.00001712 \sqrt{V_u} + \frac{0.1296}{\sqrt{V_u}}. \]  

Multiple methods of combining correction factors have been published, two of which will be presented and used for comparison purposes. The first is from Barlow et al. [78], which can be seen in eqns (15)–(19).

\[ V = V_u (1 + \epsilon_u), \]  

\[ a = a_u + \left( \frac{1}{8\pi h^2 + (c/4)^2} + \frac{1}{2\pi} \sigma \right) c_l, \]  

\[ c_l = c_{l_v} (1 - \sigma - 2\epsilon_u), \]
\[ c_d = c_{d_1} \left(1 - 3e_{sb} - 2e_{wb}\right), \quad \text{(18)} \]

\[ e_b = e_{sb} + e_{wb}. \quad \text{(19)} \]

A second method, documented by Selig et al. [69], can be seen in eqns (13)–(16)

\[ V = V_a K_{re} \left(1 + e_b\right), \quad \text{(20)} \]

\[ a = a_u + \frac{57.3\sigma}{2\pi} \left(c_{i_b} + 4c_{m},\right), \quad \text{(21)} \]

\[ c_l = c_{l_1} \frac{(1-\sigma)}{(1+e_b)^3}, \quad \text{(22)} \]

\[ c_d = c_{d_1} \frac{1-e_{sb}}{(1+e_b)^2}. \quad \text{(23)} \]

Typical data with correction are shown Figure 24 [76]. In these figures, the data presented are compared with published data from UIUC.

One Reynolds number was chosen in the drag insensitive region, 200,000, as shown in Fig. 25, to validate the experimental process by comparing it with published PROFIL data, generated XFOIL data, published data for this Reynolds number, and general airfoil theory. The published experimental data were taken at UIUC. Lift and drag curves were developed from approximately –5° to 15°. The lift and drag curves from this experimental test are shown in Fig. 24. As shown, the lift curve matches PROFIL, XFOIL and published experimental data very reasonably, as well as a theoretical airfoil slope of \(2\pi\) per radian or 0.1 per degree. Both XFOIL and experimental data predict stall at approximately 10°, but PROFIL data do not include a stall angle of attack. Noticeably, the UIUC drag data matched the predictions from XFOIL and PROFIL reasonably well, although the experimental data are consistently about 0.01 high in drag coefficient. Nonetheless, the drag curve follows the form of the prediction and UIUC data. The higher experimental values for drag may be due to the surface roughness of the foam airfoil model.

Figure 26 highlights the need for taking experimental data, especially at lower Reynolds numbers, as there is no other published data for this airfoil at a Reynolds number of 75,000. The data are presented for a Reynolds number of 75,000 for S823 airfoil. The XFOIL comparison does not capture the stall region for this airfoil nor does it capture the flow separation on the bottom of the airfoil at low negative angles of attack as is shown in the profile presented in Fig. 27.

After analysing the applied corrections for the data, the drag tare was the most significant correction, reducing the drag coefficient by at least 0.006 and nearly 0.01 for a Reynolds number of 75,000. The wind tunnel wall corrections, however, proved insignificant for low values of lift and drag. With a large wake due to large
Figure 24: Lift and drag curves for an S823 airfoil at Re = 200,000 [76].

Figure 25: Drag coefficient data over a range of Reynolds numbers, angle of attack of –0.2° [76].
amounts of separation over the airfoil surface and a subsequent increase in drag, the wind tunnel wall corrections became a greater percentage of the actual value for lift and drag coefficients but were still only noticeably at higher values. By applying all of these corrections, the lift coefficient curve matched reasonably well for all the data, and the drag curve matched the data better than originally. However, the drag coefficient curve remained noticeably higher than the predictions and UIUC data, roughly 0.01 high in drag coefficient.

Figures 28–30 show the uncorrected, tared and corrected data using the methods espoused by both Selig et al. and Barlow et al. In applying the corrections to the drag data, the drag tare was applied before the wind tunnel wall corrections were
These data do not include any corrections due to moment coefficient as seen in eqn (21) for the Barlow et al. corrections. After all the corrections, the drag curve is still noticeably higher than the predicted and UIUC data. One possibility is that the drag tare performed underestimates the actually drag added by the mount, which is somewhat likely because the shape of the curve is very similar to the other data and the lift curve matches very well. A second possibility is increased drag due to the surface roughness present on the airfoil.

Two corrections are not recommended in future studies. First, the correction for angle of attack, which never reached an order higher than the order of the fixed uncertainty of the angle measurement using both methods, is not recommended unless the angle measurement method is improved. Second, the correction for velocity has not been applied in this study because the Reynolds numbers presented are nominal and the change due to the correction is not significant enough to warrant using the corrected value. However, preliminary calculations suggest that for lower Reynolds numbers than tested in the current study, the correction may be significant and should be performed.

Figure 28: Corrections to an S823 airfoil at Re = 200,000 [76].
Figure 29: Corrections for an S823 airfoil at Re = 150,000 [76].

Table 3: Percent decrease due to corrections [77].

<table>
<thead>
<tr>
<th>Method</th>
<th>Lift</th>
<th>Drag</th>
</tr>
</thead>
<tbody>
<tr>
<td>Selig et al.</td>
<td>7.15%</td>
<td>10.84%</td>
</tr>
<tr>
<td>Barlow et al.</td>
<td>7.51%</td>
<td>11.73%</td>
</tr>
<tr>
<td>Selig et al.</td>
<td>7.18%</td>
<td>10.74%</td>
</tr>
<tr>
<td>Barlow et al.</td>
<td>7.54%</td>
<td>11.61%</td>
</tr>
<tr>
<td>Selig et al.</td>
<td>7.41%</td>
<td>11.34%</td>
</tr>
<tr>
<td>Barlow et al.</td>
<td>7.80%</td>
<td>12.32%</td>
</tr>
</tbody>
</table>

Comparing the two methods for wind tunnel wall corrections to each other, the method present by Barlow et al. is generally 0.5%–1.0% more significant than that presented by Selig et al. as seen in Table 3.

5.3 Blockage corrections for wind turbine rotors

The challenge of testing wind turbine blades in a small wind tunnel was explored by Van Treuren and Gregg [26]. Testing of small-scale models in wind tunnels,
some with test sections of 1 m × 1 m or less, has been accomplished in the past [44–48, 62, 81–83]. Blockage can be a significant factor in wind tunnel testing; however, Howell et al. suggest that fixed tunnel blockage up to 10% does not significantly affect results [46]. Blockage due to rotating wind turbine blades has not been extensively studied but still needs to be considered. Chen and Liou have documented the correction factor necessary to account for tunnel blockage due to the rotating blades, as seen in eqn (24) [35]. The correction factor, BF, is the ratio of the freestream velocity with the turbine installed, $U_M$, to the freestream velocity without the turbine installed while the wind tunnel fan is operating under the same conditions, $U$

$$BF = \frac{U_M}{U}. \tag{24}$$

This blockage factor is used whenever a velocity is involved in calculation of a figure of merit, such as the TSR. $TSR_M$, shown in eqn (25), is the tip speed ratio of the model based on the velocity of the tunnel with the wind turbine in place with the wind tunnel fan operating at the speed that was found for the operation of the

![Figure 30: Corrections for an S823 airfoil at Re = 75,000](76)
wind tunnel at the freestream velocity without the wind turbine present in the test section. Using the blockage factor, eqn (26) calculates the actual TSR for the wind turbine power system

\[
TSR_M = \frac{\Omega R}{U_M},
\]

(25)

\[
TSR = TSR_M BF = \frac{\Omega R}{U}.
\]

(26)

This blockage factor, \(BF\), is applied to the coefficient of power based on the \(U_M\) (\(C_{pM}\), eqn (27)) and used to calculate the corrected power coefficient, \(C_p\), as seen in eqn (28). For all the power coefficients, \(P\) is the rotor power from the wind turbine (a function of the angular velocity \(\Omega\)), \(\rho\) is the air density and \(A\) is the area swept by the rotating wind turbine blades

\[
C_{pM} = \frac{P_{\text{Rotor}}}{P_{\text{Freestream,M}}} = \frac{\tau\Omega}{\frac{1}{2} \rho R^2 U_M^3},
\]

(27)

\[
C_p = \frac{P_{\text{Rotor}}}{P_{\text{Freestream}}} = C_{pM} BF^3 = \frac{\tau\Omega}{\frac{1}{2} \rho R^2 U^3}.
\]

(28)

Figure 21 illustrates the application of these corrections to wind tunnel system testing. These wind tunnel blades, as seen in Fig. 16a, have diameters of 0.5 m, 0.4 m and 0.3 m tested in the wind tunnel test section described in Fig. 15. Wind tunnel blockage for these diameters is 52.8%, 33.8% and 19.0%, respectively. This is well past the upper limit of 10% that most tests seek to be below as lower than 10% typically neglects any blockage corrections.

### 5.4 3-D flow effects

Additionally, 3-D flow effects, not taken into account in traditional wind turbine design using 2-D airfoil data, have been shown to influence aerodynamic efficiency. Robinson et al. examined the impact of three-dimensionality, unsteadiness, and flow separation on downwind horizontal axis wind turbines (HAWTs) [84]. Their results show that ‘... quasi-static separation and dynamic stall analysis methods relying on purely 2-D flow characterisations may not be capable of simulating the complex 3-D flows observed with these data’. These experiments used a 10-m diameter rotor with constant chord blades and an S809 airfoil tested at several different pitch angles. They found aerodynamic loads were far in excess of 2-D wind tunnel data for near- and post-stall conditions. Testing of airfoil data and comparison with actual wind tunnel data were also done by Butterfield et al. [42]. They confirm that stall-regulated HAWTs show peak
power levels significantly greater than predicted by BEMT using 2-D airfoil data. They warn that ‘wind turbine designers should be aware of the differences when designing stall-controlled wind turbine blades or they may underestimate the peak performance and loads’. They also suggest more research is needed in these areas.

Using CFD analysis, Qiu et al. showed that there is significant spanwise flow on the suction surface of a rotating wind turbine blade [85]. The control of these behaviours using roughness has begun to be investigated and has shown to have a significant effect either to delay separation [86,87] or to increase drag [88–90]. Thus, the effects of roughness must be further analysed to determine what designs could be improved by the application of intentional roughness. At the low Reynolds numbers, roughness was found to improve performance significantly.

**NOMENCLATURE**

- **A** Wind turbine area swept by rotor blades (amps)
- **A_{ts}** Wind tunnel test section area minus the displacement thickness around the perimeter
- **a** Axial momentum factor
- **a’** Tangential momentum factor
- **BEMT** Blade element momentum theory
- **BET** Blade element theory
- **BF** Blockage factor
- **C, c** Chord
- **C_D, c_d** Drag coefficient
- **C_{d,u}** Uncorrected drag coefficient
- **C_L, c_l** Lift coefficient
- **C_{l,u}** Uncorrected lift coefficient
- **C_M, c_m** Moment coefficient
- **C_{m,u}** Uncorrected moment coefficient
- **C_P** Rotor power coefficient
- **C_{P,M}** Rotor power coefficient with wind turbine in the tunnel (blockage)
- **C_{P,S}** System power coefficient
- **C_T** Thrust coefficient
- **D** Drag
- **F_c** Centrifugal forces
- **h_{ts}** Wind tunnel test section height
- **J** Advance ratio
- **K_1** Constant in solid blockage equation
- **K_p** Rotor speed power coefficient
- **K_{vel}** Velocity correction factor
- **L** Lift
- **l** Unit length
\( M, M_A \) Pitching moment and aerodynamic moment
\( M_v \) Airfoil model volume
\( P, P_{\text{Rotor}} \) Power and rotor power
\( P_{\text{Freestream}} \) Freestream power
\( P_{\text{Freestream}, m} \) Freestream power (blockage)
\( p_{\infty} \) Freestream static pressure
\( p_d \) Static pressure at the actuator disk
\( Q, \tau \) Torque
\( R, r, R_N \) Radius
\( \text{Re}, R_N \) Reynolds number based on chord
\( \text{RPM} \) Revolutions per minute
\( sc \) Scaling parameter
\( T \) Thrust
\( \text{TSR}_M \) Tip Speed Ratio with wind turbine in the tunnel (blockage)
\( U, U_{\infty} \) Freestream velocity
\( U_d \) Velocity at the actuator disk
\( U_M \) Wind tunnel velocity with the wind turbine in the tunnel (blockage)
\( U_{\text{rel}} \) Incoming velocity to the turbine blade (axial and rotational combined)
\( U_w \) Wake velocity
\( V \) Freestream velocity
\( V_u \) Uncorrected freestream velocity
\( W \) Weight
\( x \) Chordwise distance
\( y \) Vertical distance
\( \alpha \) Angle of attack
\( \alpha_u \) Uncorrected angle of attack
\( \beta \) Pitch angle
\( \delta_r \) Segment of radial length
\( \varepsilon_b \) Blockage correction factor
\( \varepsilon_{sb} \) Solid blockage correction factor
\( \varepsilon_{wb} \) Wake blockage correction factor
\( \mu_{\text{air}} \) Dynamic viscosity of air
\( \sigma \) Streamline curvature correction factor
\( \sigma_g \) Gravitational stress
\( \sigma_A \) Aerodynamic stress
\( \sigma_C \) Centrifugal stress
\( \nu \) Translation velocity and kinematic viscosity
\( \lambda, \text{TSR} \) Tip speed ratio
\( \rho, \rho_{\text{air}} \) Air density
\( \phi \) Flow angle
\( \omega \) Natural frequencies
\( \Omega \) Blade rotational speed
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