Aerodynamic Load Distribution and Wake Measurements on a Sub-scale Wind Turbine

Arash Hassanzadeh\(^1\), Jonathan W. Naughton\(^1\), Julian LoTufo \(^2\) and Horia Hangan\(^2\)

\(^1\)Wind Energy Research Center, University of Wyoming, Laramie, WY, USA
\(^2\)WindEEE Research Institute, Western University, London, Ontario, Canada

E-mail: naughton@uwyo.edu

Abstract. Wind tunnel testing of wind turbines is critical for better understanding the blade aerodynamics and wakes as well as for producing data necessary to validate computational models. In this work, a 2 m diameter wind turbine that was designed to produce a wake with characteristics similar to that of an industrial scale turbine was tested in a large wind tunnel environment. Unsteady inflow, unsteady blade load distribution, and instantaneous wake measurements were obtained to characterize wake development and near-wake behavior. The results showed that the measurement of both blade loading and wake properties facilitate understanding of the wake’s behavior, and the blade load distribution can be used to explain the near-wake structure. Tests covering a wider range of conditions would be valuable for validating simulations of wind turbine flows.

1. Introduction

Wind turbine wake interaction affects wind farm efficiency by reducing turbine performance and blade lifetime. Reliable experimental data are needed for better understanding the development of the wake from the flow’s interaction with the blade, and the wake’s sensitivity to inflow and the turbine operating condition. Additionally, high-quality data from experiments are required to validate computational models used to simulate the wakes [1, 2]. Validation experiments have specialized requirements such as detailed measurement of the inflow, to make them useful to computational researchers. Validated computational models can be used for wind plant layout with confidence about their capabilities, thus leading to more trusted results.

There are two approaches to study the wake experimentally: field studies of full-scale turbine wakes and sub-scale wake studies. Field studies do not allow for detailed measurements, and the field conditions are variable. As a result, sub-scale testing can provide complementary results that include detailed measurements under controlled conditions. However, sub-scale tests will not be effective in this role if they do not capture at least some of the relevant physics that govern the formation and evolution of wakes from full-scale wind turbines. The downside of wind tunnel experiments is that the scale models do not operate at conditions representative of full-scale turbines, suggesting that some of the important physics might be missing. To address this problem, there is a need to match dimensionless parameters between the full-scale and sub-scale wind turbines to the degree possible. For example, a new blade design approach [3, 4] matches the non-dimensional normal and tangential force distributions for sub-scale and industrial-scale blades assuming that the resulting wakes exhibit similar features. Another important feature
missing from most of sub-scale tests is measurement of the blade loading to complement wake measurements. As the wake originates from the blade, a full understanding of the wake requires both blade and wake measurements.

The objective of this work is to demonstrate the use of measurements from a 2 m diameter wind turbine to better understand wake development and evolution. In addition, this study seeks to demonstrate the potential value of such measurements for computational fluid dynamics (CFD) validation through studying the inflow, blade aerodynamics, wake development and evolution, and their relationships. Unsteady inflow, unsteady blade pressures, and instantaneous wake measurements were obtained to characterize wake development and near-wake behavior. The results show that the blade load distribution can be used to explain the near-wake features observed.

2. Background
A number of experimental studies have been carried out to understand turbine wakes better. Experimental wind turbine wake studies consist of field measurements, and wind tunnel tests. Field measurements are considered an important tool for characterizing full-scale wind turbine wakes, since they experience real-world conditions [5, 6]. Some of the challenges posed by field measurements are the very large measurement volumes involved and the uncertainty in the boundary conditions, which limits their use for validating computational models.

Wind tunnel investigations present the advantage of repeatability with experiments being carried out under controlled test conditions. Several measurement techniques including surface pressure measurements for blade loading, and particle image velocimetry (PIV), hot-wire anemometer and laser Doppler anemometer for wake velocities have been employed for wind tunnel testing. Very limited large-scale wind tunnel experiments (UAE [7] and MEXICO [8]) have been conducted for blade load distribution or/wake measurements on wind turbines that were designed for maximum power efficiency. There have been a number of wind turbines with rotor diameter between 0.5 m to 2.5 m used for wake studies with a focus on flow-field measurements [9–11]. As an example, Bayati et al.[11] performed an experimental study to characterize the wake aerodynamics of a 2.38 m diameter wind turbine. The turbine was designed to have similar thrust coefficient to that of a full-scale wind turbine. The campaign was carried out in the Politecnico di Milano 14 m × 4 m atmospheric boundary layer test section. Wake measurements were carried out using hot-wire anemometers and PIV. There have been a number of smaller wind turbines with rotor diameter less than 0.5 m with a focus on only wake measurements mostly using geometrically scaled-down turbines [12, 13]. These and other wind tunnel studies used wind turbines that were not always designed for producing wakes similar to those of industrial-scale turbines. Moreover, the previous efforts, particularly efforts on 2 m scale or smaller turbines, were limited to wake measurements with no measurement of blade load distributions. The objective of the present study was chosen to address these issues.

3. Approach
In this section, an overview of the experimental approach is provided along with a description of the analysis performed to characterize the inflow, the blade load distribution, and the wake.

3.1. Experimental approach
A description of experimental facility and instrumentation for blade load and flow-field measurements is provided followed by a discussion of the test cases.

The testing was performed in the Wind Engineering, Energy and Environment (WindEEE) dome, which has a hexagonal test chamber with diameter of 25 m. Although the facility is capable of many different types of atmospheric flows, a straight flow with uniform and vertical shear profiles (with different turbulence intensities) was used in this work. For the straight flow,
the effective size of the tunnel is 25 m × 14 m × 3.5 m, and the tunnel is capable of operating up to 30 m/s. In this work, the tunnel speed was chosen to be 7 m/s to achieve a specific tip speed ratio (TSR) at a reasonable turbine rotational rate of approximately 600 rpm.

The experiments were performed using a three bladed wind turbine with a rotor diameter of 2 m. The blade was designed using an inverse blade element momentum (BEM) theory to produce a wake in the wind tunnel similar to that of a full-scale wind turbine [3]. Two tripped high Re number airfoils (S814 and S825) were employed in the design process in an attempt to achieve a comparable aerodynamic behavior for full-scale and sub-scale blades at both on-design and off-design conditions [14]. Figure 1 shows a solid model of the resulting blade. The blades were manufactured using 10 plies of carbon fiber for the pressure and suction sides of the blade. One of the blades was equipped with taps for surface pressure measurements. 126 pressure taps with a diameter of 0.86 mm were installed on the pressure and suction side of the blade at 5 different span-wise locations. Those stations are shown as dashed lines in Figure 1 (a) as A-E, corresponding to $r/R = 0.27, 0.40, 0.53, 0.73,$ and $0.86$. The blades were mounted to an existing turbine model with a hub height of 1.5 m using a custom-built hub.

To characterize the inflow velocity, Cobra probe measurements at $x/D = -1.1$ and $y/D = -0.7$ upstream of the turbine were acquired (see Figure 2). The Cobra Probe is a multi-hole pressure probe able to resolve 3-components of velocity and local static pressure. A vertical array of five Cobra probes was employed with a spacing of 50 cm between probes such that the middle probe was at the hub height of the turbine. The Cobra probes were continuously sampled throughout a test at an acquisition rate of 10000 Hz and output to a file at 1250 Hz. The data output rate was achieved by decimating (low-pass filtering and down-sampling) the data. A typical run time for each test case was 8 minutes, which is linked to the measurement requirements of the blade pressures.

To determine the blade load distribution, surface pressures were measured at the five different radial locations discussed above. An in-house developed compact unsteady pressure measurement system that fit within the turbine’s hub, as shown in Figure 3, was used. Two 32-channel Electronic Scanning Pressure (ESP) modules were used to acquire unsteady pressure data. A National Instruments myRIO system equipped with an FPGA was used to digitally address the ESP modules directly. The sampling at 20 kHz over 32 channels for each module led to a sampling rate of 625 Hz per channel. The pressure data were recorded for 8 minutes ensuring an uncertainty of 0.05 for pressure coefficient, which will be discussed in the analytical approach section. To account for variations in testing conditions (tunnel pressure and temperature), the pressure transducers were calibrated daily using a 6-point linear fit.

To characterize the wake, the velocity field behind the turbine was measured using PIV in the axial-vertical plane at two different downstream locations. The PIV setup for the wake measurements at $x/D = 1$ is shown in Figure 2. A dual oscillator/single head, diode pumped Nd:YLF laser with an energy of 60 mJ/pulse at 0.1-1 kHz, a wavelength of 527 nm, and a beam diameter of 3 mm was used to illuminate the flow field. A cylindrical lens was positioned
immediately in front of the laser head to convert the beam into a two-dimensional sheet. Two 12-megapixel cameras with resolutions of 4096×3072 pixels were used to cover the area of interest. Under optimal seeding conditions, considering the laser sheet’s width and intensity, and with a 10 cm overlap of the cameras’ views, an approximately 70×75 cm field of view (FOV) was achieved. PIV data were recorded for 2 minutes per test case with a sampling frequency of 55 Hz resulting in approximately 6600 images to reduce the statistical uncertainty that arises from the unsteady flow. More details regarding uncertainty analysis can be found in the analytical approach section.

Using the methods discussed above, a number of test conditions were considered for the 2m diameter wind turbine. Since wind turbine aerodynamics and wakes are highly dependent on inflow and turbine operating condition, the effects of uniform and shear inflow were considered, and the blade rotational rate was varied such that TSR of 9 (on-design condition) and 5 (off-design condition) could be examined.

Figure 2: Schematic of the experimental setup in the WindEEE dome. The locations of the Cobra probe and PIV measurements are indicated.

Figure 3: The pressure system mounted to the turbine’s hub for blade surface pressure measurements. Tubes have not been connected to the transducers, and the nose cone is removed.
3.2. Analytical approach

The methods of analyzing the measurements from the Cobra probes, the pressure system, and the PIV system are described in the following section.

The Cobra probe data consist of time histories of u, v, and w components of the wind velocity for each probe. The mean and turbulent velocities, integral length scale, and auto-spectral density function were calculated from the measured velocities. 600,000 samples collected during 8 minute experiments were used to calculate the mean velocities, and the random error with the 95% confidence interval was calculated for the uniform and shear inflow cases, which are less than 0.2% and 1.06%, respectively. The sampling rate allowed for measuring the spectra at frequencies up to 625 Hz. The 8 minute record was split into 100 individual records of 4.8 seconds so they could be averaged to reduce random uncertainty. The resulting minimum resolved frequency was \( \sim 0.2 \) Hz.

Multiple steps including data correction and azimuthal averaging were required to obtain accurate unsteady blade loading from the raw surface pressure data. Certain physical phenomenon manifest themselves on the actual pressure data and must be accounted for in the pressure measurements conducted on a wind turbine blade. In the current setup, the pressure measurements are conducted remotely, where the ESPs are connected to the surface ports through large length-to-diameter pressure tubing. In such a configuration, the tubing affects the pressure measurements in a manner similar to a low pass filter by attenuating the signal and introducing a time lag as compared to the original signal. These effects in the pressure signals were corrected using a Weiner filtered system response model (WFSRM) [15]. The parameters used in the WFSRM were fine-tuned through characterizing the tubing using a pulse test. A pressure pulse was created at each tap location on the blade surface. The input pressure pulse was measured using a high-speed pressure transducer at the surface and also remotely using the transducers in the ESPs. The remotely measured pressure signal for each channel was reconstructed using the WFSRM to compare to the reference pressure transducer signal at the input. Figure 4 compares input pressure signals with the raw pressure signals from one transducer in the ESP and the WFSRM reconstructed pressure signals. The results for two pressure ports with different length-to-diameter ratios show accurate reconstructions. The greater attenuation and lag can be observed for the port located further from the transducers (Figure 4(b)) compared to those of the port located closer (Figure 4(a)). The correction performance on all pressure taps on the blade was checked and tuned using tubing characterization. For further details on the accuracy of this technique, see reference [16].

Another important phenomenon is the centrifugal force effect, which occurs because the column of air trapped between the sensor and the port exerts a net negative force on the sensor diaphragm as the blade rotates. The resulting difference in measured and actual surface pressure, due to the centrifugal force is a function of span-wise location of the port and the blade rotation rate, and is given by

\[
P_{cen} = 0.5 \rho r_{sec}^2 \omega^2
\]

where \( \rho \), \( r_{sec} \), and \( \omega \) are the density, port’s span-wise location from the center of rotation, and turbine’s rotational rate, respectively.

After data correction, an azimuthal phase averaging was applied to the pressure data. For each port, every pressure data point was binned according to its azimuthal angle, and the pressure data in every bin was averaged. The azimuthal-averaged pressure for each port were calculated from 1000 continual pressure records of 200 samples during 8 minute of testing. These data were separated into 360 phase bins, resulting in approximately 550 samples per bin. The pressure coefficient (\( C_P \)) for each port was calculated by dividing the measured pressure by the dynamic pressure calculated from the blade local inflow velocity (\( U_{BI} \)). The \( U_{BI} \) was calculated using the Cobra probe data (turbine inflow velocity, \( U_{TI} \)), induction factor from BEM calculations, and the turbine rotational rate. The azimuthal-averaged pressures were also used
to calculate blade loading. The normal and tangential forces were calculated by integrating the azimuthal-averaged pressures at each blade span over the suction and pressure surfaces. Having the blade load distribution allows for comparison of different load cases and between measured and simulated loads.

The uncertainty values were quantified for the pressure measurements. These uncertainties consisted of systematic ($U_{sys}$) and random ($U_{ran}$) uncertainties and were calculated using approaches given by Coleman and Steele [17]. The total uncertainty for a given measurement was found using

$$U = \sqrt{U_{sys}^2 + U_{ran}^2},$$  \hspace{1cm} (2)$$

where $U$ is the total uncertainty associated with the data. The random error is calculated using a 95% confidence interval of the Student’s t-distribution as

$$U_{ran} = t_{95\%} \frac{\sigma}{\sqrt{N}},$$  \hspace{1cm} (3)$$

where $\sigma$ is the standard deviation of the data and $N$ is the number of independent samples. Systematic (bias) uncertainties arose from the pressure calibration, the inherent bias in the pressure transducer, and digitization error for the pressure [17]. The highest values of total uncertainty for $C_P$ at uniform and shear inflow cases at each bin were calculated, which are less than 0.03 and 0.05, respectively. In order to calculate the uncertainty of blade normal and tangential forces, the uncertainties from the pressure data was propagated through the integral (summation for the discrete data) used to calculate these loads [17]. The highest values of total uncertainty for those forces at uniform and shear inflow cases were found to be approximately 1.6% and 2.6%, respectively.

The PIV raw images were processed using LaVision’s DaVis software. Velocity results were computed using a multi-pass, cross-correlation method for each camera. A $128 \times 128$ pixel initial interrogation region was used, with iteratively decreasing window size to a $64 \times 64$ pixels region with a 50% overlap of the regions. In order to decrease the uncertainty due to the flow unsteadiness and noise, the mean velocity fields were determined by averaging over 2000 images.
4. Results and discussion

In this section, inflow data for various test cases will be considered first, followed by a discussion of the blade load distribution and unsteady surface pressure distribution. Following this, the wake data in the near-wake and far-wake will be discussed. Finally, the inflow, blade load, and wake data will be coupled to understand the flow behavior in the wake.

Measurement of the properties of the inflow to the turbine is critical to establish the conditions the turbine will experience. As a result, the time-averaged axial velocity, turbulence intensity, and turbulent kinetic energy (TKE) are acquired upstream of the turbine. As shown in Figure 5, the axial mean velocity for the uniform inflow is approximately 7.0 m/s at all heights, but it increases with height for the shear inflow and changes from 5.7 m/s at 0.5 m to 9.3 m/s at 2.5 m. The effects of turbine induction can be seen in Figure 5 (a) for both uniform and shear inflow cases. The velocity is affected by tip-speed ratio that is indicative of a change in induction between the cases, which is expected. The turbulence intensity is distributed uniformly for the uniform inflow and is about 7%, but, for the shear case, it decreases from 28% at the height of 0.5 m to 10% at the height of 2.5 m. The kinetic energy of the turbulence fluctuations is much smaller for the uniform inflow case compared to that of the shear case, particularly below the hub height ($H=1.5 \text{ m}$). In both cases, the TKE reduces with height. This shows that the two inflows are quite different, both in the mean and turbulence (low vs. high turbulence) velocities.

The integral length scale and the auto-spectral density function are calculated for different probes at uniform and shear inflow conditions. The integral length scale is representative of the large scale turbulence length scale. As shown in Figure 6 (a), it varies little with height for both cases, and, for the shear inflow, it is greater than that of the uniform inflow. Figure 6 (b) shows that the turbulence energy contained in the shear inflow at hub height is much higher at all frequencies than that of the uniform inflow. A clear peak in the spectrum can be observed at low frequency for the shear case, whereas no such peak is evident in the uniform case. It can be concluded that the inflow profile for the shear case is characterized by eddies with larger average size and higher levels of kinetic energy than for the uniform case.
Figure 6: Inflow characteristics for uniform and shear cases: (a) integral length scale at different heights, and (b) auto-spectral density function at hub height ($H=1.5$ m).

In addition to characterizing the inflow, quantification of the blade loads are needed to understand the development of the wake. Although the blade loads vary as the blade rotates, here we consider the blade loading when the blade is pointing straight up. Figure 7 shows the comparison between the blade loads determined from the measured pressure and the BEM results. At TSR=9, which is the on-design condition, the blade loading is higher for the shear inflow case compared to that of the uniform inflow case. For both inflows, the maximum loading occurs in the blade tip region. As shown in Figure 7 (a), BEM slightly underestimates the normal force for the uniform inflow case, but slightly overestimates it for the shear inflow case compared to the experimental data. At TSR=5, which is an off-design condition, BEM and Xfoil results do not converge for the shear inflow case. Therefore, the numerical results are only presented for the uniform inflow. Figure 7 (b) shows that, in contrast to the TSR=9 case, the maximum loading occurs at $r/R=0.5$ and decreases with increasing radius for the TSR=5 case. The normal force from the experiment is higher than that of the BEM results, particularly in the blade inboard section. Figure 7 (c) shows the tangential force distributions at TSR=9 case, where it is clear that BEM overestimates the tangential force for the both uniform and shear inflow compared to the experimental data. For the shear case, the tangential force is maximum in the outboard section for the both BEM and experiment. For the uniform inflow case, the tangential force from BEM is roughly constant, while the experiment again shows a peak in the outboard region. The tangential force from experiment in Figure 7 (d) for TSR=5 case is much lower compared to that of the BEM. Therefore, much lower power production is expected from the experiment compared to that predicted by BEM. Figure 7 shows that the normal and tangential forces are higher for TSR=9 case compared to those of TSR=5 case in the blade outboard section because of the higher blade inflow velocity resulting from the higher rotation speed for the TSR=9 case. The results are the opposite in the blade inboard section, as the blade inflow angle plays a dominant role compared to the blade inflow velocity which is less affected by the rotational velocity.

To complement the loading discussed at $\psi=0$, the variation of normal and tangential forces with blade position at three span-wise locations is considered in Figure 8. The variation of blade loading in the shear inflow cases is more noticeable compared to that of the uniform
Figure 7: Blade load distributions from the experiment and BEM model for two TSRs (TSR=9 and 5) at $\psi=0^\circ$: (a)-(b) normal force, and (c)-(d) tangential force.

inflow case because of the freestream velocity gradient. The loads are naturally higher at or near $\psi=0$ because the velocity is higher there, but lower when the blade is pointing straight down ($\psi=180^\circ$). The normal and tangential forces are approximately symmetric in the blade inboard sections, but not symmetric in the outboard section, especially for the shear inflow case at the TSR=5. From the blade loading (integrated force values), it is hard to interpret whether it is because of the stall or the inflow velocity. In addition, the tangential force in the inboard section of the blade is negative during the entire blade rotation under uniform inflow at the TSR=9 case, which could be due to the airfoil’s stall or low angle of attack.

Explaining the reasons why the normal and tangential force distributions behave as they do is somewhat difficult, and, as a result, the pressure distributions are considered for two cases (uniform, TSR=9 and shear, TSR=5). To display the variations in the pressure distribution at a given radial location, contours of phase-averaged pressure coefficient are used. Figure 9 shows the pressure distributions on the suction and pressure surfaces of the airfoils for the uniform inflow and TSR=9 case at different span-wise stations. The x-axes shows the $x/c$ or relative chord location on the airfoil surface, and the azimuthal angle is represented on the y-axes. The color scale represents the magnitude of the pressure coefficient, with red showing the maximum pressure coefficient and blue showing the minimum, or maximum suction, pressure coefficient.
Figure 8: Blade load distributions at different blade azimuthal locations for three airfoil profiles along the blade: (a)-(c) normal force, and (d)-(f) tangential force.

Because of the smaller physical chord length and the required space for pressure ports, pressure data is not available close to trailing edge for the outboard sections. It can be observed that the flow is attached on both pressure and suction surfaces along the blade span. As the inflow is relatively uniform for this case, there is only a small variation in the pressure distributions with blade phase angle, but the tower effects can be seen on the suction side in the region around $\psi=180^\circ$. Note that the iso-$C_p$ lines are overlaid on the pressure distributions to help identify the changes in surface pressure. The low pressure region on the suction side of the blade gets larger moving outboard, because the airfoils in the outboard section experience a higher angle of attack. On the pressure side, the positive pressure coefficient increases along the blade due to the higher angle of attack. Negative pressure coefficients can be observed on the pressure side, especially for the S814 airfoil at $r/R=0.27$, where the flow accelerates because of a high degree of curvature on the pressure side. This occurs on the blade where the angle of attack is low, which explains the negative tangential force observed in Figure 8 (d).

Pressure distributions are again considered to explain features found for the shear inflow TSR=5 case in Figure 8. The phase-averaged pressure distributions for this case are shown in Figure 10 at five different span-wise locations. For this case, the pressure side pressures are higher, but the suction side pressures are lower (or higher suction pressure) compared to those of the TSR=9 case shown in Figure 9. This occurs because the free-stream velocity plays a more important role in airfoil’s inflow velocity and inflow angle at lower TSRs. Therefore, the airfoils experience a higher angle of attack at TSR=5 compared to those experienced in the TSR=9
Figure 9: Phase averaged pressure distribution at different radial locations \(r/R\) along the local chord length for different azimuthal angles in the uniform inflow and TSR=9 case: (a)-(e) suction side, and (f)-(j) pressure side.

The effects of turbine tower can be observed on the suction side at \(\psi=180^\circ\), especially in the pressures near the leading edge of the airfoils. Figure 10 shows that the pressure distribution for the blade descending \((0^\circ < \psi < 180^\circ)\) is similar to the blade ascending \((180^\circ < \psi < 360^\circ)\) on the suction side of the blade for the inboard sections, where low pressure region is higher at or near \(\psi=0^\circ\) because of the higher free-stream velocity causing the higher angle of attack. The low pressure region near the leading edge of the blade in the outboard region is larger between \(180^\circ < \psi < 360^\circ\) compared to that of the smaller \(\psi\) \((0^\circ < \psi < 180^\circ)\), accounting for the asymmetrical force distributions in Figure 8. It is unclear what causes this difference, but one possibility is interference from the Cobra probes. It could be also because the blade interactions with the flow is different when ascending or descending. On the pressure side of the blade at different stations, the pressure distributions appear to be fairly symmetric for this case.

In order to characterize the wake flow behind the turbine, velocity distributions measured...
Figure 10: Phase averaged pressure distribution at different radial locations \((r/R)\) along the local chord length for different azimuthal angles in the shear inflow and TSR=5 case: (a)-(e) suction side, and (f)-(j) pressure side.

using PIV were used. The PIV measurements were in a vertical plane behind the turbine. Wake characteristics are shown in Figure 11 at \(x/D=1\) and 5 along with the turbine inflow velocities from Cobra probe. As shown in Figure 11 (a), for \(x/D=1\), TSR=9 cases have minimum velocities further outboard than TSR=5, which appear to occur inboard of the location where the measurements end. At \(x/D=5\), the wake data were only measured for the uniform inflow cases. It is shown that the velocity deficit is lower at \(x/D=5\) compared to that of the \(x/D=1\) because of the wake recovery (see Figure 11 (b)). The wake width also appears to be growing consistent with the deficit’s decrease. In the outboard region, the velocity deficit is still high for the TSR=9 case compared to that of the TSR=5 case. It is expected that these curves would cross at some inboard location as in Figure 11 (a), but the PIV measurements did not extend further inboard.

The turbulence in the wake provides further insight to wake development. Figure 11 (c) shows
that, for the both uniform and shear cases, the turbulence intensity created by the turbine (considering the discrepancy between the inflow and wake data) is higher in the tip region (around $r/R=1$) compared to that at smaller $r/R$, where the turbulence intensity is essentially the same as that of the inflow. In addition, the turbulence intensity is higher in the tip region at TSR=9 than at TSR=5 in both shear and uniform inflow cases, but the reverse is true in the mid-span of the blade. Figure 11 (c)-(d) shows that, for the uniform inflow case, the wake is characterized by higher turbulence intensity at $x/D=5$ compared to those at $x/D=1$, and the inflow due to the turbulence production and mixing in the wake is linked to the recovery of the axial velocity deficit. At $x/D=5$, the turbulence intensity is higher for TSR=9 case compared to that for the TSR=5 case for uniform inflow as shown in Figure 11 (d). This is explained by the stronger velocity gradient between the wake and the freestream flow due to the larger wake deficit in the TSR=9 case.

Understanding and explaining why the shapes of the mean velocity profile are different in
Figure 12: Flow-field velocities and blade load distributions for uniform inflow cases: (a) inflow velocity (symbols), blade local inflow axial (lines with circles) and wake velocity (lines) at $x/D=1$, (b) normalized velocity deficit, and (c) normalized normal force. Red indicates results from the TSR=5 case, and blue indicates those from the TSR=9 case.

Figure 11 (a) is somewhat difficult, and, as a result, the measured blade force distributions are considered. In order to examine the effects of blade loading on the wake velocity, the turbine inflow velocity from Cobra data, the blade inflow axial velocity from BEM, and the wake velocity data from PIV shown in Figure 12 (a) are considered. The velocity deficit (which is related to the momentum extracted) caused by the turbine blade is calculated by subtracting the axial wake velocity from the blade inflow axial velocity. Note that the blade inflow axial velocity ($U_{BIA}$) is calculated by applying induction factor effects (from BEM) to the Cobra probe data. The wake velocity deficit results are shown in Figure 12 (b). In the tip region ($r/R \approx 0.8$), the normalized normal force in Figure 12 (c) is higher for the uniform inflow, TSR=9 case compared to that of the TSR=5 case, which results in more energy extraction from the flow. As can be observed in Figure 12 (b), the axial velocity deficit in the outboard region is higher for TSR=9 case than for TSR=5 case. At smaller $r/R$, the normalized normal force for TSR=9 case drops, which explains the lower velocity deficit in the wake of TSR=9 case in the inboard region. In contrast, the uniform, TSR=5 case exhibits lower loads outboard and higher loads inboard, with a corresponding lower deficit outboard and higher deficit inboard. These results show the expected coupling between blade loading and velocity deficit and the impact of turbine operating condition (in this case the TSR) on these quantities.

5. Conclusion
Inflow and wake velocities as well as blade loads were captured for a 2 m diameter wind turbine that produced a wake with characteristics similar to that of a full-scale turbine. The measurements were analyzed to characterize the development and evolution of the wake. Pressure measurements used to calculate the blade loading were made with a tap/tubing/transducer system and were corrected to remove the tubing and the centrifugal effects. The blade load distributions were coupled with the inflow and wake velocities to understand the flow behavior in the wake. The results show a clear link between the measured blade loading and the observed wake structure.

The measurements taken in this initial study justify the extra effort required for inflow and distributed blade load measurements. Such measurements are critical to understanding the wake
behavior. Measuring only the wake or the wake with the integrated force values (e.g. thrust) limits the ability to interpret the wake velocity distributions. Additionally, unsteady pressure measurements are critical for characterizing the time-dependent loading on the blade. Although the unsteadiness in the present measurements were small, they were evident. Also, wake models will only capture the wake accurately if blade loads are predicted accurately first. This suggests that comparing modeled and experimental blade load results is critical to validation efforts.

Although this study has demonstrated the capability for flow-field and blade measurements at this scale, it is only a first effort in this area. This test capability offers the opportunity to further understand turbine wakes. For example, tests similar to those reported here at off-design conditions would be of interest due to the increased importance of unsteady blade aerodynamics and their impact on the wake. A study using multiple turbines with the downstream turbine blade instrumented would provide measurements of the unsteady loads caused by the wake. Clearly this capability opens the opportunity for a range of efforts that can increase our understanding of turbine wake behavior.

6. Acknowledgments
This work was supported by the U.S. Department of Energy, Office of Science, Basic Energy Sciences, under Award # DE-SC0012671.

References
[1] D.C. Maniaci, and J.W. Naughton. “V&V Integrated program planning for wind plant performance.” Sandia Report, SAND2019-6888. Sandia National Lab.(SNL-NM), Albuquerque, NM (United States), 2019.
[2] T.G. Trucano, P. Martin, and W.L. Oberkampf. “General concepts for experimental validation of ASCI code applications.” Sandia Report SAND2002-0341. Sandia National Lab.(SNL-NM), Albuquerque, NM (United States), 2002.
[3] A. Hassanzadeh, J.W. Naughton, C.L. Kelley, and D.C. Maniaci. “Wind turbine blade design for subscale testing.” Journal of Physics: Conference Series, vol. 753, no. 2, p. 022048. IOP Publishing, 2016.
[4] A. Hassanzadeh, and J.W. Naughton. “Design and analysis of small wind turbine blades with wakes similar to those of industrial scale turbines.” In APS Division of Fluid Dynamics Meeting Abstracts, 2016.
[5] G.V. Iungo, Y. Wu, F. Porté-Agel. “Field measurements of wind turbine wakes with Lidars.” Journal of Atmospheric and Oceanic Technology, 30 (2013): 274—287.
[6] M.L. Aitken, R.M. Banta, Y.L. Pichugina, and J.K. Lundquist. “Quantifying wind turbine wake characteristics from scanning remote sensor data,” Journal of Atmospheric and Oceanic Technology, 31 (2014): 765—787.
[7] S. Schreck. “The NREL full-scale wind tunnel experiment Introduction to the special issue.” Wind Energy 5, no. 2-3 (2002): 77-84.
[8] H. Snel, J. G. Schepers, and B. Montgomery. “The MEXICO project (model experiments in controlled conditions).” The database and first results of data processing and interpretation.” Journal of Physics: Conference Series, vol. 75, no. 1, p. 012014, IOP Publishing, 2007.
[9] G. Campanardi, D. Grassi, A. Zanotti, E.M. Nanos, F. Campagnolo, A. Croce, and C. L. Bottasso. “Stereo particle image velocimetry set up for measurements in the wake of scaled wind turbines.” Journal of Physics: Conference Series, vol. 882, no. 1, p. 012003, IOP Publishing, 2017.
[10] J. Schottler, F. Mühlle, J. Bartl, J. Peinke, M. S. Adaramola, L. Sastran, and M. Hölting. “Comparative study on the wake deflection behind yawed wind turbine models.” Journal of Physics: Conference Series, vol. 854, no. 1, p. 012032, IOP Publishing, 2017.
[11] I. Bayati, L. Bernini, A. Zanotti, M. Belloli, and A. Zasso. “Experimental investigation of the unsteady aerodynamics of FOWT through PIV and hot-wire wake measurements.” Journal of Physics: Conference Series, vol. 1037, no. 5, p. 052024, IOP Publishing, 2018.
[12] M. Bastankhah, and F. Porté-Agel. “A new miniature wind turbine for wind tunnel experiments. Part ii: Wake structure and flow dynamics.” Energies 10, no. 7 (2017): 923.
[13] W. Tian, A. Ozbay, W. Yuan, P. Sarakar P, and H. Hu. “An experimental study on the performances of wind turbines over complex terrain.” AIAA 2013-0612.
[14] A. Hassanzadeh, T. Harms, and J. W. Naughton. “Static and dynamic aerodynamic performance parameters for S814 and S825 airfoils at moderate Reynolds number.” AIAA 2019-0802.

15
[15] J. Strike, M. Hind, M. Saini, J. Naughton, M. Wilson, and S. Whitmore. “Unsteady surface pressure reconstruction on an oscillating airfoil using the wiener deconvolution method.” AIAA 2010-4799.

[16] M. Hind, P. Nikoneeyan, and J. W. Naughton. “Quantification of uncertainty in the correction of remotely measured unsteady pressure signals on pitching airfoils.” AIAA 2017-3733.

[17] H. W. Coleman and W. G. Steele. “Experimentation, Validation, and Uncertainty Analysis for Engineers.” 4th ed. Wiley, 2009.