Aeroelastic measurements and simulations of a small wind turbine operating in the built environment

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Abstract. Small wind turbines, when compared to large commercial scale wind turbines, often lag behind with respect to research investment, technological development, and experimental verification of design standards. In this study we assess the simplified load equations outlined in IEC 61400.2-2013 for use in determining fatigue loading of small wind turbine blades. We compare these calculated loads to fatigue damage cycles from both measured in-service operation, and aeroelastic modelling of a small 5 kW Aerogenesis wind turbine. Damage cycle ranges and corresponding stress ratios show good agreement when comparing both aeroelastic simulations and operational measurements. Loads calculated from simplified load equations were shown to significantly overpredict load ranges while underpredicting the occurrence of damage cycles per minute of operation by 89%. Due to the difficulty in measuring and acquiring operational loading, we recommend the use of aeroelastic modelling as a method of mitigating the over-conservative simplified load equation for fatigue loading.

1. Introduction

Small wind turbines are defined by IEC 61400.2-2013 as having a swept area less than 200 m² which corresponds to a blade diameter and power output less than 16 m and 50 kW respectively [1]. Small wind turbines have several significant operational differences compared to large wind turbines used for commercial scale generation, including, but not limited to: higher operational rotational speeds and tip speed ratios, the influence of low Reynolds number and high angle of attack aerodynamics (especially during rotor start-up), passive yaw control (typically achieved via a tail fin) which can result in high gyroscopic loads, and potential for siting within the built environment resulting in highly turbulent inlet flow and unsteady aerodynamics [2, 3, 4, 5]. These factors can lead to comparatively complex operational dynamics and service loading, which can be detrimental to fatigue critical components such as blades and drive shafts [6, 7, 8, 9].

While the large scale wind turbine industry has seen much research and development in recent history, the small wind turbine industry still lags behind in terms of research effort, particularly in regards to full scale experimental validation and quantification of operational loadings [10, 11, 12]. As a consequence, design codes are often based on potentially simplistic equations in combination with high safety factors, leading to over-conservative design cases. This creates a possible scenario whereby small wind developers may not be able to undertake certification as
per IEC 61400-2-2013 (henceforth referred to as ‘the standard’), or risk overdesigning components [13, 14]. According to the standard, design loads can be determined via three methods; experimental measurements, aeroelastic simulations, and the simplified load model (SLM) — with each method presenting various advantages and disadvantages.

While experimental in-service load measurement would provide the most accurate and reliable results, by very nature it can only be undertaken late in the product design cycle. Fully instrumenting a small wind turbine is no trivial task and designing an appropriate data acquisition system has been described by comparatively few studies studies [2, 3, 15]. Aeroelastic modelling has seen much use in commercial scale wind turbines, and comparatively limited use in small wind turbines, likely due to the high capital costs associated with software licensing (of which FAST is a notable exemption) and time cost associated with generating an appropriate model. Advantages of the SLM is that analysis of multiple designs can be undertaken rapidly with limited other resources, however this comes at the cost of higher safety factors and conservative design methodologies. To the best of the authors knowledge, these three methods have not been quantitatively compared and assessed for an operating blade.

In this study we aim to reduce the knowledge gap between small and large scale wind turbine blade fatigue loading, with regards to the acquisition of in-service blade load data, aeroelastic simulations, and comparison with the SLM. For this study in-service loads of a 5 kW Aerogenesis wind turbine will be acquired and compared to aeroelastic simulations using FAST (developed by NREL) [16]. Of particular interest will be the quantification of fatigue loads to which small wind turbine blades are subject to during service, with the fatigue loading spectra of all three methodologies compared. It should be noted that the effects of material properties will not be considered. Discussions and conclusions will be given in light of the standard, with aims in reducing aspects of the over-conservative simple load model.

2. Experimental equipment and data acquisition
The small wind turbine for this study is an Aerogenesis 5 kW upwind horizontal-axis turbine installed on campus at The University of Newcastle, Australia. The dual-bladed turbine has a rotor diameter of 5 m, with yaw controlled passively via a tail fin. This wind turbine has a cut-in wind speed of 3.5 m s\(^{-1}\), and a rated speed of 10.5 m s\(^{-1}\) corresponding to a rotor speed of 320 rpm, and a tip speed ratio, \(\lambda = 8\). The turbine was developed nominally as a Class III turbine with respect to the standard, which specifies a average hub-height wind speed, \(V_{\text{ave}} = 7.5\) m s\(^{-1}\), a design wind speed, \(V_{\text{design}} = 10.5\) m s\(^{-1}\), and a turbulence intensity of, \(I = 18\%\). The turbine is mounted on a 18 m monopole tower and utilises a self-excited induction generator. The test site is geographically located within the built environment, with turbulence intensity measured in excess of those specified by the standard for a turbine of this class.

In order to measure the operational loadings, a variety of instruments were installed on the turbine at the test site (Figure 1). Site wind conditions were logged at a height of 15 m via an anemometer and wind vane installed on a boom arm offset from the tower. Other instrumentation installed on the turbine includes; a shaft rotational speed sensor, generator output power logging, tower-top accelerometers (to measure fore-aft and side-side response), an optical yaw compass (to determine turbine heading), and a tail-fin moment sensor. These instruments will not all be detailed here as some are described in further studies [17].

One blade on the wind turbine was instrumented with seven uni-directional strain gauges at increments of 250 mm radially along the blade. These were located on the pressure side of the
aerofoil, with lead wires running internally throughout the blade to mitigate any aerodynamic derating of blade performance caused by surface discontinuities. Strain signals from the blade were recorded at a rate of 500 Hz and logged to a micro SD card located within the wind turbine hub. This relatively high sample rate was necessary due to the high operational rotational speed of the rotor and the corresponding expected complex aeroelastic response of the blade during yawing. For example, at rated rotor speed of 320 rpm, the sample rate of 500 Hz would allow for one sample to be acquired every $\sim 4^\circ$ azimuthally.

Initial strain readings were then ‘corrected’ for cross-talk effects of centrifugal strain which acts radially along the blade. In practice this is a difficult task to undertake, as centrifugal effects can be significant due to the high rotational speed and the squared relationship with centrifugal force [18]. In order to undertake this process, a finite element (FE) model of the blade incorporating anisotropic composite material properties was constructed. Virtual strain gauges were placed in locations identical to those on the blade. Static calibration using point masses show good correlation between the physical blade and modelled blade for both strain response and net blade deflections. The FE blade was simulated rotating at various speeds ranging from 0 to 400 rpm at increments of 50 rpm. The strain response from the FE gauges were then used to subtract any cross-talk effects from the in-service blade measurements.

The resulting strain readings from the seven blade channels were then equated to an equivalent bending moment (found via static calibration). A third order polynomial fit was applied to the seven radially distributed bending moment data points, and then used to back calculate the
bending moment at the blade root. Readings were only considered in the flapwise direction due to the relatively stiff lead-lag direction and minimal respective strain. The flapwise direction was deemed to be of most interest for fatigue purposes due to the low sectional stiffness and higher root moment when compared to the lead-lag direction (i.e. failure is likely to occur in this region first). Final post-processed output includes a time series log of wind speed, direction, turbine heading, and blade flapwise moment. Here we present a sample of the blade moment output data set used for further analysis in this study (Figure 2), in particular we note the large amplitude gyroscopic excursions present during high yaw events compared to the lower amplitude effects during normal operation.

Figure 2. Example of measured blade flapwise moment signal showing large amplitudes (a) due to high yaw rate gyroscopic effects (b). Time scale is arbitrary.

3. Aeroelastic modelling and simulations
An aeroelastic model of the wind turbine had been developed within FAST in order to compare results of experimentally measured blade loads and to assess the suitability for predicting blade fatigue damage. While a detailed explanation of aeroelastic theory is beyond the scope of this paper, aeroelastic modelling can be described as a coupling of aerodynamic forces with the structural elastic and inertial response of a turbine system (i.e. inlet wind conditions, blades and tower response). Other turbine dynamics such as yaw misalignment/rate, tail fin effects, generator performance, and tower motions can also be included within FAST.

In order to account for aerodynamic effects, parameters for the blade were input into FAST, including; radial chord and twist distribution, and aerofoil type. For the Aerogenesis blade a
constant SD7062 aerofoil is used for the whole aerodynamic section; purpose designed for low Reynolds number performance such as during starting or low wind speed operation. Lift and drag data was obtained for the linear region of aerofoil operation [19], which was then extrapolated for a full \( \pm 180^\circ \) of operation using the Viterna equations [20]. These large excursions from the linear lift region have the potential to occur during rotor start-up or yaw events. Aerodynamic properties (i.e. \( C_L \) & \( C_D \)) for a delta-wing tail fin [21] were also input in a similar manner to the blades. This was included to account for tail fin effects which are used for passive yaw control of the turbine. Within passive yaw control, FAST determines the nacelle angular response about the yaw axis due to: inlet wind direction, yaw rate, nacelle inertia, and the tail fin moment due to the relative wind velocity at the tail fin (which incorporates downstream rotor wake effects).

Structural aspects of the wind turbine included in the FAST model were its blades and the tower, which were both constrained as fixed cantilevered beams at the hub and ground respectively. The blades are manufactured from glass fibre reinforced polymer (GFRP) which by nature are anisotropic. FAST represents the blades as a series of beam elements with a constant sectional stiffness (flexural rigidity), density, and principal stiffness angle. PreComp [22] was utilised to produce these properties for the Aerogenesis blade using GFRP design data as input. FAST also requires mode shapes for the first two flapwise modes and the first lead-lag mode. The first two flapwise mode shapes were determined from the aforementioned FE blade model as 8.40, and 20.02 Hz respectively. The first lead-lag mode was found to be in excess of 40 Hz and was not an input for this model with the lead-lag degree of freedom not used. It should be noted that the lead-lag stiffness was calculated to be significantly greater than the flapwise stiffness, with this degree of freedom assumed to dominate. In a similar manner to the blades, the tower sectional properties were also input. The tower consists of a tapered octagonal section (AS 4100 structural steel) throughout a nominal tower height of 18 m. The first two fore-aft and side-side modes were calculated as 0.87 and 4.87 Hz respectively and with respective mode shapes determined including the lumped mass effect of the tower-top static turbine mass load.

Other parameters key to turbine operation including, rotor inertia, nacelle inertia, rotor overhang, and tail fin boom length, were input into the FAST model. Also critical to the turbine’s operation are the generator effects and rotor speed control algorithm. The Aerogenesis wind turbine utilises a self-excited induction generator (SEIG) which is essentially an induction motor operated in reverse as an induction generator [23]. In this case a shunt capacitor is connected in a delta configuration to facilitate self-exciting and variable-speed power production. Maximum power point tracking (MPPT) is a control methodology which aims to extract the maximum amount of power from a given inlet wind condition. Practically this is achieved in the Aerogenesis turbine by adjusting the generator load in such a way that the rotor is maintained at, or close to, its design tip speed ratio. It should be noted that FAST does not natively support the use of a SEIG and associated MPPT variable speed control, however, it allows for interfacing with generator and control models developed Simulink. A Simulink model of the Aerogenesis turbine generator and control system was developed within the University of Newcastle, Australia [17]. The generator current is controlled via a PI controller, with the stator current set-point determined from a MPPT curve based on a design tip speed ratio of 8. As this is an induction generator, power production does not occur until the shaft speed exceeds 1,440 rpm (synchronous speed), so wind speeds below this shaft speed are not controlled. The MPPT curve was produced for all wind speeds (above synchronous speed for a tip speed ratio of 8) based on steady-state operating performance. While the effects of power output and generator response are not within the scope of this study, the influence of the variable speed control provided by the generator and control system will impact on the rotor operation and hence blade loadings [24].
As the main turbine model has been classified and described, it is necessary to consider the input wind series for simulation purposes. A ten minute time series wind speed data set was produced within TurbSim using the Von Karman turbulence model [25]. This series is equivalent to an IEC 61400.2 Class III small wind turbine, and has a mean wind speed, $V_{ave} = 7.50 \text{ ms}^{-1}$, and a turbulence intensity of, $I = 18\%$. This wind data set was produced to represent the initial design stage of a small wind turbine of this class, whereby the designer does not have access to measured site wind data or turbulence intensities. This ten minute wind series was also expected to take a significant amount of computational time (in excess of 120 hours) to execute within FAST. While FAST itself computes comparatively rapidly, the use of a fixed time-step solver scheme and algebraic loops within the Simulink SEIG model introduces substantially longer solve times. However, without modifying and recompiling the FAST source code, this is unavoidable when required to capture the effects of variable turbine speed control.

4. IEC simplified load equations
The only fatigue case specified in the standard, Load case A: Normal operation, is detailed in Section 7.4.2 and Annex F of the standard. Only peak-to-peak load ranges are considered for the blade and drive shaft, where the operational loads are said to vary from 0.5 to 1.5 of the design parameters. For loading at the blade root in the flapwise direction (previously found to be the least stiff and most stressed region), this can be found via Equation 1 as follows:

$$\Delta M_{yB} = \frac{\lambda_{design} Q_{design} B}{B}$$

Where $\Delta M_{yB}$ is the flapwise root bending moment range, $\lambda_{design}$ is the design tip speed ratio, $Q_{design}$ is the rotor torque at the design wind speed ($1.4V_{ave} = 10.5 \text{ ms}^{-1}$), and $B$ is the number of blades. For the Aerogenesis wind turbine this moment range was calculated as 952 Nm, with a respective moment ratio (analogous to stress ratio), $R = M_{yB_{min}}/M_{yB_{max}} = 0.5/1.5 = 0.33$. This calculated range is to be applied for $352.26 \times 10^6$ fatigue cycles which is equal to a design life of ten years.

It should be noted that this study compares only the operational blade root moment in isolation of centrifugal related effects (to facilitate comparison with measurements and aeroelastic simulations). Future work is expected to investigate the effects of combination loading with centrifugal effects and assessing the developed stresses within material located at the blade root.

5. Results and discussion
Several measurement campaigns were undertaken at the wind turbine site to acquire operational blade data. Due to the nature of the acquisition system and the high frequency sample rate, acquisition time periods were limited to less than one hour in duration. Seven data files (nominally titled A to G) were selected for further analysis as these corresponded most closely to the Class III average wind speed (Table 1), we also note a higher turbulence intensity present at site.

Azimuthal averaging [2, 3] of the blade flapwise root bending moment signal was undertaken in order to isolate turbine operation to a comparatively ‘steady state’ condition where mean wind speed was measured at 10 ms$^{-1}$, and yaw error and yaw rate were limited to ±5° and ±2.5°s$^{-1}$ respectively. The blade response for these conditions were averaged in the azimuthal domain.
### Table 1. Statistics for aeroelastic simulations and measured data files

| Data file | Length (min) | $V_{\text{ave}}$ (ms$^{-1}$) | $V_{\text{max}}$ (ms$^{-1}$) | $V_{\text{min}}$ (ms$^{-1}$) | $I$ | N cycles | Cycles/min |
|-----------|--------------|-----------------------------|-----------------------------|-----------------------------|----|-----------|------------|
| Aeroelastic | 10           | 7.50                        | 12.53                       | 2.80                        | 18%| 4,582     | 458        |
| A - Measured | 41           | 8.05                        | 16.70                       | 1.10                        | 27%| 28,877    | 704        |
| B - Measured | 47           | 6.85                        | 14.40                       | 2.20                        | 26%| 32,159    | 684        |
| C - Measured | 22           | 7.88                        | 15.99                       | 2.40                        | 28%| 13,969    | 623        |
| D - Measured | 34           | 5.87                        | 12.80                       | 1.00                        | 30%| 20,224    | 588        |
| E - Measured | 51           | 7.26                        | 15.20                       | 1.60                        | 28%| 30,355    | 593        |
| F - Measured | 45           | 9.86                        | 22.00                       | 2.50                        | 30%| 31,735    | 710        |
| G - Measured | 26           | 10.33                       | 21.00                       | 3.60                        | 29%| 18,454    | 711        |

Figure 3. Comparison of azimuthally averaged blade flapwise root bending moment for measured data and aeroelastic simulations. The following operational limits were applied; mean wind speed = 10 ms$^{-1}$, yaw error = ±5°, and yaw rate = ±2.5°s$^{-1}$. Note 90° is defined as vertically upwards blade position.

To isolate any high-frequency/short-term phenomena, while preserving the long-term blade response indicative of ‘steady’ operation (Figure 3). In order to develop ‘user confidence’ in the model, this was then compared to the blade flapwise bending moment determined from loads via aeroelastic simulations. Aeroelastic simulations tended to slightly over-predict the azimuthal measurements by +0.90% to +9.90%, representing comparatively good agreement for operation at rated conditions. It is also necessary to compare blade response in the frequency domain (achieved via a power spectral density plot) to ensure that the broad operational dynamics of the turbine are captured. We note a discrepancy in the once per cycle operational frequency (i.e. 1P) of the rotor and respective blade response when comparing aeroelastic simulations and field measurements (shown in Figure 4). Several possible reasons for this include the fact that the look-up table used in the Simulink MPPT control scheme was derived from steady-state operation [23], meaning that the generator response is controlled more aggressively in order to maintain the optimal steady-state design tip speed ratio within the transient aeroelastic simula-
Figure 4. Comparison of measured (file A) and simulated blade flapwise bending moment in time domain (a), and frequency domain PSD (b). Note a shift in 1P response due to control discrepancies, and 3P response due likely due to a blade imbalance.

Due to the complex nature of the blade loading time-series signal, we introduce the usage of rainflow counting to determine the equivalent fatigue damage cycle ranges and stress ratios from a given input signal. The methodology is beyond the scope of this paper, with details and applications to wind turbines found in [26]. Blade bending moment response from both FAST simulations and site measurements was rainflow counted to determine damage cycles. After rainflow counting, any cycle ranges less than 5 Nm were rejected, and the resulting cycles were ranked in order from highest range to smallest. The number of counted cycles produced was also output from this process, whereby aeroelastic simulations tended to under-predict the number of damage causing cycles per minute of operation by 30% (when comparing the mean value of file A to G, 659 cycles/min). It should be noted that when comparing the number of cycles/min calculated via the standard (67 per minute), a significant underprediction of 89% occurs.

The results from the simplified load equations, aeroelastic simulations, and measured data are compared in Figure 5, where the flapwise bending moment range is plotted in descending
The number of counted cycles was normalised to ensure a valid comparison despite the difference in time duration between the measured and simulated data sets. We note a good visual agreement with both measured data sets and aeroelastic simulations. The bending moment ratio (equivalent to a stress ratio in the absence of material properties) is defined as the ratio of the minimum value to maximum value of a given cycle was plotted for the corresponding damage cycle. For this moment ratio plot, a moving point average was taken using a window equal to 1% of the data set in order to show trends and remove outliers. We note a strong correlation with high moment range and worsening stress ratio.

![Figure 5](image_url)

**Figure 5.** A comparison of the SLM with the measured and aeroelastic simulated data sets for blade flapwise bending moment. Rainflow counted and descending ranked damage cycles ranges (a), and corresponding mean stress ratios (b).

### 6. Conclusions and recommendations

In this study we compare the simple load model specified in the standard for fatigue life formulation with both aeroelastic simulations and in-service load measurements. While large, commercial-scale wind turbines have been the subject of much research effort regarding load measurement and aeroelastic verification, the authors are not aware of any previous studies where all three methods of assessing loadings on small wind turbine blades have been compared simultaneously. Several preliminary conclusions can be drawn from these results as follows.

Rainflow counted and ranked damage cycles produced via aeroelastic simulations show very good agreement with field measurements acquired in similar inlet wind conditions. We note that
aeroelastic simulations appear to overpredict measured damage cycle range magnitudes. While further investigation is required, we suggest several reasons for this, including: higher wind turbulence present in the measured data, limitations in the aerodynamic inflow model when the rotor is operating at large yaw misalignments (>20°), blade/tower interaction effects, and lack of blade torsional degree of freedom in FAST which may impact blade response and loadings.

Experimental measurements and aeroelastic simulations show that a majority (~90%) of the rainflow counted damage cycles consist of small, low-amplitude and low stress ratio aberrations. These are likely due to effects such as wind shear, wind turbulence, and vibrations induced by structural dynamics of the wider system (i.e. blade and tower vibrations). A small percentage (<10%) of large cycles are due to gyroscopic effects which are a function of both rotor rpm, yaw rate, and blade inertia. While blade inertia is typically low compare to large wind turbines, small wind turbines usually operate at much higher rotational speeds and high uncontrolled yaw rates due to tail fin action. Minimising large yaw rates while efficiently tracking changes in wind direction is therefore key to reducing blade fatigue damage. Future work of this study is aimed at the extrapolation of measured loads to design conditions.

When considering fatigue of small wind turbine blades, fatigue in the flapwise direction is often most critical due to the comparatively low sectional stiffness and high aerodynamic moment, when compared to the lead-lag direction. The simple load model for flapwise fatigue loading of the blade significantly overpredicts the actual in-service fatigue load cycle ranges, while underpredicting the number of damage cycles by 89%. From both aeroelastic simulations and field measurements, it is highly unlikely that damage cycles ranging from 0.5-1.5 rated loading would occur. This load regime can therefore be considered overly conservative, particularly if considered for the entire design life of the turbine. The authors therefore recommend the use of aeroelastic modelling for determining damage cycles used in fatigue life calculations, as a means to mitigate the overly conservative standard. Despite the potential cost and time investment, there is significant scope to reduce the calculated fatigue load magnitude while still being conservative when compared to experimental measurements — especially if these field measurements are to be conducted late in the product development cycle after blade production occurs.

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