Practical Considerations on Wind Turbine Blade Leading Edge Erosion Modelling and its Impact on Performance and Loads

F Papi\(^1\), G Ferrara\(^1\), A Bianchini\(^1\)

\(^1\) Department of Industrial Engineering, Università degli Studi di Firenze, Via di Santa Marta 3, 50139, Firenze, Italy.

Corresponding author: alessandro.bianchini@unifi.it

Abstract. Wind turbines operate in all sorts of weather conditions around the globe, exploiting installation sites with high power production potential. These machines operate within the atmospheric boundary layer and are therefore subject to impacts with rain, hail dust particles and insects, often leading to rapid blade deterioration and performance drops. This work aims to explore different ways of modelling wind turbine blade leading edge erosion with a medium-fidelity approach and to show how different models impact performance and loads. This provides a quick way of evaluating the effects of blade deterioration. A 2D airfoil erosion model is developed and lift and drag coefficients are calculated with Computational Fluid Dynamics (CFD). An aero-servo-elastic model of the DTU 10MW Reference Wind Turbine (RWT) is then used to evaluate the impacts of the models on performance. Differences in airfoil performance are discussed as well as differences in the turbine’s power coefficient and fatigue loading levels.

1. Introduction

In challenging weather conditions, wind turbine blades can deteriorate in a matter of years [1]. Repeated impacts of airborne particles initially increase the surface roughness of the leading edge (LE) with the formation of small pits. If left to the elements, these pits will expand into deeper gauges and eventually cause the LE paint to crack and to delaminate [2]. Leading edge protection tapes or paints are being developed and are offered by some manufacturers to improve LE resistance [3]. Despite these efforts, recent examples of extreme blade deterioration can be found [4], indicating that further research on the topic is needed. To study this phenomenon accurately, full scale test benches or full-scale CFD models would be required. These approaches are either extremely costly or computationally prohibitive. This work proposes a medium-fidelity method for a quick evaluation of erosion-related performance loss. This is done first through the development of a 2-D LE erosion model. Studying these issues numerically is challenging because of the scales that have to be resolved: these range from blade surface roughness all the way to the rotor diameter that can exceed 200 meters. For this reason, airfoil degradation is evaluated through 2D CFD, the impacts of this damage are then analyzed with full scale, BEM-based aero-servo-elastic model. Other authors have approached the problem of wind turbine blade erosion with similar methods, amongst which are Cavazzini et al. [5], Ashuri et al. [6], without however focusing on structural loads. The scopes of this work are to test different airfoil LE shape modifications that mimic erosion-induced wear patterns, and to showcase the range of variability in performance and in fatigue loads that is predicted using a multidisciplinary approach. To this end, a blade-LE damage model is developed. Various LE-damage parametrizations are tested and for each model multiple levels of deterioration are evaluated. The DTU 10MW RWT [7] is used as a testcase to compare variations in Loads and AEP, as it is considered a state of the art reference rotor. Decreases in airfoil performance
and turbine performance are discussed, as well as reductions in annual energy production (AEP) for this testcase. Finally, the effect of the various erosion parametrizations on fatigue loading of some of the turbine’s main components is investigated. Fatigue loading in such a scenario is typically not considered a concern as loads are expected to decrease with performance. As this study will show, this is found to be mostly true, although in some extreme cases negative aerodynamic damping can arise leading to unexpected load-trends.

2. Methods

In this section the erosion model is briefly described and the numerical setup, used to calculate the lift and drag coefficients, is presented. The various aspects of the aero-servo-elastic model of the DTU 10MW RWT are then discussed.

2.1. Erosion Model

Modelling wind turbine LE erosion involves accounting for pits, gouges and delamination [2]. A 2D model cannot account for these effects separately as they are inherently 3D and therefore all of them are accounted for together. The formation of pits and gouges is modelled by increasing the surface roughness on the blade nose, while delamination is modelled by changing the airfoil’s shape. As the focus of the work is to study the impact of different ways of modelling delamination, an equivalent sand grain roughness of 0.4 mm is used in all testcases. This reflects the maximum depths found by [8]. The depth of the LE delamination $\Theta$ and the coverage on the blade nose $\varepsilon$ are the main parameters that determine the shape of the eroded airfoil. Upon examining the erosion patterns used by other authors [2,9], a correlation between $\Theta$ and $\varepsilon$ can be seen, as shown in fig. 1. An additional correlation is proposed as the average between the literature curves. If we choose one of the $\varepsilon$-$\Theta$ correlations, the LE shape depends only on the coverage, which we can now define as erosion factor $\varepsilon$.

Similarly to other numerical models [10], the nose of the blade was flattened and the height of the flattened region $h_{LE}$ was assumed to be two times the erosion depth $\Theta$. The height of the delamination end $D_{end}$ is defined as:

$$D_{end} = \frac{\Theta}{k}$$

Where $k$ is an arbitrary integer. The nose of the blade and the end of the delaminated area are connected with a quadratic function.

Figure 1. LE $\varepsilon$-$\Theta$ correlation

Figure 2: LE erosion model (a). Max. erosion depth $\Theta$, chordwise erosion extent $\varepsilon$, flat LE height $h_{LE}$, min. erosion depth $D_{end}$ Erosion model comparison for $\varepsilon$10 (b).
The erosion coverage on the pressure side of the airfoil is assumed to be 1.3 times the coverage on the suction side. The reason for this is quite obvious as turbine blades are designed to operate with positive angles of attack and thus the pressure side of the airfoil is most affected by particle impacts [2]. The main parameters in the developed LE erosion model are shown in fig. 2a. Three different models were tested: the first with $\varepsilon-\Theta$ according to Sareen [2] and $k = 2$ to model heavy erosion, the second with $\varepsilon-\Theta$ according to the mean curve and $k = 3$ to model normal erosion and finally with $\varepsilon-\Theta$ according to the mean curve and $k = 3$ with a smooth transition between the delaminated area and the main part of the airfoil to model mild erosion. These choices are based on the variety of approaches to the problem that can be found by examining existing literature: while some authors use a smoother approach [11,12], others prefer to model the phenomenon with abrupt geometry changes [5,10]. Each model was tested with different $\varepsilon$ levels to reproduce different stages of the erosion process. A comparison between the models can be seen in fig. 2b.

2.2. Numerical polar calculation

The lift ($C_l$) and drag ($C_d$) coefficients of the damaged airfoils were calculated by means of a 2D CFD approach using the ANSYS® FLUENT® solver. CFD was used despite the high computational costs to correctly predict the effects of the abrupt changes in the airfoil’s geometry caused by the erosion model. Such changes are caused by cracking and chipping of the external protective layers. In fact, simple panel methods like the ones found in Xfoil were found to be unreliable for the application. To this end an Unsteady Reynolds-Averaged Navier Stokes (U-RANS) pressure formulation solved in a coupled manner approach was used. Turbulence closure was achieved using the $k-\omega$ SST turbulence model. A second order upwind numerical scheme was used for spatial discretization and a second order bounded scheme for temporal discretization. The numerical domain is shown in fig. 3a, it is shaped to model open field conditions and has a length of 74C.

Numerical discretization is done with an unstructured mesh of approximately $3.7*10^5$ elements with 750 prismatic cells to discretize the airfoil surface. This relatively high number of elements was necessary despite the increased computational costs to ensure grid independence as found by the authors in a preliminary analysis. The numerical grid around the DU96W-180 foil is shown in fig. 3b.

Two airfoils were simulated to evaluate the different erosion patterns; the DU96W-180 airfoil, for which experimental $C_l$ and $C_d$ are available [2] and the FFAW3-241 airfoil, which constitutes the most outer part of the DTU 10MW RWT blade. Both of the airfoils were tested at Reynolds number of
1.85*10^6 with a chord length of 0.457m in order to match the conditions that were tested experimentally, as well as with a chord of 3.5m and Reynolds number of 1.2*10^7, to match the operating conditions on the DTU 10WM RWT. Once CFD data were obtained, the final 360-degree polars for use in the aeroelastic study were obtained by means of the Viterna-Corrigan extrapolation [13] for AoAs below -20° and above 30°. These limits were chosen as they represent the operating range of the undamaged turbine, as seen from preliminary runs. The numerical set-up tested against experimental data provided by Sareen et al. [2] showing good agreement (fig. 4). The erosion pattern applied to the numerical model in fig. 4 is the heavy erosion pattern with ε = 6.1 to match the experimental set-up.

2.3. Aeroelastic model

Using the eroded profiles an aero-servo-elastic model of the DTU 10 MW RWT was set-up in OpenFAST [14], a state-of-the-art code for the simulation of HAWTs. The aerodynamic model is based on a Blade-Element-Momentum (BEM) formulation [15]. Simply put, the effects of induction on the flow used by the turbine’s wake is calculated via a momentum balance on the turbine’s rotor disk, while the blades are modelled with 2D polars. To this end the rotor disk is divided in a series of concentric annuli. For each annulus the blade characteristics are considered constant and a momentum balance is performed. The resulting balance yields the angle of attack (AoA) and blade forces acting on the structure. The model is also corrected to account for variable induction caused by wind shear, yaw misalignment, turbulence and dynamic stall. The structural model, ElastoDyn [16], features a mixed modal and multibody formulation and can account for blade flap-wise and edge-wise and tore fore-aft and side-side deformation. The Delft Research Controller (DRC) [17] baseline controller is used to perform the load calculations in the study. This is an open-source wind turbine baseline controller that can be set-up to control most HAWTs. The variable speed controller is able to regulate torque and pitch. The controller parameters were tuned based on the specifications in [18].

2.4. Model set-up & data postprocessing

The DTU 10MW RWT [7] is a wind turbine designed by the Danish Technical University (DTU) as a reference to test aeroelastic codes and other design tools. The size of the turbine, the materials used and construction techniques are intended to be representative of modern design trends and sizes. The main parameters of this turbine are specified in Tab. 1. In fig. 5 the turbine and the reference systems used are shown. Erosion was applied uniformly to the outer part of the DTU 10MW RWT, from 70% of the rotor radius outward, by modifying the input polar data of the FFAW3-241 airfoil. Inboard sections of the blades were left unchanged as erosion is more pronounced where tangential velocities are highest, i.e. the outer parts of the rotor [10]. According to widespread industry knowledge, erosion severity ramps up gradually towards the blade tips; this effect was not considered herein and the same erosion levels were applied to the entire affected area. While this will most likely result in overestimating the AEP losses and can be then interpreted as a worst-case scenario, it allows for comparison of the different blade damage shapes. The structural properties of the blades are also left unchanged, as the erosion process only impact the outer layers of the blade nose only with little impact on the structural dynamics. The DTU 10MW RWT is simulated in environmental conditions defined by the IEC 61400-1 [19] international standard. For each damage level, sixty-six simulations are performed, including turbulence, yaw misalignment, vertical up-flow and wind-shear. Six 10-minute simulations are performed for each wind speed with different yaw angle and turbulent wind fields, complying with the IEC 61400-1 minimum requirements. The main parameters are summarized in Tab. 2. The airfoils are simulated up to an erosion factor of 10 for all the erosion models. However, it is not appropriate in case of the DTU 10MW RWT to account for erosion factors of up to 7.5 for the mild and normal erosion models and up to 5 for the heavy erosion model. This is due to the fact that the turbine is representative of the modern, lightweight construction techniques and higher erosion levels would render the assumption of keeping the structural properties unchanged unrealistic. The calculated loads were post-processed with an in-house Python tool to calculate Damage Equivalent fatigue loads (DELs) [20] and extrapolate extreme loads.
### Table 1: DTU 10MW RWT main parameters

| Parameter                      | DTU 10 MW RWT |
|-------------------------------|---------------|
| Hub height/Rotor diameter     | 119 / 178 m   |
| Rotor mass                    | 221 t (41 t per blade) |
| Rated power/wind speed        | 10 MW / 11.4 m/s |
| Cut-in/cut-out wind speed     | 4-25 m/s      |
| Rotational speed              | 9.6 rpm       |
| Rotor tilt/cone               | 5°, 2.5°      |
| IEC Class                     | 1 A           |

### Table 2: Main simulation parameters

| Parameter                     | Aeroelastic simulations |
|-------------------------------|-------------------------|
| Mean $V_{hub}$                | 4-24 m/s increments of 2 m/s |
| Wind model                    | IEC NTM                 |
| Shear exponent                | 0.2                     |
| Elasticity                    | yes                     |
| Nacelle yaw                   | -8°, 0°, 8°             |
| Upflow                        | 8°                      |
| Controller                    | DRC Controller          |
| wind bins / sims. per bin     | 11 / 6                  |
| Sim. length / time step       | 600 s / 0.04 s          |

3. Airfoil Performance

Differences in the airfoil’s performance are what ultimately influence the aeroelastic results in a BEM-based code. In general, as the erosion factor increases, $C_l$ decreases while $C_d$ increases. In fig. 6a and 6b the $C_l$ of the both clean and damaged FFAW3-241 and DU99W-180 airfoils are shown for $Re$ of 1.85*10⁶ and Re 1.2*10⁷. As shown in fig. 6a for the FFAW3 family airfoil, maximum lift is lower for all the erosion levels at 1.85*10⁶ Re. As for the relative differences; maximum $C_l$ decreases 28.15% for the $\varepsilon = 5$ case and 50.5% for the $\varepsilon = 10$ case at 1.85*10⁶ Re, while at 1.2*10⁷ Re these values are higher, with decreases in maximum $C_l$ of 29.2% and 51.6%. The stall angle also decreases: from 12.3° in the $\varepsilon = 0$ case to 7° in the $\varepsilon = 10$ case at 1.85*10⁶ Re and from 14.9° to 7.5° at 1.2*10⁷ Re. In relative terms, the stall angle is 43% smaller for the former case and 49% smaller in the latter. The DU96W-180 shows similar results (fig. 6c): maximum $C_l$ decreases 35% at $Re$ = 1.85*10⁶ and 38% at $Re$ = 1.2*10⁷, with stall occurring 31% earlier in the former case and 38% earlier in the latter.

On the other hand, increases in $C_d$ are more pronounced at $Re$ = 1.85*10⁶ for both airfoils. For instance, the FFAW3-241 foil exhibits a 70% increase in $C_d$ at zero incidence as opposed to a 68% increase at $Re$ = 1.2*10⁷, as shown in fig. 6b. In absolute terms however, the decreases in $C_l$ are more than one order of magnitude larger than the ones in $C_d$. If we couple this with the fact that the stall angle decreases more in the $Re$ = 1.85*10⁶ tests, we can argue that erosion has heavier influence on performance at higher Re numbers and thus on large HAWTs. It’s also worth highlighting that the FFAW3-241 airfoil shows greater lift drops when the same erosion pattern is applied then the DU96W-180. This latter airfoil indeed seems to be more resistant to early onset of stall.

The lift coefficient of the FFAW3-241 airfoil with the various erosion models and levels is shown in fig. 6d. For the mild erosion model the decreases in performance are noticeable only around stall, while for the normal and heavy erosion model especially, decreases are large even in the linear region. In particular, in the case of the heavy erosion model, the decreases in performance on the FFAW3-241 are massive with maximum $C_l$ before stall of only around 0.6 for the $\varepsilon = 10$ case: the abrupt geometry variation causes the flow on the suction side of the airfoil to separate at very low AoAs.
Figure 6: Lift (a) and drag (b) coefficients of the FFAW3-241 and lift coefficient of the DU96W180 (c) airfoils at different Re numbers for the normal erosion model. Lift coefficient for the FFAW3-241 airfoil at Re = $1.2 \times 10^7$ for the normal erosion model at various $\varepsilon$ values (d).

Figure 7: Average power per wind speed bin and standard deviation (top) and average power difference (bottom) for the mild (a,b), normal (c,d) and heavy (e,f) erosion models.

4. Aeroelastic performance

Undoubtedly power output is a crucial figure for plant operators and turbine manufacturers alike. In fig. 7 the power output of the DTU 10MW RWT is shown for all of the tested erosion levels and models. For the mild erosion model decreases in performance are limited. Average power decreases of 1.2% at 4 m/s and 2.5% at 10 m/s at the highest damage level. The normal erosion model causes peak decreases in performance of around 6% at 10 m/s for $\varepsilon = 5$, while the drops in performance at $\varepsilon = 7.5$ are over 19% at 10 m/s average wind speeds. For these wind speeds average AoAs for the eroded case are highest (fig. 8), where the differences in $C_l$ from the reference are greatest (fig 6d). Finally, in case of the high erosion model, average power decreases massively: over 33% at 10 m/s. At 16 m/s average wind speed and above, the performance of all the tested cases is back on par with the reference case because the
controller is able to compensate for the decrease in airfoil efficiency by reducing the amount the blades are pitched out to curtail power.

4.1. Annual energy production

Annual Energy Production (AEP) was calculated using a Raileigh wind speed distribution with average wind speed of 8.5 m/s. This corresponds to a mean wind speed distribution of class IIA according to IEC 61400-1 [19], representing sites of high turbulence intensity and average mean wind speeds. AEP losses vary greatly from model to model. For the mild erosion model they are quite small: below 1% for $\varepsilon = 5$ and around 1.2% for $\varepsilon = 7.5$. The normal erosion model shows AEP reductions 2.8% for $\varepsilon = 5$ and over 10% for $\varepsilon = 7.5$. The heavy erosion presents losses of around 19% for $\varepsilon = 5$. These values should be intended as an upper limit to AEP decreases as no gradient in the erosion severity along the affected blade span is considered.

Figure 8: Average AoA per wind speed bin at 75% of the rotor span with STD for the normal erosion model.

Figure 9: Average, max. and min. AoA for the heavy erosion model at 10 m/s average wind speeds with STDs (filled areas).

Figure 10: Normalized lifetime DELs for the mild (a), normal (b) and heavy (c) erosion models.

5. Fatigue Loads

Damage Equivalent Loads (DELs) are a common way of comparing fatigue loads in the industry. They are derived based on simple principles in the field of fatigue. Extensive explanations on how these metrics are defined can be found in [20,21]. 1Hz DELs are calculated for each aeroelastic simulation considering an equivalent number of cycles equal to the simulation length, hence the “1Hz” name. Lifetime DELs on the other hand, estimate an equivalent loading level for the entire turbine lifetime, in this case 20 years. A Raileigh wind speed distribution is used to evaluate the lifetime DELs with a mean wind speed of 10 m/s as defined in IEC class 1A. The loads that will be compared are the tower fore-aft and side-side bending moments ($M_{XTB}$, $M_{YTB}$), the blade root in-plane and out-of-plane bending moments ($M_{XBR}$, $M_{YBR}$) and the yaw bearing fore-aft and side-side moments ($M_{XYB}$, $M_{YYB}$). The reference systems used when referring to blade and tower loading are shown in fig. 5. In fig. 10 the Lifetime DELs for the various erosion models and levels are shown. Examining fig. 10a and the $\varepsilon = 5$
case in fig. 10b, the variations in damage-equivalent fatigue loads are practically negligible, and are all within 2% of the reference value. In those cases where erosion has a significant impact on performance, as in the $\varepsilon = 7.5$ case in fig. 10b, not much variation in DELs is observed, most of the variations coming from $M_{XBR}$ and $M_{XTB}$ with normalized values of 1.03, and $M_{XYB}$ with normalized values of 1.02. For brevity reasons, only the main findings and most relevant differences between the modelling approaches will be discussed.

5.1. Blade root bending moments

$M_{XBR}$ is mainly caused by gravity, which acts on the blades once per revolution. Slight increases in this Lifetime DEL can be observed for all the cases in fig. 10b and 10c. Although the magnitude of the effects changes from case to case, the increases can be attributed to the 12 and 14 m/s wind bins. In these wind speed bins, the rotor speed of the damaged case varies more, thus causing higher fatigue bending stresses. Moreover, the higher variation in AoA causes the drag force to fluctuate more, influencing the in-plane loads the most.

As for the out-of-plane bending moment $M_{YBR}$, the magnitude of the lifetime DEL for this sensor decreases in both the eroded cases. The out-of-plane quantities are particularly affected by the variations in aerodynamic forces as the lift force is mostly directed in this direction [22]. In fig. 11 the 1Hz DELs of this load sensor indicate at which wind speeds most of the differences occur. DELs reduce mainly between 6 and 14 m/s for the $\varepsilon = 5$ case and especially for the $\varepsilon = 7.5$ case with a slight increase at 16 m/s. Two phenomena cause such reductions for the $\varepsilon = 5$ case: for wind speeds below rated $M_{YBR}$ varies once per revolution caused by the differences in blade forces due to the varying AoA induced by wind shear and yaw misalignment. At higher wind speeds, pitch regulation kicks in, causing the AoA on the blade to vary and thus the out-of-plane moment to vary as well. Wind speeds around rated are particularly critical for the controller and small differences in the aerodynamics can trigger the transition from only torque to torque & pitch regulation and vice-versa, with large impacts on loading [23]. This is clearly shown in fig. 12, where the power spectral density (PSD) of $M_{YBR}$ is compared to the blade pitch time series. The differences between the two signals are most pronounced below the once per revolution or 1P frequency (fig. 12a), which is typically linked to the relatively slow pitch regulation systems in large HAWTs (fig. 12b). The increase in 1Hz DELs for the $\varepsilon = 7.5$ case at 16 m/s is also caused by higher pitch regulation as the eroded turbine reaches rated power at a higher wind speed and therefore transitions from torque to torque & pitch regulation at a higher wind speed. For the high erosion model in fig. 10c, $M_{YBR}$ shows the same qualitative trends as the $\varepsilon = 7.5$ case in fig. 11. Upon examining the data, the slight increase in DELs is caused mainly by the 14m/s wind speed bin. Because of the lower turbine power output, blade pitch is generally very low. As wind gusts occur, the power ramps up rapidly and the controller has to intervene aggressively, thus increasing fatigue damage.

![Figure 11: $M_{YBR}$ normalized 1Hz DELs averaged per wind speed bin for the normal erosion model](image1.png)

![Figure 12: $M_{YBR}$ PSD and pitch angle timeseries for a simulation in the 14 m/s wind bin for the normal erosion model](image2.png)
5.2. Tower base bending moments

Tower base bending moments show opposite trends when increasing levels of erosion are simulated. For the normal erosion model in fig. 10b $M_{YTB}$ shows slight decreases as a result of the lower global loading on the structure. On the other hand, for the $\varepsilon = 5$ case in the heavy erosion model, despite the very low power output and global rotor thrust respect to the reference case, both $M_{X TB}$ and $M_{Y TB}$ increase. The ultimate causes of such phenomena are interesting. The tower base for-aft bending moment ($M_{X TB}$) is mostly influenced by the variations in thrust. These are, of course, linked to the sum of the forces acting on the rotor blades. Upon examination of the 1Hz DELs, the increases in fatigue loading are associated with the 10, 12 and 14 m/s wind speed bins. The observation relative to the 14 m/s bin has to do with the increased pitch activity in the damaged case, as the turbine approaches rated power at a higher wind speed. As for the 10 and 12 m/s wind speed bins, the variations have to do with blade aerodynamic damping, that can be approximately expressed as [22]:

$$\varepsilon_a (r) = \frac{1}{\mu} \rho \Omega c(r) \frac{dc}{dx}$$  

(2)

with $\Omega$ being the rotor speed and $c(r)$ the local airfoil chord. Between 10 and 12 m/s the eroded portion of the blades frequently operate between $9^\circ$ and $16^\circ$ of AoA (fig. 8). In this region the slope of the $C_l$ curve is negative for the FFAW3-241 damaged airfoil (fig. 6d), therefore leading to negative aerodynamic damping (eqn. 2). The turbulent wind spectrum broadly excites a wide range of frequencies in the out of plane direction. Blade forces are passed through the blade root to the tower that reacts to all of the rotor loads. This leads to the first tower mode to be excited and, as a consequence of the negative damping, to resonance phenomena to be accentuated. The discussed phenomenon could be counterbalanced by modifying the controller parameters and pitching the blades out, so that they can operate in the linear region. As other studies [5,6] have shown, this would also potentially lead to benefits in AEP. Finally, the negative aerodynamic damping has less influence on blade out-of-plane loads because of the different Whöler curve exponent ($m=10$ for the blades and $4$ for the tower) and because none of the blade frequencies are excited. It is also important to note that this result is specific for the simulated test case. Furthermore, this issue might not be relevant if smaller areas of the rotor are affected by erosion. Finally, if blades are so severely damaged, service and repairs will be most likely be scheduled and this effect may never become an issue if a correct maintenance is planned. As for $M_{X TB}$, all the simulated cases incur in resonance phenomena, and the resulting DELs are very similar.

6. Conclusions

The study presents an introduction to modelling of the effect of erosion on large HAWT blades and the impact this has on turbine performance. A 2D LE erosion model is developed and tested on two different airfoils. Various damaged geometries, differing for the extent and depth of the delaminated areas, are tested. The shape and depth of the end of the delamination area strongly influences the airfoil’s performance, as abrupt changes in geometry lead to very early flow separation and consequently to massive performance drops as seen in the heavy erosion model. The Reynolds number the airfoils operate at also influences the performance decrease: at higher Re numbers the performance decreases of the tested airfoils is more pronounced. Overall, predicting airfoil performance drops accurately can be challenging without further experimental data. The effects of the erosion models are evaluated on the DTU 10MW RWT trough aero-servo-elastic simulations of the turbine in IEC 61400-1 power production conditions. While performance decrease is not significant for the mild erosion model, for the normal erosion model power production decreases significantly, especially with more pronounced erosion factors. As for the heavy erosion model, massive performance decreases were observed for all of the tested configurations. With the decreases in performance, also a general decrease in fatigue loading is observed. Despite the sometimes-massive performance decreases, fatigue loading does not always decrease. Increases in tower base bending moments are observed. These are caused by the excitation of the first tower fore-aft resonance mode, induced by the negative aerodynamic damping caused by the airfoils operating in the post-stall region. While these conclusions are specific to the selected testcase, and were only found when running the model with a somewhat extreme level of...
damage that would most likely lead to real-world maintenance, this result indicates that structural instabilities connected to negative damping can indeed arise from blade LE deterioration, showing the urgent need in the near future for a further understanding of the effect of erosion on modern wind turbines.

References
[1] Rempel L 2012 Rotor blade leading edge erosion – real life experiences Wind Syst. Mag. 3
[2] Sareen A, Sapre C A and Selig M S 2014 Effects of leading edge erosion on wind turbine blade performance: Effects of leading edge erosion Wind Energy 17 1531–42
[3] Slot H M, Gelinck E R M, Rentrop C and van der Heide E 2015 Leading edge erosion of coated wind turbine blades: Review of coating life models Renew. Energy 80 837–48
[4] Røndgaard F-K, February 23ersen- and Europe 2018 Structural Failure Denmark UK WindAction | Siemens sets billions: Ørsted must repair hundreds of turbines
[5] Cavazzini A, Minisci E and Campobasso M S 2019 Machine Learning-Aided Assessment of Wind Turbine Energy Losses due to Blade Leading Edge Damage ASME 2019 2nd International Offshore Wind Technical Conference vol IOWTC2019-7578 (ASME Digital Collection)
[6] Ashuri T, Rotea M, Ponnurangam C V and Xiao Y 2016 impact of airfoil performance degradation on annual energy production and its mitigation via extremum seeking controls 34th Wind Energy Symposium 34th Wind Energy Symposium (San Diego, California, USA: American Institute of Aeronautics and Astronautics)
[7] Bak C, Zahle F, Bitsche R, Kim T, Yde A, Henriksen L C, Natarajan A and Hansen M 2013 Description of the DTU 10 MW Reference Wind Turbine (DTU Wind Energy)
[8] Han W, Kim J and Kim B 2018 Effects of contamination and erosion at the leading edge of blade tip airfoils on the annual energy production of wind turbines Renew. Energy 115 817–23
[9] Gaudern N 2014 A practical study of the aerodynamic impact of wind turbine blade leading edge erosion J. Phys. Conf. Ser. 524 012031
[10] Schramm M, Rahimi H, Stoeveresandt B and Tangager K 2017 The Influence of Eroded Blades on Wind Turbine Performance Using Numerical Simulations Energies 10 1420
[11] Castorini A, Corsini A, Rispoli F, Venturini P, Takizawa K and Tezduyar T E 2019 Computational analysis of performance deterioration of a wind turbine blade strip subjected to environmental erosion Comput. Mech. 64 1133–53
[12] Fiore G and Selig M S 2016 Simulation of Damage Progression on Wind Turbine Blades Subject to Particle Erosion 54th AIAA Aerospace Sciences Meeting 54th AIAA Aerospace Sciences Meeting (San Diego, California, USA: American Institute of Aeronautics and Astronautics)
[13] Viterna L A and Janetzke D C 1982 THEORETICAL AND EXPERIMENTAL POWER FROM LARGE HORIZONTAL-AXIS WIND TURBINES. NASA Tech. Memo.
[14] Anon 2020 OpenFAST/openfast (OpenFAST)
[15] Jonkman J M, Hayman G J, Jonkman B J and Damiani R R AeroDyn v15 User’s Guide and Theory Manual (NREL)
[16] Jonkman J 2013 Overview of the ElastoDyn Structural-Dynamics Module
[17] Mulders S and van Wingerden J 2018 Delft Research Controller: an open-source and community-driven wind turbine baseline controller J. Phys. Conf. Ser. 1037 032009
[18] Borg M 2015 LIFES50+ Deliverable D1.2: Wind turbine models for the design (Riso, Denmark: DTU Wind Energy)
[19] Anon 2005 IEC61400-1:2005 Wind turbines - Part1: Design requirements
[20] Hayman G J MLife Theory Manual for Version 1.00 12
[21] Nicolai, C 2010 Fatigue Load Monitoring with Standard Wind Turbine Signals (Univ. Stuttgart)
[22] Burton T 2001 Wind energy: handbook (Chichester ; New York: J. Wiley)
[23] Perez-Becker S, Papi F, Saverin J, Marten D, Bianchini A and Paschereit C O 2019 Is the Blade Element Momentum Theory overestimating Wind Turbine Loads? – A Comparison with a Lifting Line Free Vortex Wake Method Wind Energy Sci. Discuss. 1–38