Extending wind turbine operational conditions; a comparison of set point adaptation and LQG individual pitch control for highly turbulent wind

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Abstract. Extreme wind conditions can cause excessive loading on the turbine. This not only results in higher design loads, but when these conditions occur in practice, will also result in higher maintenance cost. Although there are already effective methods of dealing with gusts, other extreme conditions should also be examined. More specifically, extreme turbulence conditions (e.g. those specified by design load case 1.3 in IEC61400-1 ed. 3) require special attention as they can lead to design-driving extreme loads on blades, tower and other wind turbine components.

This paper examines two methods to deal with extreme loads in a case of extreme turbulent wind. One method is derating the turbine, the other method is an individual pitch control (IPC) algorithm.

Derating of the turbine can be achieved in two ways, one is changing the rated torque, the other is changing the rated rotor speed. The effect of these methods on fatigue loads and extreme loads is examined. Non-linear aero-elastic simulations using Phatas, show that reducing the rated rotor speed is far more effective at reducing the loads than reducing torque.

Then, the IPC algorithm is proposed. This algorithm is a linear quadratic Gaussian (LQG) controller based on a time invariant model, defined in the fixed reference frame that includes the first tower and blade modes. Because this method takes the dynamics of the system into account more than conventional IPC control, it is expected that these loads dealt with more effectively, when they are particularly relevant. It is expected that in extreme turbulent the blade and tower dynamics are indeed more relevant. The effect of this algorithm on fatigue loads and pitch effort is examined and compared with the fatigue loads and pitch effort of reference IPC.

Finally, the methods are compared in non-linear aero-elastic simulations with extreme turbulent wind.

1. Introduction

Extreme wind conditions can cause excessive loading on the turbine. This not only results in higher design loads, but when these conditions occur in practice, will also result in higher maintenance cost. One of the extreme wind conditions described as a design load case in the international standard IEC61400-1 ed.3, is that of the extreme turbulent wind (design load case 1.3). Although perhaps not as extreme as in the standard, turbulence levels in a farm are considerably higher in ambient conditions ([1]), which makes operation in highly turbulent conditions particularly relevant for turbines operating in a wind farm.
There are already effective methods of dealing with gusts can lead to overspeed situations \cite{2, 3} and methods that deal with shut down strategies in extreme conditions (\cite{4}). As far as the authors are aware, however, there are no specific control strategies to deal with extreme turbulence conditions (e.g. those specified by design load case 1.3 in IEC61400-1 ed. 3). So far, at ECN, no specific control parameters were used to change controller behaviour in turbulent conditions. To use a control strategy specifically for extreme turbulent wind would require a robust and fast detection of the turbulence. Alternatively, a controller could specifically be designed for extreme turbulent wind and still give adequate performance in normal turbulent wind conditions.

Two methods are examined that the deal with extreme turbulent conditions, one is derating (or: curtailment) of the wind turbine. This would require detection of the extreme turbulent wind conditions. The other method is an LQG-based individual pitch control algorithm.

The reference model that is used is a slightly modified version of the NREL 5 MW model\cite{5}. This model is referred here as the ART-model. This model is not yet publicly available, but is expected to be published. The main difference is in the blade structural and aerodynamic design, which was improved. This turbine has a relatively high power density at 401 W/m$^2$. For the wind, a class Ib location is assumed. The controller that is used is designed using ECN’s control tool ACT.

The results presented here are all obtained using non-linear, aero-elastic simulations using the program Phatas\cite{6}.

2. Derating control strategy

Derating the turbine means operating the turbine at a lower power than it could deliver based on the wind conditions and its power curve. Derating is not new; in 1996 a patent was filed \cite{7} that describes derating the turbine as a method to prevent abrupt shutdowns and extending operation at high wind speeds. For single wind turbines that would result in somewhat more production, but for wind farms this method of operation should result in much better predictability of the power output of the farm. That in turn minimizes unbalance cost.

More recently, derating has also become a tool to deal with certain grid code requirements, e.g. \cite{8, 9}. For grid compliance it is sometimes desirable to operate the wind turbine at a different power than usual to support the grid during voltage and frequency events. At ECN derating is being investigated with the purpose of fault tolerant operation\cite{4} of a wind turbine. The idea of fault tolerant control is that when a turbine has a minor fault, the controller of the turbine is adapted to allow the turbine to keep operating, usually at a lower power output to ensure lower loads. Here, the focus of derating is also on mitigating loads so a similar approach is used.

Operating at a lower power set-point than usual can be achieved in different ways. The power is obviously the product of rotational speed and torque, thus any combination of rotational speed and torque that results in lower power is derated in a way. Two methods were compared as part of this work, i.e. one that at rated power mainly reduces torque at nearly constant rotor speed and one that mainly reduces rotor speed at nearly constant torque.

Reducing the torque at the same rotational speed means operating the blade at a smaller angle of attack, i.e. pitched more to feather. In figures 1 and 2 the thrust and torque coefficients (Ct and Cq) are shown respectively. Also, the steady state trajectory of the operating curve for this turbine is shown. To explain more clearly what is meant, the operating point at 14 m/s wind is highlighted in the figures. If, in this situation derating is applied by reducing the torque, while keeping the rotor speed the same, the operating point would move horizontally, heading right from the original operating point as the power is further decreased.

Reducing the rotor speed at a nearly constant torque on the other hand, maintains the same torque coefficient (Cq) at a lower tip speed ratio. In the figures, the operating point then moves
along the same contour of $C_q$ (figure 2), heading down and right from the operating point. This strategy increases the risk of stall along the blade. To avoid stall, a minimum pitch angle as a function of estimated tip-speed ratio was included in the controller.

Table 1: Fatigue and extreme flapwise blade root moments and for-aft tower bottom moments for both strategies calculated for 13, 15 and 17 m/s normal turbulence model. Blade fatigue loads are calculated with fatigue exponent 10, the tower bottom loads are calculated with fatigue exponent 4.

| signal     | 0% red. | -5% torq. | -10% torq. | -15% torq. | -5% rpm | -10% rpm | -15% rpm |
|------------|---------|-----------|------------|------------|---------|----------|----------|
| brm fatigue| 2.89e6  | 2.86e6    | 2.85e6     | 2.83e6     | 2.77e6  | 2.64e6   |
| brm extreme| 17.1e6  | 16.6e6    | 17.4e6     | 17.2e6     | 16.9e6  | 17.1e6   | 15.1e6   |
| tbm fatigue| 4.04e6  | 3.96e6    | 3.87e6     | 3.82e6     | 3.77e6  | 3.76e6   |
| tbm extreme| 1.34e8  | 1.31e8    | 1.26e8     | 1.24e8     | 1.22e8  | 1.13e8   |

Table 1 shows fatigue and extreme blade root moments, as well as tower bottom moments, calculated for 13 and 15 and 17 m/s, normal turbulence, 10 minute, wind fields with six different seeds and varying yaw angles. Simulations were run using Phatas. Torque and rotor speed are reduced by 5, 10 and 15% relative to normal operation. These correspond to equal reductions in power.

The table shows that reducing the rotor speed reduces the blade root fatigue loads, whereas reducing the torque has nearly no effect. Reducing the rotor speed, certainly shows no negative effect on the extremes that occur in the normal turbulence. Both extreme and fatigue tower bottom moments benefit from both control strategies, but significantly more from the reductions in rotor speed.

Why the reductions in rotor speed are more effective than reductions in torque is discussed in section 5.2 of this paper.

The derated control strategies both contain a reference individual pitch controller. The derating strategy thus works in addition to reference individual pitch controller. That is in contrast with the next control strategy, which replaces the reference individual pitch controller.
3. Individual pitch control strategy

Individual pitch control (IPC) strategies have been examined for some time, e.g. [10, 11, 12, 13, 14, 15, 16, 17, 18, 19, 20]. The conclusion in general is that reductions in certain fatigue loads, such as the blade root moment and the shaft loads, in the order of 10-15% relative to collective pitch only strategies are quite feasible. Geyler and Caselitz [15] describe a model where the tower fore-aft motion and the flapwise motions of the blades is included, but the for-aft tower top rotation is not included. Bossanyi [11] describes that an LQG approach based on a linearised model is possible, but that the performance of a decoupled PI controllers can be similar if appropriate filtering is used. In a later paper [12] he indicates that the use of a 3p feedforward filter in addition to the PI controller can improve the performance a bit more, but that this feedforward mainly benefits performance if the actuator has a bandwidth of at least 2 Hz. It should be noted that this study was for a 2 MW turbine, the required bandwidth for a 5 MW turbine would be lower, probably in the order of 0.8 Hz. Larsen e.a. [13] describe a system where the pitching action is based on individual blade inflow measurements. Van Engelen [14] discusses an IPC design consisting of a 1p-PI loop and an additional control loop based on the PI control of a 2p transformed signal. This 2p-PI controller also reduces the 3p loads in the fixed frame domain. Selvam e.a. [16] also explore a combined feedback and feedforward for IPC, but use a higher order model based on and evaluated with TURBU (21). Kanev [17] also examines aerodynamic rotor balancing and pitch effort limitation methods for IPC.

Most of the models used in these studies include rotor speed variations and the first eigenmode of the tower, but, as far as the authors can discern, either ignore the blade dynamics or the tower rotation. Ignoring the blade motion seems logical, as the blade dynamics tend to have significantly higher eigenfrequencies than the dynamics of the drive train and the tower. However, once the loading is more turbulent and less coherent in time, the authors think that the dynamics of the blade should be included. Furthermore, the tower top rotation seems to give an important contribution to the loads.

A different approach is used by Stol and Balas [18]; they used a control system that control periodic loads, also known as disturbance accommodating control. Disturbance accommodating control is particularly suitable for recurring periodic loads. The papers [19] and [20] describe a possible implementation of disturbance accommodating control.

The performance of IPC is generally compared to collective pitch control, but a comparison that examines both pitch effort and performance of two IPC control methods is rare.

3.1. Method and model

The approach here is based on an analytical model of the wind turbine that includes only the first for-aft tower bending and the flapwise blade bending. By using the Coleman transform, the equations of motion of this system can be translated directly into a linear time invariant (LTI) model. This model is then extended with two filters; one on the input side to approximate the wind spectrum for the Kalman filter, and one that filters the blade root moments, to tune the response of the system to best reflect the fatigue load. The model is different from the one presented by [15] because it includes the tower top rotation as part of the loads.

We will use this model to design a LQG controller. The blade root moment (BRM) filter is tuned on the basis of fatigue load calculations for a normal wind profile at 13 and 15 m/s.

The idea behind using this approach, is that this controller may be able to control the loads more effectively when loads are determined less by static yaw and tilt components, but more by dynamic components. It is expected that for extreme turbulence this may be the case.

3.2. Design model

As mentioned, the model includes only the first for-aft tower bending and the flapwise blade bending. This kind of model could be derived using a linearisation at an operating point using
The basic model (a) consists of the first for-aft and flapwise modes as a combination of rigid bodies and hinges. (b) shows the free-body diagram of a blade, while (c) shows the free-body diagram of the tower.

Software such as Turbu [22] or DNV-GL Bladed [23]. Here, it is derived analytically to allow a clear understanding.

The modes of the blades and the tower are assumed to consist of rigid body motions and hinged connections. A side-on view of the basic model is shown in figure 3. Note that in this figure, the rotor only seems to consist of two blades, but the analysis is done on the basis of three blades. The locations of the hinges can be tuned such that the kinematics of this model match the first eigenfrequencies or the tower and blades. The rotor is assumed to rotate at a constant rotor speed. For convenience, the aerodynamic forces on the blade are assumed to act in a single point. The hinges are assumed to consist of a rotational spring and damper.

This model leads to equations for rigid-body displacement and rotation of the blades and tower in flapwise direction and to a set of boundary conditions that ensure that the displacement of the blades and the hinges is equal. The model thus includes 4 degrees of freedom.

The following items are ignored to simplify the model:

- the centripetal contributions to the equation of motion
- any vertical displacements
- rotor tilt and cone
- the translational inertia of the tower
- pitch system dynamics
- higher order modes of blades and tower
- any mass or aerodynamic mismatch of the blades
- change in flapwise stiffness due to pitching

The rotation of a blade around its hinge is then described by the following equation of motion:

$$J_{b} \ddot{\theta}_{bi} = -s_{b} \theta_{bi} - d_{b} \dot{\theta}_{bi} + s_{b} \dot{\theta}_{i} \cos \psi_{i} + d_{b} \dot{\theta}_{i} \cos \psi_{i} - d_{b} \omega \dot{\theta}_{i} \sin \psi_{i} + F_{ji} R_{g} + F_{axi}(R_{ae} - R_{g}) \quad (1)$$

Of particular interest should be the azimuth dependent contributions of the tower top motion.

The flapwise displacement of the center of gravity of the moving part of the blade is described by:

$$m_{b} \ddot{x}_{bi} = F_{axi} - F_{ji} \quad (2)$$
For the tower, the rotational motion around the tower hinge is considered and the equation of motion becomes:

\[ J_t \ddot{\theta}_t = -s_t \dot{\theta}_t - d_t \dot{\theta}_t + L_t \sum_{i=1 \ldots 3} F_{ji} - \sum_{i=1 \ldots 3} (M_{ji} - R_b F_{ji}) \cos \psi_i \]  

(3)

Where \( M_{ji} \) is the moment at the blade hinge (or: joint) which can be calculated as:

\[ M_{ji} = -s_b \dot{\theta}_bi - d_b \dot{\theta}_bi + s_b \theta_i \cos \psi_i + d_b \dot{\theta}_t \cos \psi_i - d_b \omega \theta_t \sin \psi_i \]  

(4)

As boundary condition the blade must be attached at the hinge:

\[ x_{bi} = \theta_t L_t + \theta_t R_b \cos \psi_i + \theta_{bi} R_g \]  

(5)

Where:

- \( J_b \) the rotational inertia of the outer part of the blade, relative to the center of gravity
- \( J_t \) the rotational inertia of the top part of the tower, relative to the tower hinge
- \( s_b \) the stiffness of the blade hinge
- \( s_t \) the stiffness of the tower hinge
- \( d_b \) the damping of the blade hinge
- \( d_t \) the damping of the tower hinge
- \( \theta_{bi} \) the angular displacement of blade \( i \) relative to the rotor plane
- \( \theta_t \) the angular displacement of the tower joint
- \( x_{bi} \) the flapwise displacement of the center of gravity of blade \( i \)
- \( F_{ji} \) the flapwise force in the blade hinge (or joint) of blade \( i \)
- \( F_{axi} \) the flapwise aerodynamic force on blade \( i \)
- \( \psi_i \) the azimuth of blade \( i \)
- \( R_g \) the spanwise location of the center of gravity of the blade relative to the blade hinge
- \( R_{ae} \) the spanwise location of the aerodynamic center on the blade relative to the blade hinge
- \( R_b \) the spanwise location of the blade hinge from the rotor center
- \( L_t \) the distance of the rotor center from the tower hinge
- \( \omega \) the rotor speed
- \( \dot{\cdot} \) indicates a time derivative

Note that the blade hinge location is chosen such that both the individual and collective frequencies match with the frequencies of the more complex (multibody) model.

These equations are azimuth dependent and thus time varying. By applying the Coleman transform \[24\], these equations can be made azimuth and time independent. With the Coleman transform we translate one set of variables into another set of variables using:

\[
\begin{pmatrix}
  x_{col}
  x_{yaw}
  x_{tilt}
\end{pmatrix} = C_{cm}^{-1} \begin{pmatrix}
  x_1
  x_2
  x_3
\end{pmatrix}
\]

where

\[
C_{cm}^{-1} = \begin{bmatrix}
\frac{1}{3} \sin \psi_1 & \frac{1}{3} \sin \psi_2 & \frac{1}{3} \\
\frac{1}{3} \cos \psi_1 & \frac{1}{3} \cos \psi_2 & \frac{1}{3} \\
\end{bmatrix}
\]

(6)

The return transform is done using:

\[
\begin{pmatrix}
  x_1
  x_2
  x_3
\end{pmatrix} = C_{cm} \begin{pmatrix}
  x_{col}
  x_{yaw}
  x_{tilt}
\end{pmatrix}
\]

where

\[
C_{cm} = \begin{bmatrix}
1 & \sin \psi_1 & \cos \psi_1 \\
1 & \sin \psi_2 & \cos \psi_2 \\
1 & \sin \psi_3 & \cos \psi_3 \\
\end{bmatrix}
\]

(7)

By the assuming that the rotor speed variations can also be ignored and by applying the Coleman transform to blade degrees of freedom (\( \theta_{bi} \)) and the loads, the differential equations
(equation 11) for the blades become:

\[
J_b \begin{pmatrix}
\frac{\dot{\theta}_{b_{col}}}{\dot{\theta}_{b_{yaw}}} \\
\frac{\dot{\theta}_{b_{yaw}}}{\dot{\theta}_{b_{tis}}} \\
\dot{\theta}_{b_{cm}} \\
\dot{\theta}_{b_{cm}}
\end{pmatrix} = \begin{bmatrix}
-d_b & 0 & 0 \\
0 & -d_b & 2\omega \\
0 & -2\omega & -d_b
\end{bmatrix} \begin{pmatrix}
\frac{\dot{\theta}_{b_{col}}}{\dot{\theta}_{b_{yaw}}} \\
\frac{\dot{\theta}_{b_{yaw}}}{\dot{\theta}_{b_{tis}}} \\
\dot{\theta}_{b_{cm}} \\
\dot{\theta}_{b_{cm}}
\end{pmatrix} + \begin{bmatrix}
-s_b & 0 & 0 \\
0 & (\omega^2 - s_b) & d_b\omega \\
0 & -d_b\omega & (\omega^2 - s_b)
\end{bmatrix} \begin{pmatrix}
\theta_{b_{col}} \\
\theta_{b_{yaw}} \\
\theta_{b_{tis}} \\
\theta_{b_{tis}}
\end{pmatrix} + \cdots
\]

The \( \omega \) and \( \omega^2 \) terms arise due to the time derivative of the Coleman matrices.

The same procedure can also be applied to the azimuth dependent variables in the tower equation, the joint forces and the boundary conditions. With some work, the system of equations can then be written in state-space form:

\[
\begin{bmatrix}
\dot{\theta}_t \\
\dot{\theta}_t \\
\dot{\theta}_{b_{cm}} \\
\dot{\theta}_{b_{cm}}
\end{bmatrix} = \begin{bmatrix}
0 & 0 & 0 & 0 \\
0 & 0 & 0 & 0 \\
0 & 0 & 0 & 0 \\
0 & 0 & 0 & 0
\end{bmatrix} \begin{bmatrix}
\theta_t \\
\theta_t \\
\theta_{b_{cm}} \\
\theta_{b_{cm}}
\end{bmatrix} + \begin{bmatrix}
0 & 0 & 0 & 0 \\
0 & 0 & 0 & 0 \\
0 & 0 & 0 & 0 \\
0 & 0 & 0 & 0
\end{bmatrix} \begin{bmatrix}
M_t^{-1} M_{\Theta_t} \\
M_t^{-1} M_{\Theta_t} \\
M_t^{-1} M_{\Theta_{b_{cm}}} \\
M_t^{-1} M_{\Theta_{b_{cm}}}
\end{bmatrix}
\begin{bmatrix}
0 \\
0 \\
M_t^{-1} (C_t M_t^{-1} B_{F_b} + B_{F_t}) \\
0
\end{bmatrix} F_{ax_{cm}} (9)
\]

Where the various matrices \( M_{\ldots}, B_{\ldots}, C_{\ldots}, D_{\ldots}, K_{\ldots}, S_{\ldots} \) contain the mass, stiffness, damping and cross-terms and \( \theta_{b_{cm}} \) and \( F_{ax_{cm}} \) contain the collective, tilt and yaw oriented parts of the blade motions and axial force components respectively. This equation still only describes the structural motion of the wind turbine and does not have any interaction with the surrounding fluid.

We assume that the axial forces are due to variations in the wind and pitch angle around a steady state value, and that the forces can be modelled using a first order approximation:

\[
F_{ax} = f(\bar{v}_w + \delta v_w, \bar{\phi}_b + \delta \phi_{bi}) = \bar{F}_{ax} + \delta F_{ax} = \bar{F}_{ax} + \frac{\partial F_{ax}}{\partial \bar{v}_w} \delta v_w + \frac{\partial F_{ax}}{\partial \bar{\phi}_b} \delta \phi_{bi} (10)
\]

The notation \( \bar{\cdot} \) is used to indicate a steady state value.

The dynamics of the lift of the aerofoils, such as the dynamics of the wake of the aerofoil itself as modelled by Theodorsen, [25] (or in approximated form: [26]), or the dynamics of dynamic stall (e.g. [27]) are ignored because respectively, the time scale of these dynamics is much smaller than the time-scale of pitch angle variations for IPC and at the outer part of the blade, which is most relevant for the loading and the change in loading, the angle of attack should be sufficiently small, such that during normal operation, stall should not become an issue.

The dynamics of aerodynamic damping on the blade flap wise motion is not ignored. It is calculated by including the flapwise blade velocity as a change in perceived wind speed in the aerodynamic center:

\[
\dot{\psi}_i = \dot{\bar{\psi}}_i + \dot{\bar{\phi}}_b \omega \delta \phi_{bi} = \bar{\dot{\psi}}_i R_b \omega \sin \psi_i + \dot{\bar{\phi}}_b R_{ax} + \bar{\dot{\psi}}_i L_t + \bar{\dot{\phi}}_b R_b \cos \psi_i (11)
\]

For a particular operating point \( \bar{p} \), the partial derivatives are approximated based on the
thrust coefficients \((C_T)\) of the turbine:

\[
\frac{\partial F_{ax_i}}{\partial \theta_{bi}} = \frac{1}{2B} \rho \pi R^2 \bar{v}_{col}^2 \left. \frac{\partial C_T(\bar{\lambda}, \bar{\theta}_{col})}{\partial \theta} \right|_{\bar{\rho}}
\]

\[
\frac{\partial F_{ax_i}}{\partial v_{wi}} = \frac{1}{2B} \rho \pi R^3 \bar{v}_{col} \left( 2C_T(\bar{\lambda}, \bar{\theta}_{col}) - \bar{\lambda} \left. \frac{\partial C_T(\bar{\lambda}, \bar{\theta}_{col})}{\partial \lambda} \right|_{\bar{\rho}} \right)
\]

where \(\lambda\) is the tip-speed ratio and \(B\) is the number of blades.

That means, the force in the aerodynamic center due to this velocity can be calculated as:

\[
F_{ax_i} = -\frac{\partial F_{ax_i}}{\partial v_{wi}} \dot{x}_{bi}
\]

After applying the Coleman transform to the these equations, the aerodynamic damping term can be included in the model in equation \(\text{10}\). This damping is dependent on the magnitude of \(\frac{\partial F_{ax_i}}{\partial v_{wi}}\), which in turn is dependent on the operating point. The aerodynamic damping is highest at operating points around rated wind speed and lowest at high wind speeds, when the blade is pitched further. The controller is designed for an operating point at 15 m/s. The variation in aerodynamic damping is not taken into account. Gain scheduling of the sensitivities is taken into account.

The structural model including aerodynamic damping consists of 8 states.

### 3.3. LQG IPC controller design

The IPC controller is designed as an LQG controller based on the model above. The Kalman filter in this LQG controller is used to estimate the structural states based on the tower top for-aft acceleration measurement and the blade root moment measurements, also some augmented states are used to ensure that the wind spectrum is modelled to some extent. An LQR controller is then designed to minimise the filtered tilt and yaw oriented blade root moments, using only yaw and tilt oriented components of the pitch angles. In this way, the controller does not interfere with the baseline, collective pitch and generator controller design.

#### 3.3.1. Input and output weighting function

Two weighting functions are appended to the model. One is for the wind input and is a ‘model’ of the wind excitation. The other is included to weight the blade root moments, such that the design equivalent load of the blade root moments is minimised. The input weighting function is chosen to be a first order low-pass filter. This does not do justice to the spectrum of the blade effective wind speed due to rotational sampling, in particular, the \(np\) modes, but it does capture a very rough estimate of the wind spectrum.

The output weighting function is a zero-pole-gain function, where the zeros and poles are chosen on the basis of an iterative process. This function is meant to capture some of the dynamics of calculating the design equivalent load, but because it is chosen to minimise the design equivalent load after simulation, it may also capture some of the missing dynamics.

The number of poles of this filter is chosen to be 3, the number of zeros is 2. A zero-pole combination was chosen that obtained the good reductions in design equivalent load in non-linear simulations at 13 and 15 m/s. However, the search for an ‘optimum’ weighting function was not exhaustive. Also, it is extremely unlikely that the cost curve for this ‘optimisation’ is convex. Methods such as simulated annealing or genetic algorithms would be needed.

Including the input and output weighting functions brings the total number of states to 14.
3.3.2. Other implementation issues There are some additional issues that are important for the implementation.

- Sensors: as sensors, the blade root moments and the tower top for-aft acceleration are used.
- Gain scheduling: to establish the right amount of pitching, the pitch angles must be scheduled properly on both the output side (how much pitch is needed to obtain the desired result) and inversely on the input side of the Kalman filter (how much does the pitch affect the model states).
- Filtering: it can be very effective to include specific \( np \) filters in the controller, to help the controller avoid reacting at these frequencies if they do not contribute much to the design equivalent load. No filters were implemented in the LQG IPC algorithm.
- Rotor speed dependence: the analytical model clearly shows 'whirl' type modes. These whirl modes are obviously rotor speed dependent. In our current implementation, this variation in rotor speed is not yet taken into account.

3.4. Comparison with reference IPC

To compare LQG and IPC design, it is important to examine pitch effort as well as design equivalent load reduction. The reference controller consists of a 1p IPC, where particular frequencies are filtered out, to prevent pitching at particular \( np \) frequencies.

In the figure 5, the design equivalent loads are plotted, that were calculated for six seeds at 13, 15 and 17 m/s for normal turbulent models. The pitch effort is the RMS value of the pitch speed.

The figure shows that at low control efforts LQG IPC is more effective than the reference IPC controller. At the same effort, the LQG IPC achieves up to 5% higher reductions in design equivalent load. However, at higher control efforts, this trend does not continue and it does not achieve the maximum reduction in gain obtained for the reference IPC design. It is likely that improved modelling and a more exhaustive search for appropriate weighting functions will give better results.

Because the controller model now includes the tower model, there may be some difference in the tower loads. Figure 6 shows the effect on the tower bottom for varying gain. Here, there is also a small reduction (ca. 2.5%) in the tower loads. The figure also shows that for the reference controller, there is also a clear optimum.

4. Results for extreme turbulent wind

The previous sections discussed two algorithms that are aimed at reducing loads. We now evaluate these controllers specifically for the extreme turbulence model. The turbulence factor \( c = 2 \) is used and is not extrapolated based on the fatigue load cases.

As a reference case, IPC at its default gain is used. This reference case is compared with a controller operating at reduced rotor speed, the LQG IPC controller. It should be noted that the controller with reduced rotor speed contains the reference IPC controller, whereas the LQG IPC controller is working as a replacement of the reference IPC controller. The reduction in rated rpm is 10%.

To examine how each controller affects the loads, the fatigue load and extreme loads for the extreme turbulent wind are shown in figure 7. For an extreme load case, referring to the fatigue load is unusual, but because the controllers have such different mechanisms, it was thought to be insightful for this comparison. The fatigue loads are calculated per 10 minute simulation, without taking the occurrence of the wind speed into account. Both the extreme and fatigue loads were calculated for each wind speed as the maximum of 6 different wind seeds with positive, negative and no misalignment. Control effort for the reference IPC control was on average 1.84 deg/s, for the controller with reduced rotor speed, 1.73 deg/s and for the LQG IPC controller...
Figure 4: Example of time series of the pitch rates of reference IPC (a) and LQG IPC (b) for the same load case.

Figure 5: Comparison of design equivalent load of the blade root moment vs. control effort for reference IPC (dotted) and LQG IPC algorithm (solid line). Control effort is given as the RMS value of the pitch speed.

Figure 6: Comparison of design equivalent load of the tower bottom moment vs. control effort for reference IPC (dotted) and LQG IPC algorithm (solid line). Control effort is given as the RMS value of the pitch speed.
1.75 deg/s. Based on the comparison for the fatigue loads, the reference IPC would be expected to out-perform the LQG-IPC controller. However, the figure shows that the LQG IPC algorithm is able to reduce the fatigue loads to a similar level as the reference IPC control. The highest extreme load is 3% higher than the reference controller, but, on the other hand, the control effort is 5% less. Reducing the rotor speed by 10% is a more effective way of managing extreme turbulence, but comes at the cost of reduced production in extreme turbulent wind and would require a detection algorithm for extreme turbulent wind.

5. Discussion and conclusion
5.1. Conclusions
The aim of the paper was to examine different control strategies for extreme turbulence wind. The main conclusions are:

- If extreme turbulence situations can be detected effectively and the loss of produced energy is not an issue in this case, reducing the rated rotor speed is a very effective method to deal with loading.
- For the extreme turbulence model, the current LQG IPC controller has similar overall performance to the reference IPC controller.

Concerning the proposed control strategies:

- For the fatigue load cases, reducing the rotor speed is more effective to reduce loads than reducing the torque. A 15% reduction in rotor speed, results in blade root fatigue loads reductions of 9%, tower fatigue load reductions by 7%, tower extreme loads by 16%. For a 15% reduction in torque, these numbers are respectively 2%, 4% and 6%.
- LQG IPC shows potential to improve the loads with less control effort than a reference IPC, especially when a limited control effort is desirable. However, the current model and/or estimator are not yet sufficient.
5.2. Discussion and future work

Reducing loads by reducing the power production was compared for torque and rotor speed reductions. Reducing the rotor speed is more effective at reducing the fatigue loads that occurred during the loadcases. It is also a little more effective at tackling the extreme load that occurred.

Although it is tempting to assume that because of the lower number of cycles, the fatigue load is automatically reduced, this does not directly apply to the blade root moment equivalent load. Because the material has a high resistance to fatigue (a high fatigue exponent), the highest occurring load cycles form an important part of the fatigue loading.

Although the reduced rotor speed results in higher angles of attack and operating closer to stall conditions, it is more effective at reducing the loads on the structure. This is not surprising if variations in the loading of an aerofoil are considered:

$$\Delta F_{lift} \propto c v_{eff}^{2} \frac{\partial C_{lift}}{\partial \alpha} \Delta \alpha(\omega, v_w)$$

Assuming small variations in rotor speed and wind speed do not directly affect the blade effective wind speed $v_w$, makes variations in loading directly proportional to the slope of the lift curve.

If the torque is reduced, but the rotor speed is maintained, the blade effective wind speed remains practically constant. By reducing the rotor speed, the blade effective wind speed is reduced and the variation in loading is reduced considerably. However, as mentioned before, it is important that stall is avoided, otherwise the load advantages will quickly disappear.

LQG IPC shows potential to reduce the design equivalent loads to similar load levels as the reference controller but with less control effort. However, the results also show that the models are not yet sufficient. Another question is whether less pitching will also lead to lower maintenance. It could be that because the loads are distributed over a smaller section of the pitch bearing, more maintenance is needed for LQG IPC than for reference IPC. Also, it is not yet clear how the number of pitch direction changes, which may affect the pitch drives and/or gearboxes, change with LQG IPC control.

The design of LQG IPC was done on the basis of a very basic analytical model. Earlier papers describing LQG IPC were not always clear about how the turbine modal model was derived or ignored the effect of the rotation of the tower top. This analytical model can also serve as a basis for testing the closed loop stability of IPC algorithms. Using an analytically derived model allows an analytical approach to tackle the effect of rotor speed variations on whirl modes and the states of the system obtain the same meaning and magnitude. This also enables a clear and straightforward gain scheduling strategy for both the Kalman filter and the output signals. For numerically derived modal models, this is not as straightforward, although it is possible.

Including more dynamics in the system has increased the complexity and the computational effort of the current algorithm, especially when compared to simple (P)I controllers. On the other hand, this amount of computational effort remains well within the requirements of modern PLCs. Moreover, the computational effort is still considerably less than algorithms such as model predictive controllers.

Future work will take the rotor speed variations into account in the dynamics of the whirl modes. Including additional system dynamics in the model, in particular the pitch system dynamics and the second for-aft tower mode should also result in further improvements. One item that should also be part of future work is the design of the weighting filters, in particular of the output weighting function. The process that was applied here to select the output weighting function was computationally quite intensive and the lack of a clear link to the physical properties of the model or the computational process of calculating the design equivalent load is not satisfactory.

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