Inflow-based flap control on a 10MW-scale wind turbine using a spinner anemometer

Dimitris I. Manolas, Nikos A. Spyropoulos, Giannis P. Serafeim, Vasilis A. Riziots, Panagiotis K. Chaviaropoulos, Spyros G. Voutsinas

School of Mechanical Engineering, National Technical University of Athens, GR15780 Athens, Greece
Correspondance to: manolasd@fluid.mech.ntua.gr

Abstract. In the paper, an individual flap control (IFC) algorithm based on wind speed measurements obtained with a spinner anemometer is presented. The controller uses flap actuators with the aim to remove any deterministic source of load variation on blades, associated with asymmetries of the inflow. Such load variations are concentrated on multiples of the rotational frequency (p multiples) and they are mainly due to wind yaw, inclination and atmospheric boundary layer (ABL) shear. The aim of the controller is to assist operation of the conventional feed-back individual pitch controller (IPC) and thereby reduce its control duty cycle. The performance of the proposed controller is assessed through aero-elastic simulations for the 10MW DTU Reference Wind Turbine. A subset of normal operation fatigue and ultimate Design Load Cases (DLCs) of the IEC has been simulated and load reduction capabilities of the control algorithm have been compared against those of the standard individual pitch control (IPC) loop.

1. Introduction
In the framework of the EU funded Innwind.EU project [1] a number of innovative inflow sensors and their capability to better regulate power and speed of a wind turbine and to control loads was tested. Among the different types of potential inflow sensors the project was mainly focused on hub mounted LIDARS and spinner anemometers. In the context of the above project, NTUA proposed and tested a feed-forward controller which combines turbulent inflow preview coming from a spinner mounted anemometer and trailing edge (TE) flap actuators with the aim to control loads on the blades. In particular the controller objective is to reduce periodic loads on the blades associated with inflow asymmetries such as wind yaw/inclination and shear. As the controller is formulated in a feed-forward context it cannot predict and therefore mitigate periodicity on loads coming from 1p excitation due to rotational sampling of wind turbulence. So, it can only be regarded as complementary to standard feed-back load control loops. In connection to the above, the controller has been tested in conjunction with a standard feed-back individual pitch controller (IPC) based on measurements of the three blades’ out-of-plane moments. The expected benefit through the use of the above controller is the reduction of the pitch actuators duty cycle and therefore the reduction of the fatigue damage of the pitch mechanism and bearings which is essential when dealing with very large blades.

Inflow based feed-forward control has been extensively investigated by several researchers. LIDAR based preview of the incoming wind has been used to improve standard power-speed controllers, both in the partial load region through better Cp tracking and in the full load region through improved collective pitch which leads to smoother speed regulation. Bossanyi et al in [2],
applied an additional pitch rate proportional to the level of change of the preview wind speed to improve speed regulation. Bossanyi et al in [3] used an additional pitch rate demand calculated to achieve the desired change in pitch angle at the end of the preview time. A more sophisticated approach for LIDAR based control is proposed by Schlipf et al [4] through model predictive control (MPC). Wortmann et al [5] developed an IPC controller based on LIDAR wind measurements that compensates load variations due to yaw misalignment and wind shear. On the other hand, flap based control has also been given a lot of attention by the wind community. Both pure IFC but also combined IPC&IFC methods have been tested by several researchers in the past years. Most recent developments are those of Lackner and van Kuik [6], Bæk [7] and Bernhammer et al [8] who have all applied similar IFC and IPC&IFC schemes based on Coleman’s transformation in order to reduce blade fatigue loads. Also, of relevance are the works by Barlas et al [9] and Bergamì and Poulsen [10] who have applied model predictive and linear quadratic control of adaptive distributed flap geometries. Inflow sensing using spinner anemometers has also been recently given some attention [11]. Nevertheless, integration of spinner anemometer measurements into a wind turbine control loop using flaps is novel development.

A spinner anemometer is an innovative inflow measurement sensor [11], [12], well suited for control purposes. It combines three independent sonic sensors, mounted over the spinner surface at three different locations having an azimuth shift of 120°. The three sensors measure the directional wind velocity (also with an azimuth shift of 120°). From the stagnation point in the centre of the spinner the flow accelerates over the surface and reaches flow velocities above the free wind speed. So, the position of the anemometers over the spinner must be tuned so as the measured flow velocity is the same as the free wind speed. Apparently, tuning of the position depends strongly on the shape of the spinner. Moreover, each sonic sensor has a built-in accelerometer that is used to determine azimuth position of the rotor. Through proper transformation the measured by the sensors rotating components of the wind velocity can be converted into three wind velocity components with respect to the non-rotating frame (or equivalently to wind magnitude and yaw and inclination angles) at a single point in front of the rotor. Moreover, an estimation of both the vertical and horizontal wind shear exponent can be provided through cross correlation characteristics of the axial and vertical or the axial and horizontal wind components respectively.

The proposed feed-forward flap controller is based on the following working principle; inflow asymmetries such as yaw misalignment, upflow and ABL shear cause variations of the out-of-plane moment on multiples of the rotational frequency (p multiples). Such load variations can be smeared out if out-of-phase TE flap angle variations are imposed by the controller. In the present controller lp and 2p variations (higher harmonics are considered to have a negligible effect) of the flap angle of the three blades are pre-tuned versus the magnitude of wind speed, yaw and inclination angles and shear exponent. Maps of the amplitude and phase of the flap angle variations as functions of the wind speed, yaw and tilt angles and shear exponent have been created through an automated tuning process which is based on deterministic runs over the whole range of operational wind speeds and a wide range of combinations of yaw, tilt angles and shear exponents. During wind turbine operation the above maps are treated as look-up tables that define flap angle variation envelopes depending on the wind conditions read by the spinner anemometer. The controller is combined with a standard IPC loop based on measurements of the out-of-plane bending moments of the three blades. The aim of the standard feed-back IPC is to remove lp variation due to the rotational sampling of the turbulence which has a stochastic nature.

In the paper, the performance of the combined IPC and inflow based (spinner anemometer) IFC is assessed against the pure IPC. Fatigue as well as ultimate loads reduction capabilities of the controller is assessed through time domain aeroelastic simulations for the IEC (offshore) [13] DLCs 1.2, 1.3 and 1.6. Simulations are performed for the offshore DTU 10MW Reference Wind turbine (RWT) mounted on the DTU INNWIND jacket support structure [1], [14] at 50m water depth. The results of the analysis indicate that an almost identical out-of-plane blade moment reduction of about 25% can be achieved by both controllers, in line with the reduction levels reported in the literature for IPC.
the case of the combined controller (thereafter referred to as IPFCS) the above load reduction is accompanied by an about 30% reduction in the the duty cycle of the pitch actuator compared to pure IPC, but also an increase in the blade root torsion angle induced by flaps.

2. Description of the inflow based feed-forward controller

In the present analysis an ideal spinner anemometer is considered that provides exact values of the instantaneous wind velocity components and an exact estimation of shear exponent. The magnitude of the wind speed, as well as the inflow yaw and inclination (tilt) angles can be directly calculated through the three components of the wind velocity measured by the spinner anemometer. Information about the ABL characteristics (an estimate of the shear exponent) can be also obtained through cross correlation characteristics of the axial and vertical wind components. The absolute value of the covariance of \( u, w \) components increases as the shear exponent increases.

As illustrated in Figure 1 the instantaneous magnitude of the wind speed, yaw and tilt angles, but also the shear exponent (not shown in the figure) provided by spinner anemometer measurements are low-pass filtered with the aim to remove the high frequency/low energy turbulent content, using a 1\(^{st}\) order filter with a time constant of 5sec. Filtered instantaneous wind speed, yaw and tilt angles and shear exponent input characteristics are correlated with the required 1p and 2p periodic variations of the flap angle (in terms of amplitude and phase) of the three blades that compensate their effect on blade out-of-plane moment.

The amplitude and phase of the flap angle variation (1p and 2p) will be proportional to the load amplitude and phase caused by the asymmetry of the inflow. Obviously, load amplitude depends on yaw and tilt angle and also on the wind speed and shear exponent. Therefore, maps of the amplitude and phase of the flap angle variations (as functions of the wind velocity, yaw and tilt angle and shear exponent) are pre-defined through an automated tuning process, which is based on deterministic runs over the whole range of operational wind speeds and combinations of yaw, tilt angles and shear exponents. The derived look-up table is not expected to be optimal in turbulent conditions where dynamic effects occur. However the relatively high time constant of the filter employed, only allows low frequency variations of the wind speed, and the yaw and tilt angles to be dealt with by the controller and thereby dynamic effects are mitigated.

The tuning process of the feed-forward controller is considered as an initialization mode (IPC is deactivated) and its aim is to determine the amplitude and phase of the flap motion that minimize the amplitude of the blade out-of-plane moment. It follows a standard IFC feed-back loop based on Coleman transformation as detailed next. The blade root out-of-plane bending moment signals are transformed into fixed frame moments \( M_{\text{yaw}i} \) and \( M_{\text{tilt}i} \) (where \( i=1, 2 \)) by applying the Coleman transformation (for 1p and 2p), then passed through an integral control element (I) (only considered in the tuning process) and the cyclic \( \beta_{\text{yaw}i} \) and \( \beta_{\text{tilt}i} \) angles are obtained. These angles are then back transformed into flap angle amplitudes \( \beta_1 \) and \( \beta_2 \) and phases \( \Delta \psi_1 \) and \( \Delta \psi_2 \) of the individual blades’ flap motion, via an inverse Coleman transformation. The flap angle of every blade \( k \) is then obtained through:

\[
\beta_{\text{flap}}^k = \beta_1 \cos(\psi_k + \Delta \psi_1) + \beta_2 \cos(2\psi_k + \Delta \psi_2) \quad , \quad k = 1, 3
\]

Figure 1: Feed-forward control approach.
TE flap control is performed on the outer part of the blade of the DTU 10MW Reference Wind turbine (RWT). The blade of the reference turbine comprises FFA series airfoils. The relative thickness of the outer 35% of the blade is constant and equal to t/c=0.24. The flap extends to 30% of the section chord length and 34% of the blade radius, starting at 55m from the hub center, based on the study presented in [15]. Flap motion is bounded in the range [-10°, +10°]. Higher range of the flapping motion could provoke early separation of the flow that in turn will reduce effectiveness of flap actuation. In addition, saturation limits have been imposed on the velocity of the flap motion to be less than 20°/s, as considered in [16]. In all configurations a delay of 0.1 s has been imposed on the flap motion in order to account for the dynamics of the flap actuator (through a first order filter in flap response). The abovementioned control logic is implemented in hGAST multibody, FEM, servo-aero-elastic tool [17] which is able to simulate the effect of moving flaps [18].

3. Tuning and validation based on deterministic cases

Look-up tables for the amplitudes βf1 and βf2 and the phases Δψ1 and Δψ2 have been produced for wind speeds between 6m/s and 24m/s with a step of 2m/s, yaw angles between -45° and 45° and inclination angles between -15° and 15° both assuming a step of 5°. Examples of the produced look-up tables (for the amplitudes βf1 and βf2 and the phase Δψ1) are presented in Figure 2 for the case of zero inclination angle and shear exponent 0.2, as functions of the yaw angle. As expected βf1 amplitudes are 3-6 times higher than βf2 indicating that 1p frequency dominates load variation. Phase Δψ1 depends strongly on yaw angle and presents an almost 180° difference between higher negative and higher positive yaw angles. Look-up tables are not symmetric with respect to 0° yaw angle due to the effects of shear and the build in nacelle tilt that are both included in the analysis and contribute to additional 1p excitation.

![Figure 2: Look-up tables of βf1, βf2 and Δψ1 for zero inclination angle and shear exponent 0.2 for various wind speeds as a function of the yaw angle.](image-url)
It is noted that in the tuning process no saturation is applied to the flap angle. This explains why flap amplitudes exceed 10° angle. Bounds on maximum and minimum flap angle and speed are only imposed when the above described maps are integrated into the control loop as indicated in Figure 1. In this case, flap contribution to load reduction is not maximum, however flap control is still assisting the IPC loop. Also tuning of the IPC loop is performed considering the combined IPFCS under turbulent cases for the same wind speeds (6-24m/s).

Feed-forward flap controller’s operation is initially verified under deterministic, asymmetric wind conditions. The IPC loop is not activated in the above test. Since no stochastic excitation is considered in this case the feed-forward controller is expected to effectively reduce loads. Figure 3 (left) compares a) deterministic simulation results without flap control (Baseline), b) with the spinner anemometer 1p (IFCS-1P) and c) with the combined 1p and 2p flap control (IFCS-2P). The plot presents out-of-plane blade root bending moment results at the wind speed of 16m/s for 30° yaw angle, 0° inclination angle and ABL shear exponent 0.2. It is seen that 1p variation of the bending moment due to mainly the effect of the yaw misalignment of the flow is substantially reduced when flap control is applied. Further reduction of the load amplitude is achieved when 2p flap control is superimposed on 1p control. Moreover, in Figure 3 (right) the effect of the combined 1p and 2p spinner flap control on the amplitude of the out-of-plane blade root bending moment is shown for the case that yaw angle follows a ramp variation. Yaw angle starts at -45° and linearly increases up to +45°, as shown in the figure. It is seen that although the amplitude of the out-of-plane bending moment changes with the yaw angle when no control is applied, its amplitude remains almost constant when spinner flap feed-forward control is applied.

**Figure 3**: Effect of feed-forward flap controller on out-of-plane moment variation at the wind speed of 16m/s, wind inclination 0° and ABL shear exponent 0.2; a) for yaw angle of 30° (left) and b) for a ramp variation of the yaw angle starting at -45° and increasing up to +45°.(right).

### 4. Assessment of load reduction capabilities

Assessment of load reduction capabilities of the proposed feed-forward control method is performed for the DTU 10MW RWT mounted on the DTU INNWIND jacket at 50m water depth. Both fatigue and ultimate loads are considered in the analyses. Fatigue loads are assessed on the basis of IEC DLC 1.2 (normal operation with normal turbulence conditions NTM and normal sea state NSS), while ultimate loads are calculated through DLC 1.3 (normal operation with extreme turbulence conditions ETM and NSS) and DLC 1.6 (NTM combined with severe sea state SSS). For all wind speeds simulations for yaw angles 0°, +15°, and -15° are performed. The ±15° yaw angles have been considered - higher than the 8° defined in the IEC standard - in order to investigate the performance of the controller in cases with relatively high yaw angles. Thus, lifetime Damage Equivalent Loads (DELs) and ultimate loads are estimated separately for each yaw angle. The simulated conditions are summarized in Table 1. For all wind speeds and yaw angles two turbulent seeds are simulated. So, a one hour overall simulation per wind speed is performed. In Table 2, wave characteristics (in terms of significant wave height $H_s$ and spectral peak period $T_p$) used in the simulations are provided for the different sea conditions and wind speeds.
For the class-IA 10MW RWT simulations are performed (a) for the baseline turbine without IPC and/or flap control (thereafter called baseline), (b) for the turbine with IPC only and (c) for the turbine with combined IPC& spinner anemometer based flap control (IPFCS). The aim of the analysis is i) to assess load reduction capabilities of the combined pitch/flap control loop against pure IPC and baseline power-speed controller and ii) to assess possible pitch actuator duty cycle reduction for IPC as a result of the operation of the flaps. Lifetime fatigue loads are calculated assuming the following Weibull parameters: C=11 m/s and k=2. Individual pitch or/and flap control is usually not recommended in the partial load region since the interaction of the pitch/flap controller with the basic power-speed controller could compromise power production. However, in order to assess load reduction capabilities at lower wind speeds in the present work pitch/flap operation has been also extended to wind speeds below rated reducing the annual energy production by 0.77% and 0.84% with IPC and IPFCS respectively.

### Table 1: DLCs definition for loads assessment

| DLC | Conditions | Wind speeds [m/s] | Yaw angles [deg] |
|-----|------------|------------------|------------------|
| 1.2 | NTM, NSS   | 7, 11, 15, 19, 23| -15, 0, 15       |
| 1.3 | ETM, NSS   | 11               | -15, 0, 15       |
| 1.6 | NTM, SSS   | 11               | -15, 0, 15       |

### Table 2: Definition of sea-state at 50m water depth

|        | NSS | SSS |
|--------|-----|-----|
| **U [m/s]** | 5.00 | 7.00 | 9.00 | 11.00 | 13.00 | 15.00 | 17.00 | 19.00 | 21.00 | 23.00 | all |
| **H_s [m]** | 1.14 | 1.25 | 1.40 | 1.59 | 1.81 | 2.05 | 2.33 | 2.62 | 2.93 | 3.26 | 9.40 |
| **T_p [s]** | 5.78 | 5.67 | 5.71 | 5.81 | 5.98 | 6.22 | 6.54 | 6.85 | 7.20 | 7.60 | 13.70 |

### Table 3: Lifetime DELs comparison of the DTU 10MW RWT between Baseline (absolute values), IPC and IPFCS designs (relative percentage differences) based on DLC1.2 for yaw angles -15°, 0° and 15° calculated for 20 years with Weibull parameters C=11m/s and k=2, Wöhler coefficient m=10 for the blades and m=4 for the tower and the jacket and Nref=10^7 cycles.

| Position       | Direction | Yaw angle = -15° | Yaw angle = 0° | Yaw angle = 15° |
|----------------|-----------|------------------|----------------|-----------------|
|                |           | Baseline | IPC   | IPFCS | Baseline | IPC   | IPFCS | Baseline | IPC   | IPFCS |
| Blade Root     | Flapwise  | 35852    | -35.7%| -34.5%| 31238    | -26.0%| -25.2%| 28279    | -18.8%| -17.9%|
|                | Edgewise  | 26244    | -2.2% | -1.2% | 25890    | -1.0% | -1.1% | 25608    | 0.1%  | 0.0%  |
|                | Pitching  | 531      | -19.3%| 100.3%| 465      | -9.5% | 66.2% | 426      | 0.9%  | 12.9% |
| Tower Base     | Fore-aft  | 48751    | 3.8%  | 4.0%  | 50955    | 1.5%  | 1.6%  | 54702    | -0.4% | 0.0%  |
|                | Side-side | 16470    | 0.7%  | 1.0%  | 16734    | 1.7%  | 1.4%  | 16249    | 0.5%  | 0.0%  |
|                | Yawing    | 22050    | -0.4% | -0.4% | 22191    | 0.0%  | -1.2% | 22415    | -1.2% | -4.0% |
| Jacket         | Leg-axial | 2250    | 3.4%  | 2.9%  | 2380     | 2.1%  | 1.3%  | 2512     | 1.4%  | 0.3%  |
|                | X1-axial  | 331      | 0.8%  | 0.6%  | 329      | 1.3%  | 0.6%  | 323      | 2.0%  | 0.0%  |

In Table 3 the lifetime DELs of the baseline DTU 10MW RWT are presented together with the percentage relative differences of the IPC and IPFCS (compared to baseline) for the 3 considered yaw angles (-15°, 0°, 15°). The percentage of reduction of the DEL of the flapwise bending moment is about the same for IPC and IPFCS. Flapwise bending moment DEL reduction is about 35%, 25% and 18% at -15°, 0° and +15° yaw angle respectively. Overall higher reduction levels are obtained at yaw angles that give the higher DEL, while IPC and IPFCS give almost the same flapwise DEL about
23000kNm. Pure IPC leads to about 1% higher flapwise bending moment reductions. Overall, a slight reduction of 1-2% is noted on the edgewise bending moment DEL by both controllers. Blade torsion moment DEL significantly increases by IPFCS. The level of increase is lower (12.9%) at +15° yaw and higher (100%) at -15° yaw. Depending on the yaw angle, pure IPC has either neutral or decreasing effect on torsion moment. Maximum reduction of 19.3% is obtained at yaw -15°. It is noted that TE flap motion locally increases twisting moment of the blade sections equipped with flaps justifying the torsion moment DEL increase.

A slight increase in the tower fore-aft bending moment DEL is obtained both through IPC and IPFCS. However, the increase in the DEL of the fore-aft bending moment is rather marginal, about 4% at -15° yaw about 1.5% at 0° yaw, and almost negligible at +15° yaw. A marginal increase (0-1.7%) is also noted on the side-side bending moment DEL. Tower yawing moment decreases. Slightly higher load reduction is obtained through IPFCS.

As a result of the higher tower fatigue loads, slightly increased DELs are obtained on the jacket structure as well, both at the main legs at the seabed level (z=-50m) and at the bracers of the lowest X1 (z=-39.355m). The order of increase of the axial force DEL of the jacket leg is about 3% for -15° yaw, about 1.5-2% for 0° yaw and about 1% for 15° yaw. In all cases the IPFCS gives smaller level of increase about 0.5%-1%. Similar results are obtained for the axial force DEL of the X1-connection. The maximum increase is about 2% and again the IPFCS gives slightly smaller level of increase.

Table 4: Ultimate loads comparison of the DTU 10MW RWT between Baseline (absolute values), IPC and IPFCS designs (relative percentage differences) based on DLC1.3 and 1.6 for yaw angles -15°, 0° and 15° (safety factors have been applied)

| Position   | Direction | Yaw angle = -15° | Yaw angle = 0° | Yaw angle = 15° |
|------------|-----------|------------------|--------------|--------------|
|            | Baseline  | IPC   | IPFCS | Baseline  | IPC   | IPFCS | Baseline  | IPC   | IPFCS |
| Blade Root | Flapwise  | 67349 | -3.3% | -4.0% | 65801 | -0.4% | -1.6% | 63342 | 1.6% | -0.7% |
|            | Edgewise  | 26082 | -5.5% | -6.5% | 25980 | 3.2% | -1.4% | 33512 | -5.9% | -6.0% |
|            | Pitching  | 625   | 11.3% | 5.3%  | 639   | -2.8% | 41.1% | 709   | -10.6% | 70.0%  |
|            | Combined  | 68420 | -3.0% | -3.6% | 67082 | -0.8% | -1.6% | 65982 | -1.3% | -4.4%  |
| Tower Base | Fore-aft  | 299097| -2.7% | -2.4% | 297403| -2.1% | -4.1% | 281026| 5.9%  | 2.2%   |
|            | Side-side | 39359 | -0.1% | 4.9%  | 59147 | -10.3%| -7.5% | 71832 | -23.4%| -27.1% |
|            | Yawing    | 44064 | -28.6%| -28.0%| 33725 | -5.9% | -2.6% | 39032 | -13.6%| -14.0% |
|            | Combined  | 299614| -2.7% | -2.4% | 297928| -2.2% | -4.1% | 281428| 5.9%  | 2.2%   |
| Jacket     | Leg-axial | 17534 | -0.2% | 0.0%  | 17571 | -0.6% | -2.1% | 16741 | 3.3%  | 0.6%   |
|            | X1-axial  | 930   | -4.9% | -9.4% | 932   | -9.0% | -10.8%| 873   | -2.6% | -6.1%  |

In Table 4 the ultimate loads of the baseline DTU 10MW RWT are presented together with the percentage relative differences of the IPC and IPFCS (compared to baseline) for the 3 considered yaw angles (-15°, 0°, 15°). Ultimate flapwise bending moment decreases both with IPC and IPFCS. The reduction obtained by IPFCS is 4%, 1.6% and 0.7% at -15°, 0° and +15° yaw angle respectively. Again overall higher reduction levels are obtained at yaw angles that give higher loads. Contrary to DEL comparisons (see Table 3), IPFCS is slightly more efficient by about 1% than pure IPC. It is noted that driving DLC for the flapwise bending moment is DLC 1.3. An almost 6% reduction of the ultimate edgewise bending moment is obtained both with IPC and IPFCS at ±15° yaw angles, while at 0° yaw angle IPFCS reduction is 1.4% as opposed to IPC that increases edgewise ultimate moment by 3.2%. A significant torsion moment increase is obtained through IPFCS, again due to the twisting moment induced by flap motion by 70%, 41% and 5% at yaw angles 15°, 0° and -15° respectively. On the other hand, IPC predicts a torsion moment reduction by 10.6% and 2.8% for +15° and 0° and an increase by 11.3% (higher than IPFCS) at -15° yaw angle. Overall the combined blade bending
moment decreases. Both control options reduce the ultimate combined bending moment mostly at ±15° yaw angles by about 3-4%. Again IPFCS is slightly more efficient for all three yaw angles.

Ultimate tower base fore-aft moment slightly decreases for -15° and 0° yaw angles and increases for +15° yaw angle. At -15° yaw angle both control options predict a similar reduction of about 2.5%, while at 0° yaw angle higher level of load reduction (4.1%) is obtained through IPFCS compared to IPC (2.1%). At +15° yaw angle the increase in the ultimate fore-aft moment is 5.9% with IPC and 2.2% with IPFCS. Ultimate tower base side-side moment (which is significantly lower than the fore-aft moment) decreases by about 25% with both control options at +15° yaw angle, and by about 8-10% at 0° yaw angle. At -15° yaw angle there is no effect on the ultimate side-side moment by pure IPC, while a 5% increase is obtained through IPFCS. Ultimate yaw moment significantly decreases by about 28% and 14% at -15° and +15° yaw angles respectively. At 0° yaw angle IPC reduces yaw moment by 5.9% while IPFCS by 2.6%. The level of the combined tower bending moment reduction is exactly the same with that of the tower fore-aft moment component.

As a result of the slightly reduced ultimate tower loads, a reduction in the ultimate axial loads on the jacket structure is also obtained, mainly at the bracers of the lowest X-connection X1 (z=-39.355m). The level of reduction is about 3-5% through IPC and about 6-10% through IPFCS. The ultimate axial load at the main legs at the seabed (z=-50m) is not affected at -15° yaw angle, slightly decreases by 0.6% through IPC and by 2.1% through IPFCS, while it slightly increases at +15° yaw angle by 3.3% through IPC and by 0.6% through IPFCS.

In Figure 4 and Figure 5 the statistics (minimum (min), maximum (max) and standard deviation (sdv)) of the flap and pitch angles of the different control strategies are presented for the NTM conditions. When combined IPFCS is applied, the flap angle reaches the saturation limit of +10° only at wind speeds higher than 15m/s (see Figure 4). The standard deviation of the flap angle is lower than the limit angle of 10° (goes up to 7°) indicating that flap motion stays well below the limit angles most of the time and rarely hits the upper bound. By comparing the flap angles for the three different yaw angles it is seen that flap activity increases from the negative yaw angles to the positive ones. It is also seen that min/max flap curves are not symmetrically distributed around 0° yaw. The reason for the above behaviour is that the combined effect of yaw misalignment, wind shear and nacelle tilting leads to overall lower amplitudes of the out-of-plane bending moment at negative yaw angles compared to the 0° yaw case. On the other hand higher amplitudes are obtained at positive yaw angles.

![Figure 4: Flap angle statistics (DLC1.2 –NTM, NSS).](image)

In Figure 5 the sdv of the pitch motion is shown for all control strategies. It is seen that the sdv of the pitch motion in the full load region increases (slightly at -15° yaw, about 50% at 0° yaw and about 100% at 15° yaw) when pure IPC is applied as compared to the “baseline” (no load control) case. When IPFCS is used, the level of increase of the pitch motion with respect to the “baseline” is significantly reduced (about 20% at 0° yaw angle and 30% at 15° yaw angle) as compared to the pure IPC especially for 0° and 15° yaw angles.
In Table 5 the Actuator Duty Cycle (ADC) is compared for the 3 yaw angles (0, \(\pm 15^0\)) and for the 3 control methods (Baseline, IPC, IPFCS). Lifetime ADC is calculated assuming the same Weibull parameters \(C=11\) m/s and \(k=2\). The ADC significantly increases by both active load control methods, by 1058-1333\% through IPC and by 800-934\% through IPFCS, compared to the Baseline design. The IPFCS reduces ADC by 40, 29 and 32\% for -15\^0, 0\^0 and 15\^0 yaw angles respectively, compared to the isolated IPC. So a saving of the duty cycle of the pitch actuator is achieved when spinner anemometer based flap control is engaged.

**Table 5**: Lifetime Actuator Duty Cycle comparison based on DLC1.2 (Weibull parameters \(C=11\) m/s and \(k=2\))

| Yaw angle=\(-15^0\) | Yaw angle=0\(^0\) | Yaw angle=15\(^0\) |
|----------------------|-------------------|-------------------|
| **Baseline**         | **IPC**           | **IPFCS**         |
| ADC [\(-\)]         | Diff [%]          | ADC [\(-\)]      | Diff [%]          | ADC [\(-\)] | Diff [%] |
| 100484               | -                 | 119994            | -                 | 105837       | -         |
| 1439861              | 1333\%            | 1390113           | 1058\%            | 1440507      | 1261\%    |
| 1026247              | 921\%             | 1079793           | 800\%             | 1094272      | 934\%     |

5. **Conclusions**

A preliminary assessment of a spinner based feed-forward flap controller, targeting to reduced blade loads is performed in the paper. The controller is designed to mitigate blade out-of-plane load variations driven by asymmetries of the inflow (yaw, inclinations and shear). As such, it can only be regarded as complementary to the standard feed-back control which is targeting to p multiple load variations due to rotationally sampled turbulence. Application of the new feed-forward controller in conjunction with a standard IPC loop based on measurements of the out-of-plane moment of the three blades indicates that fatigue loads reduction of 18-36\% on the blades can be achieved with about 30\% lower pitch activity compared to the pure IPC. However, an increase of the blade root torsion moment, as a result of the action of the flaps has been also recorded. On the other hand, the new controller appears to be slightly more efficient (1-2\%) than standard feed-back IPC in reducing blades’ ultimate loads (reduction 0.7-4\% obtained). The above conclusion is drawn on the basis of ultimate loads analysis performed for the driving, normal operation DLCs 1.3 (extreme turbulence) and 1.6 (severe sea conditions). It is also noted that since the controller is designed to mitigate rotor loads driven by deterministic inflow asymmetries, is expected to perform better in other ultimate DLCs such as 1.4 (coherent gust with direction change) or 2.3, 3.2, 4.2 (extreme operating gust). Overall the IPFCS does not seem to outperform pure IPC, especially if additional costs and complications related to flaps installation, operation and maintenance are considered. The effectiveness of the combined feed-forward IPFCS could be improved if combined with self-learning/self-adaptive procedures. This way better account of dynamic effects could be achieved.
6. Acknowledgments
The work presented in paper was partially funded from the European Community’s Seventh Framework Program under grant agreement No. FP7-ENERGY-2012-1-2STAGE-308974 (INNWIND.EU).

References
[1] INNWIND: INNovative WIND conversion systems (10-20MW) for offshore applications, EU project, http://www.innwind.eu.
[2] Bossanyi E, Savini B, Iribas M, Hau M, Fischer B, Schlipf D, T van Engelen, Rossetti M and Carcangiu C E 2012 Advanced controller research for multi-MW wind turbines in the UPWIND project, Wind Energy Volume 15 pp 119–145.
[3] Bossanyi E A, Kumar A, Hugues-Salas O 2014 Wind turbine control applications of turbine-mounted LIDAR, Journal of Physics: Conference Series, Volume. 555, No. 012011.
[4] Schlipf D, Schlipf D J, Kühn M 2013 Nonlinear model predictive control of wind turbines using LIDAR, Wind Energy, Volume 16, pp 1107–1129.
[5] Wortmann S, Geisler J, Konigorski U, 2016 Lidar-Assisted Feed-forward Individual Pitch Control to Compensate Wind Shear and Yawed Inflow. Journal of Physics: Conference Series, Volume 753, No 052014.
[6] Lackner M A, van Kuik G 2010 A comparison of smart rotor control approaches using trailing edge flaps and individual pitch control, Wind Energy; 13:117–34.
[7] Bak P 2011, Unsteady Flow Modeling and Experimental Verification of Active Flow Control Concepts for Wind Turbine Blades PhD Thesis, DTU RISOE.
[8] Bernhammer L, van Kuik G, De Breuker R 2016 Fatigue and Extreme Load Reduction of Wind Turbine Components using Smart Rotors, J. WindEng.Ind.Aerodyn.154, pp. 84–95.
[9] Barlas T K, van der Veen G, van Kuik G 2012 Model predictive control for wind turbines with distributed active flaps: incorporating inflow signals and actuator constraints. Wind Energy, vol 15(5), pp. 757–771.
[10] Bergami L, Poulsen N 2014 A smart rotor configuration with linear quadratic control of adaptive trailing edge flaps for active load alleviation, Wind Energy; Volume 18, Issue 4, April 2015, Pages 625–641.
[11] Pedersen T F, Demurtas G, Zahle F, 2015 Calibration of a spinner anemometer for yaw misalignment measurements. Wind Energy, Volume 18, Issue 11, pp 1933–1952.
[12] Pedersen T F, Demurtas G, Sommer A, Højstrup J 2014 Measurement of rotor centre flow direction and turbulence in wind farm environment. Journal of Physics: Conference Series, Vol. 524, No.1, 012167.
[13] IEC 61400-3: 2008. Wind Turbines-Part 3: Design Requirements for Offshore Wind Turbines.
[14] Bak C, Zahle F, Bitsche R, Kim T, Yde A, Henriksen L C, Natarajan A, Hansen M H 2013 Description of the DTU 10MW Reference Wind Turbine. DTU Wind Energy Report-I-0092.
[15] Tsiantas F, Manolos D I, Machairas T, Karakalas A, Riziotis V A, Saravanos D, Voutsinas S G 2016 Assessment of fatigue load alleviation potential through blade trailing edge morphing J. Phys.: Conf. Ser. 753 042020.
[16] Lars O.Bernhammer, Gijs A.M.van Kuik, RoelandDe Breuker 2016 Fatigue and extreme load reduction of wind turbine components using smart rotors, Journal of Wind Engineering and Industrial Aerodynamics, Volume 154, pp 84-95.
[17] Manolos D I, Riziotis V A, Voutsinas S G 2015 Assessing the importance of geometric non-linear effects in the prediction of wind turbine blade loads Computational and Nonlinear Dynamics Journal, Vol. 10, 041008.
[18] Barlas T, Jost E, Pirrung G, Tsiantas T, Riziotis V, Navarkar S T, Lutz T and van Wingerden J-W 2016 Benchmarking aerodynamic prediction of unsteady rotor aerodynamics of active flaps on wind turbine blades using ranging fidelity tools. Journal of Physics: Conference Series.