https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

WIND ENERGY SCIENCE DISCUSSIONS

Performance of Multi-Band MDE-Based Virtual Sensing for Estimating Lifetime Fatigue Damage Equivalent Loads for the IEA 15 MW Reference Wind Turbine

Mads Greve Pedersen[1,2], Jennifer Marie Rinker[3], Isaac Farrereas Alcover[2], and Jan Høgsberg[1]

[1]Department of Civil and of Mechanical Engineering, Technical University of Denmark, 2800 Kongens Lyngby, Denmark
[2]COWI A/S, 2800 Kongens Lyngby, Denmark
[3]Department of Wind and Energy Systems, Technical University of Denmark, 2800 Kongens Lyngby, Denmark

Correspondence: Mads Greve Pedersen (madg@dtu.dk)

Abstract. Growing Offshore Wind Turbines (OWTs) are increasingly vulnerable to fatigue damage, motivating stress monitoring at critical, often inaccessible locations, for asset integrity management and life-extension. Virtual sensing methodologies, such as multi-band Modal Decomposition and Expansion (MDE), offer a solution by extrapolating measurements from sensors at accessible locations. However, existing MDE studies often model the Rotor-Nacelle-Assembly (RNA) as a lumped mass inertia, thereby ignoring blade flexibility and rotor operation. This leads to errors in estimated strains or stresses, particularly close to the tower top, where blade vibrations significantly influence the structural response. Moreover, neglecting blade flexibility can also lead to inaccurate tower mode shapes, causing errors not limited to the tower top.

The present paper investigates the errors of multi-band MDE estimates resulting from modelling the RNA as a lumped inertia. To this end, a dataset of HAWC2 simulations covering the Fatigue Limit State (FLS) design life of the IEA Wind 15-Megawatt Offshore Reference Wind Turbine with a monopile foundation (IEA 15-MW RWT) is considered. Utilizing this dataset, multi-band MDE is used to estimate section moments along the entire supporting structure of the IEA 15-MW RWT. These estimates are compared against the true response extracted from the dataset in terms of Damage Equivalent Loads (DELs) and Damage Equivalent Stresses (DESs) combined for the individual Design Load Cases (DLCs). Additionally, the error of the MDE estimates is assessed for individual 10-minute time series from the same dataset. Based on the combined DELs and DESs, it is concluded that the MDE used in the present work performs well for long-term estimates, except in the area around the tower top, where blade vibrations and 3P effects significantly impact the quality of the estimates. It is shown that the MDE errors for the individual 10-minute time series are generally in the range of $\pm 5\,\%$. However, the error is as high as $180\,\%$ in the tower top, where the impact from the lumped inertia RNA model is large. Finally, the error of the MDE estimates exhibits wind speed dependency. This underlines the inherent limitation in the MDE, which assumes a linear and time-invariant response and thus cannot capture the temporal variability of the dynamic model due to changing operational and environmental conditions. In conclusion, multi-band MDE provides accurate estimates of section moments across most of the IEA 15-MW RWT supporting structure, though without capturing the effects of operational and environmental variability. Furthermore, improvements are necessary to effectively capture the effects of blade flexibility, particularly near the tower top.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

eawe european academy of wind energy
Open Access WIND ENERGY SCIENCE DISCUSSIONS

1 Introduction

During recent decades, wind turbines have been consistently growing in size, and modern Offshore Wind Turbines (OWTs) planned for deployment, such as the Vestas V236-15MW, now have a power production of up to 15 MW and rotor diameters approaching 240 m. The growth in wind turbine size results in highly flexible supporting structures (tower, transition piece, and foundation), with the lowest natural frequencies approaching the quasi-static frequency domain. This makes them susceptible to dynamic excitation from turbulence and wave loads, resulting in designs that are increasingly vulnerable to fatigue damage (Zou et al., 2023). At the same time, the ~~most recent decades~~ have experienced the emergence of Structural Health Monitoring (SHM), where data from sensors installed in a given structure is applied to inform operation and maintenance (O&M) strategies, in asset integrity assessments, and lately also for the assessment of potential life-extension through monitoring of strain histories at fatigue critical locations. However, for offshore structures, these critical locations are often located sub-soil or sub-sea, where strain sensors cannot be installed or maintained post-erection. Furthermore, pre-installed sensors are likely to be damaged during erection, while any undamaged strain sensors tend to fail after a few years (Toftekær et al., 2023). To overcome these challenges, virtual sensing has gained traction in SHM of OWTs, where structural responses (stresses or strains) are estimated by so-called virtual sensors, in which physical (above-sea) sensor signals are extrapolated to critical locations by a digital process model.

According to Zou et al. (2023), virtual sensing process models can be separated into two main categories. The deterministic approach uses model-based extrapolation, from which strain responses are estimated based on measurements from e.g. accelerometers, inclinometers, strain gauges, or 3D point tracking (Baqersad et al., 2015). The alternative probabilistic approach applies state-estimation from Kalman filters (Maes et al., 2016), augmented Kalman filters (Vettori et al., 2023), dual Kalman filters (Eftekhar Azam et al., 2015), or, more recently, from a generic latent force model (Bilbao et al., 2022; Zou et al., 2023). Lately, the use of neural networks has also entered the field of virtual sensing, e.g. when physics-guided learning from SCADA data and 10-minute acceleration statistics are used to estimate damage equivalent moments (de N Santos et al., 2023).

The present work applies the predominant deterministic model-based expansion method: Modal Decomposition and Expansion (MDE). The concept of virtual sensing by MDE was initially introduced for dynamic strain estimation in OWTs in the pioneering work by Iliopoulos et al. (2014, 2016), and subsequently extended in Iliopoulos et al. (2017) to multi-band MDE, where strain histories are estimated individually in separate frequency bands (quasi-static, low-frequency and high-frequency) based on measurements from strain gauges (for the quasi-static band) and accelerometers (for low- and high-frequency bands) using mode shapes and static deflection shapes from a Finite Element (FE) beam model with a lumped Rotor-Nacelle-Assembly (RNA) inertia. This approach has been further developed by Noppe et al. (2016), using a SCADA-driven thrust load model for quasi-static band estimation, and by Henkel et al. (2021) for estimating and validating sub-soil fatigue stresses by dual-band MDE with experimental mode shapes and Operational Deflection Shapes (ODSs).

The use of experimental ODSs and mode shapes is also applied for strain estimation using a synthetic response of the National Renewable Energy Laboratory (NREL) 5 MW Reference Wind Turbine with an OC4 jacket substructure in Henkel et al. (2020), indicating less good performance for strains in the braces due to the occurrence of local brace modes and extrapolation of the wave loading. Augustyn et al. (2021) attempts to improve the accuracy for jacket structures by including sensors in a few submerged braces and applying the wave load generated Ritz vectors from Skafte et al. (2017) and local brace modes in MDE.

Recently, Toftekær et al. (2023) have investigated the use of rotations obtained from filtered acceleration measurements in combination with Ritz vectors to estimate quasi-static stresses at the mud line of an 8.4 MW offshore wind turbine, and thereby quantifying the accuracy of the estimated stress range histories for different modal expansion configurations. Subsequently, Fallais et al. (2024) have investigated the accuracy of a single-model MDE configuration for estimating damage equivalent stresses in the lower part of an OWT supporting structure, concluding that varying operational conditions across 2000 10-minute time series only have a minor impact on the estimate precision.

Studies performing strain/stress estimates for monopile-supported OWTs, using MDE with mode shapes and Ritz vectors from an FE model (Iliopoulos et al., 2017; Noppe et al., 2016; Toftekær et al., 2023; Fallais et al., 2024), commonly consider the RNA as a lumped inertia. Consequently, the tower mode shapes that include blade motions are estimated inaccurately, and the influence of blade flexibility and rotor operation (e.g., blade vibrations, 3P effects, and operational variability) on the tower vibrations are not accounted for in the MDE. Given the inherent coupling between the tower and the blades, this simplification can introduce errors in the strains or stresses estimated in the supporting structure. Furthermore, the MDE performance is usually evaluated in the lower part of the supporting structure, where the influence from errors in the RNA model is less pronounced, giving an erroneous impression of their importance. Finally, these studies do not include wave loading separately in the MDE, thus assuming that wave loads are either insignificant or that the associated dynamic mode shapes can well capture their effects. However, these simplifications will lead to errors in the estimated strains and stresses in areas of the OWT supporting structure exposed to substantial wave loading.

The present paper addresses the errors associated with representing the rotor by a lumped RNA inertia and its influence on the MDE prediction of Damage Equivalent Loads (DELs) and Stresses (DESs) in modern scale offshore wind turbines. Furthermore, it investigates how wave loads can be explicitly included in the Ritz vectors for quasi-static and low-frequency estimation. For that precise purpose, uncertainties from soil modelling, variations in the OWT's as-built conditions, and measurement noise from sensors have been eliminated by considering the synthetic response data in Pedersen et al. (2025), which is an open access dataset (available for download at https://doi.org/10.11583/DTU.24460090) containing response simulations covering the Fatigue Limit State (FLS) design life of the IEA Wind 15-Megawatt Offshore Reference Wind Turbine with a monopile foundation (IEA 15-MW RWT) version 1.1.6 (Gaertner et al., 2020a).

The structure of the paper is as follows. Section 2 presents the data from Pedersen et al. (2025), the assessment of the performance of the IEA 15-MW RWT, and a relative lifetime damage calculation made for the individual design load cases included in Pedersen et al. (2025). Section 3 explains the multi-band MDE methodology used in the present work and the Finite Element (FE) model used to extract mode shapes and Ritz vectors for the MDE. In Section 4 the MDE is used for the estimation of Damage Equivalent Loads (DELs) and Stresses (DESs) and the MDE errors are quantified and discussed, with the final Section 5 providing conclusions and perspective for future work.

2 Data

The present work is based on synthetic wind turbine response data from the online open-access dataset "*IEA-15MW-RWT-Monopile HAWC2 Response Database*" (Pedersen et al., 2025), which is available for download at https://doi.org/10.11583/DTU.24460090 along with the relevant documentation, model- and input files, and scripts for reading and sorting data. The dataset comprises 4902 HAWC2 output files covering the Fatigue Limit State (FLS) design life of the IEA 15-MW RWT version 1.1.6, which is described in Gaertner et al. (2020a). The metocean data used for the simulations performed by Pedersen et al. (2025) is based on the metocean assessment performed for Energinet Eltransmission A/S in DHI (2023a), DHI (2023b), and DHI (2023c). The individual HAWC2 output files contain time series data from 898 sensors hereunder environmental- and operational data (e.g. hub wind speed, wave height, rotor speed, blade pitch angles, torque, thrust, and power production) and structural response data in terms of displacements, rotations, accelerations, forces, and moments in the individual structural members.

In the following sections, the IEA 15-MW RWT and the Design Load Cases (DLCs) considered in Pedersen et al. (2025) are described briefly, before the assessment of the IEA 15-MW RWT performance is conducted. Finally, the relative lifetime damage from the individual DLCs is calculated for the IEA 15-MW RWT, based on Damage Equivalent Loads (DELs).

2.1 IEA Wind 15-Megawatt Offshore Reference Wind Turbine

The IEA 15-MW RWT is a monopile-founded offshore wind turbine with a rated power of 15 MW and a cut-in, rated, and cut-out wind speed of $V_{in} = 3$ m/s, $V_r = 10.69$ m/s, and $V_{out} = 25$ m/s, respectively. The supporting structure consists of a 75 m monopile with an embedment depth of 45 m, a 15 m transition piece, and a 129.4 m tower, see Figure 1. The design of the supporting structure has been derived from the Ultimate Limit State (ULS) and modal analysis following a soft-stiff approach (Gaertner et al., 2020a), thus locating the natural frequency of approximately 0.17 Hz for the first order tower bending modes between the 1P and 3P rotor frequencies. The design of the IEA 15-MW RWT is available from the Github repository in Gaertner et al. (2023).

2.2 Modelling

As previously stated, the database in Pedersen et al. (2025) comprises synthetic wind turbine response data obtained by HAWC2 simulations, whereby it inherits the limitations and assumptions associated with HAWC2. HAWC2 calculates the aerodynamic loads based on Blade Element Momentum (BEM) theory. The implementation of BEM theory in HAWC2 has been extended to account e.g. for dynamic inflow, dynamic stall, and the rotor's yaw and tilt (Larsen and Hansen, 2021). In the present work, the turbulent wind field is modelled using the Mann Turbulence generator which is directly linked with HAWC2. The tower shadow effect is accounted for using a potential flow model, and the wind shear is implemented using the standard power law expression

$$V(z) = V(z_r)\left(\frac{z}{z_r}\right)^{\alpha} \tag{1}$$

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

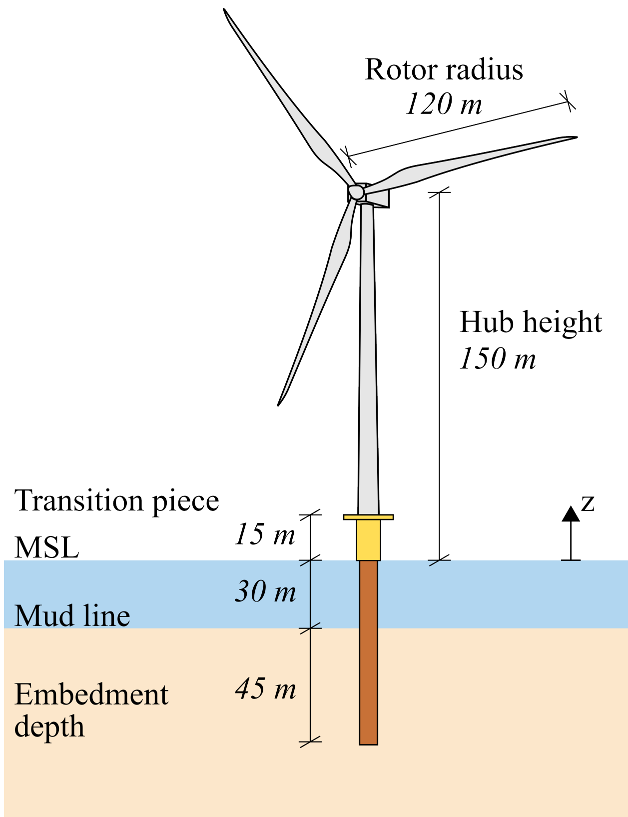

Figure 1. Overview of the IEA 15-MW RWT (data from Gaertner et al. (2020a). The RWT has a hub height of 150 m above the Mean Sea Level (MSL) and a rotor radius of 120 m. The water depth at the chosen site is 30 m. The supporting structure of the RWT consists of a 75 m monopile with an embedment depth of 45 m, a 15 m transition piece, and 129.4 m tower.

where $V(z)$ is the wind speed across the elevation z above the Mean Sea Level (MSL), z_r is the reference elevation at which the wind speed $V(z_r)$ is known (in this case at hub-height), while $\alpha = 0.08$ from the metocean assessment in DHI (2023a).

The structural modelling in HAWC2 is based on a multi-body formulation, where each body is an assembly of Timoschenko beam elements. Thus, the formulation for the structural members accounts for large deflections and rotations, geometrical non-linearities, and shear deformations (Larsen and Hansen, 2021). The soil model implemented in the model for simulations performed by Pedersen et al. (2025) utilize the lateral linear soil springs presented in Table 1. In HAWC2, the hydrodynamic forces acting on the monopile are calculated using Morison's formula. The present work ignores the current when calculating hydrodynamic forces, and the water kinematics are calculated based on the irregular Pierson–Moskowitz wave spectrum, utilising the significant wind speed-dependent wave height and the wave period from the metocean assessment in DHI (2023c).

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

Table 1. Lateral spring stiffness of soil in node n of the embedded part of the monopile (presented in Figure 7) as a result of the z-coordinate presented in Figure 1. Defined in Appendix B.2 in Gaertner et al. (2020a) and used by Pedersen et al. (2025).

n [-]	z [m]	$k_{soil,n}$ [kN/m]
10	− 30	3.54E+06
9	− 35	6.65E+06
8	− 40	9.76E+06
7	− 45	1.29E+07
6	− 50	1.60E+07
5	− 55	1.91E+07
4	− 60	2.22E+07
3	− 65	2.53E+07
2	− 70	2.84E+07
1	− 75	3.15E+07

2.3 Load Cases

The Design Load Cases (DLCs) for the Fatigue Limit State (FLS) of bottom-fixed OWTs are described in IEC 61400-3-1:2019 (IEC, 2019b). In Pedersen et al. (2025), the implementation of the DLCs follows Natarajan et al. (2016), with the input values used for the HAWC2 simulations presented in Table 2. The number of simulations in Table 2 is a result of the operational and environmental variability needed to capture the individual load cases, e.g. DLC 1.2 considers 11 different *wind speeds* at three different *yaw errors*, *wind-wave misalignments*, and *Mean Water Levels (MWL)*. Finally, six seeds are used to secure numerical robustness for the simulation of both turbulence and irregular waves. In total, this gives $11 \times 3 \times 3 \times 3 \times 6 = 1782$ simulations for DLC 1.2. According to DHI (2023b), the tidal effects at the chosen site are weak and thus only the simulations where the Mean Water Level (MWL) is equal to the Mean Sea Level (MSL) are considered, thereby discarding simulations where MWL is at either Lowest (LAT) or Higest (HAT) Astronomical Tide in the analysis conducted for the present paper.

To evaluate the lifetime damage contribution from the individual HAWC2 simulations, their representative durations are calculated based on the joint probability of the DLC occurrence and the environmental parameters: Wind speed, yaw error, and wind-wave misalignment. An overview of the input for the duration of the individual simulations is presented in Table 3. The duration of the individual DLCs is based on the recommendations in Section 7 of IEC (2019b). The application of these recommendations in the present work is presented below.

– DLC 1.2: It is expected that the wind turbine will be available for operation at normal conditions for 90 % of its 20-year lifetime. In the present work, this is interpreted as DLC 1.2 occurring 90 % of the time the wind speed falls within the cut-in and cut-out wind speed ($V_{in} = 3$ m/s and $V_{out} = 25$ m/s).

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

Table 2. Overview of DLCs from IEC (2019b) considered in Pedersen et al. (2025).

DLC	Description	Environmental parameters			No. Simulations
1.2	Power production in normal conditions	Wind speed	[4:2:24]	[m/s]	1782
		Yaw error	-10, 0, 10	[deg]	
		wind-wave misalignment	-22.5, 0, 22.5	[deg]	
		Sea level	LAT, MSL, HAT	[m]	
2.4	Power production with large yaw errors in normal conditions	Wind speed	[4:2:24]	[m/s]	132
		Yaw error	-20, 20	[deg]	
		wind-wave misalignment	0	[deg]	
		Sea level	MSL	[m]	
3.1	Start-up in normal conditions	Wind speed	3, 10.69, 25	[m/s]	18
		Yaw error	0	[deg]	
		wind-wave misalignment	0	[deg]	
		Sea level	MSL	[m]	
4.1	Shut-down in normal conditions	Wind speed	3, 10.69, 25	[m/s]	18
		Yaw error	0	[deg]	
		wind-wave misalignment	0	[deg]	
		Sea level	MSL	[m]	
6.4	Parked turbine with idle rotor in normal conditions	Wind speed	[4:2:34]	[m/s]	576
		Yaw error	-8, 8	[deg]	
		wind-wave misalignment	0	[deg]	
		Sea level	LAT, MSL, HAT	[m]	
7.2	Fault - locked rotor at azimuth angle 0°, 30°, 60°, and 90° in normal conditions	Wind speed	[4:2:24]	[m/s]	2376*
		Yaw error	-10, 0, 10	[deg]	
		wind-wave misalignment	0	[deg]	
		Sea level	LAT, MSL, HAT	[m]	

*208 simulations of the simulations for DLC 7.2 failed to converge and are disregarded in the further work.

– DLC 2.4: For operation during the occurrence of fault or loss to the electrical network, IEC (2019b) suggests that the duration may be applied as follows: 10 shut-downs per year for overspeed event, 24 hours per year of operation for events with yaw error, 24 hours per year of operation for events with pitch error, and 20 times per year with loss of electrical network connection. In Pedersen et al. (2025) only the fault "*operation for events with yaw error*" is modelled.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

Table 3. Input for joint probability used for calculating the expected life-time duration for the individual time series available in Pedersen et al. (2025).

DLC	Exposure	Wind speed	Yaw error	Wind-wave misalignment
1.2	90 %	$p(V)$ **for** $V \in [\ 3, 25]$ m/s	1/4 , 1/2 , 1/4	1/3, 1/3, 1/3
2.4	0.57 %	$p(V)$ **for** $V \in [\ 3, 25]$ m/s	1/2 , 1/2	1
3.1	0.35 %	1000/1100 , 50/1100 , 50/1100	1	1
4.1	0.35 %	1000/1100 , 50/1100 , 50/1100	1	1
6.4		$p(V)$ **for** $V \in [25, 35]$ m/s	1/4 , 1/2 , 1/4	1
7.2	8.7 %	$p(V)$ **for** $V \in [\ 3, 25]$ m/s	1/4 , 1/2 , 1/4	1

To account for the damage occurring during the remaining fault conditions specified for DLC 2.4, the duration is adjusted to 50 hours per year of operation (0.57 % of the time the wind speed falls within the V_{in} and V_{out}) in the present work.

- DLC 3.1 and 4.1: IEC (2019b) states that start-up/shut-down in normal conditions (DLC 3.1/4.1) can be expected to occur 1100 times annually: 1000 times at the cut-in wind speed, 50 times at the rated wind speed and 50 times at the cut-out wind speed (0.35 % of the total life for each of DLCs 3.1 and 4.1).

- DLC 6.4: In the present analysis, DLC 6.4 is considered to occur only when the wind speed at the hub exceeds the cut-out wind speed $V_{out} = 25$ m/s. As this DLC is the only one expected to occur for wind speeds above V_{out}, the duration of DLC 6.4 is assumed to be the total duration the hub wind speed exceeds the cut-out wind speed.

- DLC 7.2: As IEC (2019b) does not specify a duration for DLC 7.2, this work defines its duration as the time not accounted for by previous DLCs within the operational wind speed range from V_{in} to V_{out}, which is 8.7 %.

The wind speed's probability density is assumed to follow the Weibull distribution

$$p(V) = \frac{k}{A}\left(\frac{V}{A}\right)^{k-1} \exp\left(-\left(\frac{V}{A}\right)^{k}\right) \tag{2}$$

with the omnidirectional Weibull parameters $k = 2.35$ and $A = 9.91$ m/s given in DHI (2023b) for a mean wind speed $\bar{V}_{10} = 8.79$ m/s at 10 m above MSL. These values are corrected for the hub height using a wind shear for the Normal Wind Profile (NWP) presented in (1). According to IEC (2019b), only part of the wind speed spectrum is considered, namely $V_{hub} \in [V_{in}, V_{out}]$ for DLC 1.2, 2.4, 3.1, 4.1, and 7.2 and $V_{hub} \in [V_{out}, 0.7\,V_{ref}]$ for DLC 6.4. As such, it is assumed that there is no contribution to the fatigue life consumption for $V_{hub} \notin [V_{in}, 0.7\,V_{ref}]$, where $V_{ref} = 50$ m/s is the reference wind speed for wind turbine class 1 (IEC, 2019a).

Although the DLCs described above do not exhaustively represent the scenarios occurring during the actual lifetime of an OWT, they provide an overview of the fatigue-life impact from the most common and governing operating scenarios.

2.4 Performance of the IEA 15-MW RWT

When performing Modal Decomposition and Expansion (MDE) modal truncation is needed due to a limited number of sensors. Furthermore, a finite number of Ritz vectors can be included to assess the quasi-static part of the response. Hence, it is important to have an overview of the different governing loads to be accounted for in the response estimates. This section gives an example of how diverse operational and environmental conditions can impact the Damage Equivalent Loads (DELs) of the IEA 15-MW RWT, and hence contribute differently to lifetime damage. Specifically, statistical values of relevant operational parameters and the tower base Fore-Aft (FA) and Side-Side (SS) section moments are considered during normal power production (DLC 1.2).

In Figure 2, the statistics (minimum, mean, maximum) of the operational parameters (rotor speed, electrical power, generator torque, thrust, and pitch angle) and the wave amplitude are presented, while Figure 3 shows the associated statistics of the tower base FA and SS section moments and the 1 Hz Damage Equivalent Loads (DELs) for the individual HAWC2 time series (evaluated by (6)) for DLC 1.2. The operational parameters in Figure 2 are compared with steady-wind rotor performance values from Gaertner et al. (2023), generated by the *Wind-plant Integrated System Design and Engineering Model* (WISDEM), which uses the aeroelastic code OpenFAST.

Figure 2(a-e) shows that the mean values generally coincide well with the WISDEM output, and Figure 2(f) verifies that the minimum- and maximum wave amplitudes follow the development of the input significant wave height. The greatest discrepancies are observed for the thrust in Figure 2(d) and the pitch angle in Figure 2(e). The discrepancies in the thrust and pitch angle are due to: steady versus turbulent operation and the ElastoDyn beam model used in the WISDEM calculation (Gaertner et al., 2020b) not including a torsional degree of freedom (Rinker et al., 2020). The generally good match between the models indicates that the HAWC2 model may be used for further analysis.

The statistical values for the tower base FA moment presented in Figure 3(a) follow the thrust curve from Figure 2(d) as expected. The DELs associated with the tower base FA moment presented in Figure 3(c) generally increase with both the wind speed and turbulence. However, they plateau at wind speeds from approximately $12 - 16$ m/s, in which range the blades start to pitch (see Figure 2(e)). This illustrates that the DELs in the FA direction at the tower base are primarily governed by quasi-static wind loading, while operational parameters (e.g., the pitch angle) also affect the damage. Similarly to the statistical values of the tower base FA moment, the mean values of the tower base SS moment presented in Figure 3(b) follow the generator torque curve in Figure 2(c). The minimum and maximum values of the tower base SS moment are symmetric around the mean value with increasing amplitudes for increasing wind speeds. The associated DELs in Figure 3(d) also increase with the wind speed and turbulence. Furthermore, Figure 3(d) shows that the variance of the DELs increases with the wind speed up to the rated wind speed, from where it is rather significant.

To assess the cause of the high variance, the time histories of the tower base SS moment, wind speed (in the SS direction), and wave height associated with the minimum and maximum DELs for the wind speed of 14 m/s are presented in Figure 4. Considering the moment time series in Figure 4(a) and the related PSD in Figure 4(b), it is concluded that DELs are mainly driven by the first tower SS mode. There is not a significant difference in the frequency content of the wind around the natural

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

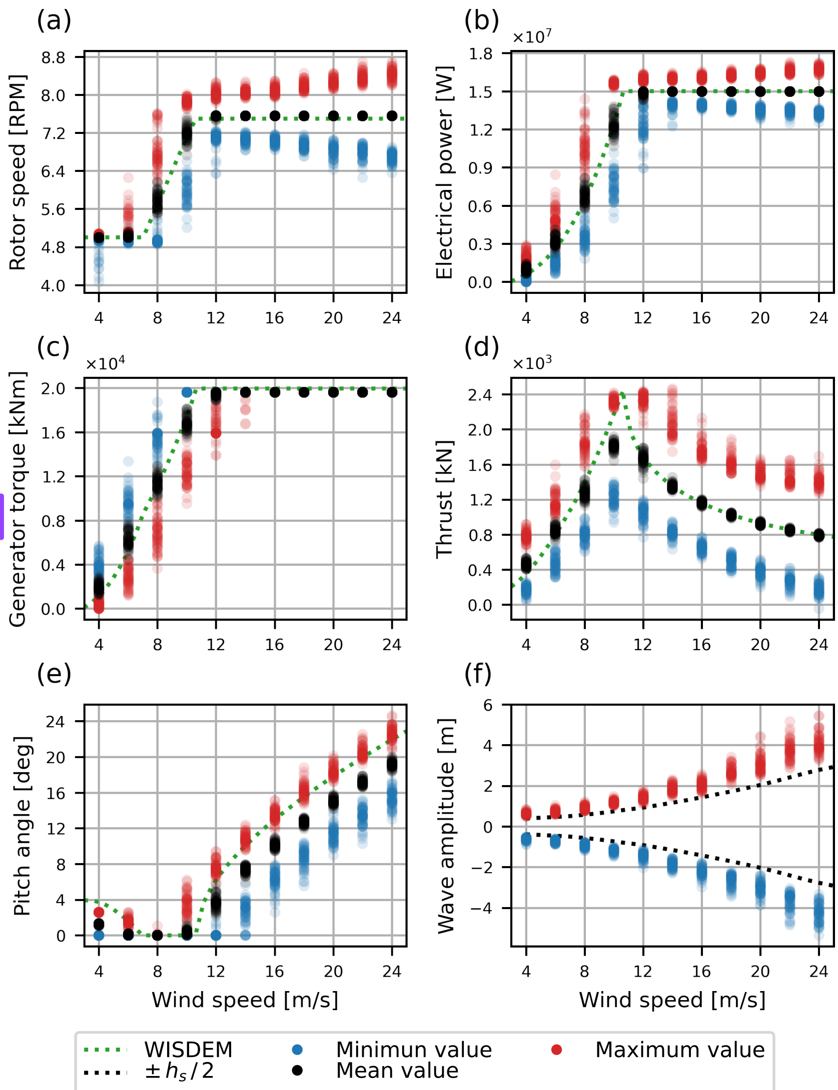

Figure 2. Statistical values (minimum, mean, maximum) for selected operational parameters (a) rotor speed, (b) electrical power, (c) generator torque, (d) thrust load, (e) pitch angle, and (f) wave amplitude depicted across the wind speed at the hub, calculated for the HAWC2 time series covering DLC 1.2 for the MWL equal to MSL.

frequencies of the first order tower bending modes. However, the mean wind speed in the SS direction is significantly higher for the maximum DEL than for the minimum DEL, due to the $-10°$ yaw error. Furthermore, the waves have an angle-of-attack of $-32.5°$ for the maximum DEL, whereas it is $0°$ for the minimum DEL. Thus, the variation in DEL magnitude is caused by the excitation of the first tower SS mode occurring for the maximum DEL, while not for the minimum DEL, likely due to

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

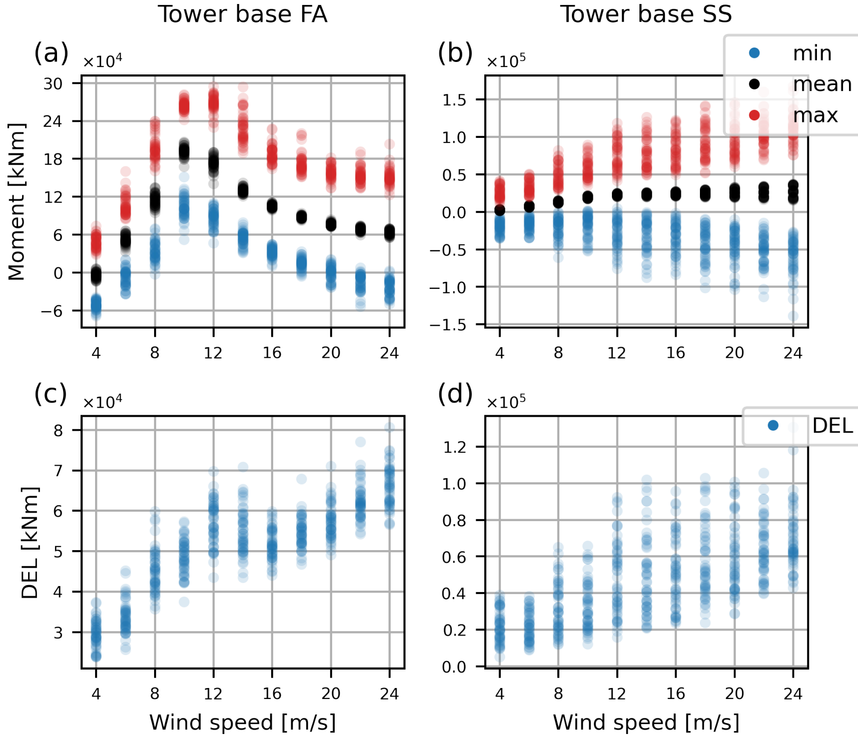

Figure 3. Statistical values (minimum, mean, maximum) of the tower base moment calculated in (a) the FA direction and (b) the SS direction, and DELs calculated in (c) the FA direction and (d) the SS direction, all based on the HAWC2 time series covering DLC 1.2 for the MWL equal to MSL.

the difference of the excitation forces resulting from the varying angle-of-attack of the wind and waves between the two time series.

In conclusion, the present section underlines that the DELs calculated for the IEA 15-MW RWT are indeed influenced by environmental parameters such as turbulence, which govern the quasi-static response, and wave direction. Furthermore, operational parameters such as pitch angles and yaw errors can, in some cases, contribute to the excitation of the dynamic modes, which significantly impacts the DELs. Thus, the MDE configuration presented in Section 4.1, is required to accurately capture both quasi-static and dynamic responses for varying operational and environmental conditions.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

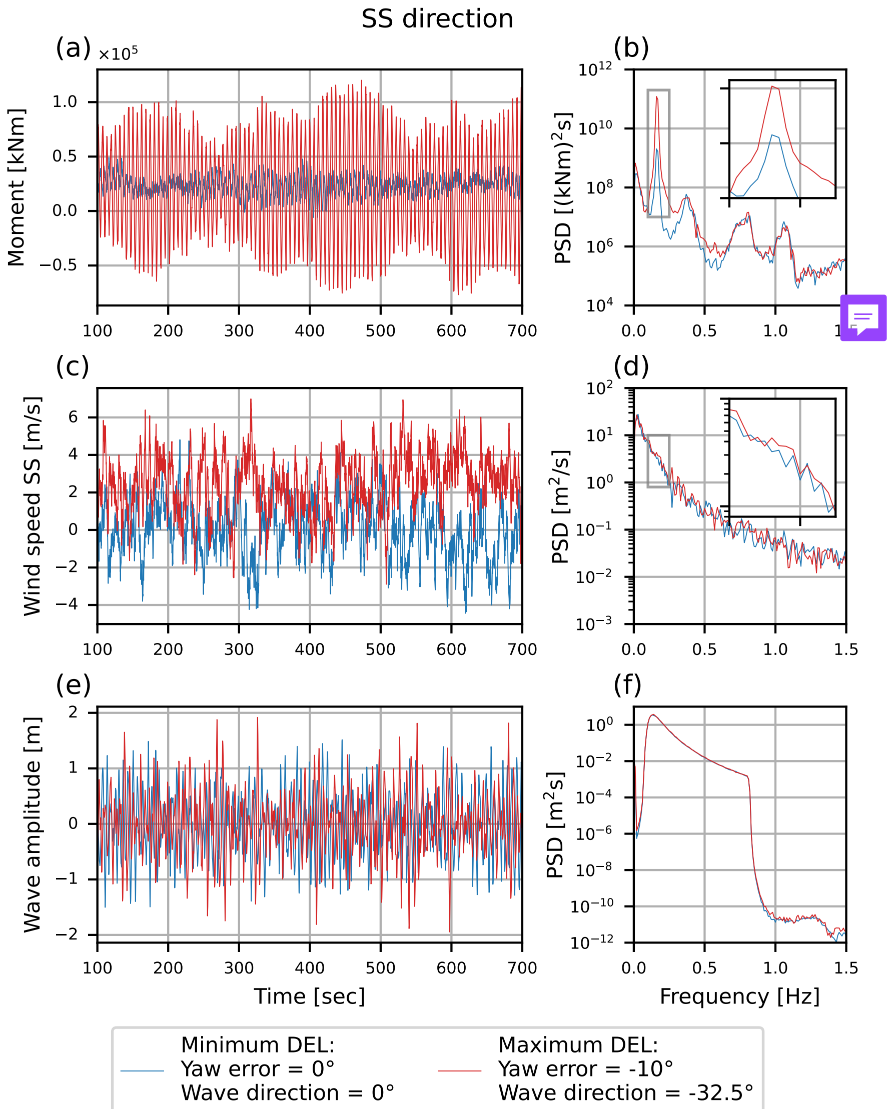

Figure 4. Time series data for maximum and minimum DELs from Figure 3(d) at 14 m/s hub wind speed: (a) time history and (b) PSD of tower base moment in the SS direction, (c) time history and (d) PSD of hub wind speed in the SS direction, and (e) time history and (f) PSD of wave amplitude (water surface elevation).

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

2.5 Relative lifetime damage results

The present section investigates the lifetime damage of the IEA 15-MW RWT caused by the individual design load cases presented in Section 2.3, thereby giving an overview of which operating scenarios are significant for the fatigue damage in the supporting structure.

According to Veldkamp (2006), the relative lifetime damage caused in a given structure by a load case i is given as

$$d_{i,rel} = \frac{n_i \, (\Delta P_{eq,i})^m}{n_T \, (\Delta P_{eq})^m} \tag{3}$$

where $\Delta P_{eq,i}$ represents the 1 Hz DEL ranges for the individual load case i, m is the Wöhler coefficient, n_i is the number of 1 Hz cycles for load case i, n_T is the total number of 1 Hz cycles in the structure's lifetime, and ΔP_{eq} is the lifetime DEL range.

In the present analysis, a similar approach to that of Veldkamp (2006) in (3) is used for the evaluation of the relative lifetime damage for individual DLCs. By adding the 1 Hz DELs from the HAWC2 simulations contained in a DLC, the relative damage of the individual DLCs is calculated as

$$d_{DLC,rel} = \frac{\sum_{s \in \mathrm{DLC}} n_s \, (\Delta P_{eq,s})^m}{n_T \, (\Delta P_{eq})^m} \tag{4}$$

where

$$n_s = p(\mathrm{DLC}, V, \theta_{yaw}, \theta_{wwm}) \, \frac{n_T}{n_{seed}} \tag{5}$$

is the number of 1 Hz cycles during the lifetime of the IEA 15-MW RWT, $p(\,)$ is the joint probability of the input parameters for the operational and environmental conditions (DLC, wind speed (V), yaw error (θ_{yaw}), and wind-wave misalignment (θ_{wwm})) used for the simulation s, and n_{seed} is the number of simulations that share these operational and environmental conditions. Note that the number of summations in (4) refers to the number of (converged) simulations in Table 2 for a given DLC at MWL equal to MSL. Finally, in (4) the 1 Hz DEL range for the individual HAWC2 simulations is evaluated as

$$\Delta P_{eq,s} = \left(\frac{\sum n_j \, \Delta P_j^m}{n_{eq}} \right)^{\frac{1}{m}} \tag{6}$$

where n_{eq} is the number of 1 Hz cycles in the time series s, while ΔP_j and n_j are the binned load ranges and corresponding number of load cycles identified from the individual time series using the Rainflow counting method from ASTM E1049-85 (2017). In the present work, a single slope S-N curve with a Wöhler coefficient of $m = 5$ is used for the supporting structure. This is based on m_1 of the S-N curves for welded and non-welded circular hollow sections from Chapter 8 in DSF/FprEN 1993-1-9 (2024), which is not representative of the damage at all locations in the supporting structures but still considered sufficiently accurate for the assessment of the impact of the individual DLCs.

The relative damage for the individual DLCs $d_{DLC,rel}$, is presented in Figure 5 for the FA and SS direction of the IEA 15-MW RWT supporting structure. From these relative damage plots in Figure 5, it is observed that there is a big resemblance in the distribution of damage across the height of the IEA 15-MW RWT for DLC 1.2 and 2.4, which is expected as these

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

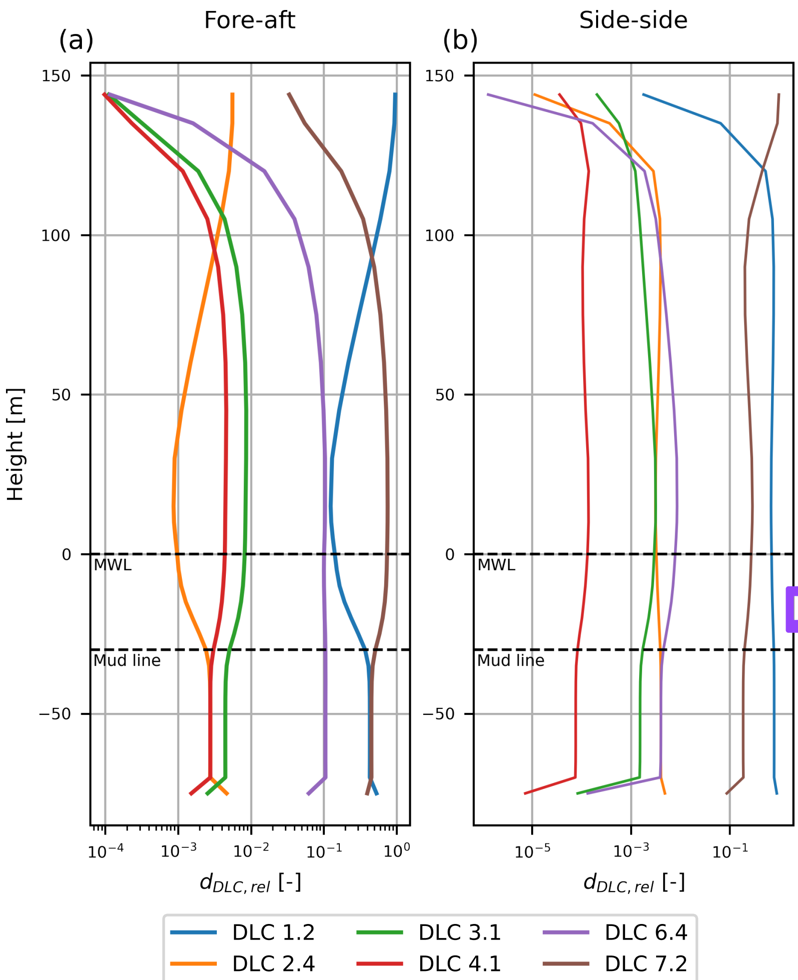

Figure 5. Relative damage for the individual DLCs calculated across the height of the IEA 15-MW RWT supporting structure as presented in (4) for the FA (a) and SS (b) direction.

load cases are both for operation in normal conditions. A similar expected resemblance is found for DLC 3.1 and 4.1, as these load cases represent start-up and shut-down, respectively. Figure 5(a) shows that approximately 99 % of the damage in the FA direction is caused by DLC 1.2 (Power production in normal conditions), DLC 6.4 (Parked - idle rotor in normal conditions), and 7.2 (Fault - locked rotor in normal conditions). In the SS direction, shown in Figure 5(b), the damage from DLC 6.4 falls below 1 %, so only DLC 1.2 and 7.2 are considered significant for the damage in the SS direction. As presented in Table 3, DLC 1.2 is significantly more frequent than DLC 6.4 and 7.2, and the significant damage contribution of this DLC is associated

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

eawe european academy of wind energy
Open Access WIND ENERGY SCIENCE DISCUSSIONS

with the large duration, whereas for DLC 6.4 and 7.2, the substantial damage contribution is associated with the large DELs (see Figure 12).

The relative damage in the FA direction in Figure 5(a) is dominated by DLC 1.2 at the tower top ($\approx 100 - 144$ m above the MSL). This is due to 3P effects (tower shadow, wind shear, and turbulence), which are significant contributors to damage in the tower top, as the varying forces on the blades and uneven loading on the rotor result in a significant moment at the hub. In the remainder of the free standing supporting structure ($\approx -30 - 100$ m), the relative damage in the FA direction is dominated by DLC 7.2. In this area, the section moments are to a higher degree governed by the global bending of the supporting structure caused by the thrust loading (for DLC 1.2) and especially the first tower FA mode (for DLC 6.4 and 7.2). The tower bending modes in the FA direction for DLC 1.2 are subject to significant aerodynamic damping arising from the operating rotor, thus explaining the smaller contribution to the relative damage from this DLC, and the larger contribution from DLCs 6.4 and 7.2, where the rotor is not operating and the aerodynamic damping is effectively negligible. Below MSL the relative damage contribution from DLC 1.2 and 7.2 approaches each other and balances out at the mud line. This is likely due to the influence of wave loads, which increase with the water depth and are less affected by the aerodynamic damping present for DLC 1.2.

The relative damage in the SS direction in Figure 5(b), is dominated by DLC 7.2 at the tower top ($\approx 120 - 144$ m above MSL), while DLC 1.2 dominates the damage below this area. Unlike the FA response, the SS response for DLC 1.2 is not significantly affected by aerodynamic damping, the 3P effects, or the thrust load variations. Consequently, the damage in both DLC 1.2 and 7.2 is primarily driven by ambient excitation at the turbine's resonant frequencies. However, the locked rotor condition in DLC 7.2 particularly influences damage at the tower top. Because the rotor is fixed in rotation and the blades are pitched 90°, the blades are more susceptible to turbulence-induced excitation, which creates a moment at the blade root. This, in turn, excites the second tower SS mode, and possibly different rotor modes, resulting in DLC 7.2's dominant contribution to damage in the upper part of the supporting structure. In the lower part, the damage patterns are more governed by the first order tower bending modes, which are similar for DLC 1.2 and 7.2. However, the significantly longer duration of DLC 1.2 (90 % of the turbine's lifetime) results in it being dominant below 120 m. This effect is visible in Figure 5(b), where the distribution of relative damage from DLCs 1.2 and 7.2 remains rather constant in the supporting structure below 100 m, with $d_{DLC,rel}$ for the two DLCs varying between $69 - 80\%$ and $19 - 29\%$, respectively.

In conclusion, the damage in the supporting structure of the IEA 15-MW RWT is governed by both normal operation conditions and conditions where the rotor is idling or locked, whereas start-up and shut-down of the wind turbine and operation with yaw error are less critical. The damage contribution across the elevation of the supporting structure arises from different local and global effects caused by different environmental and operational scenarios e.g. turbulence, 3P effects, wave loads, and inherent dynamical properties. It should be emphasised that the durations used in this analysis for the DLCs are estimated, and scenarios can occur where the durations are differently distributed between the DLCs. Therefore, it is also relevant to evaluate DELs for individual DLCs, without accounting for their specific durations, when assessing how the different operational scenarios impact lifetime damage, as done in Section 4.2.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

3 Virtual Sensing

The present section initially explains the basic concepts of multi-band MDE and the methodology applied when moving from nodal displacements to internal force estimates. This is followed by a presentation of the prediction FE model used in the subsequent estimation of Damage Equivalent Loads (DELs) and Stresses (DESs) in Section 4. Finally, the current section presents the model output with respect to dynamic mode shapes and quasi-static Ritz vectors.

3.1 Modal Decomposition and Expansion

Modal Decomposition and Expansion (MDE) is a well-established process model in virtual sensing (see Section 1). The formulation used in the present work is described in Iliopoulos et al. (2017). MDE assumes that the displacement vector $\mathbf{u}(t)$ of an undamped dynamic system can be decomposed and written as a linear combination of the system's mode shapes and modal coordinates on the matrix form

$$\mathbf{u}(t) = \mathbf{\Phi}\mathbf{q}(t) \tag{7}$$

The mode shape matrix $\mathbf{\Phi} = [\boldsymbol{\varphi}_1, \boldsymbol{\varphi}_2, \cdots, \boldsymbol{\varphi}_n]$ contains the n mode shapes ($\boldsymbol{\varphi}_j$) included to describe the dynamical system, while the modal coordinate vector $\mathbf{q}(t) = [q_1(t), q_2(t), \cdots, q_n(t)]^T$ collects the corresponding modal coordinates (q_j) at each time instant t. The mode shapes of the system $\boldsymbol{\varphi}_j$, can be derived from, e.g., experimental or operational modal analysis, while in the present work, the vectors $\boldsymbol{\varphi}_j$ are derived from an FE model representing the dynamic system in Section 3.3. Assuming that the FE model is an accurate representation of the considered dynamic system, it follows that

$$\mathbf{\Phi} = \mathbf{\Phi}_{FE} \tag{8}$$

which applies in the remainder of the paper. If the total number of Degrees of Freedom (DOFs) in the FE model is n_{dof}, the modal matrix $\mathbf{\Phi}$ becomes an $n_{dof} \times n$ array. The nodal displacement vector $\mathbf{u}(t)$ in (7) is conveniently partitioned as

$$\mathbf{u}(t) = \begin{bmatrix} \mathbf{u}_m(t) \\ \mathbf{u}_p(t) \end{bmatrix} = \begin{bmatrix} \mathbf{\Phi}_m \\ \mathbf{\Phi}_p \end{bmatrix} \mathbf{q}(t) \tag{9}$$

where the first n_m DOFs in $\mathbf{u}_m(t)$ represent those that are measured by physical sensors, while the remaining n_p DOFs in $\mathbf{u}_p(t)$ are those that are predicted by the MDE, i.e. the virtual sensors. By direct comparison of (7) and (9), the mode shape matrix is similarly partitioned into

$$\mathbf{\Phi} = \begin{bmatrix} \mathbf{\Phi}_m \\ \mathbf{\Phi}_p \end{bmatrix} \tag{10}$$

in which the $n_m \times n$ array $\mathbf{\Phi}_m$ refers to the mode shape amplitudes associated with the measured DOFs, while correspondingly the $n_p \times n$ array $\mathbf{\Phi}_p$ accounts for the remaining DOFs that are used for the subsequent prediction procedure. From the above partitioning in (9) and (10), it is seen that the total number of DOFs in the FE model is $n_{dof} = n_m + n_p$, i.e. the sum of measured and predicted DOFs.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

eawe european academy of wind energy
Open Access WIND ENERGY SCIENCE DISCUSSIONS

MDE utilises that the displacements in $\mathbf{u}_m(t)$ are available from measurements, while the remaining DOFs in $\mathbf{u}_p(t)$ are predicted simultaneously once the modal matrix in (10) can be obtained from the underlying FE-model with sufficient accuracy. It follows from (9) that the predicted nodal displacements can be expressed by the modal representation

$$\mathbf{u}_p(t) = \mathbf{\Phi}_p \mathbf{q}(t) \tag{11}$$

The modal coordinates in $\mathbf{q}(t)$, used for the extrapolation in (11), are determined by the corresponding relation

$$\mathbf{u}_m(t) = \mathbf{\Phi}_m \mathbf{q}(t) \tag{12}$$

for the measured DOFs in $\mathbf{u}_m(t)$. The inversion of this relation requires that the dynamic displacement field can be represented by at most n modes, where n must be less than or equal to the number of measured DOFs n_m. Hereby, the modal coordinates can be determined as

$$\mathbf{q}(t) = \mathbf{\Phi}_m^{\dagger} \mathbf{u}_m(t) \tag{13}$$

using the Moore-Penrose pseudo-inverse depicted by the commonly used $(\ )^{\dagger}$ symbol. The predicted nodal displacements are then obtained by substitution of (13) into (11), which then takes on its final form

$$\mathbf{u}_p(t) = \mathbf{\Phi}_p \mathbf{\Phi}_m^{\dagger} \mathbf{u}_m(t) \tag{14}$$

In virtual sensing, one of the objectives is to minimise the number of physical sensors n_m by introducing virtual sensors. Hence, the condition $n \leq n_m$ poses a challenge, as this limits the number of modes n that can be included to describe the dynamic system. Furthermore, for low frequencies, it can be desirable to perform MDE using only a subset of the measurements $\tilde{\mathbf{u}}_m(t)$ to minimise the noise introduced in the estimates, or to introduce Ritz vectors containing static deflection shapes to predict the response $\mathbf{u}_p(t)$ in frequency ranges not dominated by resonant response (see Section 3.3.2). The introduction of multi-band virtual sensing in Iliopoulos et al. (2017) utilises that the nodal displacement vector $\mathbf{u}(t)$ can be divided into separate bands B_i in the frequency domain, which when combined by summation, reattains the original nodal displacement vector

$$\mathbf{u}(t) = \sum_{i=1}^{N} \mathbf{u}_i(t) = \sum_{i=1}^{N} B_i(\mathbf{u}(t)) \tag{15}$$

where $\mathbf{u}_i$ is the nodal displacement vector band-pass filtered in the band B_i, and $i = 1, 2 \ldots N$ denotes the individual frequency bands, shown in Figure 6. Similarly, the predicted nodal displacements $\mathbf{u}_p(t)$ can be calculated in individual bands and combined by summation as

$$\mathbf{u}_p(t) = \sum_i \mathbf{u}_{i,p}(t) = \sum_i \tilde{\mathbf{\Phi}}_{i,p} \tilde{\mathbf{\Phi}}_{i,m}^{\dagger} \tilde{\mathbf{u}}_{i,m}(t) \tag{16}$$

now only including the modes and Ritz vectors $\tilde{\mathbf{\Phi}}_i$ and the measurements $\tilde{\mathbf{u}}_m(t)$ that are relevant for the band B_i. This representation assumes that the energy content of $\mathbf{u}_p(t)$ is fully captured by the sum of its filtered components in the bands B_i.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

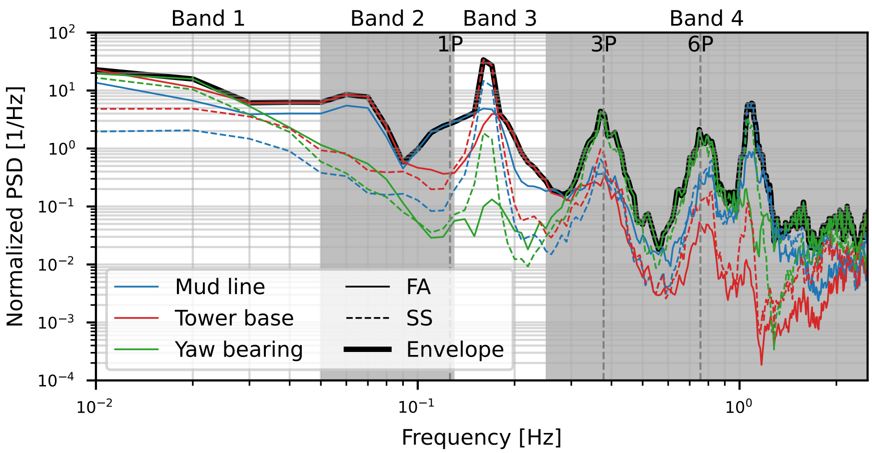

Figure 6. Normalized PSD of moment time series (from DLC 1.2). The frequency spectra of the moments at the yaw bearing, tower base, and mud line are shown in the FA and SS directions. Transparent white/grey bands indicate the frequency ranges used in the MDE (Section 4.1), representing: Band 1 (turbulence), Band 2 (turbulence and wave loads), Band 3 (first tower bending and wave loads), and Band 4 (higher dynamic modes and rotor harmonics).

3.2 Internal force estimation

The previous Section 3.1 has explained how modal decomposition and expansion can be used to predict displacement response at virtual sensor locations. The present section extends the MDE to predict internal forces based on the predicted nodal displacement vector $\mathbf{u}_p(t)$.

The section forces to be predicted by the proposed method are specific for the element of the applied FE representation, e.g. bending moments for the planar beam elements used to describe the dynamics of the present supporting structure. Let the nodal forces be contained in the nodal element vector

$$\mathbf{r}_e(t) = \begin{bmatrix} \mathbf{r}_A(t) \\ \mathbf{r}_B(t) \end{bmatrix}_e = \begin{bmatrix} f_x^A(t), & f_y^A(t), & m^A(t), & f_x^B(t), & f_y^B(t), & m^B(t) \end{bmatrix}_e^T \tag{17}$$
$$= \begin{bmatrix} -N_A(t), & V_A(t), & -M_A(t), & N_B(t), & -V_B(t), & M_B(t) \end{bmatrix}_e^T$$

for a planar (2D) beam element e between two nodes A and B, with f_x, f_y and m representing the nodal normal force, shear force and moment, respectively. As shown in (17), the corresponding section forces N, V and M are derived from the nodal force by appropriate sign changes.

For a given element (subscript) e, the element nodal force vector in (17) can be determined by the element stiffness matrix $\mathbf{k}_e$. The element stiffness relation can thus be written as

$$\mathbf{r}_e(t) = \mathbf{k}_e \mathbf{T}_e \mathbf{u}_p(t) \tag{18}$$

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

where $\mathbf{T}_e$ is a $6 \times n_p$ array that both collects and rotates the six DOFs from the global vector $\mathbf{u}_p(t)$ into the local coordinate system for element $e = 1, 2 \ldots N_e$. Elimination of the response in the predicted DOFs $\mathbf{u}_p(t)$ by (14) gives the compact representation

$$\mathbf{r}_e(t) = \mathbf{k}_e \mathbf{T}_e \mathbf{\Phi}_p \mathbf{\Phi}_m^{\dagger} \mathbf{u}_m(t) = \mathbf{D}_e \mathbf{u}_m(t) \tag{19}$$

where

$$\mathbf{D}_e = \mathbf{k}_e \mathbf{T}_e \mathbf{\Phi}_p \mathbf{\Phi}_m^{\dagger} \tag{20}$$

defines the section force matrix that predicts the section forces $\mathbf{r}_e(t)$ from the measured nodal displacements in $\mathbf{u}_m(t)$. For a model with vertical beam elements, as in the present case, the transformation matrix $\mathbf{T}_e$ is an all-zero $6 \times n_p$ matrix, except for ± 1 entries in the 6×6 block associated with the specific element e.

3.3 Prediction FE model

The prediction FE model from which the mode shapes and Ritz vectors used in the MDE are obtained is a 3D linear elastic beam model with the Rotor-Nacelle-Assembly (RNA) and transition piece modelled as lumped inertias. The beam model is presented schematically in Figure 7. The geometrical properties and the mass and stiffness input parameters for the prediction FE model are extracted from the HAWC2 model of the IEA 15-MW RWT described in Section 2.1 and presented in Appendix A.

The beam element stiffness is established according to Krenk and Høgsberg (2013), which combines the element stiffness matrix developed from the Timoshenko beam theory $\mathbf{K}_{beam,e}$ with a so-called geometric stiffness term $\mathbf{K}_{g,e}$ expressing the total element stiffness matrix as

$$\mathbf{K}_e = \mathbf{K}_{beam,e} + \mathbf{K}_{g,e} \tag{21}$$

thus accounting for the stiffness contribution adhering from the normal forces causing Euler buckling in bending, although omitting the stiffness terms associated with torsion, i.e., loads causing lateral buckling in static analysis.

The monopile foundation support conditions are modelled using lateral linear elastic soil springs in the embedded part of the monopile. The stiffness of the individual springs $k_{soil,n}$ varies with the embedment depth, as presented in Table 1. The bottom node in the beam model restrains torsion and vertical translation.

The mass contributing to the modal mass of the prediction FE model includes the distributed mass of the tower, transition piece, and monopile presented in Appendix A, the nodal mass of the transition piece M_{TP} located at the top of the transition piece, and the eccentric nodal mass and inertia tensor of the RNA, M_{RNA} and I_{RNA}, located at the distances a_x, a_y, and a_z relative to the top of the tower. The input parameters for the nodal masses for the TP and RNA and the mass moments and mass products of inertia included in the inertia tensor (I_{xx}, I_{yy}, I_{zz}, I_{xy}, I_{xz}, I_{yz},) are presented in Table 4. In addition to the mass contributions already presented, an external mass contribution referred to as the hydrodynamic mass m_{hydro} arises when a body moves in a fluid. According to Sumer and Fredsøe (1997), the hydrodynamic mass per unit length of a free circular

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

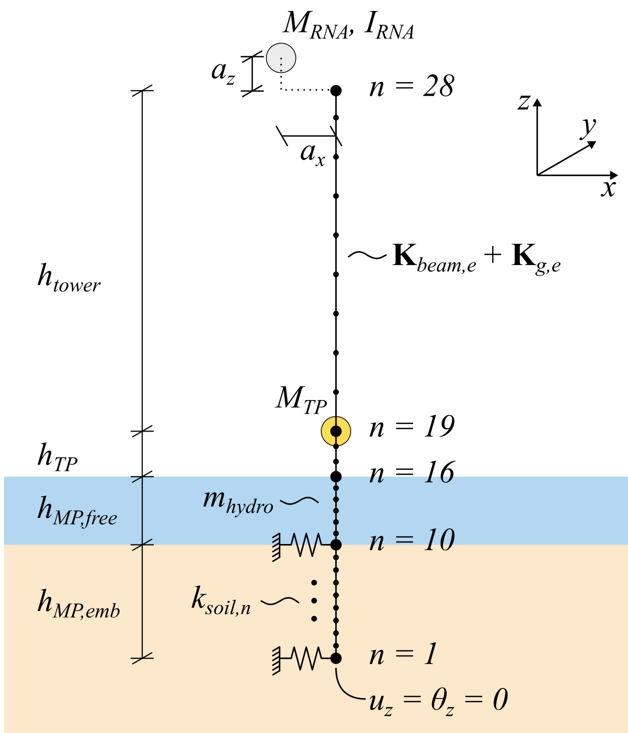

Figure 7. Schematic presentation of the prediction FE model used for the modal decomposition and expansion, including the height of the members in the supporting structure h_*, the element stiffness $\mathbf{K}_{beam,e} + \mathbf{K}_{g,e}$, the nodal masses of the transition piece M_{TP} and Rotor-Nacelle-Assembly (RNA) M_{RNA}, the RNA inertia tensor I_{RNA}, the soil stiffness in node n $k_{soil,n}$, and the hydrodynamic added mass m_{hydro}.

cylinder can be expressed as

390 $$m_{hydro} = \rho C_m A \tag{22}$$

if the current is disregarded. Here, the fluid density is $\rho = 1027$ kg/m^3, $C_m = 1$ is the hydrodynamic mass coefficient for a cylinder, and $A = \pi r^2$ is the fluid-displaced area for the monopile with radius r.

The first three tower bending mode shapes used for the MDE configuration in Section 4.1 have been calculated using the FE model presented above. They are shown in Figure 8 for displacements and bending moments in the FA and SS directions.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

Table 4. Nodal mass, inertia tensor, and Center of Gravity (CoG) of the IEA 15-MW RWT RNA, calculated based on the individual body properties extracted from HAWC2 and nodal mass of the IEA 15-MW RWT Transition Piece (TP).

M_{RNA}	9.45E+05	[kg]
a_x	-7.12E+00	[m]
a_y	0	
a_z	4.58E+00	
I_{xx}	3.52E+08	[kgm^2]
I_{yy}	1.96E+08	
I_{zz}	1.97E+08	
I_{xy}	0	
I_{xz}	-4.04E+07	
I_{yz}	0	
M_{TP}	1.00E+05	[kg]

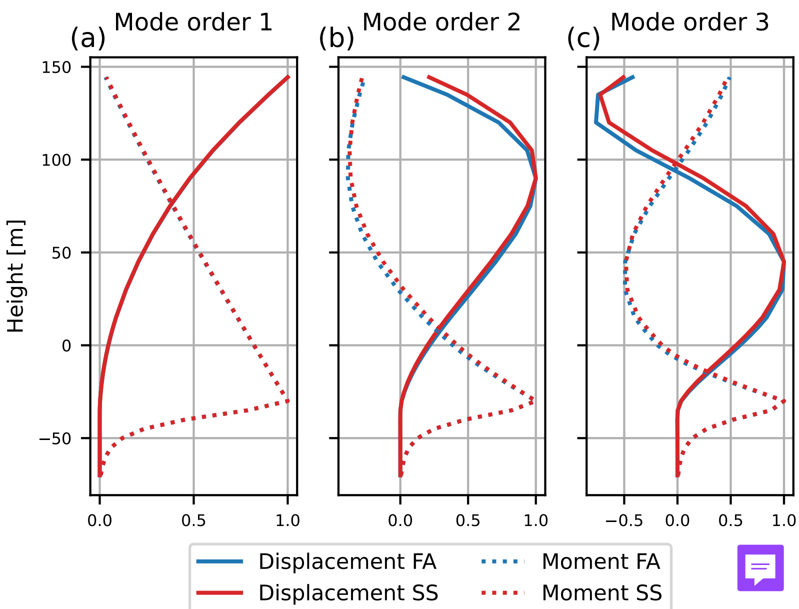

Figure 8. Mode shapes in terms of displacement and bending extracted from the prediction FE model presented in Figure 7 in the FA and SS direction: (a) the first tower bending modes, (b) the second tower bending modes, and (c) the third tower bending modes.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

eawe european academy of wind energy
Open Access WIND ENERGY SCIENCE DISCUSSIONS

3.3.1 Model Validation

In the present section, the prediction FE model presented in the previous Section 3.3 is validated. The validation is performed simply by comparing the undamped natural frequencies f_n of the prediction FE model to those of the IEA 15-MW RWT extracted using the HAWC2 built-in module *system_eigenanalysis*. The objective of the validation is to ensure that the input parameters for the prediction FE model presented in Figure 7, which are extracted from the HAWC2 model, are interpreted correctly. To ensure that the present validation is as objective as possible, the comparison is performed for a simplified HAWC2 model of the IEA 15-MW RWT, in which particular flexibilities are restrained.

As mentioned previously, the prediction FE model does not include a detailed model of the RNA. Therefore, the influence of an operating rotor, blade flexibility, and shaft torsion is not included in the prediction FE model. In the simplified HAWC2 models, this is acknowledged by restraining shaft rotation, disabling torsional deformations, and using stiff blades. The comparison aims at validating the effects of mass and stiffness terms, soil support conditions, and hydrodynamic mass used in the prediction FE model by gradually adding these terms. This yields the following three model setups for the simplified HAWC2 model:

- Model setup 1: Excluding the hydrodynamic elements and the soil model, and fixing the bottom node in all DOFs. This model resembles a bottom-fixed land-based wind turbine.
- Model setup 2: Excluding the hydrodynamic elements, while reintroducing the soil support from the original HAWC2 model in Section 2.1.
- Model setup 3: Introducing the hydrodynamic elements without water kinematics to reduce complexity.

The comparison of the natural frequencies of the simplified HAWC2 model $f_{n,HAWC2}$ and the prediction FE model $f_{n,Pred}$ are presented for the first seven modes in Table 5, in which the error is calculated as

$$\varepsilon(f_n) = \frac{f_{n,Pred} - f_{n,HAWC2}}{f_{n,HAWC2}} \tag{23}$$

As presented in Table 5, the error $\varepsilon(f_n)$ for the tower bending modes is within the range from -0.88 to 1.13%, while for the torsion mode the error range increases to $3.17 - 3.32\%$. The two models are created from different underlying beam theories and implemented in different software tools, whereby discrepancies are expected. Thus, the agreement in Table 5 is generally good, with the larger error for the torsion mode possibly arising from the geometric stiffness matrix $\mathbf{K}_{g,e}$ in (21) not affecting torsional deformations.

Based on the results in Table 5, it is concluded that the mass and stiffness terms and the soil model are reasonably implemented in the prediction FE model. Furthermore, the simple implementation of the hydrodynamic mass is deemed acceptable for the cases where waves and currents are not included in the analysis. However, it is acknowledged that the model cannot capture the effects of currents and waves, as well as boundary effects at the seabed and water line.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

Table 5. Overview of comparison of natural frequencies of three different model setups for a simplified version of the IEA 15-MW RWT HAWC2 model and the prediction FE model presented in Section 3.3.

Model setup	Mode No.	1	2	3	4	5	6	7
1	**Mode**	**1st bend.**	**1st bend.**	**2nd SS**	**2nd FA**	**1st torsion**	**3rd SS**	**3rd FA**
	$f_{n,HAWC2}$	1.31E-01	1.31E-01	6.79E-01	7.19E-01	8.05E-01	1.50E+00	1.61E+00
	$f_{n,Pred}$	1.30E-01	1.31E-01	6.75E-01	7.12E-01	7.79E-01	1.52E+00	1.61E+00
	$\varepsilon(f_n)$	-0.55%	-0.16%	-0.48%	-0.88%	**-3.17%**	1.13%	0.06%
2	**Mode**	**1st bend.**	**1st bend.**	**1st torsion**	**2nd SS**	**2nd FA**	**3rd SS**	**3rd FA**
	$f_{n,HAWC2}$	1.61E-01	1.62E-01	8.01E-01	8.47E-01	9.15E-01	1.93E+00	2.02E+00
	$f_{n,Pred}$	1.60E-01	1.61E-01	7.75E-01	8.52E-01	9.11E-01	1.95E+00	2.02E+00
	$\varepsilon(f_n)$	-0.80%	-0.29%	**-3.32%**	0.54%	-0.47%	0.94%	0.16%
3	**Mode**	**1st SS**	**1st FA**	**1st torsion**	**2nd SS**	**2nd FA**	**3rd SS**	**3rd FA**
	$f_{n,HAWC2}$	1.61E-01	1.62E-01	8.01E-01	8.37E-01	9.00E-01	1.79E+00	1.87E+00
	$f_{n,Pred}$	1.60E-01	1.61E-01	7.74E-01	8.41E-01	8.96E-01	1.81E+00	1.88E+00
	$\varepsilon(f_n)$	-0.83%	-0.49%	**-3.29%**	0.43%	-0.47%	0.97%	0.22%

3.3.2 Ritz vectors

As explained in Section 3.1, the predicted response $\mathbf{u}_p(t)$ of a dynamic system can be estimated as the sum of the predicted response in the individual frequency bands B_i based on the mode shape matrix $\mathbf{\Phi}$. However, for large-scale OWTs, the quasi-static effects arising from e.g. yawing, wind, and waves significantly contribute to the response. These effects can be captured by a linear combination of higher-order modes. However, because a modal truncation omitting higher-order modes is needed in MDE, due to the limited number of sensors available, the accuracy of the predicted response may be compromised in the quasi-static region and between the resonant peaks. Different suggestions have been made to account for the quasi-static response, where Skafte et al. (2017) suggest the use of Ritz vectors, while similar methods are applied in Iliopoulos et al. (2017), Augustyn et al. (2021), and Toftekær et al. (2023). Furthermore, Tarpø (2020) compares the use of Ritz vectors with a *modal truncation augmentation method* and finds that the difference in performance is insignificant for the considered case. In the present work, the methodology using Ritz vectors based on static loads from Skafte et al. (2017) is applied, as explained in the following.

The mode shape matrix in (10) is extended to include not only the n mode shapes of the dynamic system $\mathbf{\Phi}_d$ obtained from the eigenanalysis of the FE model presented in Section 3.3, but also the m Ritz vectors obtained from static analysis $\mathbf{\Phi}_s$,

$$\mathbf{\Phi} = \begin{bmatrix} \mathbf{\Phi}_s \; \mathbf{\Phi}_d \end{bmatrix} \tag{24}$$

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

whereby $\mathbf{\Phi}$ becomes an $n_{dof} \times (m+n)$ array. The matrix $\mathbf{\Phi_s} = [\phi_1, \phi_2, \cdots, \phi_m]$ contains the m Ritz vectors (ϕ_k), obtained by the static solution

$$\mathbf{\Phi}_s = \mathbf{K}^{-1}\mathbf{F} \tag{25}$$

where $\mathbf{K}$ is the stiffness matrix of the FE model presented in Figure 7 and $\mathbf{F}$ contains the static load vectors $\mathbf{f}_i$ representing the load effects included in the MDE. Both Toftekær et al. (2023) and Iliopoulos et al. (2017) suggest that an appropriate Ritz vector for the thrust load can be obtained by applying a horizontal nodal force at the top of the FE model tower, see Figure 9(a). Furthermore, Toftekær et al. (2023) show that a supplemental Ritz vector from the nodal tower-top moment in Figure 9(b) improves the MDE strain estimates associated with RNA yaw or uneven rotor loading. Finally, Skafte et al. (2017), Tarpø (2020), and Augustyn et al. (2021) all include load from waves in the performed MDE, see Figure 9(c). In the present work, three pairs of Ritz vectors are included in the MDE, representing the FA and SS directions, respectively. In each direction, the tower-top nodal load (a) and moment (b), and the wave loading (c) are presented in Figure 9.

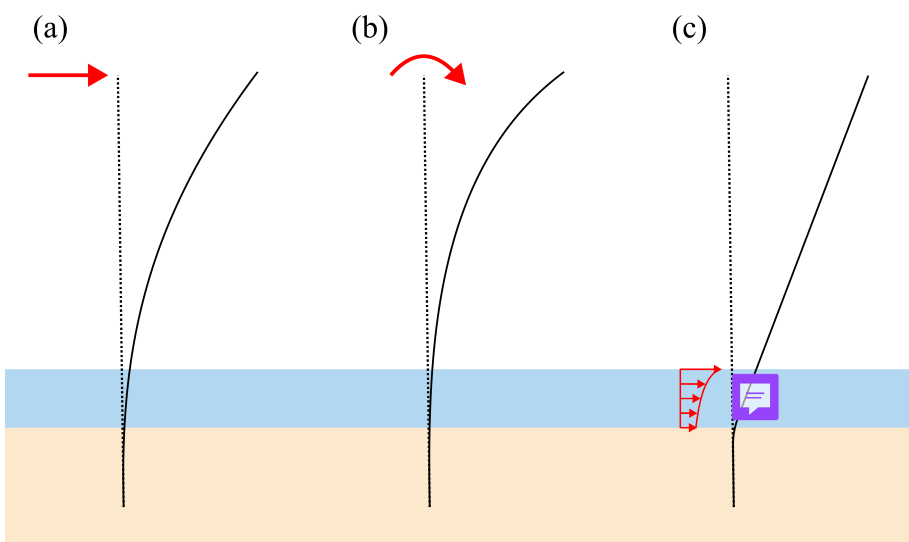

Figure 9. Loads and moments applied to determine the Ritz vectors for the estimation of the quasi-static response. Based on suggested loads in Toftekær et al. (2023). (a) shows the tower-top nodal load, (b) shows the tower top moment, and (c) shows the wave loading.

The wave load depicted in Figure 9(c) is based on the expression for the total force

$$F_x(z,t) = \frac{2\rho g H}{k}\frac{\cosh(k(z+h))}{\cosh(kh)}A(kr_0)\cos(\omega t - \delta) \tag{26}$$

on a unit height of a vertical cylinder (Sumer and Fredsøe, 1997). In the present work, normalized displacements are used, hence only the distribution across the water depth of the monopile is of interest, whereby the temporal and constant terms can

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

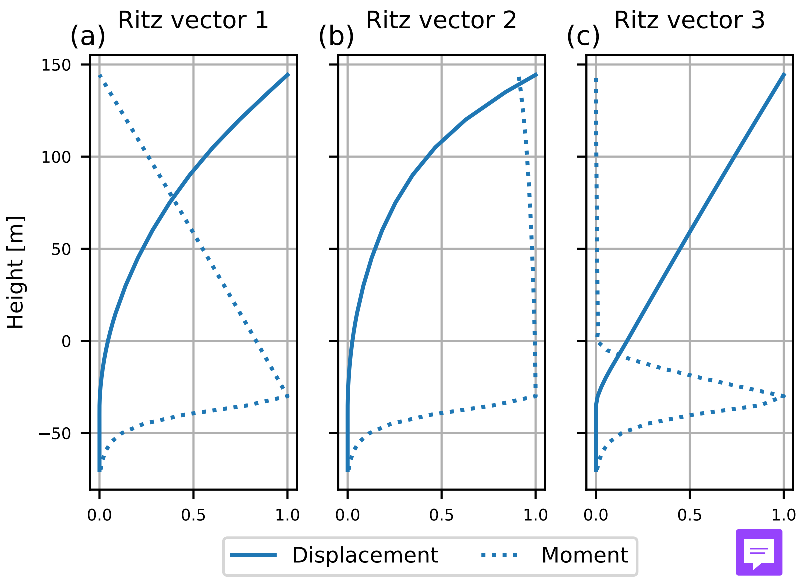

Figure 10. Ritz vectors in terms of displacement and bending moments extracted from the prediction FE model presented in Figure 7: (a) is based on nodal force in tower top, (b) is based on the nodal moment in the tower top, and (c) is based on the approximated wave load presented in (27). The three loads are illustrated in Figure 9.

be removed in (26). Thereby, the vertical distribution of the force (above the seabed) reduces to

$$F_x'(z) = \frac{\cosh(k\,(z+h))}{\cosh(kh)} \tag{27}$$

where $h = 30\,\mathrm{m}$ is the water depth and $k = \frac{2\pi}{L}$ is the deep-water wave number, derived for the wave length $L = \frac{g}{2\pi}T^2$ with the wave period $T = 6.52\,\mathrm{s}$ calculated for a hub wind speed of $V_{hub} = 10\,\mathrm{m/s}$. The distributed force in (27) assumes that the wave loads are dominated by the inertia contribution in Morison's equation, while neglecting drag. This assumption is indeed valid for $V_{hub} = 10\,\mathrm{m/s}$, for which inertia forces constitute 98.5 % of the total force. However, extending the wave load Ritz vector to be wind speed dependent might be relevant, as suggested in Tarpø (2020). The Ritz vectors obtained from the load presented in Figure 9 are presented in Figure 10 in terms of displacements and bending moments.

4 MDE estimation of damage equivalent loads and stresses

The objective of the multi-band MDE is to obtain valid estimates of strains, stress, or force histories at any given location in a given structure. The accuracy of the MDE depends not only on the quality of the FE model from Section 3.3, but also on the configuration and input data, which are presented in the next Section 4.1. The purpose of the applied multi-band Modal Decomposition and Expansion (MDE) is to evaluate the fatigue damage from bending stresses in any relevant location of the

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

supporting structure. Hence, the performance of the MDE should be assessed using a measure that accounts for the accuracy in terms of strains or forces, while also being consistent with how fatigue damage is evaluated. In Section 4.2 this comparison is therefore conducted in terms of Damage Equivalent Loads (DELs) and Damage Equivalent Stresses (DESs).

4.1 MDE setup

This section presents the basis for the MDE performed for the IEA 15-MW RWT supporting structure in terms of sensor type and placement (i.e. the HAWC2 output channels in $\mathbf{u}_m(t)$), band separation used in the frequency domain, and the choices of Ritz vectors and mode shapes used within the individual bands ($\tilde{\mathbf{\Phi}}_i$).

As presented in Section 1, it is widely accepted in the literature that the dynamic part of the response $\mathbf{u}_p(t)$ can be predicted based on measured accelerations. From these accelerations, displacements are obtained through double integration. However, for the quasi-static part of the response, the displacements are often inaccurate because measurement noise in the acceleration measurements is amplified during low-frequency integration. To overcome this challenge, Iliopoulos et al. (2017) uses strain gauge measurements as input to the MDE for the quasi-static response estimation. Alternatively, Toftekær et al. (2023) uses the low-pass filtered (vertical) accelerations obtained from DC accelerometers relative to the gravitational acceleration to estimate rotations. This has the advantage that no double integration must be performed, and no additional sensors must be installed. In the present work, $\mathbf{u}_m(t)$ therefore contains displacements and rotations for the prediction of dynamic and quasi-static responses, respectively (see Figure 11).

Obviously, the location of the accelerometers will impact the quality of the virtual sensors. Different methods have been used to optimise the sensor placement (Mehrjoo et al., 2022; Ercan and Papadimitriou, 2021). However, in practical applications, accessibility is just as relevant for the installation of sensors, since maintenance and replacement of structural health monitoring systems play a central role in the robustness of the overall system. Thus, in the present work, the physical sensors are placed at locations where internal platforms are most likely installed inside the tower (see Figure 11).

As presented in Figure 6, the multi-band MDE (16) is performed by separating the response of the IEA 15-MW RWT into four individual bands (B_1 to B_4) before combining them to the total predicted response $\mathbf{u}_p(t)$. This band separation captures the effects dominating the individual bands in terms of wind, waves, operational forces, or resonant responses without exceeding the inherent sensor limitations of the MDE. The justification of the present band separation is given below for the MDE configuration summarized in Table 6:

- B_1 represents the quasi-static domain of the response. The response in this frequency band is primarily driven by turbulence. Thus, the Ritz vectors included for the prediction in this band are obtained from the nodal force and moment. Furthermore, the wind is assumed to act as a distributed load across the tower, whereby the first tower bending mode shapes are also included.
- B_2 represents the first dynamic band, governed by wave loading with a wave frequency of $1/T_p = 0.068\,\text{Hz}$ at $V = 35$ m/s and $1/T_p = 0.18\,\text{Hz}$ at $V = 4$ m/s. Furthermore, the wind load also contributes to the response in this frequency band, whereby all three pairs of Ritz vectors are included.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

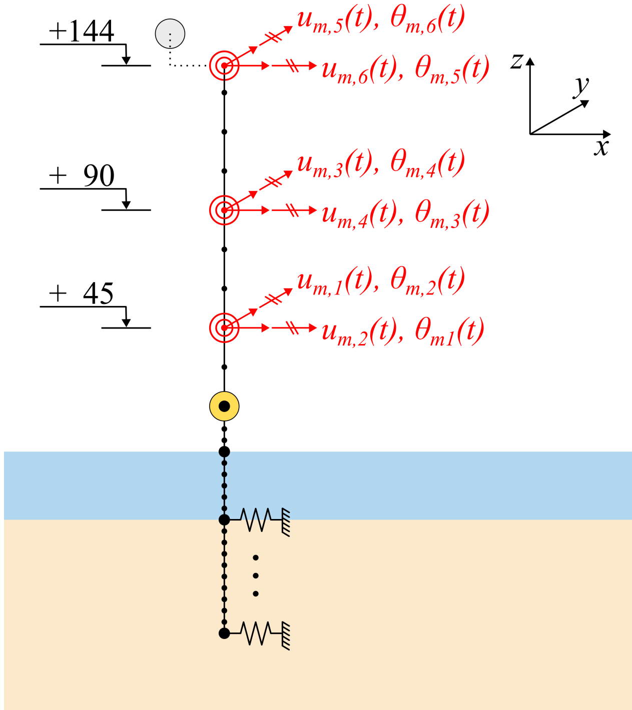

Figure 11. Measurement locations i.e. HAWC2 output channels in red in terms of displacements $u_{m,*}(t)$ included in MDE in dynamic frequency range and rotations $\theta_{m,*}(t)$ included in MDE in quasi-static frequency range

- B_3 represents the second dynamic band, in which the first tower bending modes dominate the response along with the wave loads. Hence, the first tower bending mode shapes and the Ritz vectors from wave loading are included.
- B_4 includes the higher dynamics and rotor harmonics. Here, the first three pairs of tower bending modes are included, while the first tower torsion mode is omitted as it is considered less significant for estimating bending stresses.

The following section assesses the performance of the MDE using the configuration described above and the prediction FE model presented in Section 3.3. This is achieved by comparing DELs and DESs, calculated from section moment load histories, obtained from both the MDE and the true HAWC2 output time series. The comparison is performed in both the FA and SS directions and at all nodes in the supporting structure for the DLCs described in Section 2.3.

Table 6. Configuration used for MDE in the frequency ranges B_1, B_2, B_3, and B_4 in terms of measurements, mode shapes, and Ritz vectors.

Band No. (i)	1	2	3	4
B_i	[0.00 − 0.05] Hz	[0.05 − 0.13] Hz	[0.13 − 0.25] Hz	[0.25 − 50] Hz
$\mathbf{u}_{i,m}(t)$	$\begin{bmatrix}\theta_1 & \theta_2 & \theta_3 & \theta_4 & \theta_5 & \theta_6\end{bmatrix}$	$\begin{bmatrix}u_1 & u_2 & u_3 & u_4 & u_5 & u_6\end{bmatrix}$	$\begin{bmatrix}u_1 & u_2 & u_3 & u_4 & u_5 & u_6\end{bmatrix}$	$\begin{bmatrix}u_1 & u_2 & u_3 & u_4 & u_5 & u_6\end{bmatrix}$
$\tilde{\mathbf{\Phi}}_{i,s}$	$\begin{bmatrix}\phi_1 & \phi_2 & \phi_3 & \phi_4\end{bmatrix}$	$\begin{bmatrix}\phi_1 & \phi_2 & \phi_3 & \phi_4 & \phi_5 & \phi_6\end{bmatrix}$	$\begin{bmatrix}\phi_5 & \phi_6\end{bmatrix}$	-
$\tilde{\mathbf{\Phi}}_{i,d}$	$\begin{bmatrix}\varphi_1 & \varphi_2\end{bmatrix}$	-	$\begin{bmatrix}\varphi_1 & \varphi_2\end{bmatrix}$	$\begin{bmatrix}\varphi_1 & \varphi_2 & \varphi_4 & \varphi_5 & \varphi_6 & \varphi_7\end{bmatrix}$

4.2 Damage equivalent loads and stresses

Fatigue Damage Equivalent Loads (DELs) reduce a load history to a single equivalent load range ΔP_{eq}, which is defined as the constant amplitude 1 Hz sinusoidal load causing the same amount of fatigue damage as the original load history. The same applies for fatigue Damage Equivalent Stresses (DESs) ΔS_{eq}, making DELs and DESs convenient measures for comparing fatigue contributions across load cases with different durations (Veldkamp, 2006). Thus, in the present section, the DELs and DESs combined for the individual DLCs presented in Section 2.3 are compared and discussed. Furthermore, the MDE performance is assessed, initially for DELs and DESs calculated for the individual DLCs and subsequently for the DESs calculated for the individual HAWC2 section moment time histories. In both cases, the comparison is performed in all nodes of the IEA 15-MW RWT HAWC2 model.

The DEL for a single load history $\Delta P_{eq,s}$ can be calculated as in (6), where n_{eq} is the number of 1 Hz cycles in the considered time series. Similarly, the DEL for the individual DLCs can be calculated as

$$\Delta P_{eq,DLC} = \left(\frac{\sum_{s\in\mathrm{DLC}} n_{eq}\left(\Delta P_{eq,s}\right)^m}{n_{eq,DLC}}\right)^{\frac{1}{m}} \tag{28}$$

where

$$n_{eq,DLC} = n_{eq}\, n_{seed,DLC} \tag{29}$$

is the total number of 1 Hz cycles in the simulations contained in the individual DLCs, with $n_{seed,DLC}$ being the simulation seeds for the individual DLC (i.e., the number of (converged) simulations in Table 2 for a given DLC at MWL equal to MSL). Inserting (29) in (28) yields the more compact representation

$$\Delta P_{eq,\mathrm{DLC}} = \left(\frac{\sum_{s\in\mathrm{DLC}}\left(\Delta P_{eq,s}\right)^m}{n_{seed,DLC}}\right)^{\frac{1}{m}} \tag{30}$$

As the DEL retains the unit of load, the DES $\Delta S_{eq,s}$ can be obtained by applying Navier's stress distribution formula to the DEL $\Delta P_{eq,s}$ for the individual nodes of interest in the supporting structure. However, the elements in the IEA 15-MW RWT are not consistent in terms of bending stiffness across the nodes, whereby Navier's formula will produce discontinuous stresses at the nodes. Thus, only the DES associated with the maximum nodal stresses in the monopile and tower circumference are considered for each node. Furthermore, only the contributions arising from the bending moments are included in the DESs

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

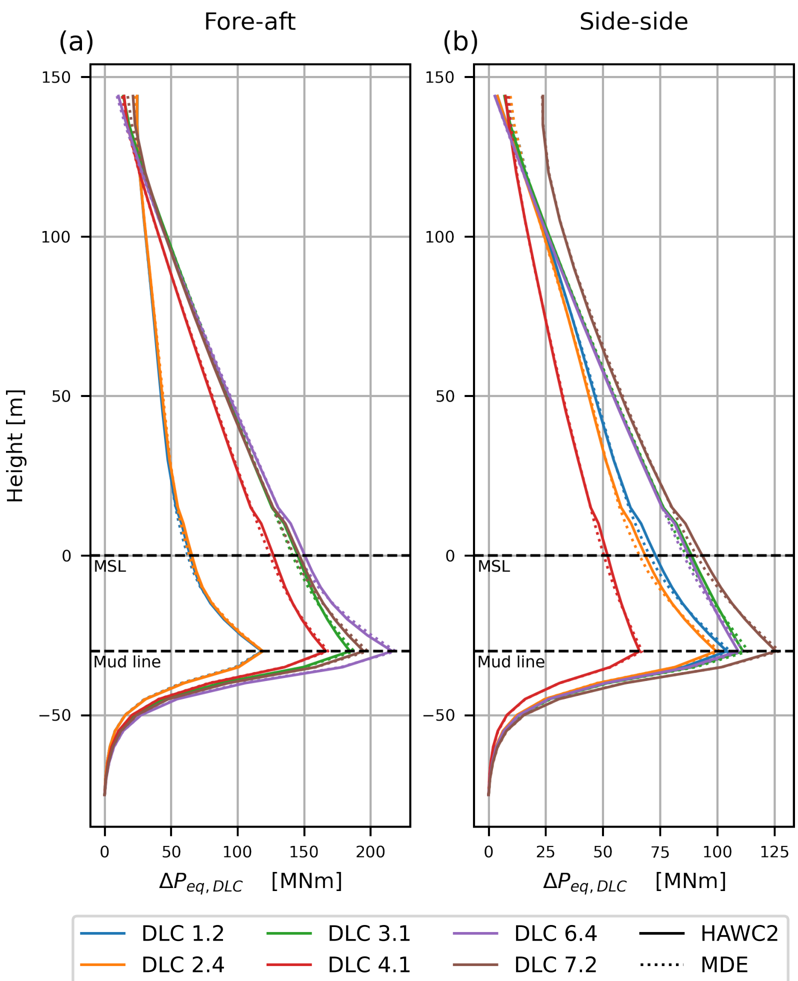

Figure 12. DELs calculated for the individual DLCs based on section moment load histories from HAWC2 (——) and MDE prediction (······) in the FA (a) and SS (b) direction of the IEA 15-MW RWT, as presented in (30).

which are calculated as

$$\Delta S_{eq,\mathrm{DLC}} = \left(\frac{\sum_{s \in \mathrm{DLC}} (\Delta S_{eq,s})^m}{n_{seed,DLC}} \right)^{\frac{1}{m}} \tag{31}$$

for the individual DLCs.

535 Figures 12 and 13 show the DELs and DESs related to the FA and SS section moments obtained from the HAWC2 simulations directly (——) and predicted using the multi-band MDE configuration from Section 4.1 (······).

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

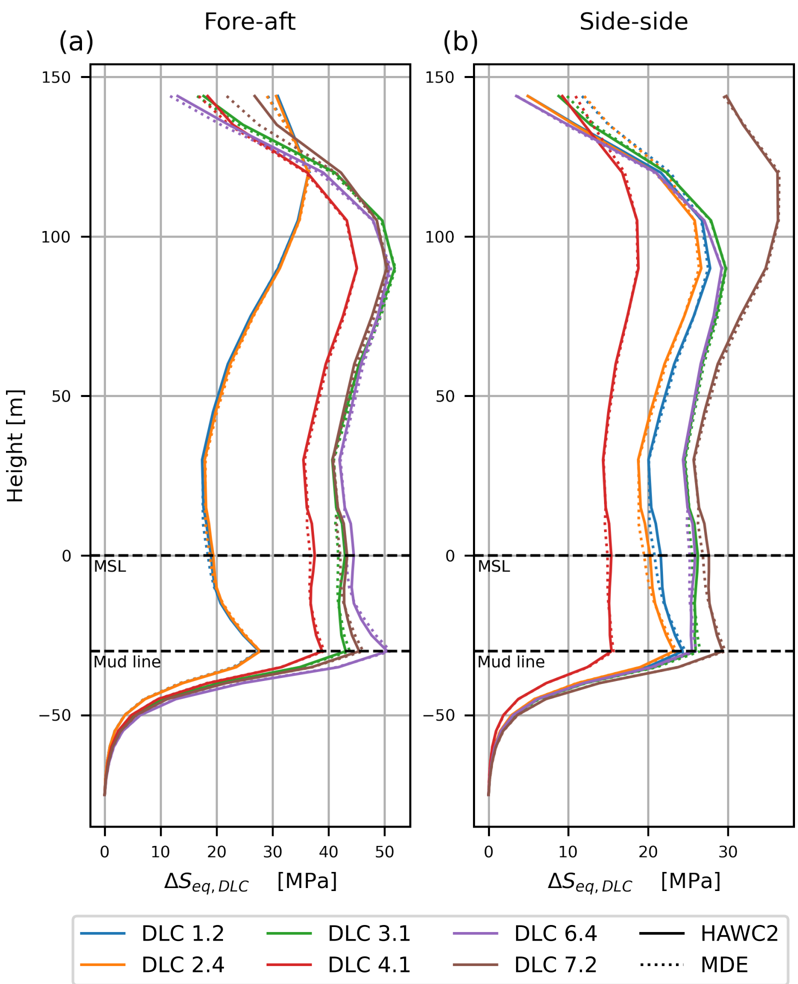

Figure 13. DESs calculated for the individual DLCs based on section moment load histories from HAWC2 (——) and MDE prediction (------) in the FA (a) and SS (b) direction of the IEA 15-MW RWT, as presented in (31).

As illustrated in Figure 12, the DELs generally look similar to the moment curve from the first tower bending modes or the thrust load (see Figure 8 and 10), with overlying effects from other loads and modes. In the FA direction (a), the operating DLCs 1.2 and 2.4 generally induce lower DELs compared to DLCs 3.1, 4.1, 6.4, and 7.2, with DLC 6.4 resulting in the maximum DEL across all DLCs and directions (FA and SS) at the mud line. The lower DELs of DLCs 1.2 and 2.4 can be attributed to the significant aerodynamic damping provided by the operating rotor, as discussed in Section 2.5. However, within the tower top region, specifically from around $120 - 144$ m, the operating DLCs show higher DELs due to uneven loading of the rotor and 3P effects, as discussed in Section 2.5. In the SS direction (b), in which the aerodynamic damping, the effects from thrust

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

load variations, and the 3P effects have less influence, the differences in DEL between operating and non-operating DLCs are generally smaller than those observed in the FA direction. It is worth noting that DLC 7.2 results in significantly higher DELs than all other DLCs at elevations above approximately 75 m. This can be attributed to the excitation of the second tower SS mode and the blade vibrations specific to this DLC, as described in further detail in Section 2.5.

In Figure 12, it is observed that for all DLCs in both the FA and SS directions, the MDE underestimates the DELs in a ± 15 m zone around the MSL. Because the error occurs in both the FA and SS direction, it is not expected to derive from inadequate modelling of the wave load. Instead, it is most likely caused by not representing the rotor flexibility in the second tower bending modes, which have a great impact on the DEL at the present location.

An inherent problem of the DELs in Figure 12 is that they do not explicitly account for changes in cross-section dimensions, whereby small DELs might still cause large stresses in regions with small tower diameters. Thus, in Figure 13, the DESs have large values in the tower-top region, where the corresponding DELs in Figure 12 are small. This indicates that the accuracy of the MDE cannot be ignored in the tower-top region. For the present analysis in Figure 13, this is especially important for DLCs 1.2 and 2.4 in the FA direction (a), and DLC 7.2 in the SS direction (b), which have their DES maxima in the tower-top region.

For the DESs estimated by MDE in Figure 13, it is seen that the multi-band MDE performs poorly at the tower top, where it consistently underestimates the DESs in the FA direction, and significantly overestimates the DESs in the SS direction for DLC 1.2 and 2.4. As discussed in section 2.5, the damage in the tower top is governed mostly by different phenomena associated with the rotor and blade dynamics, which are omitted in the RNA model. This may be the root cause of the large deviations observed for the DESs.

The MDE performance discussed above and presented in Figures 12 and 13 is based on a combined DEL and DES calculated for the individual DLCs for each elevation z along the IEA 15-MW RWT supporting structure. Thus, it corresponds to an averaged or mean error, conveniently used for assessing long-term MDE performance, although inherently sensitive to bias errors. Therefore, to assess the short-term performance of the MDE in the individual HAWC2 simulations, the relative error of the DESs is calculated for the individual HAWC2 simulations as

$$\varepsilon_{MDE} = \frac{\Delta S_{eq,s,MDE}}{\Delta S_{eq,s,HAWC2}} - 1 \tag{32}$$

where $(\,)_{HAWC2}$ denotes the DESs calculated from the HAWC2 time series of the FA and SS section moments and $(\,)_{MDE}$ denotes the DESs calculated from the corresponding MDE estimate. Figure 14 presents the relative error ε_{MDE} of the DESs, related to the FA and SS section moment and calculated for each elevation z along the IEA 15-MW RWT supporting structure.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

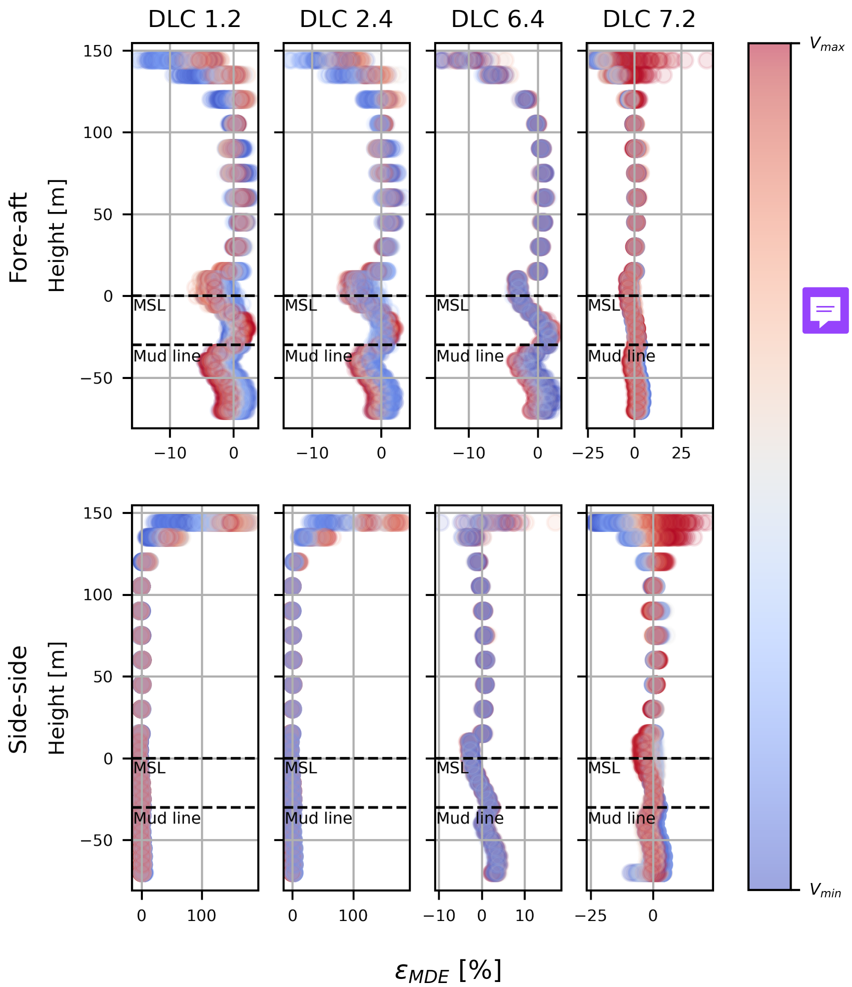

Figure 14. Error ε_{MDE} of DESs for the MDE predicted section moment load histories in the FA (top) and SS (bottom) direction of the IEA 15-MW RWT from the individual HAWC2 simulation s, as presented in (32). Color gradient represents the mean wind speed at the hub V_{hub} for the considered simulation s.

It is observed in Figure 14 that the error ε_{MDE} is predominantly in the range of $\pm 5\%$, except at the tower top, where the MDE performs inconsistently for the various DLCs. The error generally shows a dependency on the wind speed, which can be attributed to the operational and environmental variability of the IEA 15-MW RWT, arising from the varying rotor speeds, changing turbulence, and changing wave loads, which cannot be captured by the MDE, assuming a linear and time-invariant response.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

WIND ENERGY SCIENCE DISCUSSIONS

In the FA direction at the tower top elevation from $135-144$ m in Figure 14(top), the error ε_{MDE} appears to be inversely proportional to the wind speed for DLCs 1.2 and 2.4. As previously mentioned, the 3P effects significantly influence the DELs in the tower top for the operating load cases. However, the 3P effects include both tower shadow effects, wind shear, and turbulence, which makes it wind speed dependent. Therefore, the tower shadow effects can dominate in the low wind speed regime, while turbulence takes over at higher wind speeds, thereby modifying the response characteristics and consequently the MDE prediction accuracy. For DLC 6.4, no wind speed dependency of the MDE error is observed at the tower top. This is expected, as the tower-top DESs for this DLC are mainly governed by the inherent dynamics of the wind turbine (first tower FA mode and first edgewise blade mode), which are not influenced by operational variability (e.g., gyroscopic stiffening and blade pitching) in idle conditions. A similar response could then be expected for DLC 7.2 (also at standstill), where a large variance is however observed for the MDE error at the tower top. The difference between DLC 7.2 and 6.4 is the locked rotor configuration, which therefore must be the main cause of the MDE's inability to represent the tower top response from DLC 7.2, while the different azimuth angles of the locked rotor for this DLC can also affect the variance of tower top moment.

For the SS response, the error ε_{MDE} at the tower top in Figure 14(bottom) exhibits a high variability that appears proportional to the wind speed for DLCs 1.2 and 2.4. Because a similar error pattern is not observed for the non-operating DLC and the error for DLCs 1.2 and 2.4 highly depends on the wind speed, it is concluded that the error is related to the effects from the operating rotor, not captured by the MDE.

For DLC 7.2, it is observed that the MDE tends to underestimate the DES for low wind speeds while overestimating it for higher wind speeds. Furthermore, the error increases to a range between $\pm 25\,\%$, which is somewhat surprising considering the low discrepancies between DELs and DESs calculated from the MDE estimates and the HAWC2 outputs in Figures 12 and 13. Because the blade dynamics and second tower SS mode are significant at the tower top for DLC 7.2, it is concluded that the error is related to the too simple modelling of the RNA in the prediction FE model. Conversely, the wind speed dependency is more challenging to assess, although the associated increase in wind turbulence excites different modes.

Finally, the error ε_{MDE} between the MSL and the mud line in Figure 14 depends on the wind speed for all DLCs in the FA direction, while less so in the SS direction, as most clearly seen for DLC 6.4. This discrepancy is likely an effect of how the Ritz vectors include wave loads, i.e. not accounting for their sensitivity to wave height fluctuations or the dynamic interchange between drag and inertia forces. Furthermore, the wave load is applied to the monopile between mud line and MSL, thus ignoring the change in loading area during the transition from wave top to crest. In conclusion, the wave load Ritz vector is unable to capture the full complexity of the actual wave load in the IEA 15-MW RWT HAWC2 model.

When combining the conclusions from the above discussion, it is assessed that the MDE used in the present work generally performs well, except at the tower top. Hereby, the main challenges associated with the present use of MDE are:

- Capturing the local effects of the flexible and dynamic response of the rotor and blades.
- Including the effects from rotor flexibility and operation in the tower mode shapes used in the MDE.
- Including wind speed variability and time dependency of the waves in the MDE.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

Some of the errors observed in the present section may also be related to the chosen sensor locations and the associated MDE configuration presented in Section 4.1. However, as noise is not included in the present analysis, the noise-to-signal ratio is not an issue, whereby a non-optimal sensor location would have less impact in the present comparison.

5 Conclusions

This paper presents an overview of the dataset available in Pedersen et al. (2025), containing response simulations covering the Fatigue Limit State (FLS) design life of the IEA Wind 15-Megawatt Offshore Reference Wind Turbine with a monopile foundation (IEA 15-MW RWT) version 1.1.6.

The paper explores how diverse operational and environmental scenarios impact the Damage Equivalent Loads (DELs) calculated from the Fore-Aft (FA) and Side-Side (SS) section moment histories at the tower base, after which the relative lifetime damage for the individual FLS Design Load Cases (DLCs), described in IEC 61400-3-1:2019 (IEC, 2019b), is calculated at all nodes in the supporting structure of the IEA 15-MW RWT. It has been found that the DLCs representing *power production in normal conditions* (DLC 1.2), *parked turbine with idle rotor in normal conditions* (DLC 6.4), and *fault - locked rotor in normal conditions* (DLC 7.2) govern the lifetime damage of the supporting structure. The high contribution from DLC 1.2 occurs because of its high duration (90% of the design life) and the excitation at the tower top caused by 3P effects, while the contribution of DLCs 6.4 and 7.2 is large because of their high DELs associated with low aerodynamic damping.

The paper gives an overview of multi-band Modal Decomposition and Expansion (MDE) and a methodology for expressing the estimated response in sectional forces, after which it presents the Finite Element (FE) model used to calculate the Ritz vectors and mode shapes used to perform MDE. It explains the configuration used to perform MDE for the estimation of section moment time histories in the supporting structure of the IEA 15-MW RWT, which is based on rotation and displacement data from six HAWC2 sensors located at three elevations in the RWT tower (in both the FA and SS direction), and includes both the quasi-static and dynamic part of the frequency response.

The present work utilises MDE to estimate section moment histories in all nodes of the supporting structure of the IEA 15-MW RWT across different operational and environmental regimes represented in the data from Pedersen et al. (2025). Based on the moment histories, the combined DELs of the individual DLCs are calculated along with the combined DESs for the individual DLCs and the DESs from the individual HAWC2 simulations. The MDE generally performs well in estimating the combined DELs and DESs for the individual DLCs. However, notable errors occur around the tower top, specifically from 120 - 144 m above the Mean Sea Level (MSL), and at the MSL ± 15 m. These errors are attributed to the omission of local effects in the blade dynamics, and to blade flexibility not being included in the second tower bending mode shapes when using a lumped inertia Rotor-Nacelle-Assembly (RNA) model. The relative MDE errors for the DESs of the individual HAWC2 simulations ε_{MDE} are predominantly in the range of $\pm 5\,\%$, thus confirming that the MDE performs well in general. These MDE errors also underline that the MDE performs poorly around the tower top, where errors up to $180\,\%$ are observed. Finally, the MDE errors show a wind speed dependency, except in the SS direction, when the rotor idle. It is concluded that the wind speed dependency of the MDE error is caused by environmental and operational variability of the rotor, which is not captured by

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

the MDE assuming a linear and time-invariant response. Additionally, the lumped inertia RNA model and the wave load Ritz vector, which do not incorporate wind speed variability and the time-dependent nature of waves, likely contribute further to the observed wind speed dependency of the MDE error.

In future work, the authors suggest investigating errors in the frequency domain to increase confidence in the observed causes of error. The knowledge obtained from the present work will serve as a basis for updating the RNA model to include blade flexibility, and subsequently to include operational and environmental variability in the RNA modelling, e.g. by using individual RNA models for various wind speeds. The authors also plan to implement a wave load model that accounts for the waves' variation with the wind speed. Finally, it would be vital to investigate the MDE accuracy of a reduced number of physical sensors, e.g. from existing monitoring systems, not specifically designed for virtual sensing purposes.

Data availability. Dataset with synthetic wind turbine response data is available at https://doi.org/10.11583/DTU.24460090.

Code and data availability. Python code for reading data is available at https://github.com/madg-DTU/IEA-15MW-RWT-HAWC2-Monopile-Response-Database

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

Appendix A: Properties of IEA 15-MW RWT supporting structure

Table A1. Structural properties of element e in the IEA 15-MW RWT supporting structure. Including the node coordinates of the end nodes in the element $n_{e,1}$ and $n_{e,2}$, the Young's modulus E, the shear modulus G, the outer radius r, the cross section area A, the moments of inertia I_{xx} and I_{yy}, the polar moment of inertia I_p, and the distributed mass m along the height z.

Element No.	Coord $n_{e,1}$ [m]	Coord. $n_{e,2}$ [m]	E [Pa]	G [Pa]	r [m]	A [m^2]	I_{xx} [m^4]	I_{yy} [m^4]	I_p [m^4]	m [kg/m]
1	$(0,0,-75)$	$(0,0,-70)$	2.00E+11	7.93E+10	5.00E+00	1.73E+00	2.14E+01	2.14E+01	4.27E+01	1.44E+04
2	$(0,0,-70)$	$(0,0,-65)$	2.00E+11	7.93E+10	5.00E+00	1.73E+00	2.14E+01	2.14E+01	4.27E+01	1.44E+04
3	$(0,0,-65)$	$(0,0,-60)$	2.00E+11	7.93E+10	5.00E+00	1.73E+00	2.14E+01	2.14E+01	4.27E+01	1.44E+04
4	$(0,0,-60)$	$(0,0,-55)$	2.00E+11	7.93E+10	5.00E+00	1.73E+00	2.14E+01	2.14E+01	4.27E+01	1.44E+04
5	$(0,0,-55)$	$(0,0,-50)$	2.00E+11	7.93E+10	5.00E+00	1.73E+00	2.14E+01	2.14E+01	4.27E+01	1.44E+04
6	$(0,0,-50)$	$(0,0,-45)$	2.00E+11	7.93E+10	5.00E+00	1.73E+00	2.14E+01	2.14E+01	4.27E+01	1.44E+04
7	$(0,0,-45)$	$(0,0,-40)$	2.00E+11	7.93E+10	5.00E+00	1.73E+00	2.14E+01	2.14E+01	4.27E+01	1.44E+04
8	$(0,0,-40)$	$(0,0,-35)$	2.00E+11	7.93E+10	5.00E+00	1.73E+00	2.14E+01	2.14E+01	4.27E+01	1.44E+04
9	$(0,0,-35)$	$(0,0,-30)$	2.00E+11	7.93E+10	5.00E+00	1.73E+00	2.14E+01	2.14E+01	4.27E+01	1.44E+04
10	$(0,0,-30)$	$(0,0,-25)$	2.00E+11	7.93E+10	5.00E+00	1.73E+00	2.14E+01	2.14E+01	4.27E+01	1.44E+04
11	$(0,0,-25)$	$(0,0,-20)$	2.00E+11	7.93E+10	5.00E+00	1.67E+00	2.07E+01	2.07E+01	4.13E+01	1.39E+04
12	$(0,0,-20)$	$(0,0,-15)$	2.00E+11	7.93E+10	5.00E+00	1.61E+00	1.99E+01	1.99E+01	3.98E+01	1.34E+04
13	$(0,0,-15)$	$(0,0,-10)$	2.00E+11	7.93E+10	5.00E+00	1.55E+00	1.92E+01	1.92E+01	3.83E+01	1.29E+04
14	$(0,0,-10)$	$(0,0,-5)$	2.00E+11	7.93E+10	5.00E+00	1.49E+00	1.84E+01	1.84E+01	3.68E+01	1.24E+04
15	$(0,0,-5)$	$(0,0,0)$	2.00E+11	7.93E+10	5.00E+00	1.42E+00	1.76E+01	1.76E+01	3.53E+01	1.19E+04
16	$(0,0,0)$	$(0,0,5)$	2.00E+11	7.93E+10	5.00E+00	1.36E+00	1.69E+01	1.69E+01	3.37E+01	1.14E+04
17	$(0,0,5)$	$(0,0,10)$	2.00E+11	7.93E+10	5.00E+00	1.32E+00	1.64E+01	1.64E+01	3.28E+01	1.10E+04
18	$(0,0,10)$	$(0,0,15)$	2.00E+11	7.93E+10	5.00E+00	1.28E+00	1.59E+01	1.59E+01	3.19E+01	1.07E+04
19	$(0,0,15)$	$(0,0,30)$	2.00E+11	7.93E+10	5.00E+00	1.22E+00	1.52E+01	1.52E+01	3.03E+01	1.01E+04
20	$(0,0,30)$	$(0,0,45)$	2.00E+11	7.93E+10	4.99E+00	1.11E+00	1.36E+01	1.36E+01	2.72E+01	9.22E+03
21	$(0,0,45)$	$(0,0,60)$	2.00E+11	7.93E+10	4.89E+00	9.85E-01	1.10E+01	1.10E+01	2.21E+01	8.18E+03
22	$(0,0,60)$	$(0,0,75)$	2.00E+11	7.93E+10	4.58E+00	8.65E-01	8.36E+00	8.36E+00	1.67E+01	7.20E+03
23	$(0,0,75)$	$(0,0,90)$	2.00E+11	7.93E+10	4.21E+00	7.42E-01	5.95E+00	5.95E+00	1.19E+01	6.18E+03
24	$(0,0,90)$	$(0,0,105)$	2.00E+11	7.93E+10	3.78E+00	6.25E-01	4.07E+00	4.07E+00	8.14E+00	5.20E+03
25	$(0,0,105)$	$(0,0,120)$	2.00E+11	7.93E+10	3.47E+00	5.13E-01	2.98E+00	2.98E+00	5.95E+00	4.28E+03
26	$(0,0,120)$	$(0,0,135)$	2.00E+11	7.93E+10	3.37E+00	4.46E-01	2.44E+00	2.44E+00	4.87E+00	3.72E+03
27	$(0,0,135)$	$(0,0,144)$	2.00E+11	7.93E+10	3.28E+00	4.90E-01	2.59E+00	2.59E+00	5.18E+00	4.09E+03

Author contributions. Conceptualization and methodology: MGP, JR, IFA, and JH; Wind turbine response simulations: MGP and JR; Data preparation and interpretation: MGP and JR; Prediction FE model: MGP and JH; Modal decomposition and expansion: MGP; writing (original draft): MGP; supervision and writing (review and editing): JR, IFA, and JH

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

Competing interests. The contact author has declared that none of the authors has any competing interests.

Acknowledgements. This work is partially funded by the Innovation Fund Denmark (grant 1155-00008B) and COWIfonden (grant C-153.01). The authors acknowledge the use of AI language models for proofreading and enhancing the readability of this manuscript.

References

ASTM E1049-85: Standard practices for cycle counting in fatigue analysis, https://doi.org/10.1520/E1049-85R17, 2017.

Augustyn, D., Pedersen, R. R., Tygesen, U. T., Ulriksen, M. D., and Sørensen, J. D.: Feasibility of modal expansion for virtual sensing in offshore wind jacket substructures, Marine Structures, 79, 1–17, https://doi.org/10.1016/j.marstruc.2021.103019, 2021.

Baqersad, J., Niezrecki, C., and Avitabile, P.: Full-field dynamic strain prediction on a wind turbine using displacements of optical targets measured by stereophotogrammetry, Mechanical Systems and Signal Processing, 62-63, 284–295, https://doi.org/10.1016/J.YMSSP.2015.03.021, 2015.

Bilbao, J., Lourens, E.-M., Schulze, A., and Ziegler, L.: Virtual sensing in an onshore wind turbine tower using a Gaussian process latent force model, Data-Centric Engineering, 3, https://doi.org/10.1017/DCE.2022.38, 2022.

de N Santos, F., D'Antuono, P., Robbelein, K., Noppe, N., Weijtjens, W., and Devriendt, C.: Long-term fatigue estimation on offshore wind turbines interface loads through loss function physics-guided learning of neural networks, Renewable Energy, 205, 461–474, https://doi.org/10.1016/J.renene.2023.01.093, 2023.

DHI: Energy Island North Sea, Metocean Assessment, Part A: Data Basis – Measurements and Models, 2023-06-28, Tech. rep., DHI, 2023a.

DHI: Energy Island North Sea, Metocean Assessment, Part B: Data Analyses – Energy Island, 2023-08-09, Tech. rep., DHI, 2023b.

DHI: Energy Island North Sea, Metocean Assessment, Part C: Data Analyses – Wind Farm Area, 2023-08-09, Tech. rep., DHI, 2023c.

DSF/FprEN 1993-1-9: Draft no. M372165 - Eurocode 3: Design of steel structures - Part 1-9: Fatigue, 2024.

Eftekhar Azam, S., Chatzi, E., and Papadimitriou, C.: A dual Kalman filter approach for state estimation via output-only acceleration measurements, Mechanical Systems and Signal Processing, 60-61, 866–886, https://doi.org/10.1016/j.ymssp.2015.02.001, 2015.

Ercan, T. and Papadimitriou, C.: Optimal sensor placement for reliable virtual sensing using modal expansion and information theory, Sensors, 21, https://doi.org/10.3390/s21103400, 2021.

Fallais, D., Sastre Jurado, C., Weijtjens, W., and Devriendt, C.: Validation of a model-based dual-band modal decomposition and expansion approach for fatigue monitoring of offshore wind turbines, in: 11th European Workshop on Structural Health Monitoring, EWSHM 2024, vol. 29, NDT.net, https://doi.org/10.58286/29660, 2024.

Gaertner, E., Rinker, J., Sethuraman, L., Anderson, B., Zahle, F., Barter, G., Abbas, N., Meng, F., Bortolotti, P., Skrzypinski, W., Scott, G., Feil, R., Bredmose, H., Dykes, K., Sheilds, M., Allen, C., and Viselli, A.: IEA Wind TCP Task 37: Definition of the IEA Wind 15-Megawatt Offshore Reference Wind Turbine, Tech. rep., National Renewable Energy Laboratory, Golden CO, https://www.nrel.gov/docs/fy20osti/75698.pdf, 2020a.

Gaertner, E., Rinker, J., Sethuraman, L., Anderson, B., Zahle, F., Barter, G., Nikhar, A., Fanzhong, M., Pietro, B., Witold, S., George, S., Roland, F., Henrik, B., Katherine, D., Matt, S., Christopher, A., and Anthony, V.: IEA-15-240-RWT Frequently Asked Questions (FAQ), https://github.com/IEAWindSystems/IEA-15-240-RWT/wiki/Frequently-Asked-Questions-(FAQ), 2020b.

Gaertner, E., Rinker, J., Sethuraman, L., Anderson, B., Zahle, F., Barter, G., Abbas, N., Meng, F., Bortolotti, P., Skrzypinski, W., Scott, G., Feil, R., Bredmose, H., Dykes, K., Sheilds, M., Allen, C., and Viselli, A.: IEAWindTask37/IEA-15-240-RWT: 15MW reference wind turbine repository developed in conjunction with IEA Wind. Version 1.1.6, https://github.com/IEAWindTask37/IEA-15-240-RWT, 2023.

Henkel, M., Häfele, J., Weijtjens, W., Devriendt, C., Gebhardt, C. G., and Rolfes, R.: Strain estimation for offshore wind turbines with jacket substructures using dual-band modal expansion, Marine Structures, 71, https://doi.org/10.1016/j.marstruc.2020.102731, 2020.

Henkel, M., Weijtjens, W., and Devriendt, C.: Fatigue stress estimation for submerged and sub-soil welds of offshore wind turbines on monopiles using modal expansion, Energies, 14, https://doi.org/10.3390/en14227576, 2021.

IEC: IEC 61400-1:2019, Wind energy generation systems – Part 1: Design requirements, 2019a.

IEC: IEC 61400-3-1:2019, Wind energy generation systems – Part 3-1: Design requirements for fixed offshore wind turbines, 2019b.

Iliopoulos, A., Shirzadeh, R., Weijtjens, W., Guillaume, P., Hemelrijck, D. V., and Devriendt, C.: A modal decomposition and expansion approach for prediction of dynamic responses on a monopile offshore wind turbine using a limited number of vibration sensors, Mechanical Systems and Signal Processing, 68-69, 84–104, https://doi.org/10.1016/j.ymssp.2015.07.016, 2016.

Iliopoulos, A., Weijtjens, W., Hemelrijck, D. V., and Devriendt, C.: Fatigue assessment of offshore wind turbines on monopile foundations using multi-band modal expansion, WIND ENERGY, 20, 1463–1479, https://doi.org/10.1002/we.2104, 2017.

Iliopoulos, A. N., Devriendt, C., Iliopoulos, S. N., and Van Hemelrijck, D.: Continuous fatigue assessment of offshore wind turbines using a stress prediction technique, in: Health Monitoring of Structural and Biological Systems, vol. 9064, p. 90640S, https://doi.org/10.1117/12.2045576, 2014.

Krenk, S. and Høgsberg, J.: Statics and mechanics of structures, Springer, ISBN 978-94-007-6113-1, https://doi.org/10.1007/978-94-007-6113-1, 2013.

Larsen, T. J. and Hansen, A. M.: How 2 HAWC2 , the user's manual, 2021.

Maes, K., Iliopoulos, A., Weijtjens, W., Devriendt, C., and Lombaert, G.: Dynamic strain estimation for fatigue assessment of an offshore monopile wind turbine using filtering and modal expansion algorithms, Mechanical Systems and Signal Processing, 76-77, 592–611, https://doi.org/10.1016/j.ymssp.2016.01.004, 2016.

Mehrjoo, A., Song, M., Moaveni, B., Papadimitriou, C., and Hines, E.: Optimal sensor placement for parameter estimation and virtual sensing of strains on an offshore wind turbine considering sensor installation cost, Mechanical Systems and Signal Processing, 169, 108 787, https://doi.org/10.1016/j.ymssp.2021.108787, 2022.

Natarajan, A., Hansen, M. H., and Wang, S.: Design Load Basis for Offshore Wind turbines: DTU Wind Energy Report No. E-0133, DTU Department of Wind Energy, ISBN 978-87-93278-99-8, 2016.

Noppe, N., Iliopoulos, A., Weijtjens, W., and Devriendt, C.: Full load estimation of an offshore wind turbine based on SCADA and accelerometer data, in: Journal of Physics: Conference Series, vol. 753, p. 072025, https://doi.org/10.1088/1742-6596/753/7/072025, 2016.

Pedersen, M. G., Rinker, J., Høgsberg, J., and Farreras, I. A.: IEA-15MW-RWT-Monopile HAWC2 Response Database, https://doi.org/10.11583/DTU.24460090.v3, 2025.

Rinker, J., Gaertner, E., Zahle, F., Skrzypiński, W., Abbas, N., Bredmose, H., Barter, G., and Dykes, K.: Comparison of loads from HAWC2 and OpenFAST for the IEA Wind 15 MW Reference Wind Turbine, in: Journal of Physics: Conference Series, vol. 1618, p. 052052, https://doi.org/10.1088/1742-6596/1618/5/052052, 2020.

Skafte, A., Kristoffersen, J., Vestermark, J., Tygesen, U. T., and Brincker, R.: Experimental study of strain prediction on wave induced structures using modal decomposition and quasi static Ritz vectors, Engineering Structures, 136, 261–276, https://doi.org/10.1016/j.engstruct.2017.01.014, 2017.

Sumer, B. M. and Fredsøe, J.: Hydrodynamics Around Cylindrical Structures, World Scientific, 1997.

Tarpø, M.: Stress Estimation of Offshore Structures, Ph.D. thesis, Aahus University, ISBN 978-87-7507-491-4, https://ebooks.au.dk/aul/catalog/book/393, 2020.

Toftekær, J. F., Vestermark, J. T., and Jepsen, M. S.: Uncertainty of Virtually Sensed Stress Ranges in Offshore Wind Support Structures, in: Proceedings of the ASME 2023 42nd International Conference on Ocean, Offshore and Arctic Engineering, p. V001T01A011, https://doi.org/10.1115/OMAE2023-101045, 2023.

https://doi.org/10.5194/wes-2025-89
Preprint. Discussion started: 27 June 2025

Veldkamp, H. F.: Chances in Wind Energy: A probabilistic Approach to Wind Turbine Fatigue Design, Ph.D. thesis, Delft University, ISBN 978-90-76468-12-9, 2006.

Vettori, S., Di Lorenzo, E., Peeters, B., Luczak, M. M., and Chatzi, E.: An adaptive-noise Augmented Kalman Filter approach for input-state estimation in structural dynamics, Mechanical Systems and Signal Processing, 184, 109 654, https://doi.org/10.1016/j.ymssp.2022.109654, 2023.

Zou, J., Lourens, E.-M., and Cicirello, A.: Virtual sensing of subsoil strain response in monopile-based offshore wind turbines via Gaussian process latent force models, Mechanical Systems and Signal Processing, 200, 110 488, https://doi.org/10.1016/J.YMSSP.2023.110488, 2023.