

# Point-to-point reply to reviewers

Paper wes-2025-120

Title:

Large Eddy Simulation of the IEA 15-MW Wind Turbine Using a Two-Way Coupled Fluid-Structure Interaction Model

April 7, 2026

The authors wish to thank the reviewers for their comments and suggestions on the paper to enhance the manuscript. The paper has been revised accordingly. The revised parts have been highlighted in blue in the updated manuscript. If mentioned, the page number refers to the revised paper. In the following we report point-to-point replies concerning the questions and comments raised by the reviewers.

## Reviewer #1

### Main Critique Points

- ***Incomplete examination of the apparent slower wake recovery due to tower presence (Fig. 4):*** The authors compare the rotor-averaged streamwise velocity along the centerline, normalized by the undisturbed velocity at hub height, between the present work and Santoni et al. (2017). They observe a trend opposite in sign to Santoni et al. (2017), namely a slower wake recovery when tower and nacelle are included. First, this comparison is of limited interpretability because Santoni et al. (2017) investigated a 5 MW turbine under uniform inflow (i.e., different inflow and turbine class compared to the present case). While the authors acknowledge this limitation, it remains unclear what insight is gained by comparing the two datasets given the confounding differences. Second, the manuscript does not provide a quantitative investigation to establish whether the observed slower recovery is a robust physical effect or a sensitivity to modeling/setup choices (e.g., tower/rotor scaling, inflow shear setup, actuator or boundary-condition choices). The authors offer plausible hypotheses and acknowledge the lack of validation, but even though make physical conclusions based on the observed results (“indicating that wake recovery is slightly hindered by the presence of the tower” in line 330 of the manuscript).

We agree with the referee that the comparison with Santoni et al. (2017), who investigated a 5 MW turbine under uniform inflow, does not allow to understand whether the slower wake recovery observed when tower and nacelle are included, is due to the different turbine class or to the different inflow. Thus, we have carried out a LES computation of an NREL 5MW wind turbine, under the same conditions of the 15MW one (same laminar sheared inflow, wind velocity etc.). As the referee can observe in figure 4 of the paper, also included in this letter as figure 1, in this new computation, as in Santoni et al. 2017, the rotor-averaged velocity for the TN configuration in the wake remains rather higher than for the OR case, indicating that wake recovery for an NREL 5MW turbine is indeed advanced by the presence of the tower, no matter the inflow considered. Comparing these results to the present data, we can confidently

conclude that the slower wake recovery observed for the 15MW turbine in the presence of the tower appears to be due to geometrical features of the turbine itself, rather than the inflow characteristics. This comparison shows clearly that the observed physical effects do not depend on the inflow shear setup. Concerning the other possible effects mentioned, e.g., tower/rotor scaling, actuator or boundary-condition choices, we would like to specify that the tower/rotor scaling has not been freely chosen by the authors, but it is directly taken from the IEA 15-MW wind turbine definition in its monopile configuration as reported in Gaertner et al., 2020b. Concerning the actuator line method and boundary conditions, the numerical setup has been cross-validated with many other aeroelastic numerical codes in the International Energy Agency (IEA) Wind TCP Task 47 (Cacciola et al., 2025), also considering turbulent inflow conditions (Schepers et al., 2025). In particular, in the IEA Task 47, it has been reported that high-fidelity codes appear rather consistent in predicting loads for the IEA-15MW turbine (Cacciola et al., 2025). Thus, we think that the numerical setup is sufficiently robust and validated for ensuring the physical origin of the reported behaviour. We hope that the new computation carried out for a different inflow might reassure the referee and the reader in general about the conclusion that, indeed, for the 15MW turbine, “wake recovery is slightly hindered by the presence of the tower”. As suggested by the reviewer, we have included in figure 4 of the revised paper, reported here for completeness, a reproduction of the nondimensionalized plot from Santoni et al. (2017), together with the same curve for the the IEA-15MW and NREL-5MW with same inflow, along with the related text.

- *Mixing fidelities for validation: the present work compares CFD-CSD/OV with OpenFAST: ElastoDyn and CFD-CSD/T with OpenFAST: BeamDyn. This is fair insofar as the same aeroelastic DOFs are compared. However, the compared numerical models are the sum of combining different submodels that hold different fidelities, limiting the interpretability of the presented comparison. The authors recognize that the cross-solver comparisons are not directly attributable to a single modeling component; however, the manuscript nevertheless bases its central conclusions on these confounded comparisons. Without controlled sensitivity studies or a comparable reference, the resulting insights remain limited and largely descriptive*

We acknowledge that the four solvers employed differ in both their aerodynamic and structural modeling approaches. However, it is not easy to compare high-fidelity to engineering fidelity tools varying only one model at a time, since the tools freely available not always comply with what has been coded in the in-house high-fidelity code. In particular, we are not aware of engineering fidelity tools that use exactly the same structural model used in our code, thus the comparison proposed by this referee would require extensive coding. We have to remark that, in our knowledge, this work reports the only available high-fidelity aeroelastic study of the IEA-15MW wind turbine considering also the effect of tower and nacelle. As discussed in Trigaux et al. (2024), among others, the use of high-fidelity aerodynamic models is crucial to predict the aeroelastic effects of large rotors, thus we think that the results of high-fidelity computations of a large-scale wind turbine taking into account both tower/nacelle and aeroelastic effects have an intrinsic literature value on their own, set aside the comparison with other solvers.

- *Unclear storyline and vague overall goal: the presented work is a thorough and valid starting point for good aeroelastic benchmarks, however the reviewers struggled to understand the intent of the work. Is it to validate the setup of a 15 MW turbine for your inhouse solver? Or is it to examine the effects of modelling the presence of towers in aeroelastic CFD? Or is it to test the limits of BEM for 15MW turbine aeroelasticity? As a result of the many aspects the manuscript covers, the conclusion is too long (about 50 lines) and unclear. The 36-page length of the paper makes it exhausting to read.*

In order to follow the suggestion of this referee, and improve the storyline, we have decided to limit to a minimum the discussion about the effect of the tower on the flow, eliminating some figures and related text, and eliminating all comments about this point in the conclusion section. Now the paper has less than one page of discussion about the effect of the tower on the flow, which seems to us necessary for understanding what happens to the phase-averaged incidence angle, aerodynamic forces, power and thrust coefficients etc., when the blade passes in front of the tower, and no mention to it in the conclusion section. We have also rephrased some parts in the introduction and conclusion sections, in order to clarify the goal of the paper, which is to provide an accurate study of the IEA-15MW wind turbine assembly considering aeroelasticity of the blades.

- *I'm very sorry for my decision and acknowledge the rigor and diligence of the authors. I appreciate the willingness the authors showed for incorporating the reviewers' comments. However, the manuscript appears like the concatenation of two good papers that are not finished yet. I suggest rebuilding the paper from the ground up and split into two possible publications: (i) one on validating the effects of the tower and nacelle e.g., on a stiff rotor (isolating effects) and compare the observed results with another (established) LES solver; this would increase the credibility of related findings and this could be followed by (ii) one publication on comparing these simulations with the same setup but added structural response. Then you would only vary one submodel instead of varying fidelity of both, flow and structural model. In case you want to compare with BEM, try to use the same structural solver for both, FVM-LES and BEM.*

We appreciate the suggestion of the referee on splitting the paper in two, but we think that the results concerning the effects of the tower and nacelle on a stiff rotor presented here are not sufficient for granting a complete paper on this subject. Also, the comparison between the TN and RO cases in the present paper is essential for explaining the sudden drops observed in various quantities shown throughout the paper (phase-averaged incidence angle, aerodynamic forces, power and thrust coefficients etc..) in correspondence with the passage of the blade in front of the tower. Thus, in order to follow the suggestion of this referee we have decided to limit to a minimum the discussion about the effect of the tower on the flow, eliminating the figure and related text on the kinetic energy entrainment with and without the tower, and eliminating all comments about this point in the conclusion section. Now the paper has less than one page of discussion about the effect of the tower on the flow, which seems to us necessary for understanding what happens to the phase-averaged incidence angle, aerodynamic forces, power and thrust coefficients etc., when the blade passes in front of the tower. Also, in the conclusions we have eliminated any mention of the effect of the tower on the flow, in order to maintain coherence with the goal of the paper, which is to study the aeroelastic response of a large-scale 15MW wind turbine, comparing the predictions of BEM with LES (as clearly stated at the end of the introduction, page 4). We hope that this might satisfy the referee and ensure acceptance of the present work.

## References

- [1] Christian Santoni, Kenneth Carrasquillo, Isnardo Arenas-Navarro, and Stefano Leonardi. Effect of tower and nacelle on the flow past a wind turbine. *Wind Energy*, 20(12):1927–1939, 2017.

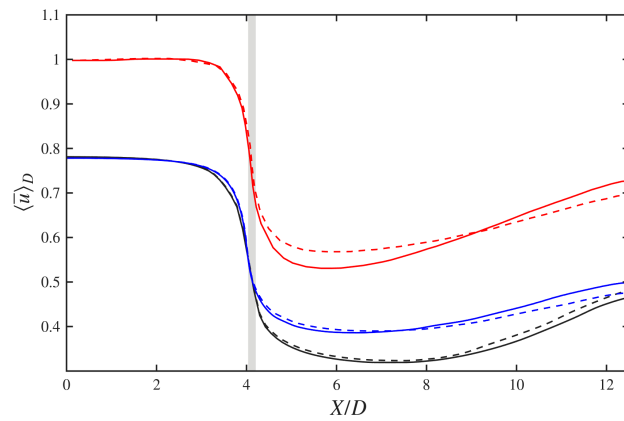


Figure 1: Rotor-averaged velocity along the streamwise direction normalized by the undisturbed velocity at the rotor height, namely,  $U_\infty = 10 \text{ m/s}$ , for the present data (black curves), a computation of the NREL 5MW turbine for the same configuration and inflow (blue curves) and the work of [1] (red curves). The grey region represents the area covered by the rotor. (RO ----, TN —).

## Reviewer #2

- *My comment (round 1): Please provide more details about the procedure used to compute the flow velocity contribution to  $u_{rel}$ . Which approach was used (Line Averaging Technique, for instance, or others?).*

*Reply: The definition of  $u_{rel}$  is given in eq. (7). The interpolation from the structural FEM nodes for  $u_{def}$  to the Cartesian CFD mesh on which  $u_{abs}$  is defined, is made with a simple linear interpolation. Then, the resulting  $u_{rel}$  is interpolated on the nodes discretizing the blade.*

*My additional comment: This means that  $u_{abs}$  is computed on the CFD mesh nodes, then what remains not clear is how this information is used to compute the sectional AOA which needs a single value of  $u_{abs}$  for its computations. Typically, the Line Averaging Technique is used to define, at each blade section a circle centered at  $c/4$  and with  $1chord$  radius where the flow velocity is computed and averaged. Please clarify this aspect.*

The evaluation of the AoA is carried out centering the actuator line at  $C/4$  and creating a circle of radius  $\epsilon$  around the line. Thereafter, a trilinear interpolation is applied using the points around the corresponding cell.

- *My comment (round 1): It is important to provide an indication of the computational costs of a typical solution.*

*Reply: The increase in computational cost induced by the structural solver is negligible, as it accounts for less than 2%.*

*My additional comment: My comment was referring the full aeroelastic solution, not only the structural part.*

The complete aeroelastic simulation required approximately 96 hours of wall-clock time, including an initial transient of about 48 hours. The computation was performed using 512 CPUs and covered roughly 90 rotor revolutions.

**The revised manuscript with the highlighted changes mentioned in this point-to-point document is attached in the following pages.**

# Large Eddy Simulation of the IEA 15-MW Wind Turbine Using a Two-Way Coupled Fluid-Structure Interaction Model

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## Abstract

The aim of the work is studying the aeroelastic response of the IEA 15 MW Reference Wind Turbine (RWT) large-scale wind turbine using a high-fidelity fluid-structure interaction solver that combines large-eddy simulation with a modal computational structural dynamics solver through a two-way coupling. The fluid solver employs the actuator line model to simulate the interaction between the turbine blades and the fluid and the immersed boundary method to model the presence of the tower and nacelle. The results are compared with those obtained by the OpenFAST software, which is a well-known numerical tool for engineering predictions. A series of simulations have been performed with and without the presence of the tower and nacelle to better understand the effects of these components on flow structures and structural deformations. The largest discrepancies among the solvers have been observed in correspondence with the blade passage in front of the tower, which induces an abrupt alteration in the local incidence angle of the flow. Moreover, by comparing the outcomes of different structural approximations, it has been established that taking into account the torsional degree of freedom considerably affects the deformations, aerodynamic loads and power coefficient. Whereas, the nonlinearity of the solver appears to have a weak effect on the same quantities.

## 30 **Keywords**

31 Aeroelasticity, Large Eddy Simulation, Actuator Line Model, Fluid-Structure Interaction, Computa-  
32 tional Fluid Dynamics, Computational Structural Dynamics, Blade Element Momentum, IEA-15MW  
33 Wind Turbine.

## 34 **1 Introduction**

35 Wind energy has become a crucial component of the global transition toward renewable energy sources.  
36 The increasing demand for clean energy has led to the development of large-scale wind turbines, such  
37 as the IEA 15-MW offshore wind turbine developed within IEA Wind Task 37 (Gaertner et al., 2020a).  
38 This turbine, with a rotor diameter of 240 meters and blades measuring 117 meters in length, rep-  
39 represents a new frontier in wind energy technology (Gaertner et al., 2020b), and research is currently  
40 pointing towards even larger rotors, reaching 22-MW of power production (Zahle et al., 2024). The  
41 increasing scale and flexibility of such newly designed turbines present significant engineering chal-  
42 lenges, particularly in predicting their aeroelastic response (Burton et al., 2011; Zheng et al., 2023).  
43 As turbines grow in size, their structural components, especially the blades, are subject to complex  
44 aerodynamic forces that cause deformations, which in turn affect the aerodynamic loads. Understand-  
45 ing these interactions is essential to improve the performance, reliability, and longevity of large-scale  
46 wind turbines (Manwell et al., 2010). In the worst cases, aeroelastic instabilities such as edgewise  
47 instability and flutter might even lead to blade damage, as reported for the Lunderskov Mobelfabrik  
48 19 m wind turbine blades (Moeller, 1997), with devastating effects on the turbine performance.

49 Aeroelasticity is critical in the design and analysis of modern wind turbines. Aeroelastic phenomena  
50 such as dynamic stall, flutter, and their effects on fatigue loadings can have significant effects on tur-  
51 bine performance, particularly as the blade length increases (Hansen, 2007). These blades experience  
52 varying aerodynamic forces along their span, which can lead to substantial deformations. When blades  
53 deform, they alter the local flow field, which in turn modifies the aerodynamic loads acting on them.  
54 This feedback loop between aerodynamic forces and structural deformation makes it very difficult  
55 to predict modern large-scale turbine performance under real-world operating conditions (Vermeer  
56 et al., 2003; Wang et al., 2016a). Accurate evaluation of these interactions is key for ensuring turbine  
57 efficiency and structural integrity, especially in offshore environments where wind conditions are more  
58 severe (Bayati et al., 2017).

59 The numerical modeling of the blades in most of the numerical aeroelastic codes used nowadays  
60 (Schepers et al., 2021) is accomplished by the Blade Element Momentum (BEM) model, due to its  
61 robustness and low computational cost. It has been shown in the framework of the IEA WIND Task  
62 47 Boorsma et al. (2023, 2024) that, if properly tuned, BEM can be a valuable engineering-type solver,  
63 complementary to higher fidelity ones which have also higher computational costs. However, BEM  
64 has still some limitations, since it relies on simplifying assumptions made on the impinging flow, such  
65 as models of dynamic stall, dynamic inflow, yaw and tilt flows, and corrections of the aerofoil data for  
66 taking into account three-dimensional effects and tip losses. More computationally expensive models  
67 exist, such as panel methods and in general potential flow solvers and/or free-vortex wake methods, as  
68 well as the actuator disc methods. Panel and free-wake vortex methods are able to capture unsteady

69 blade/rotor aerodynamics with good accuracy in different operating conditions (including off-design)  
70 whenever massive flow separation phenomena do not occur Boorsma et al. (2018); Ribeiro et al. (2023).  
71 However, those models need reference high-fidelity data in order to refine and/or assess the reliability  
72 of these lower fidelity models. Therefore, the application of computational fluid dynamics (CFD) to  
73 full-scale turbines is needed as a reference for describing the complex aerodynamics of the flow field  
74 accurately (Sørensen, 2011), although for a limited number of flow case due to its high computational  
75 cost.

76 However, coupling three-dimensional CFD simulations with computational structural dynamics (CSD)  
77 solvers taking into account the deformation of the blade is not trivial. Three-dimensional structural  
78 finite-element models are in fact able to fully describe the complex shape of a wind turbine blade but,  
79 although accurate, these models are computationally expensive and hard to implement, leading to  
80 only a few examples of coupling with CFD codes (Bazilevs et al., 2011; Yu and Kwon, 2014). Since  
81 wind turbine blades are slender structures, their structural modeling can be more easily achieved using  
82 beam models, where the blade is discretized as a series of one-dimensional beam elements, each char-  
83 acterised by a given cross-sectional stiffness and mass per unit length. One-dimensional beam models  
84 can be either modal, since natural frequencies and mode shapes of a turbine are directly related to the  
85 natural frequencies of its blades, or they can rely on the geometrically exact beam theory including  
86 non-linear effects (Sabale and Gopal, 2019).

87 Due to their ability to provide a rapid evaluation of the turbine performance, numerical tools based  
88 on the BEM approach equipped with aeroelastic modules based on one-dimensional beam models,  
89 are currently widespread (Schepers et al., 2021). A notable example is OpenFAST, a numerical code  
90 developed at NREL (Jonkman, 2013) and widely used for aeroelastic simulations, which employs BEM  
91 theory for aerodynamic modeling and various structural solvers, such as ElastoDyn (Damiani et al.,  
92 2015) and BeamDyn (Wang et al., 2016b), for structural deformation analysis. However, it is still  
93 not clear whether the predictions of such lifting-line aeroelastic codes are sufficiently accurate for  
94 large-scale turbines, in which the effect of shear and inflow turbulence can lead to complex inflows and  
95 turbine aerodynamic responses. Comparing the predictions of OpenFAST with those of a Large-Eddy  
96 Simulation (LES) equipped with a structural one-dimensional beam model has shown that, for an  
97 NREL 5MW wind turbine, the passage in front of the tower leads to large deformations which are  
98 largely underestimated by OpenFAST (Bernardi et al., 2023).

99 Concerning rotors of even larger size, such as the IEA 15-MW reference turbine, it is not yet known  
100 whether these discrepancies in the predictions of lifting-line codes with respect to CFD are even more  
101 consistent. Using the unsteady Reynolds-Averaged Navier-Stokes (URANS) equations coupled with  
102 an aeroelastic module, as reported by Pagamonci et al. (2023), has shown that neglecting the flexibility  
103 of the blades in numerical simulations leads to an underestimation of the rotor thrust of approximately  
104 2.5% for the IEA 15-MW turbine, which is not observed for the smaller NREL 5MW rotor. More-  
105 over, this work also concluded that the deformation of long, slender blades may act as a filter for the  
106 high-frequency fluctuations arising from the flow field, proving that taking into account the blades'  
107 aeroelasticity in the design process of these machines is key for the future upscaling of turbine rotors.  
108 Furthermore, Trigaux et al. (2024) observed how the use of high-fidelity aerodynamic models is crucial  
109 to predict the aeroelastic effects of large rotors. These results suggest the need to investigate this issue  
110 resorting to LES, which is capable of describing the dynamics of the flow more accurately.

111 In this context, the present work aims at studying the aeroelastic response of a large-scale 15-MW  
112 wind turbine tower/nacelle assembly, resorting to both high-fidelity and engineering-fidelity compu-  
113 tations. The investigation is conducted mainly by means of LES, whose results are compared with  
114 those obtained by more simple and less computationally expensive models, such as the OpenFAST  
115 code. Computations are performed by an in-house LES code using the immersed boundary method to  
116 model the tower and nacelle and the Actuator Line Model (ALM) for blade modeling, coupled with a  
117 structural modal solver, originally developed by Della Posta et al. (2022).  
118 The discussion of the results highlights the role of the tower and nacelle in the dynamics of the aerody-  
119 namical forces, thrust and power coefficients, as well as in the distribution of turbulent kinetic energy  
120 within the wake, which could have an impact on the aerodynamic loads of downstream turbines in  
121 wind farms. Moreover, the effect of the torsional degree of freedom has been investigated by comparing  
122 the outcomes of different structural approximations.  
123 The work is structured as follows. In section 2, the aerodynamic and structural solvers of both CFD-  
124 CSD and OpenFAST codes are described in detail. In section 3, the numerical setup is presented. In  
125 section 4, relevant results are discussed, and conclusions are drawn in section 5.

## 126 **2 Methodologies**

### 127 **2.1 CFD-CSD solver**

#### 128 **2.1.1 Flow solver**

129 The simulations of the flow around the wind turbine are carried out through Large-Eddy Simulations  
130 (LESs) of the incompressible, filtered, 3D Navier-Stokes equations, employing the in-house UTD-WF  
131 solver introduced by Santoni et al. (2015). The UTD-WF framework has been progressively devel-  
132 oped by Santoni et al. (2017, 2020) and further extended by Della Posta et al. (2022, 2023), where the  
133 aeroelastic solver and the Leishman–Beddoes dynamic stall model were implemented. The solver has  
134 been validated in its non-aeroelastic version by Santoni et al. (2017) against wind-tunnel data repro-  
135 ducing the NTNU “Blind Test” and comparing simulations to Krogstad et al. (2015) measurements,  
136 also considering the impact of tower and nacelle. Whereas, the recently developed version of the code  
137 including the two-way FSI coupling Della Posta et al. (2023) has been validated through comparison  
138 against reference datasets, including HAWC2-based results reported by Heinz (2013). The IEA 15MW  
139 wind turbine configuration considered here has been cross-validated with many other aeroelastic nu-  
140 merical codes in the International Energy Agency (IEA) Wind TCP Task 47 (Cacciola et al., 2025),  
141 also considering turbulent inflow conditions (Schepers et al., 2025). Notice that prior validations by  
142 Della Posta et al. (2022) of the CFD-CSD solver were made on a laminar uniform and a turbulent  
143 sheared inflows for a 5 MW NREL turbine, whereas our study extends the validated setting to the  
144 IEA-15 MW case for a sheared laminar inflow configuration. However, as discussed in the framework  
145 of the IEA Wind TCP Task 47 Schepers et al. (2025), turbulent fluctuations appear to have a much  
146 stronger impact than shear on load response of aero-elastic numerical codes. Moreover, high-fidelity  
147 codes appear rather consistent in predicting loads, while engineering models tend to overpredict fa-  
148 tigue loads, particularly for large rotors (Cacciola et al., 2025).

149 The code implements a second-order accurate centered finite difference scheme for the spatial dis-

150 cretization on a staggered Cartesian grid. A hybrid low-storage third-order-accurate Runge–Kutta  
 151 (RK) scheme is used for time integration of the non-linear terms (Orlandi, 2012), while the linear  
 152 terms are treated implicitly using a Crank-Nicolson scheme. The filtered governing equations are:

$$\frac{\partial u_i}{\partial t} + \frac{\partial u_i u_j}{\partial x_j} = -\frac{\partial p}{\partial x_i} + \frac{1}{Re} \frac{\partial^2 u_i}{\partial x_j \partial x_j} - \frac{\partial \tau_{ij}}{\partial x_j} + \tilde{f}_i, \quad (1)$$

$$\frac{\partial u_i}{\partial x_i} = 0, \quad (2)$$

153 where  $i, j \in \{1, 2, 3\}$  represent, in a Cartesian reference frame, the components along the streamwise  
 154 (x), wall-normal (y), and spanwise (z) directions, respectively. The Reynolds number  $Re = U_\infty D / \nu$   
 155 is defined by the undisturbed inlet velocity  $U_\infty$ , the turbine diameter  $D$ , and the kinematic viscosity of  
 156 the fluid  $\nu$ . These quantities are used as reference values to make the equations non-dimensional. To  
 157 solve the filtered equations, a Subgrid-Scale (SGS) stress model is needed. The latter describes the  
 158 interaction between the large resolved and the sub-grid unresolved scales, as described by Pino Martín  
 159 et al. (2000) and Santoni et al. (2017). Here, we employ the Smagorinsky model with constant  
 160  $C_s = 0.09$  as discussed by Martinez-Tossas et al. (2018).

161  
 162 The effect of the blades on the flow is modeled by the Actuator Line Model (ALM) (Sorensen  
 163 and Shen, 2002), by adding a forcing term to the Navier-Stokes equations, representing the force per  
 164 unit volume exerted by the rotor on the fluid. By approximating the rotor blades as straight lines  
 165 discretized into segments, it is possible to estimate the lift and drag forces per unit length on a 2D  
 166 plane as follows:

$$F_l = \frac{1}{2} \rho u_{rel}^2 C_l(\alpha) c F, \quad F_d = \frac{1}{2} \rho u_{rel}^2 C_d(\alpha) c F, \quad (3)$$

167 where  $\rho$  is the air density,  $c$  is the local chord,  $u_{rel}$  is the relative incoming velocity,  $\alpha$  is the angle  
 168 of attack, and  $F$  represents the tip loss correction factor, which employs the tip-loss model proposed  
 169 by Shen et al. (2005). The coefficients  $c_1$  and  $c_2$  of this model have been set in the following way:  $c_1$   
 170 has been set to the value reported in the Shen et al. (2005) paper ( $c_1 = 0.125$ ), whereas,  $c_2$  has been  
 171 chosen after a calibration with respect to the forces close to the tip reported by OpenFAST for the  
 172 same turbine and flow case, leading to the choice of  $c_2 = 32$ . The forces are then projected on the  
 173 flow employing a 2D Gaussian kernel, which spreads the lift and drag force vector,  $\mathbf{f}^{aero}$ , in cylinders  
 174 surrounding the actuator line,

$$\tilde{\mathbf{f}} = -\mathbf{f}^{aero} \frac{1}{\epsilon^2 \pi} \exp\left[-\left(\frac{r_\eta}{\epsilon}\right)^2\right], \quad (4)$$

175 where  $r_\eta$  is the radial distance of a generic point of the cylinder from the actuator line and  $\epsilon$  is the  
 176 spreading parameter, where  $\epsilon/\Delta \geq 2$ , with  $\Delta = \sqrt{\Delta x^2 + \Delta y^2 + \Delta z^2}$ , following Troldborg (2009). The  
 177 tower and nacelle are modeled using the Immersed Boundary Method (IBM) following the approach  
 178 described by Orlandi and Leonardi (2006).

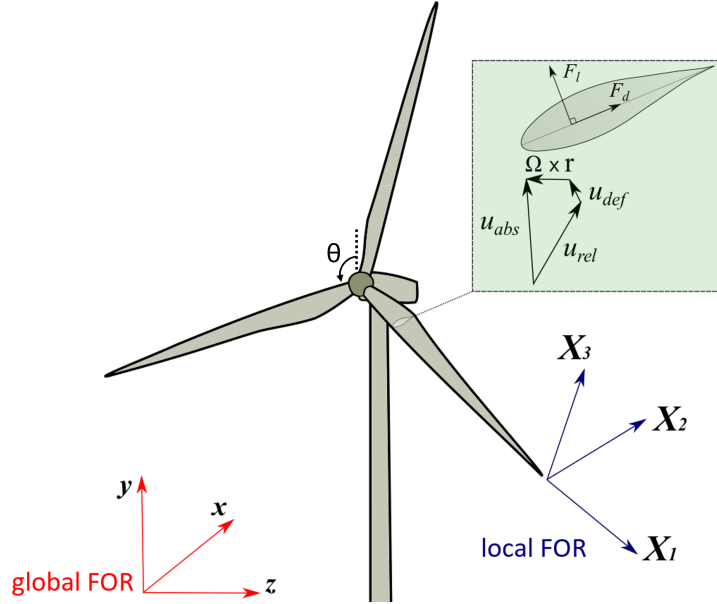


Figure 1: Sketch of the frames of reference used for the CFD and for the CSD simulations.

### 179 2.1.2 Structural solver

180 From an aerodynamic standpoint, the rotor blades represent the most flexible components within a  
 181 wind turbine. Several studies demonstrated that their modal properties have a significant impact on  
 182 the dynamics of the entire structure (Damgaard et al., 2013; Dong et al., 2018). Moreover, an analysis  
 183 of the isolated blades is also sufficient to accurately estimate the aeroelastic properties of the entire  
 184 structure, including the flutter speed (Abdel Hafeez and El-Badawy, 2018). Additionally, the tower  
 185 and shaft exhibit minimal deflection due to their stiffness. In light of the above considerations, the  
 186 aeroelastic model is constructed to encompass solely the structure of the blades.

187 The structural model used in the present study was extensively described by Della Posta et al. (2022,  
 188 2023) and will only be briefly outlined here. Under the assumption of small deformations with respect  
 189 to a relative frame of reference (FOR), the blades are assumed to be rotating beams rigidly clamped  
 190 at the hub (cantilever beams). Moreover, it is assumed that the blade deformation does not modify  
 191 the rotor inertia. With these hypothesis, a linear structural dynamic equation is obtained, taking into  
 192 account the Coriolis, centrifugal and Euler effects, that will be given in the following. Let us denote  
 193 by  $X_1$  the direction of the pitching axis. This coincides with the neutral axis of the blade, defined  
 194 as passing through the quarter of the chord. The direction of the out-of-plane flapwise motion is  
 195 indicated by  $X_2$  and is oriented in the positive streamwise direction. The in-plane edgewise direction  
 196 of  $X_3$  is defined such that the FOR is oriented as a right-handed coordinate system (Figure 1).

197 Under the assumption of linearity, the elastic generalised displacement  $\mathbf{d} = (d_i, \theta_i)$ , which includes  
 198 translational  $d_i$  and rotational  $\theta_i$  (with  $i = \{1, 2, 3\}$ ) degrees of freedom (DoFs), is decomposed along  
 199 the coordinate  $X_1$  on the neutral axis as:

$$\mathbf{d}(X_1, t) = \sum_{m=1}^M q_m(t) \boldsymbol{\psi}^m(X_1), \quad (5)$$

where  $\boldsymbol{\psi}^m(X_1)$  is the  $m$ -th elastic mode shape from the modal analysis of the structure,  $q_m$  is the corresponding modal coordinate, and  $M$  is the number of modes used. The effect of the generic motion of the FOR on the relative structural dynamics (one-way inertial coupling, since we assumed that the blade deformation does not modify the rotor inertia) is included in a modal basis by means of the methodology introduced in Reschke (2005) and further developed for the case of wind energy in Della Posta et al. (2022). Through this method, which exploits the decomposition of the acceleration in a moving FOR in the virtual work principle, we obtained a system of elastic equations with additional stiffening, damping, and loading terms depending on the angular velocity and acceleration of the rotating FOR, as:

$$\mathbf{M}\ddot{\mathbf{q}} + [\mathbf{D} + \mathbf{D}^{Co}(\boldsymbol{\Omega})]\dot{\mathbf{q}} + [\mathbf{K} + \mathbf{K}^c(\boldsymbol{\Omega}) + \mathbf{K}^{Eu}(\dot{\boldsymbol{\Omega}})]\mathbf{q} = \mathbf{e} + \mathbf{e}^c(\boldsymbol{\Omega}) + \mathbf{e}^{Eu}(\dot{\boldsymbol{\Omega}}), \quad (6)$$

where  $\mathbf{M}$ ,  $\mathbf{D}$  and  $\mathbf{K}$  denote the modal structural mass, damping, and stiffness matrices, respectively, and  $\mathbf{e}$  are the external loads expressed in modal basis, including the gravity force acting on the local centre of mass and the ALM aerodynamic forces acting on the local quarter of chord. The remaining terms are inherently related to the various contributions to the acceleration in a moving FOR. Terms with the superscript  $Co$ ,  $c$  and  $Eu$  are related to the Coriolis, centrifugal, and Euler accelerations, respectively. Given the assumption of linearity, we apply all the forces to the reference undeformed configuration. The discrete evaluation of the additional inertial terms in Equation (6) is expressed as a function only of the information known from the structural finite-element method (FEM) model and from the corresponding mode shapes, according to Saltari et al. (2017). For the modal analysis, performed on the undeformed nonrotating blade, we use a finite element model of the blade based on complete beam elements with 6 DoFs, with Euler-Bernoulli behavior for bending in directions  $X_2$  and  $X_3$ , and linear shape functions for axial and torsional deformations. We assume a lumped-mass representation, and we take into account the local offset of the centers of mass with respect to  $X_1$ . Finally, the structural matrices are assembled considering the local twist. The generalized- $\alpha$  method (Chung and Hulbert, 1993) is employed to advance the structural dynamic equation in time, which is unconditionally stable for linear problems, and second-order accurate. Details about the modal analysis are provided in Appendix A.

### 2.1.3 Fluid-structure interaction model

The two-way coupling aeroelastic model employs the ALM sectional approach, whereby the angle of attack (AoA) and relative velocity are locally modified following the instantaneous blade motion provided by the structural dynamics. In particular, the distribution of the AoA along each blade is evaluated as a function of the velocity of the fluid, the angular velocity of the rotor, and the instantaneous elastic state of the blade (which is projected back to the physical space from the modal one once the displacement is determined). The latter is generally constructed from the deformation velocity  $\mathbf{u}_{def} = \dot{\mathbf{d}}_{tr}$ , considering the time derivative of the translational degrees of freedom only, and the local vector of the deformation angles  $\boldsymbol{\theta}$  (torsion, and in-/out-of-plane angular deformations)

235 derived from the structural solver, which is forced by the updated aerodynamic loads. The algorithm  
 236 restricts inter-field communications solely at the beginning of each RK substep, thereby ensuring  
 237 optimal computational efficiency. The impact of the torsional dynamics was deemed to be limited  
 238 in light of the results obtained in previous studies on the effect of torsion for smaller wind turbines  
 239 (Chen, 2017). In order to investigate this issue for the large rotor 15MW wind turbine, in this study we  
 240 compare two different CSD models. In particular, we consider as a baseline a two-way coupling that  
 241 includes the effect of blade deformation velocity as a sole variable (CFD-CSD/OV, for Only Velocity),  
 242 and a more complete model including the torsional deformation in the coupling (CFD-CSD/T, for  
 243 Torsional). In general, the relative velocity for a rotating blade can be defined with the following  
 244 expression:

$$\mathbf{u}_{rel} = \mathbf{u}_{abs} - \boldsymbol{\Omega} \times \mathbf{r}_{OP} - \mathbf{u}_{def}, \quad (7)$$

245 where  $\mathbf{u}_{abs}$  is the filtered velocity from the fluid solver at the actuator line,  $\mathbf{r}_{OP}$  is the general radial  
 246 vector pointing to the considered section,  $\boldsymbol{\Omega}$  is the rotor rotational speed, and  $\mathbf{u}_{def}$  is the deformation  
 247 velocity of the structure at the same position. As a result, the AoA used to determine the air load  
 248 coefficients is defined as follows:

$$\alpha = \text{atan} \left( \frac{\mathbf{u}_{rel} \cdot \mathbf{E}_2}{-\mathbf{u}_{rel} \cdot \mathbf{E}_3} \right) - \phi - \theta_{tors} = \text{atan} \left[ \frac{(\mathbf{u}_{abs} - \mathbf{u}_{def}) \cdot \mathbf{E}_2}{\Omega r - (\mathbf{u}_{abs} - \mathbf{u}_{def}) \cdot \mathbf{E}_3} \right] - \phi - \theta_{tors}, \quad (8)$$

249 where  $\phi$  is the local twist angle of the blade,  $\theta_{tors}$  is the local torsional deformation,  $\mathbf{E}_i$  are the  
 250 unit vectors of the relative FOR rotating with the structure, and hence,  $v_2 = \mathbf{u}_{def} \cdot \mathbf{E}_2$  is the flapwise  
 251 deformation velocity component, and  $v_3 = \mathbf{u}_{def} \cdot \mathbf{E}_3$  is the edgewise deformation velocity component.  
 252 The simplified coupling procedure benefits from the sectional one-dimensional formulation of the  
 253 ALM, which avoids the complex treatment of the fluid-solid interface with the associated kinematic  
 254 and traction conditions.

### 255 3 Flow and structural setup

256 In this work, we consider a stand-alone IEA 15-MW wind turbine (Gaertner et al., 2020b) in its  
 257 monopile configuration. This wind turbine has a rotor diameter  $D = 240$  m with three blades of  
 258 length  $L = 117$  m. Table 1 provides the main features of the turbine.

259 The computational domain has dimensions  $12.5 \times 5 \times 3$  diameter units, as shown in Figure 2. The  
 260 distance of the turbine from the inlet of the computational domain (equal to 4D) has been determined  
 261 on the base of the reference data available in the literature, which vary in the range 2D-5D. Smaller  
 262 distances from the inlet (2D) have been employed for experimental set-up (Bartl and Satran, 2017;  
 263 Krogstad et al., 2015), whereas larger distances (in the range 2.7D-5D) are typical of numerical  
 264 simulations (Porte-Agel and Wu, 2011; Ciri et al., 2017; Allah and Sha ei Mayam, 2017; Stevens et al.,  
 265 2018). Moreover, we have verified numerically that pressure fluctuations do not generate spurious  
 266 reflections at the inlet section in our simulations. The spanwise length of the computational domain  
 267 (equal to 3D) is the same employed in previous numerical simulations (Ciri et al., 2017; Allah and  
 268 Sha ei Mayam, 2017). We have verified that, using periodic boundary conditions, the blockage effect

269 on the single turbine is negligible. Moreover, following the convergence study reported in the Appendix  
 270 A, the computational box has been discretized by a staggered grid composed of  $2049 \times 513 \times 513$  points  
 271 in the streamwise, wall-normal, and spanwise directions, respectively. The orthogonal grid is equally  
 272 spaced in the streamwise and spanwise directions and is stretched vertically, with a gradually wider  
 273 spacing starting from the region above the rotor. The grid spacing described leads to an actuator line  
 274 discretized by 86 points per blade. The time resolution of the LES computation is tied to the spatial  
 275 resolution, as defined by the stability requirements of the numerical scheme adopted. Simulations are  
 276 carried out at a constant Courant–Friedrichs–Lewy (CFL) number (Courant et al., 1967)  $CFL = 0.65$ ,  
 277 which ensures an average time step  $\overline{\Delta t} = 0.024s$ . The turbine location is 4 diameter units from the  
 278 inlet and centered in the spanwise direction. Furthermore, we impose a sheared laminar inflow velocity  
 279 profile, defined by a power law with the exponent  $\alpha = 0.05$ , and a convective outlet boundary condition,  
 280 i.e.,  $\frac{\partial u_i}{\partial t} + C \frac{\partial u_i}{\partial x} = 0$ , with the constant  $C$  set to the average value of the outflow velocity. Notice that,  
 281 as the shear is imposed at the inlet, the flow profile is allowed to change when reaching the turbine.  
 282 However, since the power law profile complies with the no-slip conditions at the wall and with the slip  
 283 conditions at the free-stream, the modifications are mostly due to the slight three-dimensionalization  
 284 of the flow due to the presence of the turbine. In the spanwise direction, periodic boundary conditions  
 285 are imposed. Moreover, slip and no-slip conditions are enforced at the top and bottom boundaries,  
 286 respectively. The turbine is subjected to a flow with a Reynolds number  $Re \approx 10^8$  and operates at  
 287 its nominal tip speed ratio (TSR) of  $\lambda = 9$ . The streamwise undisturbed velocity at the hub height  
 288 is constant and equal to  $U_\infty = 10 \text{ m/s}$ . The simulations were conducted for a time interval of 300 s  
 289 over the initial transient, which corresponds to 35 revolutions of the rotor.

290 To identify the optimal configuration for the structural model, we conducted a preliminary sensitivity  
 291 analysis and then validated the structural eigenfrequencies of the undeformed nonrotating blade with  
 292 the results found in the literature. A more detailed insight into this analysis is presented in Appendix  
 293 B, where the structural properties of this turbine are shown. Finally, a number of modes  $M_s = 15$   
 294 and a structural discretization of the blades given by  $N = 80$  equally-spaced nodes were chosen.  
 295 For comparison purposes, wind turbine simulations have been also conducted using the OpenFAST  
 296 solver *Release v3.2.0* (July 29, 2022). The aerodynamic computations are performed by the *AeroDyn*  
 297 (Jonkman et al., 2015) module which is based on the BEM theory. A Prandtl loss model is applied  
 298 to account for the tip and root effects. The structural module dedicated to the computation of the  
 299 blade deformation is contained in the *BeamDyn* module, which relies on the geometrically exact beam  
 300 theory and may resolve geometric non-linearities and large deflections (Wang et al., 2016b). In order to  
 301 compare the CFD-CSD results with a modal structural analysis, we also performed simulations using  
 302 the standalone *ElastoDyn* module, based on a modal approach and suitable for blade deformation  
 303 dominated by bending. It is worth to notice that the latter does not take into account the torsional  
 304 degree of freedom, so it is to be directly compared to the CFD-CSD/OV model, which also does not  
 305 account for the coupling between the torsional deformation and the angle of attack. As reported in the  
 306 original manual of *AeroDyn* (Moriarty and Hansen, 2005), OpenFAST couples the fluid and structural  
 307 solvers in a similar way to our CFD-CSD solvers. In particular, the local angle of attack is determined  
 308 taking into account the local deformation velocities.

Parameter	Units	Value
Power rating	$MW$	15
Rotor diameter ( $D$ )	$m$	240
Rotor orientation	–	Upwind
Number of blades	–	3
Blade length ( $L$ )	$m$	117
Hub height	$m$	150
Hub radius ( $R_{hub}$ )	$m$	3.97
Rated wind speed	$m/s$	10.59
Design tip speed ratio	–	9
Maximum rotor speed	$RPM$	7.56

Table 1: IEA 15-MW (Gaertner et al., 2020b) wind turbine main features

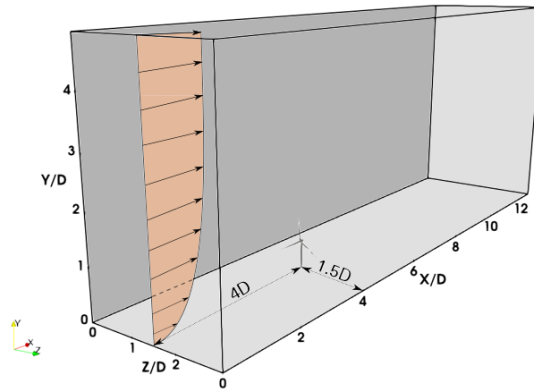


Figure 2: Sketch of the computational domain where the incoming sheared flow and the position of the turbine are highlighted.

## 309 4 Results and discussion

310 This section presents the results of two set of simulations: one modeling a rotor-only configuration  
311 (RO) and the other including the tower and nacelle (TN). Furthermore, both configurations are sub-  
312 jected to comparative analysis using the OpenFAST submodules. Firstly, the near-wake aerodynamic  
313 characteristics and the wake recovery of both configurations determined by the CFD-CSD solvers are  
314 discussed. Then, the aerodynamic loads on the blades are analyzed and the outcomes from both solvers  
315 are compared. Finally, the overall turbine performance and the effects on the blade deformation are  
316 assessed.

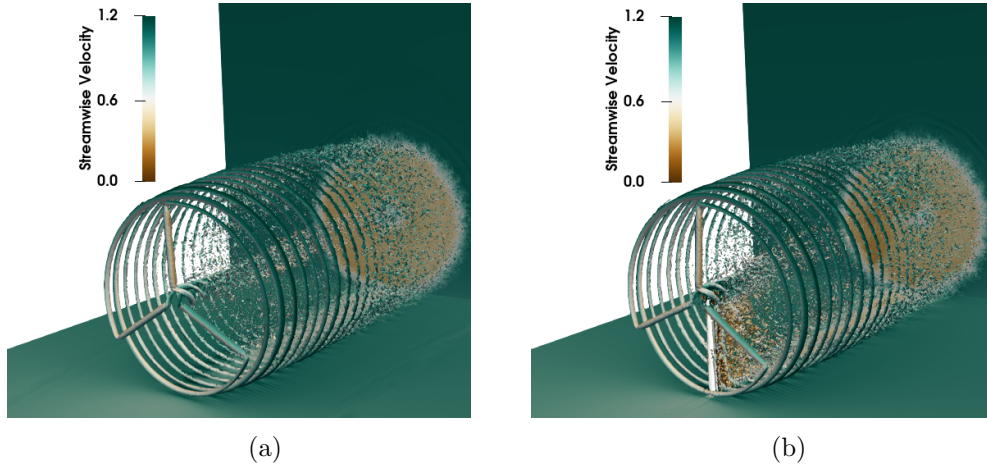


Figure 3: Q-criterion contour of the instantaneous velocity field colored by the streamwise velocity for the rotor-only case (RO) (a) and tower and nacelle (TN) (b).

#### 4.1 Flow analysis

As a first step, we analyze the flow field variables, as obtained using the CFD-CSD/T solver. Figure 3 illustrates the main coherent flow structures in the field by means of an instantaneous isosurface of the Q-criterion colored by the streamwise velocity for both cases. It is evident that the presence of the tower affects the vorticity intensity distribution along the vertical direction. In particular, the occurrence of a low-velocity recirculation zone at the tower height for the TN case can be identified, which is a result of the tower shadowing (see Figure 3b). Moreover, the TN case demonstrates a more rapid dissolution of the endogenous coherent hub vortex structures if compared to the RO case (see Figure 3a). On the other hand, the tip vortex structures appear to be minimally influenced by the presence of the tower. Figure 4 shows the rotor-averaged streamwise velocity along the flow direction, time-averaged over 30 revolutions of the rotor. Contrary to what Santoni et al. (2017) observed in their work on the 5MW reference turbine invested by a uniform inflow (see the red lines in figure 4), the rotor-averaged velocity for the TN configuration in the wake remains slightly lower than for the RO case, indicating that wake recovery is slightly hindered by the presence of the tower. In order to establish whether this opposed behaviour with respect to the results of Santoni et al. (2017) may be due to the different inflow or rather on geometrical features characterizing the two different turbines, we have carried out a computation equivalent to that of the 15MW turbine (sheared inflow,  $U_\infty = 10m/s$  etc.), but for an NREL 5MW wind turbine. As shown by the blue lines in figure 4, as in Santoni et al. (2017), the rotor-averaged velocity for the TN configuration in the wake remains rather higher than for the OR case, indicating that wake recovery for an NREL 5MW turbine is indeed advanced by the presence of the tower, no matter the inflow considered. Whereas, the larger gap between the RO/TN cases observed in the near wake by Santoni et al. (2017) is not observed in this case, and it is thus probably due to the uniform inflow. Comparing these results to the present data, we can conclude that the slower wake recovery observed for the 15MW turbine in the presence of the tower appears to be due to geometrical features of the turbine itself, not to the inflow characteristics. One possible

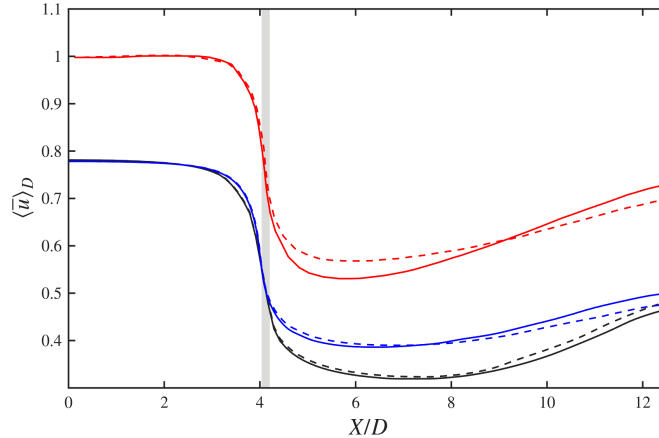


Figure 4: Rotor-averaged velocity along the streamwise direction normalized by the undisturbed velocity at the rotor height, namely,  $U_\infty = 10 \text{ m/s}$ , for the present data (black curves), a computation of the NREL 5MW turbine for the same configuration and inflow (blue curves) and the work of Santoni et al. (2017) (red curves). The grey region represents the area covered by the rotor. (RO ---, TN —).

342 explanation for this behaviour could be differences in the tower-to-rotor aspect ratio. In particular,  
 343 for the NREL 5-MW turbine, the ratio between the tower diameter and the rotor diameter is about  
 344 equal to 0.047, whereas, for the 15MW turbine, it is only about 0.027 (the tower diameters being 6m  
 345 and 6.5m, respectively). Thus, the thinner shape (in terms of diameter units) of the tower, as well as  
 346 the lower value of the incoming velocity at the tower height due to the presence of shear at the inflow,  
 347 result into a decreased mixing behind the turbine which leads to a slower wake recovery.  
 348 Although not favoring wake recovery, the tower still plays a strong role in the wake dynamics, as it  
 349 can be visualized in Figure 5, showing slices of instantaneous streamwise velocity at different tower  
 350 heights corresponding to 80% of the blade (top) and to the tip of the blade (bottom), when the blade  
 351 is in front of the tower, i.e.  $\theta = 180^\circ$  (left), and when it is far from it (right). In particular, it can  
 352 be observed that the turbulent mixing right downstream of the tower is already very high in the near  
 353 wake compared to that close to the tip of the blades. Due to the mutual effect of the asymmetry  
 354 induced by the rotation of the blades and of the wake meandering, it can be seen that, inside the  
 355 rotor disk, the tower wake bends in the spanwise direction (Figure 5, top frames), whereas it is rather  
 356 spanwise independent at a height corresponding to the blade's tip (bottom frames). Moreover, one  
 357 can see that the passage of the blade in front of the tower (left frames) induces a strong perturbation  
 358 in the flow field already upstream of the tower. In the following section, the effect of this perturbation  
 359 on the phase oscillations of several relevant quantities (aerodynamic forces, power coefficient, etc.)  
 360 will be discussed.

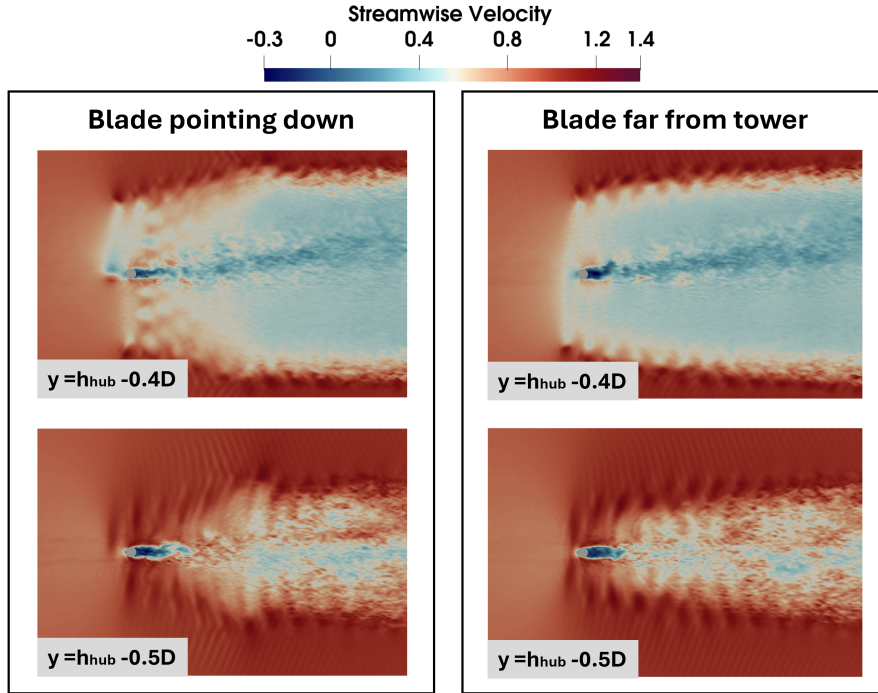


Figure 5: Instantaneous streamwise velocity on horizontal slices at different tower heights corresponding to 80% of the blade (top slices), and the tip of the blade (bottom slices). In the left configuration, the blade is in front of the tower ( $\theta = 180^\circ$ ), while on the right the blade is far from the tower.  $h_{hub} = 0.625D$  is the hub height.

## 4.2 Aerodynamic loads on the blade

The analysis of the aerodynamic loads on the blade has been conducted using the present CFD-CSD models and the engineering software OpenFAST. The same laminar sheared inflow is imposed for both solvers using a power law with the same exponent and reference streamwise velocity at the hub height. We have chosen not to impose a turbulent inflow to avoid differences in the definition of the turbulent inflow itself which might have hindered the comparison between the results of the two codes. It is important to note that the four solvers employed differ in both their aerodynamic and structural modeling approaches. Moreover, the flow that impacts the turbine is not exactly the same for the CFD and OpenFAST solvers, since in the former case it is imposed at several diameters upstream the rotor plane. As a result, it is not always possible to unambiguously determine whether the observed discrepancies in the results originate from the fluid-dynamic models or from the structural formulations.

Figure 6 depicts the following time-averaged aerodynamic quantities along the span of the blade: the local angle of attack  $\alpha$  (Figure 6a); the aerodynamic pitching moment per unit length  $M_{aero}$  (Figure 6b); the flapwise and edgewise components (normal and tangential to the rotor disk, respectively) of the aerodynamic force per unit length  $F_2$  (Figure 6c) and  $F_3$  (Figure 6d), respectively. In particular,

377 Figure 6a shows that a good agreement of the local incidence angle computed by both CFD-CSD mod-  
 378 els (solid lines) with that computed by *ElastoDyn* (circles) and *BeamDyn* (squares) is obtained from  
 379 the 20% up to the 80% of the blade length. Indeed, the differences in the root area could be ascribed to  
 380 the presence of the hub which is modeled differently by the solvers. The discrepancy of the incidence  
 381 angle observed towards the tip subsequently affects the aerodynamic loads. The  $F_2$  force in Figure 6c  
 382 shows a very good fit of the CFD-CSD/T results with that of the nonlinear solver *BeamDyn*, despite  
 383 the linearity of our in-house CSD model. The strong discrepancies with respect to the values obtained  
 384 by *ElastoDyn* can be ascribed to the absence of the torsional deformation in the latter solver. Indeed,  
 385 the CFD-CSD/OV solver, which neglects the torsional feedback in the coupling, shows very similar  
 386 results to the *ElastoDyn* solver. A similar effect can be observed by examining the reduction in  $F_3$   
 387 towards the tip of the blade (see Figure 6d). The distribution of the aerodynamic pitching moment  
 presents instead a maximum gap of about 8% from the BEM-based solvers.

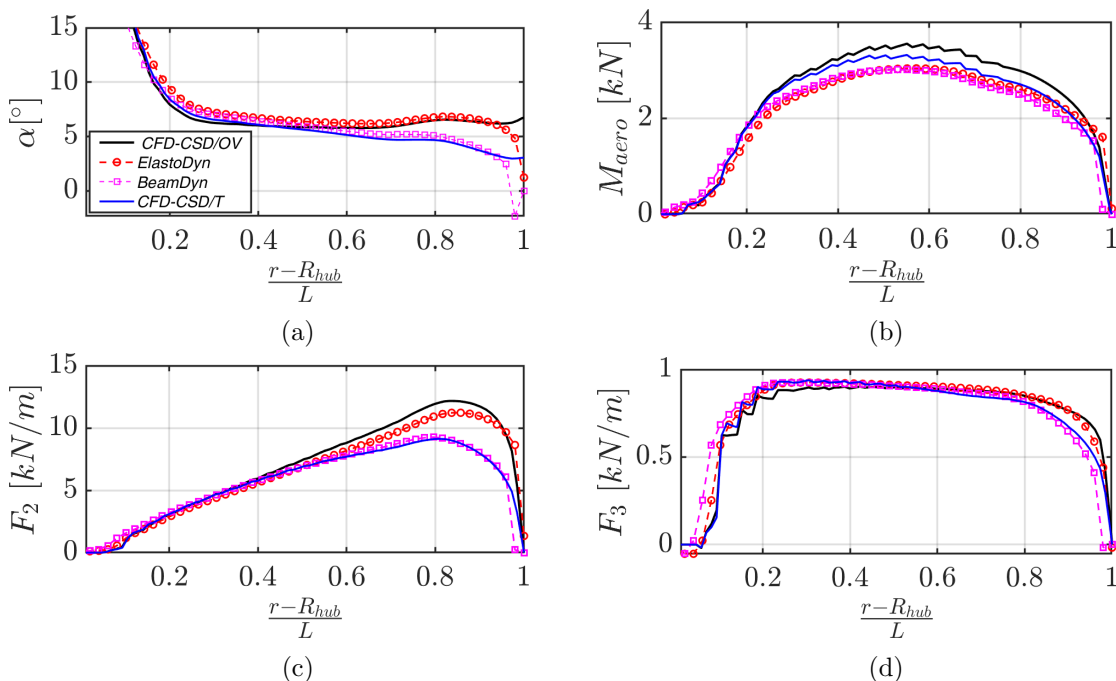


Figure 6: Average aerodynamic quantities along the blade compared between CFD-CSD/OV, CFD-CSD/T, *ElastoDyn*, and *BeamDyn*. (a) Incidence angle, (b) Aerodynamic pitch moment, (c) flapwise aerodynamic force, (d) edgewise aerodynamic force.

388 As demonstrated by Hansen (2015), the outer third of the blade span is the most critical region in  
 389 terms of deflections and deformations due to the combination of higher aerodynamic loads and reduced  
 390 structural stiffness. Therefore, a phase average of the aerodynamic quantities at the 80% of the blade  
 391 has been performed. Figure 7 reports the evolution of the incidence angle and of the aerodynamic  
 392 force components at  $\frac{r-R_{hub}}{L} = 0.8$  (being  $R_{hub}$  the hub radius and  $L$  the blade length) versus the blade  
 393 rotation angle  $\theta$ . The dynamical behavior of the aerodynamic quantities in the presence (solid lines) or  
 394

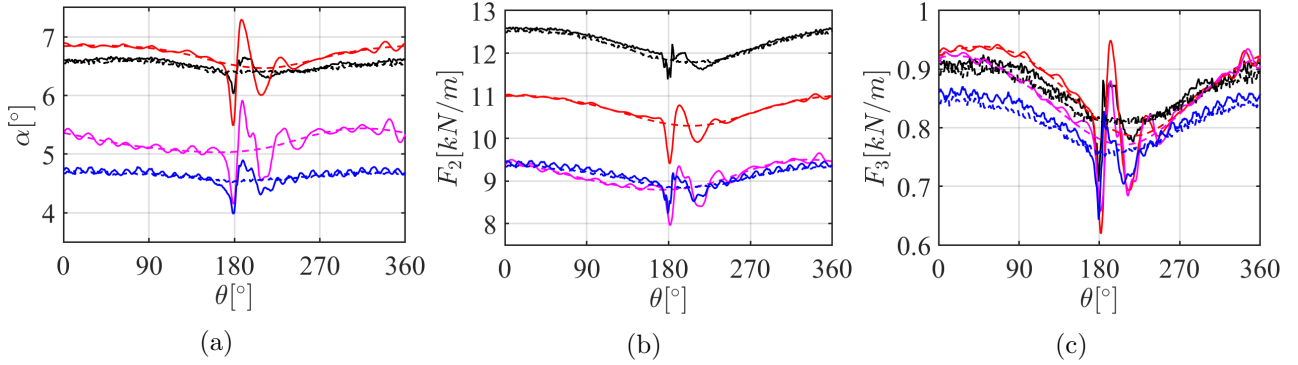


Figure 7: Phase-averaged values of: (a) the local incidence angle, (b) flapwise aerodynamic force, and (c) edgewise aerodynamic force at the 80% of the blade. CFD-CSD/OV: TN —, RO ----. CFD-CSD/T: TN —, RO ----. *ElastoDyn*: TN —, RO ----. *BeamDyn*: TN —, RO ----.

395 in the absence (dashed lines) of the tower underlines that the passage of the blade in front of the tower  
396 represents the main source of instability for the flow conditions considered. Indeed, the blade-tower  
397 interaction leads to oscillations of the aerodynamic forces and of the incidence angle around  $\theta = 180^\circ$ ,  
398 i.e., when the blade is pointing down. However, unlike the case of the NREL 5-MW turbine (Bernardi  
399 et al., 2023), this effect appears to be stronger for the BEM computations than for the CFD-CSD  
400 solver. Concerning this point, we should recall that, as pointed out by Bernardi et al. (2023), the  
401 complex flow dynamics resulting from the interaction between the blade and the tower, shown in  
402 Figure 5, may not be well described by OpenFAST, which uses a simple potential flow model. It can  
403 be observed that, between the rotor and the tower, a region with low streamwise velocity is observed.  
404 We can expect that the passage of the blade in front of the tower thus induces an alteration of the  
405 aerodynamic forces on the blade due to the decrease/increase of the streamwise velocity. This issue  
406 will be further discussed in the following, where a possible reason for the different behavior observed  
407 for the IEA 15-MW with respect to the NREL 5-MW turbine will be discussed.

408 Apart from the effect of the tower, one can observe a rather good match between the CFD-CSD/OV  
409 and *ElastoDyn* solvers for both the incidence angle and the edgewise component of the aerodynamic  
410 force, while the flapwise component presents some discrepancies. On the other hand, when torsional  
411 feedback is included, CFD-CSD/T and *BeamDyn* solvers, regardless of the linearity or non-linearity of  
412 the models, agree rather well on the aerodynamic forces, especially on the flapwise one, which shows  
413 an error  $\approx 2\%$ , while the edgewise force reaches a  $\approx 5\%$  error at azimuthal angles close to  $\theta = 0$ .  
414 Whereas, the error between the two solvers on the angle of attack reaches  $8\%$ .

415 To better investigate the local response of the different models during the blade revolution, we con-  
416 ducted a comparative analysis of the aerodynamic loads, employing phase-averaged quantities over  
417 the span. Figure 8 illustrates the percentage difference of the phase-averaged aerodynamic quantities  
418 on the rotor plane of the *ElastoDyn* (*BeamDyn*) solver with respect to the CFD-CSD/OV, defined as  
419  $|\langle \Delta\alpha/\alpha^{CFD-CSD/OV} \rangle\%|$ , and of the CFD-CSD/T model, defined as  $|\langle \Delta\alpha/\alpha^{CFD-CSD/T} \rangle\%|$ , respec-  
420 tively. In particular, in comparison to *ElastoDyn*, a higher value of the absolute incidence angle in the  
421 range of  $|\langle \Delta\alpha/\alpha^{CFD-CSD/OV} \rangle\%| = [17\%, 25\%]$  is found in the zone after the tower (see Figure 8a).

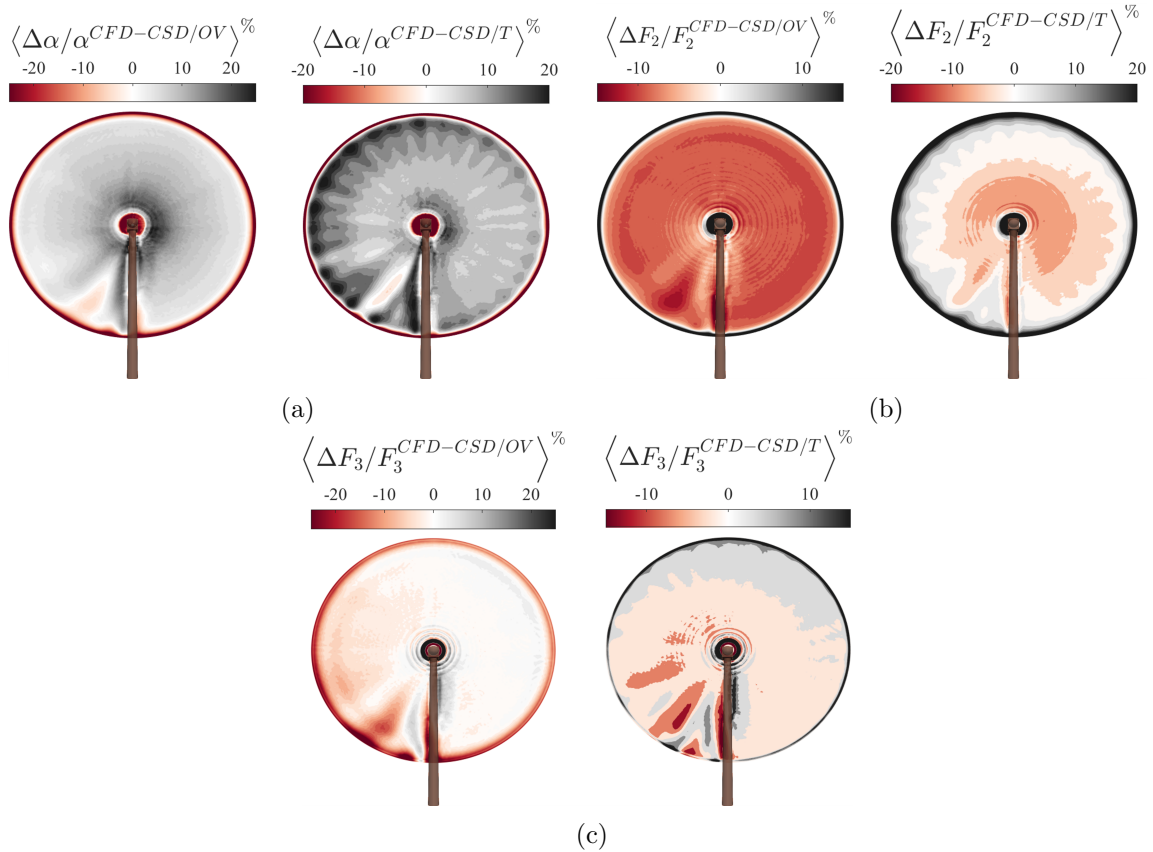


Figure 8: Phase-averaged contour plots of the percentual differences of the aerodynamic quantities between CFD-CSD/OV versus *ElastoDyn* (left), and CFD-CSD/T versus *BeamDyn* (right), respectively. (a) Incidence angle, (b) flapwise aerodynamic force, (c) edgewise aerodynamic force.

422 The difference with respect to the results obtained by *BeamDyn* tends to be higher moving from the  
 423 root to the tip with a discontinuity in the tower area, spanning the range  $|\langle \Delta\alpha/\alpha^{CFD} \rangle\%| = [35\%, 60\%]$   
 424 in the last 20% of the blade span. Furthermore, the angle of attack distribution affects the components  
 425 of the aerodynamic force. In fact, the distribution of the flapwise component of the force follows the  
 426 same pattern of the incidence angle (see Figure 8b). On the other hand, for the edgewise component  
 427 the major discrepancies are concentrated in the final radial sections of the blade toward the tip (see  
 428 Figure 8c). In general, we can conclude that the most significant discrepancies are observed in the  
 429 tip region where the three-dimensional effects are more relevant and where the complexity of the fluid  
 430 flow is strongly affected by the presence of the tower.

431 Notably, similar discrepancies are observed when comparing the CFD-CSD/T solver with the *Beam-*  
 432 *Dyn* solvers. However, in this case some high-frequency oscillations are observed for the three aerody-  
 433 namic quantities. In fact, the same oscillations are observed in the phase averaged quantities at 80%  
 434 of the blade shown in Figure 8, for both the CFD-CSD/T solver and *BeamDyn*. The frequency of  
 435 these oscillations computed by the two solvers appear very close and comparable with the natural fre-

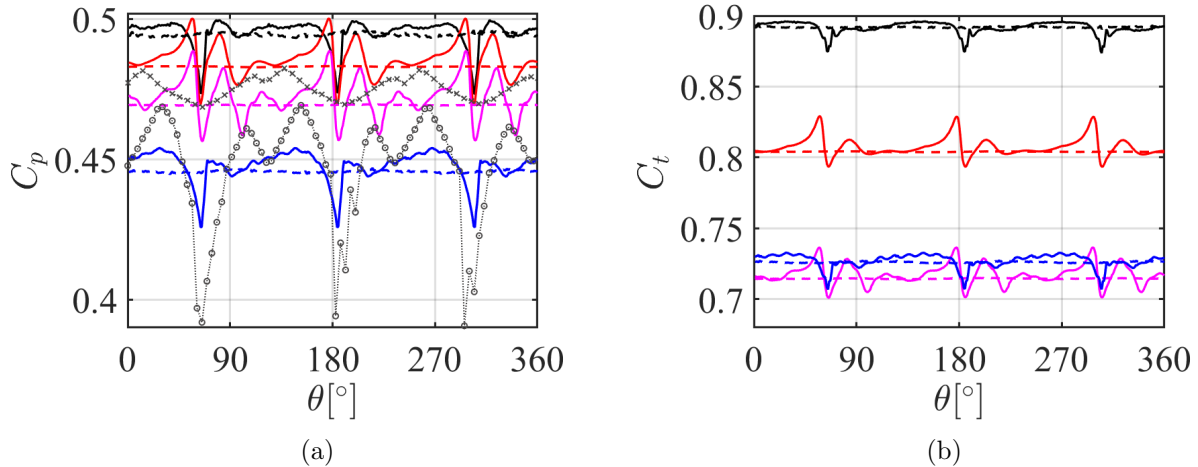


Figure 9: Phase-averaged power (a) and thrust (b) coefficients. CFD-CSD/OV: TN —, RO ----. CFD-CSD/T: TN —, RO ----. *ElastoDyn*: TN —, RO ----. *BeamDyn*: TN —, RO ----. From figure 3 of Bernardi et al. (2023):  $\circ$  *LES + CSD flexible*,  $\times$  *OpenFAST-AeroDyn*.

436 quency of the first torsional mode, although some differences can be observed in the amplitudes of the  
 437 signals, especially concerning the angle of attack ( $\approx 8\%$  of error) and the edgewise aerodynamic force  
 438 at azimuthal angles close to zero ( $\approx 6\%$  of error). Again, this observation indicates that including the  
 439 torsional degree of freedom in the structural solver is crucial for describing accurately the amplitude  
 440 and dynamical behaviour of the aerodynamic quantities.

### 441 4.3 Power and thrust coefficients

442 The aerodynamic loads previously presented are also useful to evaluate the power and thrust coeffi-  
 443 cients, defined as follows:

$$C_p = \frac{P_d}{\frac{1}{2}\rho AU_\infty^3}, \quad C_t = \frac{T_{aero}}{\frac{1}{2}\rho AU_\infty^2}, \quad (9)$$

444 where  $A = \pi D^2/4$  represents the rotor area,  $P_d$  is the aerodynamic power transferred to the rotor and  
 445  $T_{aero}$  is the overall aerodynamic thrust on the turbine.

446 Starting from the time history of  $C_p$  and  $C_t$ , we computed their phase-averaged evolution as reported  
 447 in Figure 9. The periodic passage of the blades in front of the tower for the TN configuration produces  
 448 a drop of the curves of about 10%. Eventually, the performance is restored to the value obtained in the  
 449 RO case following the elastic dynamical behavior of the structure. The results reflect the dependency of  
 450 the power and thrust coefficients on the edgewise aerodynamic force  $F_3$  and the flapwise aerodynamic  
 451 force  $F_2$  at the 80% of the blade, respectively (see Figures 7c and 7b), which are strongly influenced  
 452 by the presence of the tower. Notice that, also here, we can observe that the drop in the  $C_p$  curve  
 453 appears to be rather similarly predicted by BEM and CFD, although the BEM prediction exhibits  
 454 notable oscillations before and after the drop, whereas these are not present in the CFD results. A  
 455 different behaviour was observed for the NREL 5-MW turbine (as in figure 3 of Bernardi et al. (2023),

456 included in Figure 9 of the present paper with symbols), where this performance drop is considerably  
 457 underestimated by the BEM computations. A possible factor that may contribute to this different  
 458 behaviour may reside in the different relative geometry of the two wind turbines. Indeed, the flow  
 459 induced by a thinner tower (in diameter units), as in the case of the 15-MW wind turbine, might be  
 460 better described by a potential flow solution compared to the one induced by a thicker tower, as in  
 461 the case of the 5-MW wind turbine, and may thus lead to the observed improved agreement between  
 462 BEM and CFD results. Moreover, the differences in the flow impinging on the blade might also have  
 463 an effect. In fact, in Bernardi et al. (2023) a uniform inflow was imposed. Whereas, in the present  
 464 case, due to the shear imposed at the inflow and the limited distance from the ground of the tip of the  
 465 blade (only  $\approx 0.125D$  for the 15MW turbine), the blade is invested by a flow having a much smaller  
 466 velocity compared to the given value of  $U_\infty$  at hub height, further confirming the increased suitability  
 467 of a potential flow solution upstream of the tower. Nevertheless, we should recall that this remains a  
 468 very strong approximation, as also demonstrated by the differences in the forces and angles that have  
 469 been observed in the previous section (see Figure 8, for instance).

470 It can be concluded that the performance loss induced by the passage in front of the tower is less  
 471 pronounced for the 15 MW NREL turbine in the present configuration ( $\approx 5\%$ ) compared to the 5  
 472 MW turbine in the configuration considered in Bernardi et al. (2023) ( $\approx 15\%$ , see figure 3 of this  
 473 reference), with both BEM theory and CFD yielding similar predictions in the case of the 15 MW  
 474 turbine. However, it is worth recalling again that Bernardi et al. (2023) considered a uniform inflow,  
 475 whereas here the inflow is sheared. This can be a possible reason for this different behaviour, since  
 476 the lower wind speed in the lower part of the rotor plane leads to a lower production in the bottom  
 477 half of the rotor plane, where the tower is located. This may cause a smaller performance drop due  
 478 to the tower relative to the total produced power. Therefore, the observed difference can be not only  
 479 due to the change in turbine size, but also due to the change in inflow conditions.

480 Moreover, the present results predict that, for very large rotors and a sheared inflow, the tower effect  
 481 on blade deformations is less pronounced than for smaller rotors, although it should yet be taken into  
 482 account for accurately describing the turbine’s performance oscillations as it still represents a major  
 483 source of unsteadiness.

484 The average value of the power coefficient is much larger when the torsional deformation is neglected.  
 485 This feature is observed by both CFD and BEM approaches. However, one can observe that *ElastoDyn*  
 486 underestimates the value of  $C_p$  with respect to the corresponding non-torsional CFD model, while the  
 487 opposite is observed when comparing *BeamDyn* with the torsional CFD solver. This is probably due  
 488 to the fact that *BeamDyn* predicts higher values of the aerodynamic edgewise forces with respect to  
 489 the CFD-CSD/T approach, which are linked to a smaller torsional deformation as will be shown in  
 490 figure 11f in the next section.

491 Figure 10 shows the premultiplied Power Spectral Density (PSD) of the power (Figure 10a) and thrust  
 492 (Figure 10b) coefficients evolution. The PSD is normalized by the variance of each coefficient  $\sigma^2$  and  
 493 plotted versus the frequency normalized by the rotational frequency of the rotor,  $f/f_{rot}$  where the  
 494 latter is denoted as  $1P = f_{rot} = 7.5RPM$  and its multiples will be denoted as  $2P, 3P$  etc. In both  
 495 cases, the CFD-CSD solvers seem to provide a richer representation of the aerodynamic coefficients,  
 496 capturing the full range of flow-structure interactions. Indeed, an examination of the low-frequency  
 497 behavior reveals that both quantities exhibit isolated low-frequency peaks when using the BEM-based

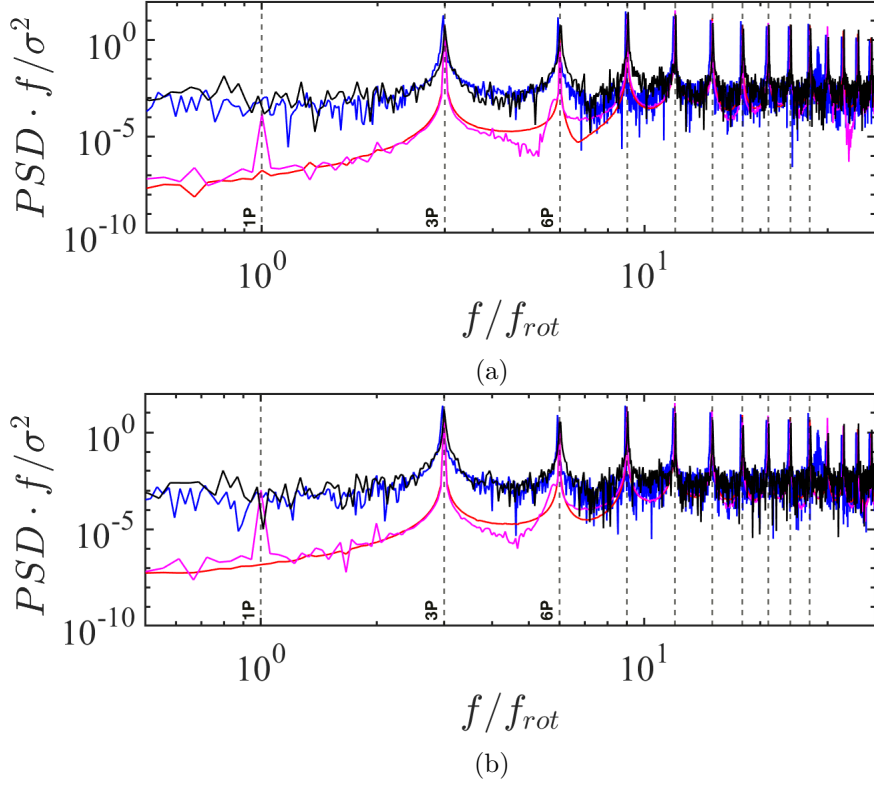


Figure 10: Power Spectral Density (PSD) of the power (a) and thrust (b) coefficients. The vertical dashed lines highlight the rotational frequency of the rotor  $1P = f_{rot} = 7.5RPM$  and the multiples of  $3P$ , respectively. CFD-CSD/OV —, CFD-CSD/T —, *ElastoDyn* —, *BeamDyn* —.

498 solvers, a phenomenon not observed with the CFD-CSD, where the low-frequency range is rather  
 499 broadband and does not present particular peaks. It is important to notice that the frequency 1P can  
 500 be directly linked to the frequency of the passage of the blade in front of the tower, but also to wind  
 501 shear loads on the blades. Concerning the first point, a potential flow solution as that used in the  
 502 BEM solver is keen to provide a simple, single-frequency response, whereas a complex, turbulent flow  
 503 is expected to result in a more broadband spectrum. Concerning the second point, we have to consider  
 504 that in LES, the power law profile is imposed at the inlet of the domain but it is free to evolve for  
 505 4 diameters before the wind turbine, altering in a non-trivial way the flow field and the consequent  
 506 frequency response of the blades. This outcome indicates that the BEM-based solvers tend to overcut  
 507 the power oscillations associated with low-frequencies that are not exactly equal to 1P or 2P. For all  
 508 solvers, however, the strongest PSD peaks are to be found at much larger frequencies (3P-6P-9P-12P),  
 509 as also observed by Pagamonci et al. (2023) by means of URANS aeroelastic simulations of the NREL  
 510 5-MW, the DTU 10-MW, and the IEA 15-MW turbines. One can also notice that the amplitude  
 511 associated with the 3P frequency appears to be consistently described by the two solvers, although  
 512 also in this range the BEM solver appears to overdamp the frequencies in between different peaks.  
 513 Moreover, a good agreement is evident between the two set of results concerning the value of the

514 frequencies and the level of the PSD for frequencies that are multiples of  $3P$ .

#### 515 4.4 Structural response

516 This section presents the analysis of the structural dynamics. Figure 11 reports the phase-averaged  
517 dynamic response of the free extremity of the blade (left column) and the time-averaged deforma-  
518 tion of the entire span (right column). Figure 11a shows how the out-of-plane deformation is mainly  
519 governed by the aerodynamic component of the force normal to the rotor plane and, hence, to the  
520 aerodynamic effects, heavily affected by the tower. In fact, it is visible how the tower placed at  
521  $\theta = 180^\circ$  produces a drop in the deformation, followed by an elastic dynamic response which restores  
522 the value far from the pointing-down position. The time-averaged maximum deformation predicted  
523 by the CFD-CSD/OV solver is 16% higher compared to the *ElastoDyn* module and 17% compared  
524 to *BeamDyn* (see Figure 11b). On the other hand, the same quantity predicted by the CFD-CSD/T  
525 solver is 17% lower compared to the *ElastoDyn* module and 13% compared to *BeamDyn* (see Figure  
526 11b). This is consistent with the fact that including the torsional degree of freedom reduces the loads  
527 (see figure 7b) and the resulting deformation. Although the trend of deformation with respect to the  
528 blade span appears similar to previous predictions based on URANS (see Pagamonci et al. (2023)),  
529 the out-of-plane deformation is rather larger, reaching 16 m at the blade’s tip. The amplitude of the  
530 deformation is however close to that obtained by Trigaux et al. (2024) using LES. Figure 11c depicts  
531 instead the in-plane deformation, which is mostly due to gravity. The results show that the shadowing  
532 effect of the tower does not influence this quantity, which is expected as the lag deformation is mainly  
533 driven by gravity. Furthermore, the discrepancies obtained between *ElastoDyn* and *BeamDyn* can be  
534 attributed to the lack of modes used by the former model to describe the translation in the edgewise  
535 direction (see Figure 11d). The discrepancy does not seem to be linked to the linearity of this model,  
536 as the result of the CFD-CSD/T solver, which is linear as well, is much closer to the *BeamDyn* results.  
537 Moreover, the results of the CFD-CSD/OV and the CFD-CSD/T models are very close each other.  
538 It can be noticed that the amplitude of the oscillation of the in-plane deflection is consistent with  
539 that reported by Trigaux et al. (2024) (see Figure 7b of their paper, reporting an oscillation between  
540  $\approx -2.3$  and  $\approx 0.2$  ), although the sign is opposite due to the different frame of reference used.  
541 A further significant insight into the deformation phenomenon is provided by the torsional DoF. Figure  
542 11e shows a comparison of the torsional angle at the tip with *BeamDyn*. Significant discrepancies can  
543 be observed between the LES and the BEM approaches, which cannot be reconducted to the different  
544 coupling procedures adopted by the models. On the one hand, *BeamDyn* and CFD-CSD/T both take  
545 into account the deformation angle in the coupling (Wang et al., 2016b), while in the CFD-CSD/OV  
546 solver the angle of attack depends only on the deformation velocity (see Equation 8). However, the  
547 gap between the BEM and the CFD-CSD/T curves is quite large, reaching approximatively 20%  
548 of the torsional deformation value. These differences likely arise from the combined effects of both  
549 aerodynamic and structural modeling approaches used in BEM and LES. Although in the present  
550 paper we have mostly focused on a comparison of the structural models, a thorough comparison of  
551 the aerodynamics modeling can be found in the report of IEA Task 47 Schepers et al. (2025), where  
552 results produced with the present code are included (see, for instance, figure 4.25 and following for  
553 non flexible blades). The discrepancy between the BEM and the CFD-CSD results is confirmed by  
554 the time-averaged torsional deformation along the span reported in Figure 11f where the maximum

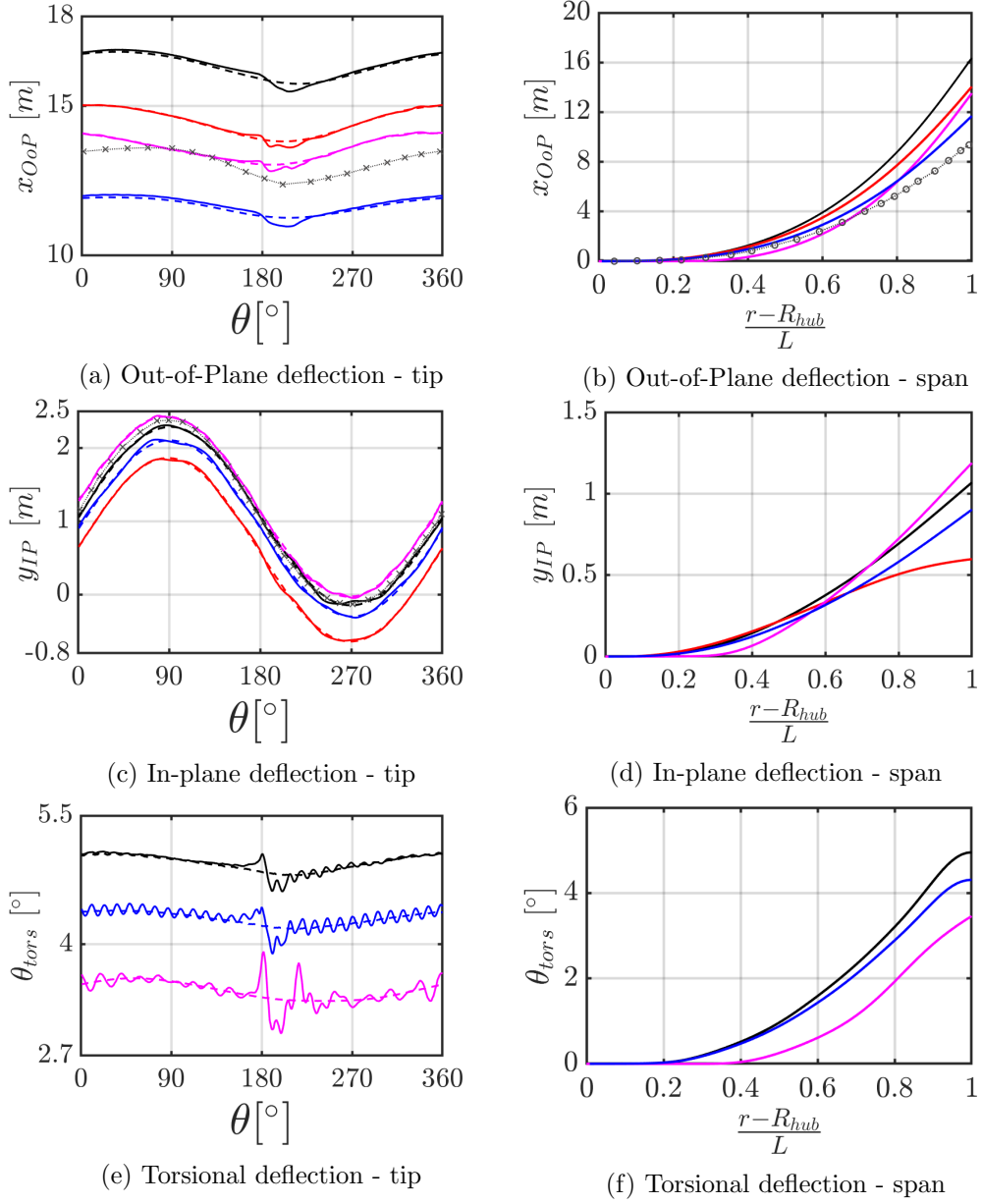
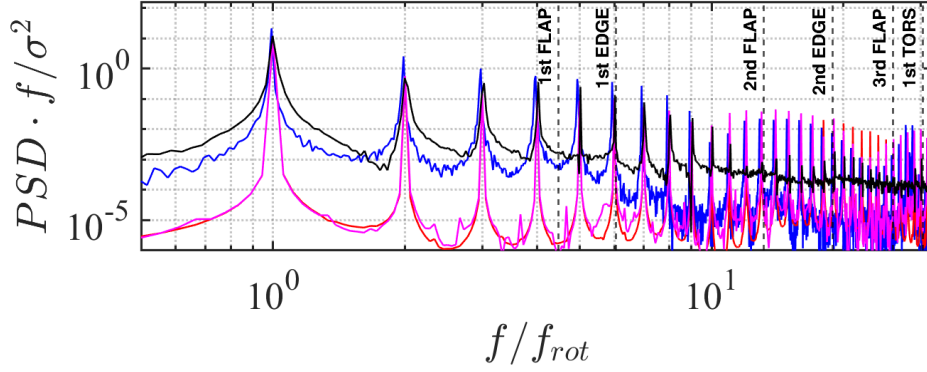
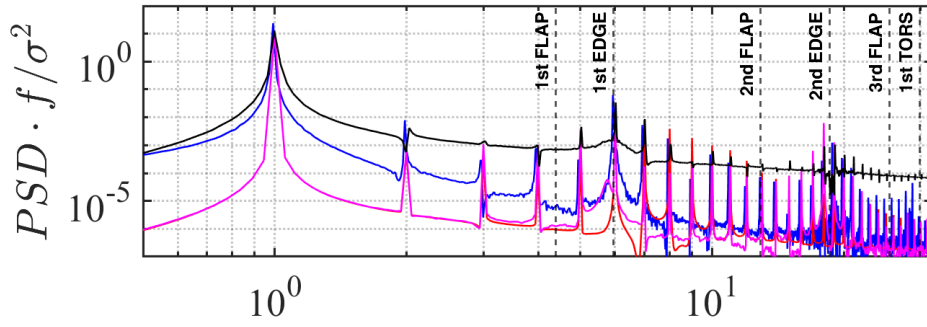


Figure 11: Phase-averaged deflections at the tip of the blade (left column) and time-averaged deflections along the blade span (right column). CFD-CSD/OV: TN —, RO ----. CFD-CSD/T: TN —, RO ----. *ElastoDyn*: TN —, RO ----. *BeamDyn*: TN —, RO ----. From figure 13 of Pagamonci et al. (2023)  $\circ$ , and figure 7a and 7b of Trigaux et al. (2024)  $\times$ .

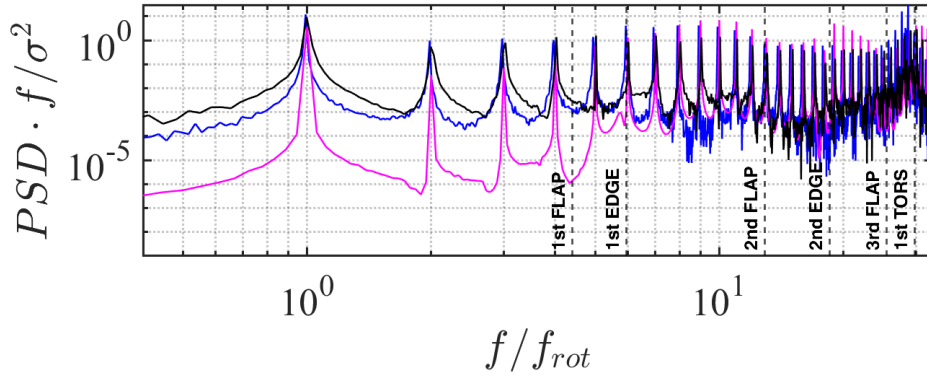
555 percentual gap of *BeamDyn* reaches 29% for the CSD-CFD/OV, and 24% for the CFD-CSD/T. It is  
 556 noteworthy that the lower torsional deformation resulting from *BeamDyn* leads to the higher aerody-



(a)  $X_{OoP}$



(b)  $y_{IP}$



(c)  $\theta_{tors}$

Figure 12: Power Spectral Density (PSD) of the out-of-plane (a), in-plane (b), and torsional (c) deformations of the blade. The vertical dashed lines represent the first 8 eigenfrequencies of the system. CFD-CSD/OV —, CFD-CSD/T —, *ElastoDyn* —, *BeamDyn* —.

557 namic loads observed in figure 7c.

558 Finally, figure 12 illustrates the Power Spectral Density (PSD) of the blade's tip deformation compo-  
 559 nents for the TN configuration (which is characterized by more complex fluid-structure interactions).  
 560 The premultiplied PSD values are normalized by the variance of the signal,  $\sigma^2$ , and plotted versus the

561 frequency normalized by the rotor frequency,  $f/f_{rot}$ . Spectral results have been corroborated through  
562 use of the Welch and Lomb-Scargle PSD estimation algorithms.  
563 Figure 12a shows the out-of-plane deformation, which we showed to be influenced mostly by the  
564 aerodynamic loading. The results indicate that, for all the numerical approaches used, the observed  
565 structural response does not exhibit a peak corresponding to the first flapwise natural frequency,  
566 suggesting that the intrinsic dynamics of the structure might play a less prominent role in the de-  
567 formation process. A similar behavior is found in the results of Trigaux et al. (2024) (see figure 6  
568 of the cited paper) for the same turbine and similar inflow conditions. Noticeably, all the numerical  
569 models recovered peaks at frequencies close to the (highly damped) second and third flapwise natural  
570 frequencies, but they appear to rather correspond to the 13<sup>th</sup> and 26<sup>th</sup> multiple of the rotational fre-  
571 quency (i.e., 13p and 26p). Both CFD-CSD solvers predict larger amplitude responses across a broad  
572 frequency range compared to OpenFAST, indicating a higher capability to capture complex flow inter-  
573 actions, including turbulence-induced vibrations. This effect is particularly pronounced at the lower  
574 frequencies, probably due to the large-scale three-dimensional structure of the flow impinging on the  
575 turbine, which is not captured by OpenFAST, also due to the fact that the impinging flow on the  
576 turbine is purely two-dimensional, while it is not for CFD. For this reason, these aspects seem to be  
577 under-represented in the *ElastoDyn* and *BeamDyn* solutions. Although the *ElastoDyn* curve aligns  
578 with both the CFD-CSD solvers at some key frequency peaks, it does not account for the fine-scale  
579 flow-structure interactions. On the other hand, the *BeamDyn* curve provides better agreement with  
580 the CFD-CSD solvers, especially at higher frequencies near the blade’s natural modes, suggesting that  
581 *BeamDyn* captures more of the structural dynamics, particularly the aeroelastic response, probably  
582 due to its nonlinearity or to the number of degrees of freedom considered. Figure 12b shows the  
583 in-plane deformation, which is primarily influenced by gravity, centrifugal, and Coriolis forces acting  
584 on the blade. The CFD-CSD solvers again demonstrate stronger low-frequency components.  
585 Figure 12c presents the torsional deformation for the CFD-CSD/T and *BeamDyn* solvers, excluding  
586 *ElastoDyn*, which neglects the torsional DoF in the model. Additionally, also this quantity demon-  
587 strates that the CFD-CSD curves predict higher amplitudes at low frequencies. However, a good  
588 agreement between the two solvers is evident at higher frequencies, especially in the range around the  
589 first torsional eigenfrequency.

## 590 5 Conclusions

591 This study investigated the aeroelastic response of the IEA 15-MW wind turbine by employing a  
592 high-fidelity Computational Fluid Dynamics (CFD) solver that couples Large-Eddy Simulation (LES)  
593 with a Computational Structural Dynamics (CSD) solver. Two different CSD solvers are considered:  
594 the CFD-CSD/OV solver, in which the only structural quantity contributing to the definition of the  
595 angle of attack is the deformation velocity, and the CFD-CSD/T solver, in which the instantaneous  
596 torsional deformation is also considered when defining the local effective incidence. The results of the  
597 two CFD-CSD solvers are compared with those of traditional engineering solvers such as *BeamDyn*  
598 and *ElastoDyn*, both relying on Blade Element Momentum (BEM) theory. Two case studies were  
599 examined: a rotor-only configuration (RO) and one that included the tower and nacelle (TN).

600 [The Power Spectral Density \(PSD\) of the power and thrust coefficients revealed that the CFD-CSD](#)

601 solver captures a broader range of flow-structure interactions, with a more broadband low-frequency  
602 response, compared to the BEM-based solvers. The isolated low-frequency peaks found in *BeamDyn*  
603 and *ElastoDyn* suggest that these solvers tend to over-simplify the aerodynamic fluctuations associ-  
604 ated with phenomena such as wind shear and tower shadowing. For the large IEA 15-MW turbine,  
605 the performance drop caused by tower passage is not very pronounced and the resulting oscillations  
606 predicted by the BEM approach appear to be larger than the CFD-CSD solver.

607 Concerning the forces on the blade and the incidence angle, one can observe a rather good match  
608 between the CFD-CSD/OV solver and *ElastoDyn*, as well as between the CFD-CSD/T model and  
609 the *BeamDyn* solver. This is likely due to the presence – or not – of the torsional feedback, while  
610 non-linearities of the structural solver appear to have only a limited impact on the observed quanti-  
611 ties. In agreement with previous studies, the results thus suggest that including the torsional degree  
612 of freedom in the structural solver is crucial for accurately describing the amplitude and dynamical  
613 behaviour of the aerodynamic quantities.

614 Moreover, it is observed that duly taking into account the torsional degree of freedom reduces the  
615 value of  $C_p$ . This feature is consistently observed by both CFD and BEM approaches. However, one  
616 can observe that *BeamDyn* predicts lower values of the torsional deformation and thus higher values  
617 of the aerodynamic edgewise forces with respect to the CFD-CSD/T approach, leading to a larger  $C_p$   
618 value than that predicted by LES. All in all, it can be concluded that for the considered setup, the  
619 CFD-CSD solvers tend to exhibit larger amplitudes at lower frequencies with respect to BEM ones.

620 The structural response of the wind turbine blade has been assessed by comparing the out-of-plane, in-  
621 plane, and torsional deformations obtained from the CFD-CSD solvers, *ElastoDyn*-based, and *Beam-*  
622 *Dyn*-based OpenFAST solver. In-plane deformation, influenced significantly by centrifugal forces,  
623 appears to be better captured by the CFD-CSD solvers, especially in the low-frequency range. Con-  
624 cerning the out-of plane deflection, large discrepancies are seen between the two CFD-CSD solvers, as  
625 well as between both BEM modules and the LES.

626 Our results underscore the importance of incorporating torsional deformation effects in the definition  
627 of the angle of attack and using high-fidelity aeroelastic models to ensure accurate predictions of wind  
628 turbine blade performance with a richer fluid dynamics. Whereas, the linearity of the structural model  
629 does not appear to have a strong effect on the aerodynamical quantities, deformations and loads. In  
630 general, the comparison of the results of the CFD-CSD solver with those of the engineering solver  
631 shows differences especially in the region behind the tower. The observed differences likely stem from  
632 the combined effects of differences in aerodynamic and structural fidelity, and cannot be uniquely  
633 attributed to one component alone.

634 Future work will explore the effect of turbulent fluctuations at the inlet to better investigate the impact  
635 of the atmospheric boundary layer on the aerodynamic forces, loads and deformations of the present  
636 turbine.

## 637 **A Appendix A. Grid convergence study for LES**

638 A grid convergence study was conducted to evaluate the sensitivity of the LES results to spatial and  
639 temporal resolution. Two further simulations were carried out using grids of different densities: a  
640 coarser mesh and a finer mesh, having approximately 40% less and more grid points than the former

641 in each spatial direction, respectively. This allowed for a more detailed resolution of flow structures  
642 and aerodynamic quantities. Moreover, both simulations use the same  $CFL = 0.65$  as the present grid.  
643 The average time step obtained and the other key parameters of different LES runs are summarized  
644 in Table A1.

Parameter	Coarse Grid	Present Grid	Fine Grid
Total number of cells	$1.31 \times 10^8$	$5.37 \times 10^8$	$1.36 \times 10^9$
Largest cell diagonal (m)	8.1	5.0	3.5
Smallest cell diagonal (m)	3.9	2.5	1.7
Actuator points per blade	54	86	128
Average time step (s)	0.043	0.024	0.012
Total number of threads	320	512	768

Table A1: Comparison of the main parameters for different meshes.

645 The comparison in figure A1 shows that the results obtained using the coarse and fine grids are  
646 extremely close to each other along the entire blade span. In particular, the curves of the angle of  
647 attack are almost indistinguishable for the coarser and the reference grid, even in the outer portion of  
648 the blade, where stronger differences were expected due to tip effects and local three-dimensionality.  
649 Slightly larger differences are recovered between the reference and the finer grid, but only at low  
650 radius. In particular, for these two grids the maximum deviation of the incidence angle  $\alpha$  between  
651 the two simulations at 80% of the span reaches a value of  $\Delta\alpha_{max} \approx 0.2^\circ$ , corresponding to a relative  
652 difference of 1.6%. Whereas, the maximum deviation between the reference and the coarser grids at  
653 80% of the blade span is  $\Delta\alpha_{max} \approx 0.1^\circ$ , corresponding to a relative difference of 1.5%. Similarly,  
654 the aerodynamic forces component distributions exhibit negligible variation between the reference and  
655 finer resolutions, and less than 1% relative variations between the reference and the coarser grids,  
656 confirming the overall consistency of the LES solution examined in the Sec. 4 with respect to mesh  
657 refinement.

658 These results indicate that the coarse grid already accurately captures the main aerodynamic features,  
659 making the use of a finer mesh unjustified given its higher computational cost and minimal accuracy  
660 gain.

## 661 B Appendix B. Validation of the structural model

662 The structural model for the IEA 15MW wind turbine has been cross-validated with many other  
663 aeroelastic numerical codes within the framework of the International Energy Agency (IEA) Wind TCP  
664 Task 47 TURBINIA (Schepers et al., 2025). In this IEA Task, a consortium of research institutions  
665 and industrial partners benchmarked their own aeroelastic codes on the IEA 15 MW wind turbine  
666 (Cacciola et al., 2025). Since we cannot report in this paper data from all these partners, we provide  
667 here a preliminary study was conducted to validate the structural model prior to coupling it with the  
668 CFD solver. Figure B1 shows the distributions of the structural and constructive properties along the  
669 blade, which were utilized as input for the modal CSD analysis. A convergence study to determine the

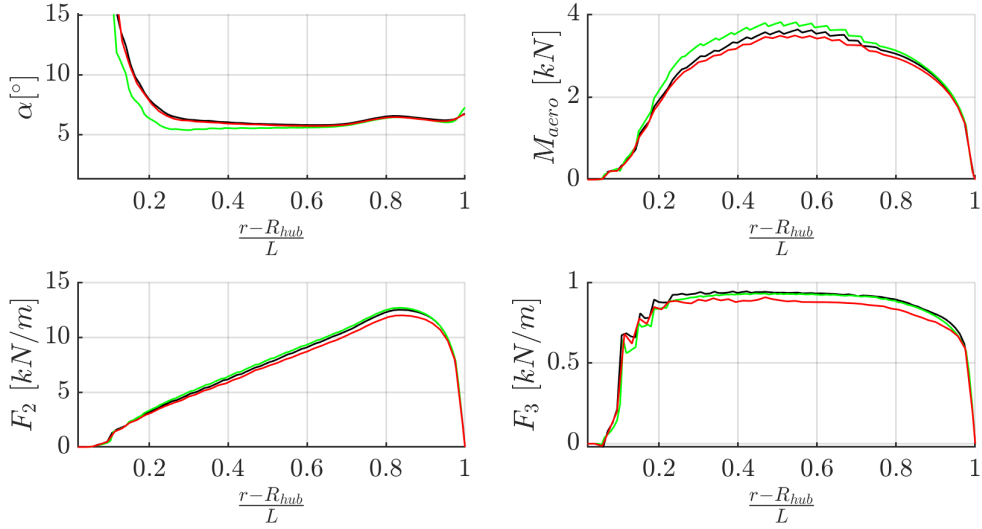


Figure A1: Average aerodynamic quantities along the blade obtained from the coarse grid (red line), the present grid (black line) and the finer grid (green line). Incidence angle (top left), Aerodynamic pitch moment (top right), flapwise aerodynamic force (bottom left), edgewise aerodynamic force (bottom right).

670 proper number of elements,  $N_e$ , (not reported here for brevity) was conducted, leading to the choice  
671  $N_e = 80$ . Furthermore, the results of the present structural analysis were compared with those of  
672 five models including: the prismatic Timoshenko model without torsion (H2-PTNT); the Timoshenko  
673 model with a fully populated stiffness matrix (H2-FPM) from the study of Rinker et al. (2020); the  
674 3D Finite Element Analysis (3D FEA) selected from Zhang et al. (2023); the ElastoDyn model; the  
675 BeamDyn model. Figure B2 shows the first 8 eigenfrequencies using the present method compared  
676 with the results of these models. The computed values of the modal frequencies appear to be consistent  
677 with the other results, although some discrepancies in the higher-order modes are observed. Moreover,  
678 an analysis of the most important modes was conducted: Table B1 provides the classification of the  
679 first 8 modes, whereas, Figures B3, B4, and B5 show the modal displacements for the first spanwise,  
680 edgewise, and torsional modes, respectively.

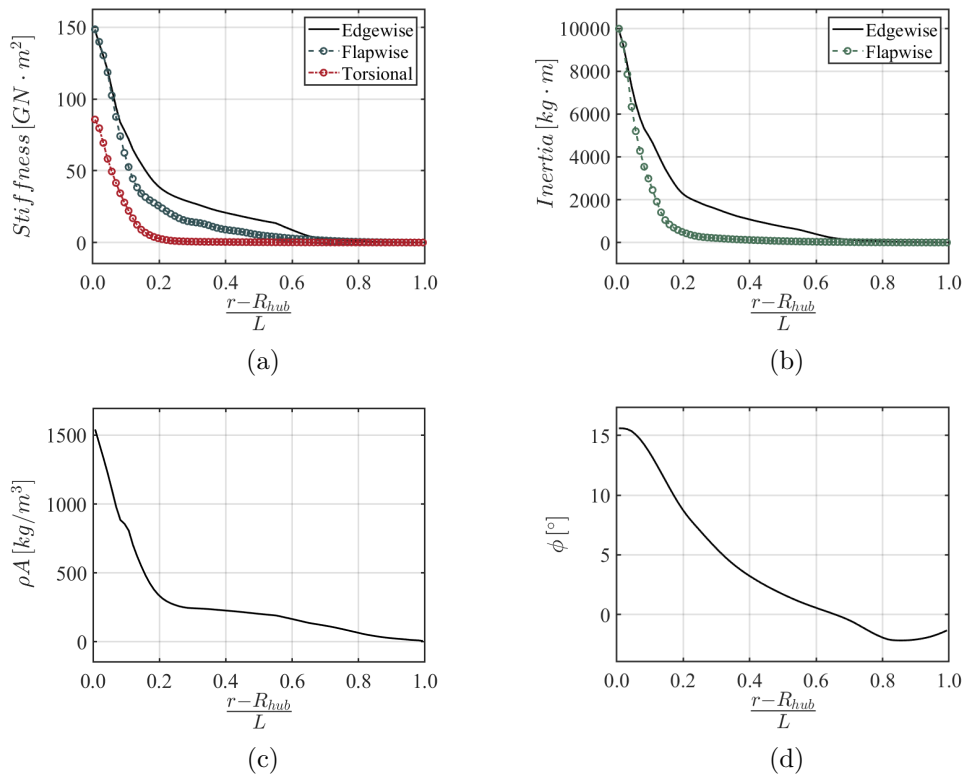


Figure B1: Structural properties of the blade along the span: (a) stiffness, (b) inertia, (c) density, (d) local twist angle.

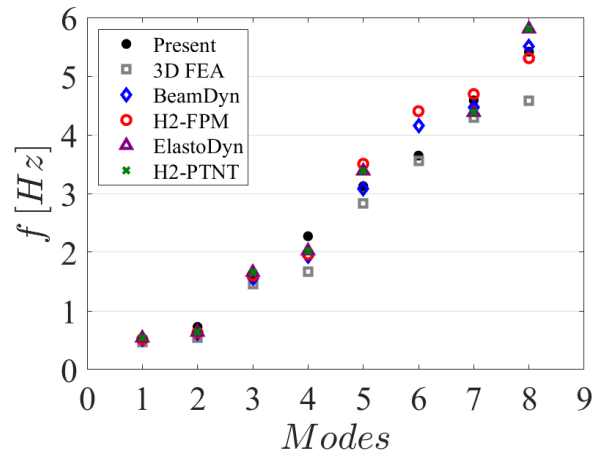


Figure B2: A comparison of the eigenfrequencies computed by different structural models.

#	$f_n[Hz]$	Mode
1	0.5369	1st flapwise
2	0.7267	1st edgewise
3	1.577	2nd flapwise
4	2.267	2nd edgewise
5	3.113	3rd flapwise
6	3.642	1st torsional
7	4.571	3rd edgewise
8	5.385	4th flapwise

Table B1: Classification of the first 8 structural modes.

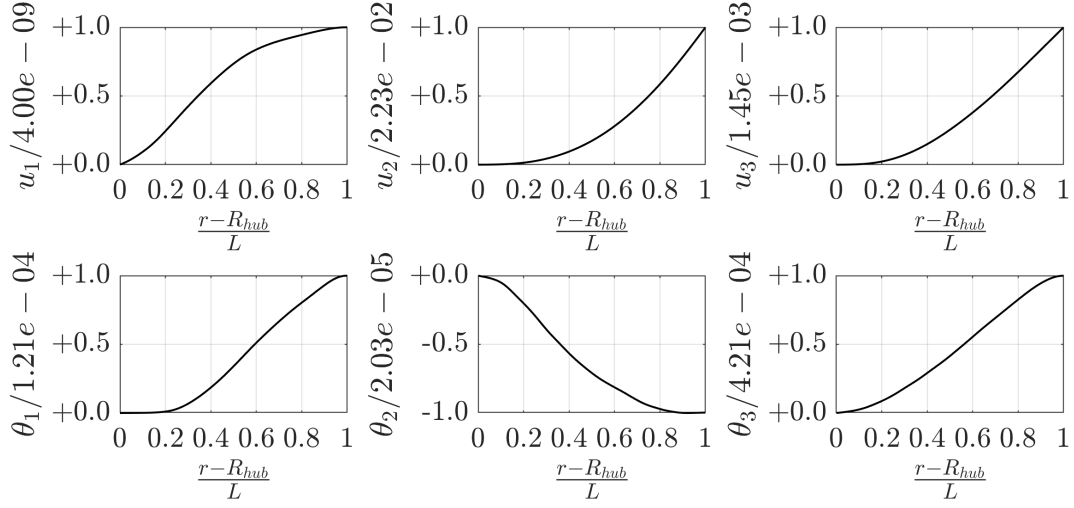


Figure B3: Mode 1 shape for all the DoFs.

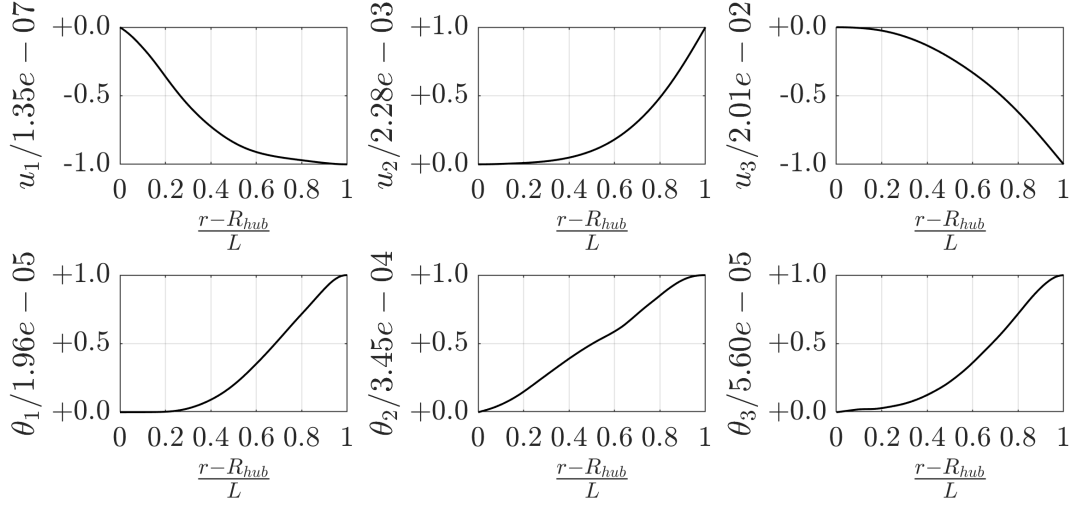


Figure B4: Mode 2 shape for all the DoFs.

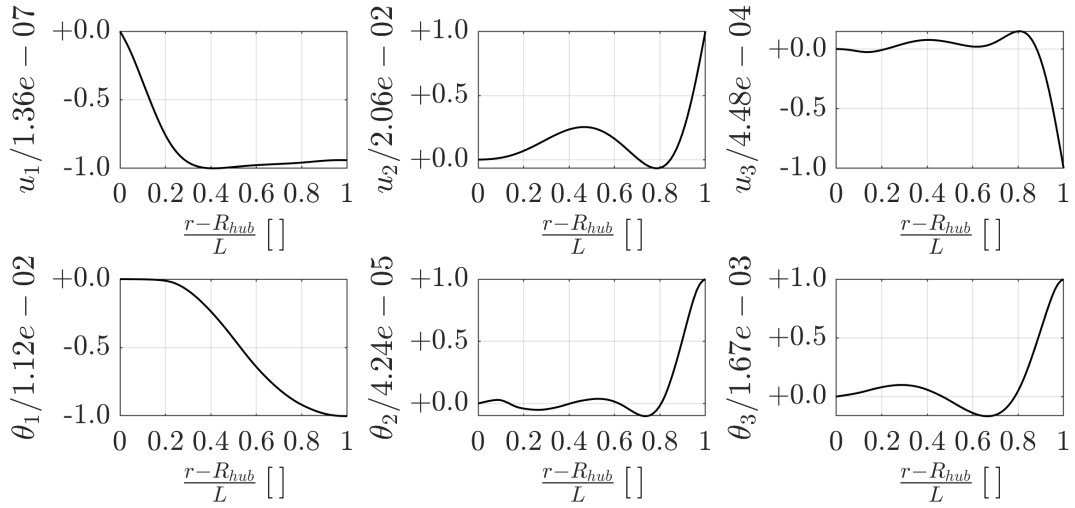


Figure B5: Mode 6 shape for all the DoFs.

681 *Author contribution.* CB: Investigation, Writing - Original draft, Formal analysis, Methodology, Soft-  
682 ware, Validation. SC: Conceptualization, Investigation, Writing - Review & Editing, Supervision. FM:  
683 Methodology, Software, Validation. GDP: Formal analysis, Writing - Review & Editing, Methodol-  
684 ogy, software. SL: Conceptualization, Software, Supervision. PDP: Conceptualization, Investigation,  
685 Writing - Review & Editing, Supervision.

686 *Competing interests.* The authors declare that they have no competing interests.

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