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Coupled aero-servo-elastic method for floating offshore wind turbine wake analysis

Lin Yang^a, Kangping Liao^a, Qingwei Ma^b, Abbas Khayyer^c, Hanbing Sun^a

^a College of Shipbuilding Engineering, Harbin Engineering University, Harbin, 150000, China

^b School of Mathematics, Computer Science and Engineering, City, University of London, UK

^c Department of Civil and Earth Resources Engineering, Kyoto University, Japan

Abstract

A new coupled aero-servo-elastic method is developed to model unsteady loads and wakes of Floating Offshore Wind Turbines (FOWTs) with elastic blades, in which the actuator curve embedding (ACE) method is for the first time coupled with a nonlinear finite element rotating beam model, rotor speed and collective blade pitch control strategies in a lab code—HEU-FOWT. The method is capable for efficient aero-servo-elastic simulation of FOWT(s) including wakes on relatively coarse Cartesian grids without requiring empirical tip loss corrections. Tests of an isolated blade and a rotating uniform cantilever beam indicate the static deformations, natural frequencies, modal shapes, and centrifugal stiffening effects are well predicted. Validations of a bottom-fixed NREL 5MW wind turbine show good accuracy for various aero-servo-elastic results in a wide range of wind speed and the rotor thrust and blade tip out-of-plane (Oop) deformation are found significantly underpredicted by 10.5% and 15.0% at rated condition if aerodynamic center offset effects neglected. Aero-servo-elastic wake analyses of a NREL 5MW wind turbine under specified surge motion show the two control strategies (constant power and constant torque mode) significantly reduced the overall far wake deficit by 38.1% and 35.6%, while blade elasticity only reduced the same quantity by 2.7%.

Keywords:

Actuator curve embedding, Coupled aero-servo-elastic, Floating offshore wind turbine, HEU-FOWT

* Corresponding Author

E-mail address: liaokangping@hrbeu.edu.cn. (Kangping Liao)

29 **1.Introduction**

30 Wind energy is one of the most fast-growing renewable and sustainable energy in the past two
31 decades with the total installed capacity from 24GW in 2001 to 743GW in 2020[1]. To further
32 driven down the levelized cost of electricity, the wind turbine rotor is continuously growing in size
33 with diameter increasing from 126m for the well-known NREL-5MW reference wind turbine
34 (RWT) [2]to 240m for the recent IEA-15MW RWT[3]. For these large-scale wind turbines, the
35 blades are usually manufactured using light-weight and flexible composite materials which can be
36 more susceptible to the influence of aeroelasticity. Proper control strategies are usually required in
37 operation for safety and best performances. For example, generator torque control strategy is
38 usually required to regulate rotor speed to maximize power extraction at below-rated conditions
39 and maintain constant rotor speed at above-rated conditions. Blade pitch control strategy
40 (collective blade pitch control[2] or individual blade pitch control[4]) is usually required to save
41 turbine from overload at above-rated conditions. Detailed reviews on wind turbine control
42 strategies are refer to [Ha et al. \[5\]](#) and [Meng et al.\[6\]](#).

43 With the recent trend of deploying wind turbines from onshore and shallow waters to deep
44 waters, modern variable-speed pitch-regulated FOWTs can experience additional six degree-of-
45 freedoms (DOFs) platform motions (surge, sway and heave for translations, pitch, roll and yaw for
46 rotations), which leads to power fluctuations, increased fatigue loads, cyclic blade deformations,
47 dynamic rotor speeds and varying blade pitch angles. The coupled aero-servo-elastic nature of
48 flexible blades and floating motions complicate the FOWT wake characteristics, which can be a
49 significant concern for wind energy industrial as the average power losses due to wake
50 interactions can be 10%~20% of the total power for large offshore wind farms according to
51 [Barthelmie et al. \[7\]](#). Therefore, it is important to take the aerodynamics, structure dynamics and
52 control effects all into consideration to design a compact floating offshore wind farm. For that
53 purpose, an aero-servo-elastic method with acceptable accuracy and affordable efficiency is
54 required.

55 For structure modeling, a flexible slender structure can be directly modeled as 3D finite
56 elements (solids or shells) or be simplified as 1D beam structure. The 3D solid or shell finite

57 element model has the potential to provide the most accurate and detailed results for structural
58 dynamics, but it is computationally expensive as shown in [Bazilevs et al. \[8, 9\]](#) and the accuracy
59 improvements for overall responses are limited when compared with nonlinear beam model [as](#)
60 [shown in Júnior et al \[10\]](#). Thus, the beam model, which includes Euler-Bernoulli beam theory,
61 Timoshenko beam theory and Geometrically Exact Beam Theory (GEBT), is preferred for blade
62 aeroelasticity analysis unless the local stress or buckling phenomena is concerned.

63 The beam structure can be discretized using modal approach, multi-body dynamic (MBD)
64 method and 1D finite element method (FEM). The modal approach represents the blade
65 deformations as a linear combination of precomputed modal shapes, which significantly reduces
66 the modeling DOFs. Although highly efficient, the accuracy of modal approach greatly depends
67 on the number of modes considered and the important torsional DOF was usually neglected. For
68 example, only three bending modes, two modes for flap and one for edge, are used in FAST
69 ElastoDyn [2]. The MBD discretizes the flexible blade into a set of beam elements which are
70 constrained by force or motion relationships[11]. In 1D FEM, the blade is discretized into a series
71 of finite beam elements with shared nodal displacements and can be more accurate than modal or
72 MBD method with slightly increased computational cost [12], so that it is adopted in this paper.

73 For aerodynamics, many existing aero-servo-elastic analysis tools , such as FAST[2],
74 HAWC2[13] and DARwind[14], are mostly based on the Blade Element Momentum (BEM)
75 method which combines the 1D momentum theory and blade element theory. It is highly efficient
76 and accurate as long as the basic assumptions are valid and many semi-empirical models are well
77 implemented, such as tip/hub loss corrections, dynamic inflow models, stall delay corrections,
78 skewed-wake corrections and dynamic stall models. However, when a FOWT pitches or surges
79 backward at a velocity similar with its ambient wakes, the rotor can operate in a so-called vortex
80 ring state (VRS), a phenomena firstly observed for helicopters in descending flight [15]. In VRS,
81 the tip and root vortices recirculate around the rotor and the basic momentum balance assumption
82 breaks down, then the validity of BEM can be questionable as pointed out by [Sebastian et al.\[16\]](#).
83 In addition, the BEM-based aero-servo-elastic tools can not directly model turbine wakes unless
84 coupled with other flow solvers.

85 In recent decade, many endeavors were devoted to coupling high-fidelity geometry-resolving

86 CFD method with Computational Structural Dynamic (CSD) method for wind turbine
87 aeroelasticity modeling, often referred as CFD-CSD or Fluid Structure Interaction (FSI) approach,
88 wherein the aerodynamic loads are obtained by numerically solving the discretized Navier-Stokes
89 equations with body-fitted meshes and the blade deformations are calculated based on efficient
90 nonlinear beam theories. Additional mesh motion solver should be solved to handle volume mesh
91 displacements. For examples, [Yu et al.](#)[17] coupled an in-house CFD code with a structural model
92 based on non-linear flap-lag-torsion beam theory using the delta-airload loose-coupling
93 strategy[18]. Coupled CFD-MBD approaches were proposed by [Li et al.](#) [19] [Liu et al.](#) [20] [Guma](#)
94 [et al.](#) [21]. [Heinz et al.](#) [22]and [Grinderslev et al.](#) [23] coupled blade-resolving CFD code
95 EllipSys3D with existed aero-elastic code HAWC2, and [Dose et al.](#)[24] implemented an inhouse
96 structural code BeamFOAM in OpenFOAM. [Although the CFD approach provides outstanding](#)
97 [forecasts for wind turbine aerodynamic performance, wake flow, and vortex shedding, which](#)
98 [cannot be achieved by other BEM-based codes as shown in the works of Alkhabbaz et al. \[25, 26\].](#)
99 [Regrettably, it demands significant resources in terms of cost, time, and high-performance](#)
100 [computing.](#) As reported in [Dose et al.](#)[24], it takes approximately 1666 core*hours per rotor
101 revolution using partitioned loose-coupling strategy with 36.38 million cells and parallelized with
102 360 cores.

103 Alternative to the low fidelity BEM method and high fidelity CFD-CSD method, the medium
104 fidelity Actuator Line Method (ALM) [27] is a good trade-off between accuracy and efficiency for
105 blade aerodynamics. In ALM, a blade is modelled as an actuator line with distributed aerodynamic
106 forces which are projected to fluid domain as source terms in the discretized Navier-Stokes
107 equations to model the effects of rotating blades on flow. ALM facilitates the usage of relatively
108 coarse Cartesian grids with significantly reduced computational cost comparing with geometry-
109 resolving method while the important wake features such as helical tip/root vortices are well
110 captured, and can be directly extended for turbine-turbine interaction simulations without any
111 theoretical limitations. Coupling the ALM with beam theory based structure model leads to the
112 concept of Elastic Actuator Line Method (EALM), which has been used for evaluating wake
113 effects on fatigue loads for wind farms[28, 29], investigating aeroelastic wake behaviors[30-32],
114 turbine-turbine wake interaction analysis[33, 34] and aero-elastic-hydro-mooring-wake

115 analysis[35].

116 However, several limitations exist for previous works in terms of EALM and some challenges
117 should be well-treated. Many works were focused on bottom-fixed wind turbines with constant
118 rotor speeds and blade pitch angles, which is not fully reflecting the real applications of FOWTs.
119 Comprehensive validations of EALM models in terms of rotor performances and blade
120 deformations in a wide range of wind speeds with satisfactory results are currently absent. Details
121 required for accurate prediction of blade in-plane and torsional deformations which are closely
122 related to the bend-bend coupling and aerodynamic center offset were less discussed. A major
123 challenge for an EALM model is to design a suitable coupling strategy to communicate data
124 between different sub models efficiently while avoiding numerical instabilities.

125 In this paper, a new coupled aero-servo-elastic method for floating offshore wind turbine wake
126 simulation is presented based on the in-house code HEU-FOWT[36]. The previous version of
127 HEU-FOWT can perform aero-servo analysis of FOWTs via an advanced actuator line method
128 with actuator curve embedding (ACE) concept[37] for rigid blade aerodynamics and including
129 generator torque and full-span, rotor-collective blade pitch control strategies. We extend the
130 capabilities of HEU-FOWT to model blade elasticities using a nonlinear finite element rotating
131 beam theory for structural dynamics. Partitioned loose-coupling strategy is adopted for data
132 communication. For simplicity, only specified platform surge motion is considered. To the best
133 knowledge of the authors, this is the first work endeavored to couple ACE method with a
134 structural model and a control model, [and the advantage of this new method is its capability for](#)
135 [efficient aero-servo-elastic simulations of FOWT\(s\) including wakes on coarse Cartesian grids](#)
136 [without requiring empirical tip loss corrections.](#)

137 The rest of this paper is organized as follows: in **section 2**, the turbine geometries and
138 numerical methods for the proposed coupled aero-servo-elastic framework are described. The
139 computational domain layouts, mesh discretization and verifications are presented in **Section 3**. In
140 **section 4**, the proposed method is carefully validated and then applied for aero-servo-elastic
141 analysis of a NREL 5MW wind turbine under specified platform surge motions including wake
142 features. The conclusions are summarized in **section 5**.

143

144 2. Theories and models

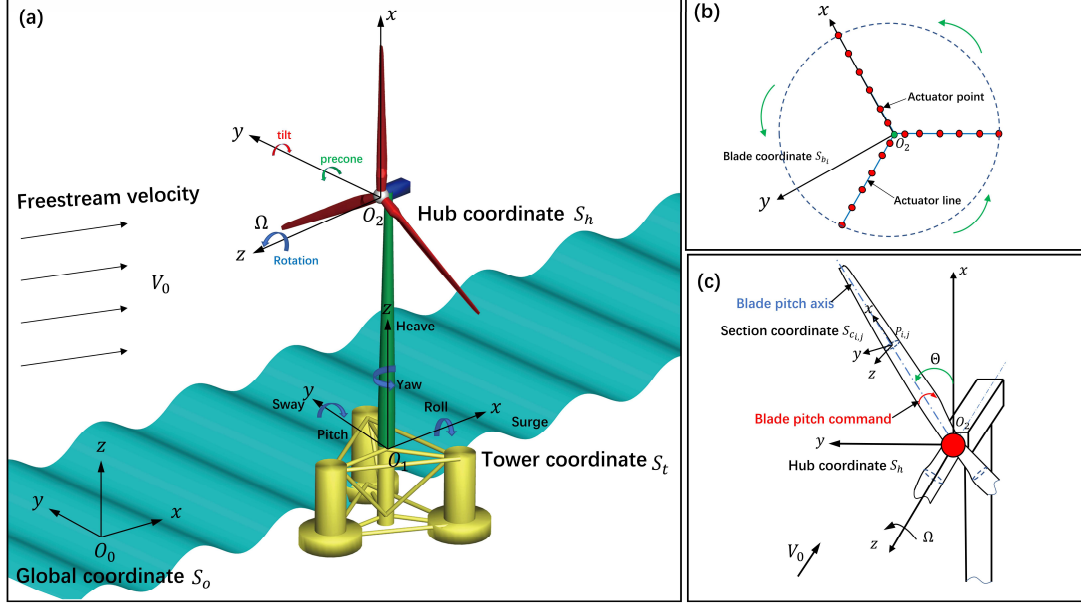
145 2.1 Wind turbine geometry model

146 The NREL 5MW wind turbine is adopted for simulation. It is a three-bladed variable-speed
147 pitch-regulated horizontal axis wind turbine with diameter $D = 126$ m and hub height $H = 90$ m.
148 Major properties are summarized in **Table 1**. The blade aerodynamic properties are defined by 17
149 sections and 7 airfoils while the blade structural properties are given on 49 sections. Detailed
150 airfoil aerodynamic coefficient tables and sectional material properties like mass, stiffness and
151 inertial are publicly available in [Jonkman et al.\[2\]](#). In this paper, only the blade aeroelasticity is
152 considered by modeling the blades as rotating cantilever beams. The influences of hub, nacelle,
153 tower on wake characteristics and the tower flexibility on blade structural responses are currently
154 neglected, but the variations of rotor speeds and blade pitch angles under platform surge motions
155 are considered. The NREL 5MW wind turbine with a OC4 submersible platform is shown in **Fig.**
156 **1 (a)** for demonstration, although the wave dynamics are currently neglected.

157 **Table 1** NREL 5MW turbine parameters.

Parameters	values
Diameter (D)	126 m
Number of blades(N_b)	3
Rotor tilt angle	5.0°
Blade upwind precone angle	2.5°
Averaged blade chord (\bar{c})	3.487 m
Cut-in, rated and cut-out wind speed	4m/s, 11.4m/s, 25m/s
Rated rotor speed (Ω_0)	12.1 rpm
Overhang, Hub height	5m, 90m
Blade airfoils	Cylinder1, Cylinder2, DU40,35, 30,25,21 and NACA64

158



159

160

161

Fig. 1. Floating offshore wind turbine system geometries and kinematics description using different reference coordinate systems.

162

2.2 Aerodynamic model

163

2.2.1 Flow solver

164

In HEU-FOWT, flow field simulation is based on Large Eddy Simulation (LES) wherein large

165

scales are directly resolved using the spatially filtered unsteady incompressible Navier-Stokes

166

equations and small scales are modeled as follows

167

$$\frac{\partial \bar{u}_i}{\partial x_i} = 0 \quad (1)$$

168

$$\frac{\partial \bar{u}_i}{\partial t} + \frac{\partial \bar{u}_i \bar{u}_j}{\partial x_j} = -\frac{1}{\rho_a} \frac{\partial \bar{P}}{\partial x_i} + \nu \frac{\partial^2 \bar{u}_i}{\partial x_j \partial x_j} - \frac{\partial \tau_{ij}}{\partial x_j} + \frac{1}{\rho_a} F_i \quad (2)$$

169

where \bar{u}_i donate the filtered velocity vectors with $i = 1,2,3$ corresponding to the x, y and z

170

directions in the global inertial coordinate system S_0 (**Fig. 1 (a)**). ν is kinematic viscosity. ρ_a is air

171

density. $\tau_{ij} = \overline{u_i u_j} - \bar{u}_i \bar{u}_j = -2\nu_t \bar{S}_{ij}$ is the sub-grid scale (SGS) stress tensor modeling the

172

effects of small scales. $S_{ij} = \frac{1}{2} \left(\frac{\partial \bar{u}_i}{\partial x_j} + \frac{\partial \bar{u}_j}{\partial x_i} \right)$ is the symmetric part of the resolved velocity gradient

173

tensor. ν_t is the sub-grid scale viscosity modeled by standard Smagorinsky SGS model as $\nu_t =$

174

$(C_s \Delta)^2 |\bar{S}_{ij}|$ with $C_s = 0.15$ is the Smagorinsky coefficient and $\Delta = (\Delta_x \Delta_y \Delta_z)^{1/3}$ is the filter

175

width. Δ_x, Δ_y and Δ_z are grid resolutions in x, y and z directions in global inertial coordinate

176 system. The effects of rotating blades on fluid flow are modelled as the body force term F_i which
177 is detailed in **Section 2.2.2**, thus LES can be used on relatively coarse Cartesian grids as direct
178 resolving of small-scale turbulences in near wall (blade surfaces) region is avoided.

179 **Eqs. (1) and (2)** are numerically solved on staggered stretching Cartesian grids using fractional
180 projection method with Constrained Interpolation Profile (CIP) method [38] including one
181 predictor step and two corrector steps. The CIP method, which consists of an advection step and a
182 non-advection step, is used for the predictor step and the first corrector step. In the advection step,
183 an intermediate velocity field is predicted by solving the advection equation $\frac{\partial \bar{u}_i}{\partial t} + \bar{u}_j \frac{\partial \bar{u}_i}{\partial x_j} = 0$ using
184 a semi-Lagrange approach which has low numerical diffusion and sub cell resolution with
185 compact upwind scheme. Then, the velocity field is corrected by viscous and body force term in
186 the non-advection step. A pressure Poisson equation is solved to update pressure field and the
187 velocity field is corrected again by the new pressure field. More details on the implementation of
188 CIP can refer to the work [Hu and Kashiwagi\[39\]](#). The pressure gradient term and viscous term are
189 spatially discretized by central difference scheme.

190 2.2.2 Actuator curve embedding model

191 The advanced actuator line method with actuator curve embedding concept (donated as
192 Actuator Curve Embedding method in this paper) proposed by [Jha and Schmitz\[37\]](#) is utilized to
193 construct the body force term F_i on the right-hand side of **Eq. (2)**. **Fig. 1(b)** shows the actuator
194 line representation of wind turbine blades with discrete actuator points. For the j^{th} section of i^{th}
195 blade, the section aerodynamics are calculated in the local section coordinate system $S_{c_i,j}$ using
196 quasi-steady 2D theory as in **Fig. 2**.

197

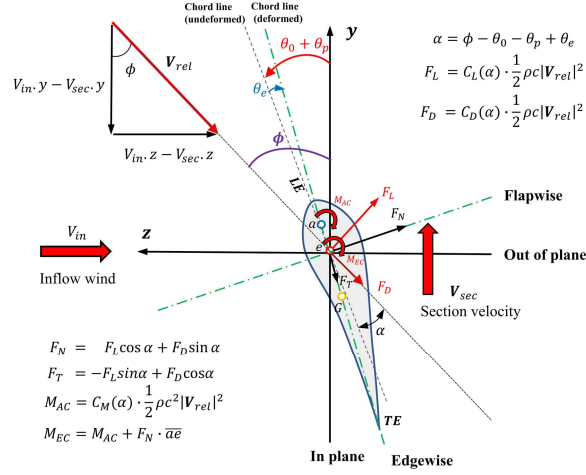


Fig. 2. Blade sectional aerodynamics. a is aerodynamic center and e is the pitch axis positoin.

Firstly, the relative velocity between each blade section and flow at sectional aerodynamic center (AC) is calculated as

$$\mathbf{V}_{rel} = \mathbf{V}_{in} - \mathbf{V}_{rot} - \mathbf{V}_{platform} - \mathbf{V}_{elastic} \quad (3)$$

where \mathbf{V}_{in} is the flow velocity sampled at AC point; \mathbf{V}_{rot} , $\mathbf{V}_{platform}$ and $\mathbf{V}_{elastic}$ is the velocity at AC due to rotor rotation, platform floating motions and blade elastic deformations, respectively.

The velocities induced by blade pitch command and blade elastic torsional deformations are currently neglected in **Eq. (3)**.

Then, the local angle of attack is obtained as

$$\alpha = \phi - \theta_0 - \theta_p + \theta_e \quad (4)$$

where $\phi = \tan^{-1} \left(\frac{-V_{rel,z}}{-V_{rel,y}} \right)$ is the local velocity inflow angle; θ_0 , θ_p and θ_e is the initial twist angle, blade pitch angle and elastic torsional angle for each blade section, respectively. Positive θ_e indicates nose-up direction which points to the negative x direction in section coordinate system

$S_{c_{i,j}}$.

Finally, the sectional aerodynamic loads are calculated as

$$F_L = C_L(\alpha) \frac{1}{2} \rho c |V_{rel}|^2 \quad (5)$$

$$F_D = C_D(\alpha) \frac{1}{2} \rho c |V_{rel}|^2 \quad (6)$$

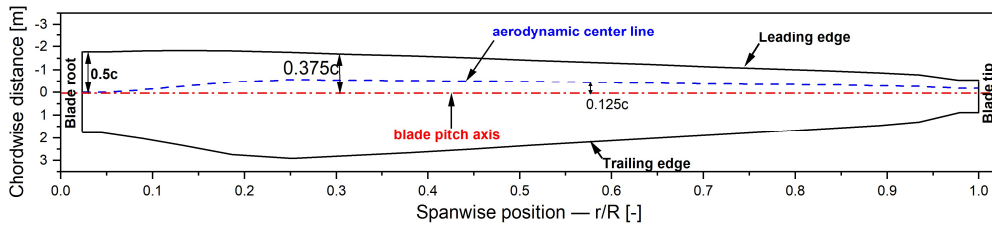
$$M_{AC} = \frac{1}{2} C_M(\alpha) \rho c^2 |V_{rel}|^2 \quad (7)$$

$$M_{PA} = \frac{1}{2} C_M(\alpha) \rho c^2 |V_{rel}|^2 + F_n * \bar{a}e \quad (8)$$

where c is local chord; C_L , C_D and C_M are sectional lift, drag and moment coefficients which are

219 interpolated from respective airfoil table according to local angle of attack α ; F_L and F_D are
 220 sectional lift and drag forces; M_{AC} and M_{PA} are sectional aerodynamic moments against the
 221 aerodynamic center and **pitch axis position**, respectively. $\bar{a}e$ is the chordwise distance between
 222 aerodynamic center (a) and pitch axis (e). $F_N = F_L \cos(\alpha) + F_D \sin(\alpha)$ and $F_T = -F_L \sin(\alpha) +$
 223 $F_D \cos(\alpha)$ are normal and tangential forces relative to local chord line.

224 The aerodynamic coefficients C_L , C_D and C_M in the airfoil tables are defined on sectional
 225 aerodynamic centers **and the blade pitch axis is defined as the reference line in structural model.**
 226 Therefore, the aerodynamic moments used for aeroelastic simulations should be calculated using
 227 **Eq. (8)** instead of **Eq. (7)** to account for the AC offset effects which have been neglected by many
 228 previous works [30] [40] [41] [42], **although it has been well addressed in the many BEM-based**
 229 **codes, such as FAST, and some vortex lattice code[43].** The positions of blade pitch axis and
 230 aerodynamic centers are schematically shown in **Fig. 3** following the more physically consistent
 231 definitions in the post of [44] instead of the original one[2], but the relative chordwise distance
 232 between pitch axis and AC is identical at each section for the two definitions which will not bring
 233 different results for lifting line based methods, such as BEM and ACE.



234 **Fig. 3.** Schematic of the NREL 5MW wind turbine blade with pitch axis and aerodynamic center line.

235 ACE differs from ALM only in the force projection process (See **Fig. 4**). In ALM, the body
 236 force term F_i at a grid point in fluid domain is obtained by accumulating the projected
 237 aerodynamic forces (using 3D Gaussian kernels) of all blade segments of all blades. This treatment
 238 leads to force overlapping and projecting body force beyond blade tips, so that the blade loads are
 239 usually overpredicted in tip regions and empirical tip loss corrections are always required for
 240 remedy. In ACE concept, the blade forces are only projected **in the plane normal to the actuator**
 241 **curve**. For a grid in fluid domain (see Q_0 in **Fig. 4**) to be influenced by blade forces, it is first
 242 associated with an artificial section between two actuator points P_m and P_{m+1} **of each actuator**
 243 **curve** (see the point F in **Fig. 4**), where point F is determined by keeping $\overline{Q_0 F}$ normal to the

245 actuator curve. The blade forces at the artificial section are interpolated using neighboring
 246 information and then are project to grid point Q_0 using 2D Gaussian kernels. Looping over the N_b
 247 blades, the body force experienced by grid Q_0 can be expressed as

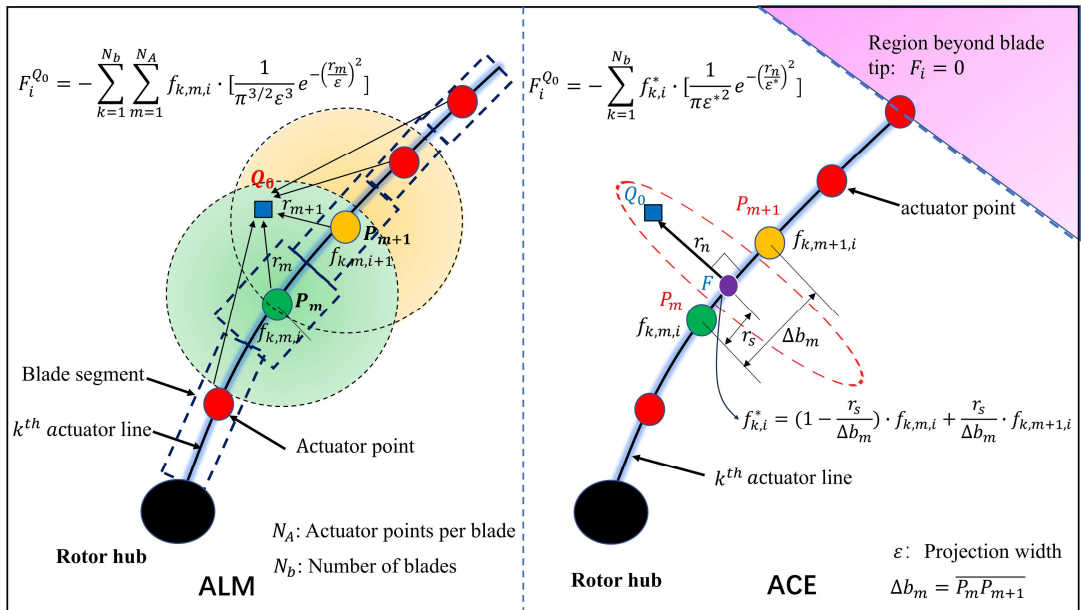
$$248 \quad F_i = - \sum_{k=1}^{N_b} f_{k,i}^* \left[\frac{1}{\pi \varepsilon^{*2}} e^{-\left(\frac{r_n}{\varepsilon^*}\right)^2} \right] \quad (9)$$

249 where N_b is the blade number, ε^* is the smooth length scale of the 2D Gaussian kernel at the
 250 artificial section, r_n is the normal distance from the grid to the associated blade section. $f_{k,i}^*$ is the
 251 i^{th} component of the total aerodynamic force (vector sum of lift and drag in global inertial
 252 coordinate system) at the artificial section of k^{th} blade. Both $f_{k,i}^*$ and ε^* are linearly interpolated
 253 using neighboring values as

$$254 \quad f_{k,i}^* = \left(1 - \frac{r_s}{\Delta b_m}\right) f_{k,m,i} + \frac{r_s}{\Delta b_m} f_{k,m+1,i} \quad (10)$$

$$255 \quad \varepsilon^* = \left(1 - \frac{r_s}{\Delta b_m}\right) \varepsilon_m + \frac{r_s}{\Delta b_m} \varepsilon_{m+1} \quad (11)$$

256 where Δb_m is the distance between neighboring points P_m and P_{m+1} , r_s is the tangential distance
 257 from point P_m along actuator line. ε_m and ε_{m+1} are smooth length scales at points P_m and P_{m+1} .
 258 $f_{k,m,i}$ and $f_{k,m+1,i}$ are i^{th} component of total aerodynamic forces at actuator points P_m and P_{m+1}
 259 of the k^{th} blade. For a curved actuator line, some fluid points on the concave and convex sides of
 260 the actuator curve may need special treatments to obtain the tangential and normal coordinates,
 261 which is addressed in Appendix A.



262

263 **Fig. 4.** Comparison of conventional Actuator Line Method (ALM)[27] and Actuator Curve Embedding (ACE)[37]
 264 approach in body blade force projection process. Q_0 is an arbitrary grid point in fluid domain.

265 The 2D Gaussian smooth length scale ε is proportional to the local chord (c^*) of an equivalent
 266 elliptic wing of a real blade to approximate the primary mode of blade distributed load. The root
 267 chord (c_0) and local chord (c^*) distribution of the equivalent elliptic wing are given in **Eq. (12)**
 268 and **(13)**. More details on the ACE implementation and the equivalent elliptic wing approximation
 269 can be found in [Jha et al.\[45\]](#). In this paper, the 2D Gaussian smooth length scale is set as $\varepsilon =$
 270 $1.2c^*$ based on our previous work [Yang et al.\[36\]](#) and the work of [Jha and Schmitz\[37\]](#).

$$271 \quad c_0 = \frac{4}{\pi R} \int_0^R c(r) dr = \frac{4\bar{c}}{\pi} \quad (12)$$

$$272 \quad c^*(r) = c_0 \sqrt{1 - \left(\frac{2r}{R} - 1\right)^2} \quad (13)$$

273 where R is blade radius and $\bar{c} = \frac{1}{R} \int_0^R c(r) dr$ is the averaged chord length of a real blade.

274 2.3 Structural model

275 The blade elasticity modeling is mainly referred to the work of [Ma et al.\[30\]](#), in which the
 276 turbine blades are modeled as nonlinear rotating Euler-Bernoulli beams and the beam structures
 277 are further discretized by two-node, 12 degrees of freedom (DOF) finite elements (**Fig. 5**). Blade
 278 element elastic deformations, including elongation (u_x), torsion (θ_x) and bending deformations
 279 (u_y, u_z), are represented using nodal displacements and elemental shape functions as in **Eq. (14)**
 280 or in matrix format as **Eq. (16)**. q^e is the element nodal displacement. L is the length of beam
 281 element. N_0, N_1, N_2, N_3 and N_4 are strain matrices.

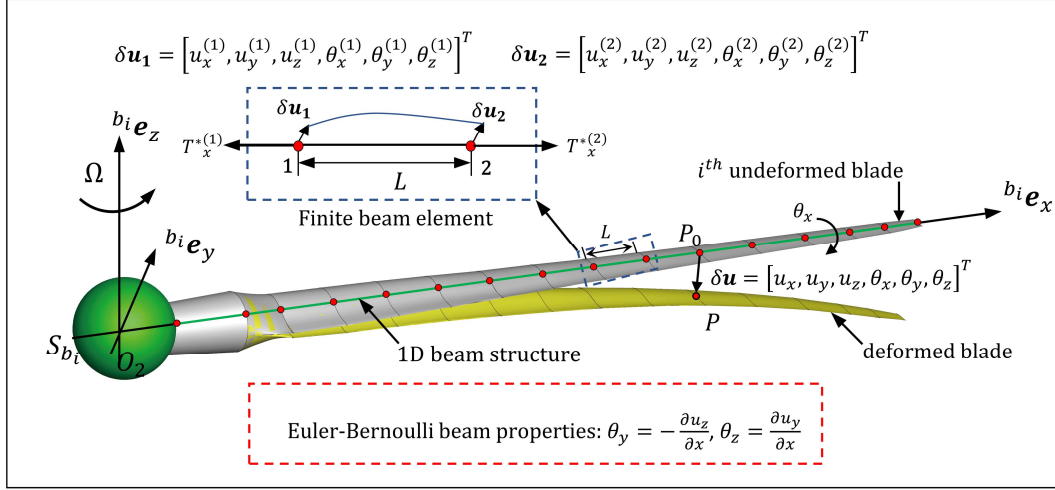
$$282 \quad \begin{bmatrix} u_x \\ u_y \\ u_z \\ \theta_x \end{bmatrix} = \begin{bmatrix} \eta_1(x) & 0 & 0 & 0 & 0 & 0 & \eta_2(x) & 0 & 0 & 0 & 0 & 0 \\ 0 & \eta_3(x) & 0 & 0 & 0 & \eta_4(x) & 0 & \eta_5(x) & 0 & 0 & 0 & \eta_6(x) \\ 0 & 0 & \eta_3(x) & 0 & -\eta_4(x) & 0 & 0 & 0 & \eta_5(x) & 0 & -\eta_6(x) & 0 \\ 0 & 0 & 0 & \eta_1(x) & 0 & 0 & 0 & 0 & 0 & \eta_2(x) & 0 & 0 \end{bmatrix} q^e \quad (14)$$

$$283 \quad \eta_1(x) = 1 - \xi, \eta_2(x) = \xi, \eta_3(x) = 1 - 3\xi^2 + 2\xi^3, \eta_4(x) = L\xi(1 - 2\xi + \xi^2)$$

$$284 \quad \eta_5(x) = 3\xi^2 - 2\xi^3, \eta_6(x) = L\xi^2(\xi - 1), \xi = \frac{x}{L}$$

$$285 \quad q^e = [u_x^{(1)} \quad u_y^{(1)} \quad u_z^{(1)} \quad \theta_x^{(1)} \quad \theta_y^{(1)} \quad \theta_z^{(1)} \quad u_x^{(2)} \quad u_y^{(2)} \quad u_z^{(2)} \quad \theta_x^{(2)} \quad \theta_y^{(2)} \quad \theta_z^{(2)}]^T \quad (15)$$

$$\begin{bmatrix} u_x \\ u_y \\ u_z \\ \theta_x \end{bmatrix} = \begin{bmatrix} N_1 \\ N_2 \\ N_3 \\ N_4 \end{bmatrix} \mathbf{q}^e = \begin{bmatrix} N_0 \\ N_4 \end{bmatrix} \mathbf{q}^e \quad (16)$$



287

288

Fig. 5. Schematic of blade elastic deformations and finite beam element representation.

289

The blade elastic responses are obtained by solving the following discretized equations of motion

290

for each rotating blade using Newmark method,

291

$$\mathbf{M}_E \ddot{\mathbf{q}} + \mathbf{C}_E \dot{\mathbf{q}} + \mathbf{K}_E \mathbf{q} = \mathbf{F}_E \quad (17)$$

292

where \mathbf{M}_E , \mathbf{C}_E , \mathbf{K}_E are global mass, damping and stiffness matrix, and \mathbf{F}_E is the generalized loads

293

for each blade, which are obtained by assembling the blade elemental matrices and r.h.s. By

294

solving the eigen value problem of Eq. (17), the eigen values correspond to the circular natural

295

frequencies and the normalized eigen vectors are modal shapes for each blade. The derivation of

296

elemental matrices and r.h.s are detailed in Appendix B.

297

2.4 Control strategies

298

The control strategies in the current code are based on the work of Jonkman et al.[2] which

299

consist of a generator torque controller and a full-span rotor-collective blade pitch controller. A

300

low-pass filter is applied to the real-time rotor speed Ω to mitigate high frequency excitation with

301

the filtered rotor speed $\tilde{\Omega}$ in discrete formulation as

302

$$\tilde{\Omega}_{n+1} = (1 - \beta)\tilde{\Omega}_n + \beta\Omega_{n+1} \quad (18)$$

303

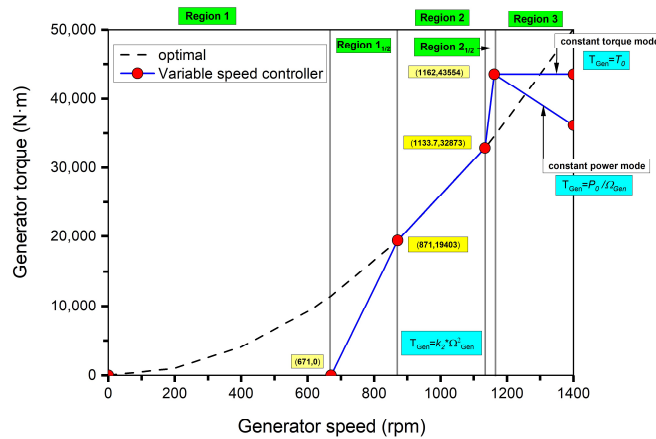
where $\beta = e^{-2\pi f_c \delta t}$ and f_c is corner frequency. δt is timestep size.

304 The dynamic rotor speed is determined through a single DOF equation with rotor aerodynamic
 305 torque (T_a) (against rotor shaft) and generator torque (T_{Gen}) as

$$306 \quad I_D \dot{\Omega} = T_a - T_{Gen} \quad (19)$$

307 where I_D is the total drivetrain inertial cast to the low-speed shaft. The real-time generator speed
 308 (Ω_{gen}) and filtered generator speed ($\tilde{\Omega}_{Gen}$) is proportional to real-time rotor speed and filtered
 309 rotor speed with gear box ratio (N_{Gear}), respectively. Namely, $\Omega_{Gen} = N_{Gear}\Omega$ and $\tilde{\Omega}_{Gen} =$
 310 $N_{Gear}\tilde{\Omega}$.

311 The generator torque T_{Gen} is calculated as a tabulated function of the filtered generator speed
 312 $\tilde{\Omega}_{Gen}$ according to five defined torque control regions: 1, 1_{1/2}, 2, 2_{1/2} and 3 (see Fig. 6). In region 1,
 313 the generator torque is set to zero as the effective wind speed is lower than cut-in wind speed. In
 314 region 2, the generator torque is proportional to the square of the filtered generator speed in order
 315 to maximize power extraction by maintaining a constant optimal tip speed ratio. In region 3, the
 316 effective wind speed exceeds the rated one and the pitch controller begins to work. The generator
 317 torque can be inversely proportional to the filtered rotor speed to maintain a constant generator
 318 power (constant power mode) or be maintained as constant nominal torque (constant torque mode).
 319 Region 1_{1/2} and 2_{1/2} are two linear transition regions. The output generator power is calculated as
 320 $P_{Gen} = \eta T_{Gen} \Omega_{Gen}$ where η is electrical efficiency of generator while the rotor aerodynamic
 321 power is calculated as $P_{aero} = T_a \Omega$.



322
 323 **Fig. 6.** Generator torque-speed plot.

324 Based on gain-scheduled proportional-integral (PI) control, the blade pitch command θ_p has the
 325 general form of

326
$$\theta_p = \left(K_{P0}\Delta\Omega + K_{I0} \int_0^t \Delta\Omega dt \right) GK(\theta)N_{Gear} \quad (20)$$

327 where K_{P0} and K_{I0} is the proportional and integral gains at zero pitch angles. $GK(\theta_p) = \frac{1}{1+\theta_p/\theta_k}$ is

328 a correction factor to account for the variations of proportional and integral gains at non-zero pitch

329 angles. θ_k is the blade pitch angle where the power sensitivity $\frac{\partial P}{\partial \theta_p}$ is doubled compared with rated

330 conditions with zero pitch angle, namely: $\frac{\partial P}{\partial \theta_p}(\theta = \theta_k) = 2 \frac{\partial P}{\partial \theta_p}(\theta_p = 0)$. $\Delta\Omega = \Omega - \Omega_0$ is rotor

331 speed error. More details are provide in [Jonkman et al. \[2\]](#) and key parameters are summarized in

332 **Table 2.**

333

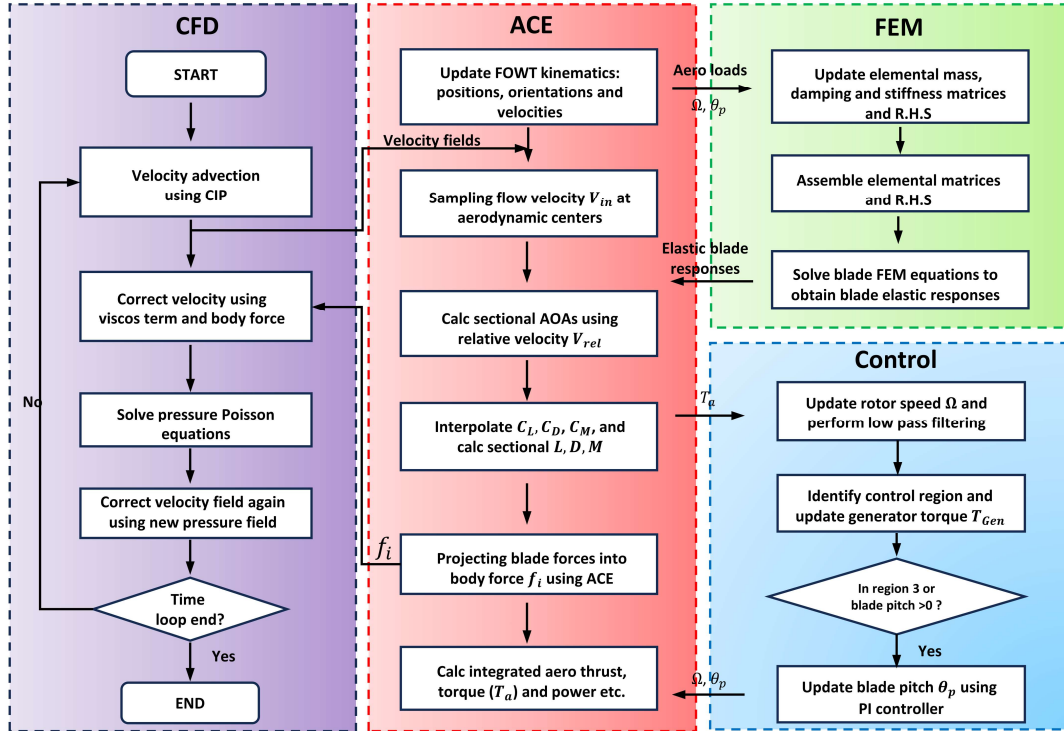
Table 2 Parameters of the NREL 5MW wind turbine controller.

Parameters	Symbols	Values
Rated generator torque	T_0	43,093.55 N•m
Rated generator mechanical power	P_0	5.296610 MW
Electrical efficiency of generator	η	94.4%
Rated generator output power		5.0 MW
Gear box ratio	N_{Gear}	97:1
Generator Torque Constant in Region 2		0.0255764 N•m/rpm ²
Corner frequency for low pass filter	f_c	0.25 Hz
Rated rotor speed	Ω_0	12.1 rpm
Default Proportional Gain at rated point	K_{P0}	0.01882681
Default Integral Gain at rated point	K_{I0}	0.008068634
Pitch Angle with doubled power sensitivity	θ_k	6.30°
Total drivetrain inertial cast to LSS	I_D	43784725.44 kg · m ⁴

334 2.5 Aero-servo-elastic coupling strategy

335 The HEU-FOWT code is written in modern Fortran language with object-oriented programing
336 and can be divided as four major modules: basic CFD solver, ACE module, control module and
337 FEM module. [The partitioned loose-coupling strategy is adopted for data communication between](#)
338 [different modules while preserving code modularity and avoiding inner loop sub iterations](#)
339 [between different sub problems.](#) For each blade, the aerodynamic loads, blade elastic deformations
340 and elastic velocities are communicated between ACE and FEM modules using cubic
341 interpolations. The control module only receives rotor aerodynamic torque as input and output
342 rotor speeds and collective blade pitch angles. [Numerical stability of the coupling strategy is](#)

343 maintained by introducing structural damping term as described in Appendix C. A flow chart
 344 describing the coupling procedures is shown in Fig. 7.



345
 346

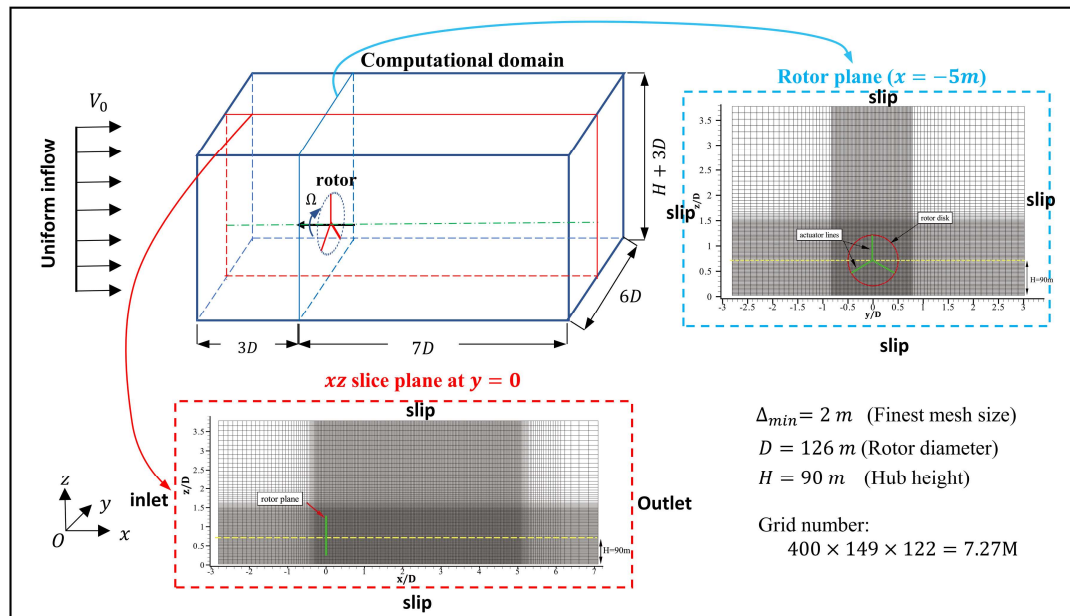
Fig. 7 Flow chart of the current aero-servo-elastic model with partitioned loose-coupling strategy.

347 3. Computational domain and mesh

348 The computational domain is $6D$ in width, $H + 3D$ in height, $10D$ in length with the outlet at $7D$
 349 downstream. The domain is discretized by stretching Cartesian grids with a wake refinement
 350 region extending $5D$ downstream with finest grid resolution of $2m$ (approximately 32 grids per
 351 blade radius), which is donated as **Grid #1** with a total grid number of 7.3 million ($400 \times 149 \times$
 352 122) as shown in Fig. 8 . However, if only the rotor loads or blade elastic deformations are
 353 concerned, the grid with a smaller wake refinement region extending only $1D$ downstream, which
 354 is donated as **Grid #2** with a total grid number of 3.2 million ($178 \times 149 \times 122$), is used to
 355 reduce computational time as between using **Grid #1** and **Grid #2** are negligible (simulation
 356 results not shown in this paper). In this paper, **Grid #1** is used for surge motion cases and **Grid #2**
 357 is used for verifications and validations. A mesh independence test is performed for a bottom-fixed
 358 NREL 5MW wind turbine with elastic blades without control ($V_0=11.4m/s$, $\Omega=12.1$ rpm, $\theta_p = 0^\circ$)

359 with the time averaged aeroelastic results (over rev. 22-24) are shown in Table 3. As shown, the
 360 relative errors between the grid resolutions of 2m and 1.5m are well below 1% for thrust, power,
 361 blade tip in-plane and out-of-plane deformations. The large relative error for blade torsion
 362 deformation is due to the very small absolute values, which should not introduce too much
 363 numerical errors. Therefore, the grid resolution of 2m in wake refinement region is used
 364 throughout this paper.

365 The time step size is determined by restricting the blade to rotate 1° per step and all cases are
 366 currently simulated in serial mode without parallelization. The real computational time for each
 367 time step is approximately 25.3s for Grid #2 (3.2 M) and 58.6 s for Grid #1 (7.3 M), wherein the
 368 computational time introduced by the aero-elastic model is approximately 1.0s for both grids.



369
 370 **Fig. 8 .** Computational domain, boundary conditions and mesh discretization (**Grid #1**).

371
 372 **Table 3** Time-averaged aeroelastic results from mesh independence test.

373

$(V_0=11.4\text{m/s}, \Omega = 12.1 \text{ rpm}, \theta_p = 0^\circ)$

Δ_{min} [m]	R/Δ_{min} [-]	Thrust [KN]	Power [MW]	Inp [m]	Oop [m]	Torsion [deg]
1.5	42	717.30	5.29	-0.59	5.71	0.09
2.0	31.5	713.90	5.24	-0.59	5.70	0.07
4.0	15.8	714.08	5.20	-0.59	5.76	0.08
8.0	7.9	741.76	5.49	-0.62	5.98	0.15

374

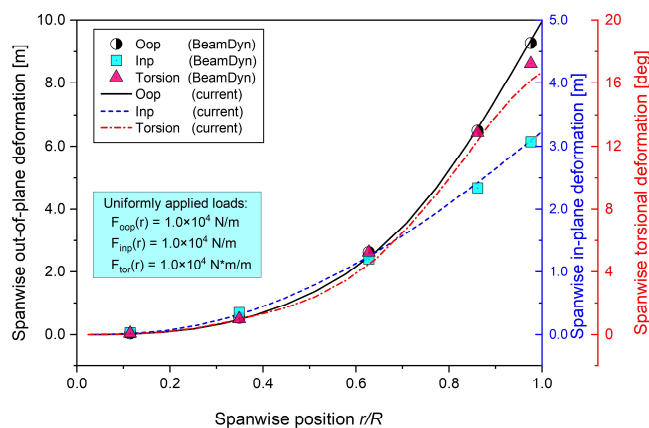
375 4. Results and discussions

376 Since the aerodynamic model and control model have been systematically validated in our
 377 previous work Yang et al. [36], we focus on the validation of structural model and coupled aero-
 378 servo-elastic model in **section 4.1**. Then, the validated aero-servo-elastic model is applied for
 379 simulation of a controlled NREL 5MW wind turbine with elastic blades under specified surge
 380 motion in **section 4.2**.

381 4.1 Validations

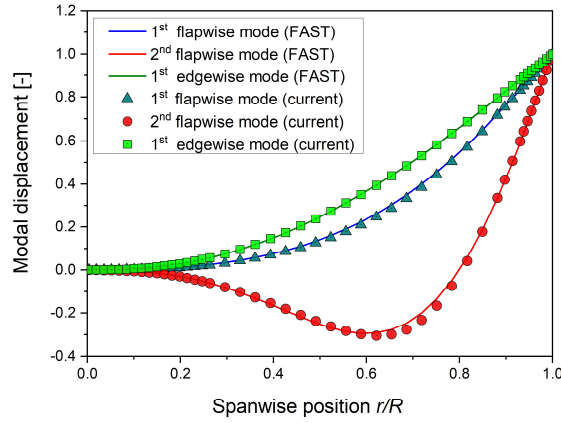
382 4.1.1 Test of a static blade

383 Prior to conducting more complicated aero-servo-elastic simulations, it is beneficial to first
 384 validate the structural model in static conditions. The in-plane, out-of-plane and torsional
 385 deformations of a single static NREL 5MW wind turbine blade under uniformly distributed loads
 386 are compared with results of BeamDyn (stand-alone version v1.01[46]) in **Fig. 9**. The first six
 387 natural frequencies are listed in **Table 4** with the results using Modal methods[42], GEBT[41],
 388 BeamFOAM[24], BModes[47] and ABQUS[47]. The lowest three modal shapes are compared in
 389 **Fig. 10** with FAST results[2]. Excellent agreements are obtained, indicating that the structural
 390 model is well established.



391
 392

Fig. 9. Spanwise static deformations under uniform applied loads.



393
394 **Fig. 10.** Modal shapes validation for static NREL 5MW wind turbine blade.

395 **Table 4** Natural frequencies (Hz) of a single blade without applied loads and rotation.

No.	Current	Modal[42]	GEBT[41]	BeamFOAM[24]	BModes[47]	ABAQUS[47]
f_{n1}	0.70	0.68	0.68	0.67	0.69	0.68
f_{n2}	1.11	1.09	1.10	1.06	1.12	1.10
f_{n3}	2.02	1.95	1.98	1.91	2.00	1.98
f_{n4}	4.12	4.00	3.99	-	4.12	3.99
f_{n5}	4.67	4.52	4.66	-	4.64	4.66
f_{n6}	5.58	5.58	5.53	-	5.61	5.53

396 4.1.2 Stiffening of a rotating uniform cantilever beam

397 Since the blades are modeled as rotating cantilever beams in this paper, a rotating uniform
 398 cantilever beam is herein used to partially validate the structural model in predicting rotation
 399 added centrifugal-stiffening effects. The mass density (ρ_s) of the uniform beam is 7840 kg/m^3 ,
 400 cross-section area (A) is $2.0 \times 10^{-4} \text{ m}^2$, elastic modulus (E) is $2.0 \times 10^{11} \text{ Pa}$, cross-section
 401 moment of inertial (I) is $2.0 \times 10^9 \text{ m}^4$, and beam total length (L) is 0.6 m . In simulation, the
 402 uniform beam is divided into 50 elements, and only the centrifugal-stiffening effects are
 403 considered in order to be consistent with the reference case setups. For better comparisons, the
 404 dimensionless angular velocity (γ) and natural frequency (μ) are introduced as

$$405 \quad \gamma = \Omega \sqrt{\frac{\rho_s A L^4}{EI}} \quad \text{and} \quad \mu = \omega \sqrt{\frac{\rho_s A L^4}{EI}}, \quad (21)$$

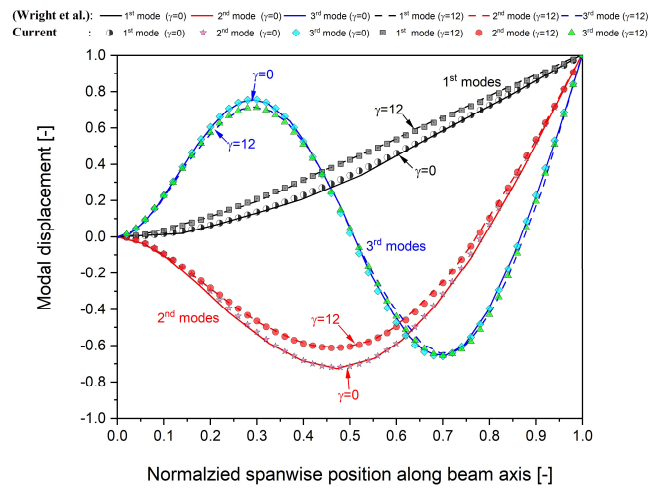
406 where Ω is angular velocity (rad/s), ω is circular natural frequency.

407 The lowest three dimensionless natural frequencies for $\gamma = 0, 1, 5$ and the lowest three modal
 408 shapes for $\gamma = 0$ and 12 are compared in **Table 5** and **Fig. 11**, respectively. The current results

409 agree well with the results of Gebhardt and Roccia[48] using nonlinear finite element beam
 410 method and the exact solutions from Wright et al.[49] using convergent power series, indicating the
 411 centrifugal stiffening effects are correctly modelled.

412 **Table 5** Comparison of dimensionless natural frequencies of a rotating uniform cantilever beam

γ	Mode	Current	Gebhardt and Roccia[48]	Wright et al.[49]
0	First	3.5159	3.5160	3.5160
	Second	22.0337	22.0345	22.0345
	Third	61.6951	61.6972	61.6972
1	First	3.6815	3.6816	3.6817
	Second	22.1802	22.1810	22.1810
	Third	61.8396	61.8418	61.8418
5	First	6.4490	6.4495	6.4495
	Second	25.4447	25.4460	25.4461
	Third	65.2023	65.2051	65.2050



413
 414
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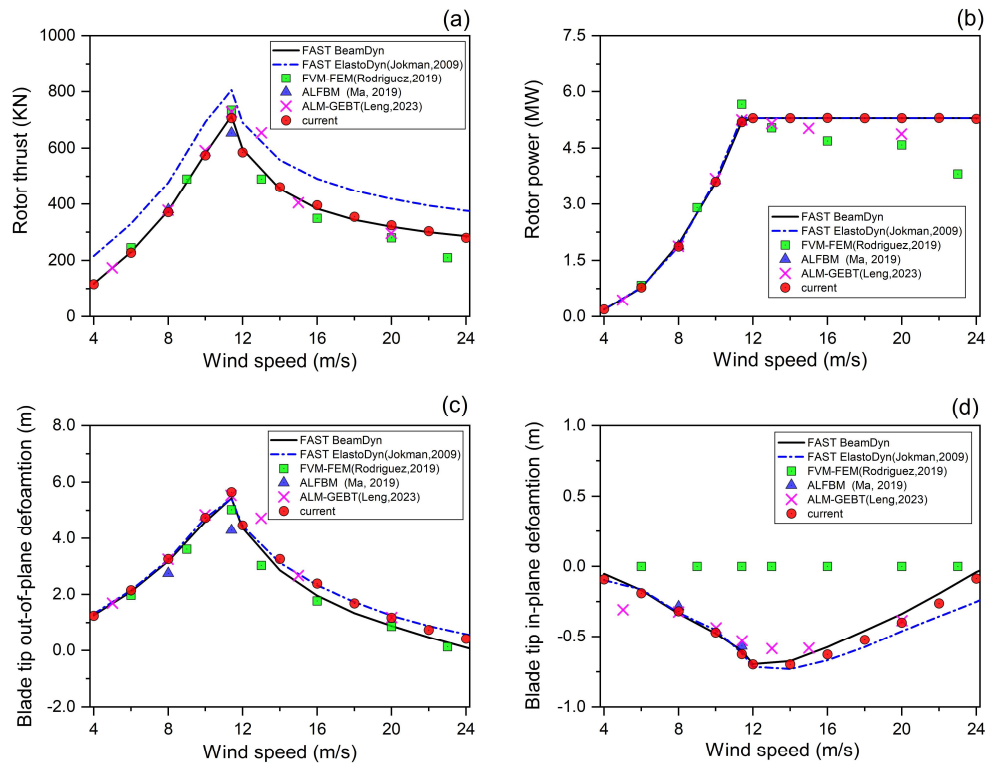
Fig. 11. Modal shapes validation for a rotating uniform cantilever beam at $\gamma = 0, 12$.

416 4.1.3 Validations for coupled aero-servo-elastic model

417 In this sub section, the coupled aero-servo-elastic performances of a bottom-fixed NREL 5MW
 418 wind turbine under uniform inflow conditions are validated. The significance of aerodynamic
 419 center offset effects on torsional deformation is particularly highlighted. The mean results are
 420 averaged over the last rotor revolution when the time histories reach steady states. Blade pitch
 421 control with constant power mode in region 3 (see Fig. 6) is used.

422 In Fig. 12, the rotor thrust, rotor power, blade tip Oop and Inp deformations at wind speeds
 423 ranging from 4 m/s to 24 m/s are cross validated with Jonkman et al.[2] using modal methods
 424 (FAST ElastDyn), Rodriguez and Jaworski[40] using free vortex wake method with nonlinear

425 finite element method (FVM-FEM), [Ma et al.](#)[30] using actuator line method with nonlinear finite
 426 element beam model(ALFBM) and [Leng et al.](#) [32] using actuator line method with geometrically
 427 exact beam theory(ALM-GEBT). The results of FAST with BeamDyn (open-FAST v2.3.0) are
 428 also added for comparison.



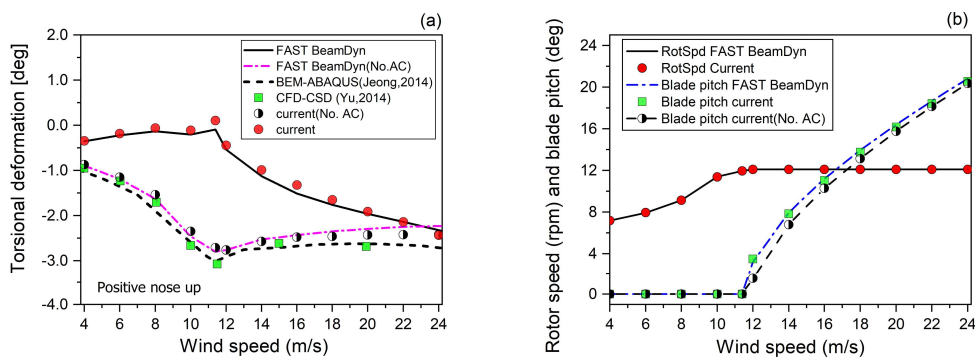
429
 430 **Fig. 12 .** Azimuth-averaged rotor performances and blade tip deflections with varying wind speeds: (a) rotor
 431 thrust, (b) rotor power, (c) out-of-plane and (d) in-plane deformations.

432 Excellent agreements are observed for the current results with FAST BeamDyn in terms of
 433 rotor thrust and power. The constant power feature at above-rated wind speeds is well captured
 434 thanks to the inclusion of control module. The thrust overprediction in [Jonkman et al.](#)[2] is due to
 435 the definition of “RotThrust” in FAST which is the total force of rotor acting on the tilted shaft,
 436 including aerodynamic force, gravity component (approximately 94kN for tilt angle of 5°) and
 437 inertial effects. The blade tip Inp and Oop deformations are slightly varied between FAST
 438 ElastoDyn and FAST BeamDyn at above-rated wind speeds. This can be explained by the
 439 different modelling methodologies on blade torsion, wherein ElastoDyn totally neglects torsion
 440 effects, BeamDyn considers the coupling between torsion and bending shear while the current

441 method models torsion as an independent DOF.

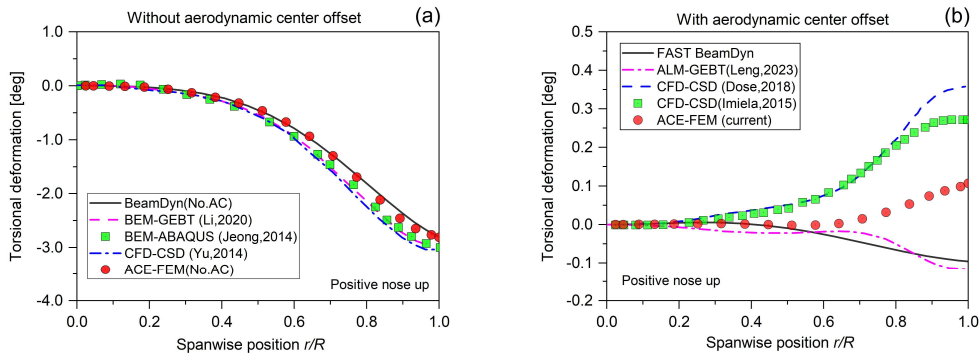
442 Since the blade torsional deformations and blade pitch angles directly impact the sectional
443 effective AOAs, a detailed discussed is followed with an emphasize on aerodynamic center offset
444 effects. The results from FAST with BeamDyn and Yu et al.[17] using CFD-CSD as well as Jeong
445 et al.[47] using BEM-ABAQUS are compared in Fig. 13 , wherein the data with “No.AC”
446 indicates no aerodynamic center offset.

447 As shown, the current results agree well with FAST BeamDyn for conditions with and without
448 AC offset. Significant discrepancies are observed for blade torsion near the rated wind speeds for
449 cases with and without AC offset (Fig. 13 (a)). Without AC offset, the large (in terms of
450 magnitude) nose-down torsional deformations can result in significantly reduced AOAs and
451 consequently the rotor performances near rated wind speeds, and the blade pitch angles required to
452 maintain rated power at above conditions are reduced (See Fig. 13 (b)). To further examine the
453 AC offset effects, the spanwise torsional deformations at rated wind speeds are plotted in Fig. 14
454 with additional results from Imiela et al. [50] and Dose et al.[24] using CFD-CSD, Li et al.[41]
455 using BEM-GEBT, and Leng et al.[32] using ALM-GEBT. The current method reproduces the
456 spanwise torsions well for cases without AC offset (Fig. 14 (a)). Although the predicted spanwise
457 torsions are slightly varied for cases with AC offset (Fig. 14 (b)) which may due to different
458 structural models and geometry preparations, the magnitudes are small and should not have
459 considerable impact on rotor performances and wake features. The reason why the results of Yu et
460 al. [17]using CFD-CSD show similar magnitudes with other results without considering AC offset
461 is unclear. The azimuth-averaged spanwise blade deflections are also well predicted as shown in
462 Fig. 15.

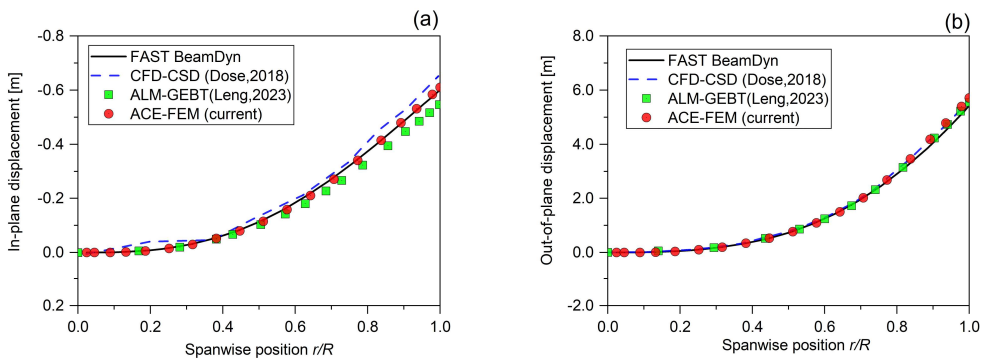


463
464

Fig. 13. Azimuth-averaged (a) blade tip torsional deformation, (b) rotor speed and blade pitch angle with varying



466 **Fig. 14.** Azimuth-averaged spanwise blade torsional deformations at rate wind speed. (a) without AC offset;(b)
 467 with AC offset.
 468



469 **Fig. 15** Azimuth-averaged spanwise blade deflections at rated wind speed: (a) Out-of-plane deformations, (b) In-
 470 plane deformations.
 471

472 A comprehensive comparison of rotor performances and blade tip deformations at rated wind
 473 speed is presented in **Table 6**, wherein the current results show the rotor thrust and Oop
 474 deformation are greatly influenced by AC offset effects which are decreased by 10.5% and 15.0%.
 475 Meanwhile, the current rotor thrust and power are only slightly decreased by 1.0% and 0.2%
 476 comparing with BeamDyn, which indicates the AC offset effects are far more significant than
 477 geometry nonlinearities and the usage of Euler-Bernoulli beam with various nonlinear effects is
 478 acceptable at least for the current 5MW-class wind turbine at rated condition.

479 **Table 6** Comparison of Azimuth-averaged rotor performances and blade tip deformations of
 480 the NREL-5MW RWT at rated wind speed.

Study	Model	Year	Inp (m)	Oop (m)	Torsion (deg)	Thrust (KN)	Power (MW)
Jonkman et al.[2]	BEM-Modal	2009	-0.61	5.47	-	814.45	5.28
Yu and Kwon[17]	CFD-CSD	2014	-0.63	4.72	-3.04	656.43	5.22
Li et al.[19]	CFD-MBD	2015	-0.58	6.38	-	759	5.41
Imiela and Wienke[50]	CFD-CSD	2015	-0.65	5.98	0.28	808	5.62
Ponta et al.[51]	BEM-GTBM	2016	-0.56	3.85	-	660.19	5.19

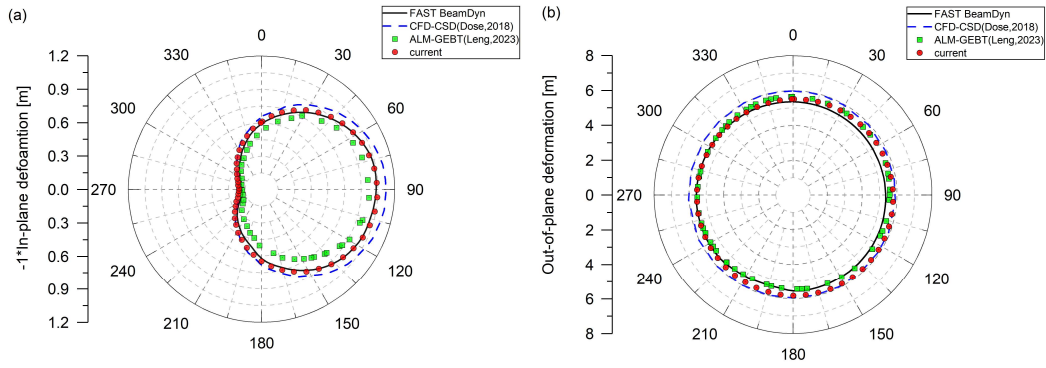
Dose et al.[24]	CFD-CSD	2018	-0.64	5.98	0.29	768.60	5.46
Sabel and Gopal[52]	BEM-GEBT	2019	-0.57	4.41	-0.29	690.72	4.97
Sabel and Gopal[53]	BEM-GEBT	2019	-0.62	4.55	-	676.12	5.17
Ma et al.[30]	ALM-Beam	2019	-0.57	4.28	-	653	5.20
Liu et al.[20]	CFD-MBD	2019	-0.60	5.60	-	733.00	4.90
Li et al.[41]	BEM-GEBT	2020	-0.57	4.49	-2.97	678.44	5.30
Leng et al.[32]	ALM-GEBT	2023	-0.55	5.53	-0.12	728.50	5.26
Zheng et al.[33]	ALM-Beam	2023	-0.28	4.95	-	681.64	4.67
BeamDyn(No.AC)	BEM-GEBT	2023	-0.59	4.69	-2.80	651.51	5.08
BeamDyn	BEM-GEBT	2023	-0.60	5.43	-0.10	713.37	5.20
Current (No.AC)	ACE-Beam-PI	2023	-0.53	4.80	-2.71	632.21	4.92
Current	ACE-Beam-PI	2023	-0.62	5.65	0.10	706.10	5.19

481 For completeness, the results of different blade types and control configurations are
482 summarized in **Table 7**. Blade elasticity is found to slightly reduce the rotor performances for no-
483 controller cases (case 1 and 3) and the controller responses (rotor speed and blade pitch) for with-
484 controller cases (case 2 and 4) which leads to an increase of rotor thrust is by 3.4%. For rigid and
485 elastic blades, the controller tends to reduce rotor performances through positive blade pitch angle
486 or reduced rotor speed.

487 **Table 7** Comparison of Azimuth-averaged rotor performances and blade tip deformations of the NREL 5MW
488 RWT at rated wind speed.

Case	Study	Wind speed (m/s)	RotSpd (rpm)	Pitch (deg)	Thrust (KN)	Power (KW)	Inp (m)	Oop (m)	Torsion (deg)
1	Rigid blades	11.4	12.1	0	723.69	5.43	/	/	/
2	Rigid blades + control	11.4	12.1	0.84	683.20	5.30	/	/	/
3	Elastic blades	11.4	12.1	0	714.53	5.25	-0.59	5.70	0.08
4	Elastic blades + control	11.4	11.8	0	706.10	5.19	-0.62	5.65	0.10

489 The azimuthal variations of blade tip deflections over the last revolution are shown as polar-
490 theta plots in **Fig. 16**, where zero theta indicates blade vertical up. The maximum Inp deflections
491 (in terms of magnitude) is observed at theta of 90° where the gravity-induced torque reaches
492 maxima and acts in the same direction of aerodynamic torque, while the minimum Inp is observed
493 at theta of 270° where the maximum gravity-induced torque and aerodynamic torque act in
494 opposite directions.
495



496
497

498 **Fig. 16.** Azimuthal variation of blade tip (a) in-plane (multiplied by -1) and (b) out-of-plane deformations at rated
499 wind speed.

500 Based on the above validations, it is evident that the proposed aero-servo-elastic method is well
501 established and can be further applied for coupled aero-servo-elastic wake behavior analysis of
502 controlled floating offshore wind turbine(s).

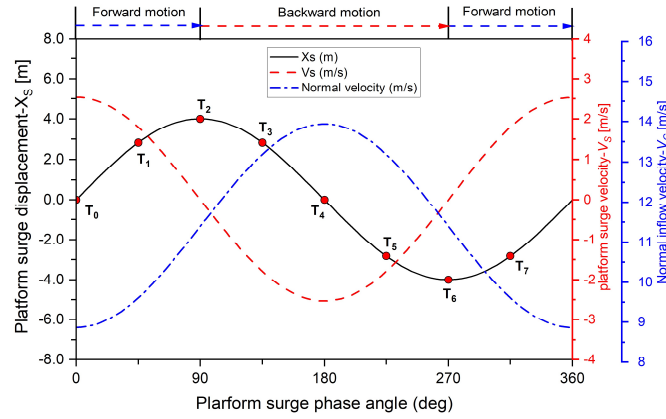
503 4.2 Surge motion cases

504 The platform surge displacement X_S is specified as sinusoidal functions as: $X_S(t) =$
505 $A_S \sin(2\pi f_S t)$ and the platform surge velocity is therefore : $V_S(t) = 2\pi f_S A_S \cos(2\pi f_S t)$, wherein
506 the A_S and f_S are surge amplitude and surge frequency. The normal inflow velocity relative to the
507 rotor plane is $V_c = V_0 - V_S$. To better analyze the coupled aero-servo-elastic responses and wake
508 features of a FOWT under specified surge motions, eight typical time instants ($T_0 \sim T_7$) are
509 introduced (see **Fig. 17**), wherein the platform moves toward downstream from the equivalent
510 position at T_0 to the downstream-most position at T_2 , and then moves back to the equivalent
511 position at T_4 , and further reaches the upstream-most position at T_6 . The minimum and maximum
512 normal inflow velocity V_c happens at T_0 and T_4 , respectively.

513 Following the work of Lienard et al.[54] , the surge frequency f_S is chosen to allow the rotor to
514 rotate two revolutions with rated rotor speed of 12.1 rpm during one surge period, so that the $f_S =$
515 $12.1/120 = 0.10083$ Hz which falls in typical wave frequency range 0.05~2.0 Hz, while a
516 smaller surge amplitude of $A_S = 4m$ is used.

517 In this sub section, four cases (A_1, A_2, A_3 and A_4) are performed to demonstrate the capabilities
518 of the proposed aero-servo-elastic method for fluid-structure interaction simulations of a

519 controlled FOWT under specified surge motion. In case A_3 and A_4 , the rotor speed control is
 520 combined two respective blade pitch control strategies, constant power mode and constant torque
 521 mode, wherein the first one seeks to maintain the rated generator power in region 3 while the
 522 second one maintains rated generator torque in region 3. The detailed case setups are listed in
 523 **Table 8** with additional aims to quantify the effects of blade elasticity and control strategies on the
 524 aero-servo-elastic responses as well as wake characteristics.



525 **Fig. 17.** Platform surge motion description[36].
 526
 527

528 **Table 8** Platform surge motion parameters and rotor operating conditions. The values of V_0 , Ω , θ_p describe the
 529 inflow wind speed, rotor speed and blade pitch angle. The symbols A_s and f_s represent the surge motion amplitude
 530 and frequency.

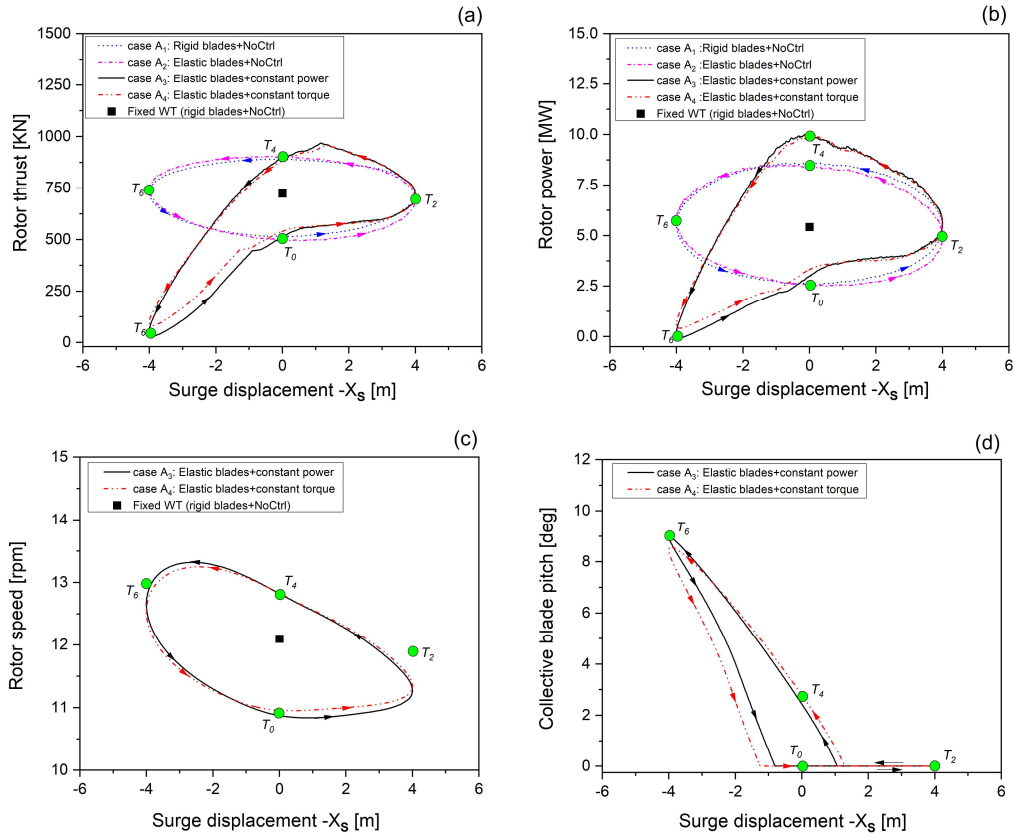
Case	Model	V_0 [m/s]	Ω [rpm]	θ_p [°]	A_s [m]	f_s [Hz]
A_1	Surge + rigid blades	11.4	12.1	0	4	0.10083
A_2	Surge + elastic blades	11.4	12.1	0	4	0.10083
A_3	Surge +elastic blades + constant power	11.4	dynamic	dynamic	4	0.10083
A_4	Surge +elastic blades + constant torque	11.4	dynamic	dynamic	4	0.10083

531 4.2.1 Unsteady loads and responses

532 In **Fig. 18**, the unsteady rotor thrust, rotor power, rotor speed and collective blade pitch angle
 533 are plotted as functions of platform surge displacement. Without control strategy (case A_1 and A_2),
 534 the rotor loads show as elliptical hysteresis loops with the minima and maxima at T_0 and T_4 . For
 535 case A_2 , the thrust varies from 493.64 kN to 904.20 kN while the rotor power varies from 2.50
 536 MW to 8.52 MW. The differences between case A_1 and A_2 are small, indicating limited effects of
 537 blade elasticity on rotor loads. In contrast, the control strategy significantly alters the rotor loads

538 not only in terms of extreme values but also the evolution patterns. For case A_3 , the minimum
539 thrust (31.70 KN) is observed at T_6 as the blade pitch angle reaches maxima of 8.97° while the
540 maximum thrust between T_2 and T_4 of 969.48 KN which coincides with the trigger of blade pitch
541 controller. The constant torque control mode (case A_4) predicts smaller blade pitch angle and thus
542 higher rotor loads from T_6 to T_0 (excluding the range with zero blade pitch) than constant power
543 mode (case A_3), leading to increased mean rotor thrust from 524.56 KN for case A_3 to 539.42 KN
544 for case A_4 . The results show it is important to include control strategies for aerodynamic analysis
545 of floating offshore wind turbines.

546 It should be noted that the blade pitch controller does not trigger and turn off immediately as the
547 rotor speed exceeds or falls below the rated one since the control region identification is based on
548 filtered generator speed ($\tilde{\Omega}_{gen} = N_{Gear}\tilde{\Omega}$) instead of the real-time one which is proportional with
549 real-time rotor speed ($\Omega_{gen} = N_{Gear}\Omega$). For case A_3 from T_2 to T_4 , the rotor speed exceeds rated
550 value at $X_S = 2.45 m$ while the pitch controller starts to function at $X_S = 1.06 m$, corresponding
551 to a surge phase-lag of 22° which is introduced by the low-pass filter (see **Eq. (18)**). The pitch
552 controller is always activated as the pitch angle is positive regardless of the value of filtered
553 generator speed, and the minimum blade pitch angle is set to zero.



554
 555 **Fig. 18.** Rotor performances and controller responses as functions of platform surge displacement: (a) rotor thrust,
 556 (b) rotor power, (c) rotor speed and (d) collective blade pitch angle.

557 Platform surge and pitch motions are greatly influenced by rotor thrust in real applications so
 558 that a deeper investigation on thrust is presented. The unsteady rotor thrust can be approximated
 559 by first-order Taylor expansion as

560
$$T_x = T_0 - \frac{\partial T_x}{\partial V_s} \dot{X}_s \quad (22)$$

561 where T_0 is the reference thrust which is chosen as the mean aerodynamic thrust in this paper. $\frac{\partial T_x}{\partial V_s}$

562 is thrust sensitivity against platform surge velocity which is a direct estimation of the platform
 563 surge aerodynamic damping. For small platform-pitch angles, the platform-pitch damping is

564 $L_{HH}^2 \frac{\partial T_x}{\partial V_s}$ as shown by Jonkman[55] wherein L_{HH} is the vertical distance between hub height and

565 platform-pitch rotational center. The platform-surge aerodynamic damping $\frac{\partial T_x}{\partial V_s}$ can be estimated as

566 the slope of the steady response of $\Delta T = -(T_x - T_0)$ versus V_s as in Fig. 19.

567

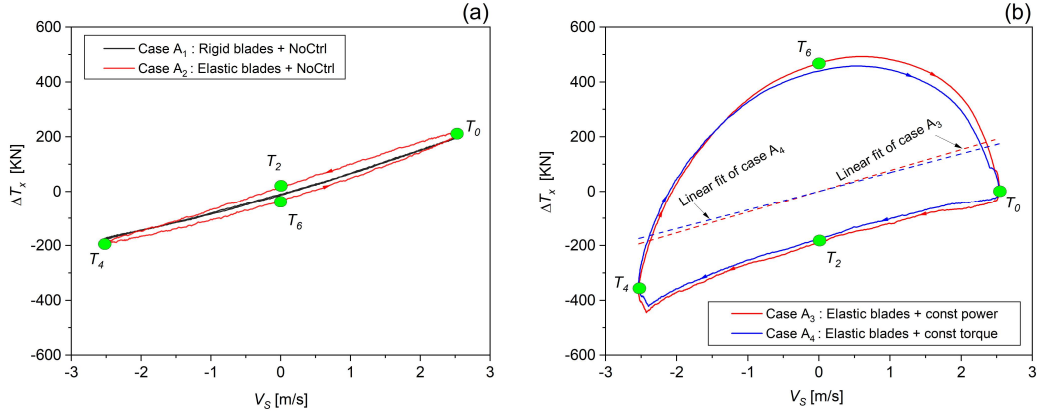


Fig. 19. The oscillating aerodynamic thrust versus platform surge velocity.

An alternative to quantify the aerodynamic damping is using the energy dissipation coefficient following the work of Apsley and Stansby[56] as

$$C_{diss} = -\frac{W_0}{\frac{1}{2}\rho AV_0^2 V_0 T_s} \quad (23)$$

where $A = 0.25\pi D^2$ is rotor sweep area. V_0 is inflow velocity. W_0 is the energy transferred from airflow to the oscillating rotor during one surge period which can be calculated as

$$W_0 = -\int_t^{t+T_s} T_x V_S dt = -\int_{-A_S}^{A_S} (T_x^U - T_x^D) dx_S \quad (24)$$

where T_x^U and T_x^D is the thrust when the rotor moves upstream and downstream, respectively. And $dx_S = V_S dt$. The minus sign is introduced to keep the conventional concept that a system is positively damped if it dissipates energy to airflow. Eq. (24) further indicates the value of W_0 is exactly the area of thrust hysteresis loops in Fig. 19 where counterclockwise loops indicate

positive aerodynamic damping. Combining Eq. (22), (23) and (24), a simple approximate

relationship between C_{diss} and normalized thrust sensitivity ($\frac{\partial C_T}{\partial \bar{V}_S}$) can be derived as

$$C_{diss} = \frac{1}{2} \frac{V_{S0}}{V_0} \frac{\partial C_T}{\partial \bar{V}_S} \quad (25)$$

where $V_{S0} = 2\pi f_S \cdot A_S$ is the maximum surge velocity. $\bar{V}_S = V_S/V_{S0}$ is the normalized surge velocity and $C_T = T_x / \frac{1}{2}\rho AV_0^2$ is thrust coefficient.

The $\frac{\partial T_x}{\partial V_S}$ and C_{diss} are listed in Table 9 together with statistical rotor thrusts and rotor powers.

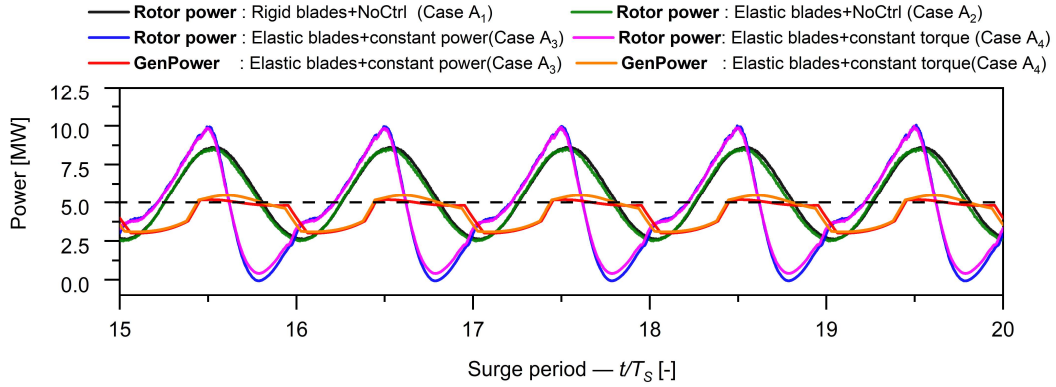
The blade elasticity leads to the increase of $\frac{\partial T_x}{\partial V_S}$ by 7.84% (comparing case A_2 to A_1) while the two

587 control strategies are shown to decrease the $\frac{\partial T_x}{\partial v_s}$ by 3.90% and 13.56% (comparing case A_3 and A_4
588 to case A_2). The relative differences calculated using C_{diss} are not identical with those calculated
589 using $\frac{\partial T_x}{\partial v_s}$ because **Eq. (22)** is just a first-order approximation to rotor thrust so that **Eq. (25)** is
590 only valid for ideal conditions where the oscillating thrust is fully in-phase with surge velocity.
591 Either $\frac{\partial T_x}{\partial v_s}$ or C_{diss} in this paper is just an overall estimation of the rotor aerodynamic damping
592 effects during one surge period while the real aerodynamic damping can vary with time.

593 **Table 9** Rotor thrust, rotor power, thrust sensitivity and rotor energy dissipation coefficient for all cases. Results in
594 parenthesis are relative differences between A_2 to A_1 , and between A_3 and A_4 to A_2 .

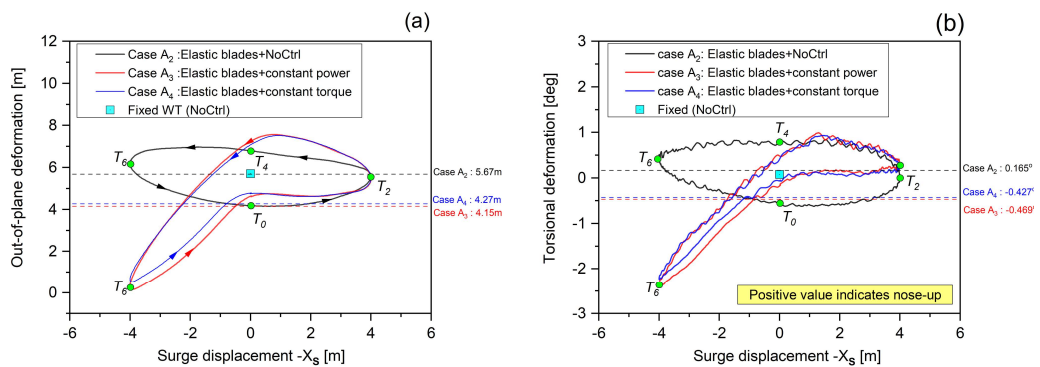
Case	Rotor thrust [KN]			Rotor power [MW]			$\frac{\partial T_x}{\partial v_s}$ [KN/m·s ⁻¹]	C_{diss} [-]
	Min	Max	Mean	Min	Max	Mean		
A_1	514.26	891.96	713.39	2.60	8.61	5.53	73.93	0.02118
A_2	493.64	904.20	709.25	2.50	8.52	5.42	79.73 (+7.84%)	0.02257 (+6.56%)
A_3	31.70	969.48	524.56	-0.06	10.00	4.50	76.62 (-3.90%)	0.02171 (-3.81%)
A_4	81.57	961.31	539.42	0.41	9.95	4.59	68.92 (-13.56%)	0.01952 (-13.51)

595 The rotor power variation range of case A_3 (-0.06 MW to 10.00 MW) is much wider than that
596 of the no-controller case A_2 (2.50MW to 8.52MW), which may be counterintuitive as most control
597 strategies are initially designed to save turbine from overloads at above-rated conditions. To
598 clarify this, the concepts of rotor power and generator power should be distinguished. Rotor power
599 is the energy extracted by rotor from airflow in unit time while generator power is the energy
600 absorbed by generator from rotor in unit time, which are calculated as $P_{rotor} = T_a \Omega$ and $P_{gen} =$
601 $\eta T_{Gen} \Omega_{Gen}$, respectively. The aerodynamic torque T_a is calculated in ACE module while the
602 generator torque T_{Gen} is obtained through the tabulated relationship (see **Fig. 6**). It is the generator
603 power not the rotor power that is directly regulated by control strategies. **Fig. 20** shows the time
604 histories of rotor power and generator power wherein the maximum generator power of case A_3
605 and A_4 are well regulated to near the rated 5.0 MW.
606

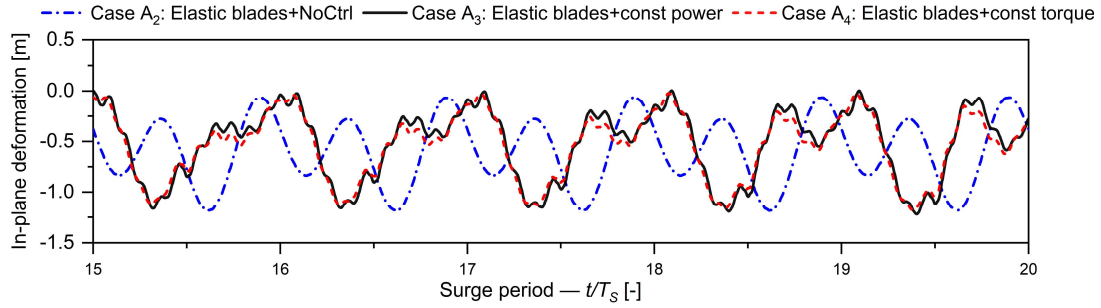


607
608 **Fig. 20.** Time histories of rotor power and generator power.

609 The blade tip out-of-plane (Oop) and torsional deformations show as similar steady hysteresis
610 loops (**Fig. 21**). The minimum Oop and torsional deformations of case A_3 and A_4 are observed at
611 T_6 with minimum rotor thrusts and the aerodynamic center offset induced sectional aerodynamic
612 moments. The mean Oops and blade torsions are consistent with the trends of mean rotor thrust.
613 Moreover, the maximum Oops are 6.96m, 7.56m and 7.52m for cases A_2 , A_3 and A_4 , indicating
614 the surge motion and control strategies can possibly lead to stronger blade-tower interactions and
615 increase the risks of tower strike. The high-frequency oscillations in **Fig. 21(b)** relate to the
616 natural frequencies of blade torsion. The blade tip in-plane deformations with control strategies
617 are not strictly periodic steady so that they are plotted as time histories in **Fig. 22**. It shows the
618 control strategies only slightly influence the maximum and minimum in-plane deformations but
619 allow for the high-order frequencies to be excited.



620
621 **Fig. 21.** Blade tip out-of-plane and torsional deformations as functions of platform surge displacement: (a) Out-of-
622 plane deformation and (b) blade torsion.

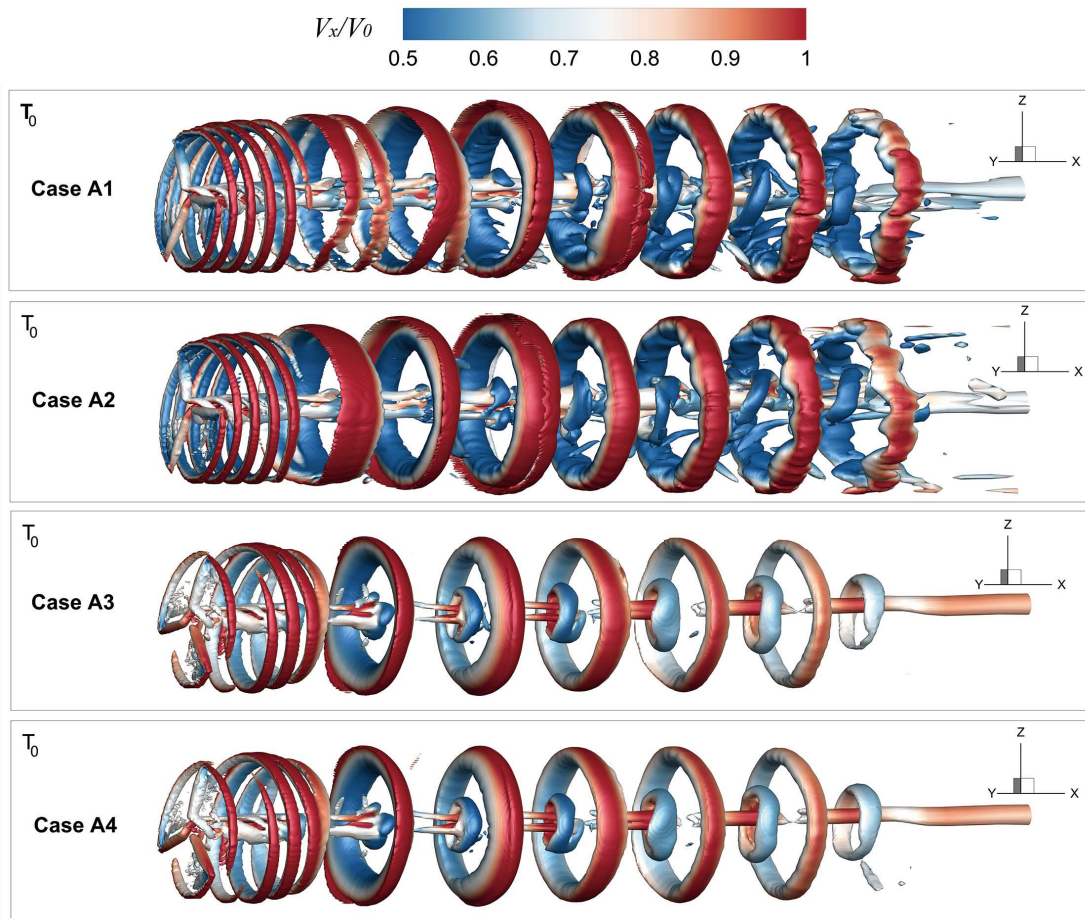


623
624
625

Fig. 22. Time histories of blade tip in-plane deformations.

626 4.2.2 Wake characteristics

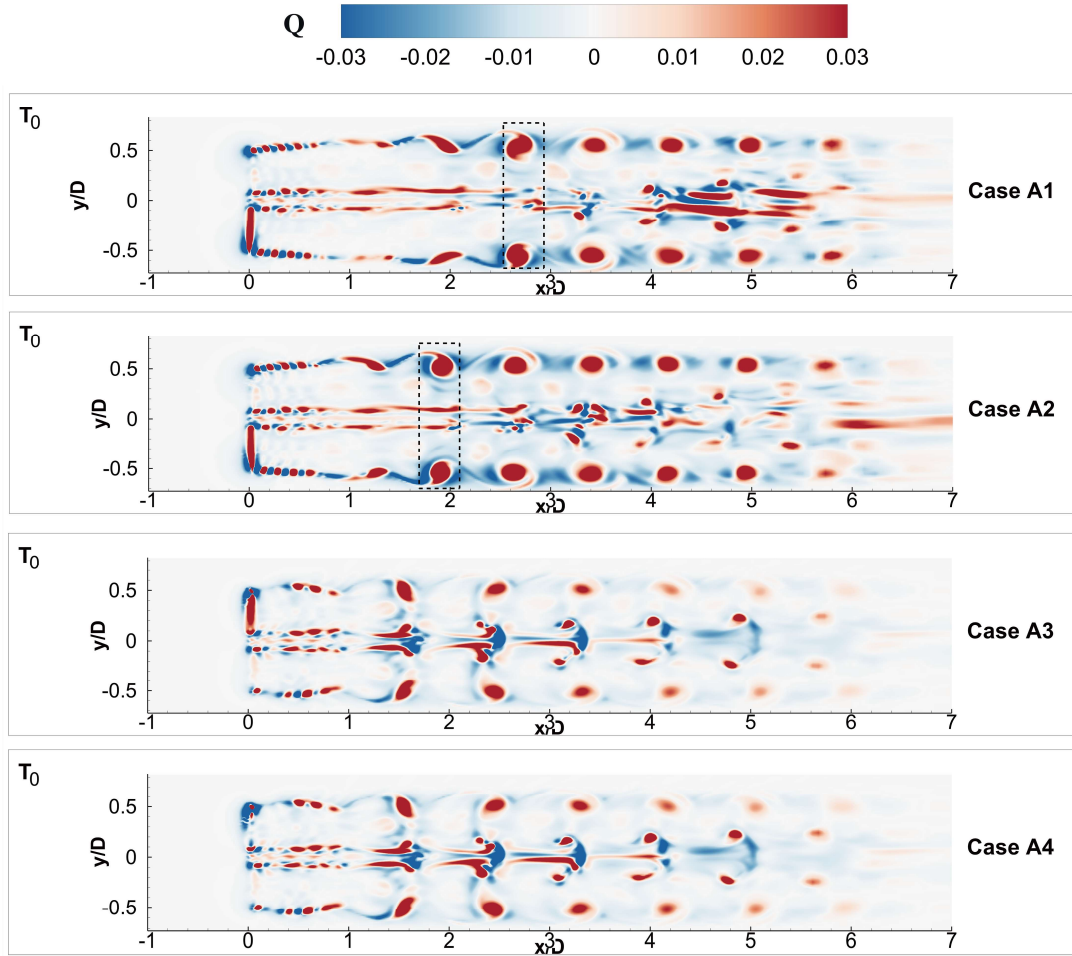
627 In **Fig. 23**, the wake vortex structures are visualized via the Q -criterion[57, 58], wherein Q is
628 the second invariant of velocity gradient tensor and is defined as $Q = -\frac{1}{2}(u_{i,i}^2 - u_{i,j}u_{j,i})$. The
629 helical tip vortices in near wake regions are transitioned and merged into a set of vortex rings.
630 This is due to the fact that the tip vortices are released from blade tips with different initial
631 streamwise velocities during one surge period, so that the vortex-vortex interactions lead to the
632 formation of vortex rings. The vortex ring structures are greatly impacted by the inclusion of
633 control strategies while the differences between two control types are negligible. **Fig. 24** shows
634 blade elasticity accelerates the formation of the first vortex ring structure (see the dashed boxes in
635 **Fig. 24 (a) and (b)**) due to blade elastic vibrations, and control strategies significantly reduce the
636 core size of the vortex rings. The rapid diffusion of wake vortices in far wake is due to the large
637 streamwise grid resolution as the wake refinement region only extends to $5D$ downstream.
638



639

640

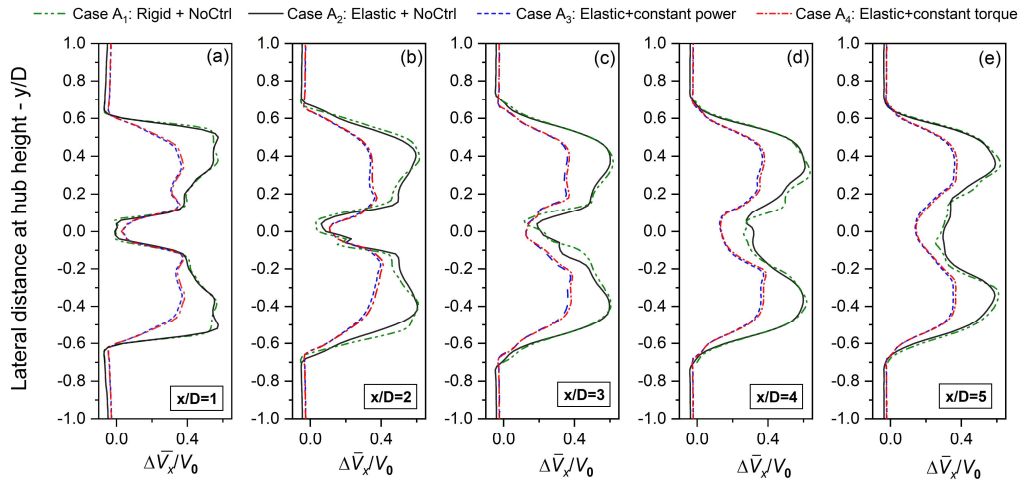
Fig. 23. Wake vortex structures at T_0 (visualized by $Q = 0.01$ and colored by normalized streamwise velocity).



641
642 **Fig. 24.** Instantaneous Q field in xy plane at hub height at T_0 for cases A_1 , A_2 , A_3 and A_4 .

643 To further quantify the wake characteristics, the streamwise velocities (V_x) are probed along
644 lateral directions (y direction) at hub height ($Z=90\text{m}$) for five downstream positions $x/D =$
645 $1, 2, 3, 4, 5$ and time averaged over the last two surge periods (19-20) in order to calculate the
646 dimensionless mean velocity deficits as $\frac{\Delta \bar{V}_x}{V_0} = 1 - \frac{\bar{V}_x}{V_0}$ which are plotted in **Fig. 25**. The $\frac{\Delta \bar{V}_x}{V_0}$ at
647 $x/D = 5$ is further transversely averaged between $-0.7 < y/D < 0.7$ to assess the overall far
648 wake deficit and the results for case A_1 , A_2 , A_3 and A_4 are 0.370, 0.360, 0.223 and 0.232,
649 respectively. The overall far wake deficit is decreased by 2.7% from 0.370 for case A_1 to 0.360 for
650 case A_2 , showing the blade elasticities slightly accelerate the far wake recovery. While the two
651 control strategies were found to significantly reduce the overall far wake deficits by 38.1% and
652 35.6% comparing case A_3 and A_4 to case A_2 . This is because the minimum blade pitch angle is set
653 as 0° for the current pitch controller. When the filtered rotor speed is above rated value, the pitch

654 controller introduces positive pitch angle and thus less power extraction. However, when the
 655 filtered rotor speed is below the rated value, the blade pitch angle is maintained as 0° instead of
 656 being negative ones to increase power extraction. As a result, the rotor extracts much less energy
 657 during one surge period compared with the no controller cases, thus the mean wake deficits are
 658 greatly reduced.



659
 660 **Fig. 25.** Time-averaged dimensionless streamwise velocity deficit ($\Delta\bar{V}_x/V_0$) at hub height along lateral direction at
 661 downstream positions of $x/D = 1, 2, 3, 4, 5$.

662 5. Summary and conclusion

663 In this paper, a new numerical method is proposed for coupled aero-servo-elastic analysis of
 664 floating offshore wind turbines including wakes. The method is based on solving incompressible
 665 Navier-Stokes Equations on stretching Cartesian grids using finite difference method. The effects
 666 of rotating blades on flow are modeled as body force terms using Actuator Curve Embedding
 667 method and a nonlinear finite element rotating beam theory is adopted for blade elasticity
 668 modeling. Both generator torque control and collective blade pitch control strategy are considered.
 669 The novelty of this method is that it is capable for efficient aero-servo-elastic simulations of
 670 FOWT(s) including wakes on coarse Cartesian grids without requiring empirical tip loss
 671 corrections. Systematic validations were performed and the aero-servo-elastic loads and wakes of
 672 a NREL 5MW wind turbine under platform surge motion were analyzed using the current new

673 [method](#). The major conclusions are as follows:

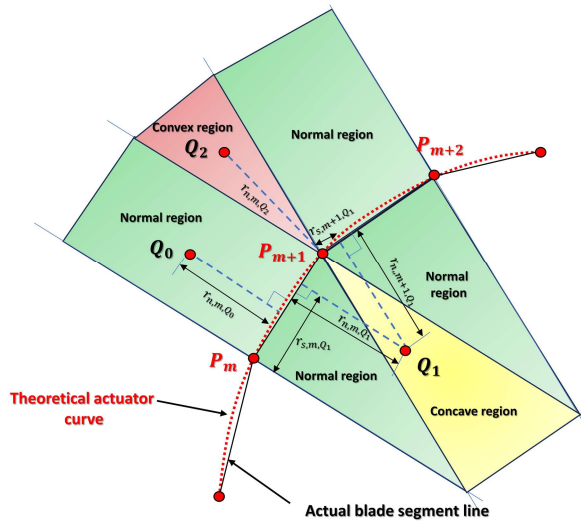
- 674 ● The proposed aero-servo-elastic method is well-established and can accurately predict
675 rotor performances, blade natural frequencies, modal shapes, elastic deformations and
676 controller responses with affordable computational cost.
- 677 ● Aerodynamic center offset effects are essential for accurate [aero-servo-elastic predictions](#).
678 [If neglected, the magnitude of blade tip torsion can be overpredicted by 26.1 times](#)
679 [\(comparing \$-2.71^\circ\$ to \$0.1^\circ\$ \) for the NREL 5MW wind turbine at rated conditions, leading](#)
680 [to the rotor thrust and blade tip out-of-plane deformation being underpredicted by 10.5%](#)
681 [and 15.0%, respectively.](#)
- 682 ● [The two control strategies \(constant power and constant torque mode\) were found to](#)
683 [significantly reduce the overall far wake deficit by 38.1% and 35.6%, while blade](#)
684 [elasticity only slightly reduces the same quantity by 2.7%. These findings indicate control](#)
685 [strategy can be much more influential than blade elasticity on far wake deficits for a](#)
686 [floating turbine in particular conditions.](#)
- 687 ● [Compared with the constant power mode, the pitch control with constant torque mode](#)
688 [slightly increased the overall far wake deficit by 4.0% which may have some negative](#)
689 [effects on downstream turbines.](#)

690 In future works, [more modern IEA 15MW wind turbine, which have more realistic offsets](#)
691 [and more significant blade torsional deformations, should be used and the method can be](#)
692 [coupled](#) with a floating body dynamic code to establish an aero-hydro-servo-elastic-mooring
693 framework with inflow shear and turbulence [in more realistic sea states](#). With the increasing
694 tower height and blade span, the inclusion of tower flexibility and modeling flexible blades with
695 more advanced geometrically exact beam theory are natural next-steps. Due to the cyclic nature
696 of blade pitch angles and blade elastic vibrations, it would be beneficial to include a dynamic
697 stall model to account for the hysteresis effects of sectional aerodynamic coefficients, especially
698 for a FOWT operating in harsh sea states or wind gust conditions where the current quasi-
699 steady aerodynamic model may fail to predict the high dynamic lift due to the delay of stall.

700

701 **Appendix A. Special region treatment in ACE**

702 Theoretically, the blade forces are projected in planes normal to the actuator curve. However,
 703 the actuator curve is usually represented by a set of discrete segment lines instead of a smooth
 704 curve. Thus, the fluid region influenced by an actuator curve can have two special regions on the
 705 concave and convex side, which are donated as concave region and convex region with the rest
 706 region donated as normal region as shown in **Fig. A.1**. In the concave region, a fluid point can find
 707 multiple planes normal to the blade segment lines. While for the fluid points in the convex region,
 708 no plane normal to any blade segment line can be find. Therefore, special treatments are required
 709 in the two special regions to obtain the required tangential and normal coordinates (r_s and r_n) for
 710 ACE implementation. The tangential coordinate is normalized as $p_{s,m} = r_{s,m}/\Delta b_m$ for better
 711 following discussions.



712

713

Fig. A.1 Concave and convex cases for ACE.

714

In HEU-FOWT code, the region type identification and the treatments for each fluid point Q
 715 based on the following criterions:

716

- If only **ONE** element satisfies $p_s \in (0,1]$ (see Q_0 in **Fig. A.1**), then Q is in the normal region, and the r_s and r_n are calculated using this element;

717

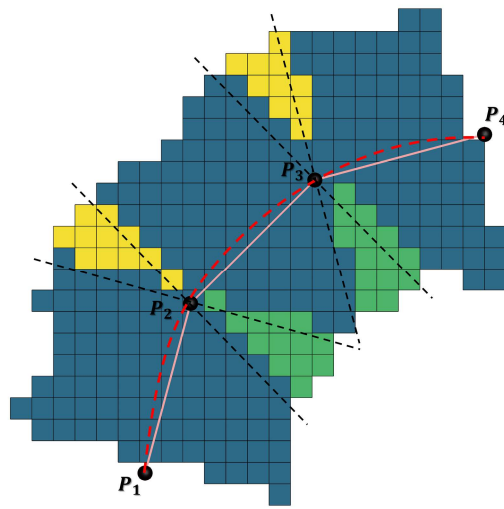
718

- If **Multiple** elements satisfy $p_s \in (0,1]$ (see Q_1 in **Fig. A.1**), then Q is in the concave region, but only the element with minimum r_n is used for calculating the final r_s and r_n ;

719

720 ● If $p_s \notin (0,1]$ for all elements but have the properties of $p_{s,m} > 1$ and $p_{s,m+1} \leq 0$ (see Q_2
 721 in **Fig. A.1**), then Q is in convex region, and it is associated with the m^{th} element. Special
 722 treatments are performed as $p_s = 1.0$ and $r_n = \overline{Q_2 P_{m+1}}$.

723 A uniformly-loaded (1000N/m) quarter arc curve with four actuator points is used to
 724 demonstrate the capabilities of the ACE method in HEU-FOWT code on treating the special
 725 regions for an actuator curve with very large deformation. The different regions are well identified
 726 (primary values located at the lowest-indexed cell corners) as shown in **Fig. A.2**, and the projected
 727 body force field (with $\varepsilon = \Delta = 4\text{m}$) in different regions are shown in **Fig. A.3**.



728

729

730

731

Fig. A.2 Region identification for a quarter arc.
 (Yellow: convex region; Green: convex region; Blue: normal region)

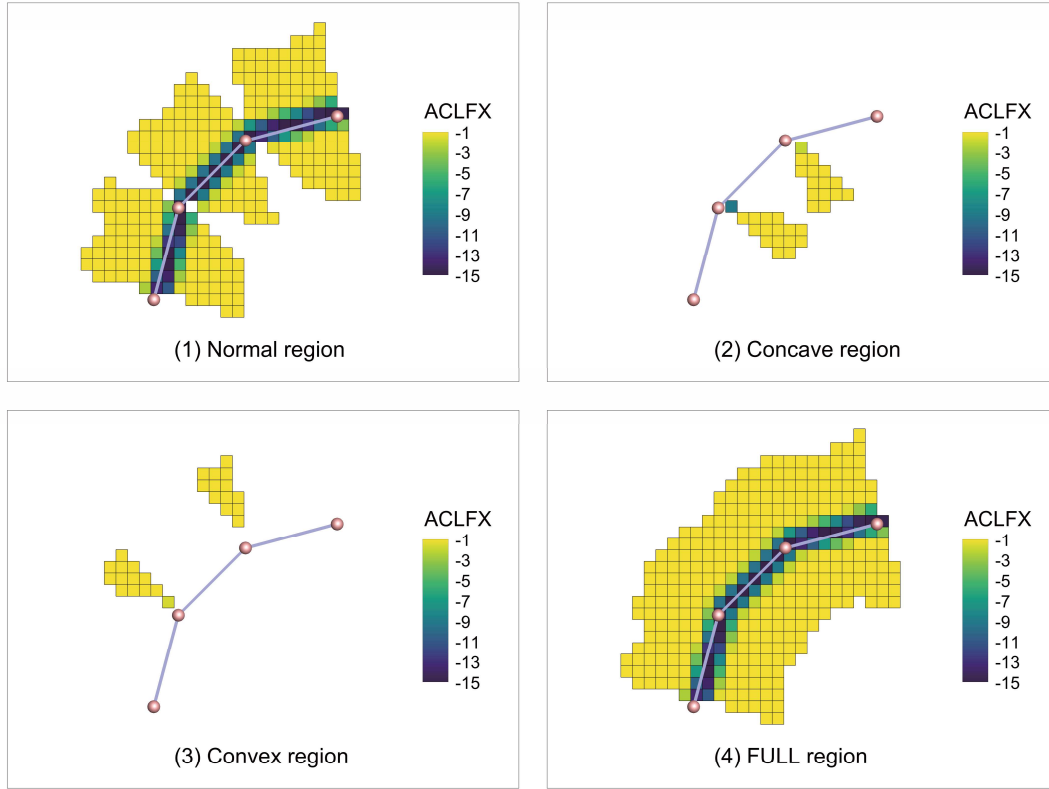


Fig. A.3 Body force field in different regions.

Appendix B. Derivation of the elemental equations of motion

The equation of motion for each blade beam element is derived using Lagrange's equations,

$$-\frac{d}{dt} \left(\frac{\partial \mathcal{T}}{\partial \dot{q}_i} \right) + \frac{\partial \mathcal{T}}{\partial q_i} + \frac{\partial \mathcal{U}}{\partial q_i} = \mathcal{P}_i \quad (\text{B.1})$$

where \mathcal{T} is total kinetic energy, \mathcal{U} is total strain energy, q_i is the i^{th} degree-of-freedom and \mathcal{P}_i is the i^{th} generalize load of the finite beam element. \dot{q}_i is the time derivative of q_i .

The total kinetic energy (\mathcal{T}) of a Euler-Bernoulli beam element is simply

$$\mathcal{T} = \frac{1}{2} \int_0^L \rho_s A (\dot{u}_x^2 + \dot{u}_y^2 + \dot{u}_z^2) dx + \frac{1}{2} \int_0^L \rho_s J \dot{\theta}_x^2 dx \quad (\text{B.2})$$

where ρ_s is mass density, A is cross-sectional area and J is rotational inertial around local x axis.

The total strain energy (\mathcal{U}) due to axial displacement, transverse bending, torsion and axial tension can be modelled using classic rod, beam, shaft and pretensioned cable models as

744
$$u = \frac{1}{2} \int_0^L EA \left(\frac{\partial u_x}{\partial x} \right)^2 dx + \frac{1}{2} \int_0^L GJ \left(\frac{\partial \theta_x}{\partial x} \right)^2 dx + \frac{1}{2} \int_0^L \left[EI_z \left(\frac{\partial^2 u_y}{\partial x^2} \right)^2 + EI_y \left(\frac{\partial^2 u_z}{\partial x^2} \right)^2 \right] dx + \frac{1}{2} \int_0^L T_x^* \left[\left(\frac{\partial u_y}{\partial x} \right)^2 + \left(\frac{\partial u_z}{\partial x} \right)^2 \right] dx \quad (\text{B.3})$$

745 where E is elastic modulus, G is shear modulus, I_y and I_z is the cross-section moment of inertial
 746 relative to local y and z axis, respectively. T_x^* is axial tension mainly resulting from the
 747 accumulated centrifugal force and gravitational force as

748
$$T_x^*(x) = \int_x^R \rho_s A [\boldsymbol{\Omega} \boldsymbol{\Omega} (\mathbf{r} + \mathbf{x}^e) + \mathbf{T} \mathbf{g}] dx \cdot [1, 0, 0]^T \quad (\text{B.4})$$

749 where \mathbf{T} is the coordinate transformation matrix from global inertial coordinate system to local
 750 blade coordinate system. $\mathbf{g} = [0, 0, -9.81]^T$ is gravitational acceleration. \mathbf{r} and \mathbf{x}^e are the initial
 751 undeformed elemental nodal position and elemental nodal elastic deformation, respectively. The
 752 dot product with vector $[1, 0, 0]^T$ is intended to extract the x component. $\boldsymbol{\Omega}$ is the skew symmetric
 753 matrix of the rotor angular speed vector $\boldsymbol{\omega} = [\omega_x, \omega_y, \omega_z]^T$ (represented by S_{b_i}) as

754
$$\boldsymbol{\Omega} = \begin{bmatrix} 0 & -\omega_z & \omega_y \\ \omega_z & 0 & -\omega_x \\ -\omega_y & \omega_x & 0 \end{bmatrix} \quad (\text{B.5})$$

755
 756 Inserting **Eq. (16), (B.2) and (B.3)** into **Eq. (B.1)**, the elemental mass matrix (\mathbf{M}^e), structural
 757 stiffness matrix (\mathbf{K}^e) and stress stiffening matrix (\mathbf{stress}^e) can be obtained as

758
$$\mathbf{M}^e = \int_0^L \rho_s A \mathbf{N}_0^T \mathbf{N}_0 dx + \int_0^L J \mathbf{N}_4^T \mathbf{N}_4 dx \quad (\text{B.6})$$

759
$$\mathbf{K}^e = \int_0^L \mathbf{B}_0^T \mathbf{S} \mathbf{B}_0 dx \quad (\text{B.7})$$

760
$$\mathbf{stress}^e = T_x^* \int_0^L \mathbf{B}_1^T \mathbf{B}_1 dx \quad (\text{B.8})$$

761 where $\mathbf{B}_0 = [\mathbf{N}'_1, \mathbf{N}''_2, \mathbf{N}''_3, \mathbf{N}'_4]^T$ is strain matrix. $\mathbf{B}_1 = [\mathbf{N}'_2, \mathbf{N}'_3]^T$. \mathbf{S} is the 4×4 matrix of structural
 762 properties. $N'_1 = \partial \mathbf{N}_1 / \partial x$ and $N''_3 = \partial^2 \mathbf{N}_3 / \partial x^2$.

763 Wind turbine blades are subjected to various applied loads, such as gravitational force,
 764 centrifugal force, Coriolis force, aerodynamic loads. Using the principle of virtual work, these
 765 applied loads can be generalized as

766
$$\mathbf{F}_g^e = \int_0^L \rho_s A N_0^T dx \cdot \mathbf{T} \mathbf{g} \quad (\text{B.9})$$

767
$$\mathbf{F}_{cen}^e = - \int_0^L \rho_s A N_0^T \boldsymbol{\Omega} \boldsymbol{\Omega} N_0 (x_e + \mathbf{q}^e) dx = \mathbf{F}'_{cen} + \mathbf{soft}^e \cdot \mathbf{q}^e \quad (\text{B.10})$$

768
$$\mathbf{F}_{cori}^e = -2 \int_0^L \rho_s A N_0^T \boldsymbol{\Omega} N_0 dx \cdot \dot{\mathbf{q}}^e = \mathbf{cori}^e \cdot \dot{\mathbf{q}}^e \quad (\text{B.11})$$

769
$$\mathbf{F}_{aero}^e = \int_0^L \mathbf{N}^T \mathbf{N} \mathbf{F}_a^e dx \quad (\text{B.12})$$

770 where $\mathbf{F}_a^e = [\mathbf{F}_x^1, \mathbf{F}_y^1, \mathbf{F}_z^1, \mathbf{M}_x^1, \mathbf{0}, \mathbf{0}, \mathbf{F}_x^2, \mathbf{F}_y^2, \mathbf{F}_z^2, \mathbf{M}_x^2, \mathbf{0}, \mathbf{0}]^T$ is the elemental nodal
 771 aerodynamic loads. \mathbf{soft}^e and \mathbf{cori}^e are spin softening matrix and generalized Coriolis matrix,
 772 respectively.

773 Combining the structure related and rotation added terms, the final form of the discretized
 774 equation of motion for a rotating blade element is

775
$$\mathbf{M}^e \ddot{\mathbf{q}}^e + (\mathbf{C}_0^e - \mathbf{Cori}^e) \dot{\mathbf{q}}^e + (\mathbf{K}^e + \mathbf{stress}^e - \mathbf{soft}^e) \mathbf{q}^e = \mathbf{F}_g^e + \mathbf{F}_{aero}^e + \mathbf{F}'_{cen} \quad (\text{B.13})$$

776 where \mathbf{C}_0^e is the structural damping matrix.

777 For the current aero-servo-elastic method, direct usage of partitioned loose-coupling strategy
 778 with elastic velocity feedbacks can lead to numerical instabilities, so that a structural damping is
 779 adopted to maintain numerical stability where the sectional damping force (\mathbf{F}^{Damp}) is proportional
 780 to the strain rate ($\dot{\boldsymbol{\epsilon}}$) as

781
$$\mathbf{F}^{Damp} = \boldsymbol{\mu}_i \mathbf{S} \dot{\boldsymbol{\epsilon}} \quad (\text{B.14})$$

782 The damping forces can be generalized using the principle of virtual work and lead to a stiffness
 783 proportional damping matrix for blade beam element as

784
$$\mathbf{C}_0^e = [\boldsymbol{\mu}] \mathbf{K}^e \quad (\text{B.15})$$

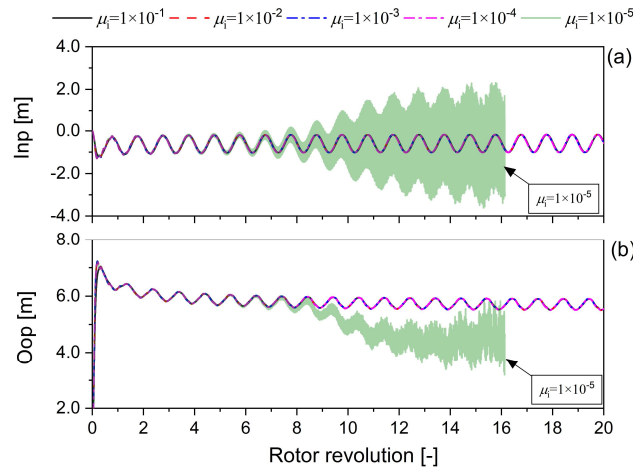
785 where $[\boldsymbol{\mu}] = \text{diag}(\mu_1, \mu_2, \mu_3, \mu_4)$ is the diagonal damping coefficient matrix. \mathbf{C}_0^e is the structural
 786 damping matrix for a beam element. \mathbf{S} is 4×4 sectional stiffness property matrix which is
 787 detailed in **Appendix C**.

788 The damping coefficient μ_i is determined based on the analysis of a single DOF mass-damper-
 789 spring system: $m_i \ddot{x} + c_i \dot{x} + k_i x = 0$, of which the circular natural frequency is $\omega_i = 2\pi f_i =$

790 $\sqrt{k_i/m_i}$ and critical damping is $c_i^0 = 2\sqrt{m_i k_i}$. Therefore, the damping ratio (ξ_i) for a stiffness
 791 proportional damping ($c_i = \mu_i k_i$) is

$$792 \quad \xi_i = \frac{c_i}{c_i^0} = \frac{\mu_i}{2} \sqrt{\frac{m_i}{k_i}} = \frac{1}{2} \mu_i \omega_i = \pi \mu_i f_i \quad (\text{B. 16})$$

793 **Eq. (B. 16)** reveals the damping coefficient for each degree-of-freedom of a flexible blade can
 794 be determined by giving a target damping ratio ξ_i for the target i^{th} mode with natural frequency
 795 f_i (HZ), for which the high modes are more damped. In this paper, the damping ratio coefficients
 796 of $\mu_i = 1 \times 10^{-3}$ are adopted following the default values in FAST BeamDyn. **Fig. B.1** shows the
 797 aeroelastic responses of a NREL 5MW wind turbine at rated conditions ($V_0=11.4\text{m/s}$, $\Omega =$
 798 12.1 rpm , $\theta_p = 0$), wherein the value of $\mu_i = 1 \times 10^{-5}$ leads to case blow up while other values
 799 of μ_i predict very similar results.



800
 801 **Fig. B.1** Blade tip deflections with different damping ratio coefficients μ_i : (a) In-plane deformations, (b) Out-of-
 802 plane deformations.

803 Appendix C. Bend-Bend coupling

804 In current method, blade elastic deformations are solved in the blade coordinate system for each
 805 blade, while the input sectional stiffness properties, such as flap stiffness and edge stiffness, are
 806 defined respective to the sectional principal axis (See **Fig. 2** for the edgewise and flapwise
 807 definitions). In sectional coordinate system, the force-deformation relationship has a simple form
 808 of

809
$$\begin{bmatrix} F_{y_p} \\ F_{z_p} \end{bmatrix} = \begin{bmatrix} K_{y_p} & 0 \\ 0 & K_{z_p} \end{bmatrix} \begin{bmatrix} u_{y_p} \\ u_{z_p} \end{bmatrix} \quad (\text{C.1})$$

810 where K_{y_p} and K_{z_p} are sectional edgewise and flapwise stiffnesses. The $[u_{y_p} \ u_{z_p}]^T$ and
 811 $[F_{y_p} \ F_{z_p}]^T$ are respective elastic deformation vectors and force vectors in sectional coordinate
 812 system.

813 **Eq. (C.1)** can be further represented in the blade coordinate system as

814
$$\mathbf{T}_1 \begin{bmatrix} F_y \\ F_z \end{bmatrix} = \begin{bmatrix} K_{y_p} & 0 \\ 0 & K_{z_p} \end{bmatrix} \mathbf{T}_1 \begin{bmatrix} u_y \\ u_z \end{bmatrix} \quad (\text{C.2.a})$$

815 or equivalently

816
$$\begin{bmatrix} F_y \\ F_z \end{bmatrix} = \mathbf{T}_1^T \begin{bmatrix} K_{y_p} & 0 \\ 0 & K_{z_p} \end{bmatrix} \mathbf{T}_1 \begin{bmatrix} u_y \\ u_z \end{bmatrix} = [\tilde{\mathbf{K}}] \begin{bmatrix} u_y \\ u_z \end{bmatrix} \quad (\text{C.2.b})$$

817 where \mathbf{T}_1 is the coordinate transformation matrix as shown in **Eq. (C.3)** and θ is the total angle
 818 from rotor plane to sectional chord line, namely, $\theta = \theta_0 + \theta_p - \theta_e$ (See **Fig. 2**).

819
$$\mathbf{T}_1 = \begin{bmatrix} \cos(\theta) & \sin(\theta) \\ -\sin(\theta) & \cos(\theta) \end{bmatrix} \quad (\text{C.3})$$

820 The sectional stiffness property matrix $[\tilde{\mathbf{K}}]$ in blade coordinate system is therefore

821
$$[\tilde{\mathbf{K}}] = \begin{bmatrix} K_{yy} & K_{yz} \\ K_{yz} & K_{zz} \end{bmatrix} \quad (\text{C.4})$$

822 where

823
$$K_{yy} = K_{y_p} \cos^2(\theta) + K_{z_p} \sin^2(\theta) \quad (\text{C.5.a})$$

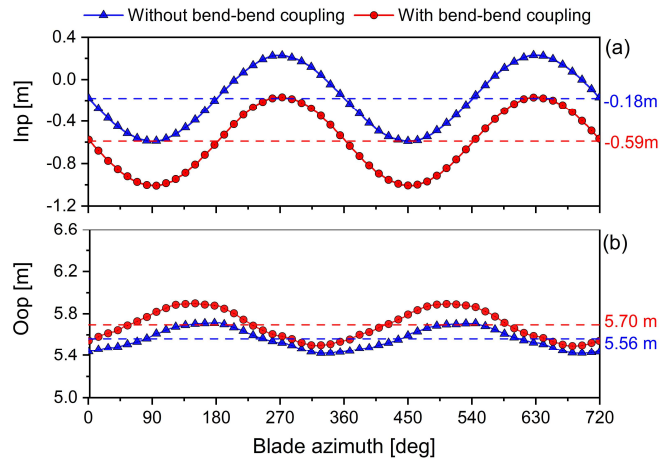
824
$$K_{zz} = K_{y_p} \sin^2(\theta) + K_{z_p} \cos^2(\theta) \quad (\text{C.5.b})$$

825
$$K_{yz} = (K_{y_p} - K_{z_p}) \sin(\theta) \cos(\theta) \quad (\text{C.5.c})$$

826 The off-diagonals of $[\tilde{\mathbf{K}}]$ are usually non-zero which introduce bend-bend coupling effects
 827 unless $K_{y_p} = K_{z_p}$ or $\theta = 0$. Considering blade elongation and torsion, the total sectional structural
 828 stiffness property matrix in blade coordinate system is

829
$$\mathbf{S} = \begin{bmatrix} EA & 0 & 0 & 0 \\ 0 & K_{yy} & K_{yz} & 0 \\ 0 & K_{yz} & K_{zz} & 0 \\ 0 & 0 & 0 & GJ \end{bmatrix} \quad (\text{C.6})$$

830 The bend-bend coupling effects on blade tip deflections are shown in **Fig. C.1**, wherein the
 831 mean in-plane deformation is substantially decreased by 227.8% while the mean out-of-plane
 832 deformation is slightly increased by 2.5% for a NREL 5MW wind turbine at rated conditions
 833 ($V_0=11.4\text{m/s}$, $\Omega = 12.1 \text{ rpm}$, $\theta_p = 0^\circ$). Bend-bend coupling effects are essential for accurate
 834 predictions of blade in-plane deformation.



835

836

Fig. C.1 Bend-bend coupling effects on blade tip deflections: (a) In-plane deformations, (b) Out-of-plane

837

deformations (Dashed lines indicate mean values).

838

Nomenclature

D	Rotor diameter [m]	ρ_a	Air density [kg/m^3]
R	Rotor radius[m]	F_i	Body force term [N/m^3]
r	Blade radial distance[m]	V_0	Streamwise inflow velocity [m/s]
H	Hub height [m]	Ω, Ω_0	Rotor speed, Rated rotor speed [rpm]
N_b	Number of blades	\bar{c}	Averaged blade chord
Δ_{min}	Finest grid resolution	c	Chord length [m]
S_0	Global inertial coordinate system	S_t, S_h	Hub, tower coordinate system
S_{b_i}	Blade coordinate system for the i^{th} blade	$S_{c_{i,j}}$	Section coordinate system for the j^{th} section of i^{th} blade
O_0, O_1, O_2	Origin of S_0, S_t, S_h	θ	Blade azimuth angle [$^\circ$]
c_0	Root chord of equivalent elliptic wing[m]	c^*	chord of equivalent elliptic wing[m]
F_L, F_D	Lift and Drag forces [N/m]	V_{rel}	Sectional relative velocity [m/s]
M_{AC}, M_{PA}	Sectional aerodynamic moments against aerodynamic center and pitch axis position [Nm/m]	V_{in}	flow velocity at aerodynamic center[m/s]
$V_{platform}$	Sectional velocity due to platform motions [m/s]	V_{rot}	Sectional velocity at aerodynamic center due to blade rotation [m/s]
C_L, C_D, C_M	lift, drag and moment coefficients [-]	$V_{elastic}$	Sectional elastic velocity [m/s]
F_T, F_N	Tangential and normal force [N/m]	α	Effective angle of attack [$^\circ$]
ε	Gaussian smooth length scale [m]	ϕ	Inflow velocity angle [$^\circ$]
ε^*	Interpolated Gaussian length [m]	θ_0	Initial twist angle [$^\circ$]
r_s, r_n	Tangential and normal distance [m]	θ_p	Blade pitch angle [$^\circ$]
Δb	Elemental length [m]	θ_e	Elastic torsional angle [$^\circ$]
$f_{k,m,i}$	i^{th} component of total aerodynamic force at m^{th} section of k^{th} blade [N/m]	$f_{k,i}^*$	i^{th} component of the interpolated total aerodynamic force from k^{th} blade [N/m]
N_0, N_1, N_2, N_3, N_4	Strain matrices	ω	Blade angular velocity vector in S_{b_i}
$\eta_i(x)$	Shape functions	$\omega_x, \omega_y, \omega_z$	x, y, z components of blade angular velocity ω
L	Blade beam element length [m]	Ω	skew symmetric rotational matrix
q^e	Beam element displacement	${}^{b_i}e_x, {}^{b_i}e_y,$	Vectors in local x, y, z directions of blade coordinate system S_{b_i}
u_x, u_y, u_z	Translational displacements in local x, y, z directions of elastic beam structure [m]	\mathcal{T}	Total kinetic energy
$\dot{u}_x, \dot{u}_y, \dot{u}_z$	Translational velocities in local x, y, z directions of elastic beam structure [m/s]	\mathcal{U}	Total strain energy
$\theta_x, \theta_y, \theta_z$	Rotational displacements in local x, y, z directions of elastic beam structure [m]	\mathcal{P}	Generalized applied load
$\dot{\theta}_x, \dot{\theta}_y, \dot{\theta}_z$	Rotational velocities in local x, y, z directions of elastic beam structure [rad/s]	ρ_s	Mass density of blade section [kg/m^3]
T_x^*	Axial tension force [N]	A	Cross-sectional aero [m^2]
\mathbf{T}	Coordinate transformation matrix from global to local blade coordinate system.	J	Sectional rotational inertial
\mathbf{g}	Gravitational acceleration [m^2/s]	E	Elastic modulus
\mathbf{r}	Initial radial position	G	Shear modulus
\mathbf{x}^e	Elastic deformations of beam element	I_y, I_z	sectional moment of inertial relative to local y and z directions
M^e, C_0^e, K^e	Elemental mass, damping, and stiffness matrices.	F_{aero}^e	Generalized aerodynamic loads.
		F_g^e	Generalized gravity loads

$stress^e$	Stress stiffening matrix	F_{cen}^e	Generalized centrifugal force
$soft^e$	Spinning softening matrix	F_{cori}^e	Generalized Coriolis force
$cori^e$	Coriolis force matrix	F_a^e	Elemental aerodynamic loads
S	Structural property matrix	M_E, C_E, K_E	Assembled mass, damping and stiffness matrices for each blade
B_0, B_1	Strain matrices	F_E	Assembled generalized loads for each blade
$\tilde{\Omega}$	Filtered rotor speed	$\Delta\Omega$	Rotor speed error
f_c	Corner frequency for low pass filter	ξ_i	Damping ratio
T_a	Rotor aerodynamic torque [Nm]	$[\mu]$	Damping coefficient matrix
T_{Gen}	Generator torque [Nm]	K_{y_p}, K_{z_p}	Sectional edgewise and flapwise stiffness.
I_D	total drivetrain inertial cast to the LSS	ω	Circular natural frequency [-]
$\dot{\Omega}$	Rotor acceleration	γ	Dimensionless rotor speed
N_{Gear}	Gear box ratio	μ	Dimensionless natural frequency
P_{Gen}, P_{aero}	Generator power and rotor power [W]	χ_s	Surge displacement [m]
η	Generator electric efficiency	V_s	Surge velocity [m/s]
K_{p_0}, K_{i_0}	Proportional and integral gains at zero pitch angle	f_s	Surge frequency [HZ]
θ_k	blade pitch angle where the power sensitivity is doubled.	V_c	Normal inflow velocity relative to rotor plane
T_0	Reference rotor thrust [N]	T_x	Rotor thrust in global coordinate system [N]
C_{diss}	Energy dissipation coefficient [-]	W_0	Energy transferred from air flow to surging rotor in one surge period
FOWT	Floating Offshore Wind Turbine	AC	Aerodynamic Center
ACE	Actuator Curve Embedding	PI	Proportional Integral
Inp	In Plane	Oop	Out of Plane
GEBT	Geometrically Exact Beam Theory	FVM	Free Vortex Method
MBD	Multi-Body Dynamic	CSD	Computational Structural Dynamic
FEM	Finite Element Method	FSI	Fluid Structure Interaction
BEM	Blade Element Momentum	ALM	Actuator Line Method
VRS	Vortex Ring State	EALM	Elastic Actuator Line Method
CIP	Constrained Interpolation Profile	LES	Large Eddy Simulation

840

841 **Data Availability**

842 All the data and simulation details in this paper are available on request from the first author.

843 Requests can be sent to: yanglin205@hrbeu.edu.cn, yanglin205@163.com.

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849 60]).

850 **Declaration of competing interest**

851 The authors declare that they have no known competing financial interests or personal
852 relationships that could have appeared to influence the work reported in this paper.

853

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900 [engineering/principles-helicopter-aerodynamics-2nd-edition-1?format=HB&isbn=9781107013353](https://www.cambridge.org/gb/academic/subjects/engineering/aerospace-engineering/principles-helicopter-aerodynamics-2nd-edition-1?format=HB&isbn=9781107013353)
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