



Design of a scaled wind turbine model for matching multiple structural load transients under blade-detachment conditions

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Abstract. Complete blade detachment simultaneously changes a wind turbine's aerodynamic, centrifugal, and gravitational loading, requiring these load changes to be matched consistently in model-scale experiments. This study develops a scaled wind turbine design method that establishes common force and moment scales to define coupled targets for rotor speed, blade mass, spanwise mass distribution, and axial aerodynamic loading. The method is demonstrated using a 1:100 model of a 10 MW prototype onshore turbine. The blade geometry is redesigned for low-Reynolds-number operation, and a modular construction combining a CNC-machined carbon-fibre spar with segmented PMI foam permits separate adjustment of the aerodynamic profile and material distribution. Centrifugal- and aerodynamic-load experiments show overall agreement between numerical predictions and measurements under the tested conditions. The resulting model provides an experimental basis for controlled blade-detachment testing and subsequent validation of numerical models of post-failure load redistribution.

1 Introduction

Blade fracture and detachment transform an intact wind turbine rotor into a strongly asymmetric system by abruptly changing its mass, rotational inertia, and aerodynamic loading. The resulting load redistribution is transmitted through the hub and nacelle to the tower and can threaten the integrity of the remaining structure. Increasing turbine ratings and rotor diameters intensify this concern because large, slender blades exhibit increasingly coupled structural and aerodynamic responses under extreme conditions (Veers et al., 2023). Material defects, bond degradation, fatigue, and severe environmental loading can all contribute to blade failure (Mishnaevsky, 2022). The full-scale events in Fig. 1a illustrate the practical motivation for resolving the structural-load transient that follows blade loss (Liu et al., 2026).

Blade-failure research includes both investigations of structural collapse and analyses of the resulting turbine response. Chen (2017) investigated the collapse of a 47 m composite blade under combined bending and torsion in a full-scale static test, identifying the failure mechanisms and progressive collapse sequence. At the turbine level, Wu et al. (2024) simulated blade fracture using geometrically exact beam theory and compared the resulting change in tower-top acceleration with field monitoring data. Liu et al. (2026) developed OpenFAST-based aeroelastic methods for complete blade detachment from large offshore wind



(a) Full-scale blade failure and detachment



Field event 1

Field event 2

**Higher instrumentation cost
 limits measurement coverage**

Unpredictable events and incomplete records

Observed failure
 and operating context

(d) Stronger post-failure analysis

Reconstruct transient load redistribution
 Assess loads on the remaining structure

(b) Controlled model-scale experiment

Load-transient matching

Aerodynamic unloading
 Centrifugal imbalance
 Gravitational load change

Preserve the principal
 load changes across scales



Active blade release at 1:100 scale

Accessible instrumentation

Multiple load components at high sampling rates

Compare measured
 and predicted loads

Refine model
 and retest

(c) Numerical model validation

Tower-top force histories: F_x , F_y , F_z
 Full-scale and model-scale simulations

**Experimentally
 validated simulations**

Interpret full-scale
 load transients

Figure 1. Role of model-scale experiments in investigating blade-detachment transients. (a) Full-scale incidents motivate the investigation, but instrumentation costs and the unpredictable nature of failure limit the available measurements. (b) Controlled blade release provides measurements of multiple load components, with scaling designed to preserve the principal changes in aerodynamic, centrifugal, and gravitational loading. (c) Comparison of measured tower-top forces with numerical predictions supports iterative model validation. (d) Experimental evidence and validated simulations strengthen the reconstruction of post-failure load redistribution and the assessment of the remaining structure. The field photographs illustrate failure events and do not identify the reference turbine used for scaling.

turbines and identified the contributions of unbalanced centrifugal force, gravitational moment, and aerodynamic unloading to the extreme structural response. These numerical advances require experimental evidence that resolves the combined load components and their evolution during detachment.

Suitable full-scale measurements are difficult to obtain. Full-scale wind turbine experiments involve substantial measurement costs, difficulties in characterizing inflow conditions, and environmental conditions that cannot be prescribed on demand (Bayati et al., 2017a). For blade-detachment validation, measurements must also resolve the changing load components and the operating conditions during the event. These requirements motivate controlled model-scale experiments for reconstructing the transient load path.



Model-scale experiments provide controlled operating and inflow conditions at lower cost and risk, while allowing multiple load components to be measured at high sampling rates (Bottasso et al., 2014; Canet et al., 2021). A controlled blade release can capture the transition from an intact rotor to an asymmetric two-blade configuration. As illustrated in Fig. 1b–d, the resulting measurements can be used to compare numerical predictions with experimental observations, identify modelling discrepancies, and assess revised models. This process can strengthen post-failure analysis by improving the reconstruction of load redistribution and the assessment of loads acting on the remaining structure. Its validity, however, depends on whether the scaled turbine reproduces the principal load changes of the full-scale system.

Scaled wind turbine rotors generally require aerodynamic and structural redesign for the quantities of interest. Fowler et al. (2013) developed a performance-matched rotor for low-Reynolds-number model tests by modifying the blade chord and thickness while retaining the prescribed blade length and operating-speed scaling. Bayati et al. (2017b) combined low-Reynolds-number airfoils with chord and twist optimization to match rotor thrust while also considering blade mass and the first flapwise natural frequency. Canet et al. (2021) examined rotor scaling under aerodynamic, elastic, inertial, and gravitational effects. Hao et al. (2023) achieved simultaneous similarity of rotor thrust and shaft torque through blade redesign and adjustments to pitch and rotational speed. Shaft torque concerns rotation about the rotor axis, whereas the first radial moment of axial loading considered here determines the aerodynamic hub-moment change following blade loss. These approaches establish a basis for model-scale testing, but they primarily address intact-turbine behaviour. Blade detachment presents a different requirement because aerodynamic unloading, rotating mass imbalance, and gravitational asymmetry change simultaneously and must retain the appropriate relative magnitudes.

Experiments that reproduce this transition remain limited. Removing a blade before testing, attaching an eccentric mass, or introducing a blade defect can represent an imbalanced rotor, but these approaches do not reproduce the sudden transition from an intact rotor to one with a missing blade. Al-Hadad et al. (2019) measured blade, shaft, and tower vibrations when an added mass was released from a rotating blade. Because the release changed an initially unbalanced rotor into a more balanced state, the change was opposite to that caused by blade loss. Al-Najjar et al. (2026) simulated ice shedding by releasing an added mass from a stationary cantilevered blade, without rotor rotation, aerodynamic loading, or coupling to the complete turbine. A controlled experiment that releases a blade from an operating rotor is therefore needed to investigate the combined load changes and their transmission through the turbine.

The experiment also requires similarity targets tailored to blade detachment. Reynolds-number similarity governs viscous aerodynamic effects, while Froude similarity relates inertial and gravitational effects (Canet et al., 2021). Under practical wind-tunnel constraints, neither criterion alone defines a common scale for the changes in aerodynamic, centrifugal, and gravitational forces and their associated moments. The present objective is therefore to preserve the relative contributions of these dominant excitations, rather than to impose complete aeroelastic similarity. This load-transient correspondence provides the physical link between the full-scale incidents and the model-scale experiment in Fig. 1.

This study develops and validates a model-scale design method for investigating structural-load transients following complete blade detachment. Scaling relations for aerodynamic, centrifugal, and gravitational loads determine the model wind speed, rotor speed, blade mass, and spanwise mass distribution. The blade is aerodynamically redesigned for low-Reynolds-number



operation and manufactured using a modular carbon-fibre-spar and PMI-foam construction that permits close control of geometry and mass. Before detachment testing, centrifugal and aerodynamic load components are evaluated separately through wind-tunnel measurements and corresponding OpenFAST simulations using rigid- and flexible-tower configurations.

The main contributions are:

1. a similarity framework that establishes common force and moment scales for the principal load changes caused by complete blade detachment; and
2. a practical, low-cost model-blade and turbine design whose principal centrifugal and aerodynamic load components are assessed through numerical–experimental comparison.

Section 2 derives the scaling relations and model design targets. Section 3 describes the blade design, manufacture, and mass verification. Section 4 presents the turbine configuration and the centrifugal- and aerodynamic-load validation. The main conclusions and range of applicability are summarized in Sect. 5.

2 Deriving scaling laws for blade-detachment experiments

2.1 Detachment-related load components to be matched

Following the load-redistribution mechanisms identified by Liu et al. (2026), the similarity design considers three dominant load changes: aerodynamic unloading, centrifugal imbalance, and gravitational loading.

Aerodynamic axial loading: Detachment removes the axial and tangential aerodynamic loads of one blade from the hub. The resulting loss of axial force directly changes rotor thrust and is the primary aerodynamic target in the present design. Tangential loading, which mainly affects rotor torque and speed evolution, is not matched independently. Matching it would generally require tip-speed-ratio (TSR) similarity to preserve the local inflow angles. For a rotor with a radius of approximately 1 m, that condition would require rotational speeds of several hundred revolutions per minute and would conflict with the centrifugal-to-gravitational load ratio derived below. In addition, the gravitational moment of the asymmetric two-blade rotor dominates the post-detachment rotor-torque contribution for the reference case (Liu et al., 2026).

With radial position r measured from the rotor axis, blade-root and tip radii r_0 and R , and axial force per unit span $q_a(r)$, the single-blade aerodynamic force and its first radial moment are

$$F_a = \int_{r_0}^R q_a(r) dr, \quad M_a = \int_{r_0}^R r q_a(r) dr. \quad (1)$$

Matching both F_a and M_a constrains the axial-force loss and the associated hub-moment imbalance. Two spanwise load distributions can produce the same resultant force but different moments; the radial location of the loading must therefore be considered when selecting the model-blade chord, twist, and airfoil sections. The aerodynamic blade design is described in Sect. 3.1.



- 95 *Centrifugal loading:* Immediately after one blade is lost, its former centrifugal-force contribution appears as a rotating imbalance. The lateral and vertical projections of this imbalance provide primarily once-per-revolution (1P) excitation at the tower top, with phase determined by rotor azimuth. Neglecting blade precone, the centrifugal force associated with a blade of line density $\mu(r)$ rotating at angular velocity Ω is

$$F_c = \Omega^2 \int_{r_0}^R r \mu(r) dr. \quad (2)$$

- 100 The force depends on the first radial mass moment and on the square of rotor speed. Rotor speed and mass placement must therefore be selected jointly.

Gravitational loading: Blade loss also changes the tower-top weight and leaves a periodic gravitational moment from the asymmetric rotor. The weight of one blade and the amplitude of its gravitational moment about the rotor axis are

$$F_g = g \int_{r_0}^R \mu(r) dr, \quad M_g = g \int_{r_0}^R r \mu(r) dr, \quad (3)$$

- 105 where g is gravitational acceleration and is identical at model and full scale. The signed moment varies with azimuth. Total blade mass governs the change in tower-top weight, whereas the first radial mass moment governs both the gravitational moment and centrifugal force. The spanwise mass distribution is consequently a primary design target.

2.2 Scaling-law formulation

- Let subscripts m and p denote the model and prototype, respectively. To preserve the relative contributions of the three dominant force changes, a common force scale κ_F is imposed:

$$\frac{F_{a,m}}{F_{a,p}} = \frac{F_{c,m}}{F_{c,p}} = \frac{F_{g,m}}{F_{g,p}} = \kappa_F. \quad (4)$$

This requirement preserves their pairwise ratios, for example

$$\frac{F_{a,m}}{F_{c,m}} = \frac{F_{a,p}}{F_{c,p}}, \quad \frac{F_{c,m}}{F_{g,m}} = \frac{F_{c,p}}{F_{g,p}}. \quad (5)$$

For a blade element of mass m at radius r , the centrifugal-to-gravitational force ratio is

$$115 \quad \frac{F_c}{F_g} = \frac{m\Omega^2 r}{mg} = \frac{\Omega^2 r}{g}. \quad (6)$$

This ratio has the form of a squared Froude number (Canet et al., 2021; Vittori et al., 2022). With the geometric scale $\lambda_L = r_m/r_p$, equality of this ratio at the two scales gives

$$\frac{\Omega_m^2 r_m}{g} = \frac{\Omega_p^2 r_p}{g}, \quad \lambda_\Omega = \frac{\Omega_m}{\Omega_p} = \lambda_L^{-1/2}. \quad (7)$$

The associated mechanical frequency and time scales are

$$120 \quad \lambda_f = \frac{f_m}{f_p} = \lambda_L^{-1/2}, \quad \lambda_t = \frac{t_m}{t_p} = \lambda_L^{1/2}. \quad (8)$$



These scales describe the rotational and structural dynamics targeted in this work; the aerodynamic convection-time scale need not be identical.

Centrifugal-force similarity further requires

$$\Omega_m^2 \int_{r_{0,m}}^{R_m} r_m \mu_m(r_m) dr_m = \kappa_F \Omega_p^2 \int_{r_{0,p}}^{R_p} r_p \mu_p(r_p) dr_p. \quad (9)$$

- 125 Using $r_m = \lambda_L r_p$, $dr_m = \lambda_L dr_p$, and Eq. 7, Eq. 9 requires the first radial mass moment to scale by $\kappa_F \lambda_L$. This integral condition does not uniquely determine the spanwise line-density distribution. A sufficient design choice that also satisfies the total blade-mass constraint is

$$\mu_m(r_m) = \frac{\kappa_F}{\lambda_L} \mu_p(r_p), \quad r_p = \frac{r_m}{\lambda_L}. \quad (10)$$

Its spanwise integral gives the blade-mass scale

$$130 \quad \frac{M_{b,m}}{M_{b,p}} = \kappa_F, \quad (11)$$

and therefore also satisfies $F_{g,m}/F_{g,p} = \kappa_F$. The masses of other tower-top components are scaled by the same factor where their gravitational contribution is part of the target response.

Because a moment contains one additional length factor, the aerodynamic and gravitational moment scale is

$$\kappa_M = \frac{M_{a,m}}{M_{a,p}} = \frac{M_{g,m}}{M_{g,p}} = \kappa_F \lambda_L. \quad (12)$$

- 135 For aerodynamic design, a corresponding reference axial-load distribution is prescribed as

$$q_{a,m}^*(r_m) = \frac{\kappa_F}{\lambda_L} q_{a,p}(r_p), \quad (13)$$

where the asterisk denotes a design target. Pointwise agreement with this reference distribution is not required, provided that its resultant and first radial moment are matched. These target integrals satisfy

$$F_{a,m}^* = \int_{r_{0,m}}^{R_m} q_{a,m}^*(r_m) dr_m = \kappa_F F_{a,p}, \quad (14)$$

$$140 \quad M_{a,m}^* = \int_{r_{0,m}}^{R_m} r_m q_{a,m}^*(r_m) dr_m = \kappa_F \lambda_L M_{a,p}. \quad (15)$$

The external blade geometry must therefore be adjusted to match both the axial force and its radial moment, while the internal material distribution must match the blade mass and first radial mass moment.

For a three-blade rotor, model-scale thrust can be written as

$$T_m = \frac{1}{2} \rho_m U_m^2 A_m C_{T,m}(\Omega_m, \beta_m, U_m) = \kappa_F T_p, \quad (16)$$



Table 1. Principal parameters of the reference wind turbine.

Parameter	Value	Unit
Rated power class	10	MW
Hub height	140	m
Blade length	112	m
Rotor diameter	230	m
Overall turbine height	255	m
Rated wind speed	10.5	m s^{-1}
Rated rotor speed	8	rpm

145 where ρ_m , U_m , A_m , $C_{T,m}$, and β_m are the model-scale air density, mean wind speed, rotor-swept area, thrust coefficient, and collective pitch, respectively. The wind-speed scale $\lambda_U = U_m/U_p$ is selected with regard to both the required aerodynamic force and the operating range of the wind tunnel.

Conventional aerodynamic model design often preserves TSR (Bayati et al., 2017b):

$$\frac{\Omega_m R_m}{U_m} = \frac{\Omega_p R_p}{U_p}. \quad (17)$$

150 Together with $\lambda_\Omega = \lambda_L^{-1/2}$, this condition would require $\lambda_U = \lambda_L^{1/2}$. At $\lambda_L = 1/100$, the rated model wind speed would therefore be only 1.05 m s^{-1} for the 10.5 m s^{-1} prototype condition. The resulting aerodynamic force would require an impractically light blade to retain the centrifugal and gravitational force ratios. Exact TSR similarity is therefore relaxed, and the blade geometry is redesigned to match the axial force and moment targets at a higher, experimentally practical wind speed. The use of aerodynamic redesign to recover selected load targets under Reynolds-number mismatch is established in model-scale rotor studies (Fowler et al., 2013; Canet et al., 2021); the common scaling of the detachment-related loads provides the specific constraints here. Tangential aerodynamic similarity is outside the scope of the present scaling.

2.3 Case study using a prototype turbine

The method is applied to a 10 MW onshore prototype developed by CRRC Zhuzhou Institute. Its principal dimensions and rated operating conditions are listed in Table 1.

160 Load directions follow the nominal FAST/OpenFAST tower-top coordinate convention (Jonkman and Buhl, 2005) shown in Fig. 2a. The x axis points downwind in the tower fore–aft direction, y is horizontal and transverse to the inflow, and z points upwards. Tower-top forces and moments are denoted by F_x , F_y , F_z and M_x , M_y , M_z , respectively. Rotor azimuth ψ is referenced to blade 1. Zero azimuth places blade 1 vertically upwards, and positive rotation is clockwise when viewed from upstream along $+x$ (Fig. 2b).

165 Experiments are conducted in the wind tunnel of the State Key Laboratory of Ocean Engineering at Shanghai Jiao Tong University, as shown in Fig. 11. The available low-speed test section is 6 m wide and 3.5 m high, provides wind speeds of

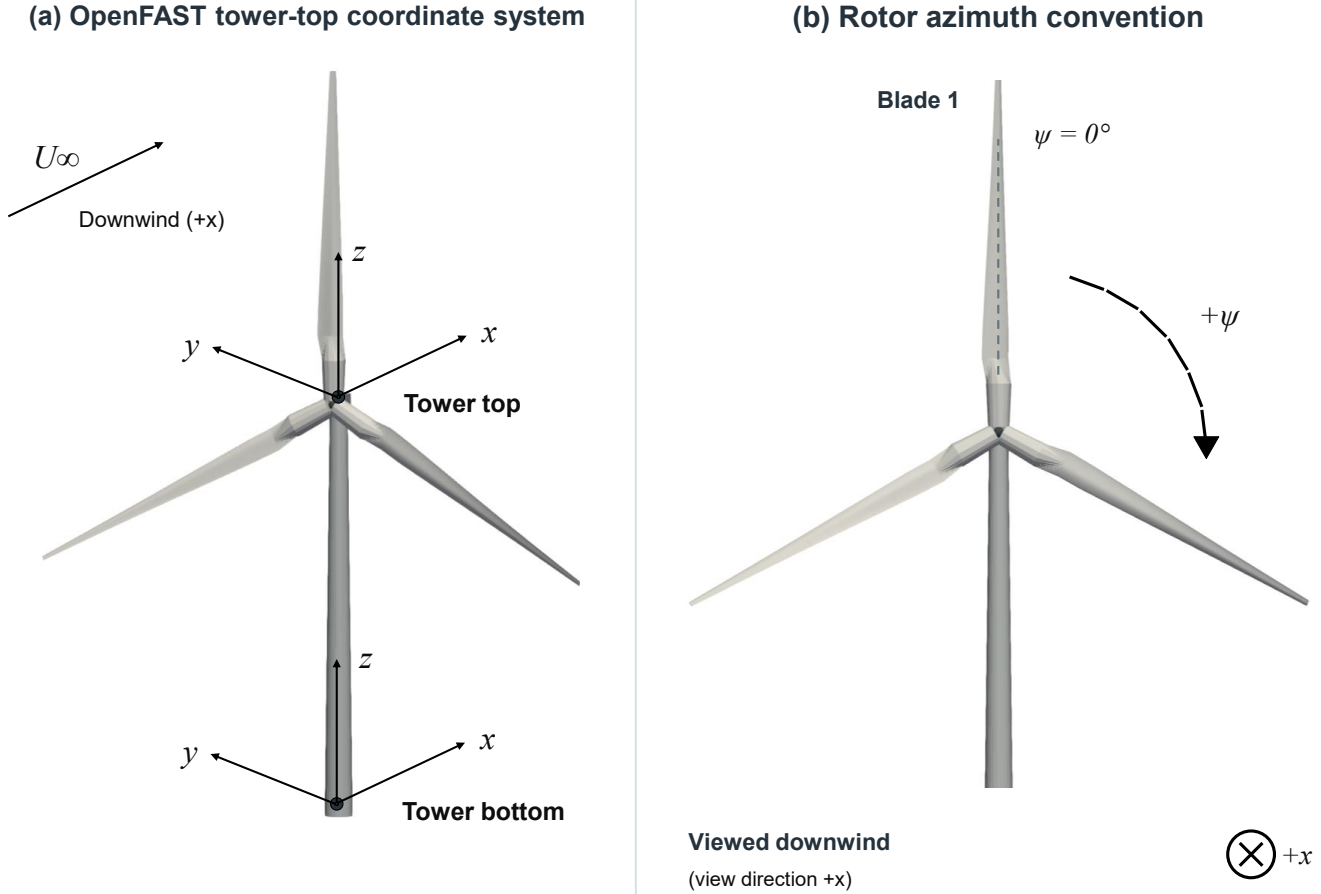


Figure 2. Coordinate and rotor-azimuth conventions: (a) tower-top and tower-base axes; (b) blade-1 azimuth reference and positive rotation direction. The crossed-circle symbol denotes $+x$ into the page. The symbol U_∞ in the figure corresponds to U in the text.

$0\text{--}20\text{ m s}^{-1}$, and permits a maximum model installation height of 3 m. Accounting for these limits, blade-tip clearance, manufacturability, and system integration (Bayati et al., 2017b) gives a geometric scale of $\lambda_L = L_m/L_p = 1/100$. The resulting turbine has an overall height of approximately 2.55 m.

170 Figure 3 shows the prototype single-blade aerodynamic force, centrifugal force, and weight calculated using Bladed software. The three components are of the same order of magnitude, although their relative contributions vary with operating condition. Near the rated wind speed of 10.5 m s^{-1} , aerodynamic thrust and blade weight are approximately 0.58 and 0.43 times the centrifugal force, respectively. Independent scaling would change these relative contributions to hub and tower loading. The common force scale must also yield aerodynamic forces large enough to measure while keeping the required component

175 masses manufacturable. These considerations lead to $\kappa_F = 10^{-5}$.

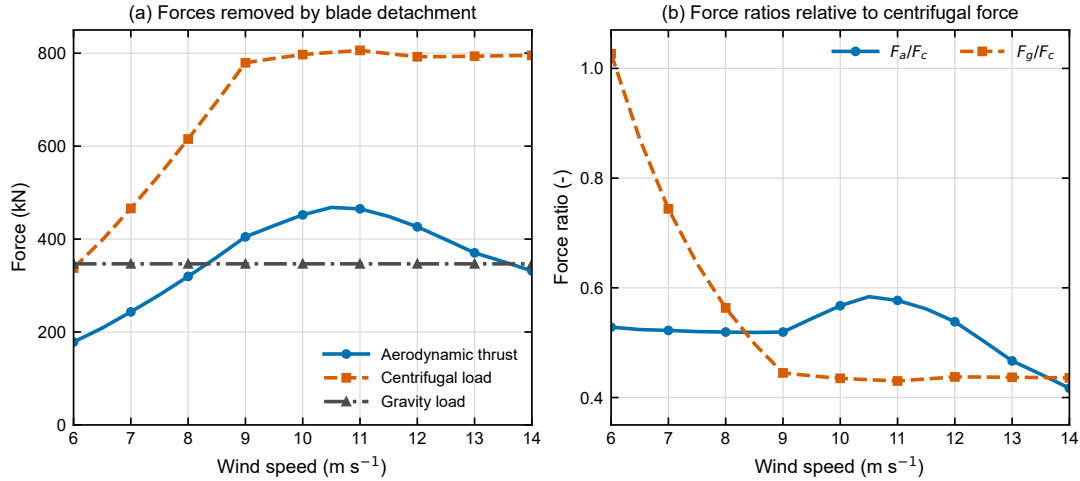


Figure 3. Prototype single-blade loads associated with detachment at different mean wind speeds: (a) aerodynamic thrust, centrifugal force, and weight; (b) ratios of aerodynamic thrust and weight to centrifugal force. The quantities are load magnitudes rather than signed tower-top components.

A nominal wind-speed scale of $\lambda_U = 1/1.6$ is selected to produce model-scale rotor thrusts of approximately 5–13 N over the operating schedule. It maps the prototype rated wind speed to approximately 6.6 m s^{-1} and keeps the model-scale inflow within the practical range of the facility. The required thrust coefficient is approximately 0.2 near the principal design condition, which is achievable for a low-Reynolds-number model rotor (Bayati et al., 2017b).

Figure 4 illustrates the resulting load-matching objective through a numerical comparison at the full-scale rated wind speed of 10.5 m s^{-1} . The full-scale tower-top forces are converted to model-scale units using $F_m^* = \kappa_F F_p$, with $\kappa_F = 10^{-5}$, and the time axis is converted using $t_m = \lambda_t t_p$, with $\lambda_t = 0.1$. These target histories are compared with the simulated response of the designed model turbine. Blade loss occurs at approximately $t_m = 10 \text{ s}$. The model reproduces the principal reduction and post-failure fluctuation in axial force F_x and the onset of periodic lateral force F_y , with post-detachment oscillation periods similar to those of the scaled full-scale target. Differences remain in the axial-force level and oscillation amplitudes, so the comparison demonstrates approximate matching of the dominant load changes. Both curves are numerical predictions; experimental calibration of the load components is presented in Sect. 4. The detailed aerodynamic and structural blade design used to approach these targets is described in Sect. 3.

The selected length, force, and nominal wind-speed scales determine the remaining target factors in Table 2. The corresponding dimensions, operating quantities, blade mass, and tower frequency are summarized in Table 3.

2.3.1 Overview of the design procedure under multiple load constraints

Figure 5 summarizes the design procedure. The reference-turbine geometry, operating conditions, and spanwise load and mass distributions are combined with the experimental constraints to select the length, force, and wind-speed scales. These

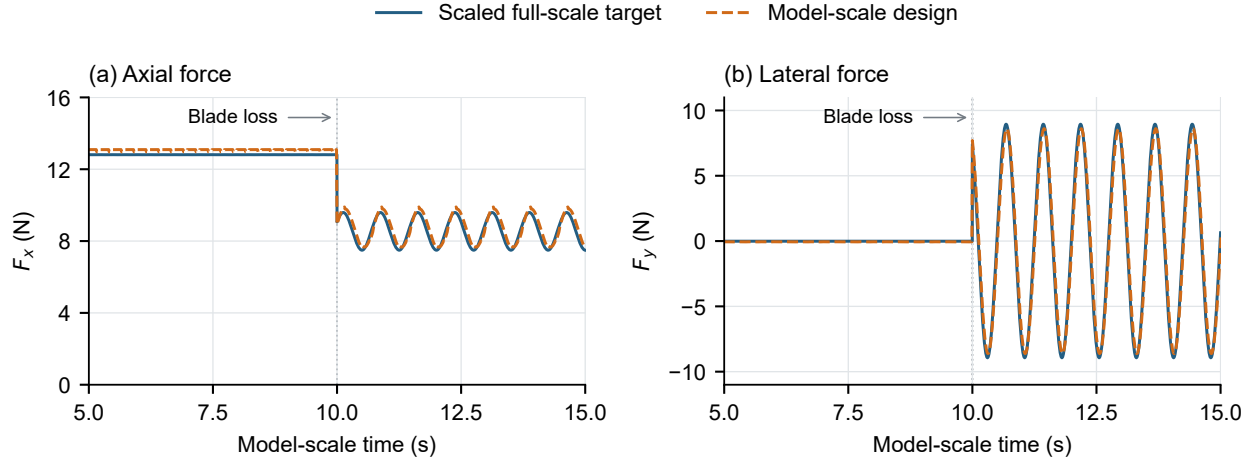


Figure 4. Numerical comparison of the scaled full-scale target and model-scale design following one-blade loss at the full-scale rated wind speed: (a) tower-top axial force F_x and (b) lateral force F_y . Full-scale forces and time are multiplied by 10^{-5} and 0.1, respectively. The shared interval 5–15 s is shown in model-scale time, with blade loss at approximately 10 s.

Table 2. Principal target scale factors for matching blade-detachment loads. All factors are model-to-prototype ratios.

Quantity	Symbol	Scaling relation	Selected value
Length and radial position	L, r	λ_L	10^{-2}
Wind speed	U	λ_U	1/1.6 (nominal)
Angular velocity	Ω	$\lambda_\Omega = \lambda_L^{-1/2}$	10
Mechanical time	t	$\lambda_t = \lambda_L^{1/2}$	10^{-1}
Mechanical frequency	f	$\lambda_f = \lambda_L^{-1/2}$	10
Acceleration	a	$\lambda_a = \lambda_L / \lambda_t^2$	1
Blade line density	μ	$\lambda_\mu = \kappa_F / \lambda_L$	10^{-3}
Blade mass	M_b	$\lambda_M = \kappa_F$	10^{-5}
First radial mass moment	$\int r \mu dr$	$\kappa_F \lambda_L$	10^{-7}
Axial load per unit span	q_a	κ_F / λ_L	10^{-3}
Resultant forces	F_a, F_c, F_g	κ_F	10^{-5}
Load moments	M_a, M_g	$\kappa_F \lambda_L$	10^{-7}

The time, frequency, and acceleration factors refer to structural motion under the mechanical time scale.

The aerodynamic convection-time scale is not imposed as an independent similarity requirement.

factors define the target aerodynamic load distribution and its radial moment, together with the target blade-mass distribution, centrifugal force, and gravitational loads. The aerodynamic geometry and internal structure are then designed as a coupled problem. Chord and airfoil geometry determine the axial loading and the envelope available for the spar, while the spar and

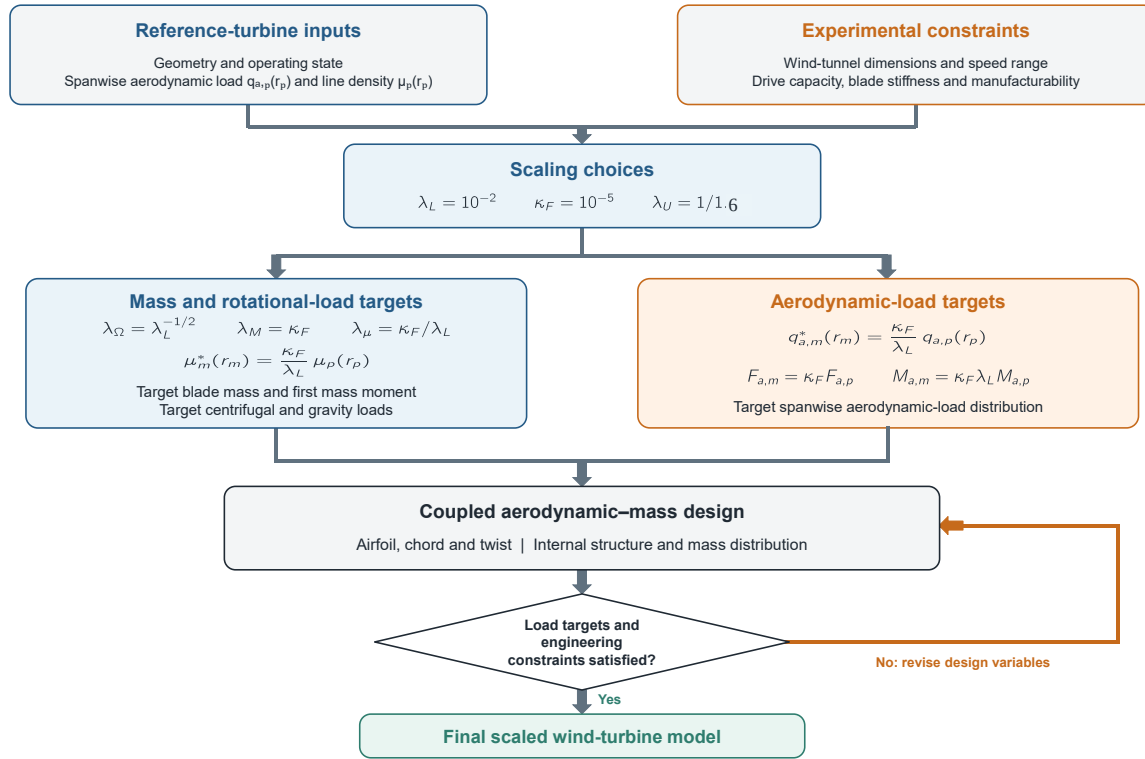


Figure 5. Scaled-model design procedure under combined aerodynamic, centrifugal, and gravitational load constraints. Aerodynamic and mass-related targets form two coupled branches of the blade design. Geometry and material distribution are revised until the target loads and engineering constraints are satisfied.

Table 3. Principal prototype quantities and corresponding model design targets.

Quantity	Prototype	Model target
Hub height	140 m	1.40 m
Blade length	112 m	1.12 m
Rotor diameter	230 m	2.30 m
Rated wind speed	10.5 m s^{-1}	$\approx 6.6 \text{ m s}^{-1}$
Rated rotor speed	8 rpm	80 rpm
Single-blade mass	35.38 t	0.3538 kg
First tower bending frequency	0.275 Hz	2.75 Hz

The target blade mass includes the blade-root fixture that detaches with the blade. The measured mass of the complete released assembly, including this fixture, is 0.3672 kg.



foam-core distributions determine mass, mass moment, and stiffness. The variables are revised until the load targets and manufacturing constraints are satisfied.

3 Model blade design, manufacture, and verification

Existing model-blade manufacturing approaches include carbon-fibre construction using CNC-machined aluminium moulds and an autoclave process (Bayati et al., 2017b). Other scaled-blade studies have considered machined foam cores with load-carrying spars and have examined their structural design and characterization (Campagnolo et al., 2014; Canet et al., 2021). These studies provide a basis for selecting a manufacturing approach suited to the aerodynamic, mass, and structural requirements of the experiment.

Blade-detachment experiments require economical, repeatable manufacture and close control of the released mass and its radial distribution. To address these requirements, the present model combines a carbon-fibre spar, which provides the principal structural load path, with PMI52 foam segments that define the aerodynamic profile. Both components are shaped by computer numerical control (CNC) machining. Their dimensions can be adjusted separately within the available airfoil envelope, facilitating control of the aerodynamic geometry and material distribution.

The force comparison in Fig. 4 illustrates the response that this design aims to reproduce. The scaling relations in Sect. 2 translate that objective into constraints on axial loading, blade mass, and their first radial moments. Aerodynamic and structural design are coupled: the external profile determines both the aerodynamic loading and the space available for the spar, while the internal material distribution governs mass and bending stiffness. Figure 6 summarizes the design and manufacturing sequence, from aerodynamic geometry and spar sizing to foam assembly and surface covering.

3.1 Aerodynamic geometry for thrust and moment matching

The aerodynamic design seeks to match the single-blade axial force and its first radial moment specified in Eqs. 14 and 15. Both quantities are required to represent the force and hub-moment changes caused by detachment. Geometry development focuses on the maximum-thrust operating point, at a model wind speed of 6.8 m s^{-1} . This design point is distinguished from the nominal rated-wind mapping in Table 3. Rotor speed follows the mechanical scaling established in Sect. 2.

SD7032 is selected as the baseline airfoil following its use in low-Reynolds-number model-rotor design (Bayati et al., 2017b, a). The blade is represented by $N = 20$ spanwise stations. Chord c_i and relative thickness $\tau_i = t_i/c_i$, where t_i is the maximum section thickness, are the principal geometric variables. Geometric twist is fixed at zero to simplify manufacture, so chord and section shape provide the main means of adjusting the loading. The collective pitch remains an operating variable, and the local angle of attack varies along the span.

The AeroDyn module of OpenFAST is used to evaluate each candidate geometry (OpenFAST, 2025). Its steady blade-element momentum formulation follows Ning (2014). For nominal axial inflow, the sectional axial force per unit span can be



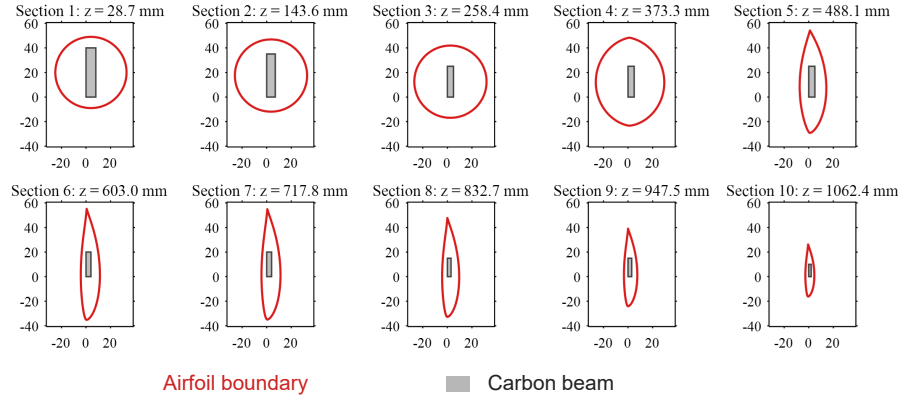
(a) Aerodynamic design

Airfoil geometry



(b) Beam sizing and calibration

Adjust cross sections to fit inside every airfoil with a clearance margin



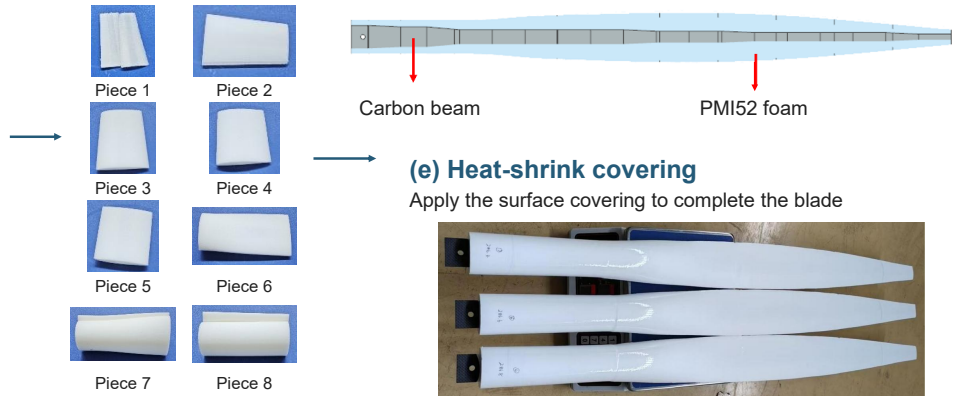
(c) Carbon beam

Manufacture in carbon fibre



(d) Foam manufacture and assembly

Machine PMI52 airfoils with internal holes and fit them onto the beam



(e) Heat-shrink covering

Apply the surface covering to complete the blade



Figure 6. Model blade design and manufacture: (a) aerodynamic geometry design; (b) sizing and calibration of the carbon-fibre spar within the local airfoil envelope, retaining a clearance margin at each section; (c) manufacture of the carbon-fibre spar; (d) machining of eight PMI52 foam segments with internal openings and assembly onto the spar; and (e) application of a heat-shrink covering to complete the blade surface. Red outlines and shaded rectangles in (b) denote the airfoil boundaries and spar cross-sections, respectively.

expressed as

$$\begin{aligned}
 q_{a,i} &= \frac{1}{2} \rho_m W_i^2 c_i C_{n,i}, \\
 C_{n,i} &= C_{l,i} \cos \phi_i + C_{d,i} \sin \phi_i, \\
 Re_i &= \frac{\rho_m W_i c_i}{\mu_{\text{air}}}.
 \end{aligned} \tag{18}$$



Here, W_i is the relative velocity, ϕ_i is the inflow angle measured from the rotor plane, and $C_{l,i}$ and $C_{d,i}$ are the lift and
 230 drag coefficients. The dynamic viscosity of air is denoted by μ_{air} , distinct from the blade line density μ . The local angle of
 attack is $\alpha_i = \phi_i - \beta_m - \theta_i$, where β_m is collective pitch and $\theta_i = 0$ is geometric twist. Changing chord affects both sectional
 force and Reynolds number; changing relative thickness requires aerodynamic coefficients appropriate to the modified section.
 Consistent section geometry and coefficient data are therefore necessary when evaluating each candidate.

The station loads are compared with the target distribution obtained from Eq. 13. Using integration weights w_i with dimen-
 235 sions of length, the integrated quantities are evaluated as

$$F_{a,m} \simeq \sum_{i=1}^N w_i q_{a,i}, \quad M_{a,m} \simeq \sum_{i=1}^N w_i r_i q_{a,i}, \quad (19)$$

where r_i is measured from the rotor centre, consistently with Eq. 1. The weights account for the spanwise station spacing.
 Convenient measures of the integrated mismatch are

$$\varepsilon_F = \frac{F_{a,m} - F_{a,m}^*}{F_{a,m}^*}, \quad \varepsilon_M = \frac{M_{a,m} - M_{a,m}^*}{M_{a,m}^*}, \quad (20)$$

240 The asterisks denote scaled targets, and the errors are signed fractional deviations. Matching the force alone does not ensure
 that its radial moment is correct. This distinction can be expressed through the radial centre of axial loading,

$$\bar{r}_a = \frac{M_{a,m}}{F_{a,m}}, \quad \frac{\bar{r}_a}{\bar{r}_a^*} = \frac{1 + \varepsilon_M}{1 + \varepsilon_F}. \quad (21)$$

At approximately matched thrust, a moment deficit indicates that the load centre must move outwards, whereas an excessive
 moment requires an inward shift. Chord and section-shape adjustments redistribute the loading, and AeroDyn evaluates their
 245 combined effect on the blade.

The root uses a fixed-diameter cylindrical section to meet root-strength and attachment requirements. Chord and relative
 thickness are adjusted over the lifting span, with a smooth transition to the root. Increased chord in selected outboard regions
 supplies the required axial force and radial moment at the design operating point. The resulting planform is governed by these
 load constraints and is not a geometric replica of the prototype blade.

250 Figure 7 summarizes the iterative procedure. Polynomial fitting smooths the station-wise chord and relative-thickness dis-
 tributions for manufacture. The fitted geometry is then re-evaluated in AeroDyn because smoothing can change both the load
 distribution and its integrals. Further adjustments address the remaining force and moment mismatch.

Figure 8 presents the spanwise chord distribution of the redesigned model blade and the corresponding aerodynamic thrust
 distribution. The larger chord in the midspan region accommodates the higher target thrust in this region. Although the spanwise
 255 thrust distributions are not identical, the design closely matches both the target axial resultant and its first radial moment. The
 final external profile defines the envelope for the foam core and spar design in Sect. 3.2.

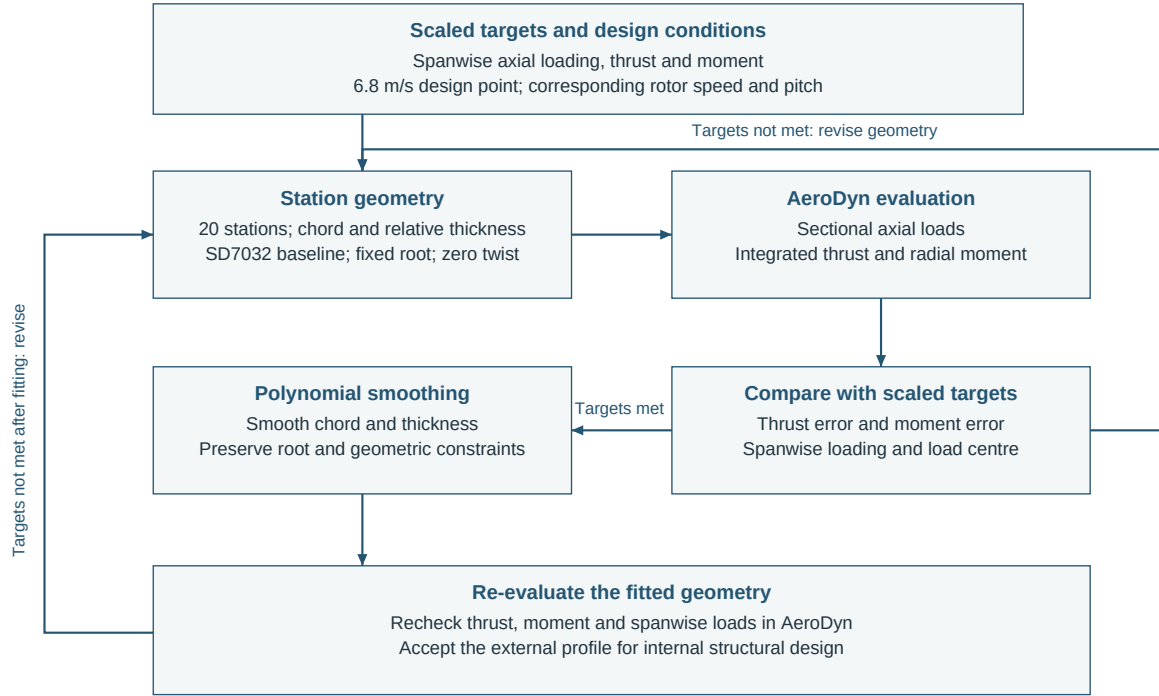


Figure 7. Aerodynamic geometry design under simultaneous thrust and moment constraints. Chord and relative thickness are adjusted with the root geometry fixed and the zero-twist constraint maintained. AeroDyn evaluates the spanwise loads and their integrals. The fitted geometry is re-evaluated before it is passed to the internal structural design.

3.2 Mass distribution and internal spar design

The internal design allocates the prescribed line density between the foam core and carbon-fibre spar. The target distribution in Eq. 10 constrains total blade mass and its first radial moment. These quantities govern the blade weight and gravitational moment, respectively, while the first radial mass moment also determines centrifugal force at a specified rotor speed.

A section-wise mass balance makes the material allocation explicit. Let $A(r)$ denote the total cross-sectional area occupied by the foam core and spar, and let $A_s(r)$ denote the spar area. The foam and spar densities are denoted by ρ_f and ρ_s , respectively. An allowance $\mu_{\text{aux}}(r)$ accounts for adhesive, the outer covering, and other distributed material. The estimated line density is then

$$\mu_m(r) = \rho_f [A(r) - A_s(r)] + \rho_s A_s(r) + \mu_{\text{aux}}(r). \quad (22)$$

For a prescribed target $\mu_m^*(r)$, the required spar area is

$$A_s^*(r) = \frac{\mu_m^*(r) - \rho_f A(r) - \mu_{\text{aux}}(r)}{\rho_s - \rho_f}. \quad (23)$$

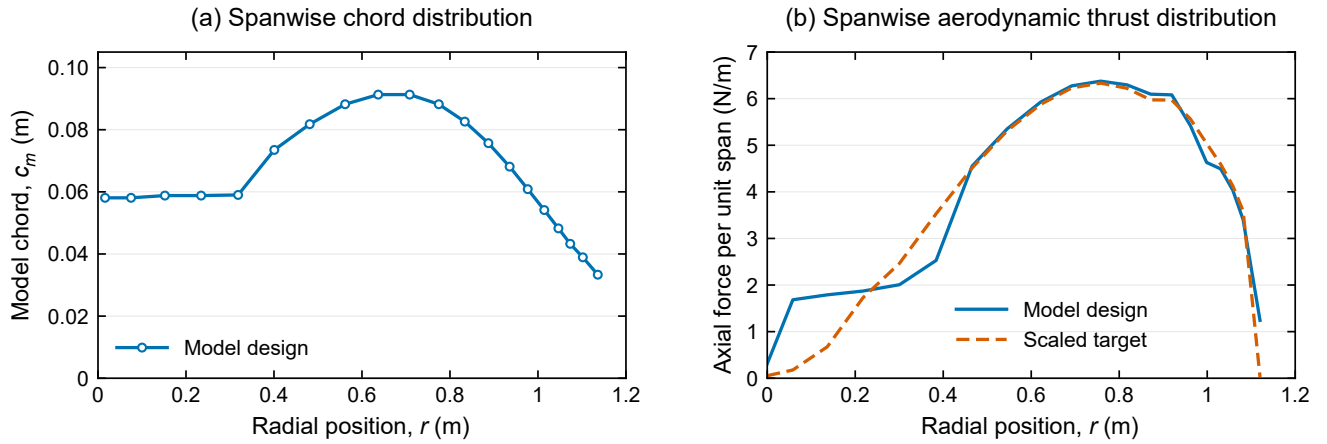


Figure 8. Aerodynamic blade-design results: (a) chord distribution of the redesigned model blade; (b) comparison of the axial aerodynamic force per unit span between the model design and the scaled target.

A feasible solution requires $0 \leq A_s^*(r) \leq A(r)$ and a spar cross-section that fits inside the local airfoil with sufficient clearance (Fig. 6b). The blade-root fixture is treated separately at its actual radial location. If the required spar cannot fit, the external
 270 geometry or material allocation must be revised. The auxiliary-mass term provides an allowance for assembly materials; its accuracy must be checked against the completed blade.

The spar is machined from carbon-fibre plate, with its cross-section varied along the span. The available plate thicknesses and local airfoil dimensions constrain the combinations of spar width and thickness. The foam supplies the remaining volume
 275 while maintaining the mass constraints. For a rectangular section, the second moment of area about a principal bending axis is $I = bh^3/12$, where h is the dimension in the bending direction. Thus, changing the section aspect ratio can change stiffness substantially even at fixed area. Changes in spar position must also be assessed relative to the composite section's neutral axis.

The spar design balances mass allocation, geometric clearance, and bending stiffness. Changing area affects both mass and stiffness, while changing aspect ratio can adjust stiffness at fixed area, subject to the airfoil envelope. A first flapwise frequency
 280 of 10 Hz is used as a blade design target, distinct from the tower-frequency target in Table 3. The manufacturing verification reported below focuses on component and assembled-blade masses.

3.3 Manufacture, assembly, and mass verification

The PMI52 foam core is machined in eight spanwise segments to reproduce the designed airfoil geometry (Fig. 6d). Each segment contains an internal opening for the carbon-fibre spar. The segments are weighed individually before assembly to



check their masses against the design values. The spar is machined from carbon-fibre plate using numerical control and weighed separately (Fig. 6c).

Table 4 compares the design and measured component masses. The two sampled spars weigh 161.6 and 162.2g, compared with the design value of 162.0g, giving deviations of -0.4 and $+0.2$ g, respectively. The eight foam segments have a combined measured mass of 73.0g, exceeding the design total of 70.64g by 2.36g (3.34%). This small excess is associated with manual machining of the spar openings and local thickening of the trailing edge to prevent breakage during manufacturing.

Table 4. Design and measured masses of the PMI52 foam segments and carbon-fibre spars. Foam-piece numbers correspond to Fig. 6d. Deviations are measured minus design mass.

Component	Design (g)	Measured (g)	Deviation (g)
PMI52 piece 1	0.96	1.2	+0.24
PMI52 piece 2	2.07	2.4	+0.33
PMI52 piece 3	3.42	3.8	+0.38
PMI52 piece 4	4.49	5.0	+0.51
PMI52 piece 5	4.77	5.2	+0.43
PMI52 piece 6	10.53	10.0	-0.53
PMI52 piece 7	24.74	25.4	+0.66
PMI52 piece 8	19.66	20.0	+0.34
Foam total	70.64	73.0	+2.36
Spar, sample 1	162.0	161.6	-0.4
Spar, sample 2	162.0	162.2	+0.2

The foam segments are fitted onto the spar and bonded in place. After curing, the joints and blade edges are trimmed, and a heat-shrink covering is applied to form the continuous external surface shown in Fig. 6e. Each completed blade is then weighed, including the foam, spar, adhesive, and covering but excluding the root connection unit. This definition is used consistently for comparison with the target blade mass of 269.8g.

The completed blades weigh 282.2–286.8g, with a mean of 284.85g (Table 5). The mean excess above the 269.8g target is 15.05g, or 5.58%, and is mainly attributed to the covering and adhesive used during assembly. The full batch spans 4.6g, approximately 1.6% of its mean mass. Thus, the manufacturing process produces a relatively small blade-to-blade variation in mass but a systematic excess relative to the target. This distinction highlights the need to control assembly additions as well as the machined components. Blades 1, 2, and 5, which have the smallest mass deviations, are selected for aerodynamic calibration. Component weighing checks the mass allocation, but total-mass agreement alone does not establish agreement in the first radial mass moment.



Table 5. Measured masses of the eight completed blades, excluding the root connection unit. Deviations are relative to the target mass of 269.8 g.

Blade Sample	Measured mass (g)	Deviation (g)
1	282.2	+12.4
2	283.4	+13.6
3	284.4	+14.6
4	286.4	+16.6
5	282.6	+12.8
6	286.4	+16.6
7	286.6	+16.8
8	286.8	+17.0
Mean	284.85	+15.05

4 Experimental validation of the design

The manufactured model-scale turbine is evaluated through two complementary sets of experiments. Two-blade tests without aerodynamic loading assess the centrifugal-load response, whereas three-blade wind-tunnel tests assess the mean aerodynamic loads. Rigid- and flexible-tower configurations distinguish rotor-load calibration from the transfer of these loads to the tower base. The following comparisons establish the model's performance under these calibration conditions before its use in blade-detachment experiments.

4.1 Experimental configurations

4.1.1 Nacelle design

The nacelle support structure is machined from aluminium alloy. A stainless-steel main shaft supported by two bearings carries the rotor weight, while a coupling transmits torque from the geared motor to the shaft. The materials and component dimensions are selected to provide sufficient strength and stiffness while meeting the scaled nacelle-mass target. The completed nacelle weighs 1.7968 kg, which is 3.2 g (0.18 %) below the target of 1.8 kg.

A brushless DC geared motor drives the rotor through a precision gearbox with a high-speed-to-low-speed reduction ratio of 71:1. The drive provides the torque required to accelerate the rotor to the target operating speeds. A dedicated field-oriented control (FOC) system uses cascaded current and speed feedback loops. A controller area network (CAN) bus records motor-side current, rotational speed, and angular position.



4.1.2 Load sensors used for the experiment

Six-component force–moment sensors manufactured by Sunrise Instruments Co., Ltd. measure the loads at the tower top (M3813E) and tower base (M4344T7). Each sensor resolves the three forces and three moments transmitted across its mounting section. Both sensors are calibrated before testing to check their measurement accuracy.

4.1.3 Flexible tower design

The flexible tower is designed to match the first fore–aft and side–side natural frequencies of the complete model turbine. Its frequencies are calculated using a finite-element representation with a Timoshenko beam element between each pair of adjacent nodes Guo et al. (2024). This representation accounts for both bending and shear deformation. The tower-top node includes the mass and rotary inertia of the rotor–nacelle assembly (RNA), six-component sensor, and mounting flange, with the RNA treated as a rigid body rigidly connected to the tower.

A uniform circular tube made from AL6061 aluminium alloy is selected for the tower, with outer diameter and wall thickness as the principal design variables. The full-scale first fore–aft and side–side natural frequencies are both approximately 0.275 Hz, giving model-scale targets of 2.75 Hz under the adopted frequency scaling. The measured total tower-top mass, including the RNA, sensor, and flange, is 3.858 kg. With this mass included, a tube of outer diameter 35 mm, wall thickness 1 mm, and length 1.27 m gives a calculated first natural frequency of approximately 2.99 Hz.

The calculated frequency is set above the scaled target to allow for sensor-cable mass, bolted-joint flexibility, and other assembly effects omitted from the idealized model. Based on our previous experimental experience, these effects are expected to reduce the frequency by approximately 5–10 %, giving a range of 2.69–2.84 Hz around the 2.75 Hz target.

Free-decay tests on the assembled turbine are used to identify the first natural frequencies from the fore–aft and side–side load-response spectra. Both measured frequencies are close to 2.75 Hz, supporting the intended matching of the dominant tower modes.

4.1.4 Nearly rigid tower design

A separate rigid tower is used for aerodynamic and centrifugal calibration in conjunction with the tower-top six-component load sensor. The tower has a monopile-like configuration comprising a hollow circular column and rectangular mounting plates at its upper and lower ends. The column is 1490 mm high, with outer and inner diameters of 60.66 and 50 mm, respectively, giving a wall thickness of 5.33 mm. The bottom plate measures 398 × 398 mm and is 7.87 mm thick. The top plate is 8 mm thick and includes a central opening of diameter 50 mm. The complete rigid-tower assembly has a mass of 20.66 kg.

The tower undergoes only small deformation under the design aerodynamic and centrifugal loading, so the change in the orientation of the tower-top sensor is correspondingly small. Consequently, the wind-induced change in the projection of the RNA weight onto the sensor axes is negligible. The sensor still carries the static RNA weight, but this contribution is removed by subtracting a wind-off reference measurement from the wind-on measurement. For measurements at matched rotor speed,



blade pitch, and assembly configuration, the mean aerodynamic force is therefore estimated as

$$\overline{\mathbf{F}}_{\text{aero}} \simeq \overline{\mathbf{F}}_{\text{wind-on}} - \overline{\mathbf{F}}_{\text{wind-off}}, \quad (24)$$

where the overbar denotes a time average and both force vectors are expressed in the same sensor coordinate system. Under these conditions, the mean force difference is attributed to aerodynamic loading, with negligible contamination from deformation-induced changes in the gravitational contribution.

4.2 Validation of centrifugal load

4.2.1 Experiment and simulation configurations

The centrifugal-load calibration uses a two-blade rotor configuration. An OpenFAST model is established using the nominal design parameters. The underlying OpenFAST model retains its three-blade rotor topology, with one blade removed through the failure configuration to reproduce the experimental two-blade state. The experiments use the nearly rigid tower without wind, with zero blade precone and shaft tilt. The simulations impose the same rotor geometry, represent the tower as rigid, and disable aerodynamic loading. Two sets of simulations are performed: a fixed-speed baseline and a simulation driven by the measured rotor-speed trajectory.

Independent laser measurements of the low-speed shaft indicate a rotational speed approximately 1.008 times the value obtained from the motor controller using the nominal gearbox ratio. The speed supplied to OpenFAST is therefore corrected as

$$n_{\text{sim}} = 1.008 n_{\text{C}}, \quad (25)$$

where n_{C} is the low-speed shaft speed derived from the controller output. The correction is applied to the steady-state mean for fixed-speed simulations and to the measured time series for speed-trajectory simulations. The corrected mean speeds range from 50.38 to 85.69 rpm for nominal settings of 50–85 rpm.

The OpenFAST output channels `YawBrFyp` and `YawBrFzp` give the tower-top lateral and vertical forces, F_y and F_z , respectively. For each component, the centrifugal-load amplitude is evaluated as half the peak-to-peak range over the steady-state interval,

$$F_{j,\text{amp}} = \frac{F_{j,\text{max}} - F_{j,\text{min}}}{2}, \quad j \in \{y, z\}. \quad (26)$$

The same amplitude definition is used for the experimental and simulated signals.

4.2.2 Comparison of measurements and simulations

Figure 9 compares the measured amplitudes with both simulation sets, plotted against corrected mean rotor speed. Both force components increase with speed in the experiments and simulations. Using the measured speed trajectory improves the F_y amplitude agreement at every tested speed. The absolute relative deviation, defined as $|F_{j,\text{amp}}^{\text{sim}} - F_{j,\text{amp}}^{\text{exp}}| / |F_{j,\text{amp}}^{\text{exp}}| \times 100\%$, ranges from 1.2 to 4.5 % for the measured-speed F_y predictions.

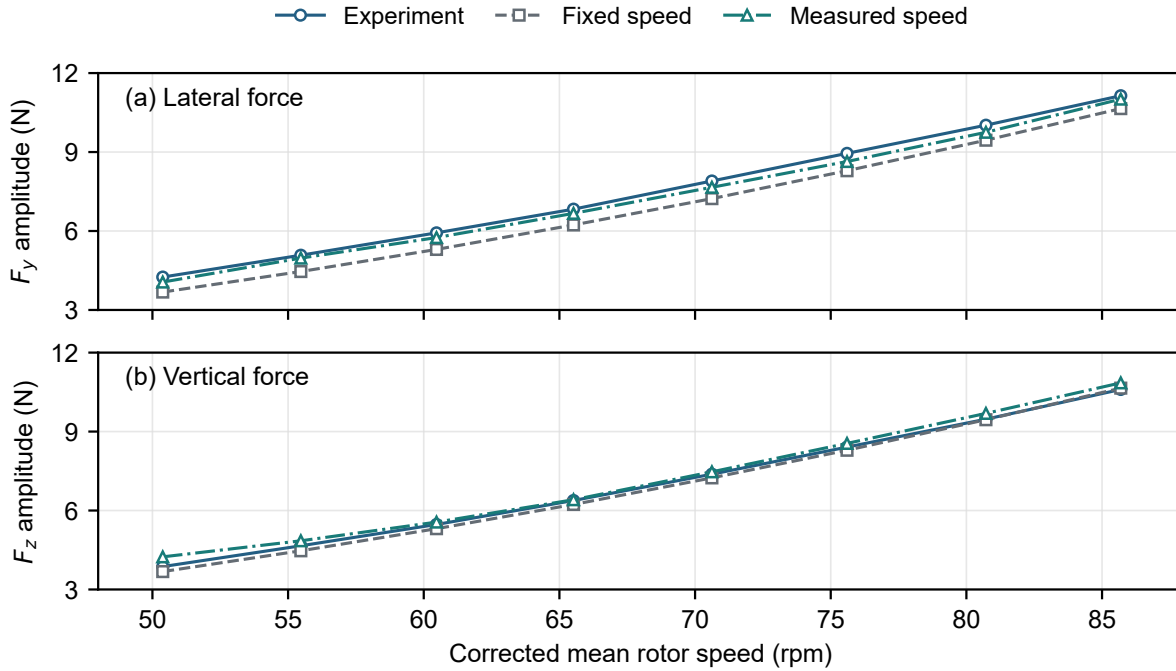


Figure 9. Two-blade centrifugal-load amplitude comparison: (a) tower-top lateral force F_y and (b) vertical force F_z . Experimental amplitudes and fixed-speed and measured-speed-trajectory OpenFAST predictions are evaluated as half the steady-state peak-to-peak range and plotted against corrected mean rotor speed. Lines connect the discrete calibration cases.

For F_z , the fixed-speed predictions are already close to the measurements. Introducing the measured speed trajectory pre-
 serves the speed-dependent trend but does not improve agreement uniformly. The measured-speed F_z deviations range from
 0.5 to 4.1 % for nominal speeds of 55–85 rpm, increasing to 9.6 % at 50 rpm.

The force histories at the nominal rated speed of 80 rpm are compared in Fig. 10. At this setting, the measured F_y am-
 plitude is 10.02 N, compared with 9.45 N for the fixed-speed simulation and 9.75 N for the measured-speed simulation. The
 corresponding F_z amplitudes are 9.47, 9.45, and 9.69 N, respectively. Thus, the speed-trajectory input primarily improves
 the lateral-force amplitude at this operating point. The measured-speed time histories are phase-aligned independently for the
 two components; the agreement shown in the figure therefore concerns phase-aligned waveforms rather than an independent
 prediction of the absolute phase.

The different levels of agreement in F_y and F_z may arise from asymmetry in the RNA mass distribution, installation offsets,
 or sensor-channel coupling; the present comparisons do not distinguish these contributions. Under the tested rigid-tower con-
 ditions, with zero precone and shaft tilt, the corrected OpenFAST model captures the increase in centrifugal-load amplitude
 with rotor speed. The measured-speed input improves F_y throughout the tested range, while the largest remaining discrepancy
 is the 9.6 % error in F_z at the lowest speed.

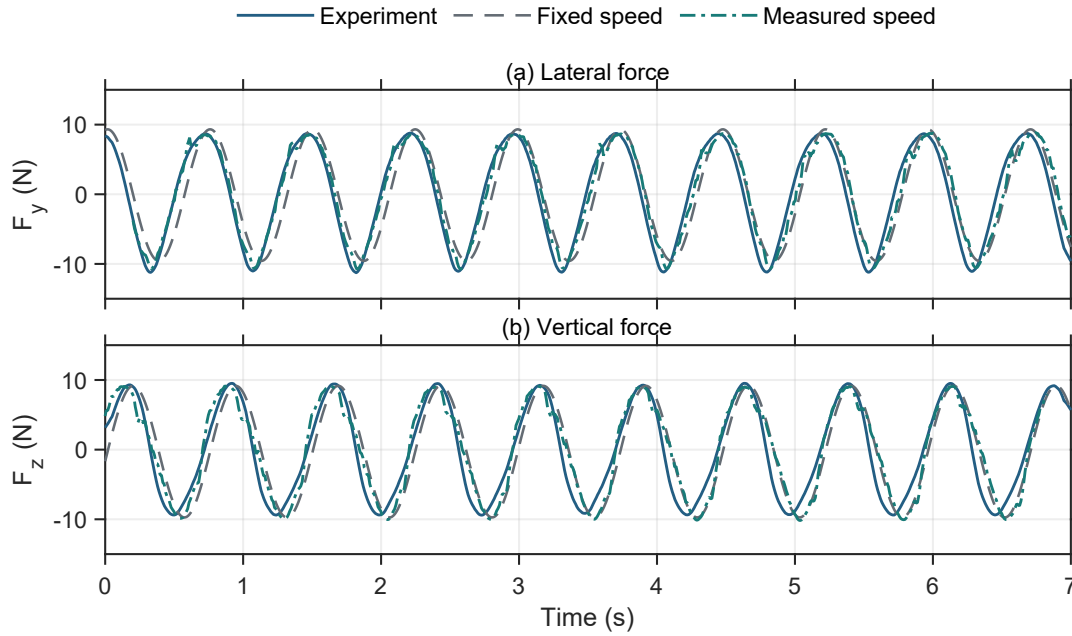


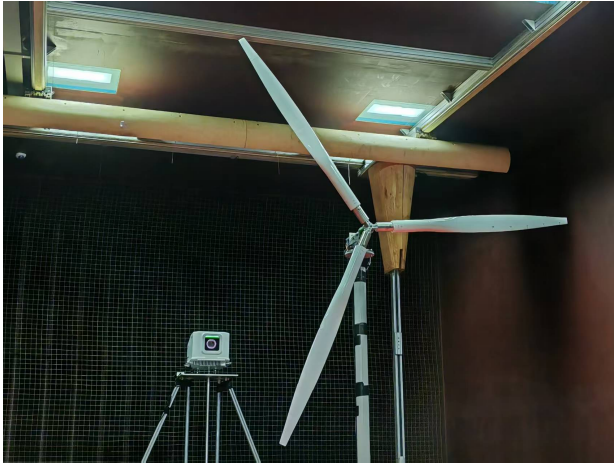
Figure 10. Force histories for two-blade centrifugal-load validation at a nominal rotor speed of 80rpm: (a) lateral force F_y and (b) vertical force F_z . Experimental measurements are compared with fixed-speed and measured-speed-trajectory OpenFAST simulations. The experimental signals are smoothed using an 11-sample moving average, and the mean is removed from each trace. The measured-speed simulation is shifted independently in each panel to align its phase with the experimental and fixed-speed reference signals.

4.3 Validation of aerodynamic load

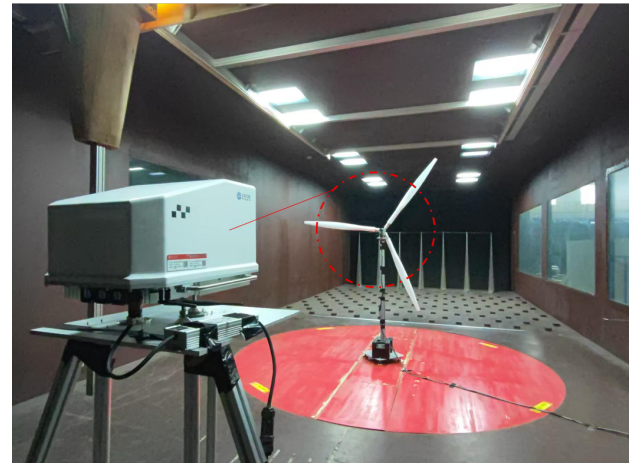
4.3.1 Inflow measurements and simulation setup

395 Aerodynamic-load validation uses the complete three-blade rotor on both towers in the wind-tunnel facility shown in Fig. 11. Scanning lidar has been demonstrated as a non-intrusive technique for measuring wind-tunnel flow fields (van Dooren et al., 2017). Here, a continuous-wave (CW) lidar measures the inflow in a plane approximately two rotor diameters upstream of the turbine. The circular scan has a radius of approximately 70 % of the blade span. Each scan takes approximately 0.5s and provides 100 line-of-sight velocity samples at uniformly spaced azimuthal positions. These measurements are converted to
 400 longitudinal wind speeds using the scan geometry to characterize the mean vertical inflow profile.

The rigid-tower tests assess the mean rotor thrust measured by the tower-top sensor, while the flexible-tower tests additionally assess its transmission to the tower-base bending moment. OpenFAST's AeroDyn module calculates the aerodynamic loads within the blade-element momentum framework using the designed model-blade geometry (Ning, 2014; OpenFAST, 2025). The airfoil lift and drag data are based on Bayati et al. (2017b), supplemented by XFOIL calculations at low Reynolds numbers
 405 (Drela, 1989), with representative section polars assigned along the span. The air density is set to the measured mean value of 1.135 kg m^{-3} .



(a) Front view



(b) Rear view and scanning ring

Figure 11. Wind-tunnel facility and CW lidar arrangement: (a) model turbine and lidar; (b) lidar viewing direction and schematic scanning ring, indicated in red.

The fifteen calibration cases are identified by the full-scale mean wind speed, $\bar{U}_p = 6\text{--}20\text{ m s}^{-1}$, in increments of 1 m s^{-1} . The nominal model rotor speeds are 62.79, 69.21, and 78.67 rpm for $\bar{U}_p = 6, 7$, and 8 m s^{-1} , respectively, and 80 rpm for the remaining cases. The full-scale wind speeds identify the reference operating conditions; the corresponding model-scale inflow
 410 is prescribed from the lidar measurements.

Lidar records are evaluated over the same steady-state intervals as the load measurements. The mean hub-height wind speed ranges from 4.00 to 6.84 m s^{-1} for the rigid-tower tests and from 4.10 to 7.03 m s^{-1} for the flexible-tower tests. After removal of isolated outliers, the measured turbulence intensity, defined as the wind-speed standard deviation divided by its mean, is $0.31\text{--}0.60\%$ and $0.33\text{--}0.54\%$, respectively. Power-law fits to the vertical profiles give wind-shear exponents of $0.004\text{--}0.030$
 415 for the rigid tower and $0.015\text{--}0.104$ for the flexible tower, with median values of 0.018 and 0.026 . These observations indicate weak shear in most cases and low inflow turbulence within the temporal and spatial resolution of the lidar. Probe-volume averaging and the scanning strategy affect lidar-based turbulence estimates (van Dooren et al., 2017; Fu et al., 2023); the reported values are therefore interpreted within those measurement limits. The measured mean profiles provide the inflow basis for the aerodynamic-load comparisons.

Figure 12 relates the lidar-measured model-scale mean wind speed to the corresponding full-scale mean wind speed used to identify each operating point. The measured wind speeds for the two tower configurations follow the same trend and differ by at most 0.20 m s^{-1} . The model-scale wind speed reaches its maximum near the full-scale rated condition and then decreases for the above-rated operating points, consistent with the aerodynamic-load matching schedule.

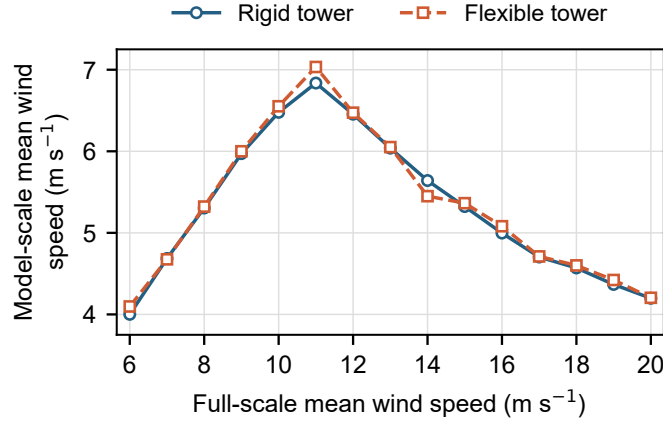


Figure 12. Lidar-measured model-scale mean hub-height wind speed for the rigid- and flexible-tower tests, indexed by the corresponding full-scale mean wind speed. Lines connect the discrete operating points.

4.3.2 Mean thrust and fore–aft bending moment

425 The comparison uses mean loads evaluated over steady-state intervals following the acceleration transient. For the rigid tower, the aerodynamic force is isolated using the wind-on and wind-off subtraction in Eq. 24. For the flexible tower, the tower-base moment is referenced to the stationary gravity baseline; the OpenFAST reference value of M_y is -3.519 N m and is subtracted before comparison. The principal quantities are the mean tower-top axial force $\overline{F}_x$ for both towers and the mean tower-base fore–aft bending moment $\overline{M}_y$ for the flexible tower. Their absolute relative error is defined as

$$430 \quad \varepsilon_Q = \frac{|\overline{Q}_{\text{sim}} - \overline{Q}_{\text{exp}}|}{|\overline{Q}_{\text{exp}}|} \times 100\%, \quad Q \in \{F_x, M_y\}. \quad (27)$$

Figure 13 shows the measured and simulated mean loads on the left axes and the corresponding errors on the right axes. For the rigid tower, both measurements and simulations show thrust increasing up to $\overline{U}_p = 11 \text{ m s}^{-1}$ and decreasing thereafter. The maximum error across all cases is 5.8%. At $\overline{U}_p = 11 \text{ m s}^{-1}$, where the measured thrust is greatest, the experimental and simulated means are 12.652 and 12.814 N, respectively, giving an error of 1.3%.

435 For the flexible tower, the maximum errors in $\overline{F}_x$ and $\overline{M}_y$ are 9.5% and 6.0%, respectively. Agreement is closer for $\overline{U}_p = 9\text{--}13 \text{ m s}^{-1}$, around the full-scale rated-wind-speed region, over which both errors remain below 4.7%. At $\overline{U}_p = 11 \text{ m s}^{-1}$, the measured and simulated thrusts are 13.005 and 13.129 N, and the corresponding base moments are 18.964 and 19.259 N m. These give errors of 1.0% and 1.6%, respectively. The simultaneous agreement in mean thrust and base moment supports the representation of the principal aerodynamic load and its transfer through the flexible tower.

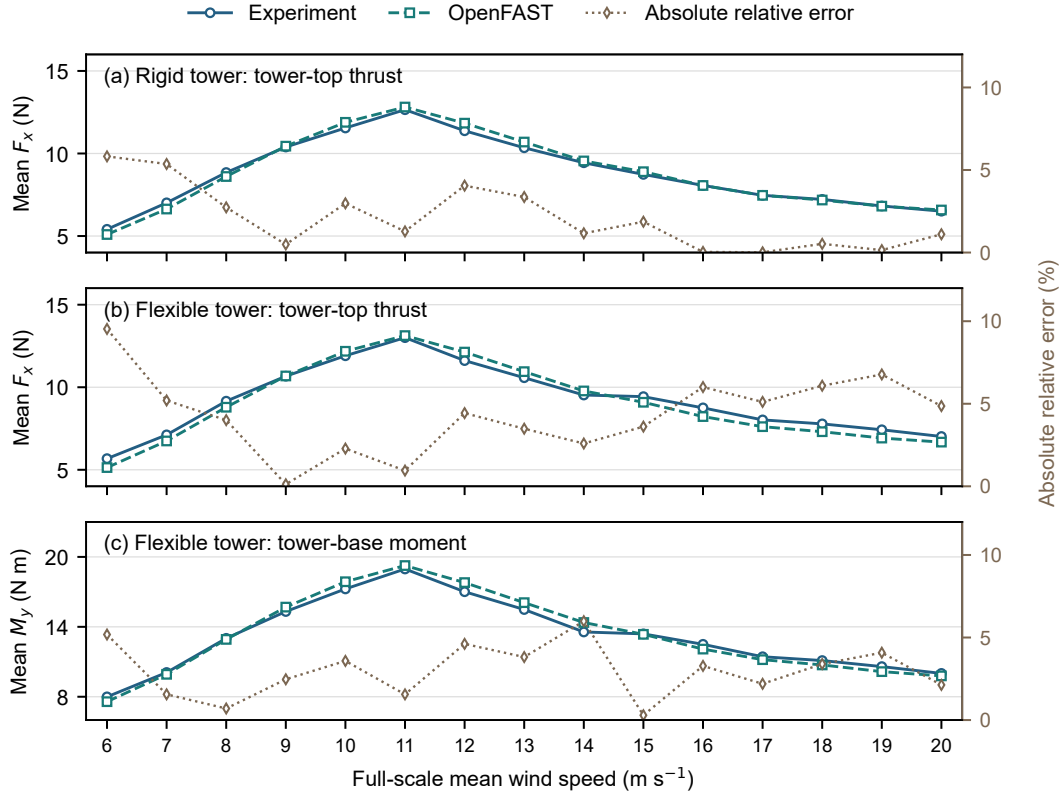


Figure 13. Mean aerodynamic-load comparison versus full-scale mean wind speed: (a) rigid-tower top thrust F_x ; (b) flexible-tower top thrust F_x ; and (c) flexible-tower base bending moment M_y . Measured and simulated means use the left axes, and absolute relative errors use the right axes. The base moment is corrected for the stationary gravity baseline. Lines connect the discrete cases.

4.3.3 Secondary force and moment components

Tables 6–8 compare the measured and simulated secondary load components. To express each discrepancy relative to the principal load at the same operating point, the normalized errors are defined as

$$\epsilon_{F_j}^{(F_x)} = \frac{|\overline{F}_{j,\text{sim}} - \overline{F}_{j,\text{exp}}|}{|\overline{F}_{x,\text{exp}}|} \times 100\%, \quad j \in \{y, z\}, \quad (28)$$

$$\epsilon_{M_j}^{(M_y)} = \frac{|\overline{M}_{j,\text{sim}} - \overline{M}_{j,\text{exp}}|}{|\overline{M}_{y,\text{exp}}|} \times 100\%, \quad j \in \{x, z\}. \quad (29)$$

These metrics avoid division by secondary components that approach zero. They quantify disagreement relative to the principal load, rather than the relative error of each secondary component itself. The flexible-tower F_z values exclude the stationary gravity contribution, with an OpenFAST reference of -34.425 N. The M_y denominator likewise excludes the stationary gravity baseline.



Table 6. Rigid-tower comparison of the secondary mean tower-top forces and their discrepancies relative to the measured mean F_x .

$\bar{U}_p$ (m s ⁻¹)	Mean F_y			Mean F_z		
	Exp.	OpenFAST	$\epsilon_{F_y}^{(F_x)}$ (%)	Exp.	OpenFAST	$\epsilon_{F_z}^{(F_x)}$ (%)
6	-0.094	-0.023	1.3	-0.432	-0.623	3.5
7	-0.063	-0.025	0.5	-0.721	-0.760	0.6
8	-0.011	-0.028	0.2	-1.068	-0.864	2.3
9	0.026	-0.040	0.6	-1.375	-1.049	3.1
10	0.046	-0.051	0.8	-1.560	-1.207	3.1
11	0.101	-0.054	1.2	-1.581	-1.305	2.2
12	0.117	-0.049	1.5	-1.558	-1.200	3.1
13	0.114	-0.044	1.5	-1.504	-1.078	4.1
14	0.087	-0.033	1.3	-1.581	-0.954	6.6
15	0.068	-0.034	1.2	-1.553	-0.891	7.6
16	0.060	-0.030	1.1	-1.513	-0.816	8.6
17	0.043	-0.026	0.9	-1.482	-0.765	9.6
18	0.031	-0.026	0.8	-1.483	-0.742	10.3
19	0.017	-0.025	0.6	-1.469	-0.713	11.1
20	-0.006	-0.028	0.3	-1.466	-0.699	11.8

The measured and OpenFAST means are in N. The normalized discrepancies use the measured mean F_x at the same operating point.

For the rigid tower, the F_y discrepancy remains below 1.5% of the measured thrust. The F_z discrepancy reaches 11.8%, with the largest values at the higher full-scale wind-speed cases, where thrust decreases. Thus, the close agreement in the principal thrust does not extend uniformly to the vertical force.

For the flexible tower, the F_y and F_z discrepancies are at most 5.6% and 5.5% of the measured thrust, respectively. The magnitudes of the secondary forces in Table 7 are smaller than the corresponding thrusts in Fig. 13, showing that F_x dominates the mean tower-top aerodynamic force.

At the flexible-tower base, the M_x and M_z discrepancies are at most 5.4% and 2.0% of the measured M_y , respectively. The secondary moments in Table 8 are also substantially smaller than the fore–aft moments in Fig. 13. Together, the force and moment comparisons support the use of F_x and M_y as the principal mean aerodynamic-load measures. This assessment concerns steady-state means; it does not establish the accuracy of fluctuating loads or the subsequent blade-detachment transient.

5 Conclusions

This study develops a model-scale design and manufacturing method for investigating wind-turbine loads following complete blade detachment. Its application to a 1:100 model of a 10 MW reference turbine leads to three main conclusions.

First, the derived similarity framework preserves the relative contributions of aerodynamic unloading, centrifugal imbalance, and gravitational loading by imposing a common force scale, κ_F , and a moment scale, $\kappa_F \lambda_L$. These requirements link rotor speed, total blade mass, spanwise mass distribution, and axial aerodynamic loading. For the present model, $\lambda_L = 10^{-2}$ and



Table 7. Flexible-tower comparison of the secondary mean tower-top forces and their discrepancies relative to the measured mean F_x .

$\overline{U}_p$ (m s^{-1})	Mean F_y			Mean F_z		
	Exp.	OpenFAST	$\epsilon_{F_y}^{(F_x)}$ (%)	Exp.	OpenFAST	$\epsilon_{F_z}^{(F_x)}$ (%)
6	-0.330	-0.010	5.6	-0.317	-0.627	5.5
7	-0.376	-0.024	4.9	-0.449	-0.765	4.4
8	-0.456	-0.045	4.5	-0.632	-0.860	2.5
9	-0.445	-0.036	3.8	-0.879	-1.039	1.5
10	-0.456	-0.034	3.5	-1.098	-1.194	0.8
11	-0.450	-0.037	3.2	-1.311	-1.296	0.1
12	-0.366	-0.034	2.9	-1.361	-1.189	1.5
13	-0.315	-0.037	2.6	-1.361	-1.066	2.8
14	-0.311	-0.038	2.9	-0.447	-0.948	5.3
15	-0.300	-0.041	2.8	-0.405	-0.880	5.0
16	-0.260	-0.039	2.5	-0.492	-0.811	3.6
17	-0.246	-0.042	2.6	-0.463	-0.764	3.8
18	-0.234	-0.040	2.5	-0.479	-0.741	3.4
19	-0.223	-0.038	2.5	-0.488	-0.713	3.0
20	-0.212	-0.037	2.5	-0.481	-0.696	3.1

The measured and OpenFAST means are in N. The normalized discrepancies use the measured mean F_x at the same operating point. The vertical-force values exclude the stationary gravity baseline.

Table 8. Flexible-tower comparison of the secondary mean tower-base moments and their discrepancies relative to the measured mean M_y .

$\overline{U}_p$ (m s^{-1})	Mean M_x			Mean M_z		
	Exp.	OpenFAST	$\epsilon_{M_x}^{(M_y)}$ (%)	Exp.	OpenFAST	$\epsilon_{M_z}^{(M_y)}$ (%)
6	-0.264	0.087	4.4	0.098	0.098	0.0
7	-0.331	0.184	5.1	0.119	0.160	0.4
8	-0.420	0.041	3.5	0.128	0.303	1.3
9	-0.510	-0.150	2.4	0.145	0.380	1.5
10	-0.547	-0.270	1.6	0.156	0.447	1.7
11	-0.590	-0.338	1.3	0.156	0.491	1.8
12	-0.540	-0.266	1.6	0.118	0.443	1.9
13	-0.508	-0.168	2.2	0.076	0.393	2.0
14	-0.422	-0.069	2.6	0.202	0.353	1.1
15	-0.423	-0.002	3.1	0.204	0.307	0.8
16	-0.386	0.085	3.8	0.181	0.283	0.8
17	-0.355	0.148	4.4	0.155	0.254	0.9
18	-0.344	0.173	4.7	0.140	0.239	0.9
19	-0.331	0.204	5.0	0.137	0.220	0.8
20	-0.316	0.222	5.4	0.126	0.209	0.8

The measured and OpenFAST means are in N m. The normalized discrepancies use the measured mean M_y at the same operating point. The M_y reference excludes the stationary gravity baseline.



465 $\kappa_F = 10^{-5}$ give a rotational-speed scale of 10 and a moment scale of 10^{-7} . Relaxing exact tip-speed-ratio similarity allows the blade geometry and operating wind speed to be selected for measurable aerodynamic loads at model scale. The resulting framework targets the dominant detachment-related force and moment changes.

Second, the proposed blade construction combines a CNC-machined carbon-fibre spar with eight PMI52 foam sections and a heat-shrink surface covering. Separating the load-carrying spar from the aerodynamic profile permits the spanwise allocation of material to be adjusted and checked through component weighing. The sampled spars differ from their 162.0 g design mass by at most 0.4 g, and the combined foam mass exceeds its target by 3.34%. Eight completed blades have a mean mass of 284.85 g and a blade-to-blade range of 4.6 g, approximately 1.6% of the mean. Their mean mass exceeds the 269.8 g target, which excludes the root connection unit, by 15.05 g; this excess is mainly attributed to the adhesive and covering. These results demonstrate repeatable manufacture and controllable component mass allocation, while identifying assembly additions as a remaining source of mass error.

Third, centrifugal- and aerodynamic-load tests provide a functional check of the manufactured blades against the design-based simulations. With measured rotor-speed histories prescribed in OpenFAST, the two-blade F_y amplitude error is 1.2–4.5% across the tested speeds. For F_z , the error is 0.5–4.1% at nominal speeds of 55–85 rpm, while the F_z error reaches 9.6% at 50 rpm. Across reference full-scale mean wind speeds of 6–20 m s⁻¹, the maximum mean-thrust errors are 5.8% for the rigid tower and 9.5% for the flexible tower. The maximum error in the flexible-tower mean fore–aft base moment is 6.0%. This agreement supports the ability of the manufactured model to reproduce the intended centrifugal and mean aerodynamic loads within the reported deviations, providing a calibrated basis for subsequent blade-detachment experiments.

Future experiments will vary the blade azimuth at detachment and the post-failure control strategy to determine their effects on extreme tower loads. Measurements at the tower top and base will be used to examine load redistribution and the resulting structural response. The resulting transient datasets will support verification and experimental validation of numerical simulation tools, including their treatment of blade removal, rotor-speed evolution, and the interaction between post-failure control and tower dynamics.

Code and data availability. The code used to reproduce the figures is available upon request from the corresponding author (fengguo@sjtu.edu.cn).

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495 *Competing interests.* The authors declare that they have no conflict of interest.

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References

- Al-Hadad, M., McKee, K. K., and Howard, I.: Vibration characteristic responses due to transient mass loading on wind turbine blades, *Eng. Fail. Anal.*, 102, 187–202, <https://doi.org/10.1016/j.engfailanal.2019.04.006>, 2019.
- Al-Najjar, I. F., Jálcs, K., and Kollár, L. E.: Experimental, numerical, and data-driven analysis of impulsive ice-shedding-induced vibrations
 505 in wind turbine blades, *Sci. Rep.*, 16, 24 446, <https://doi.org/10.1038/s41598-026-53564-7>, 2026.
- Bayati, I., Belloli, M., Bernini, L., Giberti, H., and Zasso, A.: Scale model technology for floating offshore wind turbines, *IET Renew. Power Gener.*, 11, 1120–1126, <https://doi.org/10.1049/iet-rpg.2016.0956>, 2017a.
- Bayati, I., Belloli, M., Bernini, L., and Zasso, A.: Aerodynamic design methodology for wind tunnel tests of wind turbine rotors, *J. Wind Eng. Ind. Aerodyn.*, 167, 217–227, <https://doi.org/10.1016/j.jweia.2017.05.004>, 2017b.
- 510 Bottasso, C. L., Campagnolo, F., and Petrović, V.: Wind tunnel testing of scaled wind turbine models: Beyond aerodynamics, *J. Wind Eng. Ind. Aerodyn.*, 127, 11–28, <https://doi.org/10.1016/j.jweia.2014.01.009>, 2014.
- Campagnolo, F., Bottasso, C. L., and Bettini, P.: Design, manufacturing and characterization of aero-elastically scaled wind turbine blades for testing active and passive load alleviation techniques within a ABL wind tunnel, *J. Phys. Conf. Ser.*, 524, 012 061, <https://doi.org/10.1088/1742-6596/524/1/012061>, 2014.
- 515 Canet, H., Bortolotti, P., and Bottasso, C. L.: On the scaling of wind turbine rotors, *Wind Energ. Sci.*, 6, 601–626, <https://doi.org/10.5194/wes-6-601-2021>, 2021.
- Chen, X.: Experimental investigation on structural collapse of a large composite wind turbine blade under combined bending and torsion, *Compos. Struct.*, 160, 435–445, <https://doi.org/10.1016/j.compstruct.2016.10.086>, 2017.
- Drela, M.: XFOIL: An analysis and design system for low Reynolds number airfoils, in: *Low Reynolds Number Aerodynamics*, edited by
 520 Mueller, T. J., vol. 54 of *Lecture Notes in Engineering*, pp. 1–12, Springer, Berlin, Heidelberg, https://doi.org/10.1007/978-3-642-84010-4_1, 1989.
- Fowler, M. J., Kimball, R. W., Thomas, III, D. A., and Goupee, A. J.: Design and testing of scale model wind turbines for use in wind/wave basin model tests of floating offshore wind turbines, in: *Proceedings of the ASME 2013 32nd International Conference on Ocean, Offshore and Arctic Engineering*, p. V008T09A004, American Society of Mechanical Engineers, Nantes, France,
 525 <https://doi.org/10.1115/OMAE2013-10122>, 2013.
- Fu, W., Sebastiani, A., Peña, A., and Mann, J.: Dependence of turbulence estimations on nacelle lidar scanning strategies, *Wind Energ. Sci.*, 8, 677–690, <https://doi.org/10.5194/wes-8-677-2023>, 2023.
- Guo, F., Gao, Z., and Schlipf, D.: TorqTwin—An open-source reference multibody modeling framework for wind turbine structural dynamics, *Renewable Energy*, 235, 121 268, <https://doi.org/10.1016/j.renene.2024.121268>, 2024.
- 530 Hao, H., Liao, K., Ma, Q., Zheng, X., Sun, H., and Khayyer, A.: Wind turbine model-test method for achieving similarity of both model- and full-scale thrusts and torques, *Appl. Ocean Res.*, 130, 103 444, <https://doi.org/10.1016/j.apor.2022.103444>, 2023.
- Jonkman, J. M. and Buhl, Jr., M. L.: FAST User’s Guide, Tech. Rep. NREL/EL-500-38230, National Renewable Energy Laboratory, Golden, Colorado, https://openfast.readthedocs.io/en/main/_downloads/d8bd014121d6505cb25cf49bee5eaa80/Old_FAST6_UsersGuide.pdf, 2005.
- 535 Liu, H., Guo, F., Yang, L., Gao, Z., and Song, Q.: Aeroelastic study of large offshore wind turbines under blade failure and detachment events, *Ocean Eng.*, 362, 126 304, <https://doi.org/10.1016/j.oceaneng.2026.126304>, 2026.



- Mishnaevsky, Jr., L.: Root causes and mechanisms of failure of wind turbine blades: Overview, *Materials*, 15, 2959, <https://doi.org/10.3390/ma15092959>, 2022.
- 540 Ning, A.: A simple solution method for the blade element momentum equations with guaranteed convergence, *Wind Energy*, 17, 1327–1345, <https://doi.org/10.1002/we.1636>, 2014.
- OpenFAST: AeroDyn Users Guide and Theory Manual, <https://openfast.readthedocs.io/en/v4.0.5/source/user/aerodyn/index.html>, open-FAST v4.0.5 documentation, last access: 17 September 2026, 2025.
- van Dooren, M. F., Campagnolo, F., Sjöholm, M., Angelou, N., Mikkelsen, T., and Kühn, M.: Demonstration and uncertainty analysis of synchronised scanning lidar measurements of 2-D velocity fields in a boundary-layer wind tunnel, *Wind Energ. Sci.*, 2, 329–341, <https://doi.org/10.5194/wes-2-329-2017>, 2017.
- 545 Veers, P., Bottasso, C. L., Manuel, L., Naughton, J., Pao, L., Paquette, J., Robertson, A., Robinson, M., Ananthan, S., Barlas, T., Bianchini, A., Bredmose, H., González Horcas, S., Keller, J., Madsen, H. A., Manwell, J., Moriarty, P., Nolet, S., and Rinker, J.: Grand challenges in the design, manufacture, and operation of future wind turbine systems, *Wind Energ. Sci.*, 8, 1071–1131, <https://doi.org/10.5194/wes-8-1071-2023>, 2023.
- 550 Vittori, F., Azcona, J., Eguinoa, I., Pires, O., Rodríguez, A., Morató, Á., Garrido, C., and Desmond, C.: Model tests of a 10 MW semi-submersible floating wind turbine under waves and wind using hybrid method to integrate the rotor thrust and moments, *Wind Energ. Sci.*, 7, 2149–2161, <https://doi.org/10.5194/wes-7-2149-2022>, 2022.
- Wu, X., Feng, K., and Li, Q.: A numerical method for the dynamics analysis of blade fracture faults in wind turbines using geometrically exact beam theory and its validation, *Energies*, 17, 824, <https://doi.org/10.3390/en17040824>, 2024.