

Article

Wind-Induced Vibration Behaviour of Additional Conductor and Service Safety of Fittings in Railway Overhead Contact Lines Under Canyon Winds in Harsh Environments

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Abstract

This study investigates the wind-induced vibration of railway overhead contact line additional conductors and the service safety of fittings in high-altitude canyon environments. Using the Dadu River canyon railway corridor as the engineering background, a three-dimensional stochastic wind field with temporal and spatial correlations is generated by combining the Kaimal, Tieleman and Panofsky spectra with a fourth-order autoregressive model. Wind-speed histories are converted into wind loads using quasi-steady aerodynamic theory. A geometrically nonlinear model based on the absolute nodal coordinate formulation is established, with suspension and support fittings represented by a swingable rod element and three-directional equivalent stiffness, respectively. Time-domain, rainflow-counting, power-spectral and parameter-sensitivity analyses are performed. Results show that fitting type strongly affects conductor-end response but has limited influence on mid-span vibration. Suspension fittings release wind-induced deformation through swinging, reducing stress peaks and high-amplitude cycles, whereas support fittings restrain end displacement but increase local constraint reactions and fatigue risk. The findings support fitting selection and wind-resistant fatigue design for additional conductors in canyon railway overhead contact lines.

Keywords: railway overhead contact line; additional conductor; canyon wind; wind-induced vibration; fitting; absolute nodal coordinate formulation



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1. Introduction

As electrified railways increasingly extend into high-altitude regions, deeply incised canyons and complex mountainous terrain, the natural environmental conditions encountered by railway traction-power-supply facilities are becoming increasingly severe. Railway sections represented by the Dadu River canyon are generally characterised by high elevation, pronounced topographic relief, narrow valleys and complex canyon orientations. Under the combined effects of canyon contraction, terrain-induced uplift near the bridge deck and the near-surface boundary layer, the approaching flow may undergo substantial acceleration, deflection and turbulence amplification in local areas, resulting in marked spatial non-uniformity and non-stationary fluctuations in the wind field at the bridge site [1–3].

The safe and stable operation of railway power-supply lines is essential to electrified railways. Additional conductors in a railway overhead contact line are flexible conductors installed along the railway and assigned specific electrical functions, distinct from the contact wire and messenger wire. They generally include feeder wires, reinforcing wires, return wires and protection wires. A typical electrified railway overhead contact line system is illustrated in Figure 1. The main structural parameters and design specifications of the investigated OCS are summarised in Table 1. Although additional conductors do not directly participate in the sliding current collection between the pantograph and contact wire, they perform essential functions, including traction-power transmission, return-current organisation, potential equalisation and system protection, and are therefore important components for ensuring continuous traction-power-supply operation and electrical safety [4–7]. Additional conductors are generally connected to masts, bridges or other fixed structures through fittings.

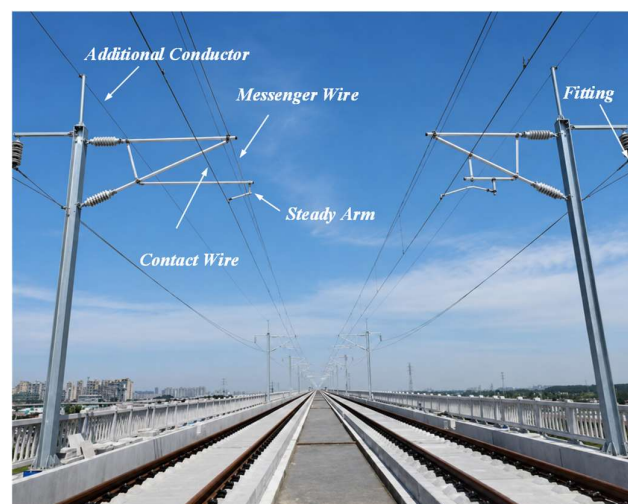


Figure 1. Schematic of an electrified railway overhead contact line system.

Table 1. Structural parameters of the electrified railway overhead contact line system.

Conductor Parameters	Property	Value
Contact Wire	Line Density	1.082 kg/m
	Tensile Rigidity	10^6 N/m
Messenger Wire	Line Density	1.068 kg/m
	Tensile Rigidity	10^6 N/m
Additional Conductor	Line Density	1.025 kg/m
	Tensile Rigidity	10^6 N/m
Dropper	Line Density	0.14 kg/m
	Tensile Rigidity	10^5 N/m
Stagger	Maximum Lateral Offset	± 0.2 m
Installation Height	Contact-Wire Height	5.3 m
Encumbrance	Vertical Distance between the Messenger and Contact Wires	1.6 m

Additional conductors are characterised by long spans, high initial tension, and comparatively low transverse stiffness and structural damping, making them highly sensitive to fluctuating wind loads. Under strong canyon winds, aerodynamic forces varying in both time and space continuously act along the conductor span and may excite random forced vibration dominated by buffeting, which is referred to here as wind-induced vibration [8,9]. When the dominant energy band of the fluctuating wind approaches a natural frequency

of an additional conductor, its lateral and vertical responses may be further amplified, accompanied by pronounced three-dimensional large displacement and geometric nonlinearity. Sustained wind-induced vibration not only increases wind-deflection amplitudes and the risk of clearance intrusion, but also subjects the support locations to repeatedly varying tension, bending action and constraint reactions. Over long-term operation, these effects may induce cumulative wear, connection loosening and fatigue damage [10,11], thereby reducing the service life of the additional conductors and fittings and impairing the reliability of the traction-power-supply system, as shown in Figure 2.

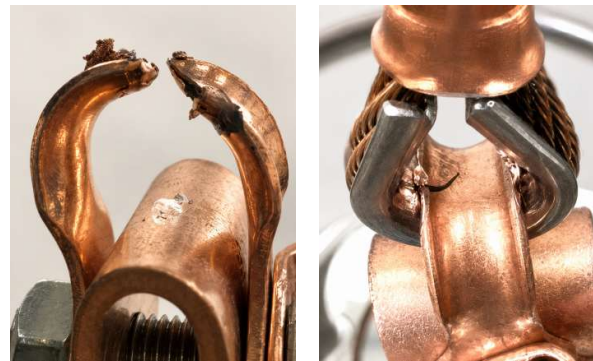


Figure 2. Typical damage to fittings.

In recent years, wind-induced vibration has remained an important research topic in bridges, overhead transmission lines, tall flexible structures and other engineering fields. For bridge structures, previous studies have systematically investigated typical wind-induced vibration modes, including buffeting, vortex-induced vibration, flutter, cable galloping and rain–wind-induced vibration, and have progressively established corresponding aerodynamic theories and dynamic-response analysis frameworks. Bridge flutter and buffeting analyses are founded on Scanlan’s flutter-derivative theory and Davenport’s stochastic wind-load theory, respectively. On this basis, da Costa et al. [12] developed a bridge-buffeting theory applicable to skew winds; Cui et al. [13] proposed a reduced-order buffeting-analysis method considering both geometric and aerodynamic nonlinearities; Zhao et al. [14] established an instantaneous power-balance method for predicting the critical flutter wind speed of bridge girders; and Li et al. [15] further developed three-dimensional multimode and full-mode nonlinear flutter-analysis approaches for long-span flexible bridges. These studies provide a theoretical and methodological basis for describing bridge aerodynamic forces and calculating dynamic responses under complex wind fields. Zhang et al. [16] further reviewed flutter, vortex-induced vibration and rain–wind-induced vibration of long-span cable-supported bridges, including suspension and cable-stayed bridges, and systematically summarised wind-tunnel testing and computational fluid dynamics methods. For wind-induced vibration of transmission lines, Zhang et al. [17] established a nonlinear hybrid dynamic model for the galloping of an iced quad-bundle conductor based on spatial curved-beam theory and solved the coupled lateral, vertical and torsional responses using modal superposition and time-domain integration. Chen et al. [18] developed a finite-element model of an iced conductor using a three-node isoparametric cable element and proposed a nonlinear numerical method applicable to steady, unsteady and stochastic wind fields by combining random-wind-field simulation with the probability density evolution method. Han et al. [19] employed a curved-beam finite-element model with three translational degrees of freedom and one rotational degree of freedom at each node and investigated the multi-degree-of-freedom coupled galloping of iced transmission lines while considering both geometric and aerodynamic nonlinearities induced by large-amplitude conductor motion. For wind-induced vibration of railway

overhead contact lines, Shi et al. [20] developed a three-dimensional pantograph–overhead contact line coupled model under crosswind and compared the applicability of different pantograph–overhead contact line modelling approaches while examining the effects of wind angle of attack, contact-wire stagger, panhead irregularity and pantograph–overhead contact line friction on current-collection performance. Song et al. [21,22] subsequently established a pantograph–overhead contact line coupled model incorporating overhead contact line geometric nonlinearity and the aerodynamic loads acting on both the pantograph and overhead contact line. Aerodynamic loads on pantograph components were obtained through computational fluid dynamics, while a spatial fluctuating wind field distributed along the overhead contact line was generated from empirical wind spectra to investigate current-collection performance under crosswinds. On this basis, Song et al. [23,24] developed a nonlinear finite-element model of the overhead contact line using the absolute nodal coordinate formulation, derived conductor aerodynamic forces according to quasi-steady theory; obtained aerodynamic coefficients through wind-tunnel tests; and constructed a three-dimensional stochastic wind field containing along-wind, crosswind and vertical fluctuating components. The time-domain, frequency-domain and extreme-value characteristics of contact-wire wind deflection were then analysed. Luo et al. [25] used the Davenport spectrum to simulate random crosswinds and investigated the coupled pantograph–overhead contact line response and wind resistance of different longitudinal suspension configurations. Duan et al. [26,27] obtained aerodynamic coefficients of contact-wire sections with different wear levels and wind angles of attack through wind-tunnel testing and combined these data with a nonlinear finite-element model to investigate aerodynamic instability and galloping of the contact wire, thereby providing experimental support for aerodynamic modelling and galloping simulation. Meanwhile, the absolute nodal coordinate formulation can describe large displacement, large rotation and coupled axial-bending deformation of flexible cables in a unified manner, and it has recently been applied to overhead contact line initial-configuration calculation, nonlinear crosswind-response analysis and modelling of complex supporting structures [28,29].

As key connecting components that transmit tension, bending moment and constraint reactions between conductors and supporting structures, fittings have dynamic properties that directly affect the operational reliability of line systems. Compared with the conductor itself, fittings generally exhibit a complex geometry, abrupt local-stiffness changes, variable contact boundaries and pronounced stress concentrations. Under wind-induced vibration, periodic conductor motion subjects the fitting body and the conductor-fitting contact region to repeatedly varying axial, bending and contact loads, together with small-amplitude relative slip. Long-term cyclic loading may induce contact wear, fretting damage, crack initiation and fatigue failure. Regarding conductor-fitting coupling, Lalonde et al. [30] established a three-dimensional finite-element model of an ACSR conductor-suspension-clamp system and analysed contact stresses and relative slip between strands and at the strand–clamp interface under cyclic bending. Omrani et al. [31] further combined a conductor-clamp numerical model with fretting-fatigue tests, enabling a multiscale assessment from the global wind-induced response to local contact-fatigue life. For U-shaped connectors in transmission lines under strong winds, Yi et al. [32] developed a coupled line-fitting dynamic model and combined it with reciprocating-wear tests to predict wear and service life. Said et al. [33] adopted a global–local multiscale method to predict contact loads, crack-initiation locations and fretting-fatigue life in clamped regions. In addition, Yang et al. [34] established an electrical–thermal–mechanical coupled model incorporating surface roughness, frictional heating, oxidation loss and contact wear, and demonstrated that the actual contact morphology has a significant influence on the wear evolution of fittings.

However, existing studies have mainly focused on contact wires and messenger wires, with evaluation indices dominated by wind deflection, mid-span vibration, pantograph–overhead contact line contact force and aerodynamic stability. Clamps and connectors are commonly simplified as concentrated masses, ideal hinges or rigid constraints, with insufficient attention paid to the swing freedom and constraint characteristics of fittings for additional conductors and the resulting redistribution of end stress. Moreover, prototype wind speeds considered in existing overhead contact line numerical analyses and wind-tunnel tests are generally within a relatively low range, while the structural parameters usually correspond to the tension levels of conventional overhead contact line systems. In particular, combined conditions involving extreme canyon wind speeds and highly tensioned additional conductors have rarely been investigated. The idealised boundary conditions commonly adopted in current studies cannot directly represent the difference between swingable and strongly constrained fittings for additional conductors, nor can they provide reliable stress time histories for fitting fatigue-load spectra and life assessment.

To address these limitations, this study investigates the wind-induced vibration behaviour of additional conductors and the service safety of different fittings under extreme canyon winds, using an additional conductor system in a Dadu River canyon railway section as the research object. The principal contributions are as follows:

- (1) To represent wind-speed acceleration, flow deflection and turbulence amplification caused by canyon topography, a three-dimensional stochastic wind field with temporal and spatial correlations is constructed using the Kaimal, Tieleman and Panofsky spectra, with an extreme design wind speed of 46.8 m/s as the upper limit. A conversion method from wind-speed time histories to wind-load time histories of the additional conductors is then established according to quasi-steady aerodynamic theory.
- (2) A nonlinear dynamic model of the additional conductors is established using the absolute nodal coordinate formulation to describe large displacement, large rotation and coupled axial-bending deformation. The suspension fitting is represented by a swingable ANCF rod element, while the local constraint imposed by the support fitting is represented by three-directional equivalent stiffness at the support node, allowing the spatial dynamic response of the additional conductor system under different fitting configurations to be described within a unified framework.
- (3) Under combined extreme-high-wind-speed and high-tension conditions, the conductor-end displacement, mid-span vibration and local stress responses associated with suspension and support fittings are compared. The effects of span length and initial tension on fitting service conditions are further investigated. By extracting stress time histories near the fittings, stress-amplitude distributions and frequency characteristics, the key dynamic loads of the additional conductor-fitting system under extreme wind are identified, providing a theoretical basis for subsequent cumulative-fatigue-damage calculation, fitting selection, wind-resistant design and service-life extension of additional conductor systems.

2. Generation of Canyon Wind-Load Time Histories

Canyon terrain is characterised by substantial relief, and airflow may undergo pronounced acceleration, deflection and turbulence amplification under the combined effects of canyon contraction, terrain-induced uplift near the bridge deck and boundary-layer adjustment. Compared with open flat terrain, the mean wind speed, wind direction and fluctuating-wind energy at canyon bridge sites exhibit stronger spatial non-uniformity. Additional conductors in railway overhead contact lines are typical long-span highly flexible cable structures with comparatively low transverse stiffness and structural damping and are therefore prone to sustained vibration under stochastic fluctuating winds. Consequently,

an equivalent static wind pressure alone cannot represent the transient loading experienced by the additional conductors and fittings under canyon winds. On the basis of the specified canyon mean wind speed, this study employs a three-dimensional stochastic-wind-field model to generate fluctuating wind-speed time histories with temporal and spatial correlations and then converts the wind-speed histories into wind-load histories acting on the additional conductors and fittings according to quasi-steady aerodynamic theory.

2.1. Decomposition of the Canyon Wind Field and Target Spectra of Three-Dimensional Fluctuating Wind Speeds

Over a finite time interval, the canyon wind field may be approximated as a stationary stochastic process. The instantaneous wind velocity at an arbitrary spatial position can be decomposed into a mean component and a zero-mean fluctuating component as follows:

$$U(p, t) = \bar{U}(p) + u'(p, t), \quad (1)$$

where $p = [x, y, z]^T$ is the spatial-position vector, $\bar{U}(p)$ is the mean wind-velocity vector, and $u'(p, t)$ is the fluctuating wind-velocity vector.

According to the report Research Progress on the Wind Field in the Dadu River Canyon of the Sichuan–Tibet Railway, prepared by the Institute of Physics, Chinese Academy of Sciences, the 100-year-return-period maximum 10 min mean wind speed at a height of 10 m above the bridge site is 21.6 m/s. However, the stiffening girder of the Dadu River Grand Bridge is approximately 375 m above the valley floor, and the OCS at deck level is therefore located far above the conventional 10 m reference height. In addition, the bridge site is affected by canyon contraction, topographic uplift and local airflow acceleration near the bridge deck. After accounting for the bridge-deck elevation, canyon topography and height variation, the reference design wind speed at deck level was determined as 41.7 m/s. For a 3 m high wind barrier with a porosity of 30%, and considering that wind pressure is proportional to the square of wind speed, the corrected design wind speed under the influence of the wind barrier can be expressed as

$$\bar{U}_c = \bar{U}_b \sqrt{\beta_s}, \quad (2)$$

where $\bar{U}_b = 41.7$ m/s is the design reference wind speed at bridge-deck level. The wind-pressure correction coefficient, β_s , was obtained from 1:50-scale canyon wind-tunnel tests and CFD simulations considering wind-yaw angles of 0° and 30° . Accordingly, the corrected design wind speed at the canyon bridge deck was determined as 46.8 m/s. Accordingly, 46.8 m/s is adopted as the upper limit of the canyon mean wind speed after wind-barrier correction and is used to construct stochastic wind-speed time histories for the extreme-wind case.

To describe the distribution of canyon-wind energy over different frequency ranges, the Kaimal spectrum [35], Tieleman spectrum [36] and Panofsky spectrum [37] are adopted for the along-wind, crosswind and vertical fluctuating velocity components, respectively. The corresponding one-sided power spectral densities are expressed as

$$\begin{aligned} S_h(z, n) &= \frac{200\bar{v}_z^2 f}{n(1+50f)^{5/3}}; f = \frac{nz}{\bar{v}_z} \\ S_p(z, n) &= \frac{13\bar{v}_z^2 f}{n(1+20.16f)^{5/3}}; f = \frac{nz}{\bar{v}_z} \\ S_v(z, n) &= \frac{6\bar{v}_z^2 f}{n(1+4f)^2}; f = \frac{nz}{\bar{v}_z}, \end{aligned} \quad (3)$$

where $S_h(z, n)$, $S_p(z, n)$ and $S_v(z, n)$ are the spectra of the along-wind, crosswind and vertical fluctuating velocity components, respectively; n is the wind-spectrum frequency;

and $\bar{v}_z$ is the steady wind speed at reference height z . These empirical spectra describe the frequency-dependent decay of fluctuating-wind energy in the three directions and have been used to construct spatial stochastic wind fields for railway overhead contact lines.

A fourth-order autoregressive (AR) model is combined with the empirical wind spectra to simulate multidirectional stochastic wind-speed time histories. Specifically, at a given time, t , the stochastic wind velocity, $V(\mathbf{X}, \mathbf{Y}, \mathbf{Z}, t)$, at a spatial point can be expressed as

$$\begin{aligned} V(\mathbf{X}, \mathbf{Y}, \mathbf{Z}, t) &= -\sum_{i=1}^4 \boldsymbol{\varphi}_i V(\mathbf{X}, \mathbf{Y}, \mathbf{Z}, t - i\Delta t) + \mathbf{N}(t) \\ \mathbf{X} &= [x_1, x_2, \dots, x_m]^T \\ \mathbf{Y} &= [y_1, y_2, \dots, y_m]^T \\ \mathbf{Z} &= [z_1, z_2, \dots, z_m]^T \end{aligned} \quad (4)$$

where $\mathbf{X}$, $\mathbf{Y}$ and $\mathbf{Z}$ are vectors of the spatial coordinates; m is the total number of spatial points; Δt is the time step; $\boldsymbol{\varphi}_i$ is the autoregressive-coefficient matrix; and $\mathbf{N}_k(t)$ is the noise term. The autoregressive-coefficient matrix, $\boldsymbol{\varphi}_i$, and the stochastic sequence, $\mathbf{N}_k(t)$, are determined as follows:

$$R_{gh}(t) = \int_0^\infty S_{gh}(f) \cos(2\pi ft) d\omega, \quad (5)$$

where $g, h = 1, 2, \dots, m$ denotes the different directions at the spatial points.

The autocorrelation function, $R(j\Delta t)$, represents the correlation between wind velocities at different locations for a specified time lag. The correlation function can be written in matrix form as

$$R(j\Delta t) = \begin{bmatrix} R_{11}(j\Delta t) & R_{12}(j\Delta t) & \dots & R_{1n}(j\Delta t) \\ R_{21}(j\Delta t) & R_{22}(j\Delta t) & \dots & R_{2n}(j\Delta t) \\ \vdots & \vdots & \dots & \vdots \\ R_{n1}(j\Delta t) & R_{n2}(j\Delta t) & \dots & R_{nn}(j\Delta t) \end{bmatrix}, \quad (6)$$

where $j = 1, 2, \dots, p$. Each term, $R(j\Delta t)$, reflects the temporal correlation of the wind velocity and can be further determined from

$$R(j\Delta t) = \sum_{i=1}^p R[(j-i)\Delta t] \boldsymbol{\varphi}_T^i, \quad (7)$$

Accordingly, the autocorrelation matrix, $R(0)$, in the wind-field model and its associated stochastic-process model can be further expanded as

$$R(0) = \sum_{i=1}^p \boldsymbol{\varphi}_i R(i\Delta t) + R_N, \quad (8)$$

To implement this procedure, Cholesky decomposition is used to obtain the correlation matrix, $\mathbf{R}_N$, of the stochastic process, with $\mathbf{R}_N = \mathbf{L}\mathbf{L}^T$. The noise term, $\mathbf{N}(t)$, of the stochastic wind velocity is therefore

$$\mathbf{N}(t) = \mathbf{L} \cdot \mathbf{Q}(t), \quad (9)$$

where $\mathbf{Q}(t) = [\mathbf{Q}_1(t), \mathbf{Q}_2(t) \dots \mathbf{Q}_n(t)]^T$ is an independent normally distributed stochastic process. $\mathbf{Q}_k(t)$ denotes the normally distributed stochastic-process sequences of the wind-velocity components in the different directions, ensuring both randomness and autocorrelation in the simulation. The stochastic wind velocities generated using this method are shown in Figure 3. The black lines represent the auto power spectra of the simulated wind-speed time histories, while the red lines represent the target spectra.

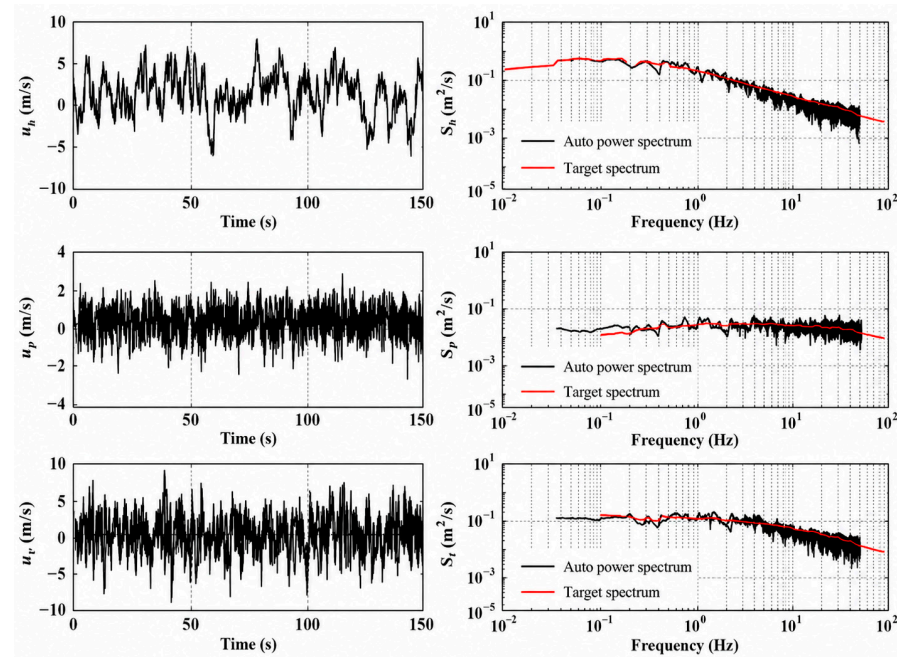


Figure 3. Three-directional stochastic wind-speed time histories.

2.2. Conversion from Wind-Speed Time Histories to Wind-Load Time Histories

After the three-dimensional wind-speed time histories at each spatial position are obtained, quasi-steady aerodynamic theory is used to calculate the transient wind loads acting on the additional conductors. Because conductor motion changes both the relative flow velocity at the cross-section and the effective angle of attack, the ambient wind velocity and conductor velocity must be considered simultaneously in the aerodynamic-force calculation. The coordinate transformation and the decomposition of lift and drag are illustrated in Figure 4. As illustrated in Figure 4, the left-hand side presents the spatial model of the additional conductor, while the right-hand side shows its cross-sectional view and the decomposition of the aerodynamic forces. Plane 1 and Plane 2 are two mutually perpendicular planes used to decompose the wind velocity into three directional components.

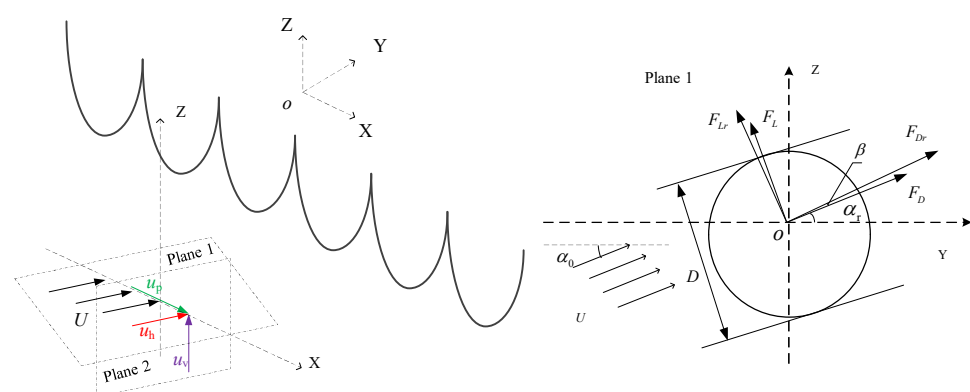


Figure 4. Relationship between wind velocity and the decomposition of lift and drag.

Because the wind-load component along the X-axis makes a negligible contribution to structural wind-induced vibration, only the lateral and vertical motions of the additional conductor are considered. The relative flow velocity at the cross-section is given by

$$U_r(t) = \sqrt{[U + u_h(t) - \dot{y}(t)]^2 + [u_v(t) - \dot{z}(t)]^2}, \quad (10)$$

The corresponding effective angle of attack is

$$\alpha_r(t) = \alpha_0 + \arctan \left[\frac{u_v(t) - \dot{z}(t)}{U + u_h(t) - \dot{y}(t)} \right], \quad (11)$$

where $\dot{y}(t)$ and $\dot{z}(t)$ are the lateral and vertical velocities of the additional conductor cross-section, respectively; α_0 is the initial angle of attack corresponding to the mean wind; and α_r is the effective angle of attack considering both fluctuating wind and conductor motion.

The drag and lift per unit length of the additional conductor are respectively expressed as

$$\begin{aligned} F_{Lr}(t) &= \frac{1}{2} \rho_a D C_L[\alpha_r(t)] U_r^2(t) \\ F_{Dr}(t) &= \frac{1}{2} \rho_a D C_D[\alpha_r(t)] U_r^2(t), \end{aligned} \quad (12)$$

where ρ_a is the air density; D is the characteristic diameter of the additional conductor; and C_L and C_D are the drag and lift coefficients, respectively. The aerodynamic coefficients of the additional conductor were obtained through CFD calculations based on its actual cross-sectional geometry. A structured mesh containing more than 40,000 cells was employed. The height of the first near-wall mesh layer was set to 0.04 mm, and the boundary-layer growth rate was set to 1.1, ensuring that $y^+ < 1$. A velocity-inlet boundary condition was applied at the inlet, while the conductor surface was treated as a no-slip wall. The SST $k-\omega$ turbulence model was adopted. The residual convergence criterion was set to 10^{-5} , and the time step was 5×10^{-5} s, with a total of 2000 time steps calculated for each case. During the time-domain simulation, the aerodynamic coefficients at intermediate angles of attack were obtained through linear interpolation between the adjacent CFD data points.

Transforming the drag and lift into the global coordinate system gives the lateral and vertical wind loads per unit length:

$$\begin{aligned} F_Y(t) &= F_{Dr}(t) \cos[\alpha_r(t)] - F_{Lr}(t) \sin[\alpha_r(t)] \\ F_Z(t) &= F_{Dr}(t) \sin[\alpha_r(t)] + F_{Lr}(t) \cos[\alpha_r(t)], \end{aligned} \quad (13)$$

Substitution into the above wind-load calculation procedure yields the wind-load distributions acting on the additional conductor, as shown in Figure 5.

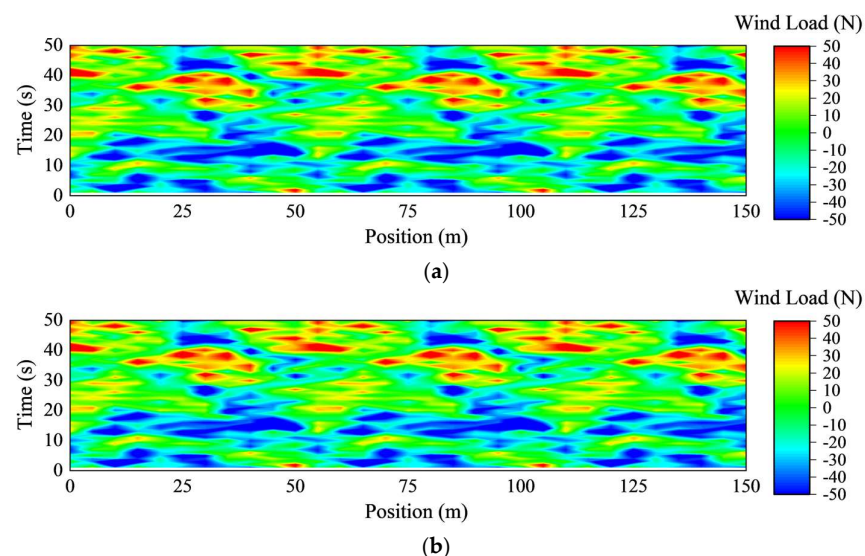


Figure 5. Wind loads acting on the additional conductor. (a) Lateral wind load on a 150 m span of the additional conductor at a wind speed of 46.8 m/s. (b) Vertical wind load on a 150 m span of the additional conductor at a wind speed of 46.8 m/s.

3. Development of the Additional Conductor and Fitting Models

Based on the canyon wind-speed histories and aerodynamic model of the additional conductors described above, a spatial dynamic model capable of representing conductor large displacement, fitting swing and local constraints at support locations is required to determine the nonlinear dynamic response of the additional conductor system under wind loading. Additional conductors have long spans, appreciable initial tension and low transverse stiffness, and may therefore exhibit pronounced geometric nonlinearity under strong winds. Meanwhile, different fitting types have distinctly different load-transfer mechanisms. The typical fittings considered in this study are the suspension and support types, as illustrated in Figure 6. The suspension fitting primarily carries axial tension while permitting swing within a certain range, whereas the support fitting restricts displacement of the additional conductor near the support point through local constraints.

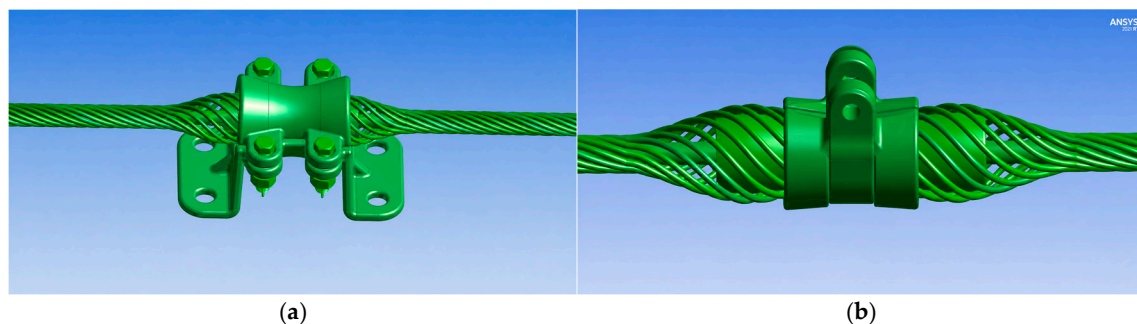


Figure 6. Structural models of the fittings. (a) Support fitting. (b) Suspension fitting.

Existing overhead contact line modelling studies have employed 12-degree-of-freedom beam elements based on the absolute nodal coordinate formulation to describe the geometric nonlinearity of contact and messenger wires, together with tension-only nonlinear axial elements to represent dropper slackening. These models can simultaneously account for large displacement, large rotation and coupled axial-bending deformation of the cables. Building on this framework, the present study extends the absolute nodal coordinate formulation (ANCF) beam element to model the additional conductors, represents the suspension fitting as an ANCF rod element carrying axial action only, and describes the elastic constraint of the support fitting by modifying the local stiffness matrix of the additional conductor ANCF beam element. ANCF employs the absolute nodal position and position gradient as generalised coordinates and is well suited to large-rotation and large-deformation analysis of flexible line structures. Its mass matrix is independent of the element configuration, avoiding the additional calculations required by continuous updating of local coordinate systems in conventional co-rotational formulations.

3.1. ANCF Beam Element for the Additional Conductor

The additional conductor is discretised using a two-node ANCF beam element with six degrees of freedom per node, as shown in Figure 7. The arrows shown in the ANCF beam element indicate the local direction of axial deformation associated with beam elongation.

Each node contains three position coordinates and three position-gradient coordinates; therefore, each beam element has 12 generalised coordinates. For an arbitrary additional conductor beam element, the vector of generalised coordinates is expressed as

$$\mathbf{e}_b = \left[\mathbf{r}_i \ \mathbf{r}_x^i \ \mathbf{r}_j \ \mathbf{r}_x^j \right]^T, \quad (14)$$

where $\mathbf{r}_i$ and $\mathbf{r}_j$ are the position vectors of the two end nodes of the element in the global coordinate system, respectively, namely:

$$\mathbf{r}_i = \begin{bmatrix} x_i & y_i & z_i \end{bmatrix} \quad \mathbf{r}_j = \begin{bmatrix} x_j & y_j & z_j \end{bmatrix}, \quad (15)$$

where $\mathbf{r}_x^i$ and $\mathbf{r}_x^j$ are the first derivatives of the nodal position vectors with respect to the material coordinate, χ , and describe the tangent directions of the conductor neutral axis at the nodes.

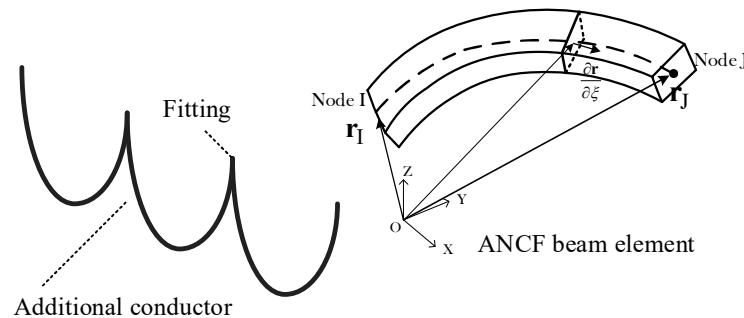


Figure 7. ANCF model of the additional conductor.

Let the unstressed length of the element be L_0 . The dimensionless local coordinate is defined as

$$\xi = \frac{\chi}{L_0}, \quad (16)$$

The position vector of an arbitrary material point within the element can be expressed as

$$\mathbf{r}(\chi, t) = \mathbf{S}(\xi) \mathbf{e}_b(t), \quad (17)$$

where the shape-function matrix is

$$\mathbf{S} = \begin{bmatrix} S_1 \mathbf{I}_3 & S_2 \mathbf{I}_3 & S_3 \mathbf{I}_3 & S_4 \mathbf{I}_3 \end{bmatrix}, \quad (18)$$

where $\mathbf{I}_3$ is the third-order identity matrix and the scalar shape functions are

$$\begin{aligned} S_1 &= 1 - 3\xi^2 + 2\xi^3 \\ S_2 &= L_0(\xi - 2\xi^2 + \xi^3) \\ S_3 &= 3\xi^2 - 2\xi^3 \\ S_4 &= L_0(-\xi^2 + \xi^3) \end{aligned} \quad (19)$$

Differentiating Equation (17) with respect to the material coordinate, χ , gives the first and second position gradients of the conductor neutral axis:

$$\mathbf{r}_\chi = \mathbf{S}_\chi \mathbf{e}_b \quad \mathbf{r}_{\chi\chi} = \mathbf{S}_{\chi\chi} \mathbf{e}_b, \quad (20)$$

When the material coordinate, χ , is taken as the arc-length coordinate in the unstressed configuration, the axial strain of the conductor can be expressed as

$$\varepsilon = \frac{1}{2} \left(\mathbf{r}_\chi^T \mathbf{r}_\chi - 1 \right), \quad (21)$$

The curvature of the conductor neutral axis is

$$\kappa = \frac{\|\mathbf{r}_\chi \times \mathbf{r}_{\chi\chi}\|}{\|\mathbf{r}_\chi\|^3}, \quad (22)$$

where ε and κ characterise the axial tensile deformation and bending deformation of the additional conductor, respectively. Neglecting shear and torsional deformation, the elastic potential energy of the additional conductor beam element is

$$U_b = \frac{1}{2} \int_0^{L_0} (EA\varepsilon^2 + EI\kappa^2) d\chi, \quad (23)$$

where E is the equivalent elastic modulus of the additional conductor, A is the cross-sectional area, and I is the second moment of area.

The generalised elastic internal-force vector of the additional conductor beam element is obtained by differentiating the elastic potential energy with respect to the generalised coordinates:

$$\mathbf{Q}_b = \frac{\partial U_b}{\partial \mathbf{e}_b}, \quad (24)$$

Substituting Equation (23) into Equation (24) gives

$$\mathbf{Q}_b = \int_0^{L_0} \left[EA\varepsilon \frac{\partial \varepsilon}{\partial \mathbf{e}_b} + EI\kappa \frac{\partial \kappa}{\partial \mathbf{e}_b} \right] d\chi, \quad (25)$$

where $\mathbf{Q}_b$ denotes the generalised elastic internal-force vector of the additional conductor beam element.

The corresponding tangent stiffness matrix is

$$\mathbf{K}_b = \frac{\partial \mathbf{Q}_b}{\partial \mathbf{e}_b}, \quad (26)$$

Because the axial strain and curvature vary with the current configuration, $\mathbf{Q}_b$ and $\mathbf{K}_b$ are nonlinear functions of the generalised coordinates. During the dynamic solution, the elastic internal force and tangent stiffness must be recalculated from the conductor configuration at each iteration. The ANCF beam element with an updated tangent stiffness matrix can therefore describe the spatial large deformation of the flexible conductor under wind loading.

Let the mass per unit length of the additional conductor be μ . The consistent mass matrix of the beam element is then

$$\mathbf{M}_b = \int_0^{L_0} \mu \mathbf{S}^T \mathbf{S} d\chi, \quad (27)$$

Because the shape functions of the ANCF beam element depend only on the material coordinate and the unstressed length, the mass matrix is independent of the current element configuration and can be calculated before the time-domain simulation.

The consistent damping matrix of the beam element is expressed as

$$\mathbf{C}_T = a_0 \mathbf{M}_T + a_1 \mathbf{K}_T, \quad (28)$$

where a_0 and a_1 are the mass-proportional and stiffness-proportional damping coefficients, respectively. Their values were determined based on the prescribed modal damping ratios at two selected natural frequencies.

3.2. ANCF Rod Element for the Suspension Fitting

The suspension fitting connects the fixed support and the additional conductor through a suspension component. Its principal function is to transmit axial tension while allowing the additional conductor to swing within a certain range under wind loading. In this study, the suspension fitting is represented by a two-node ANCF rod element in

which only axial deformation is considered, while bending and torsional deformations are neglected.

The generalised-coordinate vector of the suspension-fitting rod element is

$$\mathbf{e}_h = [\mathbf{r}_a \ \mathbf{r}_b]^T, \quad (29)$$

where $\mathbf{r}_a$ and $\mathbf{r}_b$ are the position vectors of the two end nodes of the rod element, respectively. The upper node is generally connected to the fixed support and the lower node to an additional conductor node.

The relative position vector between the two end nodes is defined as

$$\mathbf{d} = \mathbf{r}_b - \mathbf{r}_a, \quad (30)$$

The current length of the rod element is

$$l = \|\mathbf{d}\|, \quad (31)$$

Let the unstressed length of the suspension fitting be L_h . Its axial strain is

$$\varepsilon_h = \frac{\mathbf{d}^T \mathbf{d} - L_h^2}{2L_h^2}, \quad (32)$$

The elastic potential energy of the rod element is

$$U_h = \frac{1}{2} E_h A_h L_h \varepsilon_h^2, \quad (33)$$

where E_h and A_h are the equivalent elastic modulus and equivalent load-carrying area of the suspension fitting, respectively.

Define the constant matrix:

$$\mathbf{B} = \begin{bmatrix} -\mathbf{I}_3 & \mathbf{I}_3 \end{bmatrix}, \quad (34)$$

The relative position vector can then be expressed as

$$\mathbf{d} = \mathbf{B} \mathbf{e}_h, \quad (35)$$

The generalised elastic internal-force vector of the suspension fitting is

$$\mathbf{Q}_h = \frac{\partial U_h}{\partial \mathbf{e}_h}, \quad (36)$$

The corresponding tangent stiffness matrix is

$$\mathbf{K}_h = \frac{E_h A_h}{L_h} \left[\frac{\mathbf{B}^T \mathbf{d} \mathbf{d}^T \mathbf{B}}{L_h^2} + \varepsilon_h \mathbf{B}^T \mathbf{B} \right], \quad (37)$$

3.3. Equivalent-Stiffness Model of the Support Fitting

The support fitting is generally connected directly to a mast, cantilever or other fixed structure, and its primary function is to constrain the spatial displacement of the additional conductor at the support location. Because the fitting dimensions are small relative to the conductor span, and because local fitting stress and deformation are not the principal focus of this study, no independent flexible element is established for the support fitting. Instead, it is represented by three linear springs located at the support node of the additional conductor.

During mesh discretisation of the additional conductor, the installation coordinates of the support fitting are made coincident with an ANCF beam-element node. Let the equivalent stiffnesses of the support fitting in the global x , y and z directions be k_x , k_y and k_z , respectively. Its three-directional stiffness matrix is then

$$\mathbf{K}_s = \begin{bmatrix} k_x & 0 & 0 \\ 0 & k_y & 0 \\ 0 & 0 & k_z \end{bmatrix}, \quad (38)$$

Let the current position vector of the support node be

$$\mathbf{r}_s = \begin{bmatrix} x_s & y_s & z_s \end{bmatrix}, \quad (39)$$

The fixed reference position of the support fitting is

$$\mathbf{r}_{s0} = \begin{bmatrix} x_{s0} & y_{s0} & z_{s0} \end{bmatrix}, \quad (40)$$

The displacement of the support node relative to the fixed reference position is

$$\mathbf{u}_s = \mathbf{r}_s - \mathbf{r}_{s0}, \quad (41)$$

The restoring-force components exerted by the support fitting on the additional conductor node are

$$\begin{aligned} F_x &= -k_x(x_s - x_{s0}) \\ F_y &= -k_y(y_s - y_{s0}) \\ F_z &= -k_z(z_s - z_{s0}) \end{aligned} \quad (42)$$

This treatment describes the spatial-displacement constraint imposed by the support fitting on the additional conductor without introducing an independent fitting element or additional degrees of freedom.

3.4. System Dynamic Equations

After the initial equilibrium configuration is obtained, the additional conductor ANCF beam elements, suspension-fitting ANCF rod elements and nodal stiffnesses of the support fittings are mapped into the global coordinate system according to the finite-element assembly relationships.

Under canyon winds, the global dynamic equation of the additional conductor-fitting system can be written as

$$\mathbf{M}\ddot{\mathbf{X}}(t) + \mathbf{C}\dot{\mathbf{X}}(t) + \mathbf{K}\mathbf{X}(t) = \mathbf{F}_T(t) + \mathbf{F}_G + \mathbf{F}_{h/s}, \quad (43)$$

where $\mathbf{M}$ is the global mass matrix, $\mathbf{C}$ is the global damping matrix, and $\mathbf{K}$ is the global stiffness matrix; $\ddot{\mathbf{X}}(t)$, $\dot{\mathbf{X}}(t)$ and $\mathbf{X}(t)$ are the global generalised acceleration, velocity and coordinate vectors, respectively; $\mathbf{F}_T(t)$ is the global time-varying wind-load vector obtained from the canyon-wind model; $\mathbf{F}_G$ is the global gravitational-load vector; and $\mathbf{F}_{h/s}$ is the global restoring-force vector generated by the suspension or support fittings.

To solve the nonlinear dynamic response, a time step, Δt , is selected and the Newmark- β recursive scheme is adopted:

$$\begin{aligned} \ddot{\mathbf{X}}(t + \Delta t) &= \frac{1}{\alpha \Delta t^2}(\mathbf{X}(t + \Delta t) - \mathbf{X}(t)) - \frac{1}{\alpha \Delta t} \dot{\mathbf{X}}(t) - \left(\frac{1}{2\alpha} - 1\right) \ddot{\mathbf{X}}(t) \\ \dot{\mathbf{X}}(t + \Delta t) &= \dot{\mathbf{X}}(t) + (1 - \beta) \Delta t \ddot{\mathbf{X}}(t) + \beta \Delta t \ddot{\mathbf{X}}(t + \Delta t) \end{aligned} \quad (44)$$

where α and β control the interpolation accuracy. Values of $\alpha = 0.25$ and $\beta = 0.5$ are selected to obtain the constant-average-acceleration scheme.

The equation of motion at time $t + \Delta t$ is then obtained as

$$\bar{K}_T X(t + \Delta t) = \bar{F}_T(t + \Delta t), \quad (45)$$

where $\bar{K}_T$ is the effective stiffness matrix and $\bar{F}_T(t + \Delta t)$ is the effective load vector:

$$\bar{K}_T = K_T + \frac{1}{\alpha \Delta t^2} M_T + \frac{\beta}{\alpha \Delta t} C_T, \quad (46)$$

$$\begin{aligned} \bar{F}_T(t + \Delta t) = & F_T(t + \Delta t) + M_T \left(-\frac{1}{\alpha \Delta t^2} (X(t + \Delta t) - X(t)) + \frac{1}{\alpha \Delta t} \dot{X}(t) + \left(\frac{1}{2\alpha} - 1 \right) \ddot{X}(t) \right) \\ & + C_T \left(-\frac{\beta}{\alpha \Delta t} (X(t + \Delta t) - X(t)) + \left(\frac{\beta}{\alpha} - 1 \right) \dot{X}(t) + \frac{(\alpha - 2)\Delta t}{2\beta} \ddot{X}(t) \right) \end{aligned} \quad (47)$$

The above procedure enables the large-deformation dynamic response of the additional conductors to be solved. Because the stiffness matrix varies with time, the system exhibits nonlinear behaviour.

4. Numerical Simulation of Wind-Induced Vibration with Two Fitting Types

4.1. Reproduction of Wind-Induced Vibration Under the Two Fitting Configurations

To investigate the influence of fitting constraints on the wind-induced response of the additional conductor, a nonlinear dynamic model of a single span is established. In accordance with the Dadu River design specification, the span length is 50 m and the initial conductor tension is 12 kN. The additional conductor is discretised by ANCF beam elements of 0.5 m length, giving 100 elements in total. The suspension fitting is represented by the ANCF rod element described above, whereas the support fitting is implemented by constraining the spatial displacement of the conductor-end node. The time step is 0.001 s, the total simulation duration is 200 s, and the approaching-wind speed is 46.8 m/s. The two cases employed identical initial configurations, conductor parameters, aerodynamic coefficients, and fluctuating-wind histories. Specifically, the wind-speed histories were generated only once, and the same saved time histories of the along-wind, cross-wind, and vertical wind-speed components were subsequently applied to both fitting models. This setting eliminates the influence of differences between random wind-field realisations on the comparison results. During the calculation, the elastic internal force and tangent stiffness of the ANCF beam elements are updated in real time according to the current conductor configuration, thereby accounting for geometric nonlinearity under wind loading. This procedure is consistent with the step-by-step updating of structural stiffness and nonlinear internal force adopted in previous studies of overhead contact line wind-induced vibration. The three-directional equivalent stiffnesses of the support fitting were obtained through axial finite-element simulations conducted in ANSYS 2021 R1. A solid finite-element model of the support fitting was established, and uniaxial tensile loads were applied separately along the three orthogonal directions. The equivalent stiffness in each direction was then calculated from the corresponding load–displacement curve obtained from the simulations. Therefore, these stiffness values are numerical results derived from finite-element simulations rather than measured, design, or assumed parameters. The specific structural parameters are summarised in Table 2.

Table 2. Main structural and numerical parameters of the additional conductor model.

Conductor Parameters	Value
Conductor Diameter (mm)	20.6
Cross-Sectional Area (mm ²)	333.33
Mass (kg/km)	1058
Elastic Modulus (Gpa)	65.0
Standard Span Length (m)	50
Initial Tension (kN)	12
Stiffness of the Suspension Fitting (N/mm)	4000
Axial Stiffness of the Support Fitting (N/mm)	197
Horizontal Stiffness of the Support Fitting (N/mm)	126
Vertical Stiffness of the Support Fitting (N/mm)	126
Element Length (m)	0.5
Damping Coefficient	0.002

Figure 8 presents the conductor-end displacement responses under the two fitting configurations. The red curves correspond to the suspension fitting and the black curves to the support fitting. The conductor end undergoes pronounced horizontal and vertical displacements with the suspension fitting, whereas the end displacement remains close to its initial position with the support fitting. This difference arises because the suspension-fitting rod element can swing about its upper connection, allowing the conductor end to move spatially under wind loading, while the support fitting strongly restricts translational motion at the end. The horizontal–vertical displacement trajectory further shows a distinct arc-shaped path for the suspension-fitting case, indicating strong coupling between horizontal and vertical motions. By contrast, the support-fitting trajectory is concentrated near the coordinate origin. No uniform displacement limits are specified separately for the additional conductor, suspension fitting and support fitting. The allowable conductor displacement is governed by the required electrical and mechanical clearances under the maximum wind-deflection condition, whereas the fitting responses are assessed according to their allowable working ranges and structural strength. Therefore, the displacement results presented in this section are primarily used to compare the wind-induced dynamic responses associated with the two fitting constraint configurations.

By comparison, the mid-span displacements under the two fitting configurations show broadly consistent trends, as illustrated in Figure 9. In both cases, the horizontal displacement fluctuates continuously about a pronounced mean wind-deflected position, while the vertical displacement exhibits random oscillations of alternating sign. The mean horizontal displacement and the vertical fluctuation range are slightly larger with the suspension fitting, although the response amplitudes in the two cases remain of the same order. This indicates that, at the present tension and wind speed, the mid-span response is governed primarily by the distributed aerodynamic loading, initial conductor tension and global flexibility. The influence of the fitting type on the mid-span response is weaker than its influence on conductor-end motion.

To evaluate the influence of different fitting constraint configurations on the stress state at the end of the additional conductor, the stress time history was extracted from the conductor cross-section at the connection between the additional conductor and the fitting. This location serves both as the boundary constraint of the additional conductor and as the load-transfer interface, where the combined effects of axial force and bending moments are

relatively significant. Based on the instantaneous axial force and biaxial bending moments at the connection cross-section, the maximum normal stress is calculated as

$$\sigma(t) = \frac{N(t)}{A} + \frac{M_y(t)c_z}{I_y} + \frac{M_z(t)c_y}{I_z}, \quad (48)$$

where $N(t)$ is the instantaneous axial force at the connection cross-section, including the static axial force induced by the initial tension and the dynamic axial force caused by the wind load; $M_y(t)$ and $M_z(t)$ are the instantaneous bending moments about the local y and z-axes of the connection cross-section, respectively; A is the conductor cross-sectional area; I_y and I_z are the second moments of area in the corresponding directions; and c_y and c_z are the distances from the outermost edge of the cross-section to the corresponding neutral axes.

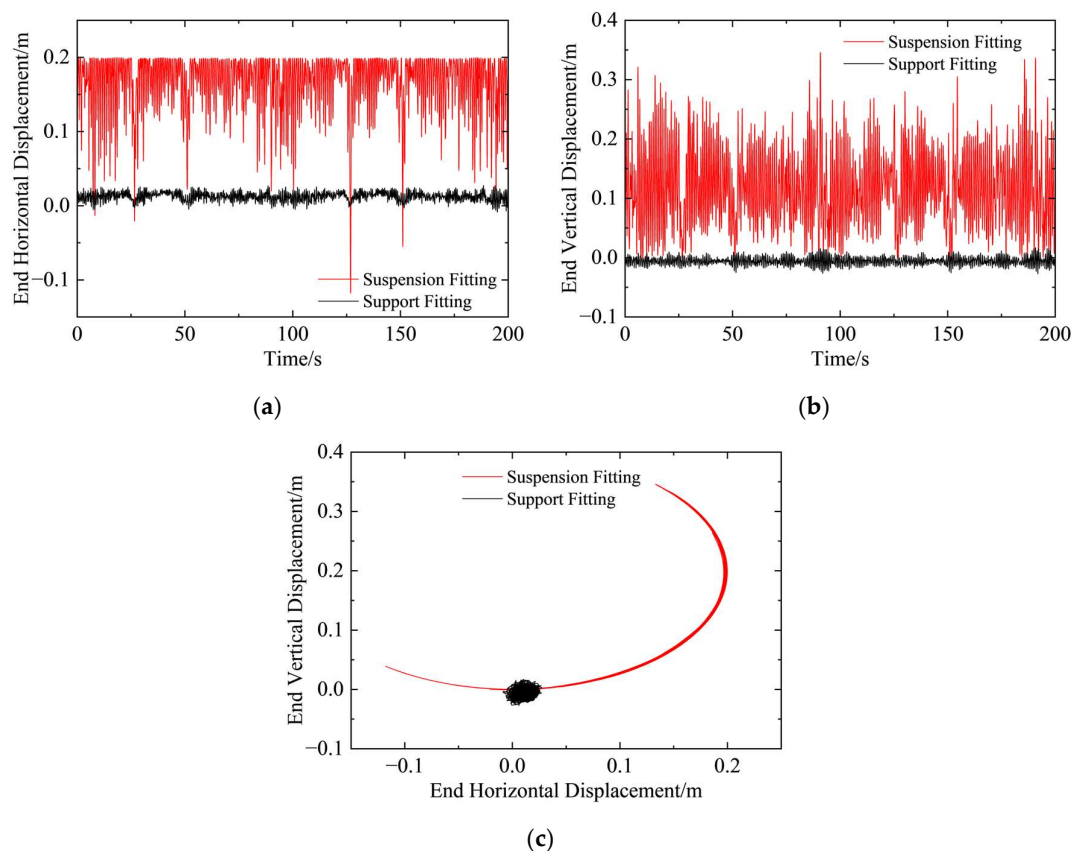


Figure 8. Conductor-end displacement responses under the two fitting configurations. (a) End horizontal displacement. (b) End vertical displacement. (c) End displacement trajectory.

The conductor-end stress time histories in Figure 10 exhibit pronounced random fluctuations under both fitting configurations, with broadly similar overall trends. However, the support-fitting case produces higher instantaneous stress peaks during several time intervals. This is because the constrained end prevents wind-induced deformation from being released through end displacement, converting part of the dynamic loading into larger local constraint reactions. Although the suspension fitting increases the range of end motion, its swing capability alleviates the end-constraint effect to some extent. The two fitting types therefore produce different response mechanisms: the suspension fitting releases wind action primarily through conductor-end motion and consequently exhibits larger end displacement, whereas the support fitting suppresses end motion through stronger boundary constraints but may increase local end stress.

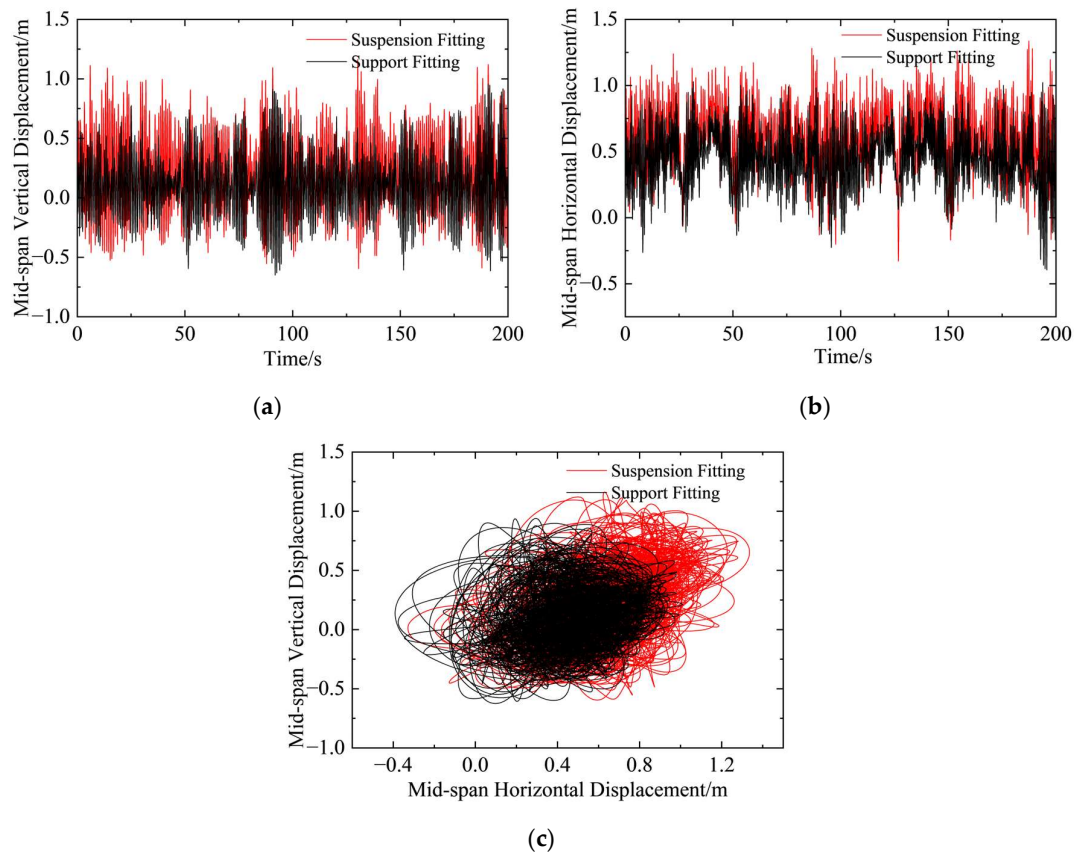


Figure 9. Mid-span displacement responses under the two fitting configurations. (a) Mid-span vertical displacement. (b) Mid-span horizontal displacement. (c) Mid-span displacement trajectory.

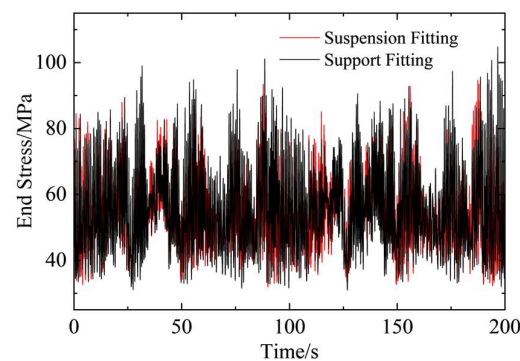


Figure 10. Conductor-end stress time histories under the two fitting configurations.

In summary, the fitting constraint has a decisive influence on the wind-induced response at the end of the additional conductor but a comparatively limited influence on the mid-span response. The suspension fitting reduces the degree of end constraint, although particular attention must be paid to insufficient clearance and interference with adjacent components caused by large swing amplitudes. The support fitting effectively controls conductor-end displacement, but the stress peaks and long-term fatigue damage at the support location require further assessment.

4.2. Performance Evaluation of the Two Fitting Types

To investigate the effects of fitting constraints on the alternating-stress characteristics and potential fatigue risk at the additional conductor end, rainflow counting is performed on the end-stress time histories associated with the suspension and support fittings for a representative case with an initial tension of 12 kN and a span length of 50 m. Rainflow

counting decomposes a random stress time history into equivalent stress cycles with different stress ranges, mean stresses and cycle counts. For the i th identified cycle, the stress range and mean stress are defined as follows:

$$\Delta\sigma_i = \sigma_{\max,i} - \sigma_{\min,i} \quad (49)$$

$$\sigma_{m,i} = \frac{\sigma_{\max,i} + \sigma_{\min,i}}{2} \quad (50)$$

where $\sigma_{\max,i}$ and $\sigma_{\min,i}$ are the maximum and minimum stresses of the i th cycle, respectively; $\Delta\sigma_i$ is the stress range; and $\sigma_{m,i}$ is the cycle mean stress. Rainflow counting provides the number of cycles, n_i , corresponding to different stress ranges, from which the conductor-end stress-cycle spectrum $\{\Delta\sigma_i, \sigma_{m,i}, n_i\}$ can be constructed to characterise the amplitude composition and cycle distribution of random wind-induced stress under the two fitting configurations. Figure 11 presents the rainflow-counting results for the two fitting models.

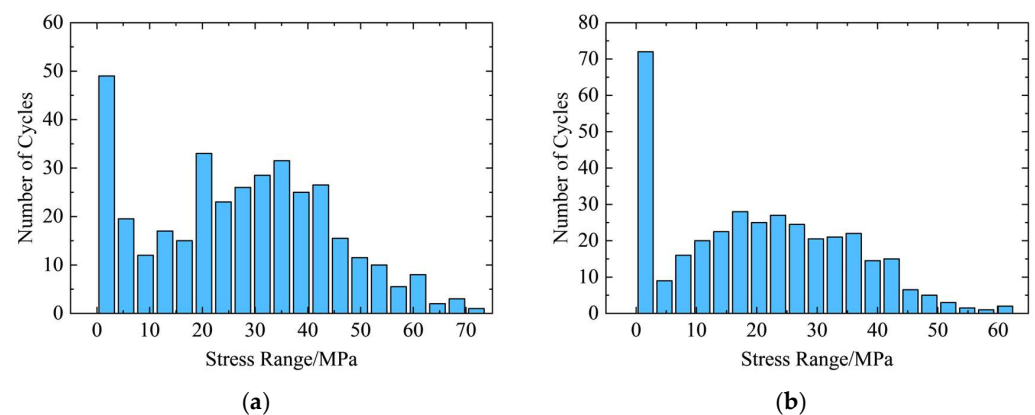


Figure 11. Rainflow-counting results. (a) Support fitting. (b) Suspension fitting.

The results reveal distinct differences between the end-stress cycle spectra associated with the two fitting types. For the suspension-fitting case, stress cycles are concentrated primarily in the low-stress-range region, with the largest number of small-amplitude cycles near zero. The cycle count generally decreases as the stress range increases; high-amplitude cycles above 45 MPa are relatively infrequent, and the maximum stress range is 60 MPa. This indicates that the suspension fitting permits spatial motion of the conductor end within a certain range, allowing part of the wind-induced deformation to be released through end motion. Consequently, the end-stress response is dominated by low-amplitude, high-frequency cycles. By contrast, the cycle distribution for the support-fitting case shifts towards medium and high stress ranges. In addition to low-amplitude cycles, the number of cycles within 20–45 MPa increases markedly, with concentrated distributions near 20 MPa and 30–40 MPa. The upper tail of the stress-range distribution also extends beyond 70 MPa. The allowable nominal stress of the conductor was determined as 111 MPa based on its calculated breaking force and the suspension-point safety factor specified in the Chinese railway industry standard *Code for Design of Railway Traction Power Supply* (TB 10009–2016). TB 10009–2016. The maximum conductor-end stress under all investigated conditions remained below this limit.

These results indicate that the support fitting imposes a stronger constraint on conductor-end displacement. Wind-induced deformation cannot be fully released through end swing, and part of the dynamic action is converted into larger local constraint reactions and alternating stresses, thereby increasing the number of medium- and high-stress-range cycles.

To clarify the influence of fitting constraints on dynamic stress transmission and frequency composition at the additional conductor end, frequency-domain analysis is conducted on the end-stress time histories associated with the suspension and support fittings for the same case. The fluctuating stress is obtained by removing the mean component from the stress time history:

$$\sigma'(t) = \sigma(t) - \bar{\sigma} \quad (51)$$

where $\sigma(t)$ is the conductor-end stress time history, $\bar{\sigma}$ is the mean stress over the analysed interval, and $\sigma'(t)$ is the fluctuating stress representing the wind-induced dynamic effect.

Because random wind-induced stress exhibits broadband and non-stationary fluctuations, its power spectral density is estimated using Welch's averaged periodogram method. Dividing the fluctuating-stress time history into K mutually overlapping segments gives

$$S_{\sigma}(f) = \frac{1}{K} \sum_{k=1}^K \frac{1}{F_s U} \left| \sum_{n=0}^{N_w-1} w(n) \sigma'_k(n) \exp\left(-j \frac{2\pi f n}{F_s}\right) \right|^2 \quad (52)$$

where

$$U = \frac{1}{N_w} \sum_{n=0}^{N_w-1} w^2(n) \quad (53)$$

where $S_{\sigma}(f)$ is the power spectral density of the fluctuating end stress, F_s is the unified sampling frequency, N_w is the number of samples in each segment, $w(n)$ is the window function, $\sigma'_k(n)$ is the fluctuating stress in the k th data segment, and U is the energy-normalisation coefficient of the window function. To facilitate comparison of spectral-energy differences at different frequencies, the power spectral density is converted to logarithmic form:

$$S_{\sigma, \text{dB}}(f) = 10 \log_{10} \left[\frac{S_{\sigma}(f)}{S_0} \right] \quad (54)$$

where the reference value is $S_0 = 1 \text{ MPa}^2/\text{Hz}$.

Figure 12 presents the power spectra of the fluctuating end stresses under the two fitting configurations. For both fitting types, the stress energy is concentrated mainly in the low-frequency range, particularly within 0–4 Hz, whereas the power spectral density decreases substantially with increasing frequency above 4 Hz. This indicates that, under the present span length and tension, the conductor-end stress response is controlled primarily by low-order global vibration, while high-frequency vibration makes a relatively limited contribution to the overall stress fluctuation.

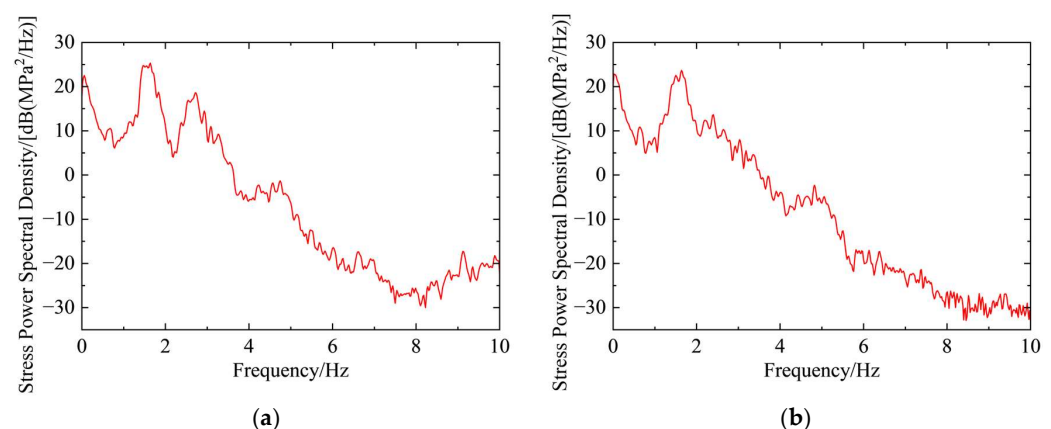


Figure 12. Stress power spectral densities under the two fitting configurations. (a) Support fitting. (b) Suspension fitting.

For the suspension-fitting case, the conductor-end stress spectrum exhibits a dominant peak near 1.5 Hz and several secondary peaks within 2.0–3.2 Hz. The low-frequency spectral energy is relatively continuous and the band surrounding the dominant peak is comparatively broad, indicating that the suspension fitting permits a degree of conductor-end swing and allows wind-load energy to be transmitted over a wider low-frequency range through end motion and global conductor deformation. As frequency increases further, the spectral density decreases smoothly and reaches a low level above 8 Hz. The support-fitting case also exhibits a dominant peak near 1.5 Hz, together with another pronounced peak near 2.6–3.0 Hz. Compared with the suspension fitting, the support fitting produces sharper peaks and more pronounced energy variation between peaks, indicating stronger frequency selectivity of the stress response after the end translational constraint is increased. The fixed constraint limits the release of wind-induced deformation through conductor-end displacement, causing part of the input energy to be converted into constraint reactions and local stresses concentrated near several specific frequencies. A moderate recovery of spectral energy is also observed within 8–10 Hz for the support fitting, suggesting that the stronger boundary constraint may enhance local or higher-order vibration components.

Comparison of the two spectra shows that the fitting type does not significantly alter the basic low-frequency-dominated nature of the end-stress response, but it clearly affects the distribution of stress energy within the low-frequency range. The suspension fitting exhibits a broader low-frequency energy distribution, reflecting the release and dispersion of stress response through flexible end swing. By contrast, the support fitting produces more concentrated discrete spectral peaks, indicating that rigid constraint amplifies stress responses at specific frequencies. This observation is consistent with the rainflow-counting results: the greater number of medium- and high-stress-range cycles under the support-fitting configuration may be associated with the concentration of spectral energy near specific low-order frequencies. Although the suspension fitting produces more pronounced end motion, its dynamic stress energy is more broadly distributed and high-amplitude stress cycles are less likely to occur.

To evaluate the influence of design parameters on the service conditions of the two fittings, the design span length and initial tension of the additional conductor are varied, and the conductor-end stress distributions are statistically analysed for the suspension- and support-fitting cases. In Figure 13, each box represents the principal distribution range of the stress samples, the median line indicates the central stress level, and the upper and lower whiskers represent the dispersion of the stress response over the calculation period. The red and blue boxes correspond to the support and suspension fittings, respectively.

The results for different span lengths show that, when the span increases from 40 m to 50 m, the median conductor-end stress for both fitting types increases only slightly, while the stress-distribution range and upper extreme increase markedly. The increase in the upper stress level is more pronounced for the support fitting, and the instantaneous stress peak approaches 100 MPa at a span length of 50 m. The stress dispersion also increases for the suspension fitting, but its upper extreme remains generally lower than that for the support fitting. Increasing the span length enlarges the effective wind-exposed length and the global lateral flexibility of the conductor, thereby strengthening the spatial deformation and dynamic-amplification effects induced by wind loading and subjecting the end connection to more pronounced alternating loads. Because the support fitting strongly constrains conductor-end displacement, wind-induced deformation is more readily transformed into constraint reactions and local stresses at the support. The suspension fitting can release part of the deformation through swing within a certain range and therefore produces a comparatively lower upper stress extreme.

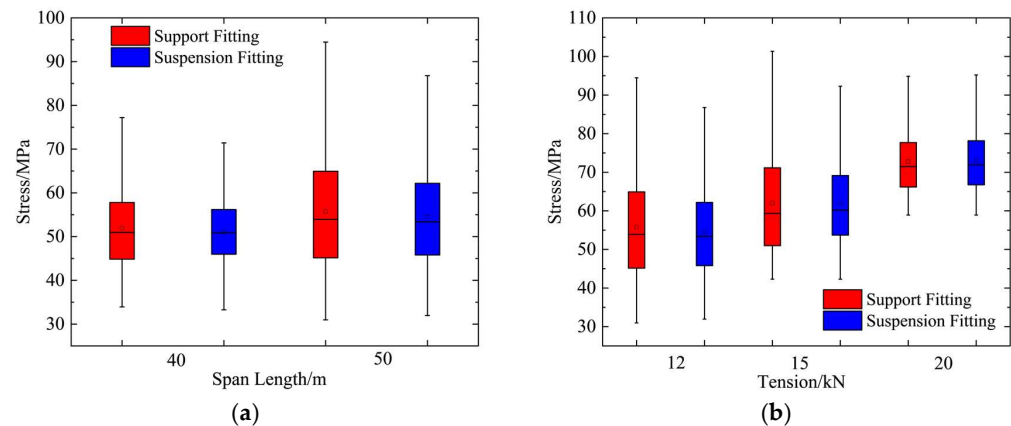


Figure 13. Effects of span length and initial tension on fitting-region stress. (a) Effect of span length. (b) Effect of initial tension.

Initial tension has a more significant influence on the stress level. As the tension increases from 12 kN to 20 kN, the stress distributions under both fitting configurations shift towards higher values, and the median stress rises from approximately 53 MPa to approximately 71 MPa, demonstrating that initial tension is the principal parameter governing the overall end-stress level. At the same tension, the median stresses of the two fitting types are similar, but the support fitting generally produces a larger upper extreme and a wider dispersion range; this difference is particularly evident at 12 kN and 15 kN. Although increasing tension enhances the global conductor stiffness and suppresses wind-induced displacement, it also raises the initial axial stress. Under the fixed constraint, wind-induced bending stress and local constraint stress are superimposed, making high instantaneous stress peaks more likely near the support fitting.

The rainflow-counting results, power spectral analyses and stress statistics for different span lengths and initial tensions demonstrate distinct adaptation mechanisms of the two fitting types under canyon winds. The support fitting suppresses conductor displacement by strengthening the end constraint but more readily converts wind-induced deformation into large local constraint reactions, resulting in more medium- and high-amplitude stress cycles, concentrated spectral energy within specific low-frequency bands and larger upper stress extremes. The suspension fitting releases part of the wind-induced deformation through conductor-end swing; its stress cycles are concentrated more strongly in the low-amplitude range, the spectral energy is more broadly distributed, and the end-stress extremes are generally lower. These differences remain consistent when the span length and initial tension are varied. Although the end-stress levels of both fittings increase with span length or tension, the suspension fitting continues to exhibit a narrower stress dispersion and lower high-value responses than the support fitting. Within the parameter range investigated, the suspension fitting therefore provides better stress adaptability and deformation-release capability under stochastic canyon winds. This advantage is not limited to a single design case but remains stable as the structural parameters vary. It should nevertheless be noted that the stronger deformation-release capability of the suspension fitting may be accompanied by larger conductor-end displacement. Practical fitting selection should therefore consider clearance constraints, displacement limits and fatigue life in a comprehensive manner.

5. Conclusions

This study investigated the wind-induced response of additional conductors and different fittings in railway overhead contact lines under strong canyon winds. The main conclusions are as follows:

- (1) The Kaimal, Tieleman and Panofsky spectra, combined with a fourth-order autoregressive model, can be used to construct a three-dimensional stochastic wind field with temporal and spatial correlations. Quasi-steady aerodynamic theory enables the fluctuating wind-speed time histories to be converted into lateral and vertical wind-load time histories acting on the additional conductors.
- (2) The nonlinear additional conductor model established using the absolute nodal coordinate formulation can describe large displacement, large rotation and coupled axial-bending deformation. Representing the suspension fitting as a swingable rod element and the support fitting by three-directional equivalent stiffness effectively captures the differences in constraint mechanisms and load-transfer paths between the two fitting types.
- (3) The fitting type has a significant influence on the conductor-end response but a comparatively limited effect on the mid-span vibration. The suspension fitting releases part of the wind-induced deformation through swing and therefore produces larger end displacement but fewer instantaneous stress peaks and high-amplitude stress cycles. The support fitting effectively limits end displacement but increases local constraint reactions and stress concentration.
- (4) Increasing either the span length or the initial tension raises the stress level near the fitting, with initial tension exerting the more pronounced influence. Under identical structural parameters, the suspension fitting exhibits better dynamic-stress adaptability, although its larger swing amplitude and the associated clearance risk must also be considered in engineering applications.

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