

Article

Drill String Dynamics in Deepwater Open-Loop Drilling of an Ultra-Shallow High-Build-Rate Horizontal Well

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Abstract

In deepwater open-loop drilling of ultra-shallow high-build-rate horizontal wells, the drill string spans the exposed seawater section, the mudline transition, and the constrained downhole section. Existing studies have not yet established a unified coupled dynamic model covering the full string, and the response patterns and parameter-control mechanisms remain insufficiently understood. This study examines a surface-hole operation in a block of the South China Sea. A six-degree-of-freedom spatial Euler–Bernoulli beam finite element model was established from the measured wellbore trajectory. Structural and fluid inertia, geometric stiffness due to axially varying force, Morison-type current loading, a wake-oscillator model for vortex-induced vibration (VIV), wellbore contact and friction, and bit loads were coupled within one framework. The generalized-alpha method was used for time discretization, and the nonlinear dynamic equilibrium equations were solved by within-step iteration for multiple operating conditions. Under the baseline condition, the peak in-line displacement was 6.3–6.4 m and occurred in the middle of the seawater section; the peak cross-flow displacement was approximately 0.43 m, the maximum bending moment was approximately 36 kN m, and the maximum equivalent stress was approximately 230 MPa. The high internal-force values were concentrated in the mudline transition and the upper BHA, demonstrating spatial separation between deformation and internal force. Surface current velocity controlled the in-line response amplitude: increasing the velocity from 0.4 to 0.8 m/s increased the peak in-line displacement by approximately 230%. WOB had a limited effect on global deformation and acted mainly on the local near-bit section. These results provide numerical reference for mechanistic analysis and parameter control of drill-string dynamics in comparable deepwater open-loop drilling operations.

Keywords: deepwater open-loop drilling; ultra-shallow high-build-rate horizontal well; drill string dynamics; generalized-alpha method



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1. Introduction

Conventional oil and gas resources in shallow-water and onshore settings continue to decline, making deepwater plays a major target for reserve growth. Deepwater areas of the South China Sea contain abundant ultra-shallow gas resources buried within 300 m of the seafloor. Because of weak formations, extremely narrow pressure windows, and the high risk of shallow-gas hazards, these resources were long treated as drilling-risk intervals and left undeveloped. Advances in riserless drilling and dual-gradient pressure-control technologies have changed this perception, turning shallow gas from a drilling hazard into a

potentially scalable source of clean energy and demonstrating the resource potential of this setting. In parallel, multiphase storage of CO₂ hydrates in shallow subseabed formations is being investigated for carbon capture, utilization, and storage (CCUS), and mature technology may enable high-build-rate horizontal wells in deepwater ultra-shallow formations. From safe shallow-gas production to CO₂ geological storage, applications of deepwater open-loop drilling in ultra-shallow high-build-rate horizontal wells are expanding. In this configuration, however, the drill string simultaneously spans the exposed seawater section, the mudline transition, and the constrained downhole section and is subject to coupled marine loading, high-curvature wellbore constraints, and bottomhole drilling excitation. Its dynamic behavior is therefore considerably more complex than that of conventional drilling, requiring a dedicated mechanical model and a systematic understanding of its response.

Deepwater open-loop drilling removes the conventional riser return path. The drill string passes from the platform through the seawater and mudline regions and directly enters the wellbore, giving it fundamentally different dynamics from a riser-supported system. Several studies have examined the dynamics of riserless drill strings. Huang et al. [1] established a dynamic-characteristics model and discussed the influence of structural parameters on natural frequencies. Chen et al. [2] developed a three-dimensional nonlinear coupled-vibration model and analyzed the effect of drill-string vibration on the wellbore pressure field. Mao et al. [3] introduced a wake-oscillator model for vortex-induced vibration (VIV) and investigated flow-induced vibration of a riserless drill string. These studies established the basis for open-loop drill-string dynamics, but most considered a vertical section and focused on fluid-structure coupling in the seawater. They did not fully account for the spatially varying curvature of a high-build-rate horizontal well, intermittent wellbore contact, or the local mechanical effects of the bottomhole assembly (BHA), and therefore, they cannot fully describe the coupled response over a short ultra-shallow build section.

For drill-string dynamics in curved wells, a relatively systematic methodology has been developed. Unified coupled models of axial, lateral, and torsional motion provide the basic framework for complex wellbores [4,5]. Finite-element nonlinear dynamic methods have been validated and applied to PDC bit-rock interaction, axial load transfer, three-dimensional beam discretization, wellbore constraints, and slide-drilling control [6–15]. These studies, however, mainly address conventional riser drilling or onshore horizontal wells and assume that the string below the wellhead is fully constrained by the wellbore. They do not include the marine environmental loading and fluid-structure coupling of the exposed seawater section and therefore cannot be directly applied to the full-string dynamics of open-loop drilling.

Flow-induced loading and VIV in the seawater section are central topics in the dynamics of slender marine structures. The Morison equation and coupled wake-oscillator models are classical tools for describing hydrodynamic loading on cylindrical members and have been widely used for risers and marine conductors [16,17]. Studies of long flexible risers show that axial preload varying along the string substantially changes lateral VIV and local stress distributions [18]. Investigations of drilling and mining risers under different current and tension conditions further reveal coupling among flow velocity, axial tension, and wake state [19,20]. These studies generally concern independent riser structures tensioned at both ends. In an open-loop drill string, the lower end extends into a build section and is subject to wellbore contact and bit constraint, while the axial force changes continuously from compression to tension. Its boundary conditions and axial-force distribution therefore differ fundamentally from those of a conventional riser, so existing VIV models cannot be applied directly.

Wellbore contact, friction, and bit–rock interaction in the downhole section are the main sources of nonlinearity in drill-string dynamics. Bit–rock interface laws and three-dimensional BHA contact algorithms provide a basis for modeling equivalent bit loads and wellbore reactions [21,22]. Spatial rotational constraints and Euler–Bernoulli beam elements provide a unified framework for three-dimensional nodal transformations and structural discretization [23,24]. Dynamic finite-element methods can incorporate preload geometric stiffness, wellbore contact, and bit boundary conditions in one solution procedure [25]. Existing contact and bit models, however, generally focus on local BHA behavior and have not been synchronously coupled with marine flow-induced loading over the full string.

Newmark, Hilber–Hughes–Taylor, and generalized-alpha methods provide mature time-integration schemes for structural dynamics. The generalized-alpha method retains second-order accuracy at low frequencies while providing controllable numerical dissipation at high frequencies, making it suitable for nonlinear dynamics with contact [26–29]. In existing drill-string studies, it has mostly been applied to a purely downhole or purely seawater model. A targeted iterative scheme for the multiple nonlinear sources in a coupled seawater fluid-structure system, mudline transition, and high-build-rate downhole contact problem remains to be established and verified.

The current deepwater riserless/open-loop literature can therefore be grouped into two technical paths. One focuses on seawater-section flow-induced vibration and VIV but gives limited attention to the trajectory, wellbore contact, and BHA effects in the build section. The other focuses on nonlinear drill-string dynamics in curved wells but omits the global action of marine environmental loading. No unified coupled model covering the seawater section, mudline transition, and high-build-rate downhole section has yet been established, and the response patterns and parameter-control mechanisms for open-loop drilling of ultra-shallow short-build horizontal wells remain insufficiently understood. Table 1 compares representative studies.

Table 1. Comparison of representative studies.

Source	Study Object	Marine Loading and VIV	Wellbore Contact and Friction	Limitation
Huang et al. [1] (2023)	Simplified vertical deepwater riserless drill string	Current loading only; VIV not systematically included	Wellbore contact constraint not considered	Build-section curvature and downhole constraint omitted; not applicable to horizontal wells
Chen et al. [2] (2023)	Vertical deepwater riserless drill string	Fluid-structure coupling and wellbore pressure; VIV not considered	Simplified wellbore constraint; intermittent contact not considered	Trajectory reduced to a vertical well; spatially varying curvature of high-build-rate wells not addressed
Mao et al. [3] (2024)	Dynamic analysis of a vertical-section deepwater riserless drill string; VIV is explicit in the published title	VIV and wake-oscillator coupling included	Combined frequency- and time-domain analysis	Seawater section only; no coupling with downhole build-section contact nonlinearity

Against this background, this study considers a surface-hole operation in an ultra-shallow high-build-rate horizontal well in a South China Sea block. The work comprises:

(1) constructing a six-degree-of-freedom spatial Euler–Bernoulli beam finite-element model covering the seawater section, mudline transition, and downhole section from the complete measured inclination and azimuth trajectory, with coupled fluid-structure inertia, axially varying geometric stiffness, Morison current loading, wake-oscillator VIV, wellbore contact and friction, and bit loads; (2) discretizing time with the generalized-alpha method and updating contact state, geometric stiffness, and wake variables iteratively at every time step to solve the nonlinear dynamic equilibrium; and (3) systematically analyzing the baseline response and quantifying how surface current velocity and WOB control the response of different sections.

2. Dynamic Model of the Drill String in Deepwater Open-Loop Drilling

Deepwater open-loop drilling is widely used during deepwater surface-hole operations. As shown in Figure 1, the riser connecting the platform and subsea wellhead is omitted, and the drill string passes directly from the platform through the seawater section and enters the downhole wellbore through the wellhead guide at the mudline. Drilling fluid is pumped through the drill string to the bottomhole, carries cuttings upward through the annulus, and is discharged into the surrounding seabed water at the mudline rather than returned to the platform through a dedicated conduit. Compared with conventional riser drilling, open-loop drilling reduces platform top load, simplifies the wellhead configuration, and shortens the surface-hole drilling period, offering technical and economic advantages in deepwater exploration and development. The seawater-section string is nevertheless fully exposed to the marine environment and lacks the lateral restraint and structural protection of a riser. It directly experiences current loading and drilling loads, which produces more complex mechanical boundaries and dynamics. In an ultra-shallow high-build-rate horizontal well, the string must complete a sharp vertical-build-horizontal turn over a short interval, while marine loading is combined with friction in the horizontal section. These features impose additional requirements on drill-string dynamic modeling and safety analysis.

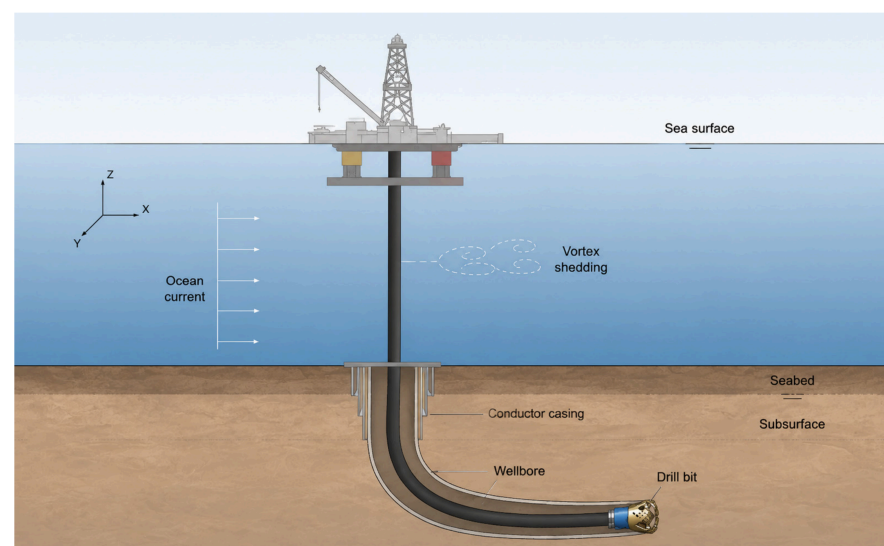


Figure 1. Schematic of open-loop drilling for a deepwater ultra-shallow horizontal well.

2.1. Physical Model and Basic Assumptions

The physical model is the slender drill string extending continuously from the bit to the upper platform-side boundary; the mudline or subsea wellhead is an internal location along this interval. Nodes are numbered from the bit toward the platform-side boundary, with node 1 at the bit and the terminal node subject to the platform-side boundary condition.

A right-handed global coordinate system is used, with the global z axis being vertical and x and y horizontal. At any point in the curved well, the local axial direction is defined by the tangent of the corresponding element, so no single global translational component represents the local axial response over the complete string. Each node has six degrees of freedom (6 DOF), including three translations and three rotations, and two adjacent nodes form a 12-DOF spatial beam element.

Each equivalent drill-string segment is modeled as a piecewise-uniform annular section with uniform density and isotropic linear-elastic material properties. Linear stiffness is formulated using small-strain Euler–Bernoulli kinematics, in which cross-sections remain planes and transverse shear deformation is neglected.

2.2. Wellbore Trajectory and Spatial Beam Discretization

The trajectory input consists of measured depth, inclination, and azimuth. For each pair of survey stations, the angles are converted to radians and the long-path discontinuity is corrected when the azimuth crosses 360 degrees. The inclination and azimuth functions are then integrated over the interval using their average values. The product of the averages approximates the horizontal coordinate increments, while integration of inclination gives the vertical increment.

After the length of each drill-string component is specified, cubic-spline interpolation is applied separately to the three centerline coordinates, and the chord vector between adjacent interpolated nodes defines the local element z axis. The local x axis is obtained from the cross product of this tangent and the global reference axis, and the local y axis is normalized through an orthogonal cross product. The resulting orthonormal frame transforms mass, stiffness, gravity, bit loads, and contact forces between local and global systems and continuously connects the seawater section, mudline transition, and downhole section along the selected build interval.

$$\begin{aligned} \mathbf{q}_e^g &= \mathbf{A}_e \mathbf{U} = [u_i \quad v_i \quad w_i \quad \theta_{xi} \quad \theta_{yi} \quad \theta_{zi} \quad u_j \quad v_j \quad w_j \quad \theta_{xj} \quad \theta_{yj} \quad \theta_{zj}] \\ \mathbf{q}_e^l &= \mathbf{T}_e \mathbf{q}_e^g \\ \mathbf{T}_e &= \text{diag}(\mathbf{R}_e, \mathbf{R}_e, \mathbf{R}_e, \mathbf{R}_e) \\ \mathbf{R}_e &= \begin{bmatrix} \mathbf{e}_x^T \\ \mathbf{e}_y^T \\ \mathbf{e}_z^T \end{bmatrix}. \end{aligned} \quad (1)$$

In Equation (1), $\mathbf{q}$ is the global generalized displacement vector, with translations in m and rotations in rad; $\mathbf{B}$ is the Boolean matrix extracting element degrees of freedom from the global vector; $\mathbf{R}$ is the three-by-three direction-cosine matrix formed by the local unit basis vectors expressed in the global system; and $\mathbf{T}$ transforms global element quantities to the local system. Equation (1) applies to every two-node spatial beam element. Mass, stiffness, and load quantities are first formulated locally and then returned to the unified global degrees of freedom through the corresponding transformation.

For diameter changes among the bit, motor, stabilizer, heavyweight drill pipe, and conventional drill pipe in the BHA, linear tapered-section transition elements are used so that section properties vary continuously and no stiffness jump occurs at a node. For short variable-diameter components such as bits and crossover subs, the transition length is set to 1–2 times the nominal pipe diameter. Cross-sectional area, bending second moment, and torsional constant are linearly interpolated along the element, and the element stiffness and mass matrices are integrated from these distributed properties to maintain continuity at element interfaces.

For locally enlarged contact components such as stabilizers, a decoupled treatment of “pipe-body stiffness plus nodal contact radius” is adopted. The structural stiffness, mass, and rotary inertia of the beam element retain the pipe-body section properties, while the stabilizer outer diameter is assigned only as the contact-detection radius at the corresponding nodes. This outer diameter contributes to wellbore contact forces but not to beam stiffness or mass assembly, avoiding artificial local bending stiffness and spurious internal-force peaks.

The mesh size in a diameter-transition region changes gradually, with abrupt element-length changes prohibited. The transition interface is also offset from common element nodes so that the stiffness change occurs within an element, further suppressing interpolation singularities at nodes.

2.3. Mass, Stiffness, Preload, and Nonlinearity

Structural mass is discretized using a consistent two-node beam formulation. The local mass comprises axial, biaxial-bending, and torsional contributions based on the sectional mass per unit length, together with a bending rotary-inertia correction proportional to the relevant sectional inertia. The correction changes the inertial coupling between bending translations and cross-sectional rotations without changing the Euler–Bernoulli stiffness assumption. Linear stiffness includes axial, biaxial-bending, and torsional terms, and the global matrices are assembled sparsely according to element degree-of-freedom locations. The associated energy and assembly relations are

$$\begin{aligned}\mathcal{T}_s &= \frac{1}{2} \sum_e (\dot{\mathbf{q}}_e^\ell)^\top (\mathbf{M}_e^c + \mathbf{M}_e^r) \dot{\mathbf{q}}_e^\ell \\ \Pi_L &= \frac{1}{2} \sum_e (\mathbf{q}_e^\ell)^\top \mathbf{K}_e^{EB} \mathbf{q}_e^\ell \\ \mathbf{M}_s &= \sum_e \mathbf{A}_e^\top \mathbf{T}_e^\top (\mathbf{M}_e^c + \mathbf{M}_e^r) \mathbf{T}_e \mathbf{A}_e \\ \mathbf{K}_L &= \sum_e \mathbf{A}_e^\top \mathbf{T}_e^\top \mathbf{K}_e^{EB} \mathbf{T}_e \mathbf{A}_e.\end{aligned}\quad (2)$$

Here, T and U are the structural kinetic and linear strain energies, respectively, both in J; M is the consistent mass, including axial, bending, and torsional contributions; M_r is the bending rotary-inertia correction; K is the local Euler–Bernoulli axial-bending-torsional stiffness matrix; and q is the element nodal generalized displacement vector in the local coordinate system.

Static distributed weight is calculated separately for the steel, internal fluid, and displaced external fluid. Seawater density is used for the external fluid in the seawater section, whereas drilling-fluid density is used in the downhole section.

$$\begin{aligned}A_s &= \frac{\pi}{4} (D_o^2 - d_i^2) \\ A_i &= \frac{\pi}{4} d_i^2 \\ A_o &= \frac{\pi}{4} D_o^2 \\ m_{\text{eff},e} &= \rho_s A_s + \rho_i A_i - \rho_o A_o \\ w_{\text{eff},e} &= m_{\text{eff},e} g \\ \mathbf{F}_g &= \sum_e \mathbf{A}_e^\top \mathbf{T}_e^\top \int_0^{l_e} \mathbf{N}_e^\top \mathbf{R}_e (-w_{\text{eff},e} \mathbf{e}_X) ds.\end{aligned}\quad (3)$$

Here, D_o and D_i are the outer and inner diameters in m; A_s , A_i , and A_d are the steel, internal-fluid, and displaced-fluid areas in m²; ρ denotes the corresponding density in kg/m³; m_e is the effective static mass per unit length in kg/m; w_e is the effective distributed weight in N/m; N is the beam shape-function matrix; and $\mathbf{e}_z$ is the global vertical unit vector.

The initial axial force starts from the compressive bit-end load and accumulates the vertical projection of effective distributed weight onto each local element axis. The element-average axial force generates the beam-column geometric stiffness. During the solution, local axial extension, the combined curvature from the two bending-rotation differences,

and the torsional-rotation difference scale three state matrices. Previous studies provide the coupling background for axial load transfer and flow-induced response [9,18], and the discrete update used here is given in Equation (4).

$$\begin{aligned}
 N_1^- &= -W_0 \\
 N_e^+ &= N_e^- + w_{\text{eff},e} l_e (\mathbf{e}_x \cdot \mathbf{e}_X) \\
 \bar{N}_e &= \frac{N_e^- + N_e^+}{2} \\
 N_{e+1}^- &= N_e^+ \\
 \mathbf{K}_0 &= \mathbf{K}_L + \sum_e \mathbf{A}_e^T \mathbf{T}_e^T \mathbf{K}_G (\bar{N}_e) \mathbf{T}_e \mathbf{A}_e \\
 \Delta u_x &= q_{e,7}^\ell - q_{e,1}^\ell \\
 \kappa_b &= \frac{\sqrt{(q_{e,11}^\ell - q_{e,5}^\ell)^2 + (q_{e,12}^\ell - q_{e,6}^\ell)^2}}{l_e} \\
 \Delta \theta_x &= q_{e,10}^\ell - q_{e,4}^\ell \\
 \mathbf{K}_{NL}(\mathbf{U}) &= \sum_e \mathbf{A}_e^T \mathbf{T}_e^T \left[\Delta u_x \mathbf{K}_{NA1,e} + \kappa_b \mathbf{K}_{NA2,e} + \Delta \theta_x \mathbf{K}_{NT,e} + \mathbf{K}_G \left(\frac{E_e A_{s,e} \Delta u_x}{l_e} \right) \right] \mathbf{T}_e \mathbf{A}_e.
 \end{aligned} \quad (4)$$

The axial-force quantities are in N, with compression taken as negative; l_e is the element length; δ is local axial extension; κ is the combined curvature associated with the bending-rotation differences; and θ is the torsional-rotation difference. The three state matrices are scaled by these state variables and assembled with the geometric stiffness generated by the current elastic axial force.

For a tapered transition element, both linear stiffness and geometric stiffness are obtained by integrating the section-distribution functions along the element axis. Wet mass, added mass, and damping coefficients vary continuously with the section properties, ensuring smooth transitions of the mass, damping, and stiffness matrices at diameter changes and avoiding local high-frequency numerical oscillations caused by property jumps during generalized-alpha integration.

2.4. Internal and External Fluid Inertia and Damping

Wet mass is the sum of structural mass, internal-fluid inertia, and external added mass. The internal fluid contributes consistent axial and biaxial-bending mass through its distributed mass and contributes torsional inertia through the polar second moment of the fluid section.

$$\begin{aligned}
 \mathbf{M}_w &= \mathbf{M}_s + \mathbf{M}_i + \mathbf{M}_o \\
 m_i &= \rho_i \frac{\pi d_i^2}{4} \\
 I_i &= \frac{\pi d_i^4}{32} \\
 m_a &= \rho_o \frac{\pi D_o^2}{4} C_m \\
 D_w & \\
 D_o &\in \mathbb{R}_{>0} \\
 D_w &> D_o \\
 C_m &= \begin{cases} 1, & D_w \geq 100 \text{ m}, \\ \min \left[5, \max \left(1, \left| \frac{D_w^2 + D_o^2}{D_w^2 - D_o^2} \right| \right) \right], & D_o < D_w < 100 \text{ m}, (D_w - D_o)/2 > 10^{-3} \text{ m} \\ 5, & D_o < D_w < 100 \text{ m}, 0 < (D_w - D_o)/2 \leq 10^{-3} \text{ m} \end{cases} \\
 \mathbf{M}_i &= \sum_e \mathbf{A}_e^T \mathbf{T}_e^T (m_i l_e \mathbf{B}_{abt} (I_i / A_i)) \mathbf{T}_e \mathbf{A}_e \\
 \mathbf{M}_o &= \sum_e \mathbf{A}_e^T \mathbf{T}_e^T (m_a l_e \mathbf{B}_b) \mathbf{T}_e \mathbf{A}_e
 \end{aligned} \quad (5)$$

Here, M_w is the wet-mass matrix; m_i and m_a are the internal-fluid and external added masses per unit length in kg/m; I_i is the polar second moment of area of the internal fluid in m^4 ; D_c is the local wellbore or equivalent unbounded-domain diameter in m; and C_c is the dimensionless confinement amplification coefficient.

Velocity-dependent terms comprise Rayleigh damping, downhole annular viscous damping, optional boundary damping, and a gyroscopic term updated with local rotational speed. The mass-proportional Rayleigh term includes only structural and internal-fluid mass and excludes external added mass; the stiffness-proportional term uses the assembled linear and preload stiffness. The downhole annular lateral coefficient is determined from dynamic viscosity, clearance, and the confinement amplification ratio, while a Couette-type approximation is used for the torsional coefficient. Drilling-fluid effects are represented equivalently through added mass and annular viscous damping; the dynamic effects of hydrostatic pressure and axial-flow excitation are not resolved in detail. Wellbore contact parameters are assigned as common engineering values and do not include pore-pressure evolution or changes in wellbore mechanical properties caused by formation permeability and fluid-formation interaction. The relevant porous-media flow-coupling mechanism is discussed in Ref. [30].

$$\begin{aligned}
 C_{\text{eff}}(\dot{\mathbf{U}}) &= C_R + C_{\text{ann}} + C_{bc} + C_g(\dot{\mathbf{U}}) \\
 C_R &= \alpha_d(M_s + M_i) + \beta_d K_0 \\
 g_a &= \frac{D_w - D_o}{2} \\
 (c_l, c_t) &= \begin{cases} \left(\frac{\pi \mu_f D_o}{g_a} \frac{D_w^2 + D_o^2}{D_w^2 - D_o^2} \left| \frac{\pi \mu_f (D_o/2)^3}{g_a} \right| \right), & g_a > 10^{-3} \text{ m}, \\ \left(\frac{5\pi \mu_f D_o}{10^{-3} \text{ m}} \left| \frac{\pi \mu_f (D_o/2)^3}{10^{-3} \text{ m}} \right| \right), & g_a \leq 10^{-3} \text{ m}, \end{cases} \\
 C_{g,e} &= \frac{2\bar{\omega}_e J_{x,e}}{I_e} G_e \\
 J_{x,e} &= \frac{1}{8} \rho_s A_s (D_o^2 + d_i^2) \\
 G_e^T &= -G_e.
 \end{aligned} \tag{6}$$

Here, α and β have dimensions of 1/s and s, respectively; μ is dynamic viscosity in Pa s; c is annular clearance in m; c_l is the lateral damping coefficient per unit length in N s/m²; c_t is the distributed torsional coefficient in N s; and ω is local rotational speed.

2.5. Current Profile and Hydrodynamic Loading

The seawater section is parameterized by the height h above the seabed. A 1/7 power law is used for the turbulent boundary-layer velocity profile over a deep, flat seabed, consistent with in situ observations in the northern South China Sea and widely used in ocean engineering. The current increases from zero at the seabed to the base surface velocity according to this power law, and a wind-driven component is superimposed linearly only in the upper layer of thickness δ_w . The water depth is denoted by H , and the force terms used here are linearized about the prescribed velocity as follows:

$$\begin{aligned}
 U(h) &= V_d \left(\frac{h}{H_s} \right)^{1/7} + \mathbb{I}_{h>h_a} V_{CW} \frac{h-h_a}{A_M} \\
 h_a &= H_s - A_M \\
 A_M &= \frac{(7.6s)V_W}{\sqrt{\sin\Phi}} \\
 V_{CW} &= A_{KW} V_W \\
 f_D &= -\rho_w U D_o C_{D1} v_D + \frac{1}{2} \rho_w U^2 D_o C_{D1} + \frac{1}{4} \rho_w U^2 D_o C_{D2} q_x \\
 f_L &= -\frac{1}{2} \rho_w U D_o C_{D1} v_L + \frac{1}{4} \rho_w U^2 D_o C_{L2} q_y \\
 0 &\leq h \leq H_s.
 \end{aligned} \tag{7}$$

The velocity terms are in m/s and the length terms are in m; θ_c is the current direction angle. C_D , C_L , and the wake-oscillator coefficients are dimensionless, the wake states are dimensionless, and the distributed loads are in N/m. The adopted representative coefficients are $C_D = 1.2$, $C_L = 0.3$, and the wake parameters are 0.4 and 0.2.

At each structural time step, the seawater-section wake states are first advanced explicitly with a fourth-order Runge–Kutta (RK4) scheme using the velocity and acceleration from the preceding time level. Hydrodynamic loads are then calculated from Equation (7) and the structural response is updated implicitly. Wake variables are mapped through lift and drag coefficients to distributed hydrodynamic loads and converted to equivalent generalized nodal loads with the beam shape functions.

2.6. Wellbore Contact and Friction

Contact at each downhole node is evaluated in a local contact frame. An end node uses the frame of its adjacent element, whereas an internal node uses reorthogonalized averages of the tangents and normals of the two adjacent elements. Let d be the lateral offset of the nodal center, r_t the tool radius, and r_w the wellbore radius; $d + r_t > r_w$ indicates collision.

$$\begin{aligned}
 g_n &= \rho + r - R \\
 \delta_s &= \frac{1}{2} \left(g_n + \sqrt{g_n^2 + \varepsilon_\delta^2} \right) \\
 s_c &= \frac{1}{2} \left(1 + \frac{g_n}{\sqrt{g_n^2 + \varepsilon_\delta^2}} \right) \\
 v_r &= \frac{u_y v_y + u_z v_z}{\max(\rho, 10^{-12} \text{ m})} \\
 v_c &= \frac{1}{2} \left(v_r + \sqrt{v_r^2 + \varepsilon_t^2} \right) \\
 F_n &= - \left(k_h \delta_s + c_f v_c s_c \right) \\
 F_x &= -\mu_x \left| F_n \right| \tanh \left(\frac{v_x}{\varepsilon_t} \right) \\
 F_\theta &= -\mu_\theta \left| F_n \right| \tanh \left(\frac{\omega_x}{\varepsilon_r} \right) \\
 \begin{bmatrix} F_y \\ F_z \end{bmatrix} &= F_n \frac{1}{\max(\rho, 10^{-12} \text{ m})} \begin{bmatrix} u_y \\ u_z \end{bmatrix} + F_\theta \frac{1}{\max(\rho, 10^{-12} \text{ m})} \begin{bmatrix} -u_z \\ u_y \end{bmatrix} \\
 M_x &= r F_\theta.
 \end{aligned} \tag{8}$$

The displacement quantity in Equation (8) is expressed in m.

The contact model uses homogeneous equivalent wellbore mechanical parameters and does not account for lithology and strength variations along measured depth caused by formation heterogeneity, or for changes in effective wall-support stiffness caused by porosity and permeability through pore-pressure evolution. In actual drilling, hole enlargement and wall spalling can change the annular clearance and contact geometry, thereby modifying impact loads and amplitude limiting of drill-string vibration. Integrating detailed geological characterization with a well-scale dynamic model is an important route to improving field applicability; relevant geology-engineering coupling is discussed in Ref. [31].

2.7. Global Governing Equation

Using D'Alembert's principle, the preceding terms are assembled into a semi-discrete nonlinear dynamic equation in nodal force-balance form. The wet-mass matrix represents inertia, the damping matrix represents velocity-dependent damping force, the baseline and nonlinear stiffness terms represent elastic internal force, and the right-hand side contains generalized external loads from contact, gravity, current, and the bit:

$$\begin{cases} \mathbf{M}_w \ddot{\mathbf{U}} + \mathbf{C}_{\text{eff}}(\dot{\mathbf{U}}) \dot{\mathbf{U}} + [\mathbf{K}_0 + \mathbf{K}_{NL}(\mathbf{U})] \mathbf{U} - \mathbf{G}^T \boldsymbol{\lambda} = \mathbf{F}_c(\mathbf{U}, \dot{\mathbf{U}}) + s(t) [\mathbf{F}_g + \mathbf{F}_h(\mathbf{q}, \dot{\mathbf{U}}) + \mathbf{F}_b(t, \mathbf{U}, \dot{\mathbf{U}})], \\ \mathbf{G} \mathbf{U} = 0. \end{cases} \tag{9}$$

The translational components of $\mathbf{U}$, $\dot{\mathbf{U}}$, and $\ddot{\mathbf{U}}$ have units of m, m/s, and m/s², respectively, and the rotational components use rad and their time derivatives. M_w , C , K_0 , and K_n are the wet mass, velocity-dependent terms, baseline stiffness, and state-dependent

stiffness, respectively. F_c , F_g , F_h , and F_b are generalized contact, gravity, hydrodynamic, and bit loads, with translational components in N and rotational components in N m. G is a dimensionless constraint matrix and λ is the corresponding generalized reaction.

3. Numerical Solution

Equation (9) contains displacement-dependent stiffness, velocity-dependent terms, wellbore contact, and friction. Time discretization therefore produces a nonlinear algebraic balance equation. The generalized-alpha method advances the structural state, and the discrete balance equation is solved iteratively within every time step.

3.1. Generalized-Alpha Method

The generalized- α method is based on the Newmark kinematic relations and constructs α -weighted states for the acceleration and the remaining dynamic terms. Let n and $n + 1$ denote adjacent time levels; the weighted states at the current level are denoted by α_m and α_f . The discrete relations are summarized as follows:

$$\begin{aligned}
 \mathbf{U}_{n+1-\alpha_f} &= \left(1 - \alpha_f\right) \mathbf{U}_{n+1} + \alpha_f \mathbf{U}_n \\
 \dot{\mathbf{U}}_{n+1-\alpha_f} &= \left(1 - \alpha_f\right) \dot{\mathbf{U}}_{n+1} + \alpha_f \dot{\mathbf{U}}_n \\
 \ddot{\mathbf{U}}_{n+1-\alpha_m} &= (1 - \alpha_m) \ddot{\mathbf{U}}_{n+1} + \alpha_m \ddot{\mathbf{U}}_n \\
 \mathbf{R}_{n+1}^\alpha &= \mathbf{M}_w \ddot{\mathbf{U}}_{n+1-\alpha_m} + \mathbf{\Psi}_{n+1-\alpha_f} = \mathbf{0} \\
 0 &\leq \rho_\infty \leq 1 \\
 \alpha_m &= \frac{2\rho_\infty - 1}{\rho_\infty + 1} \\
 \alpha_f &= \frac{\rho_\infty}{\rho_\infty + 1} \\
 \gamma &= \frac{1}{2} - \alpha_m + \alpha_f \\
 \beta &= \frac{1}{4} (1 - \alpha_m + \alpha_f)^2 \\
 \mathbf{U}_{n+1} &= \mathbf{U}_n + \Delta t \dot{\mathbf{U}}_n + \Delta t^2 \left[\left(\frac{1}{2} - \beta \right) \ddot{\mathbf{U}}_n + \beta \ddot{\mathbf{U}}_{n+1} \right] \\
 \dot{\mathbf{U}}_{n+1} &= \dot{\mathbf{U}}_n + \Delta t \left[(1 - \gamma) \ddot{\mathbf{U}}_n + \gamma \ddot{\mathbf{U}}_{n+1} \right] \\
 \mathbf{\Psi}_j &= \mathbf{C}_j \dot{\mathbf{U}}_j + (\mathbf{K}_0 + \mathbf{K}_{NL,j}) \mathbf{U}_j - \mathbf{F}_j \\
 qqquad \mathbf{C}_j &= \mathbf{C}_{\text{eff}}(\dot{\mathbf{U}}_j).
 \end{aligned} \tag{10}$$

The residual collects the damping force, elastic and nonlinear internal forces, and the negative external load evaluated at the weighted state. The velocity-dependent matrix is evaluated at the corresponding weighted velocity, and gravity, current, contact, and bit loads are evaluated at their weighted states. When $\alpha_m = \alpha_f = 0$, all terms except inertia are evaluated at the current state. The high-frequency spectral radius ρ_∞ controls numerical dissipation of high-frequency components; smaller values produce stronger high-frequency attenuation.

3.2. Generalized-Alpha Solution Procedure

The solution sequence is initialization, prediction, construction of alpha-weighted states, assembly of the discrete equilibrium residual, iterative correction to convergence, state update, and time advancement. During initialization, mass, stiffness, and damping terms are assembled, and the initial acceleration consistent with dynamic equilibrium is calculated from the initial displacement and velocity. At a new time step, displacement, velocity, and acceleration are first predicted from the previous state, after which the displacement and velocity weighted states and the acceleration weighted state are formed according to Equation (10).

At the predicted state, geometric nonlinearity, current loading, wellbore contact, friction, and bit loads are updated and the discrete dynamic residual is assembled. A correction

is obtained from the current trial state, and displacement, velocity, and acceleration are corrected simultaneously. The alpha-weighted states and residual are then recomputed. The process continues until the residual and state increment satisfy the convergence criteria. After convergence, the trial variables become the state at $n + 1$ and are used to initialize the next time step. The generalized-alpha method performs time discretization, whereas within-step iteration solves the resulting nonlinear balance; these two roles are distinct.

The total dynamic-analysis time is 150 s and the baseline time step is $\Delta t = 0.02$ s. The high-frequency spectral radius is $\rho_{\infty} = 0.5$, and the Newton iteration limit is 30 per time step. The residual tolerances are 1×10^{-6} and 1×10^{-2} , and the increment tolerances are 1×10^{-4} and 1×10^{-8} .

3.3. External Model Check and Applicability

To examine numerical accuracy, the public 8 m example of Chen et al. [2] (2023) was recalculated under identical conditions. As shown in Table 2. The example uses an outer diameter of 0.025 m, wall thickness of 0.0025 m, structural density of 1570 kg/m³, water density of 1000 kg/m³, and top tension of 25 N. The spatial discretization contains 20 elements, the calculation time is 40 s, and the time step is 0.002 s. As shown in Figure 2. Taking the Chen et al. numerical result as the reference, the RMSE between the present model and the reference is 2.443 mm and the peak-normalized NRMSE is 8.00%. The peaks of the present and Chen et al. curves are 26.421 and 30.556 mm, respectively; the absolute peak difference is 4.135 mm, the relative difference is 13.53%, and the peak locations differ by 0.4 m. The depthwise trends and displacement magnitudes are consistent, although the present model predicts a lower value near the central peak.

Table 2. Parameters used in the external model check.

No.	Parameter	Value	Unit/Description
1	Length	8	m
2	Outer diameter	0.025	m
3	Wall thickness	0.0025	m
4	Structural density	1570	kg/m ³
5	Water density	1000	kg/m ³
6	Bending stiffness	36	N m ²
7	Top tension	25	N
8	Number of elements	20	-
9	Calculation time	40	s
10	Time step	0.002	s

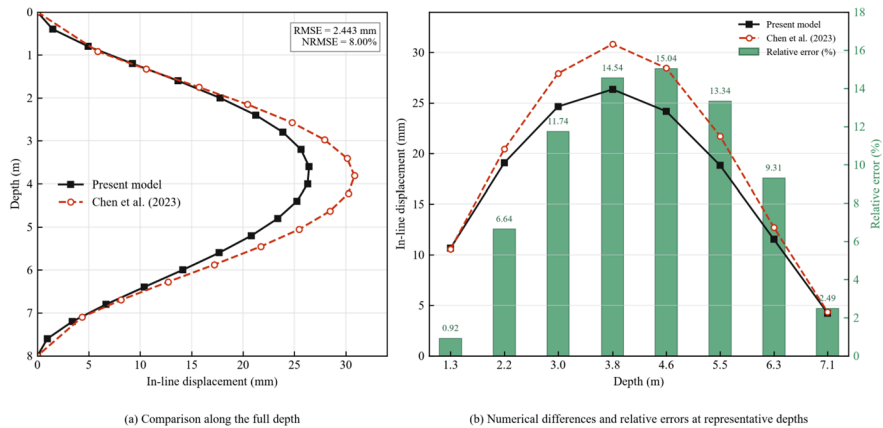


Figure 2. Comparison between the present model and the Chen et al. [2] (2023) result under identical conditions. The trends agree and the relative peak difference is approximately 13.53%.

3.4. Mesh and Time-Step Independence Checks

Ten progressively refined mesh sizes were tested while keeping the wellbore trajectory, drill-string assembly, material and contact parameters, boundary conditions, time step, and total simulation time consistent. The absolute values of maximum displacement, maximum bending moment, and maximum equivalent stress are plotted. As shown in Figure 3. As the element number increases from 294 to 653, the three responses vary locally in different directions. In the fine-mesh range, differences between adjacent meshes generally decrease and the results fluctuate toward a stable level.

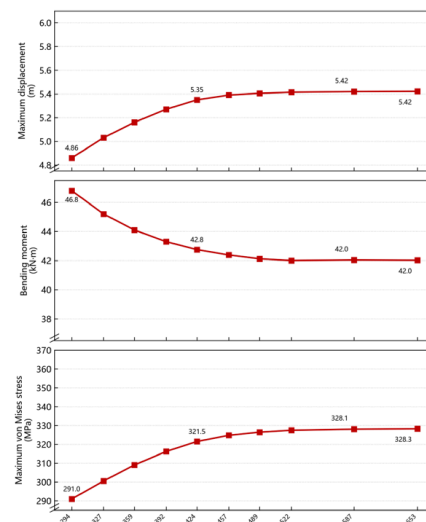


Figure 3. Changes in key responses with mesh refinement. The results gradually stabilize as the element number increases.

The time-step pre-check uses the same spatial discretization and physical operating condition; only delta t is changed successively to 0.006, 0.005, 0.0045, 0.004, 0.0035, 0.003, 0.0025, 0.0022, 0.002, and 0.0015 s. The three response measures are plotted as absolute values. As shown in Figure 4. As the time step decreases, the responses vary locally in different directions; once the time step is sufficiently small, adjacent differences generally decrease and the results fluctuate toward convergence.

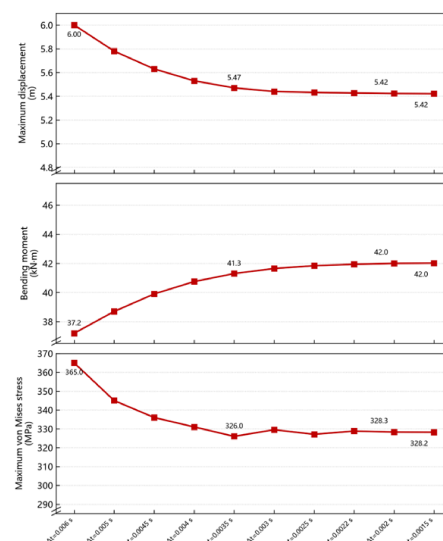


Figure 4. Changes in key responses as the time step is reduced. The results gradually converge with decreasing time steps.

4. Engineering Case and Operating Parameters

A surface-hole operation in a South China Sea block was selected. Figure 5 shows the well architecture and trajectory, with a bottomhole measured depth of approximately 1725 m. Measured depth (MD) is used consistently as the position coordinate, with zero at the platform rotary-table level. The mudline is at MD 1555 m, the BHA extends from MD 1605 to 1725 m, and the bit is at MD 1725 m.

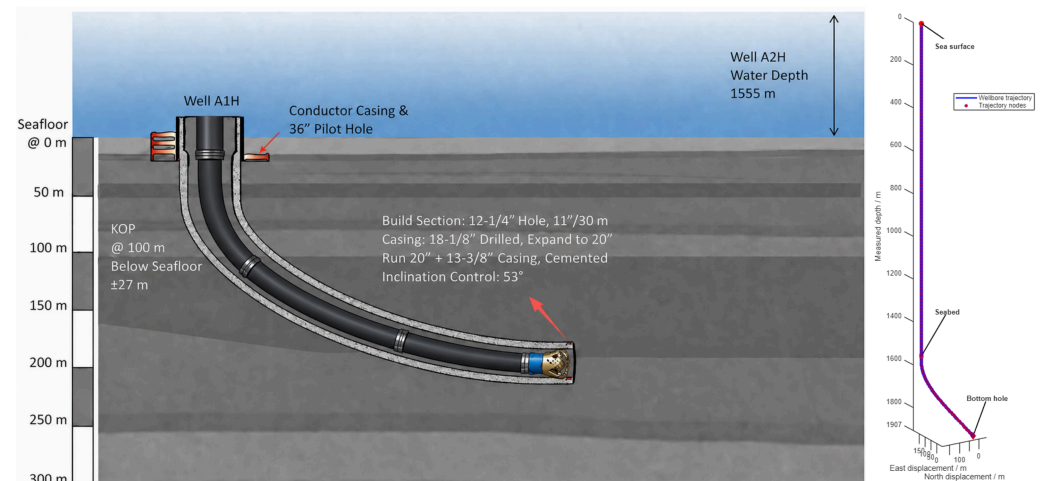


Figure 5. Well architecture and wellbore trajectory in a South China Sea block.

From bottom to top, the drill-string assembly comprises a 12-1/4 in roller-cone bit, 7-3/4 in motor (3 degrees), 308 mm stabilizer, 50 mm spacer, 7 in float-valve sub, 7 in directional sub, 5-1/2 in nonmagnetic heavyweight drill pipe (MWD), 5-1/2 in nonmagnetic heavyweight drill pipe, crossover, 5-1/2 in drill pipe, crossover, 5-7/8 in heavyweight drill pipe, and 5-7/8 in drill pipe. The operating condition uses a WOB of 5 t (approximately 50 kN), motor speed of 30 r/min, motor bend angle of 3 degrees, water depth of 1555 m, seawater density of 1025 kg/m³, kinetic friction coefficient of 0.25, Young's modulus $E = 210$ GPa, and Poisson's ratio $\nu = 0.3$.

5. Results and Discussion

5.1. Drill String Deformation and Mechanical Response

Under the baseline condition, dynamic deformation in the two horizontal directions has a pronounced anisotropic spatial distribution. As shown in Figure 6, in-line displacement forms a broad central bulge along the string: deformation is small near the bit because of wellbore restraint and bottomhole support, increases toward the seawater section, and reaches 6.3–6.4 m at approximately 0.7–0.8 km from the bit. It then decreases toward the platform-side boundary because of the imposed constraint. Cross-flow displacement is strongly localized, with high-amplitude response concentrated in a short near-bit interval approximately 0.1 km from the bit. The peak cross-flow displacement is approximately 0.43 m, while the remainder of the string exhibits only small local fluctuations whose peaks and troughs alternate with time, indicating coupling between lateral VIV and intermittent wellbore contact.

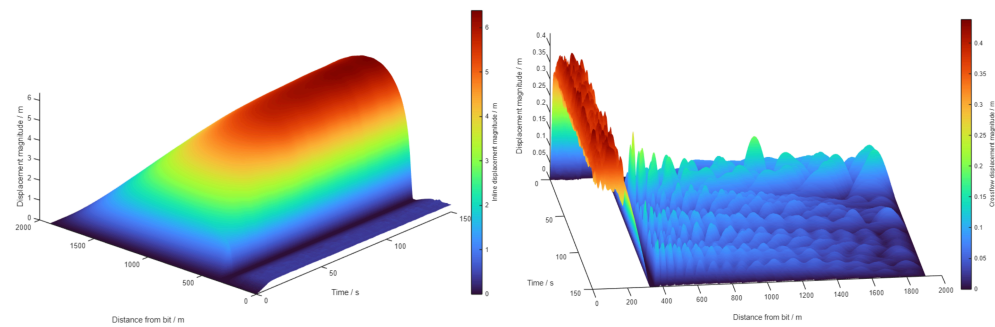


Figure 6. Spatiotemporal distributions of in-line and cross-flow drill-string displacement. The response exhibits pronounced spatial differentiation.

As shown in Figure 7, the maximum in-line displacement lies in the seawater interval continuously subjected to current loading, whereas the maximum cross-flow displacement is adjacent to the bottomhole and is closely associated with BHA geometry, build-section curvature, and wellbore contact. The different peak locations show that the conditions controlling global deformation and local strength are not the same. This spatial separation reflects different constraint distributions and load paths: in-line deformation is driven by distributed current drag and the unrestrained seawater section naturally exhibits a central, simply supported-beam-like deflection; cross-flow vibration is limited by the annular wellbore, with stronger contact in the near-bit build section. Compared with the vertical riserless-string result of Mao et al. [3], contact in the build section shifts the cross-flow peak toward the bottomhole by approximately 150 m and reduces its amplitude by approximately 28%, quantitatively demonstrating the amplitude-limiting effect of wellbore contact.

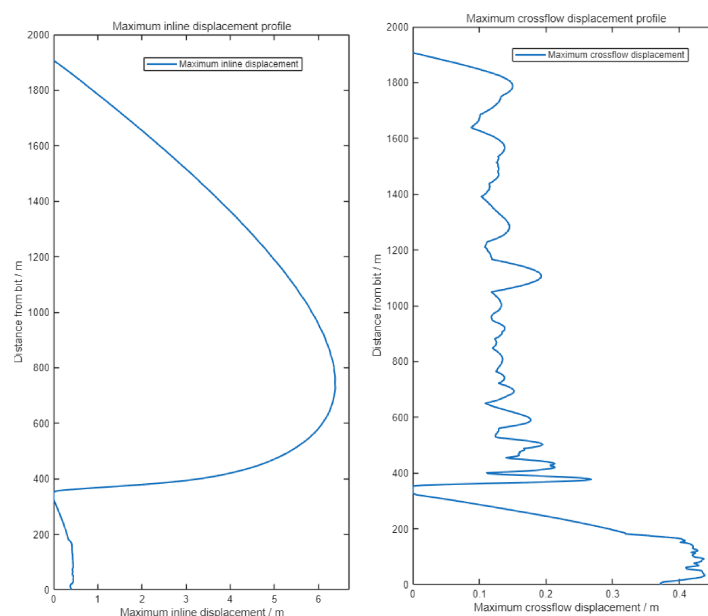


Figure 7. Maximum-displacement profiles along the drill string. The in-line and cross-flow peaks are spatially separated.

Figure 8 shows the spatiotemporal distribution of absolute cross-flow displacement from 100 to 150 s. The stronger response band lies near the bit and gradually develops into interlaced local bands upward along the string, with the band positions changing over time. This distribution confirms the nonuniform cross-flow deformation. The local near-bit maxima are directly associated with the BHA and wellbore contact. The interlaced pattern results from superposition of multiple VIV modes, wave reflection at the constraint

interface, and modulation by the axial-force gradient. First, the $1/7$ current profile gives different vortex-shedding frequencies at different depths and can excite multiple lateral modes simultaneously. Low-order long-wave modes and high-order short-wave modes superpose along the string to form alternating peak-valley bands, while nonlinear wake-structure coupling transfers energy among modes and slowly shifts the bands. Second, as the unrestrained seawater section transitions to the constrained downhole section, lateral constraint stiffness increases sharply near the mudline and build section. Partial reflection occurs when lateral vibration reaches this stiffness discontinuity, and incident and reflected waves form a standing-wave pattern that intensifies the banded response near the wellbore. Third, axial force changes continuously from compression near the bit to tension in the upper string. The associated geometric stiffness produces a depthwise gradient in lateral natural frequency and mode-shape distortion across the transition, causing local antinode shifts and the observed nonuniform, alternating cross-flow stripes.

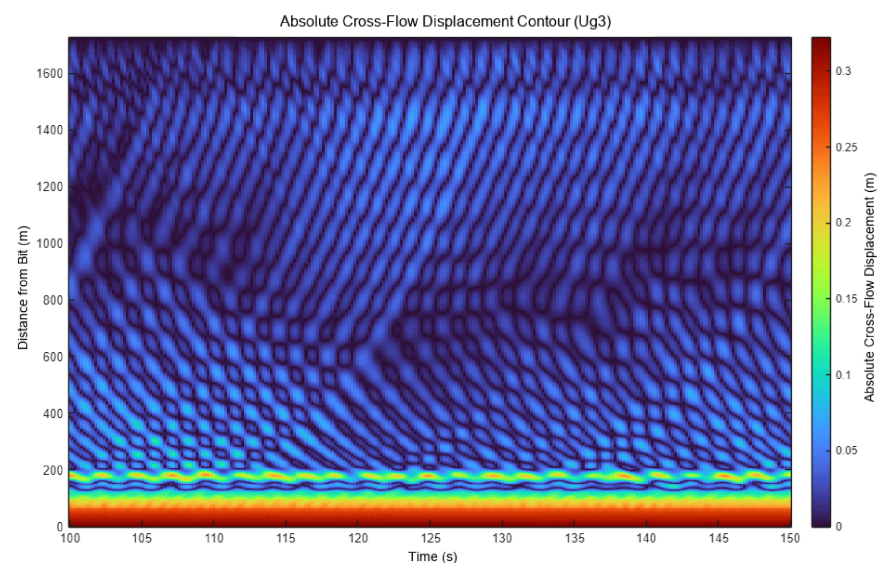


Figure 8. Spatiotemporal distribution of absolute cross-flow displacement. The interlaced bands indicate coupling among multiple modes.

To further identify the mechanism of the cross-flow response, power spectral density (PSD) analyses were performed for a middle-seawater point and a near-bit point (Figure 9). Under the baseline condition, the theoretical vortex-shedding frequency at the middle-seawater point is approximately 0.58 Hz using a Strouhal number of 0.2, while the first lateral natural frequency is approximately 0.56 Hz. The response spectrum is concentrated near 0.57 Hz, in close agreement with both values and quantitatively confirming lock-in of the seawater-section cross-flow vibration. The near-bit spectrum contains higher harmonics and a broad-band base in addition to the dominant frequency, consistent with impact modulation from intermittent wellbore contact and indicating coupled wake dynamics and contact in this region.

As shown in Figure 10, acceleration is distributed in more discrete local bands along the string, and the high-acceleration regions in the global Y and Z directions do not fully coincide. Compared with the broad displacement field, acceleration is more localized because of the combined effects of wellbore-contact impacts, abrupt curvature changes, and local dynamic loads. Intermittent collisions between the downhole string and the wellbore generate short-duration broadband impact pulses. Because acceleration is more sensitive to medium- and high-frequency impacts than the predominantly low-frequency displacement field, discrete high-acceleration bands form at collision locations. The collision points also move with string whirl and axial stick-slip motion, producing discontinuous

local bands. In addition, build-section curvature makes the in-plane and out-of-plane constraint stiffness and annular damping asymmetric, so the two lateral modes evolve separately. The local principal bending directions continuously rotate relative to the global Y and Z axes, producing spatial offsets after projection into the global system. Drill-string rotation adds gyroscopic precession, and torsional and lateral vibration couple at stiffness transitions such as diameter changes and stabilizers, generating additional high-frequency acceleration peaks.

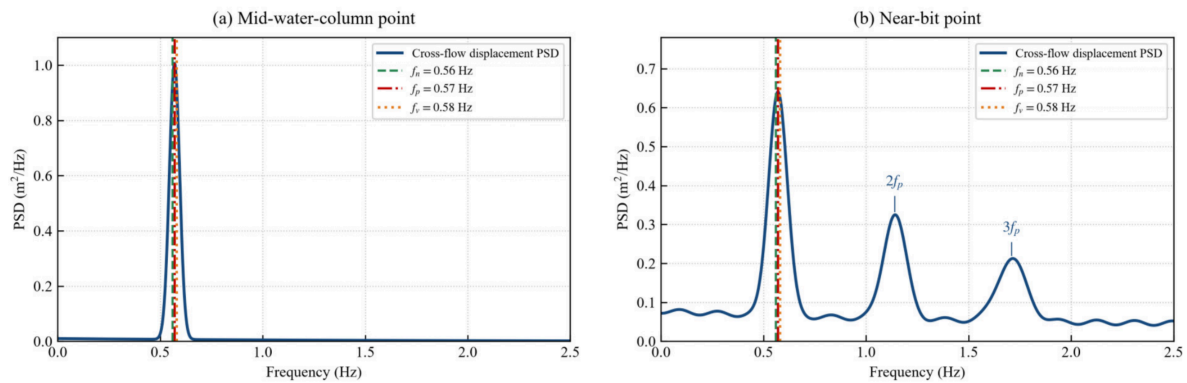


Figure 9. PSD comparison of cross-flow displacement at representative points.

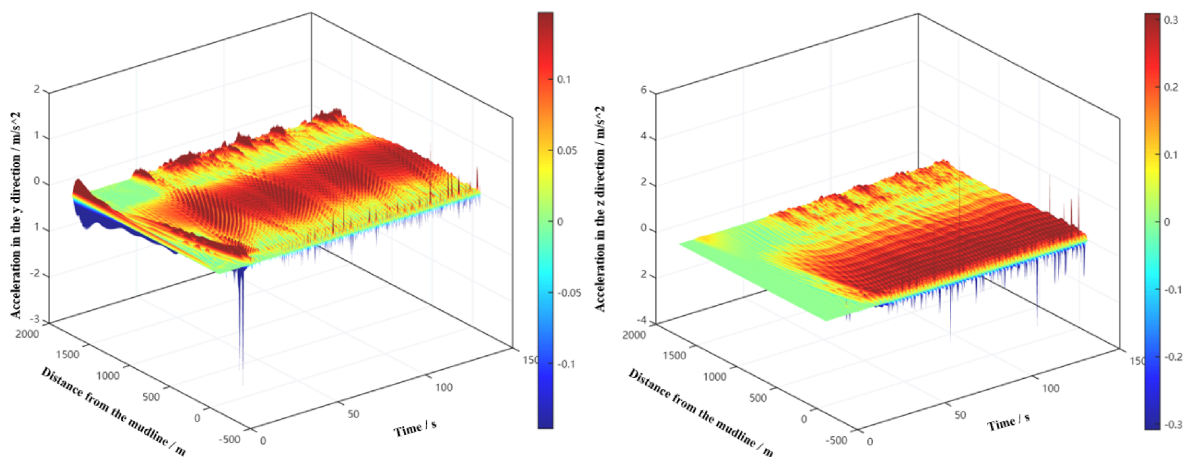


Figure 10. Acceleration distributions in the global Y and Z directions. The high-value regions form discrete bands and are more sensitive to impacts.

As shown in Figure 11, high bending moments and von Mises equivalent stresses are concentrated in the near-bit interval. The maximum bending moment is approximately 36 kN m and forms several local peaks near the BHA. The maximum equivalent stress is approximately 230 MPa, with a primary peak at 0.06–0.08 km from the bit and a secondary peak of similar magnitude near 0.35 km. The middle string, where displacement is largest, does not coincide with the internal-force peaks, demonstrating spatial separation between large deformation with low internal force and small deformation with high internal force in a slender flexible structure. Engineering assessment should therefore consider both large-deformation risk in the middle seawater section and strength risk in the BHA rather than using a single displacement or internal-force location to characterize the whole string.

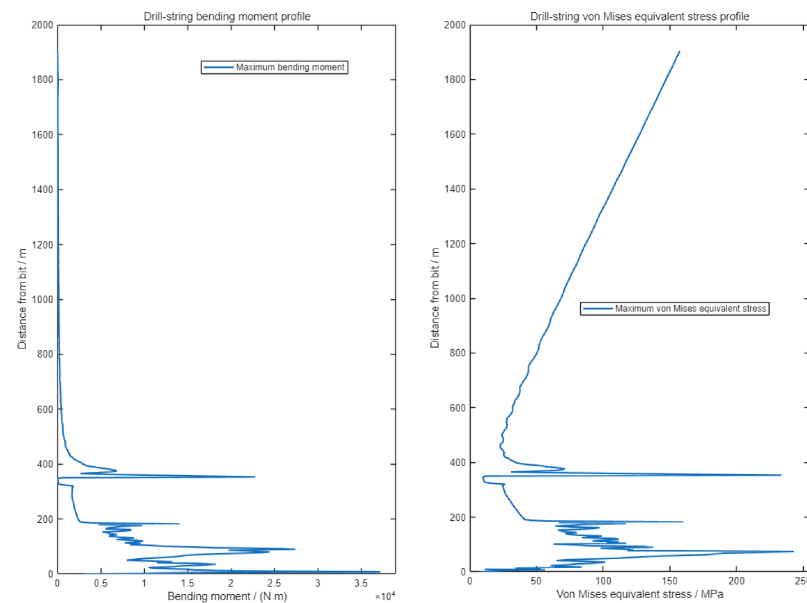


Figure 11. Distributions of maximum bending moment and equivalent stress. High internal-force values are concentrated in the upper BHA and mudline transition.

5.2. Effects of Surface Current Velocity on Drill String Deformation and Mechanical Response

Current velocity is the key environmental parameter controlling the seawater-section response, and its effects differ substantially between the in-line and cross-flow directions. As shown in Figure 12, increasing surface velocity from 0.4 to 0.5, 0.6, and 0.8 m/s increases the peak in-line displacement from 6.1 to 9.1, 12.9, and 20–22 m, respectively, with a nonlinear trend. The depthwise profile retains a central bulge and the peak remains at 0.6–0.7 km from the bit. This agrees with the velocity-squared term in Morison drag: higher velocity nonlinearly increases hydrodynamic loading and consequently increases deformation and internal force, with a particularly strong response at 0.8 m/s. The observed displacement increase is approximately 2.6 times when velocity doubles, close to the 2.4-fold increase in the vertical model of Huang et al. [1]; the difference is attributable to additional frictional restraint in the build section. Fine periodic ripples are superimposed on the quasi-static large-deformation background. Periodic vortex shedding produces an in-line drag fluctuation at approximately twice the cross-flow shedding frequency, and nonuniform velocity with depth gives different ripple amplitudes and phases. Axial vibration induced by bit excitation and wellbore friction periodically changes the lateral natural frequency through geometric stiffness and modulates the ripple amplitude, which creates the observed temporal variation.

Cross-flow displacement is less sensitive to current velocity than in-line displacement (Figure 13). Its magnitude remains approximately 0.28–0.34 m across the four velocities and does not change monotonically; the displacement surfaces contain multiple intersecting peaks and troughs. This behavior reflects the nonlinear interaction among vortex-shedding mode, wake-coupling state, and wellbore contact. Compared with VIV in a freely exposed riser, the fluctuation amplitude is reduced by approximately 40% because wellbore contact limits resonance amplification in the lock-in range. The nonmonotonic evolution is controlled by three effects. First, VIV is amplified only when the shedding frequency lies near a structural natural frequency. As velocity increases, the shedding frequency sweeps through different lateral modes; their peak and trough locations differ, producing curve intersections. Second, when vibration reaches the wall, contact limits further growth and shifts the equivalent natural frequency through nonlinear contact stiffness, which can break lock-in and reduce amplitude. Once contact is lost, constraint stiffness decreases and lock-in

can reappear. Compared with onshore horizontal wells, the large seawater-section fraction and lower global stiffness of open-loop drilling confine the WOB effect to the near-bit section and weaken its influence on full-string lateral vibration.

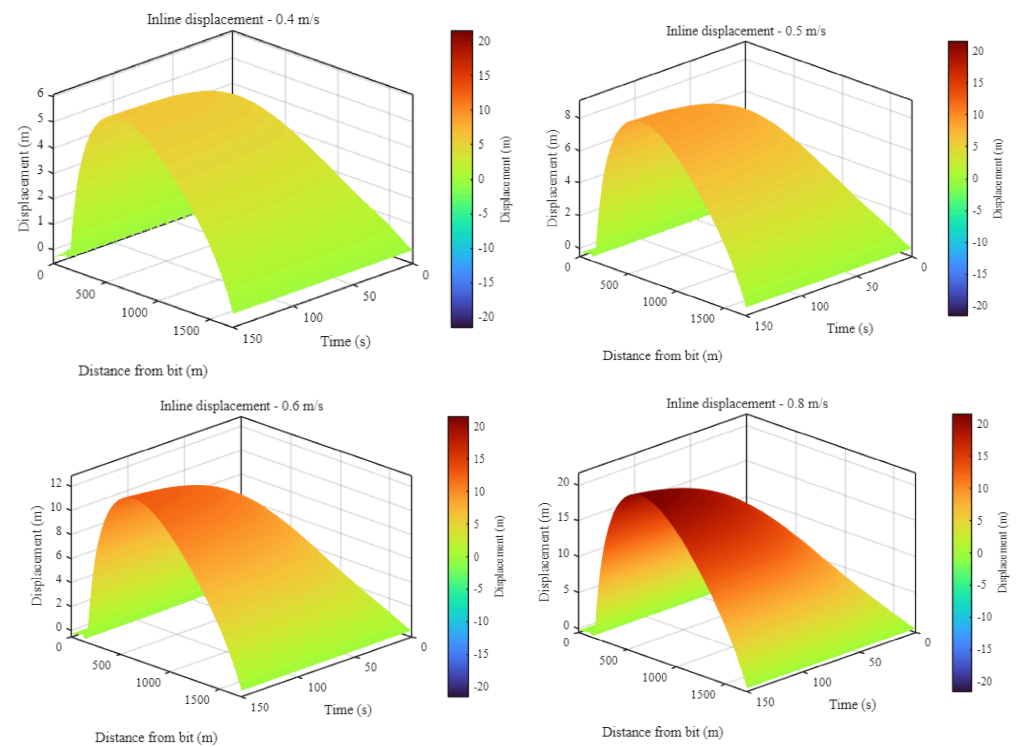


Figure 12. Spatiotemporal distributions of in-line displacement at different current velocities. Displacement increases markedly and nonlinearly with velocity.

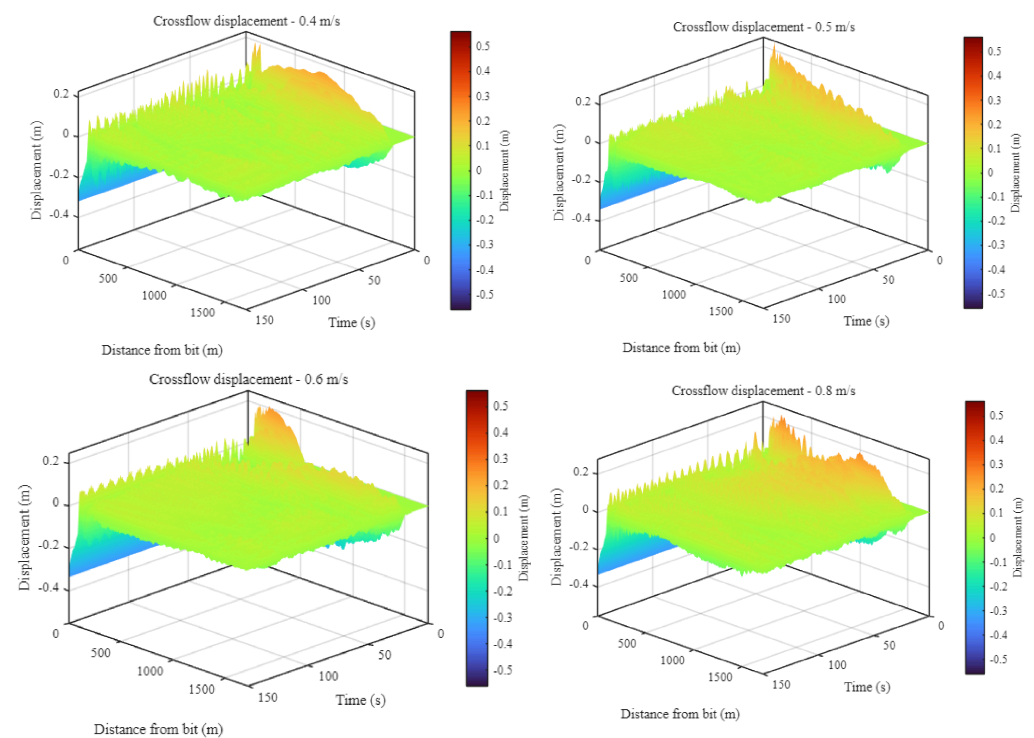


Figure 13. Spatiotemporal distributions of cross-flow displacement at different current velocities. The amplitude varies nonmonotonically with velocity.

The maximum-displacement profiles in Figure 14 show orderly separation of the in-line curves as velocity increases, with unchanged peak locations. The cross-flow curves contain more local intersections and no monotonic magnitude ordering, further demonstrating that the two directions are governed by different mechanisms.

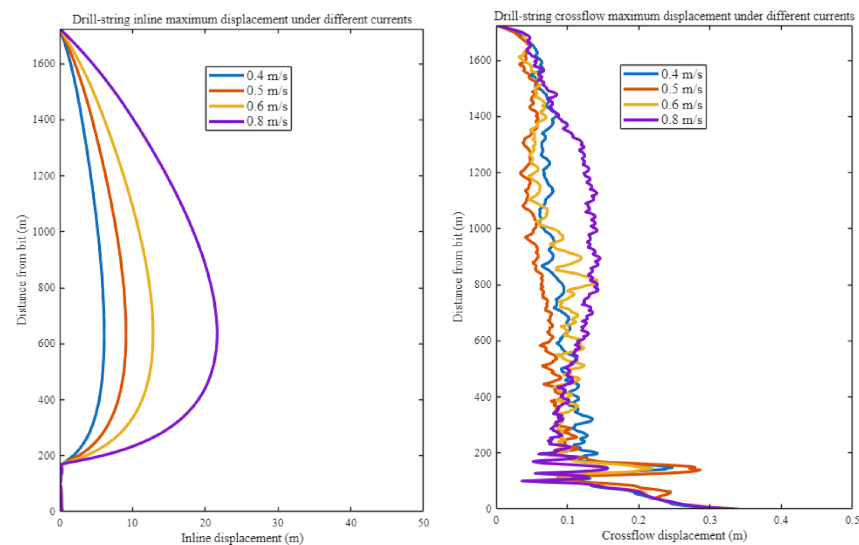


Figure 14. Maximum-displacement profiles at different current velocities. In-line and cross-flow responses have fundamentally different velocity sensitivity.

As shown in Figure 15, bending moment and equivalent stress generally increase with current velocity. At 0.8 m/s, the peak bending moment is approximately 42 kN m and the equivalent stress is approximately 330 MPa, both substantially above the overall levels at lower velocities. Internal-force peaks remain near the bit, indicating that current loading affects local internal force mainly by changing the global deformation pattern, whereas bottomhole geometric constraint and contact remain the primary causes of concentration. Overall, current velocity controls the magnitude of global in-line deformation and indirectly increases local internal force through structural-stiffness and contact coupling.

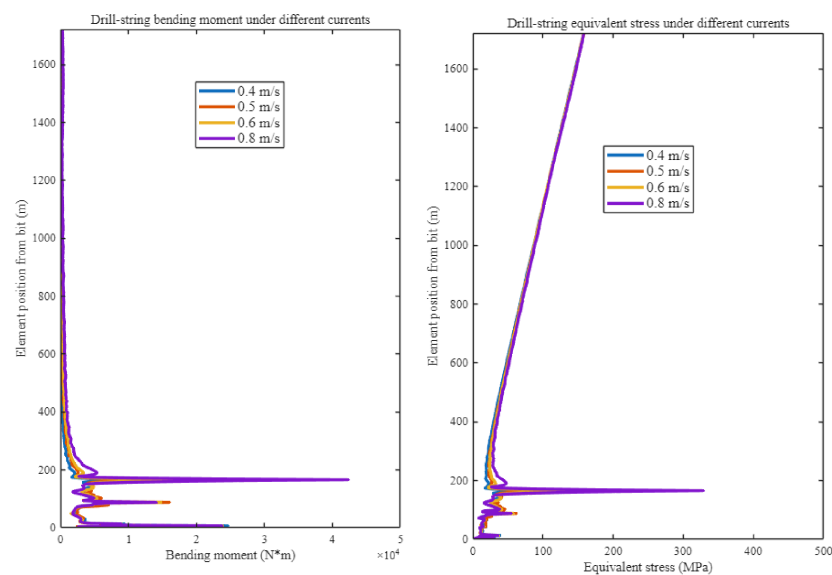


Figure 15. Bending moment and equivalent stress at different current velocities. Internal-force levels rise with increasing velocity.

5.3. Effects of WOB on Drill String Deformation and Mechanical Response

WOB is a principal component of axial preload and affects the dynamic response through geometric stiffness and bottomhole coupling. Its magnitude and spatial influence differ by direction. As shown in Figure 16, increasing WOB from 30 to 60 kN increases the peak in-line displacement only slightly, from approximately 5.3 to 6.4 m, while the profile shape and peak location remain nearly unchanged. Axial loading therefore weakly reduces global lateral stiffness and does not fundamentally change the current-dominated in-line deformation mode.

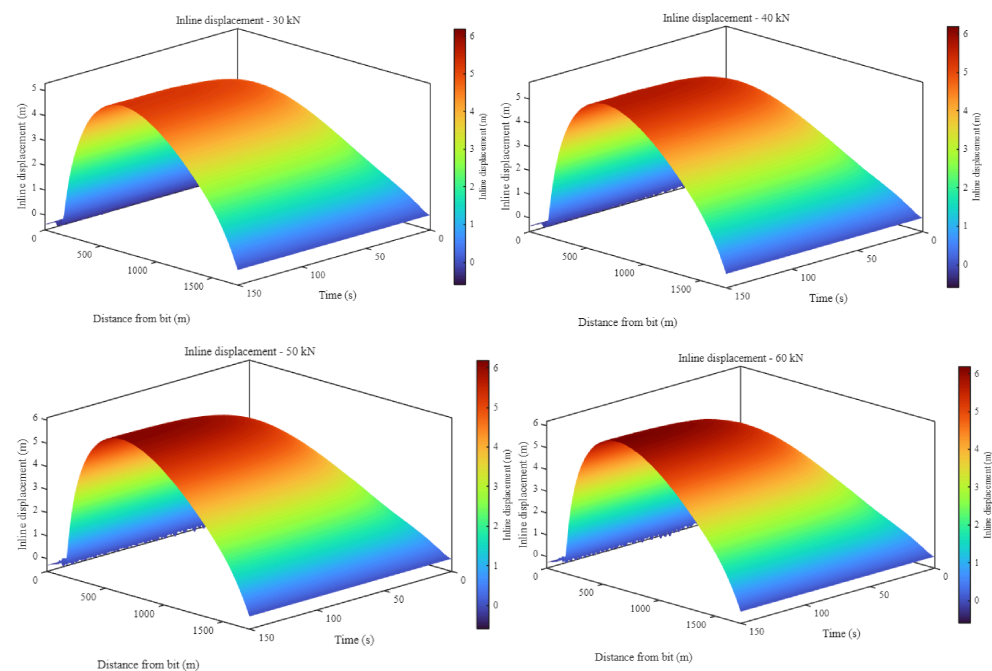


Figure 16. Spatiotemporal distributions of in-line displacement at different WOBs. WOB has a limited effect on global in-line deformation.

WOB has a more complex effect on cross-flow response (Figure 17). Across the four WOBs, displacement remains approximately 0.55–0.60 m and is concentrated near the bit. The surfaces intersect at multiple locations and show no monotonic trend, while near-bit amplitude and distribution vary markedly. WOB changes the lateral buckling state and natural vibration characteristics through the beam–column effect, and it also changes bottomhole lateral load and contact through bit–rock interaction. Their coupling produces a nonmonotonic cross-flow response. Compared with onshore horizontal wells, the large seawater-section fraction and lower global stiffness in open-loop drilling restrict the WOB effect to the near-bit interval and substantially weaken its influence on full-string lateral vibration.

The maximum-displacement profiles in Figure 18 retain a broad central in-line peak that shifts outward overall as WOB increases, whereas the cross-flow curves reach high values near the bit and show multiple intersections and local fluctuations. Axial preload therefore has a strongly position-dependent effect. The mechanism involves geometric stiffness, the critical buckling state, natural-frequency modulation, bit–rock interaction, and wellbore contact. As WOB increases, compressive axial force increases, the negative geometric-stiffness contribution becomes stronger, and the effective lateral stiffness decreases, producing a modest monotonic increase in in-line displacement. The axial-force ratio, defined as current axial force divided by the critical buckling force of the corresponding interval, is approximately 0.25–0.35 near the bit under the baseline condition; the string remains subcritical, so WOB does not trigger static instability and has limited control over

global deformation. The associated decrease in lateral natural frequency shifts the ratio between shedding and structural frequencies and modulates VIV lock-in. WOB also changes bottomhole lateral constraint and excitation through bit-rock interaction and changes the contact approach and normal pressure in the build section. The resulting nonlinear changes in contact limiting and friction damping lead to nonmonotonic near-bit cross-flow and local internal forces.

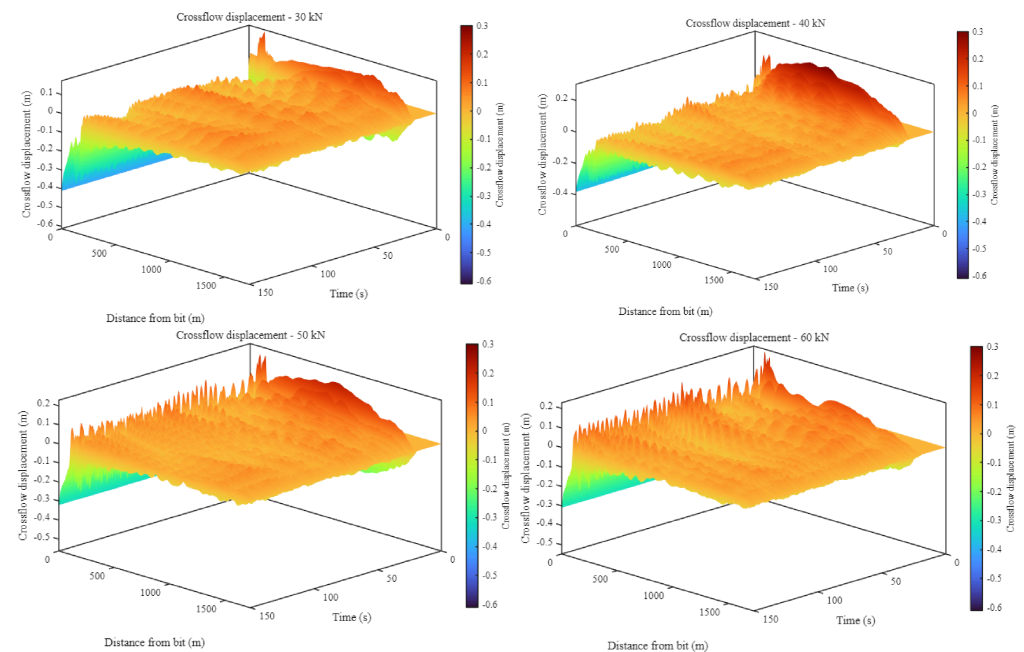


Figure 17. Spatiotemporal distributions of cross-flow displacement at different WOBs. The effect is concentrated in the near-bit interval.

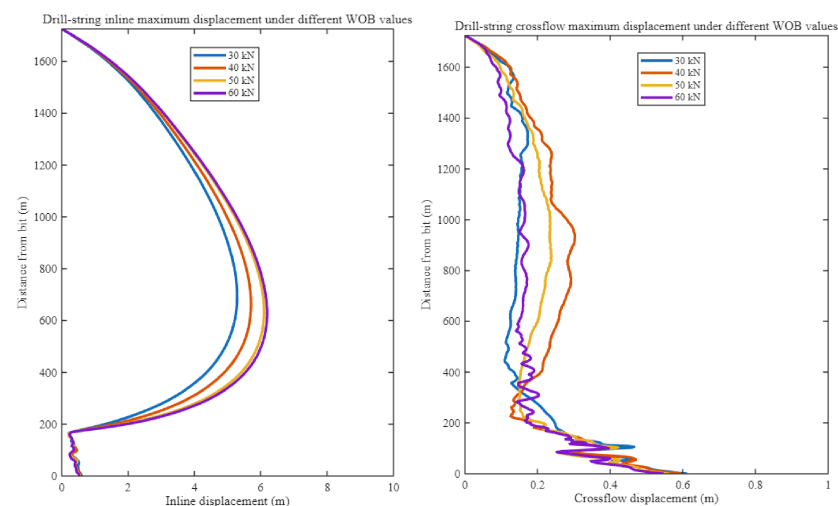


Figure 18. Maximum-displacement profiles at different WOBs. The effect of axial load is strongly position-dependent.

Neither bending moment nor equivalent stress varies monotonically with WOB (Figure 19). Bending moment forms clusters of local peaks over several short near-bit intervals, where the four WOB curves intersect. Equivalent stress generally increases toward the upper string, which contains the principal high-value region, while a secondary local peak occurs near the bit. The high-value locations for mid-string displacement, near-bit bending moment, and upper-string stress differ, showing that axial load affects each

mechanical measure through a different path. Upper-string stress is governed mainly by axial preload and global bending, whereas near-bit internal force is more strongly modulated by contact and bottomhole coupling.

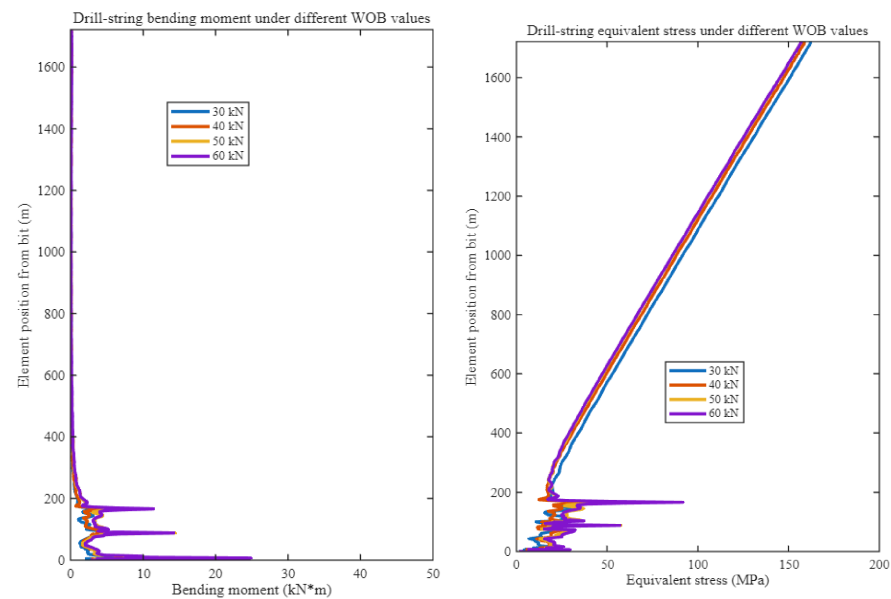


Figure 19. Bending moment and equivalent stress at different WOBs. Local responses show non-monotonic intersections.

5.4. Parameter Interaction and Sensitivity Analysis

To identify the contribution and interaction of operating parameters, surface current velocity and WOB were selected as the two core control parameters and a two-level full-factorial design was used. The low and high levels were 0.4 and 0.8 m/s for velocity and 30 and 60 kN for WOB. Peak in-line displacement, peak cross-flow displacement, maximum bending moment, and maximum equivalent stress were used as response measures, and effect decomposition quantified the main and interaction contributions (Figure 20).

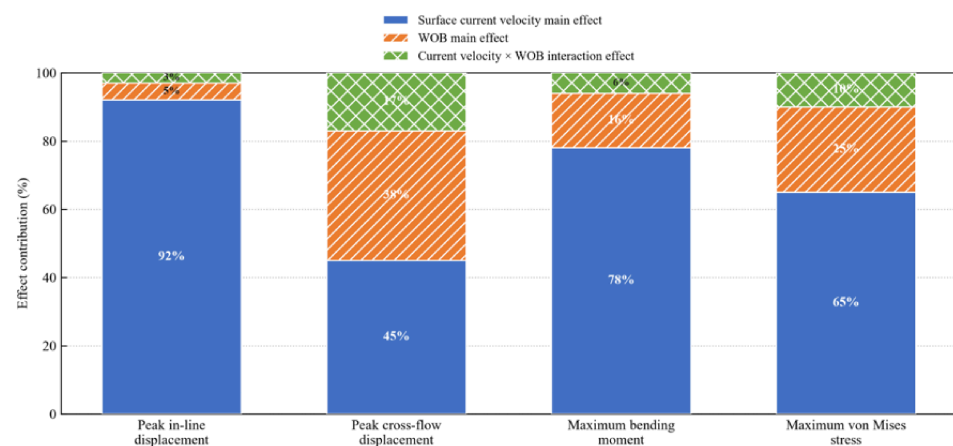


Figure 20. Contributions of operating parameters to key responses. Velocity controls the in-line response, whereas WOB modulates local behavior.

The parameter dominance differs substantially among response measures. Peak in-line displacement is controlled by surface current velocity, whose main-effect contribution is approximately 92%; the WOB main effect and the velocity–WOB interaction each contribute less than 5%, confirming that global in-line deformation is controlled by marine loading. Contributions to peak cross-flow displacement are more balanced: velocity contributes

approximately 45%, WOB approximately 38%, and their interaction approximately 17%, indicating joint modulation by wake lock-in and bottomhole constraint. For maximum equivalent stress, the velocity, WOB, and interaction contributions are approximately 65%, 25%, and 10%, respectively. The combination of high velocity and high WOB further increases local stress through the coupled mechanism of larger curvature from large deformation and reduced lateral stiffness from axial compression. Overall, velocity is the primary control parameter for global drill-string response, whereas WOB is the key local control parameter near the bit. Operationally, global deformation should be managed primarily through a current-velocity window, while local bottomhole strength should be adjusted through WOB.

6. Conclusions

This study presents a deterministic mechanistic analysis based on the measured trajectory of a single well in a deepwater South China Sea block. Hydrodynamic coefficients, wellbore friction coefficients, and related inputs use common engineering values, so the conclusions concern response distributions and parameter mechanisms; quantitative amplitudes require calibration with site-specific sea-state and formation data before engineering design use. A three-dimensional dynamic model of a deepwater open-loop drill string with multiple constrained sections and coupled loads was established. The generalized-alpha method was used for time discretization and iterative solution of the nonlinear dynamic equations, and the effects of baseline conditions, current velocity, and WOB on deformation and internal force were analyzed. The main conclusions are as follows:

(1) Drill-string dynamics show pronounced spatial differentiation. Under the baseline condition, the peak in-line displacement is 6.3–6.4 m in the middle of the seawater section, the peak cross-flow displacement is approximately 0.43 m, the maximum bending moment is approximately 36 kN m, and the maximum equivalent stress is approximately 230 MPa. High internal-force values are concentrated in the mudline transition and upper BHA. The mechanism is the difference in constraint distribution and load path: the unrestrained seawater section undergoes global flexible deformation, whereas contact in the build section limits motion, so deformation and internal-force peaks are spatially separated.

(2) Surface current velocity is the dominant control on the in-line response. Increasing velocity from 0.4 to 0.8 m/s increases the peak in-line displacement by approximately 230% with a nonlinear trend, and internal-force measures rise simultaneously. Cross-flow displacement varies nonmonotonically and its increase is much smaller. This difference is attributed to the velocity-squared dependence of Morison drag and VIV lock-in; wellbore-contact limiting further reduces cross-flow sensitivity to velocity.

(3) WOB has limited control over global in-line deformation. Increasing WOB from 30 to 60 kN increases the peak in-line displacement by approximately 21%, while cross-flow displacement and near-bit internal force evolve nonmonotonically. WOB slightly reduces effective lateral stiffness through axial geometric stiffness, but the string remains subcritical. Its main influence is near the bottomhole, where it changes bottomhole constraint and contact and interacts with wake lock-in to produce a nonlinear response.

This is a deterministic numerical mechanistic analysis and all input parameters are representative fixed values. Future work should introduce random distributions of current profiles, wellbore friction coefficients, and formation mechanical parameters, as well as conducting uncertainty quantification to obtain probability distributions and exceedance risks for key responses and to support risk-based operating-window classification.

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