

Article

Flow Characteristics and Pipeline Stability in Slope-Confined Submarine Canyons

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Abstract

Numerical simulations are conducted to investigate the near-bed flow behaviors of submarine canyons with periodic ridge-valley topography on continental slopes. In the numerical simulation, two topographic parameters are defined: α represents the basal slope angle of the continental slope, and β denotes the side slope angle of the submarine canyon. The topographic modulation of these two parameters on near-bed flow velocity and pipeline on-bottom stability is explored. The results show that the ribbed slope exerts two distinct modulation mechanisms on near-bed flow fields, which are governed by the relative angle between incoming flow and ridge-valley structures. When the flow is perpendicular to the canyon axis ($\theta = 0^\circ$), local topographic contraction and expansion accelerate flow over ridge crests and reduce near-bed velocity in valleys as β increases. In contrast, when the flow is parallel to the canyon ($\theta = 90^\circ$), lateral confinement by bilateral ridges forms a topographic channel, leading to persistent flow acceleration throughout the entire valley area. Moreover, such directional velocity differences substantially alter the on-bottom stability of pipelines deployed within the canyon. Two pipe-soil interaction models are employed in the stability analysis: the Verley-Lund model for a flat clayey seabed and the Slope-Silt model for a sloping silty seabed. The sensitivity of their predictions to soil undrained shear strength is further evaluated.

Keywords: continental slope; submarine canyons; pipelines; on-bottom stability



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1. Introduction

Subsea pipelines crossing continental slopes transport hydrocarbons from abyssal plains to continental shelves, and their on-bottom stability is critical to pipeline safety [1]. Submarine canyons are widely distributed on the continental slopes and provide effective hydrodynamic shielding for subsea pipelines against hydrodynamic loads induced by currents [2]. Slope-confined submarine canyons, as shown in Figure 1, are primarily formed by the combined effects of tectonic activity, erosion by sediment gravity flows, and sea-level changes. Such canyons generally exhibit U-shaped or V-shaped cross-sectional profiles, and their geometric parameters, including length, width, depth, and longitudinal gradient vary significantly with regional topographic, geological, and hydrodynamic conditions [3–6].

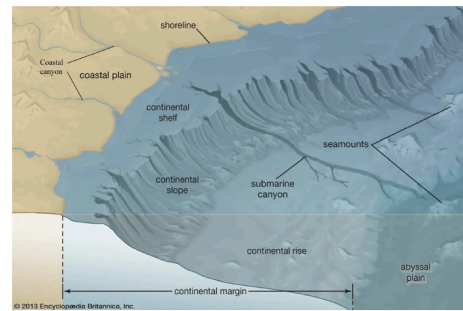


Figure 1. The schematic of the continental margin, image captured from <https://www.britannica.com/science/submarine-canyon> accessed on 20 May 2026.

Submarine canyons exert substantial control over near-bed current dynamic on continental slopes. Over the past few decades, extensive investigations on the flow along continental slopes have been conducted [7–9]. Nevertheless, flow modulated by the submarine canyons has rarely been investigated, despite their significance for evaluating the hydrodynamic loading on subsea pipelines. As typical steep-slope landforms incising the continental slope, slope-confined submarine canyons exhibit unique steep topographic morphology. This distinctive topographic feature can not only disturb the contour-parallel flow patterns but also significantly modulate the velocity distribution and flow direction characteristics of deep-sea currents by altering their vertical and horizontal flow paths [9–11]. Despite these advances, the objectives and spatial scales considered in previous hydrodynamic studies differ from those required for pipeline stability assessment. Existing investigations have mainly focused on large-scale abyssal circulation, contour current dynamics, shelf-slope water exchange, or the hydrodynamic response to an individual canyon configuration [7–11]. These studies demonstrate that submarine topography can substantially redirect and redistribute near-bottom currents, but they do not provide a systematic parameterization of the combined effects of the basal slope angle, canyon side-slope angle, and inflow orientation. From an engineering perspective, pipeline loading is governed by the local near-bed velocity and its spatial variation along the pipeline route, rather than only by the domain-averaged or upstream current velocity. Changes in canyon geometry and inflow direction may produce localized acceleration over ridges and canyon flanks, flow deceleration or recirculation within recessed regions, and redistribution of momentum between the vertical and lateral directions. Therefore, a systematic evaluation of these geometric and directional effects is necessary before canyon-modified currents can be incorporated into engineering design and stability assessment.

Insufficient understanding of canyon-modified flow characteristics may result in inaccurate estimation of the hydrodynamic loads acting on submarine pipelines installed within canyon terrain. This uncertainty is particularly important because canyon-associated geohazards, including turbidity currents and submarine landslides, may impose severe hydrodynamic and impact loads on subsea pipelines [12,13]. Conventional empirical formulations and numerical approaches for hydrodynamic load prediction generally prescribe the ambient current independently of the local canyon topography and therefore cannot fully represent the spatially varying near-bed velocity field encountered by pipelines crossing canyon terrain.

Compared with submarine pipelines deployed on a flat seabed, pipelines laid on canyon slopes exhibit significant differences in on-bottom stability. Subsea pipelines are subjected to drag and lift forces arising from currents, while the basal slope of the continental slope also alters the force balance state of the pipelines [14]. The on-bottom stability of submarine pipelines is essentially a complex coupling process among hydrodynamic loads, pipeline structures, and seabed soil, in which the pipe–soil interaction constitutes

a key factor governing the instability modes of pipelines, such as sliding and overturning [15–17]. Since the 1980s, researchers have developed different pipe–soil interaction models through a series of pipe–soil interaction tests to predict the ultimate soil resistance on pipelines under flow-induced forces. Representative formulations include the energy-based pipe–soil interaction model [18], the Verley–Lund resistance model for pipelines placed on clay soils [19], and the model proposed by Zhao et al. for pipelines on a sloping silt seabed [20]. The ultimate soil resistance refers to the maximum reaction force supplied by seabed sediments to surface-laid subsea pipelines for restraining pipeline instability under external hydrodynamic loads. In most existing pipe–soil interaction models, the ultimate lateral soil resistance is taken as the superposition of sliding resistance and passive soil resistance components, where the passive resistance correlates with the work dissipated by the pipeline during lateral displacement [18,21,22]. In addition, submarine canyons exert a significant influence on flow characteristics, and pipe–soil interaction tends to induce soil liquefaction around pipelines. These factors jointly increase the complexity of on-bottom stability evaluation for subsea pipelines deployed inside submarine canyons [23]. Although considerable progress has been made in the understanding of pipeline instability and pipe–soil interaction, the hydrodynamic and geotechnical components have generally been investigated separately. Hydrodynamic stability studies commonly employ controlled or prescribed current or wave conditions [14–16,20], whereas pipe–soil interaction models primarily describe soil resistance, pipe penetration, and lateral displacement under idealized monotonic, cyclic, or energy-based loading paths [17–19,21–23]. Such approaches are essential for identifying the governing mechanisms of pipeline instability, but they do not explicitly transfer the spatially varying near-bed velocity field generated by canyon topography into pipeline stability calculations. In particular, the influence of canyon-induced flow acceleration and attenuation on the predicted critical stability condition has received limited attention. Consequently, a quantitative connection between the geometric characteristics of slope-confined submarine canyons, the resulting near-bed flow modification, and the on-bottom stability of pipelines remains to be established. To address the above research gaps, the present study develops an integrated framework linking canyon-modified hydrodynamics with the on-bottom stability of subsea pipelines. This study aims to establish a quantitative correlation among canyon topography, near-bed hydrodynamics, and subsea pipeline stability, addressing the research gap arising from the decoupling of hydrodynamic analysis and geotechnical stability evaluation in existing studies. Based on an idealized periodic ridge-valley terrain, the coupled effects of basal slope angle α , canyon side-slope angle β , and inflow orientation θ on the time-averaged near-bed flow field are systematically revealed, and the independent hydrodynamic influence of each topographic parameter, as well as the differences between along-canyon and cross-canyon flow characteristics, are clarified. Empirical formulas relating canyon geometric parameters to the flow amplification and attenuation factors are established to realize the rapid quantitative characterization of terrain effects on local near-bed velocity. Furthermore, adopting two classic pipe–soil interaction models, the spatially varying flow field is incorporated into pipeline stability calculations to explore the mechanisms through which canyon-induced flow variations affect pipeline stability. This study provides a theoretical reference for the safety design of subsea pipelines in canyon sea areas.

The rest of this study is structured as follows. Section 2 briefly describes the numerical model established herein. Analyses of time-averaged flow field characteristics are presented in Section 3, where the regulation exerted by submarine canyons on upstream inflow is highlighted. In Section 4, the on-bottom stability of subsea pipelines is evaluated with consideration of canyon-modified inflow conditions and the pipe–soil interaction. Finally, Section 5 summarizes the main conclusions of this study.

2. Numerical Model of Flow Across and Along Canyons

2.1. Control Equations and Numerical Setup

The seabed is idealized as either a smooth slope or a periodically arranged ridge-valley terrain, as shown in Figure 2. Small-scale topographic undulations and sediment transport processes of the actual seabed are neglected in this study. As illustrated in Figure 3, the computational domain covers a horizontal extent of $2140 \text{ m} \times 2400 \text{ m}$, with a maximum water depth of 500 m. This domain size ensures fully developed flow regimes within both the smooth slope and ridge-and-valley regions. The incident-flow angle (θ) is defined as the angle between the incoming current and the cross-ridge axis. Accordingly, $\theta = 0^\circ$ corresponds to cross-ridge current, $\theta = 90^\circ$ denotes along-ridge flow, and $\theta = 45^\circ$ refers to oblique incoming flow. Figure 3 depicts the boundary configuration for the $\theta = 0^\circ$ case, with prescribed inflow orientations for other incident angles realized by rotating the terrain while maintaining identical boundary configurations.

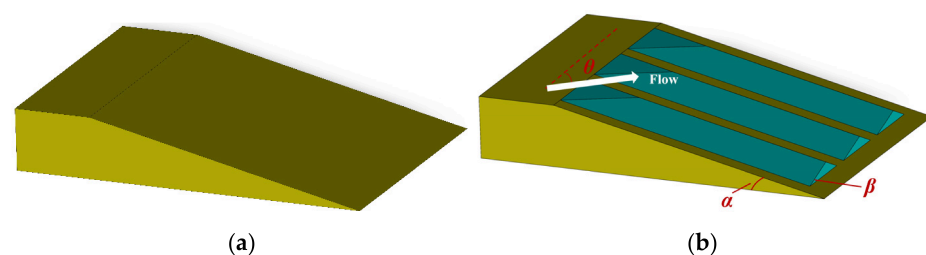


Figure 2. The schematic of (a) the ideal smooth slope and (b) the ribbed slope.

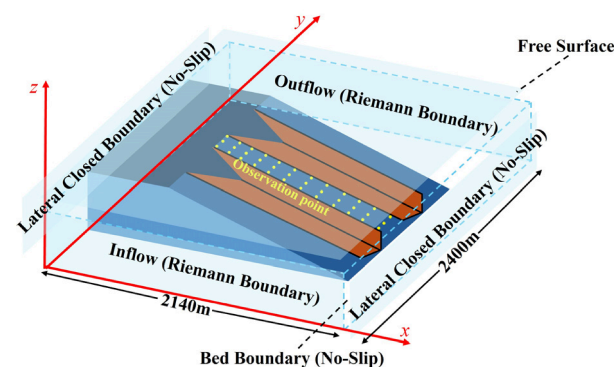


Figure 3. The schematic of the computational domain.

The periodic ridge-valley terrain adopted in this study is a process-oriented idealization designed to isolate the individual and coupled effects of the basal slope angle α and canyon side-slope angle β on the near-bed flow field. Natural submarine canyons may additionally exhibit geometric asymmetry, along-axis curvature, spatially varying side slopes, local bed roughness, and multiple characteristic length scales. These morphological characteristics are not explicitly represented by the present parameterized terrain. Therefore, the numerical results obtained herein should be interpreted as representing the principal topographic flow modification mechanisms associated with α and β , rather than as a direct reproduction of a specific natural submarine canyon.

The computational domain is discretized with a structured quadrilateral mesh featuring a uniform horizontal spacing of 10 m in two orthogonal directions. The water column is divided into 20 non-uniform σ -layers, with the vertical resolution progressively refined near the seabed. The bottom-most layer accounts for 0.32% of the local water depth, enabling improved spatial resolution for near-bed flow and terrain-induced hydrodynamic effects.

A schematic of the computational mesh is illustrated in Figure 4, with further assessment of the selected horizontal and vertical mesh resolutions provided in Section 2.2.

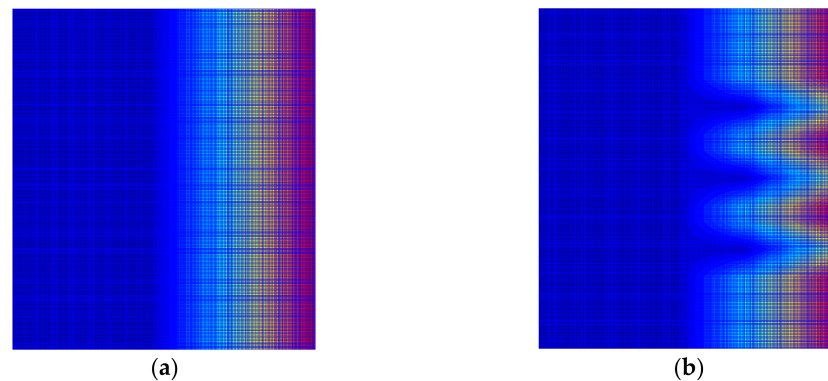


Figure 4. The schematics of grid discretization: (a) the ideal smooth slope, (b) the ribbed slope.

The governing equations consist of the continuity equation and momentum equation simplified under the shallow-water assumption and the Boussinesq approximation, collectively known as the shallow-water equations. Horizontal discretization is implemented within an orthogonal curvilinear coordinate framework, where a Cartesian coordinate system is adopted for the horizontal plane with the x - and y -axes defined as illustrated in Figure 3. The horizontal velocity vector is decomposed into the corresponding Cartesian components as follows:

$$U_h = ue_x + ve_y \quad (1)$$

where U_h denotes the horizontal velocity vector; u and v correspond to the velocity components along the x - and y -directions, respectively; and e_x and e_y stands for the corresponding unit vectors. Two vertical coordinate frameworks are available in Delft3D (Version 4.04.02): the σ -coordinate system and the Cartesian z -coordinate system. The definition of the σ -coordinate is given as follows:

$$\sigma = \frac{z - \zeta}{d + \zeta} = \frac{z - \zeta}{H} \quad (2)$$

where z is the vertical coordinate of the Cartesian coordinate system, ζ is the free-surface elevation measured relative to the still water level, and d refers to the depth of static water, defined as the vertical distance from the seabed to the undisturbed free surface. The total water depth is expressed as $H = d + \zeta$, as illustrated in Figure 5.

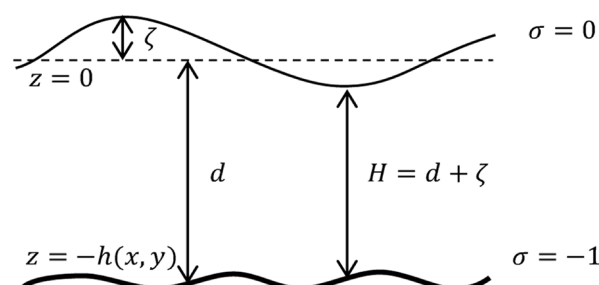


Figure 5. The schematic of the σ -coordinate system.

Following the transformation from the Cartesian z -coordinate system to the σ -coordinate system, the continuity equation and the two horizontal momentum equations are expressed as follows:

$$\frac{\partial \zeta}{\partial t} + \frac{\partial(d + \zeta)u}{\partial x} + \frac{\partial(d + \zeta)v}{\partial y} + \frac{\partial w}{\partial \sigma} = 0 \quad (3)$$

$$\frac{\partial u}{\partial t} + u \frac{\partial u}{\partial x} + v \frac{\partial u}{\partial y} + \frac{\omega}{(d + \zeta)} \frac{\partial u}{\partial \sigma} - f v = -\frac{1}{p_w} p_x + F_x + \frac{1}{(d + \zeta)^2} \frac{\partial}{\partial \sigma} (\nu_v \frac{\partial u}{\partial \sigma}) + M_x \quad (4)$$

$$\frac{\partial v}{\partial t} + u \frac{\partial v}{\partial x} + v \frac{\partial v}{\partial y} + \frac{\omega}{(d + \zeta)} \frac{\partial v}{\partial \sigma} + f u = -\frac{1}{p_w} p_y + F_y + \frac{1}{(d + \zeta)^2} \frac{\partial}{\partial \sigma} (\nu_v \frac{\partial v}{\partial \sigma}) + M_y \quad (5)$$

where ω corresponds to the vertical velocity in the σ -coordinate system; f stands for the Coriolis parameter; P_x and P_y are the pressure gradient terms aligned with the x - and y -axes; F_x and F_y refer to the Reynolds stress terms; ν_v is the vertical eddy viscosity coefficient. M_x and M_y denote source terms and are assigned a value of zero, as no external forcing is incorporated in this model.

The vertical velocity (ω) presented in Equations (3)–(5) is defined with respect to moving σ -coordinate surfaces and thus differs from the physical vertical velocity (w) in the Cartesian framework. The physical vertical velocity is derived via the following coordinate transformation relation:

$$w = \omega + u(\sigma \frac{\partial H}{\partial x} + \frac{\partial \zeta}{\partial x}) + v(\sigma \frac{\partial H}{\partial y} + \frac{\partial \zeta}{\partial y}) + (\sigma \frac{\partial H}{\partial t} + \frac{\partial \zeta}{\partial t}) \quad (6)$$

With the pressure term simplified under the hydrostatic approximation, the expression for pressure (p) is derived by integrating over the water column with constant water density and uniform atmospheric pressure assumed, as follows:

$$\frac{1}{\rho_w} P_x = g \frac{\partial \zeta}{\partial x} \quad (7)$$

$$\frac{1}{\rho_w} P_y = g \frac{\partial \zeta}{\partial y} \quad (8)$$

In Equations (4) and (5) the Reynolds stresses F_x and F_y are evaluated based on the Boussinesq assumption, and their definitions are given as follows:

$$F_x = \frac{\partial \tau_{xx}}{\partial x} + \frac{\partial \tau_{xy}}{\partial y} \quad (9)$$

$$F_y = \frac{\partial \tau_{yx}}{\partial x} + \frac{\partial \tau_{yy}}{\partial y} \quad (10)$$

where the horizontal shear stresses components (τ_{xx} , τ_{xy} , τ_{yx} and τ_{yy}) are calculated via Equations (11)–(13), respectively.

$$\tau_{xx} = 2\nu_H (\frac{\partial u}{\partial x} + \frac{\partial u}{\partial \sigma} \frac{\partial \sigma}{\partial x}) \quad (11)$$

$$\tau_{xy} = \tau_{yx} = \nu_H (\frac{\partial u}{\partial x} + \frac{\partial u}{\partial \sigma} \frac{\partial \sigma}{\partial x} + \frac{\partial v}{\partial x} + \frac{\partial v}{\partial \sigma} \frac{\partial \sigma}{\partial x}) \quad (12)$$

$$\tau_{yy} = 2\nu_H (\frac{\partial v}{\partial y} + \frac{\partial v}{\partial \sigma} \frac{\partial \sigma}{\partial y}) \quad (13)$$

Given the orthogonal Cartesian grid adopted and the assumption of a spatially uniform horizontal eddy-viscosity coefficient ν_H , the divergence of horizontal turbulent stresses simplifies into the following Laplacian form:

$$F_x = \nu_H \left(\frac{\partial^2 u}{\partial x^2} + \frac{\partial^2 u}{\partial y^2} \right) \quad (14)$$

$$F_y = \nu_H \left(\frac{\partial^2 v}{\partial x^2} + \frac{\partial^2 v}{\partial y^2} \right) \quad (15)$$

where ν_H is the eddy viscosity coefficient, which is related to the eddy viscosity coefficient ν_ν .

The k - ε turbulence model is adopted to achieve closure of the governing equations, and the definition of the eddy viscosity coefficient ν_ν is given as follows:

$$\nu_\nu = c'_\mu L \sqrt{K} \quad (16)$$

$$L = c_D \frac{k \sqrt{k}}{\varepsilon} \quad (17)$$

where $c'_\mu = c_\mu^{1/4}$ ($c_\mu = 0.09$) is model coefficient, L is the mixing length, k is the turbulent kinetic energy, ε is the turbulent kinetic energy dissipation rate, and $c_D = c_\mu^{3/4} \approx 0.1643$ is a model coefficient.

The transport equations for k and ε are nonlinearly coupled through the eddy diffusion coefficients D_k , D_ε and other dissipation terms, and their expressions are given as follows:

$$\frac{\partial k}{\partial t} + u \frac{\partial k}{\partial x} + v \frac{\partial k}{\partial y} + \frac{\omega}{d + \zeta} \frac{\partial k}{\partial \sigma} = \frac{1}{(d + \zeta)^2} \frac{\partial}{\partial \sigma} \left(D_k \frac{\partial k}{\partial \sigma} \right) + P_k + P_{kw} + B_k - \varepsilon \quad (18)$$

$$\frac{\partial \varepsilon}{\partial t} + u \frac{\partial \varepsilon}{\partial x} + v \frac{\partial \varepsilon}{\partial y} + \frac{\omega}{d + \zeta} \frac{\partial \varepsilon}{\partial \sigma} = \frac{1}{(d + \zeta)^2} \frac{\partial}{\partial \sigma} \left(D_\varepsilon \frac{\partial \varepsilon}{\partial \sigma} \right) + P_\varepsilon + P_{\varepsilon w} + B_\varepsilon - c_{2\varepsilon} \frac{\varepsilon^2}{k} \quad (19)$$

where P_k , P_ε , B_k , and B_ε are the production terms and dissipation terms of turbulent kinetic energy and dissipation rate, respectively. Additionally, P_{kw} and $P_{\varepsilon w}$ are the wave-induced production terms of turbulent kinetic energy and dissipation rate, respectively. $c_{2\varepsilon}$ is a model coefficient, which is set to 1.92.

Boundary conditions appropriate for the numerical framework are prescribed to complete model closure on the basis of the aforementioned governing equations. The kinematic boundary condition imposed at the free surface is expressed as follows:

$$\omega|_{\sigma=0} = 0 \quad (20)$$

The dynamic free-surface boundary condition is:

$$\frac{\nu_\nu}{H} \frac{\partial u}{\partial \sigma} \Big|_{\sigma=0} = \frac{1}{\rho_w} \left| \vec{\tau}_s \right| \cos(\theta) \quad (21)$$

$$\frac{\nu_\nu}{H} \frac{\partial v}{\partial \sigma} \Big|_{\sigma=0} = \frac{1}{\rho_w} \left| \vec{\tau}_s \right| \sin(\theta) \quad (22)$$

where θ is the angle between the direction of the wind stress vector and the positive y -axis, and τ_s represents surface wind stress. Zero values are assigned to the right-hand terms

of this equation when wind forcing is excluded from the numerical setup. The stress can generally be calculated using the following widely adopted quadratic expression:

$$\left| \vec{\tau}_s \right| = \rho_a C_d U_{10}^2 \quad (23)$$

where ρ_a is the air density, U_{10} represents the wind speed at a height of 10 m above the free surface, C_d is the drag coefficient of wind, whose value depends on the magnitude of U_{10} .

The boundary condition for the turbulence model at the free surface is:

$$k|_{\sigma=0} = \frac{u_{*s}^2}{\sqrt{c_u}} \quad (24)$$

$$\varepsilon|_{\sigma=0} = \frac{u_{*s}^3}{\frac{1}{2}\kappa\Delta z_s} \quad (25)$$

where u_{*s} denotes the surface friction velocity induced by wind; κ is the von Kármán constant with a value of 0.4. Both the turbulent kinetic energy and dissipation rate at the free surface are zero when wind effect is neglected.

The bottom kinematic boundary condition is:

$$\omega|_{\sigma=-1} = 0 \quad (26)$$

The bottom dynamic boundary condition are:

$$\frac{\nu_v}{H} \frac{\partial u}{\partial \sigma} \Big|_{\sigma=-1} = \frac{1}{\rho_w} \tau_{bx} \quad (27)$$

$$\frac{\nu_v}{H} \frac{\partial u}{\partial \sigma} \Big|_{\sigma=-1} = \frac{1}{\rho_w} \tau_{by} \quad (28)$$

where τ_{bx} and τ_{by} represent the seabed shear stresses, which can be solved using the quadratic friction law applied in the three-dimensional model.

$$\tau_b = \frac{g\rho_w \vec{u}_b \left| \vec{u}_b \right|}{C_{3D}^2} \quad (29)$$

where $\vec{u}_b$ is defined as the horizontal flow velocity at the first grid layer above the seabed, and C_{3D} is the Chezy coefficient for the three-dimensional model. Given that the horizontal flow velocity within the wave boundary layer exhibits a logarithmic vertical distribution, $\vec{u}_b$ can be related to the friction velocity (u_*).

$$\vec{u}_b = \frac{\vec{u}_*}{\kappa} \ln\left(1 + \frac{\Delta z_b}{2z_0}\right) \quad (30)$$

the bottom shear stress follows the quadratic friction law, which is expressed in terms of the friction velocity.

$$\tau_b = \rho_w \vec{u}_* \left| \vec{u}_* \right| \quad (31)$$

through the combination of the above equations, the following can be derived:

$$C_{3D} = \frac{\sqrt{g}}{k} \ln\left(1 + \frac{\Delta z_b}{2z_0}\right) \quad (32)$$

where $z_0 = k_s/30$ denotes the seabed roughness height, k_s represents the Nikuradse roughness height, and Δz_b is the reference height, defined as the vertical distance from the seabed to the center of the first grid layer adjacent to the seabed.

The bottom boundary condition for the turbulent equations is given by:

$$k|_{\sigma=-1} = \frac{u_{*b}^2}{\sqrt{C_u}} \quad (33)$$

$$\varepsilon|_{\sigma=-1} = \frac{u_{*b}^3}{\frac{1}{2}\kappa\Delta z_b} \quad (34)$$

The Riemann condition, based on the theory of Engquist and Majda [24], is applied at the open boundary, with its expression given as follows:

$$U \pm \zeta \sqrt{\frac{g}{d}} = F_R(t) \quad (35)$$

Furthermore, the no-slip solid wall boundary condition specifies that flow velocity, shear stress, convective terms, and diffusive terms are all equal to zero.

The governing equations are discretized by the finite difference method, and the Crank–Nicholson implicit scheme is adopted to solve the discretized equations, achieving a favorable balance between numerical stability and accuracy. Within the Delft3D (Version 4.04.02) numerical model, the shallow-water equations are discretized using the Alternating Direction Implicit (ADI) method for efficient numerical solution. A second-order central difference scheme is applied to convective terms, while the same central difference formulation is utilized for diffusive terms.

2.2. Verification of Convergence and Accuracy of the Numerical Model

Mesh convergence verification is carried out using five different mesh refinement schemes with horizontal spacings of 2.5 m, 5 m, 10 m, 20 m, and 40 m. A constant surface current velocity of 2 m/s is assigned as the inflow boundary condition for all the cases; velocity time series at four representative near-seabed monitoring points are examined, with their coordinates, and the results are illustrated in Figure 6.

Taking the results from the 2.5 m mesh as the benchmark reference, the relative deviations obtained with the 40, 20, and 10 m mesh are 2.44%, 1.03%, and 0.53%, respectively. The gradual decline in relative deviations confirms mesh convergence as horizontal mesh resolution is refined. The 10 m mesh limits relative deviations to 0.53% and entails far lower computational cost than the 2.5 m and 5 m meshes, so this mesh scheme is adopted for all subsequent numerical simulations.

Further time-step convergence tests are conducted based on the 10 m mesh scheme. Three different time-steps (0.05 min, 0.1 min, and 0.2 min) are adopted, and the velocity time series at the characteristic locations for each case are presented in Figure 7. It can be observed that when the time-step is reduced from 0.2 min to 0.1 min, the calculation accuracy is significantly improved. With further reduction of the time-step to 0.05 min, negligible discrepancies are detected between the results of the 0.05 min and 0.1 min cases. Accordingly, a fixed time-step of 0.1 min is adopted for all subsequent numerical calculations.

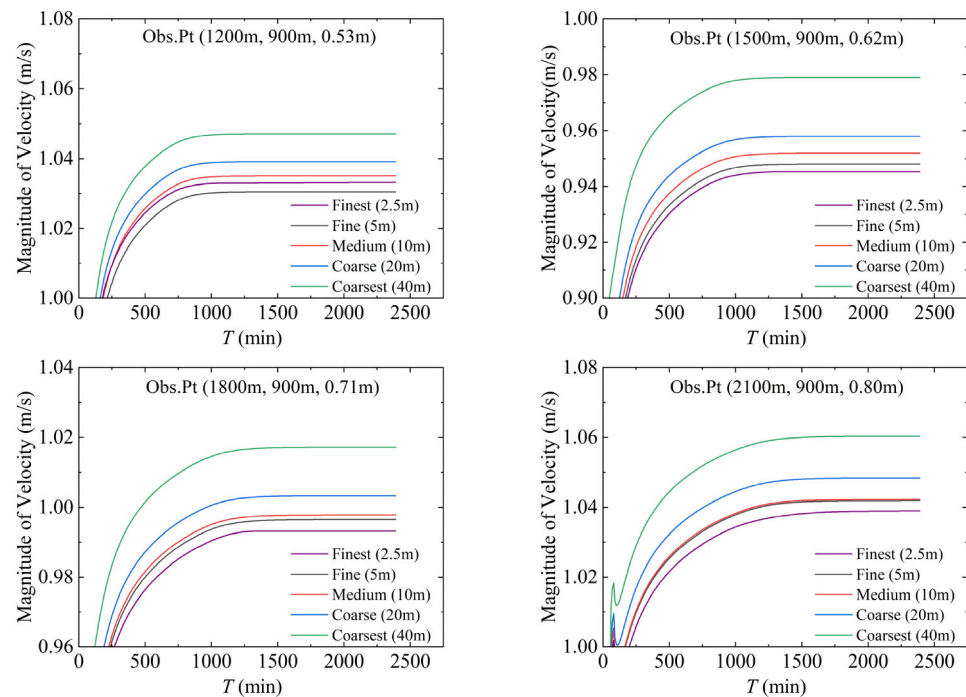


Figure 6. The time series of velocity at four characteristic locations near the seabed (with coordinates (x, y, z) as $(1200, 900, 0.53)$, $(1500, 900, 0.62)$, $(1800, 900, 0.71)$, and $(2100, 900, 0.8)$) for five different mesh resolution schemes.

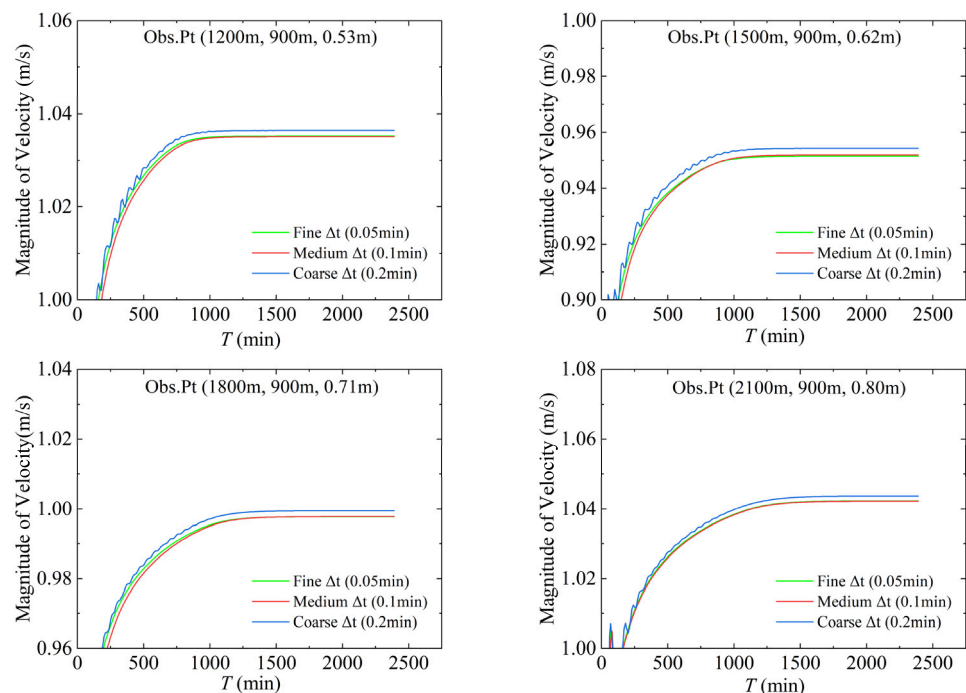


Figure 7. The time series of velocity at four characteristic locations near the seabed (with coordinates (x, y, z) as $(1200, 900, 0.53)$, $(1500, 900, 0.62)$, $(1800, 900, 0.71)$, and $(2100, 900, 0.8)$) for three different time-step resolution schemes.

Referring to the theoretical analysis from Leeuwen et al. [25] and the numerical simulation results of Hibma et al. [26], two slope configurations were simulated using the aforementioned numerical model: one with a short distance of 20 km, and the other with a long distance of 80 km, as shown in Figure 8. The computational domains and model parameters adopted are consistent with those reported in the existing literature [26,27].

Numerical predictions for the two slope configurations are compared against the results of Leeuwen et al. [25] and Hibma et al. [26], with the comparisons presented in Figure 9a,b. The comparisons show that the present model reproduces the principal phase and amplitude characteristics of the free-surface elevation and flow velocity reported for both the short- and long-slope cases.

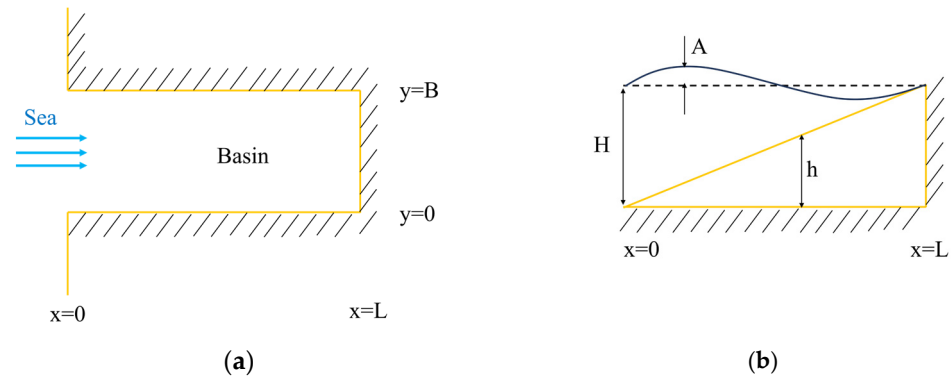


Figure 8. The schematic of the slope: (a) Plan view of the slope, (b) Front view of the slope.

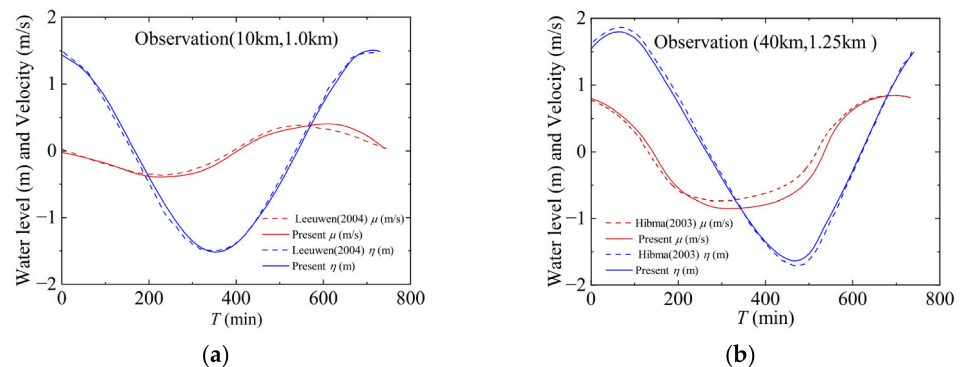


Figure 9. The comparisons of numerical results: (a) short slope [25], (b) long slope [26].

To further strengthen the assessment of the predictive capability of the numerical model, an independent validation is conducted against the trench-flume measurements of van Rijn [27]. The experimental facility consists of a 30 m long and 0.5 m wide flume, with an upstream water depth of 0.39 m and a mean approach velocity of 0.51 m/s. The trench has an excavation depth of 0.175 m and side slopes of approximately 1:3. The experimental geometry is reconstructed in Delft3D using a longitudinal grid resolution of 0.30 m, ten non-equidistant vertical layers, a computational time-step of 0.24 s, the standard $k-\epsilon$ turbulence closure, a White–Colebrook roughness height of 0.025 m, and a horizontal eddy viscosity of $5 \times 10^{-4} \text{ m}^2/\text{s}$. The first 5 min of the simulation is treated as the spin-up period, and the simulated velocity profiles are averaged over the interval from 5 to 10 min.

The measured streamwise velocity profiles at five centerline sections (Profiles 1, 4, 6, 7, and 8) are digitized from the data reported by Faghihirad et al. [28], in which they are identified as the experimental measurements of van Rijn [27]. The simulated time-averaged velocities are linearly interpolated to the elevations of the corresponding experimental measurements. As shown in Figure 10, the model generally reproduces the measured vertical velocity distributions and the principal flow deceleration and recovery across the trench. Across the 31 experimental data points, the overall root-mean-square error (RMSE) and normalized-root-mean-square error (NRMSE) are 0.068 m/s and 15.3%, respectively, indicating acceptable agreement between the numerical predictions and laboratory measurements. Although the trench geometry is not identical to the idealized submarine-

canyon topography considered in this study, the comparison provides an independent experimental assessment of the model's capability to reproduce flow adjustment over abrupt sloping and recessed-bed terrain.

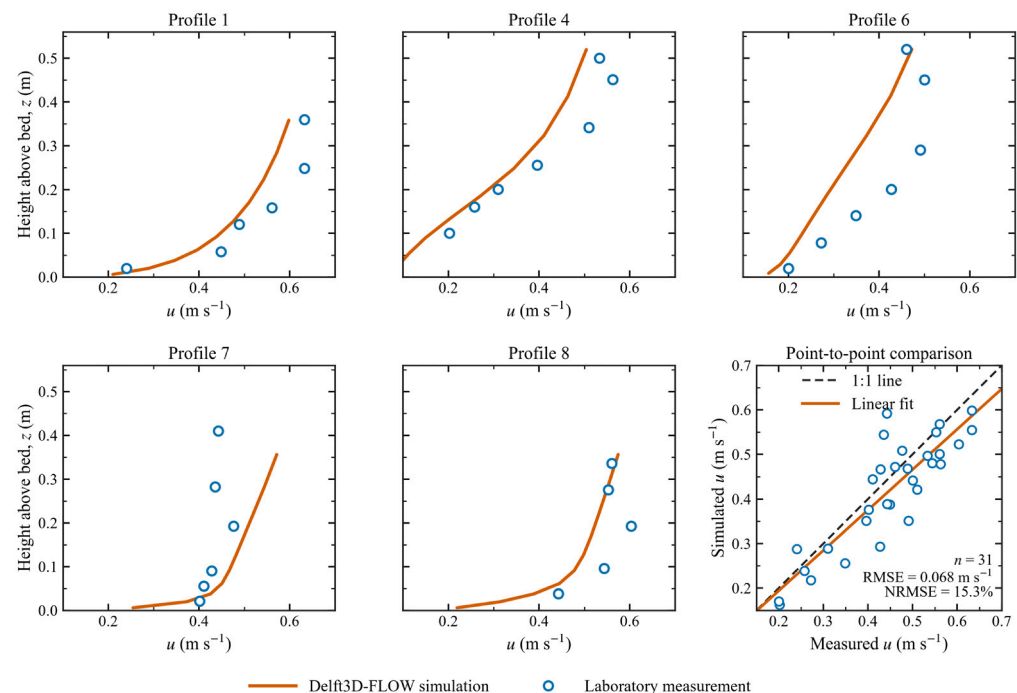


Figure 10. Comparison between the measured and simulated streamwise velocity profiles at Profiles 1, 4, 6, 7, and 8 of the van Rijn trench-flume experiment, together with the overall point-to-point comparison [27].

3. Flow Characteristics Under Different Inflow Attack Angles

Flow characteristics are first analyzed for the two orthogonal inflow orientations of $\theta = 0^\circ$ and $\theta = 90^\circ$. The effects of the ridge–valley topography on the near-bed flow field are examined, together with the sensitivity of flow attenuation or amplification to the basal slope angle (α) and canyon side-slope angle (β). Based on these two limiting inflow orientations, a directional superposition method is subsequently developed to predict the flow response at the intermediate orientation of $\theta = 45^\circ$, and its predictions are compared with the corresponding numerical simulations.

3.1. Flow Characteristics at the Attack Angle of $\theta = 0^\circ$

Velocity contours on the bottom layer ($\sigma = 1$) for both smooth and ribbed slopes are presented in Figure 11 at an attack angle of $\theta = 0^\circ$, with a surface inflow velocity of 2 m/s. Compared with the smooth slope, the ribbed slope induces a regular modification in the flow field when exposed to perpendicular impingement by unidirectional flow on a series of parallel ridges. Local high-velocity regions form at the ridge crests, whereas local low-velocity regions develop in the ridge valleys; these two flow regimes exhibit distinct spatial distributions closely correlated with canyon geometric characteristics.

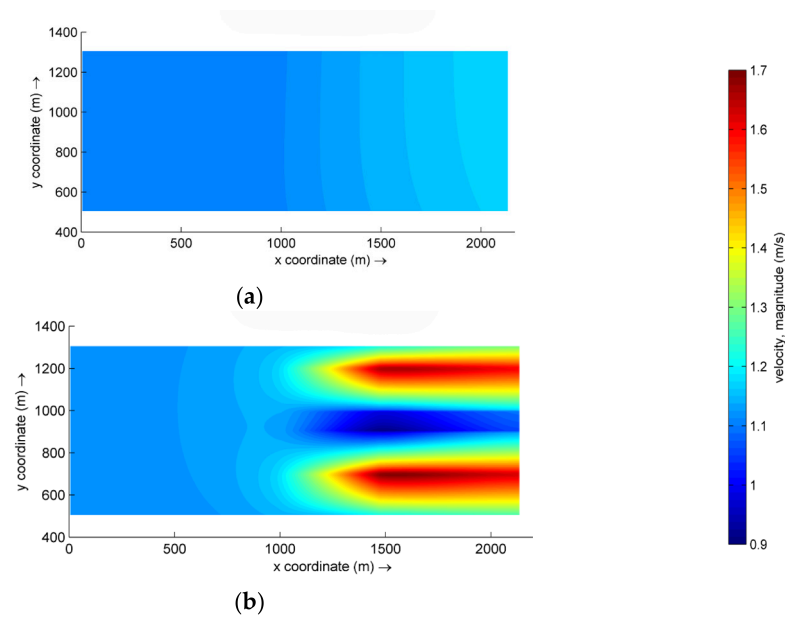


Figure 11. Contours of velocity at $\sigma = 1$ on the slope of $\alpha = 10^\circ$ (a) without and (b) with ribs of $\beta = 20^\circ$ ($\theta = 0^\circ$).

Velocity vector contours extracted from y - z planes at multiple streamwise x coordinates for the ribbed slope are shown in Figure 12. As the flow approaches the ridges, topographic constriction triggers streamline convergence, resulting in flow acceleration over the ridge crests. Immediately downstream of the crests, flow expands into the widened canyon cross-section, leading to a marked decrease in near-bed velocity, which persists in an attenuated state.

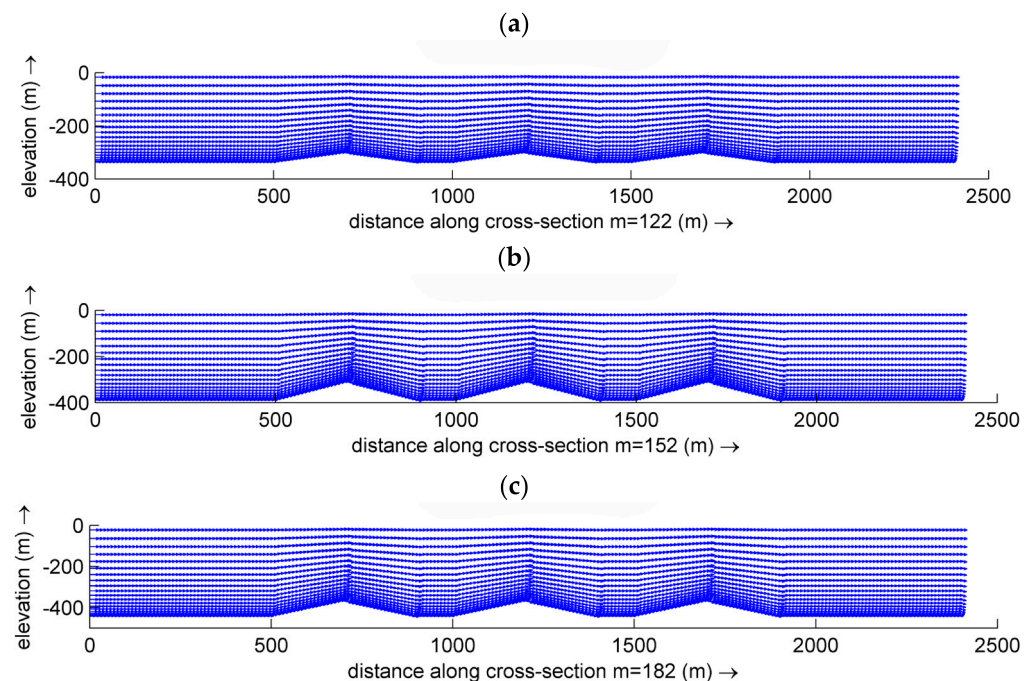


Figure 12. Contours of velocity vectors in the y - z plane at (a) $x = 1200$ m, (b) $x = 1500$ m, and (c) $x = 1800$ m for the ribbed slope.

For the cross-canyon inflow condition, the flow response can be interpreted in terms of continuity, pressure adjustment, and momentum redistribution. As the incoming current approaches the upstream face of a ridge, the effective flow passage contracts and the

streamlines converge, causing local acceleration over the ridge crest. After passing the crest, the flow enters the expanded valley section and is subjected to an adverse pressure adjustment. This expansion promotes vertical momentum exchange and increases the tendency for local flow separation and turbulent mixing on the downstream side of the ridge. Consequently, the near-bed momentum recovers more slowly than the flow in the upper part of the water column, producing a persistent low-velocity region within the valley. Repetition of this process over successive ridges generates alternating high-velocity zones at the ridge crests and sheltered low-velocity zones in the valleys. Therefore, the canyon topography does not provide spatially uniform shielding; instead, it redistributes the hydrodynamic exposure between the ridge crests and valley floors.

To quantify the attenuation effect induced by ridge-valley structures on the near-bed flow velocity, a two-dimensional monitoring array is deployed, with monitoring points arranged as follows: twelve points are distributed along the streamwise x -direction of the slope, and three rows are arranged along the cross-slope y -direction. The positions of these sampling points are indicated by the yellow symbols in Figure 13. A velocity amplification factor is defined by comparing velocity value obtained at identical sampling points for the smooth and ribbed slope cases; its mathematical expression is given in Equation (36).

$$\alpha_c = (V_{\text{ridge}}/V_{\text{slope}} - 1) \times 100\% \quad (36)$$

where V_{ridge} and V_{slope} denote the near-bed velocities over the ribbed and smooth slopes, respectively. As can be deduced from this definition, positive values of the velocity amplification factor $\alpha_c > 0$ correspond to flow acceleration of near-bed flow induced by ridge topography. In contrast, values of $\alpha_c < 0$ indicate that the ridge topography reduce the near-bed velocity. The spatially averaged amplification factor is defined as the arithmetic mean of the local amplification factors at all monitoring points. This quantity is used hereafter to compare the overall topographic effects for different combinations of α and β .

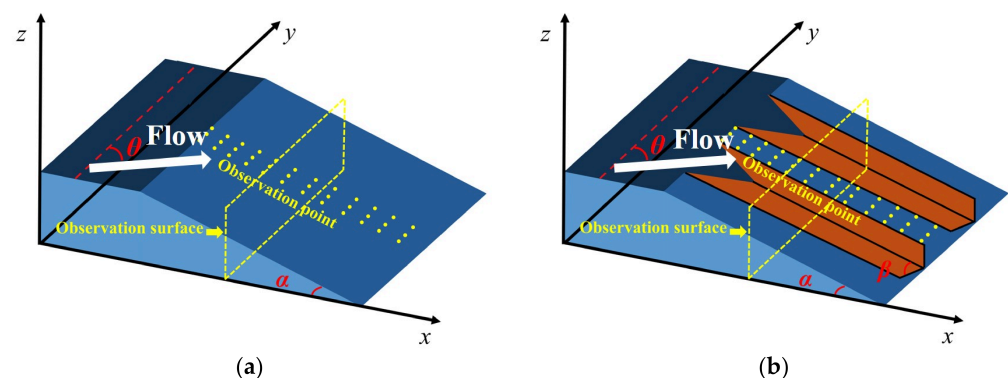


Figure 13. The schematic of seabed bottom-layer flow velocity monitoring points on the slope of $\alpha = 10^\circ$ (a) without and (b) with ribs of $\beta = 20^\circ$.

Based on the velocity amplification factor defined in Equation (36), the velocity attenuation results for surface inflow velocities ranging from 1 to 1.75 m/s are presented in Figure 14. Although the velocity attenuation coefficients of the ridges and valleys show considerable differences along the y -direction, their overall variation trends along the x -direction remain consistent and exhibit strong similarity. The relative attenuation is found to be independent of the incoming surface current magnitude within the tested velocity range. This finding indicates that the flow regulation mechanism is controlled kinematically by the terrain topography instead of being dynamically scaled with flow velocity, which facilitates the development of simplified predictive models.

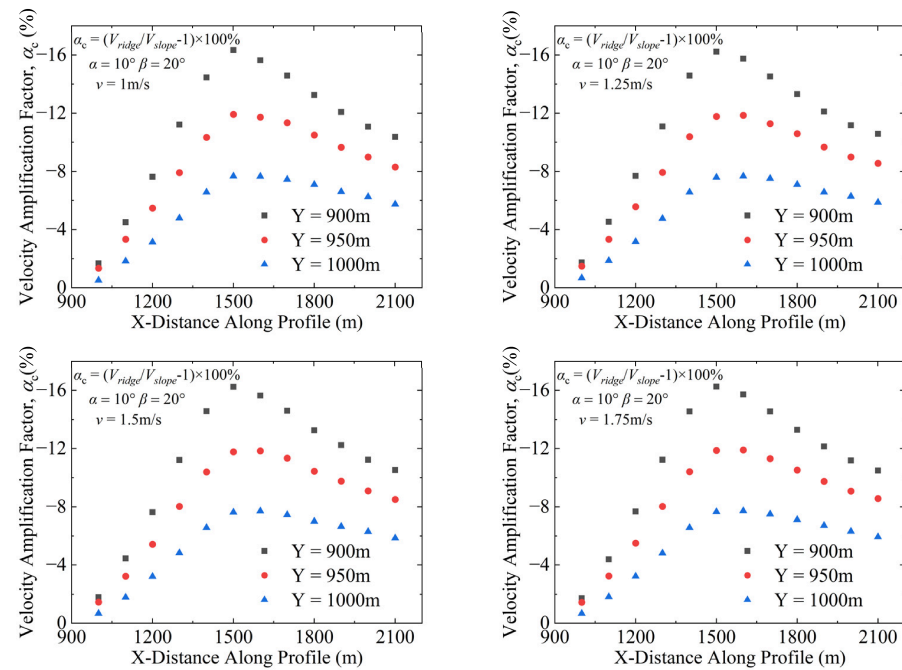


Figure 14. Velocity attenuation acquired based on simulations with surface flow velocity ranging from 1–1.75 m/s.

The intensity of the flow velocity attenuation effect at the valley exhibits significant sensitivity to the geometric parameters of the slope and ridge, as shown in Figure 14. To further quantify the impacts of geometric features on the amplification factor, the base slope angle (α) and ridge angle (β) are adopted to parameterize the morphologies of the ribbed slope with a parameter schematic provided in Figure 13. Taking the case with a base slope angle of $\alpha = 10^\circ$ in Figure 15 as an illustration, for a fixed base slope, increasing the ridge steepness (β) increases the maximum velocity attenuation. Specifically, as β increases from 10° to 30° , the maximum attenuation grows from approximately 7% to 26%.

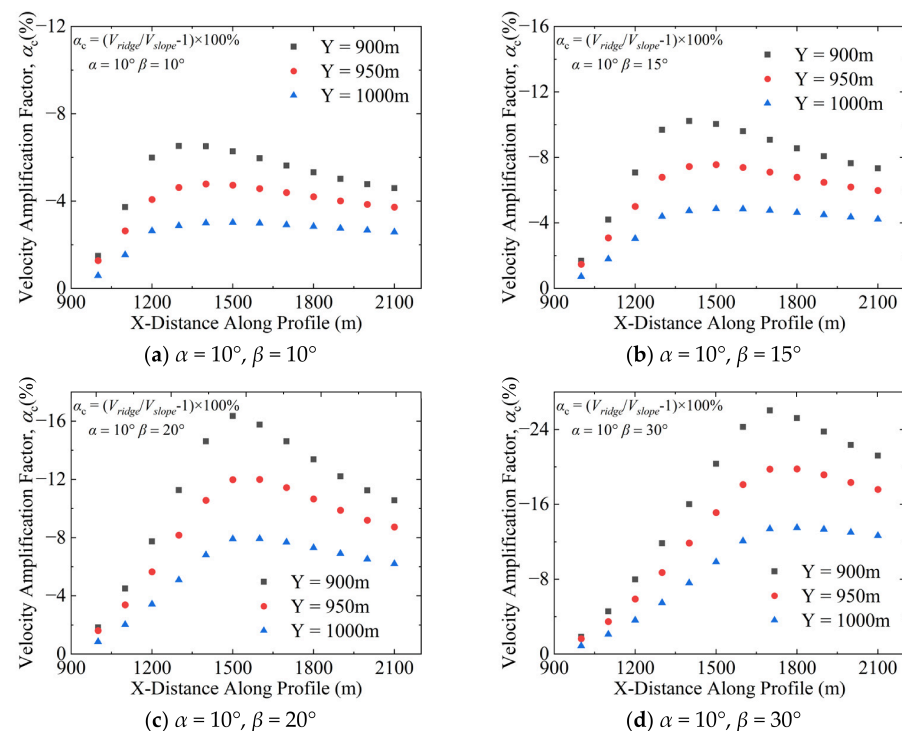


Figure 15. Velocity amplification factor along the x-direction for various ridge angles at $\alpha = 10^\circ$.

The velocity amplification factor for ridge angles of 10° and 20° are shown in Figure 16; it can be found that the base slope angle (α) also exerts a regulatory effect on the flow velocity attenuation of the valley. More importantly, a strong nonlinear coupling exists between α and β . Simultaneous increase in both angles yields multiplicative growth in attenuation magnitude. Among all the tested topographic combinations, the peak attenuation value of approximately 22% is observed for the case ($\alpha = 15^\circ, \beta = 20^\circ$); in contrast, only around 4.5% attenuation is induced by the gentle topographic combination ($\alpha = 5^\circ, \beta = 10^\circ$), with a remarkable difference between these two cases.

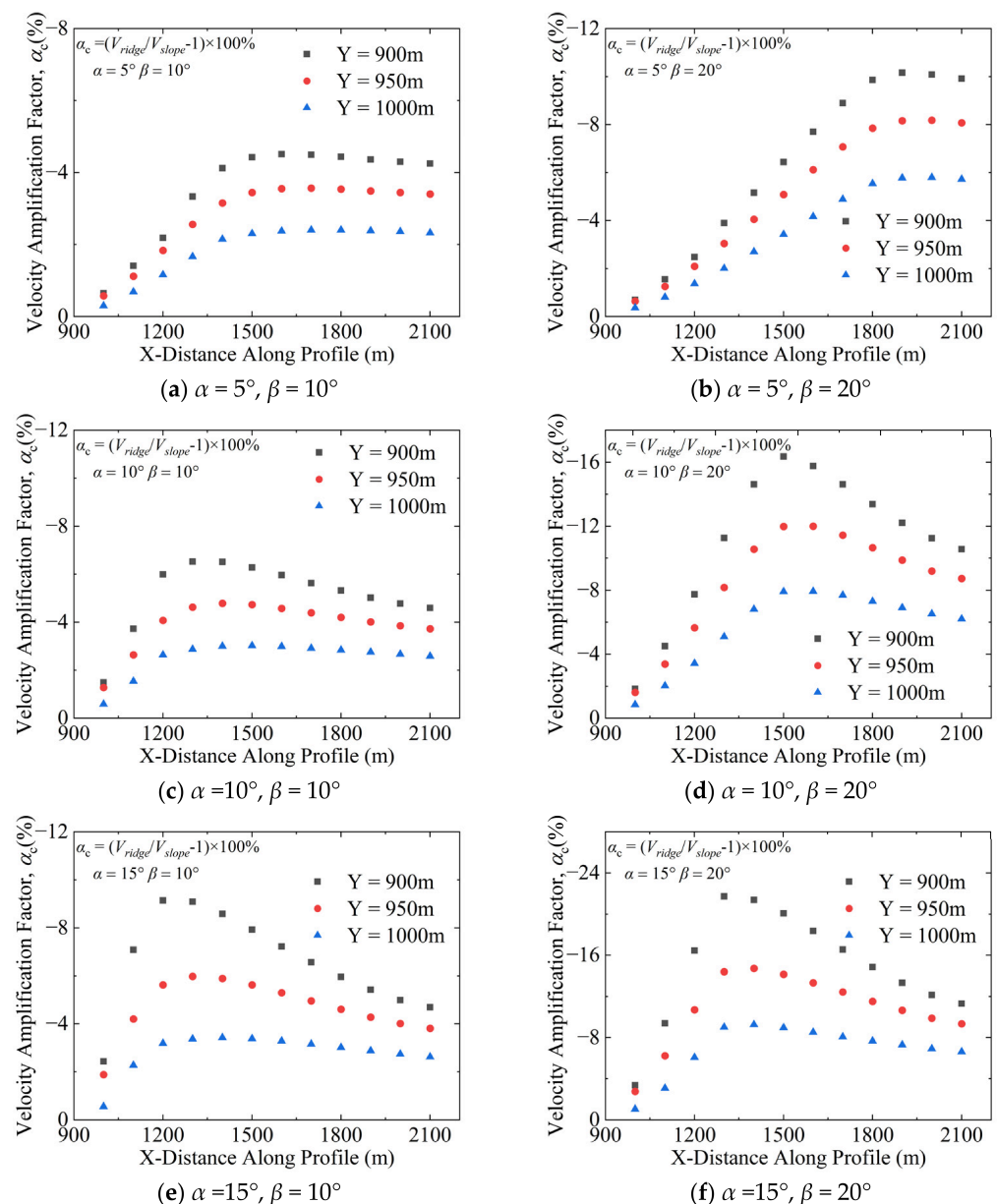


Figure 16. Velocity amplification factor along the x-direction for various base slope angles at ridge angles of 10° and 20° .

The basal slope angle α and canyon side-slope angle (β) influence the flow through related but physically distinct mechanisms. The parameter β primarily controls the local abruptness and relief of the ridge-valley geometry. Increasing β strengthens streamline curvature over the ridge crest, enlarges the effective expansion from the crest into the valley, and increases topographic form resistance and momentum loss. In contrast, α controls the background depth gradient and the large-scale pressure adjustment experienced by

the approaching flow. When both α and β are small, the current can adjust relatively gradually to the seabed relief. When both parameters increase, the stronger background depth variation interacts with the more abrupt ridge-valley geometry, enhancing vertical flow deflection, downstream expansion, and delayed near-bed velocity recovery. The two effects therefore reinforce each other, providing a physical explanation for the nonlinear coupling between α and β observed in Figures 14 and 15. Because the amplification factor is defined relative to the corresponding smooth slope with the same α , these trends represent the incremental hydrodynamic effect of the ridge-valley topography rather than the effect of the basal slope alone. For this reason, Figures 14 and 15 retain the discrete results at individual monitoring locations to preserve the local spatial variations along the ridge-valley profiles. These data are not independently fitted as functions of x , because the purpose of these figures is to illustrate the spatial distribution rather than to establish an additional along-profile predictive relationship. The combined dependence on α and β is instead quantified below using the spatially averaged amplification factor and the regression relationship presented in Figure 17.

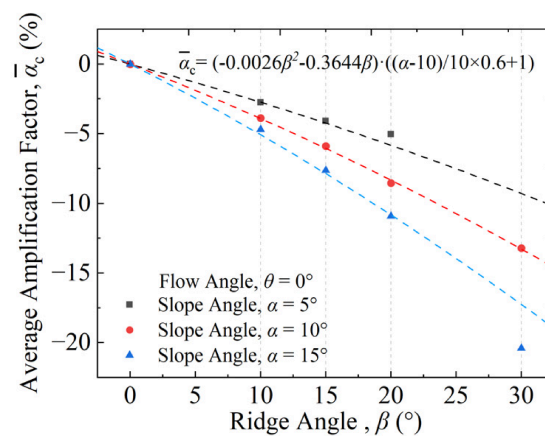


Figure 17. Average amplification factor based on all simulations at $\theta = 0^{\circ}$.

To quantitatively characterize the complex influence of seabed topography on the near-bed flow field, the spatially averaged amplification factor ($\bar{\alpha}_c$) is introduced herein to comprehensively reflect the flow velocity amplification and attenuation behaviors. For an inflow angle of $\theta = 0^{\circ}$, based on the simulation data of eleven different topographic combinations (with varying slope angles α and ridge angles β), a second-order polynomial empirical predictive model for the averaged amplification factor ($\bar{\alpha}_c$), which takes α and β as independent variables, is constructed using the multivariate nonlinear regression method. This response derived from this correlation is illustrated in Figure 17, and the corresponding empirical expression is provided below:

$$\bar{\alpha}_c = (-0.0026\beta^2 - 0.3644\beta) \cdot ((\alpha - 10)/10 \times 0.6 + 1) \quad (37)$$

3.2. Flow Characteristics at the Attack Angle of $\theta = 90^{\circ}$

A detailed analysis of flow behaviors for inflow at an attack angle of $\theta = 90^{\circ}$ is performed, with near-bed velocity contours of the fully developed flow field illustrated in Figure 18. In contrast to the $\theta = 0^{\circ}$ inflow case, the physical mechanism underlying topographic effects on flow undergoes a significant transition. Instead of exhibiting local acceleration or deceleration due to the contraction and expansion of flow cross-sections, the fluid is longitudinally constrained by the topography along the flow direction, thereby manifesting channelized flow characteristics. Driven by the lateral confinement exerted by adjacent ridges on the flow, the fluid is efficiently concentrated at the valley of the ribbed

slope. The dominant characteristic of this flow regime is consistent flow acceleration inside valleys, where near-bed velocity magnitudes are substantially elevated compared with those over planar slopes under identical bathymetric conditions.

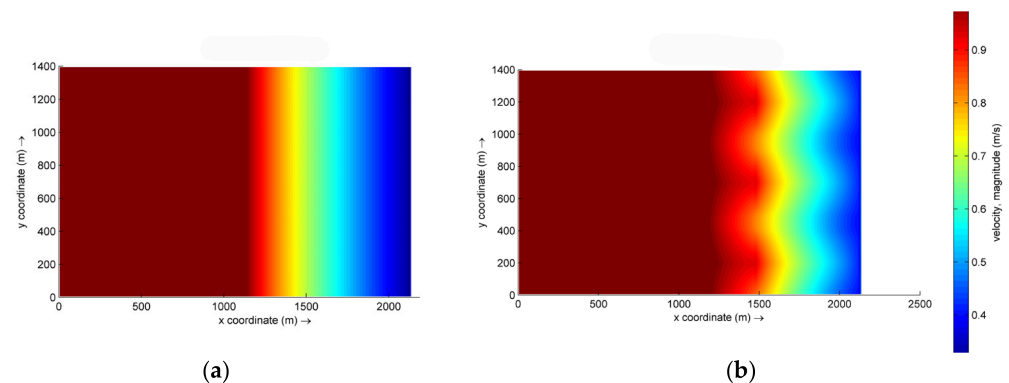


Figure 18. Contour of velocity at $\sigma = 1$ on the slope of $\alpha = 10^\circ$ (a) without and (b) with ribs of $\beta = 20^\circ$ ($\theta = 90^\circ$).

Near-bed velocity amplification factors measured at monitoring points along cross-sections of $y = 900, 950$, and 1000 m in the ridge valley are provided in Figure 19. Compared to the observed flow velocity attenuation under the inflow condition of 0° , the shielding effect of the ridge valley on the flow completely vanishes under the 90° condition. A significant velocity amplification effect of the flow is observed across the full measurement domain, as shown in Figure 18. As the flow enters the valley region, the velocity amplification factor ($\bar{a}_c$) rapidly increases from approximately 0% to 16%. Furthermore, this amplification effect is more distinct in the downstream region of the valley ($x > 1500$ m). In addition, the velocity amplification factors at the monitoring points across different cross-sections exhibit obvious overlap, which indicates that the flow within the core region of the valley system is well-mixed, thereby forming a continuous high-velocity channel throughout the entire ridge valley area.

At $\theta = 90^\circ$, the dominant mechanism changes from repeated cross-ridge contraction and expansion to along-canyon channeling. Because the current is approximately aligned with the canyon axis, it does not repeatedly pass over the ridge crests, and the form losses associated with crest passage and downstream expansion are substantially reduced. Instead, the ridges on both sides of the valley act as lateral boundaries that suppress transverse spreading and redirect the incoming momentum into the along-canyon direction. The greater hydraulic depth of the valley also reduces the relative contribution of bottom resistance per unit of transported volume under the imposed pressure gradient. As a result, the flow retains its along-canyon momentum and develops into a persistent high-velocity corridor. The progressive increase in amplification toward the downstream region shown in Figure 18 is consistent with the cumulative effect of lateral confinement and restricted transverse momentum diffusion. Increasing β strengthens the lateral confinement and further limits exchange between the valley current and the surrounding flow, thereby explaining why a steeper canyon side slope produces stronger velocity amplification for $\theta = 90^\circ$.

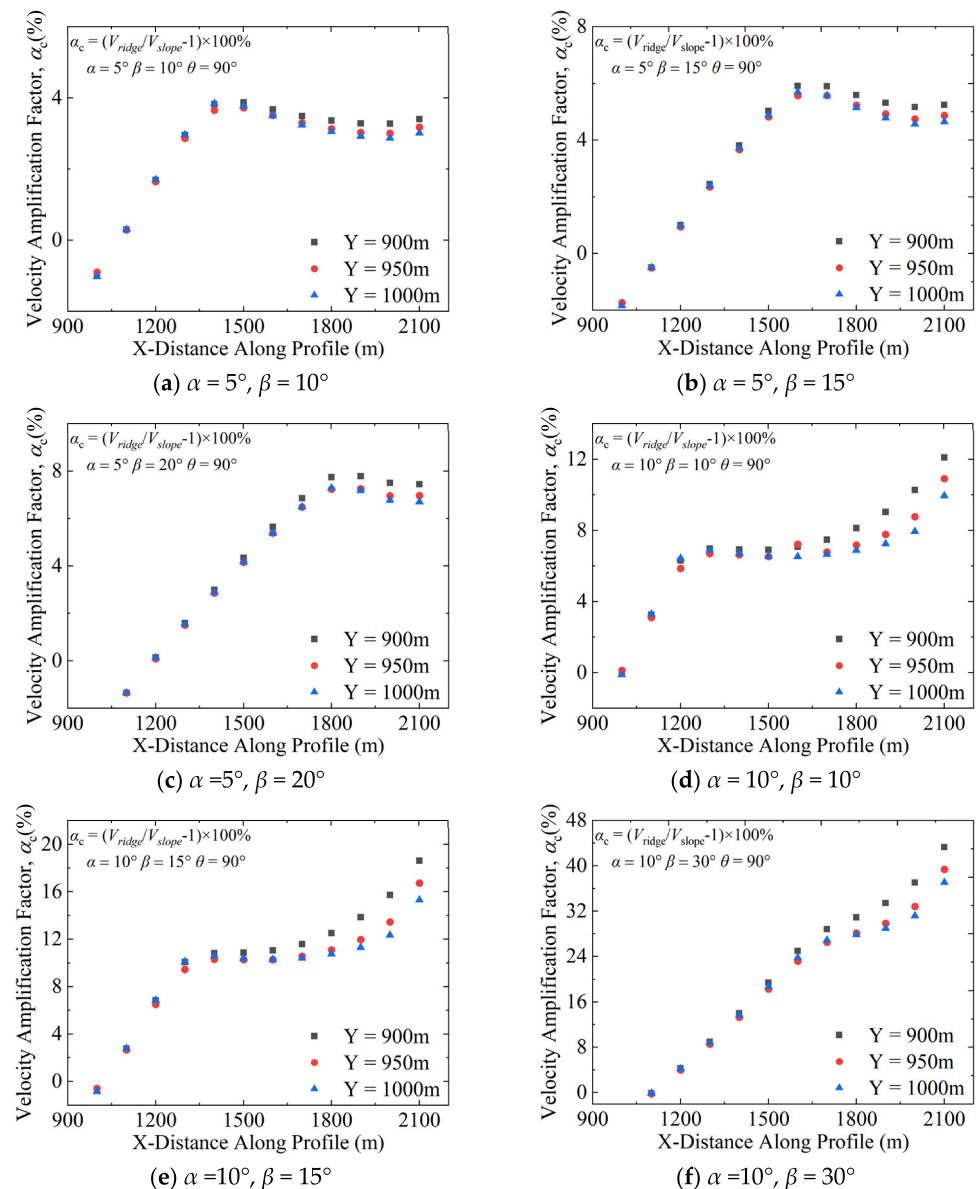


Figure 19. The amplification factors for the bottom flow velocity at the monitoring points on cross-sections located at $y = 900, 950$, and 1000 m within the ridge-valley at $\theta = 90^\circ$.

Consistently with the analysis adopted for $\theta = 0^\circ$, the averaged amplification factor is utilized to quantify the near-bed velocity amplification in the valley for $\theta = 90^\circ$. Regression fitting is conducted based on the results obtained under different topographic conditions, with the fitting relationship presented in Figure 20 and the corresponding empirical formula provided as follows:

$$\bar{\alpha}_c = (-0.0051\beta^2 + 0.6165\beta) \cdot ((\alpha - 10)/10 \times 1.2 + 1) \quad (38)$$

To independently evaluate the predictive performance of the empirical correlations, four additional CFD cases are simulated using parameter combinations that are not included in the calibration datasets for Equations (37) and (38). Two additional topographic combinations, $(\alpha = 7.5^\circ, \beta = 15^\circ)$ and $(\alpha = 12.5^\circ, \beta = 20^\circ)$, are examined under the inflow orientations of $\theta = 0^\circ$ and $\theta = 90^\circ$. The comparison between the CFD-calculated and correlation-predicted spatially averaged amplification factors is presented in Table 1.

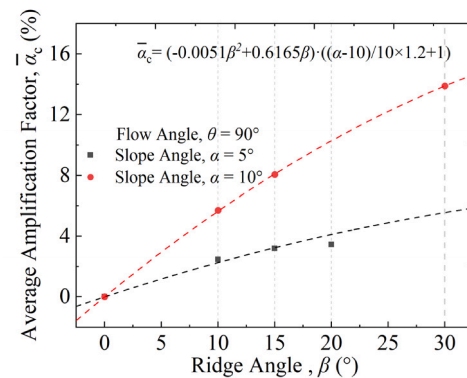


Figure 20. Average amplification factor based on all simulations at $\theta = 90^\circ$.

Table 1. Independent validation of the empirical correlations for $\theta = 0^\circ$ and $\theta = 90^\circ$.

θ ($^\circ$)	α ($^\circ$)	β ($^\circ$)	CFD Result (%)	Prediction Equations (37) and (38) (%)
0	7.5	15	−5.65	−5.14
0	12.5	20	−10.29	−9.58
90	7.5	15	3.56	5.67
90	12.5	20	12.47	13.38

Across the four independent cases, the mean absolute error (MAE) and root mean square error (RMSE) are 1.06 and 1.23 percentage points, respectively. The RMSE normalized by the range of the CFD-calculated amplification factors is 5.40%, and the coefficient of determination is $R^2 = 0.980$. The empirical correlations correctly reproduce the attenuation under cross-canyon inflow ($\theta = 0^\circ$), the amplification under along-canyon inflow ($\theta = 90^\circ$), and the increase in the magnitude of flow modification for the topographic combination with larger α and β . Three of the four validation cases exhibit absolute deviations of no more than 0.91 percentage points. The largest deviation, 2.11 percentage points, occurs for $\alpha = 7.5^\circ$, $\beta = 15^\circ$, and $\theta = 90^\circ$. Overall, the independent validation results indicate that the proposed correlations provide a reasonable reduced-order representation of the sign, general trend, and approximate magnitude of terrain-induced flow modification within the investigated parameter range.

3.3. Estimations of Flow Characteristics at the Attack Angle of $\theta = 45^\circ$

Based on the numerical results for flow conditions at attack angles of $\theta = 0^\circ$ and $\theta = 90^\circ$, the flow behaviors at an intermediate angle of $\theta = 45^\circ$ can be reliably predicted by the superposition principle. The physical basis of this method lies in the fact that 45° inflow contains both cross-ridge and along-ridge velocity components. Their individual contributions to the flow field are quantified by the $\theta = 0^\circ$ and 90° benchmark cases, respectively.

At an inflow angle of 45° , the near-seabed flow field exhibits a typical hybrid characteristic, which is primarily controlled by two competing velocity components: the cross-ridge and along-ridge velocity components. Specifically, the cross-ridge velocity component, analogous to the $\theta = 0^\circ$ inflow scenario, tends to induce an attenuation effect on the near-bed flow velocity in the valley. Conversely, the along-ridge velocity component, matching the $\theta = 90^\circ$ inflow scenario, facilitates the topographic channeling of flow along the valley and generates a progressive amplification effect on the near-seabed flow velocity.

Fully developed near-seabed velocity contours over smooth and ribbed slopes at the 45° inflow are illustrated in Figure 21. In terms of spatial velocity distribution, the flow behaviors under the 45° inflow condition are essentially the result of the competition and dynamic equilibrium between the two aforementioned mechanisms: cross-ridge-induced velocity attenuation and along-ridge-induced velocity amplification. Consequently, the

overall flow effect under the 45° inflow condition can be quantitatively characterized by the vector superposition of the flow responses corresponding to the two pure-direction benchmark cases ($\theta = 0^\circ$ and $\theta = 90^\circ$).

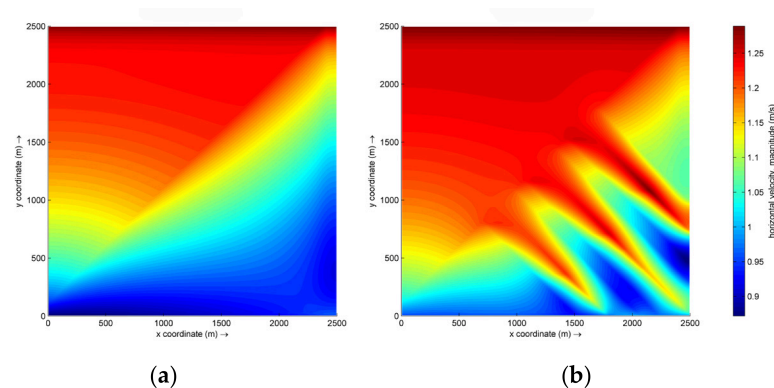


Figure 21. Contours of velocity under the 45° inflow condition at $\sigma = 1$ on the slope of $\alpha = 10^\circ$: (a) without and (b) with ribs of $\beta = 20^\circ$.

The intermediate $\theta = 45^\circ$ response can therefore be understood as competition between two distinct topographic mechanisms. The cross-canyon velocity component loses near-bed momentum through ridge crossing, downstream expansion, and enhanced form resistance, whereas the along-canyon component retains and concentrates momentum through lateral confinement. Consequently, increasing β does not necessarily produce a uniform increase or decrease in the near-bed velocity under oblique inflow. Its net effect depends on the relative magnitudes of the cross-canyon and along-canyon components. When the cross-canyon component dominates, a steeper ridge-valley geometry produces stronger attenuation; when the along-canyon component becomes dominant, the same increase in β promotes channelized acceleration. This competition explains why inflow orientation can reverse the sign of the topographic velocity modification. It also indicates that canyon geometry alone is insufficient for assessing hydrodynamic exposure without considering the current direction.

The near-bed velocity amplification factor for specified topographic parameters (α , β) under the 45° inflow condition is predicted based on the two extreme inflow cases ($\theta = 0^\circ$ and $\theta = 90^\circ$). The detailed prediction procedure for 45° inflow is outlined as follows: the corresponding empirical models are adopted to calculate the predicted values of $\bar{\alpha}_c$ for the same topography under $\theta = 0^\circ$ and $\theta = 90^\circ$ inflow conditions, which represent the pure shielding (deceleration) effect and pure channeling (acceleration) effect, respectively. Subsequently, the superposition principle is applied to obtain the predicted value under the $\theta = 45^\circ$ inflow condition via linear superposition of the two component values. It is considered that this method is essentially a weighted average or vector superposition of the effects from the two orthogonal directions.

To verify the effectiveness of the proposed method, Figure 22 presents the predicted values of $\bar{\alpha}_c$ based on empirical models for the two extreme inflow directions ($\theta = 0^\circ$ and $\theta = 90^\circ$), together with the results from numerical simulations, under the 45° inflow condition for two typical topographies: ($\alpha = 5^\circ$, $\beta = 20^\circ$) and ($\alpha = 10^\circ$, $\beta = 20^\circ$). It can be observed that the averaged amplification factor $\bar{\alpha}_c$ values predicted by the superposition principle for the two specific topographies, are approximately -2.37% and -5.36% , respectively, indicating a slight velocity attenuation in the bottom flow in the ridge-and-valley. In contrast, the corresponding $\bar{\alpha}_c$ values obtained from numerical simulations are -2.4% and -5.96% , respectively. The predicted values are generally consistent with the numerical results, thereby supporting the effectiveness of the proposed superposition method. Furthermore,

it confirms that the flow behaviors relative to complex topography exhibits a superposition property with respect to inflow direction, at least for such idealized periodic terrain.

$$\bar{\alpha}_c(45^\circ) = \text{sgn}([\bar{\alpha}_c(90^\circ)]^2 - [\bar{\alpha}_c(0^\circ)]^2) \sqrt{|\bar{\alpha}_c(90^\circ)]^2 - [\bar{\alpha}_c(0^\circ)]^2|} \quad (39)$$

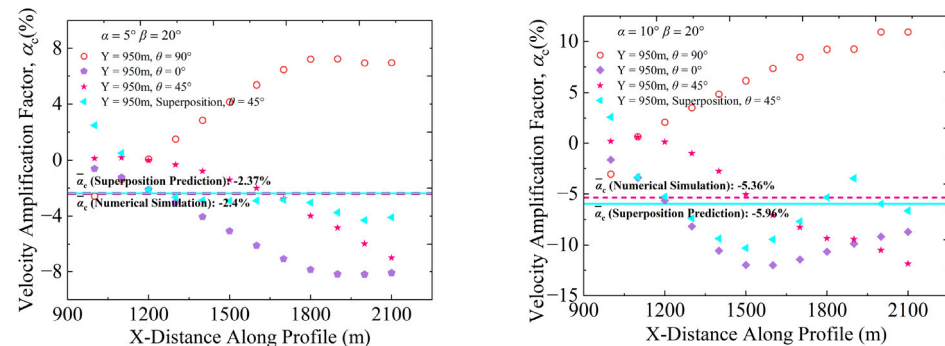


Figure 22. The comparison between predictions and simulations.

As formulated in Equation (39), the parameter $\bar{\alpha}_c(45^\circ)$ is calculated by combining the corresponding values under the 0° and 90° inflow conditions. In addition, the formulation can represent both amplification and attenuation under the 45° inflow condition. It should be emphasized that this formulation does not assume simple linear additivity between the 0° and 90° cases; instead, it combines their contributions through the signed difference of their squared amplification factors. Because Equations (36)–(39) are derived from the numerical results for the idealized periodic ridge-valley terrain, their applicability is restricted to the geometric configurations and parameter ranges examined in this study. These equations should therefore be regarded as reduced-order relationships for describing the trend and relative intensity of flow amplification or attenuation, rather than as universal predictive equations for arbitrary natural canyon bathymetry. Further verification using non-periodic three-dimensional terrain and site-specific hydrodynamic conditions is required before these relationships can be applied to highly irregular natural submarine canyons.

4. On-Bottom Stability of Pipelines Under Canyon Flow Effects

Based on the findings of near-seabed velocity attenuation characteristics in submarine canyons, this section focuses on the on-bottom stability of pipelines deployed in the valleys of slope-confined submarine canyons. Quantitative analyses of the pipeline on-bottom stability under unidirectional flow loading are carried out to reveal the hydrodynamic impacts of canyon-induced near-bed flow fields on pipeline stability.

4.1. Assessment Model of Pipeline Stability

The schematic layout of a pipeline laid within the canyon is shown in Figure 23. Herein, x , y , and z denote the axes of the global Cartesian coordinate system, while x^* , y^* , and z^* represent the axes of the local Cartesian coordinate system fixed to the pipeline. Consistent with the preceding section, α and β denote the angles of the slope and ridge. In addition, the angles between the incoming flow direction, the pipeline axis and the global y -axis within the horizontal plane are denoted by θ and γ , respectively.

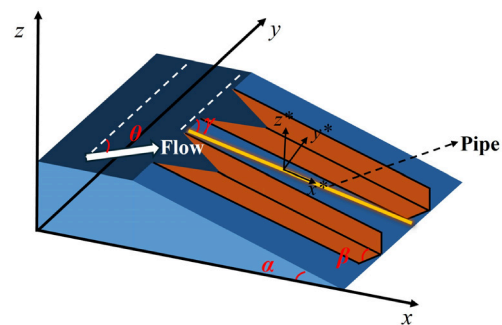


Figure 23. The schematic layout of a pipeline laid within the canyon.

Owing to the decomposition and calculation of hydrodynamic loads being carried out in the local coordinate system, the coordinate transformation relations between the global coordinate system and the pipeline local coordinate system are presented herein, as shown in Figure 24. Figure 24a illustrates the current loads acting on the pipeline within the global coordinate system. The vertical axis (y) points to geographic north, and the horizontal axis (x) points to geographic east. The orientation of each component is quantified by the azimuth angles defined relative to the north axis. Pipe heading (γ) refers to the angle between the pipeline axis (the red line in Figure 24a) and the north axis (y), ranging from 0° to 180° , while current heading (θ) represents the angle between the current flow direction (indicated by the arrow labeled “current” in Figure 24a) and the north axis (y), varying from 0° to 360° . Figure 24b defines local coordinate system with the local seabed attached to the pipeline: The x^* -axis represents the main extension direction of the pipeline, the y^* -axis lies in the transverse plane perpendicular to the pipeline axis and is orthogonal to the x^* -axis, and the z^* -axis is defined following the right-hand rule. Two geometric angles are defined to quantify the seabed slopes in the planes along and perpendicular to the pipeline axis respectively, with detailed rules given below:

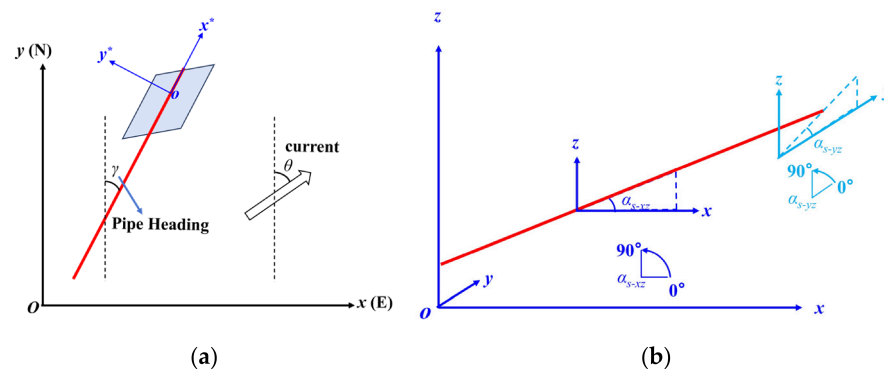


Figure 24. Definition rule of coordinate systems: (a) global, (b) local (* denotes the local coordinate system).

(a) Seabed slope along the pipeline α_{s-xz} : the angle between the pipeline axis and the local x -axis in the xOz vertical plane. The angle α_{s-xz} is measured counterclockwise from the positive local x -axis to the pipeline axis. This angle is relevant to the axial stability of the pipeline.

(b) Seabed slope perpendicular to the pipeline α_{s-yz} : the angle of the seabed surface against the yOz vertical plane. The angle α_{s-yz} is measured counterclockwise from the positive local y -axis to the seabed surface. This angle is relevant to the lateral stability of the pipeline.

For the on-bottom stability analysis of pipeline, seabed slope defined in the global coordinate system should be transformed into the local coordinate system aligned with the pipeline axis. The corresponding transformation relationships are given as follows:

$$\alpha_{xz} = \arctan(\tan \alpha \cdot |\sin \gamma|) \quad (40)$$

$$\alpha_{yz} = \arctan(\tan \alpha \cdot |\cos \gamma|) \quad (41)$$

The lateral stability in the yOz plane and the axial stability in the xOz plane are illustrated in Figure 25. The calculations of all the force components are identical to those suggested in DNV (2021) [29].

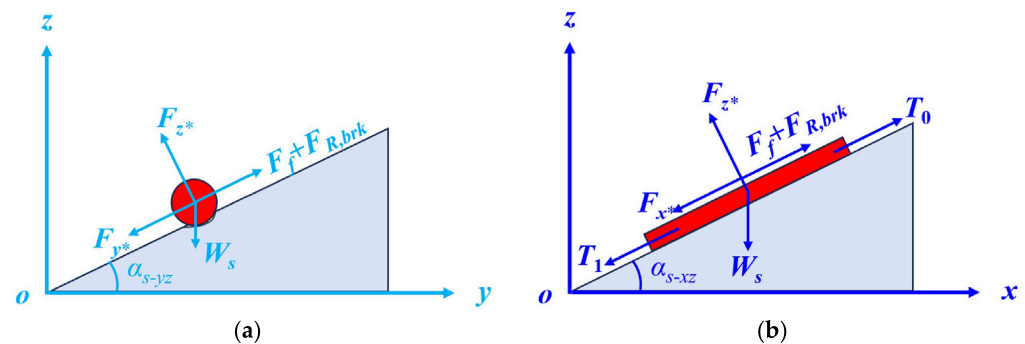


Figure 25. Definition of (a) lateral and (b) axial static stability (* denotes the local coordinate system).

When the pipeline is likely to move in the yOz plane, the lateral utilization factor is calculated as follows:

$$Utilization_{y*} = \gamma_{SC} \frac{F_{y*} + \mu_{n0} F_{z*} + W_s |\sin \alpha_{s-yz}|}{\mu_{nd} W_s \cos \alpha_{s-yz} + F_{r,brk-up}} \leq 1 \quad (42)$$

The utilization factor for vertical stability is written as follows:

$$Utilization_{z*} = \gamma_{SC} \frac{F_{z*}}{W_s \cos \alpha_{s-yz}} \leq 1 \quad (43)$$

When the pipeline is likely to move downwards in the xOz plane, the axial utilization factor is calculated as follows, assuming uniform tension distribution ($T_0 = T_1$) and negligible axial hydrodynamic load ($F_x = 0$):

$$Utilization_{x*} = \gamma_{SC} \frac{\mu_{a0} F_z + W_s \sin \alpha_{s-xz}}{\mu_{ad} W_s \cos \alpha_{s-xz}} \leq 1 \quad (44)$$

In the above equations, μ_{n0} and μ_{nd} are the perpendicular friction factors, while μ_{a0} and μ_{ad} are the axial friction factors without and with the effect of seabed slope, respectively. The values of μ_{nd} and μ_{ad} are taken from Gao et al. [14] and Shi et al. [30], respectively. The pipeline remains stable in the corresponding direction when the $Utilization_{i*}$ ($i = x, y, z$) is less than unity. Conversely, a value of $Utilization_{i*}$ greater than unity indicates pipeline instability along that direction.

4.2. Pipe–Soil Interaction Model

The stability of subsea pipelines is essentially a dynamic response induced by the imbalance between hydrodynamic loads and seabed soil constraint resistance. Taking an appropriate pipe–soil interaction model is critical for evaluating the stability of pipelines. In this study, the Verley–Lund model applicable to a flat cohesive clay seabed and the

Slope–Silt model proposed by Zhao et al. [20] for a sloping silt seabed are adopted for the pipe–soil interaction analysis. For the Verley–Lund model, the total soil resistance F_h acting on the pipeline consists of Coulomb frictional force F_f and passive soil resistance F_r induced by pipeline settlement.

$$F_h = F_f + F_r \quad (45)$$

The Coulomb frictional force can be calculated by the following Formula (46):

$$F_f = \mu F_c \quad (46)$$

where the friction coefficient μ of clay soil is set to 0.2, and F_c denotes as the vertical contact force exerted by the pipeline on the seabed. The maximum passive soil breakout resistance F_r is calculated by Formula (47).

$$\frac{F_r}{Ds_u} = 4.13G^{-0.392} \left(\frac{z}{D} \right)^{1.31} \quad (47)$$

where z is the penetration depth of the pipeline into the seabed, s_u denotes the undrained shear strength of seabed clay, and the coefficient $G = s_u / D\phi$, in which ϕ is the submerged unit weight of soil. The initial dimensionless penetration depth of the pipeline $(z/D)_{init}$ can be calculated by Formula (48):

$$\left(\frac{z}{D} \right)_{init} = 0.0071(SG^{0.3})^{3.2} + 0.062(SG^{0.3})^{0.70} \quad (48)$$

where the coefficient $S = F_c / Ds_u$.

To describe the lateral pipe–soil interaction on a sloping silt seabed, Zhao et al. [20] developed an experimentally based predictive model using mechanical loading tests on partially drained silt. The tested soil was explicitly classified as silt rather than clay. Its response was characterized as intermediate between the fully drained behavior generally associated with sand and the approximately undrained behavior commonly assumed for low-permeability clay. Partial drainage in silt and other intermediate soils has also been reported in previous geotechnical studies [31]. Accordingly, this model is referred to as the Slope–Silt model throughout the present study. For shallowly embedded pipelines, the ultimate lateral soil resistance is primarily governed by interface friction and soil cohesion, whereas suction associated with negative pore-water pressure behind the pipe becomes increasingly important at greater embedment. The corresponding predictive relationship is expressed as follows:

$$\frac{F_{Ru}}{\phi D^2} = \begin{cases} \frac{\psi_1 G \cos(\varphi + \alpha_{s-yz}) + \psi_2 \frac{c}{\phi D} \cos \varphi}{G \cos(\varphi + \alpha_{s-yz}) - \psi_2 \frac{c}{\phi D} \sin \varphi}, \frac{|e_0|}{D} \leq 0.05 \\ 6.16 \left(\frac{e_0}{D} \right)^2 + 0.015 \frac{|e_0|}{D} + 0.16 \frac{c}{\phi D}, \frac{|e_0|}{D} > 0.05 \end{cases} \quad (49)$$

where F_{Ru} is the ultimate lateral soil resistance of the pipeline per unit length, ϕ denotes the submerged unit weight of soil, ψ_1 is the apparent friction coefficient at the interface between the pipeline and silt, taken as 0.53. ψ_2 is the comprehensive coefficient accounting for cohesion resistance and lateral soil berm effects, taken as 0.16. e_0 is the initial embedment depth of the pipeline, and c is soil cohesion. $\varphi = \arctan(C_L/C_D)$ is the angle between the resultant force and the seabed surface. C_D and C_L are the drag and lift coefficients of pipe.

In this study, the water depth is set to 400 m, and only the hydrodynamic loads exerted by currents acting on a pipeline laid on canyon slopes are considered in calculations. The initial inflow velocity prior to slope-induced flow modification is set to 1 m/s, with a flow

duration of three hours. Moreover, the outer diameter of the pipeline is 0.59 m, the effective submerged weight of the pipeline per unit length is 1950 N/m, and the seawater density is 1025 kg/m³. For the Verley–Lund clay model, the undrained shear strength s_u is set to 3 kPa. For the Slope–Silt model, the silt cohesion c and submerged unit weight γ' are set to 3 kPa and 8 kN/m³, respectively. Although the same numerical strength value is adopted in the two baseline calculations, s_u and c represent different soil parameters and are not treated as physically equivalent. Based on the above-mentioned computational parameters, the incident flow angle (θ), pipeline laying angle on the slope (γ), seabed slope angle (α), and canyon ridge angle (β) are selected as the primary independent variables in this study. The corresponding calculation cases are summarized in Table 2.

Table 2. The calculation cases.

Parameters	Values
flow angle (θ)	0°, 45°, 90°
pipeline laying angle on the slope (γ)	0°, 5°, 10°, 15°, 20°
seabed slope angle (α)	0°, 10°, 20°, 30°
canyon ridge angle (β)	45°, 90°
pipe–soil interaction model	Verley–Lund model [19], Slope–Silt model (Zhao’s model) [20]
undrained shear strength (s_u) [kPa]	1, 2, 3, 6, 10, 14, 16, 18

4.3. Sensitivity of Pipeline Lateral Stability to Different Parameters

To clarify the influence of various angular parameters on the on-bottom stability of the pipeline, this section investigates the sensitivity differences among the incident flow angle, pipeline laying angle, seabed slope inclination, and canyon ridge angle. In addition, the Slope–Silt model is adopted for all cases, with a uniform undrained shear strength of 3 kPa assigned to the weakly consolidated, saturated silty seabed at a water depth of 400 m to represent the low-strength characteristics of the surficial soil. It should be noted that the stability of the pipeline in the x^* and z^* directions is only related to the pipeline laying direction and terrain slope. Variations in hydrodynamic loads caused by changes in canyon ridge and incident flow angle exert negligible effects on the above stability; thus, only the lateral (y^* -direction) stability of the pipeline is analyzed hereinafter.

Figure 26 presents the results of pipeline lateral stability under a pipeline laying angle of 90° with various incident flow angles and slope and canyon ridge angles. It can be observed that the $Utilization_{y^*}$ of all cases is less than 1 under the environmental loads and working conditions adopted in this study, which indicates that the pipeline maintains lateral stability. Furthermore, at this pipeline laying angle, the lateral stability of the pipeline is nearly independent of the seabed slope and only related to the canyon ridge.

Distinct differences in pipeline stability can be identified under varying inflow directions. As illustrated in Figure 26a, for inflow travelling perpendicular to the pipeline, increasing the canyon ridge angle improves the pipeline’s lateral on-bottom stability at the fixed basal seabed. This is mainly attributed to the obvious reduction of lateral flow velocity at the canyon valley, which decreases the lateral hydrodynamic loads acting on the pipeline. When the included angle between the incident flow direction and the pipeline laying direction is 45° (Figure 26b), increasing the canyon ridge angle can enhance the pipeline lateral stability under small seabed slopes ($\alpha < 6^\circ$). Nevertheless, as the seabed slope rises, enlarging the canyon ridge angle will degrade the pipeline’s lateral stability. The primary reason is that the lateral flow velocity at the canyon valley attenuates under gentle seabed slopes, lowering the lateral hydrodynamic loads imposed on the pipeline. In contrast, the flow velocity shifts from attenuation to acceleration with the increase in

seabed slope, leading to a gradual rise in lateral hydrodynamic loads on the pipeline. When the incident flow is parallel to the pipeline (Figure 26c), the hydrodynamic loads contain no lateral component of the pipeline. Neither the seabed slope angle nor the canyon ridge angle exerts an influence on the pipeline lateral stability, and the pipeline remains stable throughout all the tests.

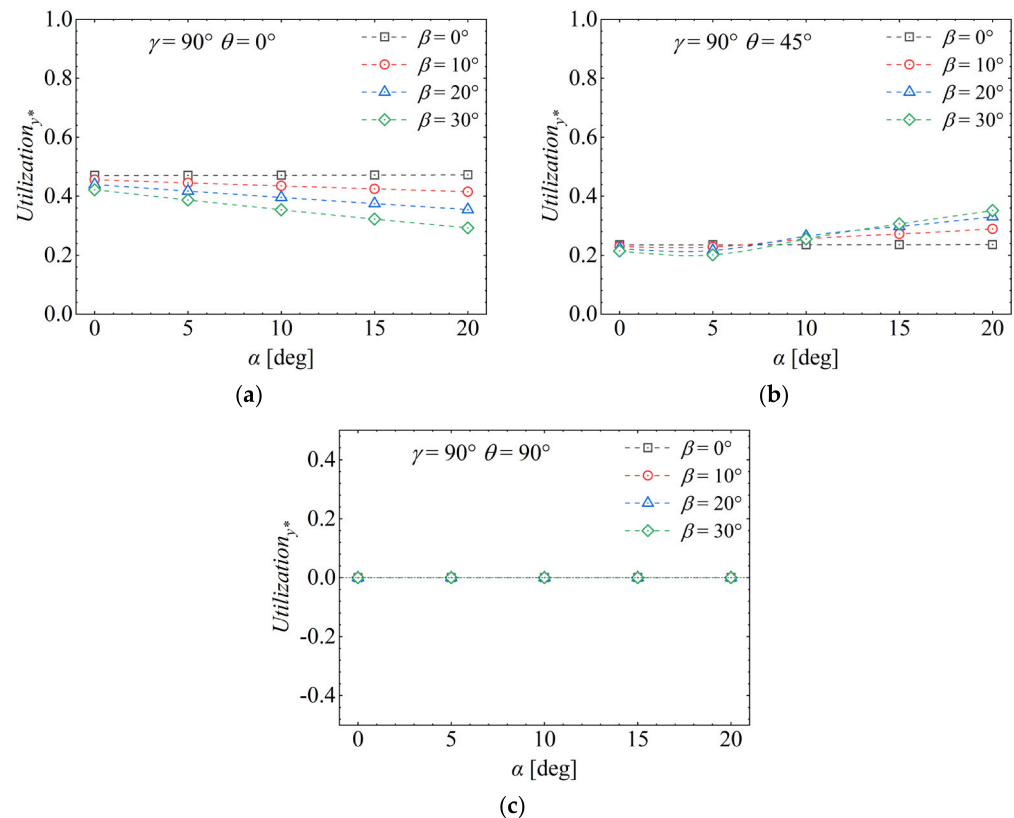


Figure 26. Lateral stability of pipelines under 90° laying angle with varying incident flow and slope and canyon ridge angles: (a) $\theta = 0^\circ$, (b) $\theta = 45^\circ$, (c) $\theta = 90^\circ$.

Considering that pipeline laid along a canyon valley is commonly arranged at inclined angles in practical engineering, a pipeline laying angle of 45° is adopted to evaluate the lateral stability of the pipeline under combinations of different incident flow and slope and canyon ridge angles. The corresponding results are shown in Figure 27. Compared with pipeline laid at a laying angle of 90°, the lateral stability of pipelines with a 45° laying angle is jointly affected by the seabed slope and canyon ridge. Under the three incident flow angles considered in this study (0°, 45°, and 90°), the lateral stability of pipelines decreases remarkably with the increase in slope angle. Under the specified environmental loads, when the slope angle reaches 4°, the $Utilization_{y^*}$ exceeds 1 under incident flow angles of 0° and 90°, which triggers lateral instability of the pipeline. By contrast, the critical slope inclination for lateral instability is approximately 8° when the incident flow angle is 45°.

Furthermore, it can be observed from Figure 27a,c that the sensitivity of pipeline lateral stability to canyon ridge angle intensifies as the slope angle rises. Notably, the canyon ridge angle exerts opposite effects under two extreme flow directions. For an incident flow angle of 0°, increasing the canyon ridge angle reduces the $Utilization_{y^*}$ and improves the lateral stability of the pipeline. However, when the incident flow angle equals 90°, a larger canyon ridge angle leads to a higher $Utilization_{y^*}$ and weaker pipeline lateral stability. This discrepancy mainly arises from the fundamental transformation of the regulation effect of ridge topography on the lateral flow velocity distribution under different incoming flow directions. Flow acceleration or attenuation induced by ridge topography directly

affects the hydrodynamic loads acting on pipelines and their stability evaluation results. Neglecting this effect may result in insufficient lateral sliding stability of pipelines and elevated failure risks; alternatively, over-conservative estimation of hydrodynamic forces can introduce excessively large safety margins and reduce the economic efficiency of the design.

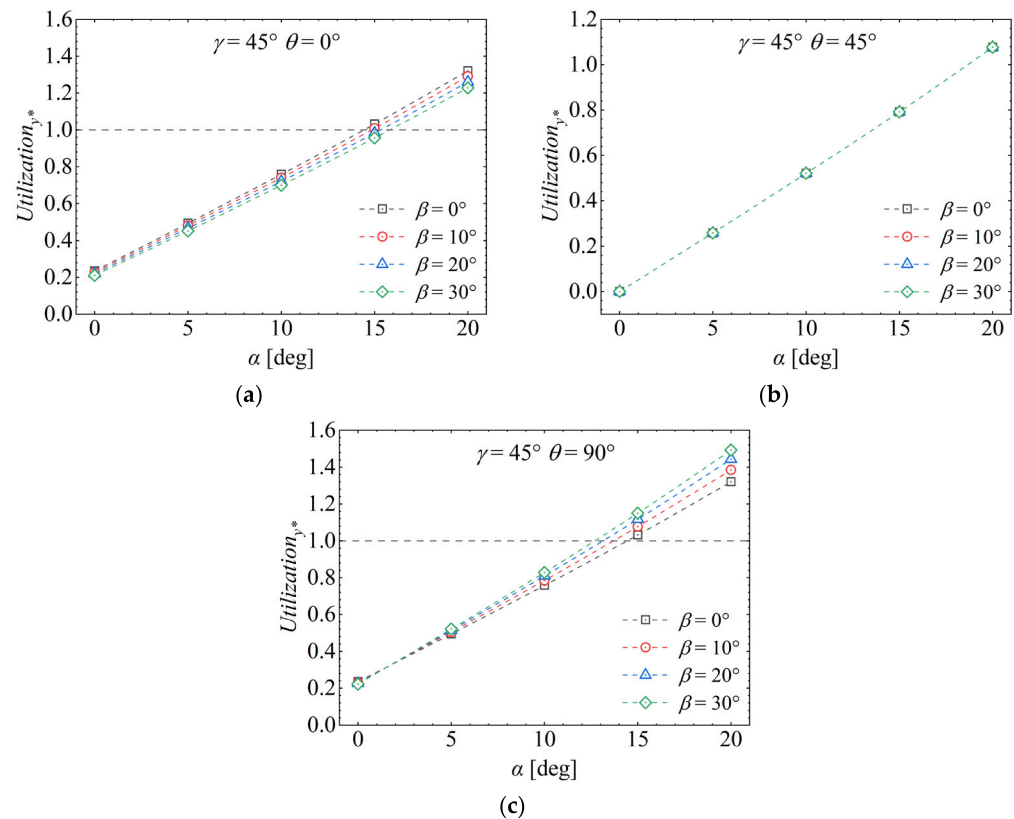


Figure 27. Lateral stability of pipelines under 45° laying angle with varying incident flow and slope and canyon ridge angles: (a) $\theta = 0^\circ$, (b) $\theta = 45^\circ$, (c) $\theta = 90^\circ$.

These hydrodynamic mechanisms have direct implications for pipeline routing and preliminary stability assessment. Because the drag and lift forces acting on a pipeline are approximately proportional to the square of the local flow velocity, a moderate velocity modification may produce a substantially larger change in hydrodynamic loading. For example, the approximately 16% near-bed velocity amplification observed for the along-canyon flow condition corresponds to an increase of approximately 35% in velocity-squared loading, if the hydrodynamic coefficients remain unchanged. Cross-canyon flow may reduce the average velocity within a valley, but the accelerated regions over ridge crests can still constitute localized loading hotspots. Conversely, an along-canyon current can generate a continuous high-velocity corridor, and the use of the upstream or domain-averaged velocity alone may therefore underestimate the pipeline load. The proposed amplification factors can be used as a preliminary screening tool to identify potentially sheltered or amplified sections and to compare alternative pipeline alignments. However, final engineering design should incorporate site-specific bathymetry, current profiles, soil variability, and pipeline properties, because the present relationships are derived from idealized periodic ridge-valley terrain within a limited parameter range.

4.4. Sensitivity of Pipeline Lateral Stability to Pipe–Soil Interaction Models and Soil-Strength Parameters

To reveal the effects of pipe–soil interaction model selection and variations in seabed soil strength on pipeline lateral stability assessments, sensitivity analyses are conducted with respect to the pipe–soil interaction model and undrained shear strength. Two pipe–soil interaction models are considered: the Verley–Lund model for a flat seabed and the Slope–Silt model developed for a sloping seabed. To represent the spatial variability in the strength of submarine canyon sediments, eight representative values of undrained shear strength are selected, covering a broad range from relatively low to high seabed soil strength. Figures 28–30 compare the lateral stability results predicted by the two models under different canyon-slope angles, canyon-ridge angles, undrained shear strengths, incident-flow angles, and pipeline-laying angles.

The results indicate that, within the investigated parameter ranges, the selection of the pipe–soil interaction model and variations in undrained shear strength do not alter the overall trend of the pipeline lateral stability utilization ($Utilization_{y*}$) with slope angle, but affect its magnitude. Under the present calculation conditions, the two models exhibit different sensitivities to s_u . The $Utilization_{y*}$ predicted by the Verley–Lund model gradually increases with increasing s_u and tends to stabilize when $s_u \approx 6$ kPa. In contrast, the Slope–Silt model exhibits a non-monotonic response, with its predicted utilization reaching a peak at approximately $s_u = 2$ kPa, subsequently decreasing, and then tending to level off when $s_u > 6$ kPa. These results demonstrate that the discrepancies between the two models are not constant but depend strongly on the undrained shear strength of the soil.

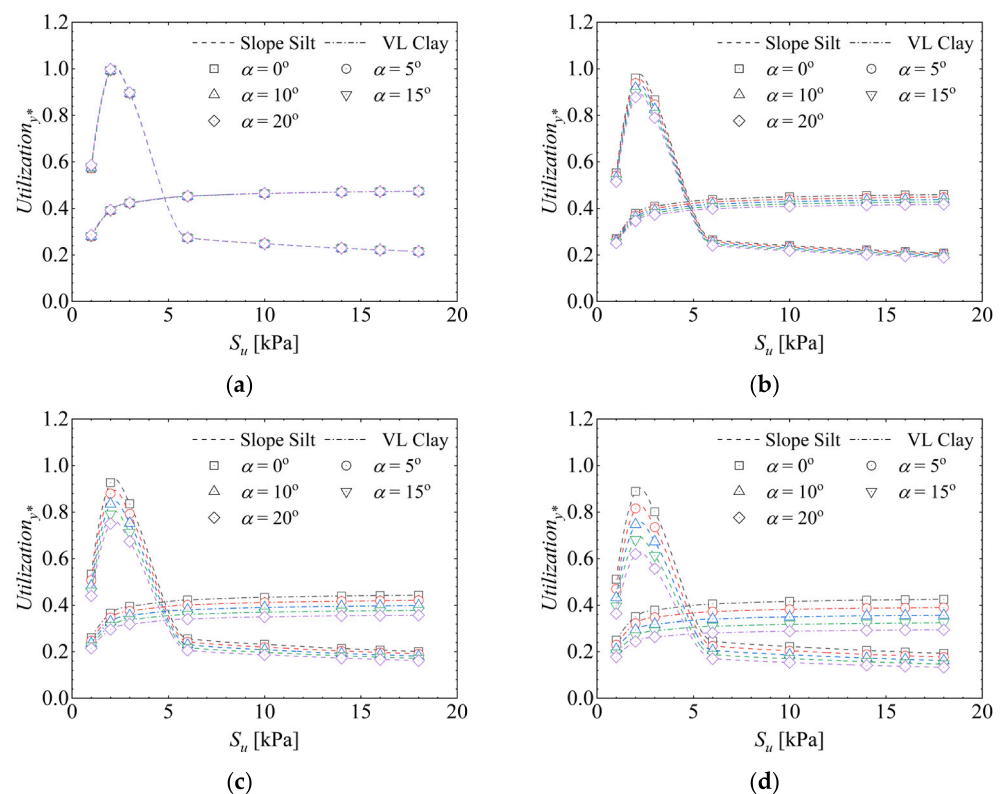


Figure 28. Comparison of pipeline lateral stability at a laying angle of 90° and an incident flow angle of 0° under different combinations of soil undrained shear strength, pipe–soil interaction model, seabed slope angle, and canyon ridge angle: (a) $\beta = 0^\circ$, (b) $\beta = 10^\circ$, (c) $\beta = 20^\circ$, (d) $\beta = 30^\circ$.

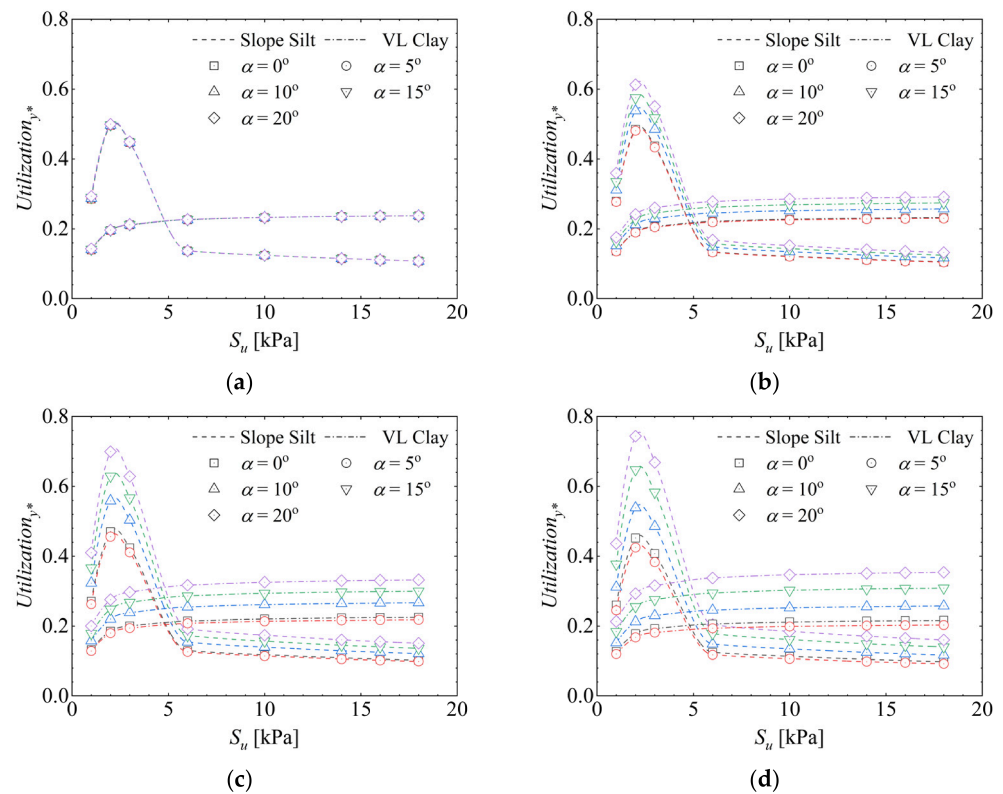


Figure 29. Comparison of pipeline lateral stability at a laying angle of 90° and an incident flow angle of 45° under different combinations of soil undrained shear strength, pipe–soil interaction model, seabed slope angle, and canyon ridge angle: (a) $\beta = 0^\circ$, (b) $\beta = 10^\circ$, (c) $\beta = 20^\circ$, (d) $\beta = 30^\circ$.

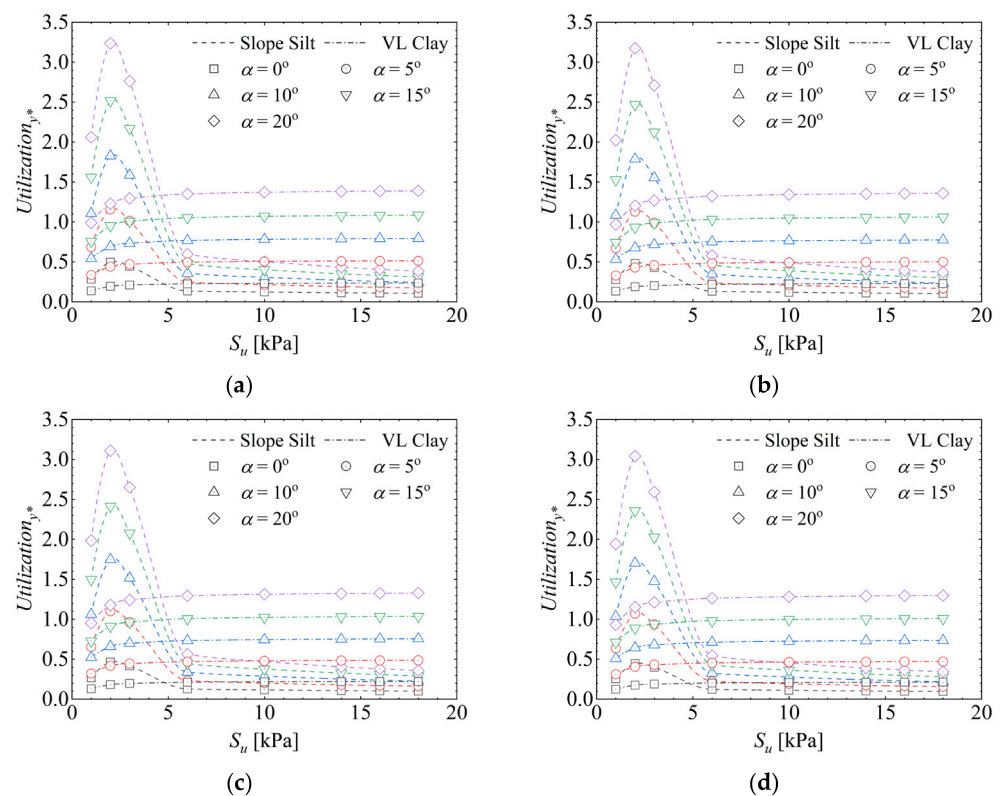


Figure 30. Comparison of pipeline lateral stability at a laying angle of 45° and an incident flow angle of 0° under different combinations of soil undrained shear strength, pipe–soil interaction model, seabed slope angle, and canyon ridge angle: (a) $\beta = 0^\circ$, (b) $\beta = 10^\circ$, (c) $\beta = 20^\circ$, (d) $\beta = 30^\circ$.

The above results mainly arise from the different ways in which the two models relate soil strength, pipeline embedment depth, and lateral resistance. In the Verley–Lund model, increasing s_u enhances soil shear strength but reduces the predicted pipeline penetration depth, thereby weakening embedment-related resistance. Under the present conditions, the effect of reduced embedment is relatively pronounced at low soil strengths, causing $Utilization_y^*$ to increase with s_u ; as the two competing effects gradually approach equilibrium, the utilization tends to stabilize. By contrast, the Slope–Silt model employs a piecewise resistance mechanism. In the deep-embedment regime, embedment- and suction-related resistance decreases with increasing s_u , whereas cohesive resistance progressively increases. Their competition causes the utilization to peak near $s_u = 2$ kPa and then decline. When the embedment ratio falls below the critical value $|e_0|/D = 0.05$, the model switches from the deep-embedment branch to the shallow-embedment branch, in which interface friction and cohesion dominate. Because the two branches use different resistance formulations, this transition produces a pronounced decrease in utilization over $s_u = 3$ –6 kPa. Thereafter, further soil strengthening increases the frictional and cohesive resistances under shallow-embedment conditions, causing the utilization to decline gradually. Therefore, the relative ordering of the results predicted by the two models changes with s_u , reflecting the combined effects of the coupling between soil strength and pipeline embedment and differences in the resistance mechanisms adopted by the models.

5. Conclusions

Numerical analyses of near-bed flow fields over a ridge-valley canyon are conducted for two inflow orientations ($\theta = 0^\circ$ and 90°). The topographic modulation of near-bed flow attenuation and acceleration by ribbed slopes is investigated, together with the sensitivity of flow behaviors to the geometric parameters, namely the slope angle and ridge angle (α and β). Based on the near-bed flow velocity distributions modified by ribbed slopes, quantitative assessments are further carried out for the lateral on-bottom stability of submarine pipelines laid in a slope-confined canyon. Within the scope of this work, the main conclusions are summarized as follows:

Ribbed slopes exert two distinct modulation mechanisms on near-bed flow fields, which are governed by the relative angle between incoming flow and ridge-valley structures. For the $\theta = 0^\circ$ cases, the contraction and expansion of local topographic cross-sections accelerate the flow over ridge crests and attenuate the near-bed flow velocity in the valley. Moreover, for the $\theta = 90^\circ$ cases, the lateral confinement of bilateral ridges forms a topographic channel, resulting in persistent flow acceleration across the entire valley region.

The topographic modulation intensity of near-bed flow is highly sensitive to the slope angle (α) and ridge angle (β) with obvious nonlinear coupling; larger values of the two parameters strengthen flow attenuation or acceleration. Second-order polynomial empirical formulas with α and β as independent variables are established for $\theta = 0^\circ$ and 90° to rapidly quantify terrain-induced flow modification. The near-bed flow behaviors under 45° oblique inflow conditions can be reconstructed by superposing orthogonal flow components aligned perpendicular and parallel to the ribs. The vector superposition prediction method derived from the two sets of empirical models yields an error of approximately 5% and accurately reproduces near-bed flow features under oblique currents, which verifies the directional superposition property of flow over periodic ridge-valley slopes.

A ridge-valley canyon affects the lateral on-bottom stability of subsea pipelines by modifying near-bed flow velocity, and the overall influence is governed by the relative angle between incoming flow and pipeline alignment. When the inflow travels perpendicular to ridge structures, increasing the ridge angle weakens transverse flow and improves pipeline lateral stability. In contrast, parallel inflow triggers a topographic channelization

effect that accelerates near-bed flow; a larger ridge angle reduces the lateral safety margin against sliding. Rising sloping seabed angle continuously degrades pipeline lateral stability, and lateral instability occurs once the slope exceeds the critical threshold. Pipe–soil interaction models considerably determine the accuracy of lateral stability assessment. Under the calculation conditions considered in this study, the choice of pipe–soil interaction model does not alter the overall trend of the pipeline lateral stability utilization with slope angle, but significantly affects its magnitude and sensitivity to soil strength. Within the range of $s_u = 1\text{--}3$ kPa, the Slope–Silt ridge-valley model predicts higher utilization values; when $s_u \geq 6$ kPa, the Verley–Lund model yields higher values. Therefore, the relative conservatism of the two models is not constant, and model selection should account for the site-specific soil strength and pipeline embedment condition.

A limitation of the present study is that the canyon topography is represented by an idealized periodic ridge-valley geometry. Although this configuration permits the effects of α and β to be quantified independently, it does not reproduce the asymmetry, non-periodicity, along-axis variability, or multiscale roughness of natural submarine canyons. Future work will construct non-periodic three-dimensional canyon models using high-resolution multibeam bathymetry and multiple digitized cross-sections. Combined with representative site-specific current conditions, these models will be used to examine whether the amplification and attenuation trends described by Equations (36)–(38), as well as the directional superposition relationship in Equation (39), remain valid for realistic canyon terrain. The deviations from the idealized correlations will also be quantified to support their subsequent recalibration or extension for site-specific engineering applications.

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Abbreviation

The following abbreviation is used in this manuscript:

DNV Det Norske Veritas

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