

Article

The Numerical Study of the Hydrodynamic Characteristics of Heave Plates with Salient Edges and Spatially Mismatched Arrangements

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Abstract

This study investigates the hydrodynamic characteristics of single-layer heave plates with varying salient edges and triple-layer configurations with size mismatches under forced oscillation, utilizing 3D overset mesh numerical simulations. For single-layer plates, the 0° edge configuration maintains high hydrodynamic coefficients across all conditions, whereas the 135° edge peaks under specific parameters. Introducing horizontal gaps consistently degrades performance. For triple-layer plates, increasing the spacing ratio mitigates spatial flow interference, significantly enhancing hydrodynamic coefficients. Furthermore, introducing size differences creates a stepped mismatched configuration that effectively mitigates wake shielding and enhances fluid entrainment. Consequently, the coefficients increase steadily with the absolute size difference, reaching optimal heave suppression in the triple-layer arrangement with a large spacing and a ± 20 m size mismatch. Finally, a highly accurate empirical formula ($R^2 > 0.92$) is proposed to predict the damping (C_d) and added mass (C_a) coefficients, effectively capturing the nonlinear coupling effects of spacing ratio and size difference. These findings provide practical theoretical guidance for optimizing vibration reduction systems in offshore platforms.

Keywords: heave plate; damping coefficient; added mass coefficient

1. Introduction

As a critical floating structure supporting offshore oil and gas exploitation, the Spar platform is susceptible to severe vertical displacements under long-period waves and harsh sea conditions, subsequently threatening the safety of risers and mooring systems. To ensure the stable operation of Spar platforms, it is a common engineering practice to suppress resonance by increasing system damping or adjusting the natural heave period to avoid the dominant wave energy frequency band [1]. Given the inherent low damping in Spar platforms, which easily induces large-amplitude heave motions near the natural frequency, scholars have proposed adding heave plates at the bottom of the cylindrical platform to substantially enhance hydrodynamic damping [2]. To date, this technology has been widely applied to Tension Leg Platforms (TLPs) and semi-submersible floating wind turbines for the optimization of their heave response characteristics [3–7]. Jiang et al. [8] proposed an integral heave plate design for semi-submersible wind turbines to replace the traditional three-independent-plate structure. Their study confirmed that this integral



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design exhibits superior hydrodynamic performance in heave and pitch motions, rendering it an excellent alternative.

Investigations into the hydrodynamic characteristics of heave plates predominantly rely on physical model tests and numerical simulations, among which forced oscillation tests serve as a common approach [9]. Thiagarajan et al. [10] examined TLP columns with attached heave plates through forced oscillation tests in still water. The results confirmed that, compared to a bare column without plates, the addition of a heave plate significantly increases the pressure drag and heave added mass of the structure.

Furthermore, the hydrodynamic characteristics of perforated heave plates have garnered considerable attention and have been extensively studied [11–14]. Introducing perforations on heave plates serves as another prevalent optimization strategy: by modulating the perforation ratio, the hydrodynamic characteristics can be precisely controlled. The presence of these perforations alters the local flow regime as the fluid passes through the plate, influencing vortex formation and dissipation, thereby regulating the added mass and damping coefficients of the structure. Tao et al. [13] compared the damping mechanisms of solid and perforated plates under forced oscillation, indicating that solid plates exhibit the strongest damping effect in the high Keulegan–Carpenter (KC) number regime, whereas the damping enhancement of perforated plates is more pronounced under low KC number conditions. Mentzoni et al. [12] arrived at a similar conclusion, further clarifying that the hydrodynamic behavior of perforated plates strongly depends on the motion amplitude.

The edge geometry of a heave plate has an impact on its hydrodynamic performance. Shen et al. [15] investigated the hydrodynamic characteristics of heave plates with various edge shapes for truss Spar platforms, concluding that heave plates with tapered edges exhibit the best hydrodynamic performance. Tian et al. [16] demonstrated through forced oscillation tests that sharp-edged structures can excite higher damping and added mass compared to rounded edges. Zhang et al. [17] experimentally investigated the damping coefficients of heave plates with varying thicknesses, finding that at identical KC numbers, reducing the plate thickness yields a larger damping coefficient. Chen et al. [18] designed petal-shaped heave plates, which, in numerical calculations, demonstrated superior damping performance over traditional circular plates. Zha et al. [19] developed dual-layer flower-shaped heave plates with three-petal, six-petal, and nine-petal arrangements. Their simulations verified that staggered flower-shaped plates significantly outperform conventional circular plates in vibration reduction, with the three-petal dual-layer staggered configuration demonstrating the most outstanding heave suppression capability for floating platforms. Ezoji et al. [20] evaluated performance by introducing the ratio of the plate edge area to the total area, discovering that the damping coefficient climbs significantly when this parameter slightly exceeds 0.1, whereas a threshold below 0.1 leads to damping attenuation. Existing research has primarily focused on simple edge geometries or the overall macroscopic shape of heave plates, leaving a noticeable gap in the literature regarding complex edge configurations, such as salient edge structures. The introduction of a salient skirt structure can effectively manipulate the near-wall flow field, thereby enhancing the hydrodynamic performance of the system. This approach offers a cost-effective means to improve the stability of floating platforms, presenting substantial research significance and practical engineering value.

The number of plates and spatial spacing of multi-layer heave plates represent another core research focus. Fischer et al. [21] explored the effects of the number of plate layers and spacing on the damping ratio, pointing out that although the total damping force of the system strengthens with an increasing number of plates, the addition of the first plate provides the most significant leap in damping. Tao et al. [22] conducted numerical analyses on the forced oscillation of a cylinder with dual-layer plates, finding that excessively small

plate spacing leads to intense vortex shedding interference, which consequently weakens the overall damping. The study by Koh et al. [23] yielded a convergent conclusion: appropriately widening the spacing between dual-layer plates helps synchronously enhance both damping and added mass, thereby achieving a superior heave response suppression effect. The aforementioned literature primarily focuses on heave plates with identical geometries, investigating the effects of plate quantity and inter-plate spacing on their overall performance. While previous studies regarding plate geometry have concentrated on how horizontal planar shapes influence hydrodynamics, research concerning plate spacing has addressed configurations in the vertical plane. However, the spatial arrangement of multi-layer heave plates with varying geometries in the vertical direction remains largely unexplored. By altering the sizes of individual heave plates, the profile of the flow-facing surface in the vertical plane can be modified, thereby enhancing the hydrodynamic performance of the system. This approach can effectively improve system stability without significantly increasing installation complexity or economic costs, presenting considerable research significance and practical application value.

Therefore, this study investigates the flow field evolution and hydrodynamic coefficients of salient edge structures under different horizontal gaps for single-layer plates. For multi-layer plates, it examines the flow field and hydrodynamic behaviors of stepped arrangements under varying spacing conditions. Forced oscillation numerical simulations are conducted on these configurations. The corresponding hydrodynamic coefficients are extracted and thoroughly analyzed. The revealed underlying mechanisms provide theoretical guidance for the optimal design of novel heave plates.

2. Methodologies

2.1. Governing Equations

To accurately simulate the transient hydrodynamic responses and the highly nonlinear vortex shedding mechanism around heave plates, the incompressible Reynolds-Averaged Navier–Stokes (RANS) equations are adopted in this study. The ambient water is modeled as an incompressible viscous Newtonian fluid, and the macroscopic flow field is governed by the mass and momentum conservation equations. The corresponding continuity equation and the Cartesian tensor form of the RANS equations can be expressed as:

$$\frac{\partial u_i}{\partial x_i} = 0 \quad (1)$$

$$\frac{\partial(\rho u_i)}{\partial t} + \frac{\partial(\rho u_i u_j)}{\partial x_j} = -\frac{\partial p}{\partial x_i} + \frac{\partial}{\partial x_j} \left(\mu \frac{\partial u_i}{\partial x_j} - \rho \overline{u'_i u'_j} \right) + \rho g_i \quad (2)$$

where x_i ($i = 1, 2, 3$) represents the Cartesian coordinates, u_i is the ensemble-averaged velocity component, p is the time-averaged static pressure, ρ and μ denote the fluid density and dynamic viscosity respectively, and ρg_i representing the gravitational body force. The term $-\rho \overline{u'_i u'_j}$ defines the unknown Reynolds stress tensor induced by turbulent fluctuations, which necessitates the introduction of an appropriate turbulence closure model to resolve the flow characteristics.

2.2. Turbulence Modeling

When a heave plate with sharp edges undergoes forced harmonic oscillation, it inevitably induces massive flow separation, strong adverse pressure gradients, and complex vortex-vortex interactions. To reliably predict these intricate boundary layer behaviors, this study utilizes the Menter Shear Stress Transport (SST) k - ω model for turbulence closure. This advanced hybrid model has a prominent advantage by using a blending function F1

to seamlessly combine the standard k - ω and k - ε models, where the k - ω model is activated in the near-wall sublayer to take advantage of its superior resolution capability for adverse pressure gradient flows, while the k - ε model is applied in the fully turbulent far-field region. This approach successfully overcomes the inherent shortcomings of a single model regarding free-stream sensitivity and near-wall flow separation prediction. The transport equations for turbulent kinetic energy (k) and specific dissipation rate (ω) are expressed as:

$$\frac{\partial(\rho k)}{\partial t} + \frac{\partial(\rho u_j k)}{\partial x_j} = \tilde{P}_k - \beta^* \rho \omega k + \frac{\partial}{\partial x_j} \left((\mu + \sigma_k \mu_t) \frac{\partial k}{\partial x_j} \right) \quad (3)$$

$$\frac{\partial(\rho \omega)}{\partial t} + \frac{\partial(\rho u_j \omega)}{\partial x_j} = \frac{\gamma}{\nu_t} \tilde{P}_k - \beta \rho \omega^2 + \frac{\partial}{\partial x_j} \left((\mu + \sigma_\omega \mu_t) \frac{\partial \omega}{\partial x_j} \right) + 2\rho(1 - F_1) \sigma_{\omega 2} \frac{1}{\omega} \frac{\partial k}{\partial x_j} \frac{\partial \omega}{\partial x_j} \quad (4)$$

where μ is the dynamic viscosity, μ_t represents the turbulent eddy viscosity, and $\tilde{P}_k$ is the modified production term of turbulent kinetic energy. The empirical model constants (β^* , β , σ_k , σ_ω , γ , $\sigma_{\omega 2}$) are intrinsically blended via the F1 function to ensure an optimal transition between the inner and outer boundary layers.

2.3. Forced Oscillation and Hydrodynamic Coefficients

The movement of the heave plate in water is a vertical periodic motion. Its kinematic function follows a sinusoidal harmonic motion, which can be expressed as:

$$S(t) = A \sin(\omega t + \varphi) \quad (5)$$

where $S(t)$ is the vertical displacement, ω is the angular oscillation frequency, φ is the initial phase angle, and A is the motion amplitude.

For the complex hydrodynamic time-series data obtained from the simulations, a Fast Fourier Transform (FFT) technique is employed for frequency-domain analysis. This technique extracts the first-order hydrodynamic force $F^{(1)}$, which corresponds to the fundamental frequency of the structure's oscillation. Subsequently, based on the phase relationship, it is decomposed into a cosine component and a sine component:

$$F^{(1)} = F_{\cos}^{(1)} \cos(\omega t + \varphi) + F_{\sin}^{(1)} \sin(\omega t + \varphi) \quad (6)$$

where $F_{\sin}^{(1)}$ and $F_{\cos}^{(1)}$ are the first-order hydrodynamic components of $F^{(1)}$ in the cosine and sine phases, respectively.

During the heave motion, the presence of an upper connecting structure breaks the structural symmetry between the top and bottom surfaces of the heave plate, resulting in an unequal pressure distribution. To eliminate the influence of this structural appendage on the calculation of the plate's hydrodynamic coefficients, the effect of dynamic pressure variations on the vertical surfaces must be removed, as mathematically described below:

$$F^{(1)} = F^D - \frac{\pi}{4} L_1^2 P_b + \frac{\pi}{4} (L_1^2 - L_2^2) P_t \quad (7)$$

where L_1 and L_2 are the diameters of the heave plate and the upper structure, respectively; P_b and P_t are the dynamic pressures acting on the bottom and top surfaces of the heave plate.

For the isolated heave plate, the first-order hydrodynamic force $F^{(1)}$ in the heave direction can be characterized by the Morison equation:

$$F^{(1)} = C_a \rho V \ddot{S}(t) + \frac{1}{2} C_d \rho A_s \dot{S}(t) \left| \dot{S}(t) \right| \quad (8)$$

where C_a and C_d are the added mass coefficient and the damping coefficient, respectively; V is the displaced volume of the heave plate; A_s is the projected area of each heave plate in the Z-direction; and ρ is the density of seawater.

To determine the optimal C_a and C_d from the highly nonlinear force time-series extracted from the CFD solver, an objective function Q is constructed. The data fitting is performed by minimizing the residual sum of squares (RSS) between the Computational Fluid Dynamics (CFD)-computed values and the theoretical Morison values:

$$Q = \sum [F_{CFD}(t_i) - F_{Morrison}(t_i)]^2 \quad (9)$$

Based on the aforementioned criteria, by applying Fourier decomposition or the energy equivalence principle, the integral formulations for calculating the damping coefficient C_d and the added mass coefficient C_a can be systematically derived:

$$C_a = \frac{\int_{t_1}^{t_2} F^{(1)}(t) \cdot \ddot{S}(t) dt}{\rho V \int_{t_1}^{t_2} \ddot{S}(t)^2 dt} \quad (10)$$

$$C_d = \frac{\int_{t_1}^{t_2} F^{(1)}(t) \cdot \dot{S}(t) |\dot{S}(t)| dt}{\frac{1}{2} \rho A_s \int_{t_1}^{t_2} \dot{S}(t)^4 dt} \quad (11)$$

It is important to explicitly emphasize the definition of the reference volume V used in this study to ensure transparency regarding the calculation of the added mass coefficient (C_a). In many conventional experimental studies, the added mass is normalized using an equivalent reference volume ($V_{eq} = A_s \times D$, where D is the characteristic width). However, the present numerical study utilizes the actual physical displaced volume of the thin plate ($V = A_s \times t$, where t is the physical thickness of the plate).

The adoption of the actual physical displaced volume rather than the equivalent reference volume offers physical and engineering advantages. First, it ensures physical transparency. For a thin heave plate, the physical entity is determined solely by its area and thickness. Using the actual volume accurately reflects the mass of the fluid displaced by the structure, establishing a more logical proportional relationship between the added mass and the actual structural mass. Second, it eliminates geometric ambiguity. The conventional equivalent cube assumption artificially treats a thin plate as a massive cube with a height equal to its width, which is geometrically distorted. The actual volume avoids the arbitrary selection of a characteristic height, making the coefficient definition more rigorous. Finally, it facilitates direct application to full-scale design. In full-scale prototype modeling, the thickness t is a definitive design parameter. Coefficients calculated using the actual volume directly reflect the inertial force characteristics of the structure under real sea conditions, minimizing potential conversion errors.

Because the characteristic width D is significantly larger than the thickness t ($D/t = 50$ in the current configurations), the use of the actual displaced volume inherently yields numerically much larger C_a values compared to those typically reported in the literature (which generally range from 0.4 to 0.8). This fundamental definition is explicitly clarified a priori to rationalize the apparent numerical discrepancy with prior literature, thereby ensuring the physical consistency of the subsequent validation process.

3. Model Parameters and Computational Setup

3.1. Model Parameters

Figure 1 shows the geometric configurations and key dimensional parameters of the heave plates proposed in this study, covering the core design variables including

edge inclination angle, horizontal distribution spacing, vertical inter-layer spacing, and inter-layer size difference.

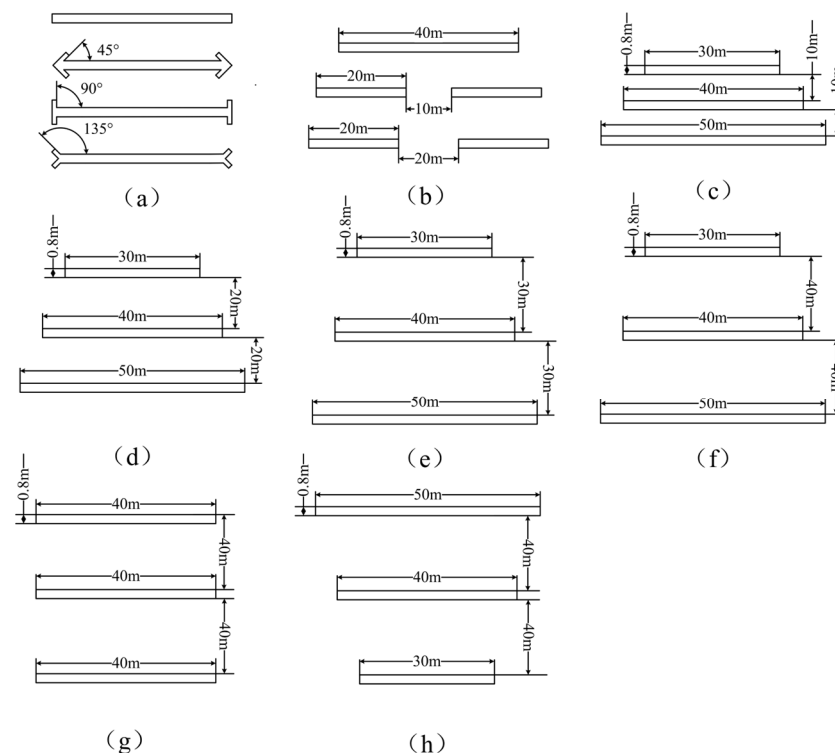


Figure 1. Detailed dimensions of the proposed heave plates. (a) heave plate configurations with varying edge angles; (b) heave plates with an edge angle of 0° and varying horizontal distribution spacings; (c) three-layer heave plates (spacing = 10 m) with lengths of 30 m, 40 m, and 50 m from top to bottom; (d) three-layer heave plates (spacing = 20 m) with lengths of 30 m, 40 m, and 50 m from top to bottom; (e) three-layer heave plates (spacing = 30 m) with lengths of 30 m, 40 m, and 50 m from top to bottom; (f) three-layer heave plates (spacing = 40 m) with lengths of 30 m, 40 m, and 50 m from top to bottom; (g) three-layer heave plates (spacing = 40 m) with equal lengths of 40 m; (h) three-layer heave plates (spacing = 40 m) with lengths of 50 m, 40 m, and 30 m from top to bottom.

For single-layer heave plates, the base characteristic side length is set to $D = 40$ m, and the salient edge angles α are configured as 0° , 45° , 90° , and 135° , with their specific two-dimensional cross-sectional profiles illustrated in Figure 1a. To investigate the effect of horizontal distribution gaps on the structural hydrodynamic coefficients (i.e., damping coefficient and added mass coefficient) under different edge angles, horizontal distribution spacings of 0 m, 10 m, and 20 m are introduced for each angle scheme. To keep the total projected area facing the incoming flow constant, the base heave plate is divided into four equal-area sub-plates, each with a characteristic side length of $d = 20$ m. A dimensionless parameter, namely the horizontal gap ratio l/d , is defined to quantitatively describe the different gap arrangement schemes; the geometric topology of this parameter is illustrated in Figure 1b.

Regarding the triple-layer heave plate configurations, Figure 1c–f present arrangement schemes with varying vertical spacings. The physical spacing L between adjacent layers is kept equal and sequentially increased ($L = 10$ m, 20 m, 30 m, and 40 m), while the side length of the middle-layer base plate remains constant at $D = 40$ m. The characteristic length $D = 40$ m corresponds to the projected area of the floating platform base. This full-scale simulation mitigates the Reynolds number (Re) scale effects inherent in scaled model tests. It provides hydrodynamic coefficients directly applicable to engineering design. To

facilitate dimensionless engineering analysis, the “dimensionless spacing ratio” L/D is defined as the core parameter representing the vertical spatial arrangement.

Furthermore, Figure 1f–h illustrate the inter-layer size difference arrangement schemes under a fixed dimensionless spacing ratio of $L/D = 1.0$ (i.e., $L = 40$ m). By introducing heave plates of different sizes with characteristic side lengths of $H_1 = 30$ m and $H_2 = 50$ m, respectively, and calculating the side length difference between adjacent layers in a top-to-bottom sequence, the inter-layer size difference Δd is defined. Consequently, three typical spatial arrangement schemes are constructed: the inverse stepped arrangement ($\Delta d = -10$ m, as shown in Figure 1f), the equal-size coincident arrangement ($\Delta d = 0$ m, as shown in Figure 1g), and the positive stepped arrangement ($\Delta d = 10$ m, as shown in Figure 1h). This study aims to utilize the size difference Δd to quantitatively describe the influence of the spatial mismatch characteristics of multi-layer plate edges on their hydrodynamic performance.

3.2. Computational Domain and Boundary Conditions

To minimize the interference of boundary effects on the flow field evolution, a large-scale computational domain with dimensions of $6D$, a width of $6D$, and a height of $9D$ is established in this study (where the characteristic length $D = 40$ m corresponds to the side length of the base heave plate). At the initial moment, the heave plate rests at the geometric center of the computational domain. The topology of the boundary conditions for the computational domain is illustrated in Figure 2, and the specific configurations are detailed as follows:

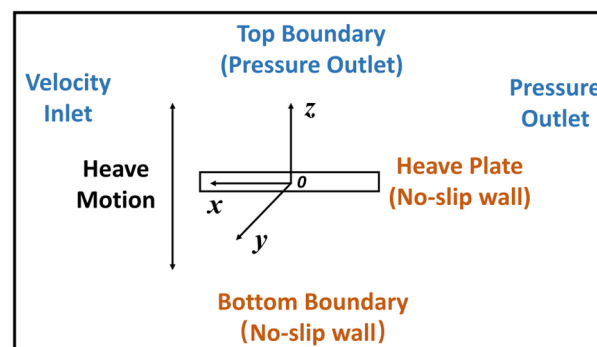


Figure 2. Boundary condition setup for the computational domain.

The left boundary of the computational domain is set as a velocity inlet, with the inflow velocity fixed at 0 m/s to simulate a still water environment. Both the right and top boundaries are set as pressure outlets with a relative static pressure (gauge pressure) of zero, and identical turbulence parameters are specified for both the inlet and outlets. The bottom, front and back side walls, as well as the surfaces of the heave plate, are all defined as no-slip walls. In particular, the kinematic compatibility condition is strictly imposed on the surfaces of the heave plate, ensuring that the velocity of fluid particles is exactly consistent with the instantaneous oscillation velocity of the rigid body.

When addressing the problem of large-amplitude forced oscillation of structures, the overset grid technology is introduced in this study to effectively prevent severe mesh distortion and negative volume errors caused by large deformations in traditional dynamic meshes. This technology allows the body-fitted component grid wrapping the heave plate to perform high-fidelity rigid translations within the background grid, without requiring any topological reconstruction or stretching.

The spatial discretization of the computational domain adopts a high-quality meshing strategy predominantly based on structured hexahedra. To balance computational accuracy

and solution efficiency, a three-level local spatial transition refinement is implemented on the background grid. The base cell size for the far-field outermost grid is set to 6 m. Meanwhile, to accurately capture the complex vortex shedding and wake evolution induced by the heave motion, a specialized refinement zone with a height of 6 m is customized along the core motion path within the background domain, where the cell size transitions to 0.8 m. Furthermore, to ensure the accuracy of the flow field solution in the near-wall boundary layer, the surface mesh size on the heave plate is further refined to 0.4 m. Combined with an estimated boundary layer thickness of 0.15 m, the boundary layer mesh is arranged in the normal direction, and the dimensionless wall distance y^+ is strictly controlled to be maintained around 30, thereby satisfying the wall function criteria of the selected turbulence model.

The commercial software ANSYS Fluent R2022 is utilized to solve the governing partial differential equations. The Coupled algorithm is employed for pressure-velocity coupling to enhance numerical stability during the overset mesh motion. For spatial discretization, the PRESTO! scheme is applied to the pressure field. The momentum, turbulent kinetic energy, and specific dissipation rate equations are discretized using the Second-Order Upwind scheme to minimize numerical diffusion. Additionally, the First-Order Implicit scheme is adopted for the transient formulation to ensure stable temporal integration during the overset mesh motion.

4. Verification and Validation

4.1. Verification

Taking the equal-size arrangement with a dimensionless spacing ratio of $L/D = 1.0$ and a size difference of $\Delta d = 0$ m as the benchmark case, mesh and time step independence verification is performed under a typical forced oscillation condition: motion amplitude $A = 1.27$ m, Keulegan–Carpenter (KC) = 0.2, motion period $T = 25$ s. In terms of spatial discretization, three levels of mesh schemes are constructed: coarse (1.54 million cells), medium (2.36 million cells), and fine (5.87 million cells), with the baseline time step for verification uniformly set to 0.1 s.

During the initial phase of the numerical calculation, the background flow field remains completely stationary. If the full-amplitude velocity and acceleration of the structural motion are applied instantaneously, it will easily induce pressure shock oscillations in the flow field. Meanwhile, abrupt displacement of the structural boundaries may cause mesh update failure or trigger negative cell volume errors, eventually leading to the divergence of the numerical calculation. To overcome this numerical instability, a soft-start algorithm is introduced in this study. This algorithm modulates the input signal during the first two motion periods ($2T$) of the calculation by superimposing a linear ramp function $R(t)$:

$$R(t) = \begin{cases} t/(2T), & 0 \leq t < 2T \\ 1, & t \geq 2T \end{cases} \quad (12)$$

The modified velocity governing equation is expressed as:

$$v_{input}(t) = R(t) \cdot A\omega \cos(\omega t) \quad (13)$$

The above soft-start mechanism ensures a smooth and continuous transition of the heave plate from the initial stationary state to the full-amplitude stable oscillation state, thus significantly improving the numerical robustness of the transient flow simulation.

Table 1 compares the damping coefficients and added mass coefficients calculated using the three different mesh density schemes under a fixed time step of $\Delta t = 0.1$ s.

The results indicate that the relative deviation in the added mass coefficient between the medium and fine meshes is approximately 1%, and the relative deviation in the damping coefficient also remains around 1%. Since the relative deviation between the two sets of results is well within the acceptable numerical error range, and considering the balance between computational efficiency and solution accuracy, the medium-density mesh scheme is selected for all subsequent simulations in this study.

Table 1. Mesh independence verification.

Number of Cells (Million)	C_{ai}	$(C_{ai+1} - C_{a1}) / C_{a1}$	C_{di}	$(C_{di+1} - C_{d1}) / C_{d1}$
1.54	23.305		7.094	
2.36	23.603	1.28%	7.174	1.134%
5.87	23.626	1.38%	7.181	1.235%

The independence verification for the time step is presented in Table 2. As the time step is reduced from 0.2 s to 0.1 s, the rates of change for both the added mass coefficient and the damping coefficient decrease significantly and the numerical results gradually converge. Moreover, the computational deviations between different time steps are consistently within an extremely small order of magnitude, indicating that the numerical results have converged and are independent of the time step. Based on a dual assessment of computational resource consumption and result reliability, all subsequent formal numerical simulations uniformly adopt the medium-density mesh and strictly set the time step to $\Delta t = 0.1$ s.

Table 2. Time-step independence verification.

Time Step (s)	C_{ai}	$(C_{ai+1} - C_{a1}) / C_{a1}$	C_{di}	$(C_{di+1} - C_{d1}) / C_{d1}$
0.05	23.902		7.182	
0.1	23.603	−1.25%	7.174	−0.111%
0.2	23.216	−2.87%	7.161	−0.292%

4.2. Validation Studies

Extensive physical model tests have been conducted in the academic community to investigate the forced oscillation characteristics of heave plates. To verify the accuracy and reliability of the numerical simulation method proposed in this paper, this section conducts a comparative analysis using the physical experimental data from Li et al. [9] as a benchmark.

The physical model experiments were conducted in the large-scale nonlinear wave basin at the State Key Laboratory of Coastal and Offshore Engineering, Dalian University of Technology. The basin has overall geometric dimensions of 60.0 m in length, 4.0 m in width, and 2.5 m in depth. Across all test conditions, the actual still-water working depth in the basin was maintained at a constant 2.0 m. A forced oscillation mechanism was employed in the experiments. Specifically, a vertically mounted servo motor was utilized to drive a slider, thereby forcing the underlying heave plate to undergo periodic vertical oscillatory motion in still water. The model plate was rigidly connected to the upper slider via two steel rods, each with a diameter of 20 mm. Governed by the servo motor, the slider reciprocated periodically along a guide rail. The experimental setup and its corresponding schematic are illustrated in Figure 3.

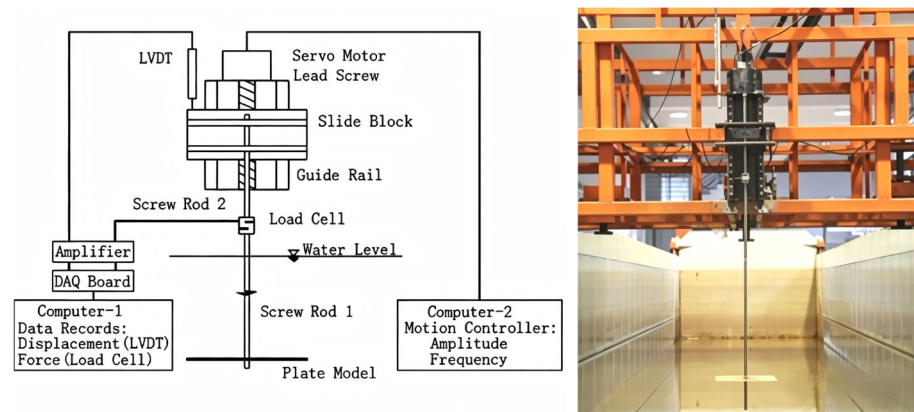


Figure 3. Experimental setup used in previous studies.

The validation conditions selected in this study are listed in Table 3. Specifically, the motion amplitude a of the forced oscillation is derived based on the specified Keulegan–Carpenter (KC) numbers and the characteristic plate width. According to the definition in Equation (14), KC numbers of 0.2, 0.4, 0.6, 0.8, and 1.0 are selected for this validation scheme, helping to deduce the corresponding theoretical amplitudes.

$$KC = \frac{2\pi a}{L} \quad (14)$$

where L is the characteristic width of the heave plate, a and f denote the motion amplitude and frequency of the forced oscillation, respectively.

Table 3. Experimental conditions.

Water Depth (m)	Size (mm)	Plate Thickness (mm)	Motion Frequency (Hz)	Amplitude (mm)
1.2	400	5	0.2	12.7, 25.5, 38.2, 50.9, 63.7

Based on the Morison equation, the hydrodynamic governing equation for the forced oscillation system, including the heave plate, can be expressed as:

$$(M + \rho \nabla C_a) \ddot{X} + \frac{1}{2} \rho A C_d |\dot{X}| \dot{X} = F \quad (15)$$

where M is the total mass of the system, ρ is the water density, ∇ is the total submerged volume of the heave plate including the connecting rod, and A is the characteristic projected area of the heave plate facing the flow; C_a and C_d are the added mass coefficient and the damping coefficient to be determined, respectively. X , $\dot{X}$, and $\ddot{X}$ correspond to the instantaneous displacement, velocity, and acceleration during the forced motion, respectively; F represents the total transient fluid excitation force acting on the heave plate system. For the study of the forced oscillation characteristics of thin plates in a fluid, the submerged volume ∇ is typically simplified and defined using an “equivalent cube” volume, i.e., $\nabla = A \times L$ (where L represents the width of the plate).

By combining the aforementioned specified conditions with the Morison theoretical framework, the corresponding added mass coefficient C_a and damping coefficient C_d can be derived from the time–history force data extracted via CFD numerical simulations. Figure 4 shows the comparison between the numerical results obtained in this study and the experimental data reported by Li et al. Table 4 presents the relative deviations of the simulated damping and added mass coefficients compared to the experimental values.

The hydrodynamic coefficients under forced oscillation calculated by the numerical model exhibit excellent agreement with the experimental measurements, with the relative difference maintained at approximately 1%. This fully validates the accuracy of the established numerical simulation method and the reliability of the computational strategy employed in this study.

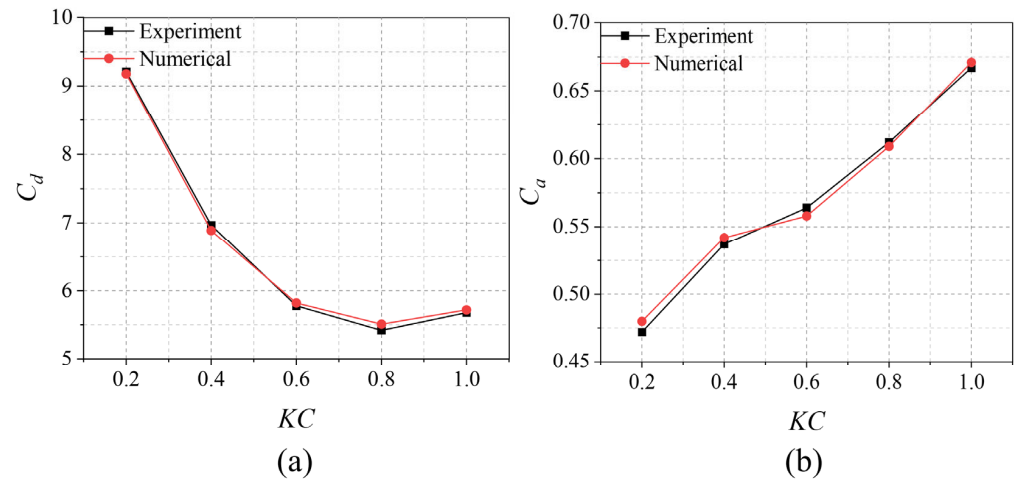


Figure 4. Comparison of experimental and numerical results: (a) damping coefficient; (b) added mass coefficient.

Table 4. Relative differences between the simulated hydrodynamic coefficients and the experimental values.

KC	Experimental C_d	Simulated C_d	Experimental C_a	Simulated C_a	Error of C_d (%)	Error of C_a (%)
0.2	9.21	9.18	0.47	0.48	−0.33	1.70
0.4	6.97	6.89	0.54	0.54	−1.15	0.93
0.6	5.78	5.82	0.56	0.56	0.69	−1.06
0.8	5.42	5.51	0.61	0.61	1.66	−0.49
1.0	5.68	5.72	0.67	0.67	0.70	0.60

Regarding the magnitude of the added mass coefficient (C_a), a significant numerical difference is observed compared to existing literature. As explicitly defined earlier in Section 2.3, this study utilizes the actual physical displaced volume ($V = A_s \times t$) for normalization, whereas many conventional experimental studies, such as Li et al. (2013) [9], employ an equivalent cubic volume ($V_{eq} = A_s \times D$). In our full-scale model, the characteristic width D (40 m) is exactly 50 times the physical thickness t (0.8 m). Consequently, the raw C_a values extracted from the simulations (which exceed 30) are inherently much larger in magnitude. However, once these values are converted using the equivalent volume approach, the adjusted range of 0.6 to 0.8 aligns well with the benchmark data from Li et al. (2013) [9]. This consistency confirms that the apparent discrepancy is purely a result of the reference volume definition and further substantiates the high fidelity of the current numerical framework.

Regarding the scope of application, the current numerical framework employs a full-scale prototype dimension ($D = 40$ m). This approach significantly reduces the Reynolds number (Re) scale effects inherent in traditional Froude-scaled experiments. Under high Reynolds number conditions ($Re > 10^5$), forced flow separation at the salient edges dominates the hydrodynamic damping. The investigated KC range ($0.2 \leq KC \leq 1.0$) matches the typical small-amplitude, high-frequency heave responses of deep-water floating platforms.

Meanwhile, the current model presents specific limitations. It relies on a single-degree-of-freedom forced harmonic oscillation assumption. This isolation separates the heave dynamics from multi-body fluid–structure interactions and multi-directional wave kinematics. Future research should incorporate fully coupled simulations under irregular waves.

5. Results and Discussions

This section presents a detailed description and mechanistic analysis of the numerical simulation results. To ensure the completeness of the flow field evolution and the reliability of the data, all numerical simulations are conducted based on the following unified initial conditions and computational settings: (1) The initial flow field is defined as a still-water environment, with the global initial velocity field being zero. (2) At the initial moment ($t = 0$), the heave plate rests at the static equilibrium position of the system. (3) To ensure the full development of the flow field, the total computational time is set to 6 complete forced oscillation periods, including a soft-start phase for the first two periods. (4) The motion amplitude of the forced oscillation is dynamically matched with the KC number (for example, when $KC = 0.2$, the corresponding amplitude is set to 1.27 m), while the motion period is uniformly set to $T = 25$ s.

5.1. Heave Hydrodynamic Forces

Due to the introduction of a soft-start mechanism in the initial stage of the numerical calculation, the early force time–history curves inevitably encompass an unsteady transient response process. Observations of the heave hydrodynamic time–history curves reveal that starting from the third motion period, the peak and trough amplitudes of the hydrodynamic forces converge to a periodic steady state. Therefore, to minimize the influence of initial transient effects, the unstable data of the first two periods (approximately 0–50 s) are truncated during post-processing, and the subsequent four complete periods in the steady state are used for the extraction and in-depth analysis of the hydrodynamic coefficients.

Figure 5 illustrates the forced oscillation hydrodynamic time–history curves of single-layer heave plates with various salient edge angles under the arrangement condition where the dimensionless horizontal gap ratio is $l/d = 0$. As observed from the heave hydrodynamic curves, with the increase in the plate edge angle, the heave hydrodynamic force exerted on the plate exhibits a trend of initially decreasing and subsequently increasing, with the peak hydrodynamic force reaching its maximum at 135° . It is worth noting that the heave hydrodynamic force time history for the 45° edge angle scheme shows slight high-frequency fluctuations. The underlying physical mechanism can be attributed to the 45° inward-contracting salient skirt structure, which forms an acute-angled groove at the plate edge. This special geometric topology intensifies the instability of flow separation and vortex shedding evolution in the local boundary layer. As a result, the high-frequency small-scale vortex shedding in the flow field alternately acts on the plate surface, thus inducing nonlinear micro-amplitude oscillations in the macroscopic hydrodynamic loads.

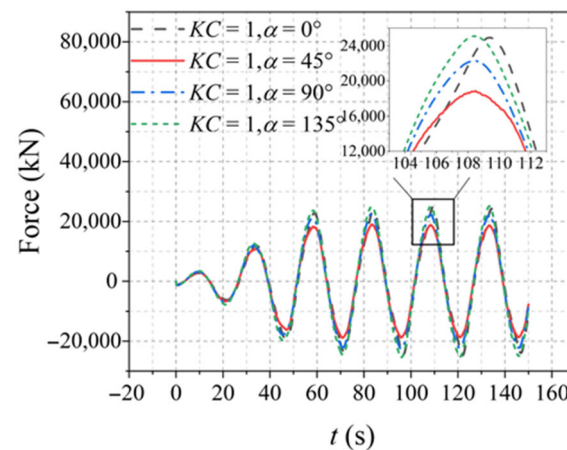


Figure 5. Heave hydrodynamic curves for varying edge angles under the specific condition of $KC = 1$ and $L/D = 0$.

5.1.1. Effects of Edge Salient Angles and Horizontal Distribution Spacings

Hydrodynamic time histories within a single steady-state motion period are extracted to compare the force variations among different configurations. At a dimensionless horizontal gap ratio of $l/d = 0$, the heave hydrodynamic forces varying with the KC number and edge angle are illustrated in Figure 6.

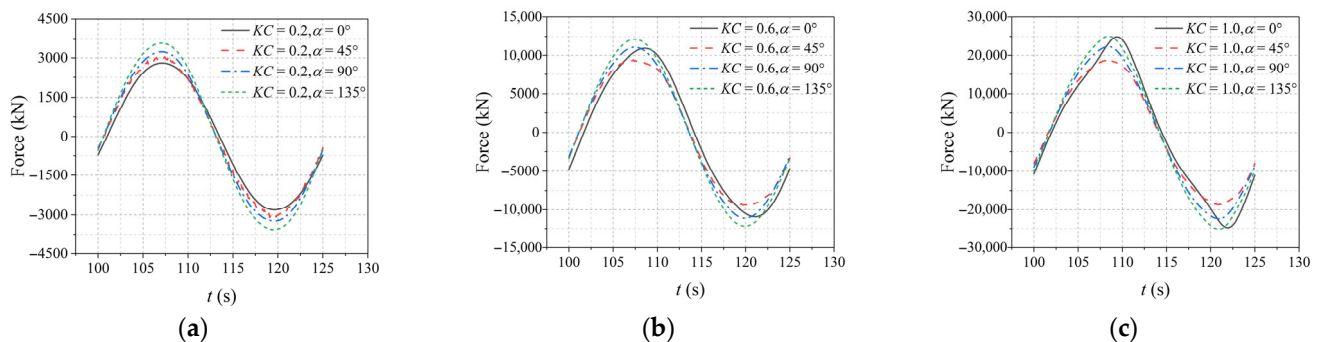


Figure 6. Time histories of the heave hydrodynamic forces for varying edge angles under the specific condition of $l/d = 0$: (a) $KC = 0.2$; (b) $KC = 0.6$; (c) $KC = 1.0$.

Overall, the macroscopic heave hydrodynamic forces exhibit periodic oscillations consistent with the forced motion displacement. The hydrodynamic amplitudes increase significantly as the KC number increases. At $KC = 0.2$, the peak heave hydrodynamic forces range from approximately 1500 to 2500 kN. However, at $KC = 1.0$, the amplitudes surge to over 20,000 kN. This phenomenon indicates that the contributions of fluid drag and inertial forces to the structural hydrodynamic loads enhance significantly with greater motion amplitudes.

Meanwhile, it can be clearly observed that the distortion of the curve shape relative to a standard sine wave becomes more pronounced with increasing KC number, which can be attributed to the enhanced nonlinearity of the hydrodynamic system. According to the Morison theoretical equation, the inertia force term is linearly correlated with acceleration, while the drag force term is proportional to the square of the velocity. When the KC number is relatively small, the system operates in the “inertia-dominated regime,” yielding a highly regular sinusoidal curve. As the KC number increases, the flow velocity v increases, leading to a significant rise in the proportion of the velocity-squared term (drag term) within the total hydrodynamic force. This inherent nonlinear characteristic of the squared term causes the wave crests to become sharper or induces waveform distortion.

Table 5 presents the influence of the dimensionless horizontal gap ratio l/d on the peak heave hydrodynamic force under various KC numbers, with the plate edge angle fixed at 0° . Overall, under identical KC number conditions, the amplitude of the heave hydrodynamic force exhibits a significant attenuation trend as the spacing ratio increases.

Table 5. Peak heave hydrodynamic forces under different distribution spacing ratios.

KC	l/d	Edge Angle ($^\circ$)	Peak Heave Hydrodynamic Force (kN)
0.2	0	0	2803.788
0.2	0.5	0	1606.657
0.2	1	0	1548.389
0.6	0	0	10,966.919
0.6	0.5	0	7625.010
0.6	1	0	7220.517
1.0	0	0	24,926.322
1.0	0.5	0	19,457.850
1.0	1	0	18,990.029

The underlying fluid dynamic mechanisms can be attributed to two primary aspects: First, the introduction of horizontal gaps disrupts the geometric integrity of the base plate, inducing short-circuit leakage of the fluid between the upper and lower surfaces of the structure, which subsequently leads to a substantial reduction in pressure drag. Second, the widening of the gap partitions the originally intact large-scale structure into multiple isolated small-scale units, thereby shortening the effective characteristic length of the structure. The edge flow-around effect directly penetrates the central region of each sub-plate, severely diminishing the volume of entrained water that the plate surface can effectively trap and displace. In summary, the combined effect of fluid leakage through the gaps and the reduction in the effective characteristic length leads to a drastic decrease in the overall hydrodynamic force on the structure.

5.1.2. Effects of Plate Spacing Ratio and Size Difference

Figure 7 illustrates the heave hydrodynamic time histories under various KC numbers and spacing ratios (L/D) with a size difference (Δd) of -10 m.

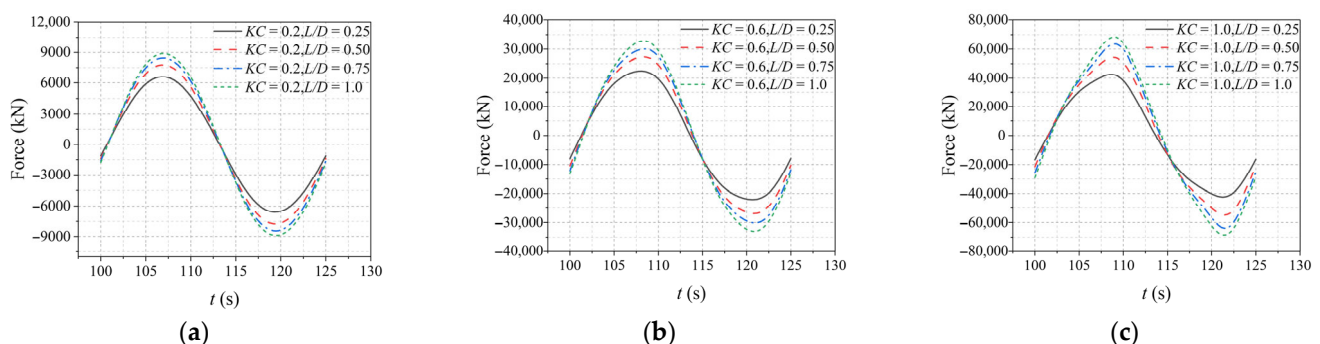


Figure 7. Time histories of the heave hydrodynamic forces for three-layer heave plates with varying plate spacings (L/D) under the condition of $\Delta d = -10$ m: (a) $KC = 0.2$; (b) $KC = 0.6$; (c) $KC = 1.0$.

The amplitude increases significantly with an increasing KC number, and it also escalates as the spacing ratio L/D increases: under the identical KC number, the peak value at $L/D = 1.0$ (green curve) reaches the maximum, whereas the peak at $L/D = 0.25$ (black curve) is the minimum. The underlying mechanism is that an extremely strong flow interference occurs between the three layers of plates when the spacing ratio is small.

This causes the fluid between the plates to be “locked in” and move synchronously with the structure, thus suppressing the disturbance of the flow field. Conversely, when the spacing ratio is enlarged, the “locked-in” state of the inter-plate water body is disrupted, enabling each plate layer to interact more fully with the surrounding fluid. Consequently, the fluid separation and vortex evolution become more intense, ultimately leading to an augmentation in the total hydrodynamic amplitude.

Furthermore, consistent with the force evolutions of the single-layer plate described previously, as the KC number continuously increases, the degree of higher-order harmonic distortion in the hydrodynamic curves of the three-layer heave plate, relative to a standard sine wave, becomes increasingly severe. This phenomenon is similarly attributed to the elevation of the instantaneous flow velocity at higher KC numbers, which significantly increases the weight of the velocity-squared term within the total excitation force and sharply amplifies the inherent nonlinear effects of the system.

Table 6 quantitatively demonstrates the influence of the plate size difference (Δd) on the peak heave hydrodynamic forces of triple-layer heave plates under a dimensionless spacing ratio $L/D = 1.0$ and various KC numbers. Overall, at the same KC number, the configurations with $\Delta d = -10$ m and 10 m exhibit nearly identical peak hydrodynamic forces, both of which are higher than the equal-size arrangement where $\Delta d = 0$ m.

Table 6. Peak heave hydrodynamic forces under different plate size difference schemes.

KC	Δd (m)	L/D	Peak Heave Hydrodynamic Force (kN)
0.2	−10	1.0	8893.8
0.2	0	1.0	7917.5
0.2	10	1.0	8892.9
0.6	−10	1.0	33,178.6
0.6	0	1.0	29,821.2
0.6	10	1.0	33,204.9
1.0	−10	1.0	68,774.4
1.0	0	1.0	62,683.7
1.0	10	1.0	68,663.9

Regarding the underlying mechanism, when the size difference Δd is 10 m or −10 m, the triple-layer plates form a stepped spatial topology. This generates significant spatial misalignments of the plate edges in the vertical direction. Since forced heave is a vertically symmetric periodic reciprocating motion, the effective frontal misalignment area of each plate layer exposed to the free flow is practically equivalent, regardless of whether the plate size presents a positive increment (10 m) or a negative decrement (−10 m). Consequently, within a complete oscillation cycle, the intensities of their interaction and momentum ex-change with the water are equivalent, resulting in nearly identical peak hydrodynamic forces.

5.2. Hydrodynamic Coefficients

5.2.1. Effects of Edge Salient Angles and Horizontal Distribution Spacings

Figure 8 illustrates the evolution of the damping coefficient (C_d) with the KC number (ranging from 0.2 to 1.0) for heave plates with varying salient edge angles, under dimensionless horizontal gap ratios (l/d) of 0, 0.5, and 1.0. Overall, the C_d of the 0° edge configuration decreases continuously as the KC number increases. For the other edge configurations, C_d also exhibits a decreasing trend with increasing KC numbers in the low-to-medium KC regime ($KC < 0.8$). However, it shows a slight rebound at high KC numbers. A comparison among the different edge angle curves reveals that the geometric topology of the structural

edge significantly influences the damping coefficient. Under most medium-to-high KC conditions, the 0° edge configuration maintains a high damping coefficient. In contrast, the 45° and 90° edge configurations exhibit the lowest damping coefficients, and their curves highly overlap.

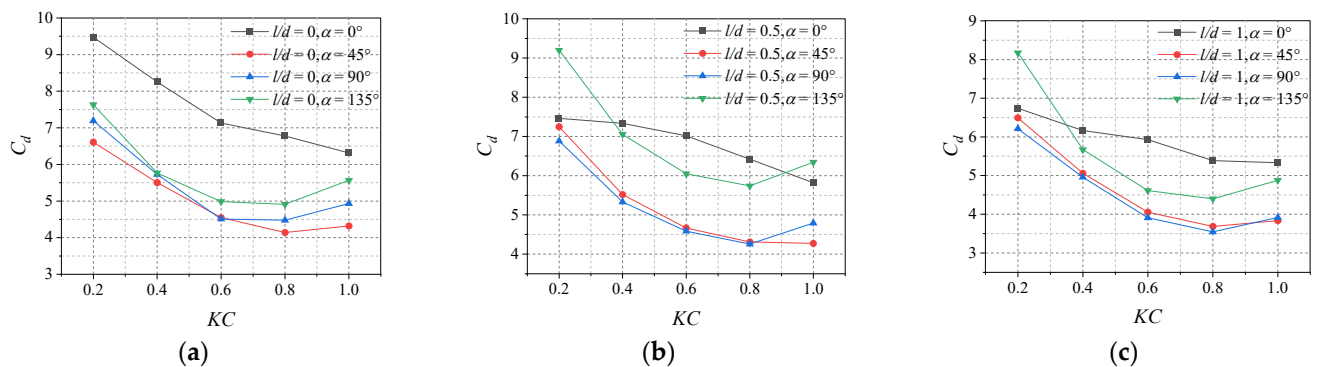


Figure 8. Variation in the damping coefficient C_d with KC number for heave plates with different edge angles under varying dimensionless horizontal gap ratios (l/d): (a) $l/d = 0$; (b) $l/d = 0.5$; (c) $l/d = 1.0$.

Correspondingly, Figure 9 illustrates the evolution of the added mass coefficient (C_a) with the KC number for the heave plates under the identical configurations. Unlike the decreasing trend of the damping coefficient, the added mass coefficient (C_a) exhibits a monotonically increasing trend with increasing KC numbers. Specifically, the 0° configuration (black square curve) achieves the global maximum C_a value at $l/d = 1.0$ and $KC = 1.0$. However, across the entire parameter range, the 135° edge configuration maintains the highest overall C_a levels. The C_a values for the 45° (red circle) and 90° (blue triangle) configurations consistently remain at the lowest levels.

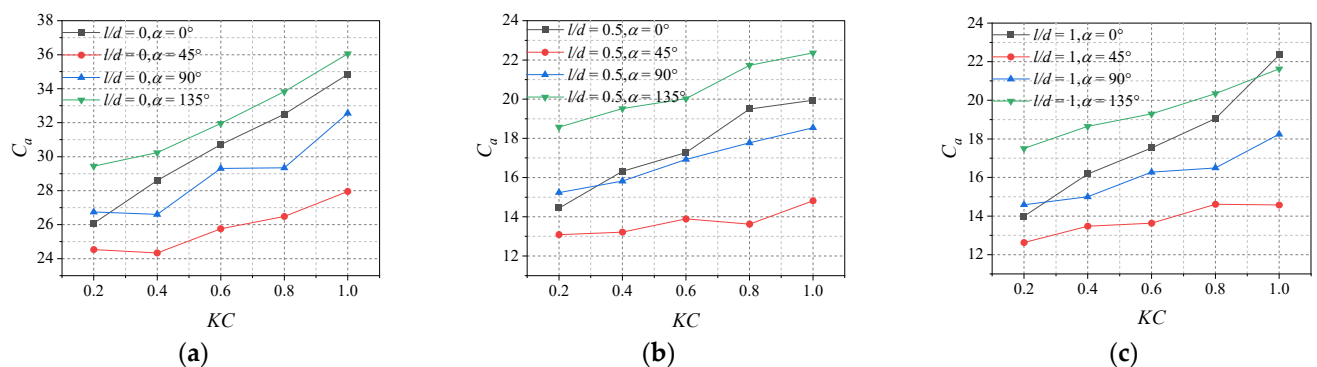


Figure 9. Variations in the damping coefficient C_a with KC number for heave plates with different edge angles under varying dimensionless horizontal gap ratios (l/d): (a) $l/d = 0$; (b) $l/d = 0.5$; (c) $l/d = 1.0$.

To investigate the fluid dynamic mechanisms underlying the effect of the edge angle on the damping coefficient, Figure 10 presents the velocity contours for all edge configurations at $t = 100$ s. These contours display both the velocity isolines and streamlines.

For the 45° edge configuration, the salient structure forms an inward-contracting acute groove at the plate periphery. During the vertical oscillation of the heave plate, the near-wall fluid undergoes radial acceleration from the center toward the edge. As the high-speed fluid impacts this acute-angled region, it generates numerous small, fragmented vortices. The subsequent fluid bypasses these recirculation vortices and moves downward over the top of the salient edge. This motion generates a high-speed wake and edge-shedding vortices. However, the acute-angled structure obstructs the high-speed flow. This

obstruction suppresses flow separation and wake expansion, thereby reducing the scale of the vortices shed from the plate edge. Consequently, this leads to a decrease in C_d .

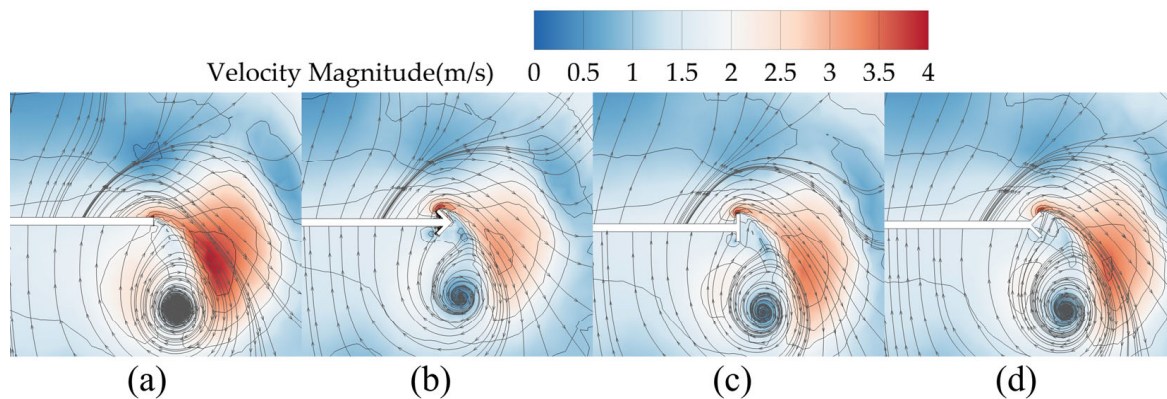


Figure 10. Velocity contours for heave plates with different salient edge angles at $t = 100$ s: (a) $\alpha = 0^\circ$; (b) $\alpha = 45^\circ$; (c) $\alpha = 90^\circ$; (d) $\alpha = 135^\circ$.

For the 90° edge configuration, the salient structure forms a right-angled baffle at the plate periphery. During the vertical oscillation, high-speed fluid also impacts this baffle. After decelerating, the fluid moves downward over the top of the salient edge. This motion generates a high-speed wake and edge-shedding vortices. Although the 90° baffle also obstructs the high-speed flow to suppress flow separation and wake expansion, thereby reducing the scale of the shedding vortices, it is more easily bypassed by the fluid than the acute groove of the 45° configuration. It does not generate fragmented vortices. Consequently, its wake velocity is higher, resulting in a larger C_d value.

For the 135° outward-expanding edge structure, as the accelerated fluid reaches the edge, it continues to accelerate along the inclined deflecting surface. Upon reaching the outermost tip, the fluid undergoes severe boundary layer separation and generates wake vortices. Compared to the 45° and 90° configurations, the 135° configuration produces a higher-speed wake and larger-scale wake vortices. The generation and shedding of these large-scale vortices significantly dissipate the kinetic energy of the flow field. This leads to an increase in the damping coefficient (C_d).

Compared to the aforementioned configurations, the 0° edge configuration lacks salient structures to obstruct the fluid at the periphery. After undergoing radial acceleration from the center to the edge, the near-wall fluid moves directly downward. This unobstructed motion generates a high-speed wake and edge-shedding vortices. Both the wake velocity and the scale of these shedding vortices are significantly larger. Consequently, this results in a higher damping coefficient (C_d).

While velocity contours can reveal the kinematics of the fluid during the motion of the heave plate, relying solely on velocity distributions makes it difficult to accurately quantify the spatial evolution morphology of complex vortices. Furthermore, it cannot effectively distinguish rotation-dominated vortex structures from strong shear deformation zones in the flow field. Therefore, this section introduces the Ω vortex identification criterion for the refined processing of the flow field. This criterion can significantly reduce the interference of strong shear deformations and clearly capture the true vortex cores dominated by rigid-body rotation. Typically, a threshold of $\Omega = 0.52$ is adopted as the boundary for vortex identification. A region is considered to contain a vortex when $\Omega \geq 0.52$, and a larger value of Ω indicates greater vorticity.

Figure 11 presents the transient Ω vortex contours of heave plates with different edge angles under the condition of $l/d = 0$ at $t = 100$ s. The salient edges with different angles alter the instability characteristics and velocity gradients of the local shear layer at the plate

extremities, which in turn dictate the scale and evolution mode of the wake vortices, as well as the macroscopic energy dissipation characteristics of the structure.

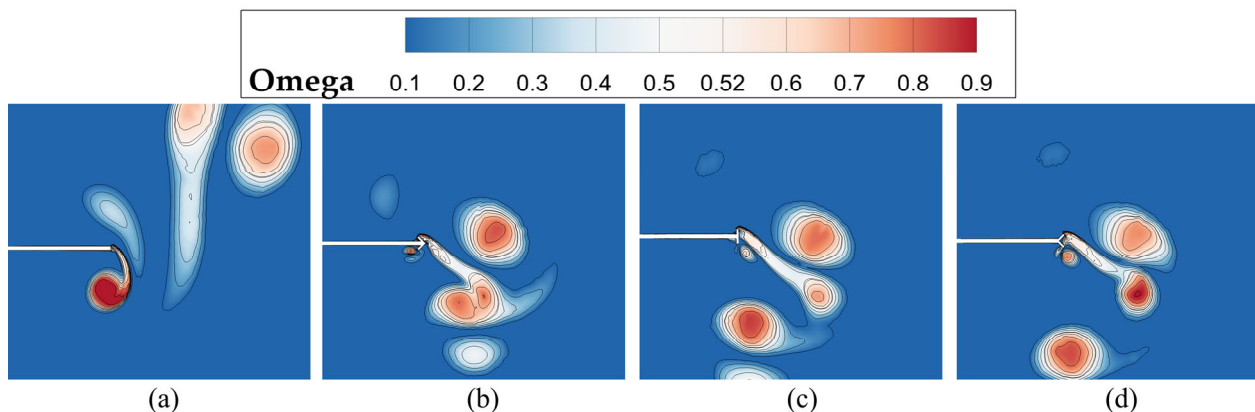


Figure 11. Vorticity contours for heave plates with different salient edge angles at $t = 100$ s: (a) $\alpha = 0^\circ$; (b) $\alpha = 45^\circ$; (c) $\alpha = 90^\circ$; (d) $\alpha = 135^\circ$.

In the 0° edge configuration, there is no salient structure at the plate extremity. The near-wall fluid, accelerating along the radial direction of the plate, undergoes a relatively thorough flow separation upon crossing the sharp edge. As the flow develops, the separated shear layer becomes fully unstable and rolls up, forming a large-scale primary shed vortex with the highest core Ω value. During the motion of the heave plate, the generation, periodic shedding, and viscous dissipation of large-scale vortices constitute the core mechanism for mechanical energy dissipation in the flow field. The unobstructed flow separation mode of the 0° plate highly promotes the generation and dissipation efficiency of large-scale vortices, providing a fundamental flow mechanism perspective on why this configuration exhibits the highest damping coefficient (C_d).

Upon the introduction of inward salient edges, the development of the primary vortices in the flow field is subjected to varying degrees of interference and suppression, resulting in a consequent reduction in energy dissipation capacity. In the 45° configuration, the acute-angled groove at the plate extremity impedes the radial acceleration of the fluid and suppresses the full roll-up of the separated shear layer. Consequently, both the scale and the core Ω value of the primary shed vortex are significantly lower than those in the 0° configuration. In the 90° configuration, the vertically protruding baffle at the extremity similarly obstructs radial fluid acceleration and restricts the expansion of the separated shear layer. As a result, the scale and intensity of the primary shed vortex are markedly attenuated compared to the baseline 0° case. The damping coefficient remains at the same level as that of the 45° configuration, with their respective curves exhibiting a high degree of coincidence. Fundamentally, both inward-protruding structures alter the flow separation mode at the plate extremities, thereby suppressing the generation of large-scale concentrated vortices and diminishing the total kinetic energy of vortex shedding. Ultimately, this leads to a significant decline in both the viscous dissipation capacity and the overall damping effect of the system.

As for the 135° configuration, despite the presence of a salient structure, it does not form a decelerating groove. Instead, the fluid can accelerate obliquely upward along the plate edge. The sharp tip similarly induces pronounced flow separation, accompanied by the generation of shed vortices. The peak Ω value within the primary vortex core reaches 0.9; however, the spatial extent of this high-value region is smaller than that observed in the 0° configuration. Correspondingly, its damping dissipation capacity is also slightly reduced relative to the 0° baseline.

Table 7 quantitatively presents the damping coefficient (C_d) and added mass coefficient (C_a) of the heave plates under various combinations of the dimensionless horizontal gap ratio (l/d) and salient edge angle (α) at $KC = 1.0$. As the horizontal gap ratio (l/d) increases, both C_d and C_a exhibit an overall decreasing trend across all edge angle configurations.

Table 7. Heave hydrodynamic coefficients under different arrangement schemes ($KC = 1.0$).

KC	l/d	Edge Angle ($^\circ$)	C_d	C_a
1.0	0	0	6.32	34.84
1.0	0.5	0	5.82	19.94
1.0	1	0	5.33	22.34
1.0	0	45	4.32	27.95
1.0	0.5	45	4.27	14.82
1.0	1	45	3.84	14.58
1.0	0	90	4.93	32.56
1.0	0.5	90	4.79	18.54
1.0	1	90	3.92	18.24
1.0	0	135	5.57	36.06
1.0	0.5	135	6.35	22.36
1.0	1	135	4.88	21.63

This trend is particularly evident for the added mass coefficient (C_a). At $l/d = 0$, C_a remains within a high range of 27.95 to 36.06. However, once horizontal gaps are introduced, C_a drops sharply to a lower range of 14.58 to 22.36.

To investigate the underlying physical mechanism, Figure 12 displays the pressure contours at $t = 106.25$ s for the 135° edge configuration under various horizontal gap ratios. With the presence of horizontal gaps, the area of the high-pressure region decreases significantly. This indicates that the gaps disrupt the planar geometric continuity of the heave plate and the confinement of the flow field. During the periodic heave motion, the high-pressure fluid originally accumulating on the upstream surface leaks through these gaps directly into the low-pressure zone. This flow leakage reduces the pressure difference drag between the upper and lower structural surfaces. Furthermore, it severely diminishes the water volume entrained by the heave plate which leads to the decrease in C_a .

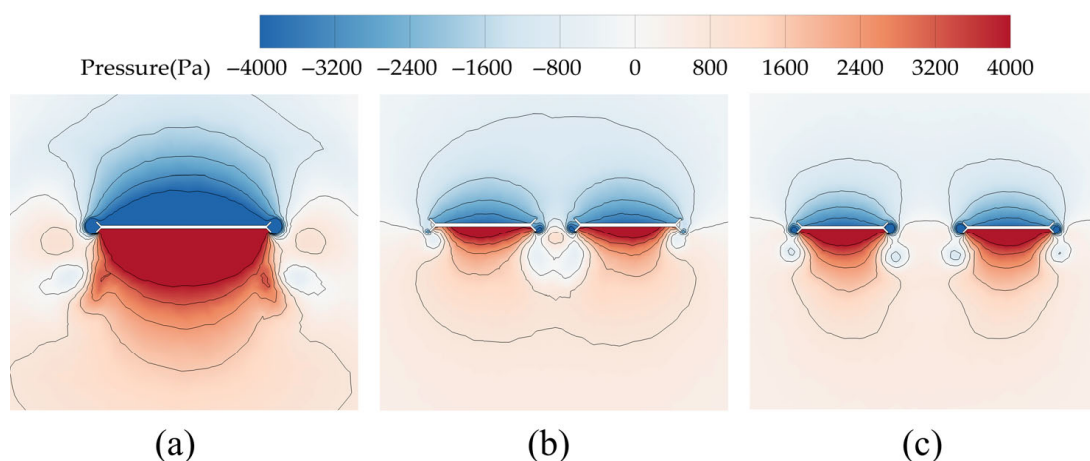


Figure 12. Pressure contours for the 135° salient edge configuration under different horizontal gap ratios at $t = 106.25$ s: (a) $l/d = 0$; (b) $l/d = 0.5$; (c) $l/d = 1.0$.

To more intuitively illustrate the effects of the edge angle and horizontal gap ratio on the hydrodynamic coefficients, two-parameter heatmaps detailing the percentage variation are plotted (Figures 13 and 14). These heatmaps utilize the C_d and C_a values at $l/d = 0$

and $\alpha = 0^\circ$ as the baselines. As observed from the heatmaps, at $KC = 0.2$ and 0.6 , the configuration with $l/d = 0$ and $\alpha = 0^\circ$ exhibits the maximum damping coefficient (C_d). At $KC = 1.0$, the configuration with $l/d = 0.5$ and $\alpha = 135^\circ$ achieves the maximum C_d . The minimum C_d values are consistently concentrated in two configurations: ($l/d = 1.0, \alpha = 45^\circ$) and ($l/d = 1.0, \alpha = 90^\circ$).

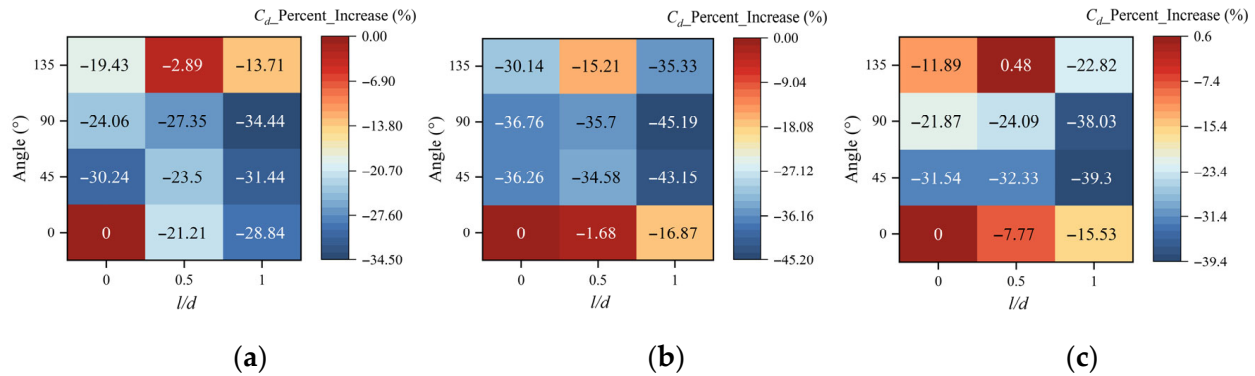


Figure 13. Percentage variation heatmaps of the damping coefficient (C_d) with respect to the edge angle and horizontal gap ratio (l/d): (a) $KC = 0.2$; (b) $KC = 0.6$; (c) $KC = 1.0$.

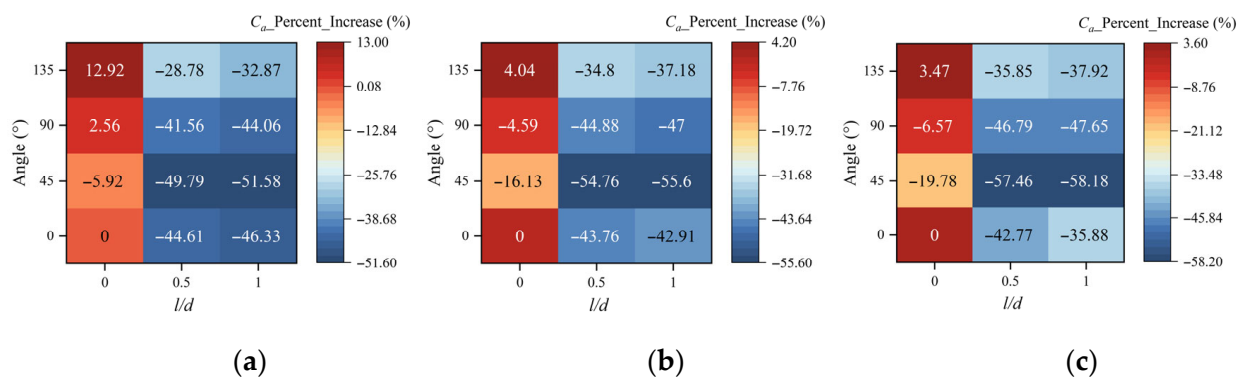


Figure 14. Percentage variation heatmaps of the added mass coefficient (C_a) with respect to the edge angle and horizontal gap ratio (l/d): (a) $KC = 0.2$; (b) $KC = 0.6$; (c) $KC = 1.0$.

Regarding the distribution characteristics of the added mass coefficient (C_a), the horizontal gap ratio (l/d) plays a decisive role. Once horizontal gaps are introduced to the structure, the C_a values for any given edge angle experience a substantial decrease compared to the baselines.

Comprehensive analysis indicates that although the ($l/d = 0.5, \alpha = 135^\circ$) configuration achieves a marginal extreme gain of +0.48% in C_d relative to the baseline, its C_a plummets by 35.82%. This makes it impractical for actual engineering applications. In contrast, while the ($l/d = 0, \alpha = 135^\circ$) configuration experiences a 19.43% reduction in damping, it gains a 12.92% increase in the added mass coefficient. This increase helps prolong the natural heave period of the system, effectively bypassing the wave periods associated with peak energy. Therefore, from the perspective of comprehensive hydrodynamic benefits for suppressing the overall heave motion of the floating platform, the configuration with $\alpha = 135^\circ$ and $l/d = 0$ is the optimal design within the parameter space of this study.

5.2.2. Effects of Plate Spacing Ratio and Size Difference of the Three-Layer Heave Plate

Figure 15 details the evolution of the damping coefficient (C_d) with the KC number (ranging from 0.2 to 1.0) for triple-layer heave plates. This evolution is presented under combined configurations of three different size differences ($\Delta d = -10$ m, 0 m, and 10 m) and

various spacing ratios (L/D). Overall, across the entire investigated parameter space of size differences and spacing ratios, C_d decreases as the KC number increases. This trend is highly universal. It remains substantially unchanged by the introduction of plate size differences.

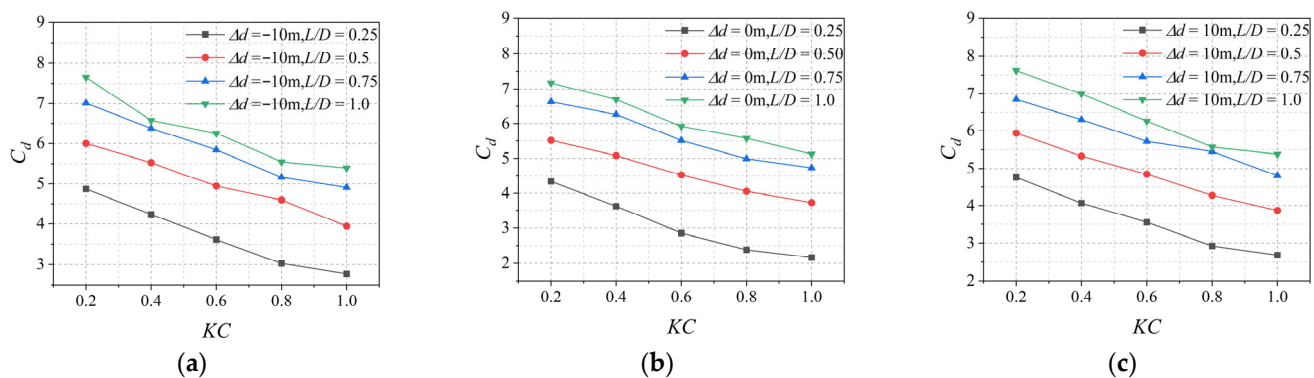


Figure 15. Variation in the damping coefficient C_d with KC number for heave plates with different spacing ratios (L/D) under varying size differences (Δd): (a) $\Delta d = -10$ m; (b) $\Delta d = 0$ m; (c) $\Delta d = 10$ m.

Under given conditions of size difference and KC number, C_d increases significantly as the spacing ratio (L/D) increases. Taking the specific condition of $\Delta d = -10$ m and $KC = 0.6$ as an example, when the spacing ratio is at its minimum ($L/D = 0.25$), C_d is only around 3.6. However, when L/D increases to 1.0, C_d surges to approximately 6.2.

Figure 16 illustrates the variation in the added mass coefficient (C_a) with the KC number for triple-layer heave plates under identical conditions. Across all size differences and spacing ratios, the added mass coefficient (C_a) exhibits a monotonically increasing trend as the KC number increases.

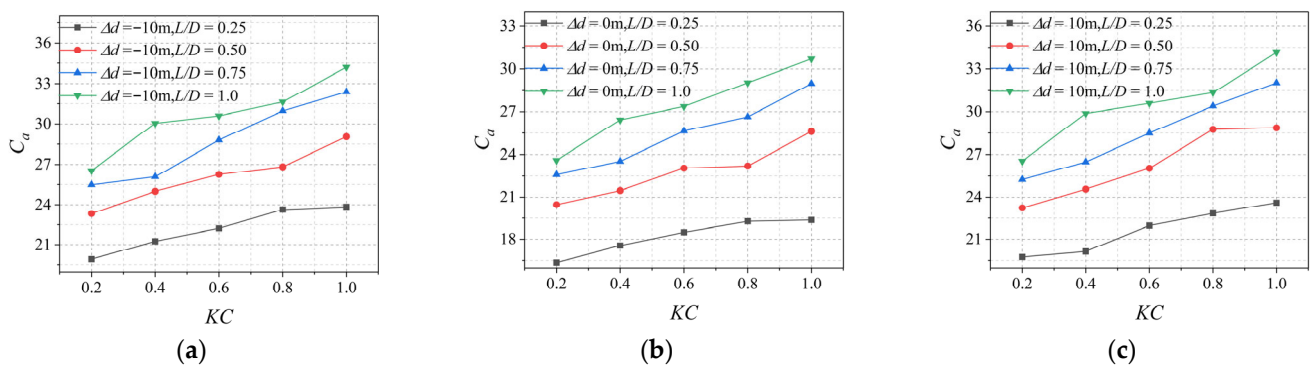


Figure 16. Variation in the added mass coefficient C_a with KC number for heave plates with different spacing ratios (L/D) under varying size differences (Δd): (a) $\Delta d = -10$ m; (b) $\Delta d = 0$ m; (c) $\Delta d = 10$ m.

A vertical comparison of the plotted curves reveals the profound impact of the spacing ratio (L/D) on the added mass coefficient. A smaller spacing ratio induces intense spatial interference. This decelerates the inter-plate fluid velocity and reduces both the generation of edge vortices and the disturbance to the flow field. Consequently, both C_d and C_a remain at relatively low values. As L/D increases, this inter-plate interference weakens. Sufficient spatial clearance allows each plate layer to interact more fully with the surrounding static fluid through shearing and squeezing effects. This substantially enhances the overall energy dissipation capacity of the system. Simultaneously, as the plate spacing increases, the volume of fluid “locked” in the intermediate region between adjacent plates also expands. This leads to a concurrent increase in both C_d and C_a .

To further evaluate and verify the “edge misalignment effect” under various plate size differences, two new mismatched configurations are introduced: $\Delta d = -20$ m and

$\Delta d = 20$ m. These correspond to top, middle, and bottom plate diameters of 20 m, 40 m, and 60 m, and 60 m, 40 m, and 20 m, respectively. Table 8 systematically presents the damping coefficient (C_d) and added mass coefficient (C_a) of the triple-layer heave plates. These values are provided for combined configurations of different dimensionless spacing ratios and size differences at $KC = 1.0$.

Table 8. Heave hydrodynamic coefficients under different arrangement schemes ($KC = 1.0$).

KC	Δd (m)	L/D	C_d	C_a
1.0	−20	0.25	4.06	31.52
1.0	−10	0.25	2.77	23.85
1.0	0	0.25	2.16	19.37
1.0	10	0.25	2.68	23.60
1.0	20	0.25	4.06	31.53
1.0	−20	0.5	4.61	34.82
1.0	−10	0.5	3.94	29.03
1.0	0	0.5	3.73	25.61
1.0	10	0.5	3.87	28.85
1.0	20	0.5	4.68	35.41
1.0	−20	0.75	5.26	37.28
1.0	−10	0.75	4.91	32.40
1.0	0	0.75	4.73	28.99
1.0	10	0.75	4.81	32.04
1.0	20	0.75	5.23	37.25
1.0	−20	1.0	5.70	38.77
1.0	−10	1.0	5.40	34.19
1.0	0	1.0	5.12	30.70
1.0	10	1.0	5.38	34.14
1.0	20	1.0	5.68	38.71

Following the addition of these two configurations, it is evident that the impact of the spacing ratio on the added mass and damping coefficients is independent of the size difference. Under various size difference conditions, both C_d and C_a increase as the spacing ratio increases. To deeply analyze the fluid dynamic mechanisms by which the spacing ratio influences these coefficients, pressure contours at $t = 106.25$ s (Figure 17) and velocity contours at $t = 100$ s (Figure 18) are presented for the configuration with a size difference of -20 m. The velocity contours display the velocity isolines and streamlines near the middle plate. The pressure contours display the pressure isolines.

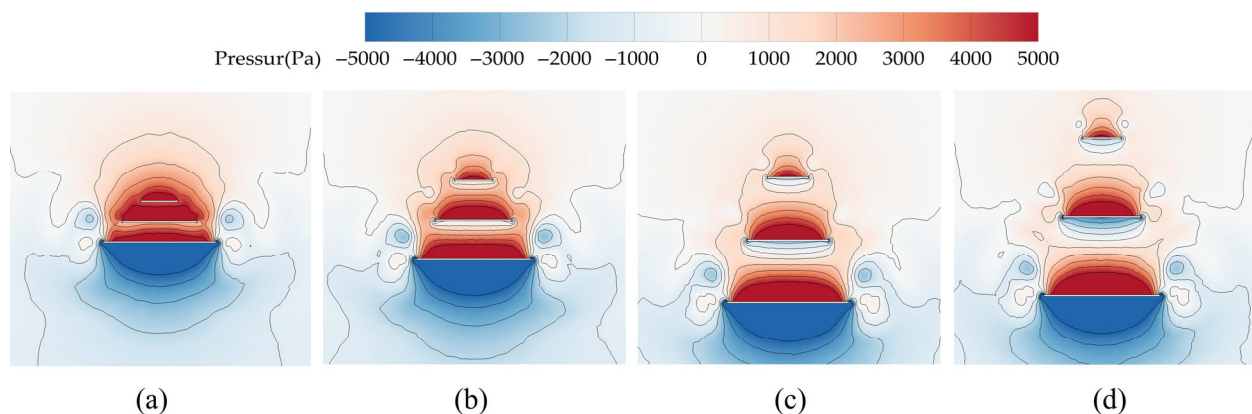


Figure 17. Pressure contours of the three-layer heave plate with a size difference of $\Delta d = -20$ m under different dimensionless spacing ratios at $t = 106.25$ s: (a) $L/D = 0.25$; (b) $L/D = 0.50$; (c) $L/D = 0.75$; (d) $L/D = 1.0$.

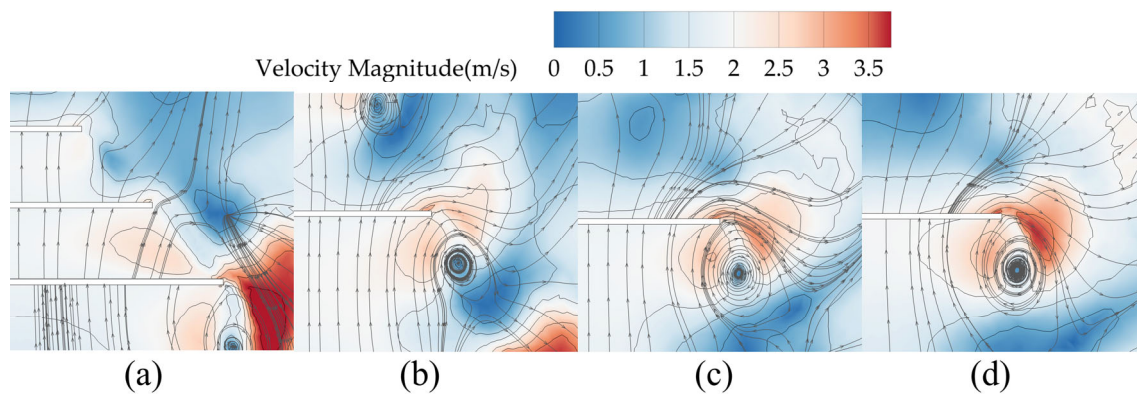


Figure 18. Velocity contours of the three-layer heave plate with a size difference of $\Delta d = -20$ m under different dimensionless spacing ratios at $t = 100$ s: (a) $L/D = 0.25$; (b) $L/D = 0.50$; (c) $L/D = 0.75$; (d) $L/D = 1.0$.

The pressure contours indicate that intense flow shielding effects occur among the plates under small spacing conditions. The high-pressure regions are almost entirely distributed on the upstream surfaces of the heave plates. Meanwhile, the low-pressure regions are concentrated behind the bottom plate. The pressure gradient across the upper and lower surfaces of the middle plate is extremely gradual. It fails to establish an effective pressure difference. Under these conditions, the triple-layer plates function approximately as a single structure with an enormous thickness. This limits the enhancement of the overall pressure difference drag.

As the plate spacing increases, the spatial flow interference is effectively broken. Ex-ternal fluid enters the inter-plate regions. Consequently, pressure difference zones gradually emerge on the upper and lower surfaces of each plate layer. The superposition of the pressure difference drag from each plate explains the increase in the total structural drag with larger spacing from a statics perspective.

The velocity contours indicate that at a spacing ratio of 0.25, the inter-plate fluid velocity is low. The external streamlines almost entirely predominantly bypass the periphery of the entire triple-layer structure. This demonstrates a lack of relative sliding in the inter-plate fluid. Consequently, it fails to generate significant viscous friction dissipation. When the spacing is sufficiently enlarged, fluid enters the inter-plate gaps. As the fluid flows past the edge of each plate layer, it triggers flow separation and edge-shedding vortices.

As observed in Figure 18b, although the increased spacing enables the middle plate to generate a wake and edge-shedding vortices, these still interfere with the upflow from the bottom plate. The interference between the wake and the upflow creates a stagnant water mass near the vortex center. This stagnant mass hinders the further development of the shedding vortices and attenuates energy dissipation. Further increasing the spacing weakens this interference. The contours reveal that the wake velocity increases with larger spacing. This indicates enhanced local velocity gradients and fluid shear forces, thereby explaining the increase in the damping coefficient (C_d) with larger spacing.

Figure 19 displays the transient Ω vortex contours of the triple-layer heave plates for different plate spacing ratios L/D at $t = 100$ s, specifically for the case with a size difference of $\Delta d = -20$ m. It is evident from the contours that the plate spacing ratio serves as the core parameter governing the generation, development, and inter-plate wake interference of edge vortices. As L/D increases from 0.25 to 1.0, the spatial shielding effect of the inter-plate flow field continuously attenuates, leading to more pronounced flow separation at the edges of each plate. Consequently, the quantity, scale, and core Ω values of the shed vortices exhibit a significant upward trend. These microscopic vortex evolution characteristics are highly consistent with the macroscopic trends discussed previously,

where the damping coefficient (C_d) and added mass coefficient (C_a) increase with L/D . The specific flow mechanisms are analyzed as follows:

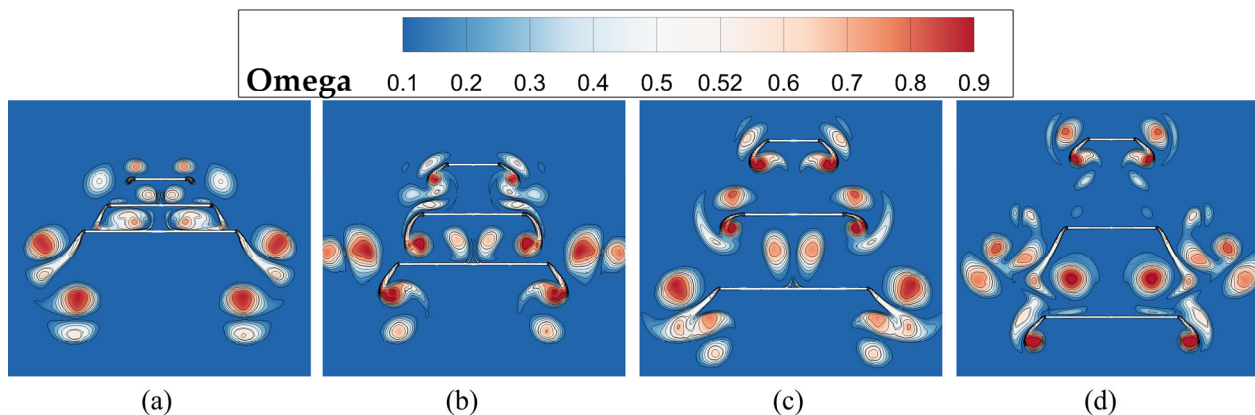


Figure 19. Vorticity contours of the three-layer heave plate with a size difference of $\Delta d = -20$ m under different dimensionless spacing ratios at $t = 100$ s: (a) $L/D = 0.25$; (b) $L/D = 0.50$; (c) $L/D = 0.75$; (d) $L/D = 1.0$.

At $L/D = 0.25$, the inter-plate gaps are extremely narrow, resulting in intense flow shielding and interference effects among the three plates. Vortex structures are generated only at the outer edges of the top and bottom plates, while the edges of the middle plate fail to trigger any significant flow separation. The minimal spacing forces the fluid between the plates to heave synchronously with the structure, resulting in a lack of relative slip and velocity gradient between the layers, which strongly inhibits the instability and roll-up of the shear layer. Due to the severe deficit in the generation and viscous dissipation of large-scale vortices in the flow field, both C_d and C_a plunge to their lowest values across the entire parameter range. This strongly supports the observation from the previous velocity contours that the “external streamlines predominantly bypass the structural envelope.”

At $L/D = 0.5$, the inter-plate flow interference is slightly mitigated, allowing external fluid to penetrate the gaps and enhancing local flow separation effects. The scales of the shed vortices at the edges of the top and bottom plates expand noticeably, with core Ω values jumping to 0.7–0.8. Distinct shed vortices peel off from the edges of the middle plate for the first time, though their scale and intensity remain constrained by the narrow space. Crucially, the wake of the upper plate collides and interferes with the upwash flow of the middle plate, restricting the vortex structures from extensively evolving into the far field. This interference mechanism aligns with the previous conclusion that “stagnant water masses hinder the development of shed vortices,” elucidating the underlying reason why the hydrodynamic coefficients in this configuration, despite improving relative to the $L/D = 0.25$ case, remain restricted.

At $L/D = 0.75$, the further expansion of the spacing substantially diminishes the inter-plate wake interference effect. At this stage, sufficient flow separation is triggered at the edges of each plate, giving rise to independent and structurally intact shed vortices. The scale and structural compactness of the primary vortices at the edges of all three plates are significantly enhanced, while the mutual interference between inter-plate vortices has degenerated into mild wake induction. Because each plate can undergo momentum exchange independently, the generation and outward shedding of large-scale vortices become the dominant mechanisms for energy dissipation within the flow field. Simultaneously, the widened inter-plate gaps increase the volume of the fluid mass effectively entrained and trapped by the structure, thereby driving up both C_d and C_a .

At $L/D = 1.0$, the inter-plate flow interference essentially dissipates, and the flow separation at the edges of the three plates reaches a highly developed state. Each layer generates massive, highly compact, concentrated shed vortices with core Ω values reaching up to 0.9. These primary vortices develop fully in the streamwise direction, without exhibiting any obvious inter-plate collision or fragmentation. This largely independent mechanism of periodic generation and shedding of intense vortices substantially enhances the mechanical energy dissipation efficiency of the flow field. Furthermore, the expansive physical space between the plates increases the fluid mass entrained by the strong vortices; consequently, both C_d and C_a under this configuration reach their peak values across the entire parameter matrix.

An analysis of the two newly introduced configurations reveals the impact of the size difference on the hydrodynamic coefficients. Both the damping and added mass coefficients increase with the absolute value of the plate size difference. The fundamental distinction among the various size differences lies in the vertical distribution of the plates, specifically the geometric misalignment effect of the multi-layer plate edges. To investigate the fluid dynamic mechanisms by which the size difference influences the damping coefficient, velocity contours (Figure 20) are presented for heave plates with varying size differences at a spacing ratio of 1.

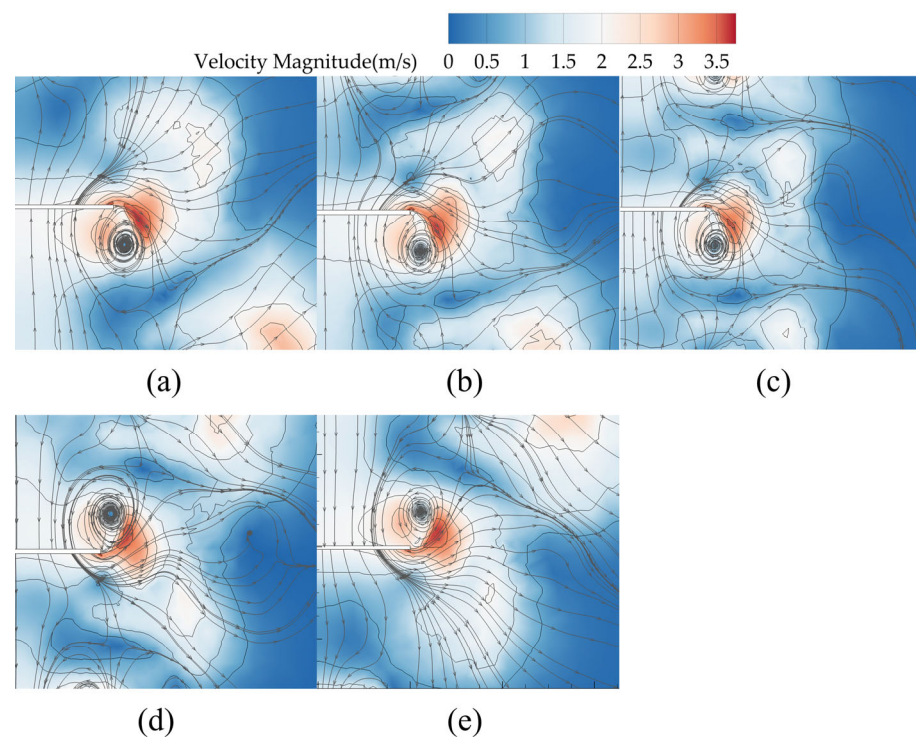


Figure 20. Velocity contours of the three-layer heave plate with a spacing ratio of $L/D = 1.0$ under different size differences at $t = 100$ s: (a) $\Delta d = -20$ m; (b) $\Delta d = -10$ m; (c) $\Delta d = 0$ m; (d) $\Delta d = 10$ m; (e) $\Delta d = 20$ m.

At a size difference of $\Delta d = 0$ m, the vertical projected areas of the triple-layer plates substantially overlap. During periodic heave motion, the wake generated by the upper plate interferes with the upflow from the lower plate. This interference creates low-velocity zones. It reduces both the wake velocity and the scale of the edge-shedding vortices, thereby attenuating the overall energy dissipation. When a size difference exists, the edges of the upper and lower plates exhibit vertical geometric misalignment. This misalignment mitigates the interference between the upflow and the wake. It consequently increases the wake velocity and the scale of the shedding vortices. This results in a higher damping

co-efficient (C_d) compared to the equal-size configuration, which suffers from excessive projection overlap.

Furthermore, forced heave motion is a simple harmonic oscillation. During this periodic reciprocating process, the stepped misaligned edges expose a larger upper surface area of the heave plate to the fluid. This enhances the fluid entrainment effect of the structure. Simultaneously, configurations with size differences drag and disturb more external fluid via these misaligned edges. The misalignment effect expands the displaced water volume. This ultimately leads to an increase in the added mass coefficient (C_a).

Additionally, the table reveals that configurations with identical absolute size differences exhibit highly consistent C_d and C_a values. This indicates that within the investigated spacing ratio range, the magnitude of the size difference (i.e., the misaligned area or effective frontal staggered area) is the dominant factor determining the added mass. Conversely, the direction of the misalignment demonstrates clear symmetry in its contribution to the total inertial force.

Figure 21 illustrates the transient Ω vortex contours of the triple-layer heave plates corresponding to different plate size differences at $t = 100$ s under the condition of a plate spacing ratio of $L/D = 1.0$. As observed from the contours, the vertical misalignment of the edges induced by the plate size difference is the core parameter governing the intensity of flow interference and the development of vortex structures in multi-layer plates. As the absolute value of the size difference $|\Delta d|$ increases from 0 m to 20 m, the interference effect between the wake of the upper plate and the upwash flow of the lower plate continuously weakens, resulting in increasingly sufficient flow separation at the edges of each plate layer. The scale, core Ω values, and the spatial extent of the rotation-dominated regions throughout the flow field all exhibit a significant increasing trend. This is highly consistent with the macroscopic trends discussed previously, where both the damping coefficient C_d and added mass coefficient C_a climb synchronously with the increase of $|\Delta d|$. The specific flow mechanisms are analyzed as follows:

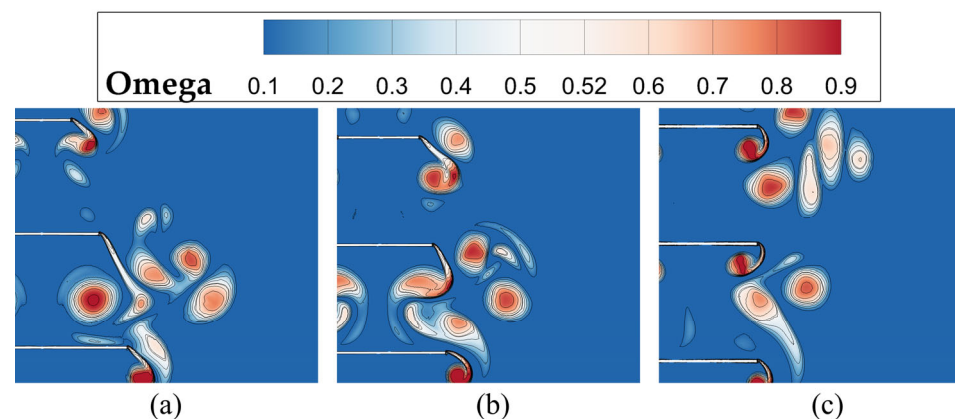


Figure 21. Vorticity contours of the three-layer heave plate with a spacing ratio of $L/D = 1.0$ under different size differences at $t = 100$ s: (a) $\Delta d = -20$ m; (b) $\Delta d = -10$ m; (c) $\Delta d = 0$ m.

At $\Delta d = 0$ m, the vertical projections of the edges of the three plates overlap completely. During the periodic heave motion, the shed wake vortices from the upper plate interfere with the upwash flow from the lower plate, directly causing the vortex structures to shrink and hindering their full development. The flow separation at the edges of the middle plate is severely suppressed by the strong inter-plate interference, yielding the smallest spatial extent of large-scale rotation-dominated regions in the entire flow field. This characteristic fundamentally explains why the uniform-size configuration yields the lowest hydrodynamic coefficients from a flow field perspective. The inter-plate interference

undermines the sufficiency of flow separation, drastically reducing the generation efficiency and viscous dissipation capacity of large-scale vortices. Simultaneously, the coincident edge structures suppress the fluid entrainment effect of the plates, ultimately causing both C_d and C_a to reside at their lowest levels across the entire parameter range.

At $\Delta d = -10$ m, distinct vertical geometric misalignment emerges at the plate edges. The interference effect between the wake and upwash flow of adjacent plates is significantly mitigated, substantially weakening the inter-plate flow disturbance. More sufficient flow separation occurs at the edges of each plate, forming independent and intact shed vortex structures. The scale of the vortices and their developmental extent in the streamwise direction are significantly larger than those of the uniform-size baseline configuration. The edge misalignment allows each plate layer to exchange momentum independently with the surrounding fluid, enabling a more thorough process of generation, shedding, and viscous dissipation of large-scale vortices. Meanwhile, the stepped misaligned edges enlarge the effective flow-facing area of the structural body and enhance the entrainment effect on the surrounding fluid. Therefore, both C_d and C_a of this configuration are markedly elevated compared to the uniform-size configuration.

At $\Delta d = -20$ m, the vertical misalignment of the plate edges reaches its maximum, and the flow interference between the upper and lower plates is substantially weakened, allowing the edge flow separation of the three plates to reach a more fully developed state. Concentrated shed vortices with intact structures are generated at the edges of each plate. These vortex structures develop fully along the streamwise direction, forming multiple sets of continuous, large-scale rotation-dominated zones. The quantity, scale, and intensity of the vortex structures in the entire flow field reach their peaks. The generation and dissipation of large-scale intense vortices magnify the mechanical energy dissipation efficiency of the flow field. Simultaneously, the large misaligned structure increases the effective flow-facing area and the fluid entrainment range of the plates, leading to the maximum added fluid volume among all conditions. Consequently, both C_d and C_a for this configuration achieve their peak values within the entire parameter range.

To quantitatively evaluate the comprehensive enhancement effects of the two factors—plate size difference and spacing ratio—on the hydrodynamic performance of the heave plates, two-parameter heatmaps detailing the percentage variation are plotted (Figures 22 and 23). These heatmaps utilize the hydrodynamic coefficients of the ($L/D = 0.25$, $\Delta d = 0$ m) configuration as the baseline.

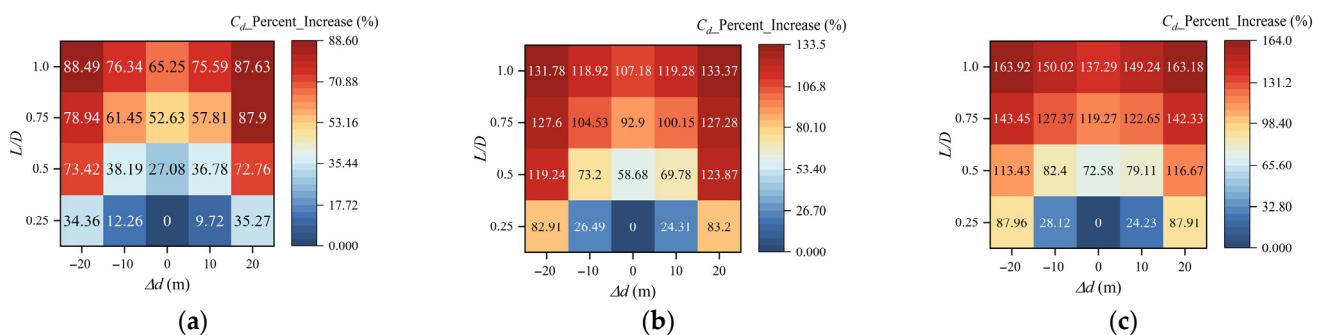


Figure 22. Percentage variation heatmaps of the damping coefficient (C_d) with respect to the size difference (Δd) and dimensionless spacing ratio (L/D): (a) $KC = 0.2$; (b) $KC = 0.6$; (c) $KC = 1.0$.

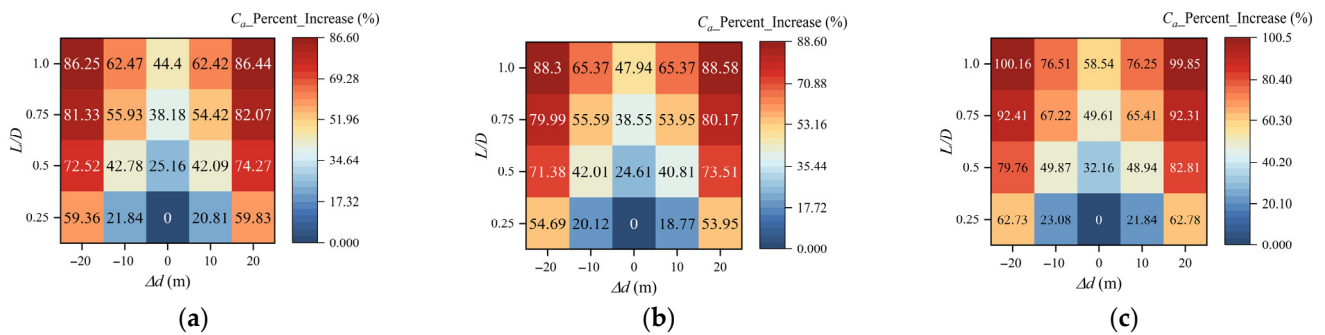


Figure 23. Percentage variation heatmaps of the added mass coefficient (C_a) with respect to the size difference (Δd) and dimensionless spacing ratio (L/D): (a) $KC = 0.2$; (b) $KC = 0.6$; (c) $KC = 1.0$.

The color gradient evolution in the heatmaps clearly demonstrates that both the damping coefficient (C_d) and the added mass coefficient (C_a) exhibit a significant monotonically increasing trend with the dimensionless spacing ratio (L/D). This trend occurs regardless of whether a size difference exists in the system. Simultaneously, under all spacing ratio conditions, the hydrodynamic coefficients surge as the absolute value of the size difference ($|\Delta d|$) increases. They exhibit highly consistent physical symmetry along the central axis of $\Delta d = 0$ m on the heatmaps.

Based on the quantitative analysis of the aforementioned two-parameter variation heatmaps, the triple-layer stepped arrangement with $L/D = 1.0$ and a size difference of ± 20 m consistently occupies the apex of the entire parameter matrix. This superiority applies to both the damping dissipation capacity and the fluid entrainment capacity.

Therefore, the configuration with $L/D = 1.0$ and $\Delta d = \pm 20$ m is the optimal design among the triple-layer plate arrangements investigated in this study. For practical engineering applications, designers can flexibly select either the +20 m or −20 m configuration based on the center of gravity distribution or mooring connection requirements at the platform base.

5.3. Empirical Formulas for Hydrodynamic Coefficients of Spatially Mismatched Heave Plates

Due to intense spatial flow interference and edge vortex shedding, the hydrodynamic characteristics of multi-layer heave plates with size differences are highly complex. Effective empirical formulas for estimating the damping and added mass coefficients of such stepped arrangements are currently lacking. Therefore, this study proposes an equivalent mapping method. This method transforms the complex multi-parameter coupling effects into quantifiable corrections to a baseline model.

To ensure the dimensional consistency and universality of the empirical formula, a dimensionless misalignment ratio δ is introduced as $\delta = |\Delta d|/D$, where D is the characteristic width of the baseline plate. The previous mechanistic analysis indicates that the direction of the stepped misalignment (positive or inverted steps) exhibits symmetric effects on the total hydrodynamic forces. Therefore, the absolute value of the size difference ($|\Delta d|$) is adopted in the calculations.

Based on the equivalent mapping theory, the total hydrodynamic coefficient (C_{total}) of the triple-layer stepped heave plates is decoupled into two components: the baseline hydrodynamic coefficient without misalignment (C_{base}) and the misalignment amplification factor (K_{mis}). Their relationship is expressed as follows:

$$C_{total} = C_{base} \times K_{mis} \quad (16)$$

In the absence of a size difference ($\delta = 0$), C_{base} is primarily governed by the KC number and the dimensionless spacing ratio (L/D). By performing a multiple nonlinear regression

analysis on the baseline CFD data and accounting for the interaction between KC and L/D , C_{base} is fitted into the following polynomial function:

$$C_{base} = a_0 + a_1 KC + a_2 (L/D) + a_3 KC \cdot (L/D) + a_4 KC^2 + a_5 (L/D)^2 \quad (17)$$

Subsequently, to map the complex staggered effects onto the baseline model, the misalignment factor (K_{mis}) is introduced to quantitatively characterize the synergistic enhancements induced by edge misalignment. Utilizing KC , L/D , and δ as independent variables, the relational expression for K_{mis} is derived as follows:

$$K_{mis} = 1 + c_1 \delta + c_2 \cdot \delta \cdot (L/D) + c_3 \cdot \delta \cdot KC + c_4 \delta^2 \quad (18)$$

To construct a high-quality fitting dataset, this study extracted all CFD hydrodynamic results across the entire parameter matrix, encompassing all simulated combinations of KC numbers, spacing ratios, and size differences. Specifically, the KC numbers were set to 0.2, 0.4, 0.6, 0.8, and 1.0; the spacing ratios were selected as 0.25, 0.5, 0.75, and 1.0; and the misalignment ratios (δ) were assigned values of 0, 0.25, and 0.5. Consequently, a comprehensive fitting dataset comprising 100 data points was established. To determine the unknown parameters in the aforementioned formulas, a global iterative fitting was performed on the CFD data across all conditions using the nonlinear least squares method. The specific fitting coefficients for the damping coefficient (C_d) and the added mass coefficient (C_a) are summarized in Table 9.

Table 9. Fitting parameters and coefficients of determination for the empirical formulas.

Parameters	C_d	C_a
a_0	2.92	10.917
a_1	−3.997	3.17
a_2	9.158	21.349
a_3	0.143	6.219
a_4	1.122	−0.409
a_5	−4.158	−10.342
c_1	0.764	0.848
c_2	−1.421	−0.739
c_3	0.309	0.015
c_4	1.16	0.791

To intuitively evaluate the accuracy and reliability of the empirical formulas, Figure 24 presents a scatter plot comparing the CFD-simulated values of the hydrodynamic coefficients with the formula-predicted values. The solid red line in the figure represents the ideal state of a perfectly accurate prediction (Exact match), while the two dashed black lines form the $\pm 10\%$ error bands. As shown in Figure 20, the coefficients of determination (R^2) for the C_d and C_a empirical formulas reach 0.9287 and 0.9747, respectively. Furthermore, this study incorporated residual analysis and computed statistical evaluation metrics, including the root mean square error (RMSE) and the mean absolute percentage error (MAPE). The detailed results are summarized in Table 10. As indicated by the analysis, the MAPE of the C_a prediction formula is merely 2.30%, with the maximum relative error strictly controlled within 5%, demonstrating a robust approximation accuracy. Meanwhile, the MAPE of the C_d prediction formula is only 5.12%. This corroborates, from both statistical and visual perspectives, that the equivalent mapping model achieves an exceedingly high fitting accuracy and effectively captures the nonlinear synergistic effects between the inter-plate spacing and the size difference.

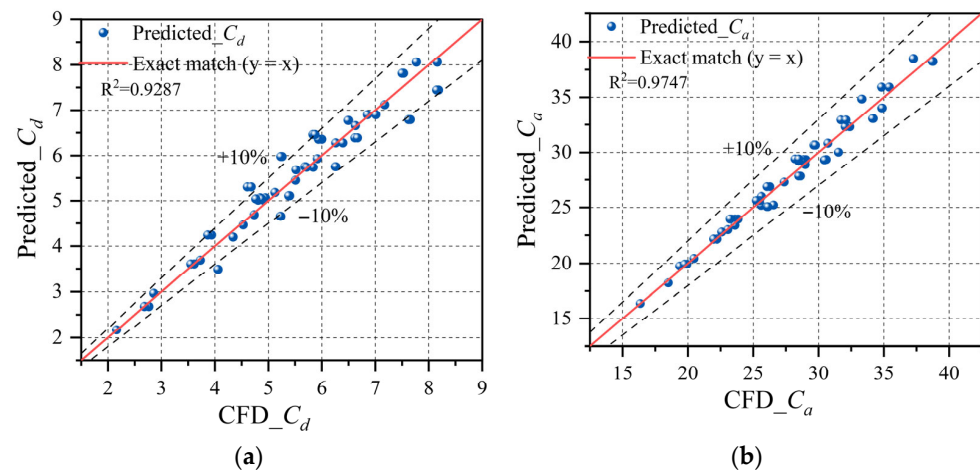


Figure 24. Scatter plots comparing the CFD-simulated values and the formula-predicted values of the hydrodynamic coefficients: (a) damping coefficient (C_d); (b) added mass coefficient (C_a).

Table 10. Statistical evaluation metrics for the fitting accuracy of empirical formulas.

Evaluation Metrics	C_d	C_a
Coefficient of Determination (R^2)	0.9287	0.9747
Root Mean Square Error (RMSE)	0.3787	0.8259
Mean Absolute Error (MAE)	0.2847	0.6799
Mean Absolute Percentage Error (MAPE)	5.12%	2.30%

Finally, it is crucial to clarify the nature and applicable boundaries of these empirical formulas. The proposed equations represent a mathematical correlation derived specifically from the current CFD dataset within the investigated parameter space ($0.2 < KC < 1.0$, $0.25 < L/D < 1.0$, and $0 < \delta < 0.5$). While the fitting performance within this dataset is robust, it currently lacks independent out-of-sample experimental validation. Consequently, these formulas should not be treated as a universally predictive model. Beyond the specified parameter regime, the vortex shedding modes and fluid interference mechanisms may undergo nonlinear abrupt changes. Therefore, these formulas primarily serve to quantitatively summarize the synergistic effects of inter-plate spacing and size difference within the specific scope of this study. Extreme caution must be exercised if extrapolating them to scenarios involving larger oscillation amplitudes or greater edge misalignments.

6. Conclusions

A series of single-layer heave plates with various salient edge structures and horizontal gaps, along with triple-layer heave plates featuring different spacing ratios and size differences, are proposed in this paper. Forced oscillation simulations were conducted on these configurations to comparatively analyze the damping coefficient (C_d) and added mass coefficient (C_a) and to investigate their underlying mechanisms. The main conclusions are summarized as follows:

- (1) For single-layer heave plates, the salient edge angle significantly influences hydrodynamic performance. The 0° edge configuration maintains high hydrodynamic coefficients across all tested conditions. Meanwhile, the 135° outward-expanding edge configuration achieves the maximum damping (C_d) and added mass (C_a) coefficients under specific motion parameters.
- (2) For triple-layer heave plates, increasing the dimensionless spacing ratio (L/D) effectively mitigates the spatial interference between plates, thereby enhancing the overall hydrodynamic coefficients of the structure. Introducing size differences creates a

stepped misaligned arrangement. This misalignment design overcomes the wake shielding limitations inherent in the equal-size baseline plates, causing both C_d and C_a to increase as the absolute size difference ($|\Delta d|$) increases.

- (3) For multi-layer staggered configurations with size differences, this study proposes an equivalent mapping method. This method decouples the total hydrodynamic coefficient into the baseline hydrodynamic coefficient without misalignment (C_{base}) and the misalignment amplification factor (K_{mis}), which quantitatively characterizes the misalignment gain. Consequently, an empirical correlation model based on the KC number, L/D , and dimensionless misalignment ratio (δ) is established to quantitatively summarize the CFD results.

In summary, this study demonstrates that optimizing the geometric edge topology and adopting multi-layer spatial misaligned arrangements can effectively enhance the added mass and damping coefficients. For single-layer plates, the configuration with a 135° edge structure and no horizontal gap ($l/d = 0$) exhibits the optimal heave suppression effect. For triple-layer plates, the stepped arrangement with a large spacing ratio ($L/D = 1.0$) and a ± 20 m size misalignment demonstrates the optimal heave suppression performance.

However, this study solely investigates the hydrodynamic coefficients of the heave plates in the heave degree of freedom, without considering the multi-degree-of-freedom coupled motions typical in actual marine environments. Future work will investigate the multi-degree-of-freedom responses of floating platforms equipped with the proposed heave plates under wave excitation, with a particular focus on the pitch–heave coupling effect.

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Abbreviations

The following abbreviations are used in this manuscript:

C_a	Added mass coefficient
C_d	Damping coefficient
CFD	Computational Fluid Dynamics
KC	Keulegan–Carpenter
RANS	Reynolds-Averaged Navier–Stokes
SST	Shear Stress Transport
FFT	Fast Fourier Transform
TLP	Tension Leg Platform

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