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# Biomimetic Groove and Elliptical Bluffness Synergy for Enhanced Vortex-Induced Vibration Excitation

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**ABSTRACT:** This study investigates the passive amplification of aerodynamic excitation forces through coordinated bluff-body geometric modifications to quantify the vortex-induced vibration (VIV) energy harvesting potential of stationary cylinders in the laminar regime. Two-dimensional laminar simulations on fixed bodies isolate geometric effects from structural feedback. Circumferential biomimetic grooves are first optimised on a circular baseline at  $Re = 200$  using a Taguchi orthogonal array (L9), identifying groove amplitude as the dominant control parameter and selecting  $N = 24$ ,  $Amp = 5\%$  as the optimal configuration, which yields a 21% increase in the root-mean-square lift coefficient ( $C_{l,rms}$ ) and a 12.5% Strouhal number reduction relative to the smooth circular baseline. The optimal groove profile is coupled with vertically-oriented bluff elliptical bases at three aspect ratios ( $AR = 1.0, 0.75$ , and  $0.50$ ). Proper Orthogonal Decomposition (POD), wake fluctuation energy (WFE) mapping, and a simplified one-degree-of-freedom (1-DOF) structural projection model quantify the aerodynamic excitation power across all six configurations. Results reveal a geometric sweet spot at  $AR = 0.75$ , where the Grooved configuration achieves the highest projected aerodynamic excitation power ( $P_{est} = 0.5184$  W/m), corresponding to a 2.24-fold amplification of the available fixed-body forcing relative to the smooth circular baseline and outperforming the aggressively bluff Grooved  $AR = 0.50$  geometry. This counter-intuitive result is attributed to groove-to-boundary-layer interaction saturation: at  $AR = 0.50$ , the shortened streamwise body dimension suppresses the shear-layer tripping mechanism. POD analysis confirms high modal coherence at the optimum, with the first two modes capturing 96.2% of the total fluctuation energy, indicating forcing characteristics favourable for sustained VIV lock-in. A complementary Reynolds-number sensitivity analysis conducted at  $Re = 100$  and  $150$  further reveals that the groove contribution at  $AR = 0.50$  remains uniformly suppressed relative to  $AR = 0.75$  across the full laminar range, confirming that the saturation mechanism is a robust geometric feature rather than a Reynolds-specific artefact. However, the absolute geometric optimum is Reynolds-number dependent.  $AR = 0.50$  delivers stronger excitation at  $Re = 100$ , the two configurations converge at  $Re = 150$ , and  $AR = 0.75$  is the best-performing aspect ratio among those evaluated at  $Re = 200$ , indicating the onset of the boundary-layer saturation mechanism at  $Re = 150$ .

**KEYWORDS:** Vortex-induced vibration; biomimetic surface grooves; elliptical cylinder; wake aerodynamics; Taguchi optimization; CFD

## 1 Introduction

The accelerating global transition toward renewable and distributed energy systems has prompted significant research interest in extracting useful power from fluid-structure interaction phenomena, particularly from low-speed hydrokinetic currents in rivers, tidal channels, and shallow coastal ocean streams. While large-scale hydraulic turbines remain the dominant technology for hydrokinetic conversion, there exists

a rapidly expanding demand for compact, maintenance-free micro-energy harvesting systems designed to power autonomous Internet of Things (IoT) sensors, deep-ocean instrumentation, and remote wireless structural health monitoring nodes. In such deployment environments, including subsea pipelines, riverbed monitoring stations, and internal flow conduits, the available flow velocities are inherently extremely low ( $U_\infty < 0.1$  m/s), confining the operating Reynolds number strictly within the laminar regime ( $100 \leq Re \leq 500$ ) [1,2]. Among the fluid-structure interaction mechanisms available at these ultra-low flow speeds, Vortex-Induced Vibration (VIV) has emerged as one of the most promising physical principles for small-scale energy conversion [3,4]. As a fluid flows past a bluff body, alternating low-pressure vortices detach periodically from its upper and lower surfaces, forming a characteristic pattern known as the Kármán vortex street. These alternating vortices generate a periodic cross-flow lift force oscillating at the Strouhal frequency, which drives an elastically mounted structure into sustained transverse oscillations [5,6]. When the vortex shedding frequency approaches the natural frequency of the mechanical system, a condition termed lock-in or synchronization, oscillation amplitudes are substantially amplified, providing the large-displacement, sustained mechanical motion required for efficient energy extraction [7,8]. The fundamental engineering appeal of VIV energy harvesting lies in its entirely passive, self-sustaining character: no active control systems, no rotating machinery, and no external power input are required. The seminal VIVACE (Vortex Induced Vibration Aquatic Clean Energy) concept introduced by Bernitsas et al. [1] demonstrated that properly designed elastically mounted cylinders can achieve lock-in oscillations in marine currents as slow as 0.25 m/s, establishing the practical viability of the VIV paradigm. Investigating these mechanisms at low Reynolds numbers therefore constitutes not merely a theoretical computational simplification, but a highly realistic operating condition for a well-defined and growing class of next-generation micro-power generation devices.

Nature has long provided engineering inspiration for passive aerodynamic control strategies. Among the most extensively studied biomimetic surface modifications are circumferential longitudinal grooves and surface dimples, whose geometric principles draw directly from biological archetypes such as the ribbed epidermis of the cactus (*Cereus repandus*) and the sinusoidal leading-edge tubercles of the humpback whale (*Megaptera novaeangliae*) flipper [9,10]. The cactus spine-groove pattern has proven particularly effective as a passive boundary layer manipulation strategy: the convex ridges separating adjacent grooves act as localized tripping elements that promote turbulent mixing in the near-wall shear layer, effectively delaying the onset of flow separation and reducing the extent of the near-wake recirculation region [11]. The direct aerodynamic consequence is a substantial reduction in the mean pressure drag, a narrowing of the wake, and, crucially from the perspective of structural vibration, a suppression of the alternating lift force amplitude [12,13]. The numerical and experimental investigations have consistently confirmed these beneficial stabilizing effects: Babu and Mahesh [14] performed direct numerical simulations of flow past cactus-shaped cylinders at  $Re = 20, 100$ , and  $300$ , reporting drag reductions of up to 22% and lift coefficient reductions exceeding 47%, attributed to quiescent recirculating flow within the cactus cavities. Furthermore, Talley et al. [15] reported analogous drag reduction behavior across a range of groove wave numbers. The overwhelming consensus in the published literature therefore positions biomimetic surface grooves exclusively as a passive vibration-suppression technology, a fundamental paradigm that the present study deliberately, rigorously, and systematically inverts.

Independently of surface modifications, the fundamental cross-sectional geometry of a bluff body exerts decisive and well-characterized influence on the scale of the aerodynamic forces it generates. The smooth circular cylinder ( $AR = 1.0$ ) serves as the canonical reference geometry throughout the VIV literature [16]. Elliptical cross-sections parametrized by the aspect ratio  $AR = b/a$ , where  $b$  is the streamwise semi-axis and  $a$  is the cross-flow semi-axis, represent a systematically controllable class of bluff bodies with markedly richer



wake dynamics [17]. As the aspect ratio is reduced below unity while the frontal dimension  $a$  is held constant, the body presents an increasingly short and blunt streamwise profile to the oncoming flow. This enhanced bluntness compels the attached boundary layer to separate at a substantially earlier angular position from the forward stagnation point, elongating the free shear layer trajectory and enlarging the recirculation bubble in the near wake [18–20]. The expanded recirculation region is associated with a non-linear escalation of the mean pressure drag and, far more consequentially for VIV energy harvesting, a dramatic amplification of the fluctuating cross-flow lift force. For vertically-oriented bluff elliptical profiles, as adopted in the present study, this relationship is subject to a geometric limit: as the streamwise dimension becomes extremely short, the available boundary-layer development length diminishes and the shear-layer roll-up is constrained near the body surface, introducing a non-monotonic dependence of lift excitation on aspect ratio [21]. Furthermore, the Strouhal number does not vary monotonically with aspect ratio for the vertically-oriented geometry: the specific cross-sectional proportions govern the shear-layer formation frequency through a balance of separated flow length and vortex convection speed, which is examined in detail in the Section 3. These geometric effects make the present configuration particularly sensitive to the identification of an optimal aspect ratio, rather than simply minimising  $AR$  to maximise excitation [22,23].

Despite the substantial and parallel bodies of knowledge accumulated in biomimetic surface engineering and bluff-body aerodynamics, a critical and precisely defined gap persists at their intersection. Within the existing literature, grooved or textured surface modifications on cylindrical structures have predominantly been investigated as passive flow-control strategies for suppressing flow-induced vibrations and reducing structural loading [24–26]. Earlier studies on cactus-inspired and wavy cylinders primarily focused on the aerodynamic behaviour of these biomimetic surface modifications rather than on enhancing aerodynamic excitation for VIV energy harvesting applications [11,13,15]. Conversely, existing VIV energy harvesting research has concentrated almost exclusively on optimizing the mechanical and electromechanical transduction subsystem, including damping ratios, spring stiffnesses, piezoelectric coupling coefficients, and mass ratios, while treating the generator cylinder as a fixed, smooth, and circular geometric baseline [1,2,7,8,27]. No systematic computational optimization study has investigated whether biomimetic surface grooves, deliberately redesigned and parametrically optimized not for suppression but for amplification, can synergistically interact with a geometrically bluff elliptical base ( $AR < 1.0$ ) to maximize the available aerodynamic power for VIV energy harvesting. The potential synergistic interaction between optimized surface grooves and bluff elliptical cross-sections of varying aspect ratio, wherein the grooves perturb the separating shear layer and modulate vortex roll-up coherence while the elliptical bluntness governs the recirculation geometry and the available lift excitation magnitude, constitutes a relatively unexplored research area in passive aerodynamic excitation engineering. Critically, the interplay between groove spacing and boundary-layer thickness suggests that this synergy is not simply maximised by minimising the aspect ratio, but rather reaches an optimum at an intermediate bluntness level where the two geometric mechanisms are mutually reinforcing. The present study is explicitly motivated by, and systematically designed to address, this specific knowledge gap.

### **1.1 Objectives and Novelty of the Current Study**

This study aims to explore how passive geometric modifications can amplify coherent wake forcing, and to quantify the resulting VIV energy harvesting potential for bluff bodies with tailored cross-sections. A fully coupled fluid-structure interaction model would inevitably introduce nonlinear feedback between cylinder motion and wake dynamics, complicating the isolation of purely geometric effects. To avoid this confounding influence, a fixed-cylinder computational framework is adopted, enabling the available aerodynamic excitation capacity to be evaluated strictly as a function of body geometry.

To achieve this objective, a three-stage computational framework is developed and executed within the laminar flow regime that corresponds directly to realistic ultra-low-speed hydrokinetic deployment conditions.

- Stage I employs a Taguchi L9 orthogonal array at  $Re = 200$  to efficiently identify the optimal biomimetic groove wave number ( $N$ ) and amplitude ( $Amp$ ) that maximize the root-mean-square lift coefficient ( $C_{\ell,rms}$ ) on a circular baseline ( $AR = 1.0$ ).
- Stage II systematically couples the optimal groove profile with elliptical cross-sections of progressively decreasing aspect ratio ( $AR = 1.0, 0.75$ , and  $0.50$ ) at  $Re = 200$ , quantifying how geometric bluntness interacts with the biomimetic surface modification to maximise aerodynamic excitation forces and near-wake fluctuation energy (WFE) concentration.
- Stage III conducts a Reynolds-number sensitivity analysis at  $Re = 100$  and  $Re = 150$  for the two leading Grooved configurations ( $AR = 0.75$  and  $AR = 0.50$ ) and their smooth counterparts, establishing the robustness of the identified geometric optimum and the Reynolds-dependent activation of the boundary-layer saturation mechanism across the laminar deployment window.

The novelty of the present research comprises four distinct contributions:

- *Inverted biomimetic paradigm.* Unlike previous studies that employ biomimetic surface grooves exclusively for vibration suppression, the present work deliberately redesigns and optimises groove geometry to *amplify* cross-flow aerodynamic excitation forces for energy harvesting purposes.
- *Geometric synergy concept.* The coupled interaction between optimised surface grooves and bluff elliptical cross-sections of varying aspect ratio is systematically investigated, introducing the concept of geometric synergy to identify the optimal bluntness level at which the two geometric mechanisms are mutually reinforcing.
- *Multi-perspective energy harvesting quantification.* POD modal analysis, WFE mapping, spectral (PSD) characterization, mean surface pressure distribution, and a simplified one-degree-of-freedom (1-DOF) structural projection model are integrated to provide a comprehensive, physics-informed assessment of the projected aerodynamic excitation potential for VIV energy harvesting across all evaluated configurations.
- *Reynolds-dependent geometric optimum.* By extending the analysis to lower Reynolds numbers, the study demonstrates that the saturation of the groove contribution at  $AR = 0.50$  is a robust geometric feature of the laminar regime. In contrast, the absolute geometric optimum shifts from  $AR = 0.50$  at  $Re = 100$  to  $AR = 0.75$  at  $Re = 200$ , indicating the onset of the boundary-layer saturation mechanism at  $Re = 150$ .

## 2 Methodology

This section establishes the comprehensive computational and mathematical framework employed to evaluate the geometric amplification of aerodynamic excitation and the associated near-wake characteristics. To systematically isolate and quantify the impact of tailored geometric bluntness and biomimetic surface modifications without the confounding influence of structural feedback, a robust two-dimensional (2D) numerical methodology on fixed bodies is utilized throughout. The subsequent subsections detail: (i) the mathematical definition of the selected circular and elliptical geometries and the biomimetic groove parameterization; (ii) the governing fluid dynamics equations and the complete numerical setup within the laminar flow regime; (iii) the comprehensive independence studies and code validation procedures; and (iv) the theoretical foundations of both the Taguchi optimization method and Proper Orthogonal Decomposition (POD) modal analysis. This structured presentation is intended to ensure the full reliability, transparency, and reproducibility of all reported findings.

## 2.1 Geometric Modeling and Aerodynamic Formulations

The baseline circular and elliptical cross-sections are defined by the aspect ratio (AR):

$$AR = \frac{b}{a} \quad (1)$$

where  $a$  and  $b$  denote the full vertical (cross-flow) and horizontal (streamwise) axis lengths of the cross-section, respectively. Throughout this study,  $a$  is held strictly constant across all configurations, ensuring an identical frontal projection to the incoming flow. Accordingly,  $D = a$  serves as the reference length for all non-dimensional aerodynamic force and frequency calculations in the present study.

To achieve aspect ratios  $AR \leq 1.0$ , only  $b$  is systematically reduced. Three elliptical bases are evaluated, namely  $AR = 1.0$  (circular baseline),  $AR = 0.75$ , and  $AR = 0.50$ . Reducing  $b$  below  $a$  produces progressively shorter and more blunt streamwise bodies that impose increasingly severe adverse pressure gradients on the attached boundary layer, forcing earlier flow separation and broader near-wake recirculation regions. This deliberate geometric bluntness is the primary aerodynamic amplification mechanism exploited in Stage II.

The biomimetic grooved surface is generated by superimposing a cosinusoidal perturbation onto the base cross-section along the outward-facing surface normal. In a general vectorial form valid for all aspect ratios:

$$\mathbf{r}(s) = \mathbf{r}_0(s) + a \cdot AR \cdot Amp \cdot \cos\left(\frac{2\pi Ns}{L}\right) \hat{\mathbf{n}}(s) \quad (2)$$

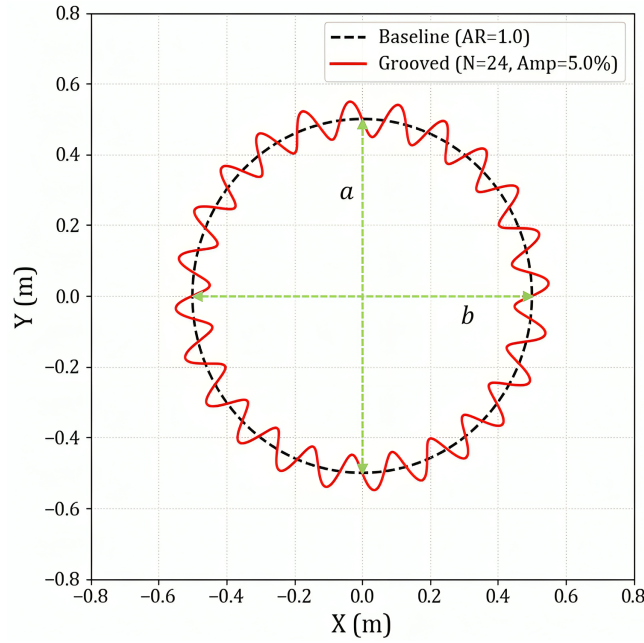
where  $\mathbf{r}_0(s)$  is the position vector on the smooth base ellipse,  $s$  is the arc-length coordinate measured from the leading stagnation point,  $L$  is the total perimeter of the base ellipse,  $N$  is the integer wave number,  $Amp$  is the non-dimensional groove amplitude expressed as a percentage of the streamwise dimension ( $b = a \cdot AR$ ), and  $\hat{\mathbf{n}}(s)$  is the outward-facing unit normal vector. The arc-length parameterisation ensures physically uniform groove wavelength regardless of local curvature variations, and the proportional amplitude scaling with  $b$  prevents excessive structural distortion at lower aspect ratios.

For the circular baseline ( $AR = 1.0$ ,  $b = a$ ), the normal coincides with the radial direction and Eq. (2) reduces to:

$$r(\theta) = \frac{a}{2} [1 + 2Amp \cdot \cos(N\theta)] \quad (3)$$

where  $\theta$  is the angular position from the leading stagnation point. Here the prefactor  $a/2 = D/2$  is the base radius of the circular cross-section, consistent with this full-dimension convention. Higher values of  $N$  produce more groove peaks per circumference, while larger  $Amp$  generates deeper surface undulations. The interaction between these two parameters governs the degree of shear layer perturbation, and their joint optimisation via the Taguchi method (Section 2.4.1) constitutes Stage I of the computational framework. Fig. 1 illustrates the baseline circular cross-section ( $AR = 1.0$ ) and the biomimetic grooved profile ( $N = 24$ ,  $Amp = 5.0\%$ ). The sinusoidal surface undulation prescribed by Eq. (3) is clearly visible as a periodic radial perturbation superimposed on the circular baseline.

Additionally, the groove amplitude ( $Amp$ ) is proportionally scaled with respect to the modified horizontal thickness  $b$  across different aspect ratios. This proportional scaling prevents excessive structural distortion of the grooved profiles at lower aspect ratios, where a fixed absolute amplitude would physically transform the surface undulations from the intended biomimetic cactus-inspired groove geometry into an extreme gear-tooth profile that would fundamentally alter the targeted boundary layer separation mechanisms.



**Figure 1:** Schematic definition of the baseline circular ( $AR = 1.0$ ) and grooved ( $N = 24$ ,  $Amp = 5.0\%$ ) geometries, illustrating the full axes  $a$  and  $b$ .

The flow regime is characterized by the Reynolds number, defined using the constant frontal height  $D = a$  as the reference length:

$$Re = \frac{U_{\infty} D}{\nu} \quad (4)$$

where  $U_{\infty}$  is the uniform free-stream velocity and  $\nu$  is the kinematic viscosity of the fluid. All simulations are conducted at this fixed Reynolds number, ensuring that the flow remains laminar and that any observed differences in aerodynamic behavior are solely attributable to geometric modifications rather than changes in flow regime.

The primary aerodynamic quantity of interest for VIV energy harvesting evaluation is the instantaneous cross-flow lift coefficient,  $C_{\ell}$ , defined as:

$$C_{\ell} = \frac{F_L}{\frac{1}{2} \rho U_{\infty}^2 D} \quad (5)$$

where  $F_L$  is the instantaneous transient cross-flow lift force per unit span acting on the fixed bluff body,  $\rho$  is the fluid density,  $U_{\infty}$  is the free-stream reference velocity, and  $D$  is the constant characteristic frontal height. The root-mean-square fluctuation of the lift coefficient,  $C_{\ell, \text{rms}}$ , quantifies the time-averaged magnitude of the alternating aerodynamic excitation force and serves as the primary optimization objective in this study:

$$C_{\ell, \text{rms}} = \sqrt{\frac{1}{T} \int_0^T (C_{\ell} - \overline{C_{\ell}})^2 dt} \quad (6)$$

where  $\overline{C_{\ell}}$  denotes the time-mean lift coefficient and  $T$  is the integration interval encompassing a statistically sufficient number of fully developed, quasi-periodic vortex shedding cycles. A higher  $C_{\ell, \text{rms}}$  directly indicates

a stronger alternating aerodynamic forcing, which in a coupled VIV system would drive larger oscillation amplitudes and correspondingly greater energy extraction capacity.

The dominant vortex shedding frequency  $f_s$  is non-dimensionalized as the Strouhal number:

$$St = \frac{f_s D}{U_\infty} \quad (7)$$

The Strouhal number provides a universal non-dimensional characterization of the vortex shedding periodicity across different geometric configurations and flow conditions. Changes in  $St$  with aspect ratio carry important structural implications: a lower  $St$  at a given  $Re$  translates to a lower dimensional shedding frequency  $f_s$ , enabling structural lock-in to be achieved with softer mechanical systems or at lower flow velocities, both desirable properties for micro-scale VIV energy harvesters deployed in extremely slow currents. For the vertically-oriented elliptical geometry evaluated in this study, the Strouhal number does not vary monotonically with aspect ratio. The specific cross-sectional shape and the balance between shear-layer separation angle and vortex convection distance jointly determine the shedding frequency, producing a non-trivial dependence on  $AR$  that is characterised quantitatively in the Results section.

To evaluate the feasibility of utilizing the enhanced wake instabilities for practical energy extraction, a simplified one-degree-of-freedom (1-DOF) structural projection model is employed to estimate the available power coefficient ( $C_{p,est}$ ):

$$C_{p,est} = \frac{P_{est}}{\frac{1}{2}\rho U_\infty^3 D} = \frac{F_{rms}^2 / (2m\zeta\omega_n)}{\frac{1}{2}\rho U_\infty^3 D} \quad (8)$$

where  $P_{est}$  is the projected extractable mechanical power,  $F_{rms}$  is the RMS lift force derived directly from the fixed-body simulations ( $F_{rms} = \frac{1}{2}\rho U_\infty^2 D \cdot C_{\ell,rms}$ ),  $m$  is the effective oscillating system mass,  $\zeta$  is the total structural-plus-electrical damping ratio, and  $\omega_n$  is the natural angular frequency of the theoretical coupled oscillator. In the present study, representative fixed values of the mass ratio  $m^* = m/m_d$  (where  $m_d$  is the displaced fluid mass) and the damping ratio  $\zeta$  are adopted throughout, as detailed in Section 3.5.2. The 1-DOF projection model, originally rooted in the linear resonant response framework established by Bernitsas et al. [1] and widely adopted in the VIV energy harvesting literature [28–31], provides a standardized and physically meaningful metric that enables direct comparison of the geometric amplification potential across different configurations, independent of the specific structural or electromechanical transduction system ultimately employed. The present 1-DOF model provides a relative measure of aerodynamic excitation across configurations rather than an absolute prediction of harvested power. Since the structural parameters are held fixed,  $P_{est}$  is interpreted throughout as a comparative fixed-body aerodynamic forcing metric. It quantifies the relative excitation available to a hypothetical coupled oscillator and reflects comparative trends across configurations. It is not a prediction of the actual harvested power, oscillation amplitude, or lock-in behaviour, which depend on the specific device design and would require fully coupled fluid-structure interaction simulations.

## 2.2 Numerical Setup and Flow Configuration

All transient numerical simulations are performed using a two-dimensional, pressure-based, laminar flow formulation within the ANSYS Fluent commercial CFD solver environment. The simulation framework is deliberately two-dimensional, which accurately captures the dominant planar Kármán vortex street dynamics and the associated periodic aerodynamic force fluctuations at  $Re = 200$  while affording the computational efficiency necessary for the parametric optimization and comparative studies constituting the core of this investigation. Although secondary three-dimensional wake instabilities (Mode A transition)



begin to emerge in circular cylinder wakes at  $Re$  around 190 [32], numerical studies conducted specifically at  $Re = 200$  have demonstrated that two-dimensional simulations reproduce the aerodynamic force coefficients and Strouhal number with negligible deviation from their three-dimensional counterparts [33–35], confirming that the primary energy-carrying Kármán vortex street and its associated cross-flow forcing remain fundamentally two-dimensional at this Reynolds number. Although these three-dimensional effects, principally spanwise vortex dislocations, would drain a fraction of the spanwise-coherent fluctuation energy and somewhat reduce the absolute force magnitudes, they act on the same primary Kármán mode shared by all six geometries; the comparative ranking and amplification factors that constitute the central conclusions of this study are expected to remain qualitatively similar, although some quantitative changes in amplification factors and relative ranking cannot be excluded without dedicated three-dimensional simulations. The Reynolds-sensitivity analysis at  $Re = 100$  and  $Re = 150$  lies, moreover, entirely within the strictly two-dimensional laminar regime. Since the present study targets comparative aerodynamic excitation across geometries rather than absolute force prediction, the two-dimensional framework is well suited to the intended geometric optimisation, with the reported absolute force levels interpreted as upper-bound estimates.

The two-dimensional, incompressible, unsteady laminar flow is governed by the continuity equation and the Navier-Stokes momentum equations:

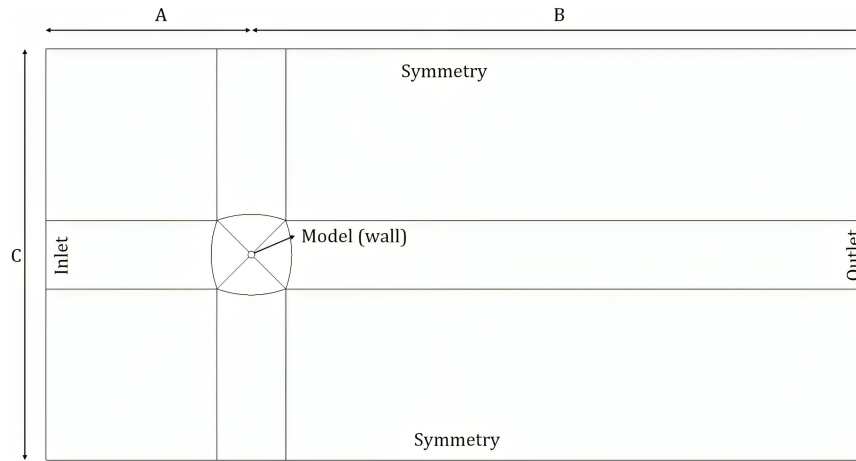
$$\nabla \cdot \mathbf{u} = 0 \quad (9)$$

$$\frac{\partial \mathbf{u}}{\partial t} + (\mathbf{u} \cdot \nabla) \mathbf{u} = -\frac{1}{\rho} \nabla p + \nu \nabla^2 \mathbf{u} \quad (10)$$

where  $\mathbf{u}$  is the velocity vector field comprising the streamwise ( $u$ ) and cross-flow ( $v$ ) components,  $t$  denotes physical time,  $p$  is the pressure field,  $\rho$  is the fluid density, and  $\nu$  is the kinematic viscosity.

To ensure robust and accelerated convergence for the highly unsteady wake dynamics, the Pressure-Implicit with Splitting of Operators (PISO) pressure-velocity coupling algorithm is employed, utilizing both skewness and neighbor corrections to maintain solution stability on the complex unstructured mesh topology near the grooved cylinder surfaces. Spatial discretization of velocity gradients is performed using the Green-Gauss Node Based method. A second-order scheme is applied for pressure interpolation, while the momentum equations are discretized using a second-order upwind scheme to minimize artificial numerical diffusion across the computational grid [36]. Temporal advancement is governed by a second-order implicit (Crank-Nicolson blended) formulation, which precisely captures the time-dependent, quasi-periodic vortex shedding dynamics and the resulting aerodynamic force fluctuations with minimal phase error [37].

The computational domain is a two-dimensional rectangular channel surrounding the fixed cylinder model. The domain boundaries are parameterized using three key distances: the upstream length  $A$  from the velocity inlet to the cylinder center, the downstream length  $B$  from the cylinder center to the pressure outlet, and the total lateral height  $C$  of the domain, as illustrated in Fig. 2. A uniform, steady velocity inlet condition ( $U_\infty = \text{const.}$ ,  $v = 0$ ) is applied at the upstream boundary. A zero gauge pressure outlet condition is imposed at the downstream boundary, permitting the fully developed vortex wake to exit the domain without artificial reflection. Symmetry boundary conditions are applied at the upper and lower lateral boundaries to simulate an unconfined far-field environment. A no-slip, no-penetration wall boundary condition ( $u = v = 0$ ) is applied across the entire cylinder surface. The cylinder is stationary and fixed throughout all simulations.



**Figure 2:** Schematic of the computational domain illustrating the upstream distance (**A**), downstream wake region (**B**), lateral height (**C**), and applied boundary conditions.

The computational mesh consists of a structured quadrilateral cell topology throughout the far-field domain, transitioning to a carefully designed multi-layer boundary layer mesh in the near-wall region. The boundary layer mesh is constructed with geometric stretching, with the first computational cell layer set at a thickness of  $6 \times 10^{-4}$  m from the wall, directly resolving the viscous boundary layer characteristic of low-Reynolds-number laminar flows without requiring any wall function treatment.

Once the transient simulations reach a statistically steady, fully developed periodic state, confirmed by the stabilization of the lift coefficient time history to a consistent quasi-sinusoidal signal, the initial start-up phase (the first 50% of the total simulation time) is strictly discarded to prevent any transient contamination. For the fully developed regime, instantaneous velocity field snapshots are exported from the solver at intervals of 50 computational time steps ( $\Delta t_{\text{sample}} = 0.5$  s). These snapshots comprise the streamwise and cross-flow velocity components at all grid nodes. This sampling rate is deliberately chosen to yield approximately 10 snapshots per shedding cycle, providing more than twice the minimum sampling frequency required by the Nyquist–Shannon sampling theorem [38] for the dominant Strouhal shedding frequency while maintaining efficient computational storage and memory allocation. The extracted temporal window encompasses 20 consecutive fully developed vortex shedding cycles, yielding a matrix of 200 snapshots. This dataset constitutes the velocity field input required for the subsequent Proper Orthogonal Decomposition (POD) modal analysis and wake fluctuation energy (WFE) spatial mapping.

### 2.3 Independence Studies and Model Validation

To guarantee the numerical accuracy and physical fidelity of all computed results, and to eliminate any contamination from spatial or temporal discretization errors, a comprehensive three-part independence study is conducted on the smooth circular cylinder at  $Re = 200$  prior to performing any geometric modification simulations. The three independence parameters evaluated are: (i) the computational domain extent, (ii) the mesh spatial resolution, and (iii) the physical time step size. Each parameter is varied systematically while keeping the other two fixed at their refined levels.

#### 2.3.1 Domain Size Independence

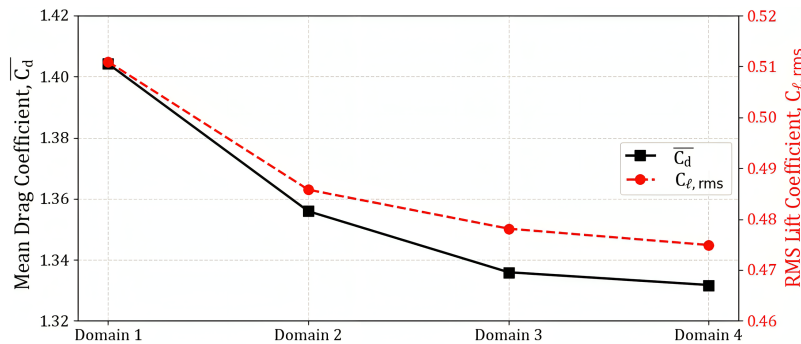
The spatial independence study is performed first to eliminate unphysical boundary interference effects that arise when the domain boundaries are placed too close to the cylinder. Four domain configurations of

progressively increasing extent are evaluated, as detailed in Table 1. The upstream distance  $A$  is chosen to ensure that the inlet boundary condition is imposed in a region of genuinely undisturbed, uniform flow, while the downstream distance  $B$  must be sufficient to allow the periodic vortex street to fully develop and dissipate before reaching the pressure outlet.

**Table 1:** Geometric parameters of the evaluated computational domains for spatial independence study. All dimensions are normalized by the cylinder diameter  $D$ .

| Configuration | A (Upstream) | B (Downstream) | C (Lateral) |
|---------------|--------------|----------------|-------------|
| Domain 1      | $7.5D$       | $22.5D$        | $15D$       |
| Domain 2      | $15D$        | $45D$          | $30D$       |
| Domain 3      | $30D$        | $90D$          | $60D$       |
| Domain 4      | $60D$        | $180D$         | $120D$      |

The aerodynamic coefficients, namely the mean drag coefficient ( $\overline{C_d}$ ) and RMS lift ( $C_{\ell, \text{rms}}$ ), are monitored and compared across the four domain sizes, as shown in Fig. 3. The results demonstrate complete stabilization of both quantities at Domain 3 ( $30D \times 90D \times 60D$ ), beyond which further domain expansion to Domain 4 produces changes of less than 0.1% in both  $\overline{C_d}$  and  $C_{\ell, \text{rms}}$ . Domain 3 yields a solid blockage ratio of approximately 1.67% ( $D/C = 1/60$ ), which is enough for laminar bluff body simulations and effectively eliminates artificial far-field confinement effects [39]. Domain 3 is therefore selected as the standard computational domain for all subsequent simulations.



**Figure 3:** Domain size independence study.

### 2.3.2 Grid Resolution Independence

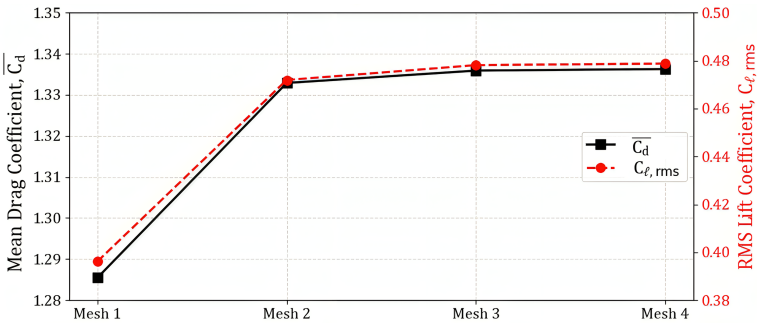
Following the domain selection, a rigorous grid resolution independence study is conducted to determine the minimum spatial discretization required for accurate resolution of the near-wall boundary layer and the shear layer dynamics. Four structured mesh configurations of progressively increasing density are generated, differing in the number of computational nodes distributed around the cylinder circumference, as summarized in Table 2. In all mesh configurations, the first layer thickness and the boundary layer expansion ratio (1.1) are maintained constant, ensuring that only the circumferential node count governs the differences between refinement levels.

The grid convergence behaviour of  $\overline{C_d}$  and  $C_{\ell, \text{rms}}$  across the four mesh levels is presented in Fig. 4. The transient aerodynamic force convergence analysis demonstrates that both  $\overline{C_d}$  and  $C_{\ell, \text{rms}}$  become fully grid-independent beyond Mesh 3 (240 nodes around circumference), with differences between Mesh 3 and the finest Mesh 4 remaining below 0.3%. Mesh 1 substantially underestimates  $C_{\ell, \text{rms}}$  due to insufficient resolution

of the shear layer curvature near the separation point. The substantially greater computational cost of Mesh 4 relative to Mesh 3, combined with its negligible improvement in predicted aerodynamic coefficients, justifies the selection of Mesh 3 as the standard computational mesh for all production simulations.

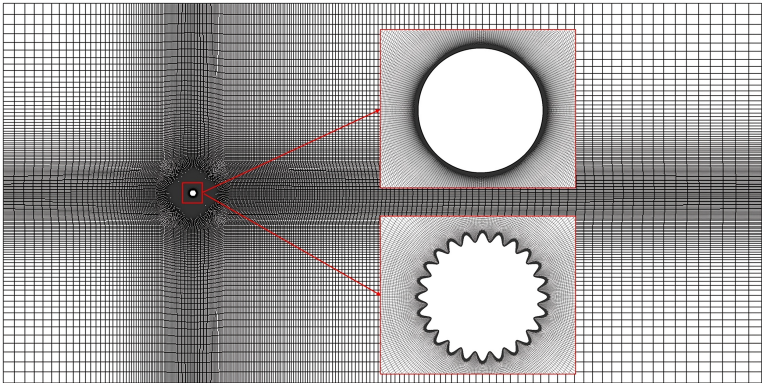
**Table 2:** Grid independence study parameters based on the number of nodes distributed around the cylinder circumference.

| Grid Configuration | Nodes Around Cylinder | Approx. Total Cell Count |
|--------------------|-----------------------|--------------------------|
| Mesh 1             | 80                    | 29,000                   |
| Mesh 2             | 160                   | 46,000                   |
| Mesh 3             | 240                   | 63,000                   |
| Mesh 4             | 360                   | 89,000                   |



**Figure 4:** Grid size independence study.

The grid-independence study established a converged baseline resolution of 240 circumferential nodes (Table 2). For the grooved and elliptical geometries, the mesh is scaled to preserve the baseline cell size while adequately resolving the grooves, with identical wall-normal first-cell thickness across all cases. Consequently, all configurations inherit the grid-independent baseline resolution, with finer effective resolution in the groove regions. The final computational mesh employed in the simulations is shown in Fig. 5, comprising a structured quadrilateral far-field grid and a refined near-wall boundary layer mesh around the baseline and grooved cylinders.



**Figure 5:** Computational mesh topology illustrating the structured far-field quadrilateral grid and the highly refined near-wall boundary layer mesh around the baseline and grooved circular cylinder.

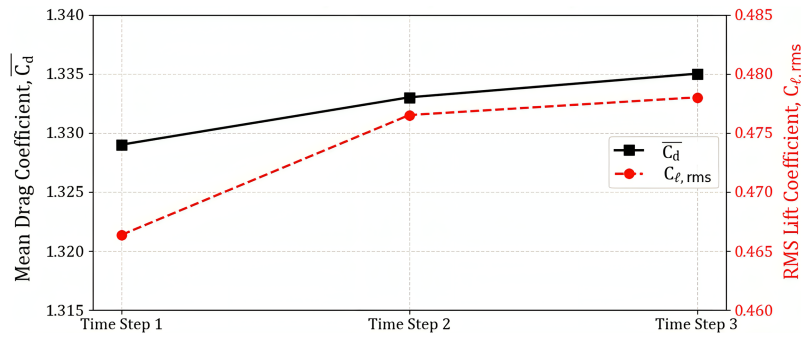
### 2.3.3 Temporal Discretization Independence

The temporal resolution study evaluates the sensitivity of the computed transient aerodynamic forces to the physical time step size  $\Delta t$ . A sufficiently small time step (TS) is required to accurately resolve the instantaneous dynamics of vortex formation and shedding, satisfy the Courant-Friedrichs-Lewy (CFL) stability criterion within the highly unsteady near-wake region, and ensure that the spectral content of the lift force signal is captured without aliasing or numerical damping. Three time step sizes are evaluated, as listed in Table 3.

**Table 3:** Time step size independence study parameters and corresponding approximate CFL numbers.

| Configuration    | Time Step $\Delta t$ (s) | Approx. Max. CFL |
|------------------|--------------------------|------------------|
| Time Step Size 1 | 0.020                    | 1.82             |
| Time Step Size 2 | 0.010                    | 0.98             |
| Time Step Size 3 | 0.005                    | 0.51             |

The independence test results, as illustrated in Fig. 6, confirm that the transient force fluctuations, including their amplitude, phase, and spectral content, are accurately and consistently captured at a time step of  $\Delta t = 0.01$  s (TS 2). This temporal resolution ensures that the maximum CFL number remains below 1.0 throughout the critical near-wake and boundary layer regions. The coarser time step TS 1 ( $\Delta t = 0.02$  s) introduces measurable numerical damping that artificially reduces the peak-to-peak lift amplitude by approximately 4%, while TS 3 ( $\Delta t = 0.005$  s) produces negligible changes relative to TS 2 while incurring approximately twice the computational cost.



**Figure 6:** Time step size independence study.

Accordingly, the combination of Domain 3, Mesh 3, and Time Step Size 2 is selected as the standard computational configuration for all transient simulations. Each simulation is executed for a minimum of 20,000 computational time steps (200 s of physical time), 34–44 complete vortex shedding cycles depending on the specific Strouhal frequency of the simulated configuration.

### 2.3.4 Code Validation

The physical and numerical parameters employed throughout the study are summarised in Table 4 for completeness and reproducibility. The validation of the adopted numerical framework is then performed using the baseline smooth circular cylinder case.

**Table 4:** Physical and numerical parameters of the computational setup.

| Parameter                                   | Value/Setting                                 |
|---------------------------------------------|-----------------------------------------------|
| <i>Flow and fluid properties</i>            |                                               |
| Reference length, $D (=a)$                  | 1 m                                           |
| Reynolds number, $Re$                       | 200                                           |
| Free-stream velocity, $U_\infty$            | $1 \text{ m s}^{-1}$                          |
| Fluid density, $\rho$                       | $1 \text{ kg m}^{-3}$                         |
| Kinematic viscosity, $\nu$                  | $5 \times 10^{-3} \text{ m}^2 \text{ s}^{-1}$ |
| Flow regime                                 | Two-dimensional, laminar, incompressible      |
| <i>Solver and discretisation</i>            |                                               |
| Solver type                                 | Pressure-based, transient                     |
| Pressure-velocity coupling                  | PISO                                          |
| Spatial discretisation                      | Second-order upwind                           |
| Temporal discretisation                     | Second-order implicit                         |
| Time step, $\Delta t$                       | 0.01 s                                        |
| Maximum Courant number                      | 0.98                                          |
| Convergence criterion (scaled residuals)    | $1 \times 10^{-6}$ per time step              |
| <i>Domain, boundary conditions and mesh</i> |                                               |
| Upstream extent                             | $30D$                                         |
| Downstream extent                           | $90D$                                         |
| Lateral (cross-stream) extent               | $60D$                                         |
| Blockage ratio                              | 1.67%                                         |
| Inlet/outlet                                | Velocity inlet/pressure outlet                |
| Lateral boundaries                          | Symmetry                                      |
| Cylinder surface                            | No-slip wall                                  |
| Nodes around cylinder (baseline mesh)       | 240                                           |
| Total cell count (baseline mesh)            | 63,000                                        |

To validate the numerical framework and solver settings, the baseline smooth circular cylinder is simulated at  $Re = 200$  and the resulting aerodynamic force coefficients and Strouhal number are compared against well-established reference data, comprising the direct numerical simulation of Henderson [40], the experimental measurements of Williamson [41], the lattice Boltzmann simulation of Hu et al. [34], and the finite-volume study of Rajani et al. [35].

The present simulation predicts a mean drag coefficient of  $\overline{C_d} = 1.333$  in good agreement with Henderson [40] and Rajani et al. [35], an RMS lift coefficient  $C_{\ell, \text{rms}} = 0.4765$  within the range reported by Hu et al. (0.499) and Rajani et al. (0.4244), and a Strouhal number  $St = 0.1953$  that matches the reference values to within their reported uncertainty bounds, as shown in Table 5. Notably, the two-dimensional Strouhal number agrees closely with the experimentally measured value of Williamson [41], whose physical flow is inherently three-dimensional, indicating that the primary Kármán shedding characteristics are captured reasonably well by the adopted two-dimensional framework at  $Re = 200$ . The framework is therefore reliable for the subsequent grooved and elliptical configurations, and all aerodynamic metrics are presented and interpreted as comparative trends across geometries within this two-dimensional laminar setting.



**Table 5:** Code validation against established literature for flow past a smooth circular cylinder at  $Re = 200$ .

| Source             | Study Type     | $\overline{C_d}$ | $C_{\ell,rms}$ | St     |
|--------------------|----------------|------------------|----------------|--------|
| Henderson [40]     | DNS (2D)       | 1.340            | N/A            | 0.197  |
| Williamson [41]    | Experimental   | N/A              | N/A            | 0.197  |
| Hu et al. [34]     | LBM (2D)       | 1.394            | 0.499          | 0.195  |
| Rajani et al. [35] | Numerical (2D) | 1.337            | 0.4244         | 0.1957 |
| Present Study      | Numerical (2D) | 1.333            | 0.4765         | 0.1953 |

## 2.4 Optimization Framework and Modal Analysis

### 2.4.1 Taguchi L9 Orthogonal Array Optimization

To systematically and efficiently identify the biomimetic groove configuration that yields the maximum aerodynamic excitation without incurring the prohibitive computational cost of a full factorial parametric study, the Taguchi robust design optimization method is employed in the first stage of the computational framework. The Taguchi method utilizes orthogonal arrays to strategically sample the design space in a balanced and resolution-maximizing manner [42,43]. An L9 orthogonal array is implemented in this study, evaluating three levels of each of the two groove parameters ( $N$  and  $Amp$ ) in nine strategically selected simulation runs. The L9 array provides full resolution of both main effects while requiring the minimum number of simulation runs needed to achieve this resolution. The specific parameter levels evaluated are: wave number  $N \in \{20, 24, 28\}$  and amplitude  $Amp \in \{3\%, 4\%, 5\%\}$  of the reference length  $D$ . The lower bound of the  $N$  range is set at 20 to ensure a minimum groove wavelength sufficient for meaningful shear layer interaction, while the upper bound of 28 represents the practical limit beyond which the inter-groove angular spacing approaches the length scale of the near-wall viscous sublayer at the present laminar Reynolds number. The amplitude range balances the requirement for a geometrically significant surface perturbation ( $Amp \geq 3\%$ ) against the risk of excessive structural distortion ( $Amp > 5\%$ ), which would compromise the biomimetic character of the grooved profile.

Because the primary objective for VIV energy harvesting is to maximize the alternating cross-flow aerodynamic force, the “Larger-the-Better” Signal-to-Noise ( $S/N$ ) ratio is selected as the Taguchi objective:

$$S/N = -10 \log_{10} \left[ \frac{1}{n} \sum_{i=1}^n \frac{1}{y_i^2} \right] \quad (11)$$

where  $y_i$  corresponds to the root-mean-square lift coefficient ( $C_{\ell,rms}$ ) obtained from each individual simulation run  $i$  in the L9 array, and  $n$  is the number of observations per run. Since the laminar flow at fixed Reynolds number is deterministic and reproducible, each configuration was simulated once, and  $n$  corresponds to the single converged  $C_{\ell,rms}$  obtained from a statistically stationary lift signal. The L9 array therefore serves as an efficient structured design of experiments for the main effects of  $N$  and  $Amp$ , with the  $S/N$  ratio acting as a monotonic objective transform of  $C_{\ell,rms}$  rather than a noise-robustness measure. The associated numerical uncertainty is bounded by the independence studies of Section 2.3, below approximately 0.3% in  $C_{\ell,rms}$ , with correspondingly small uncertainties in  $St$  and  $P_{est}$ . The main effects of each parameter level are subsequently assessed by averaging the  $S/N$  ratios across all runs sharing a given parameter level, enabling the optimal combination to be identified as the parameter levels that individually maximize the average  $S/N$  response.

### 2.4.2 Proper Orthogonal Decomposition (POD) Modal Analysis

Following the identification and coupling of the optimal groove geometry with the elliptical base configurations, Proper Orthogonal Decomposition (POD) is applied to the time-resolved near-wake velocity field data to elucidate the underlying physical mechanisms driving the aerodynamic force amplification and to systematically quantify the spatial coherence and energetic organization of the vortex shedding structures [44,45]. POD is a mathematically optimal technique for extracting the most energetically dominant spatial flow structures from an ensemble of velocity field snapshots: it decomposes the highly unsteady near-wake into a ranked series of orthogonal spatial modes ordered strictly by their wake fluctuation energy content, ensuring that the first mode always captures the greatest possible fraction of the total fluctuation energy.

The snapshot-based POD method is implemented [46]. The fluctuating velocity field  $\mathbf{u}'(\mathbf{x}, t)$  is expressed as a linear superposition of  $M$  time-independent orthogonal spatial modes  $\boldsymbol{\varphi}_k(\mathbf{x})$  and their corresponding time-varying scalar coefficients  $a_k(t)$ :

$$\mathbf{u}'(\mathbf{x}, t) = \sum_{k=1}^M a_k(t) \boldsymbol{\varphi}_k(\mathbf{x}) \quad (12)$$

The spatial modes  $\boldsymbol{\varphi}_k$  are computed as the eigenvectors of the two-point spatial cross-correlation matrix constructed from the snapshot ensemble, while the time coefficients  $a_k(t)$  are obtained by projecting each snapshot onto the corresponding spatial mode. The eigenvalue  $\lambda_k$  associated with each mode directly represents the energy of wake fluctuation contributed by that mode, and the normalized fractional energy content of each mode is given by  $\lambda_k / \sum \lambda$ . The cumulative energy fraction provides a rigorous quantitative measure of the degree of wake coherence: for a highly organized, synchronized vortex shedding process, as expected and desired for sustained VIV energy harvesting, the first two spatial modes together capture the overwhelming majority (typically > 95%) of the total fluctuation energy, confirming the existence of a dominant, low-dimensional, periodic wake structure.

The physical significance of POD in the context of VIV energy harvesting is profound: a wake dominated by its first two spatial modes is a wake in which virtually all of the available fluctuation energy is organized into a single coherent, periodic oscillation at the Strouhal frequency. This high modal coherence implies that the aerodynamic lift force on the cylinder is similarly dominated by a single, stable periodic component, representing the forcing quality required to drive sustained lock-in oscillations in a coupled structural resonator. The POD modal energy distribution therefore serves as a critical diagnostic for assessing not merely the magnitude of the aerodynamic excitation, but also its regularity and temporal stability, both essential qualities for reliable, long-duration energy harvesting in practical deployment.

## 3 Results and Discussion

The results are organised into six thematic subsections following the logical progression of the computational framework. The Taguchi L9 optimisation outcome and optimal groove selection are presented first in [Section 3.1](#), followed by the aerodynamic force and spectral response characterisation in [Section 3.2](#). The modal coherence and structural stability diagnostics are presented in [Section 3.3](#), and near-wake topology and instantaneous dynamics are examined in [Section 3.4](#). The energy-oriented wake assessment is presented in [Section 3.5](#), where the projected energy harvesting amplification is quantified across all six configurations and the optimum geometry is identified. The study culminates in the Reynolds-number sensitivity analysis in [Section 3.6](#), which establishes the robustness and Reynolds dependence of the identified geometric optimum across the laminar regime.

### 3.1 Parametric Optimisation of Biomimetic Grooves

The Taguchi L9 simulation matrix and the resulting aerodynamic metrics are presented in Table 6. The signal-to-noise ratio analysis identifies groove amplitude ( $Amp$ ) as the dominant control parameter. Averaging the  $S/N$  ratios across all runs at each amplitude level confirms a monotonic and physically consistent improvement with increasing groove depth, whereas the wave number  $N$  exerts a negligible main effect, with the level-averaged  $S/N$  ratios for  $N = 20, 24$ , and  $28$  differing by less than 0.02 dB, indicating that the influence of  $N$  is substantially weaker than that of  $Amp$  over the parameter range considered. This outcome agrees with previous laminar studies showing that shear-layer instability is governed primarily by perturbation amplitude rather than spatial frequency [13].

**Table 6:** Taguchi L9 orthogonal array results and aerodynamic performance metrics at  $Re = 200$ .

| Run ID   | $N$ | $Amp$ (%) | Aerodynamic Coefficients |                |        |                 | $P_{est}$ (W/m) | $S/N$ (dB) |
|----------|-----|-----------|--------------------------|----------------|--------|-----------------|-----------------|------------|
|          |     |           | $\overline{C_d}$         | $C_{\ell,rms}$ | $St$   | $F_{rms}$ (N/m) |                 |            |
| Baseline | –   | –         | 1.3331                   | 0.4765         | 0.1953 | 0.2383          | 0.2313          | –6.439     |
| Run 01   | 20  | 3         | 1.4094                   | 0.5232         | 0.1953 | 0.2616          | 0.2788          | –5.627     |
| Run 02   | 20  | 4         | 1.4451                   | 0.5477         | 0.1953 | 0.2739          | 0.3056          | –5.230     |
| Run 03   | 20  | 5         | 1.4873                   | 0.5758         | 0.1709 | 0.2880          | 0.3860          | –4.794     |
| Run 04   | 24  | 3         | 1.4057                   | 0.5222         | 0.1953 | 0.2611          | 0.2778          | –5.644     |
| Run 05   | 24  | 4         | 1.4431                   | 0.5472         | 0.1953 | 0.2736          | 0.3050          | –5.237     |
| Run 06   | 24  | 5         | 1.4870                   | 0.5768         | 0.1709 | 0.2884          | 0.3873          | –4.779     |
| Run 07   | 28  | 3         | 1.4048                   | 0.5224         | 0.1953 | 0.2612          | 0.2779          | –5.640     |
| Run 08   | 28  | 4         | 1.4434                   | 0.5486         | 0.1953 | 0.2743          | 0.3065          | –5.216     |
| Run 09   | 28  | 5         | 1.4864                   | 0.5766         | 0.1709 | 0.2884          | 0.3870          | –4.782     |

The physical origin of this asymmetry lies in the laminar near-wall dynamics at  $Re = 200$ . A deeper groove imposes a stronger localised adverse pressure gradient at each ridge, accelerating shear-layer detachment and intensifying the subsequent vortex roll-up regardless of circumferential density. The spatial frequency  $N$ , by contrast, governs the inter-groove angular spacing; at  $N = 24$  this spacing is approximately  $15^\circ$ , which is well matched to the coherent near-wall flow structures at this Reynolds number. Further increasing  $N$  to 28 tightens the spacing to  $12.9^\circ$ , at which point the individual groove ridges begin to act collectively as a near-wall roughness layer rather than as discrete shear-layer tripping elements, diminishing their aerodynamic effectiveness [13,47].

With the dominant parameter fixed at  $Amp = 5\%$ , three candidate configurations remain, namely Run 03 ( $N = 20$ ), Run 06 ( $N = 24$ ), and Run 09 ( $N = 28$ ). Runs 06 and 09 yield virtually identical  $C_{\ell,rms}$  values (0.5768 vs. 0.5766), with a difference of less than 0.04%, which is negligible for practical purposes. The selection is therefore governed by physical consistency rather than numerical ranking. At  $N = 24$  the inter-groove spacing preserves the intended biomimetic tripping character, whereas  $N = 28$  risks shifting below the effective groove-to-boundary-layer interaction range established for cactus-inspired geometries [13,47]. Run 06 ( $N = 24$ ,  $Amp = 5\%$ ) is accordingly selected as the optimal groove configuration for Stage II.

Run 06 achieves  $C_{\ell,rms} = 0.5768$  and  $P_{est} = 0.3873$  W/m, representing a 21% increase in  $C_{\ell,rms}$  and a 12.5% downward shift in Strouhal number (from  $St = 0.1953$  to  $St = 0.1709$ ) relative to the smooth baseline. This Strouhal reduction is structurally advantageous for low-speed lock-in, as it lowers the natural frequency

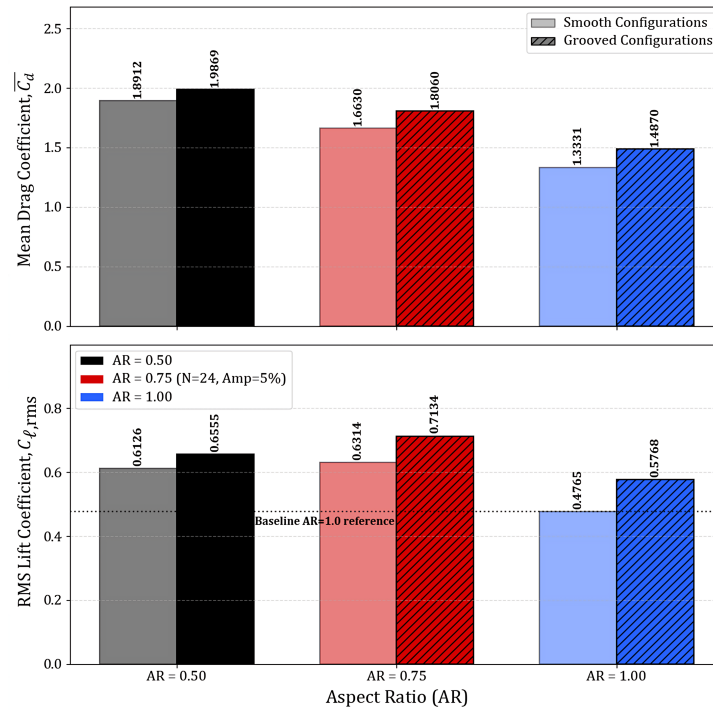
required for synchronisation and broadens the operating range of a micro-harvester in ultra-low-velocity environments [1,8].

The groove parameters ( $N$  and  $Amp$ ) were optimised on the circular baseline at  $Re = 200$  and subsequently transferred to the elliptical and lower-Reynolds-number configurations without re-optimisation. Given the weak sensitivity to  $N$  identified by the Taguchi analysis and the fact that  $Amp = 5\%$  was the largest amplitude considered and yielded the highest aerodynamic performance within the examined range, the selected groove geometry was retained for all subsequent cases.

### 3.2 Aerodynamic Force Response and Spectral Characteristics

#### 3.2.1 Force Coefficient Comparison

Fig. 7 presents the mean drag coefficient ( $\overline{C_d}$ ) and RMS lift coefficient ( $C_{\ell,rms}$ ) for all six Stage II configurations, grouped by aspect ratio with smooth baseline and optimally grooved cases displayed side by side.



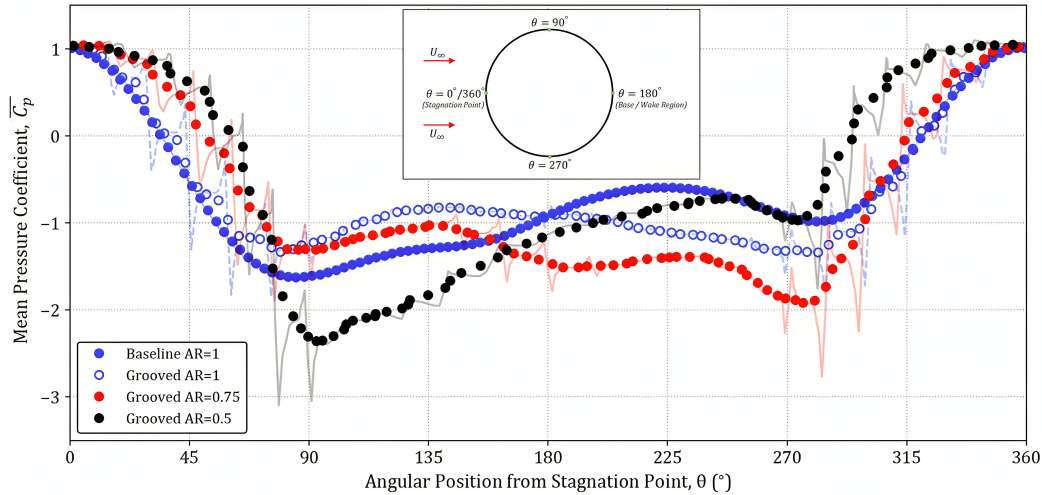
**Figure 7:** Mean drag ( $\overline{C_d}$ ) and RMS lift ( $C_{\ell,rms}$ ) coefficients for the smooth and optimally grooved configurations across varying aspect at  $Re = 200$ .

Examining the smooth baseline series first, increasing geometric bluntness from  $AR = 1.0$  to  $AR = 0.50$  raises  $\overline{C_d}$  monotonically from 1.333 to 1.891, confirming the expected non-linear escalation of pressure drag as the streamwise body dimension is shortened and an increasingly severe adverse pressure gradient is imposed on the attached boundary layer. The RMS lift coefficient follows a different trend:  $C_{\ell,rms}$  increases from 0.4765 at  $AR = 1.0$  to 0.6314 at  $AR = 0.75$ , but then falls slightly to 0.6126 at  $AR = 0.50$ . This non-monotonic behaviour is a characteristic of the vertically-oriented elliptical geometry adopted in this study. At  $AR = 0.50$ , the streamwise body is so compressed that the boundary layer barely develops before separating, limiting the effective shear-layer roll-up length and partially constraining the alternating lift magnitude despite the

increased drag bluntness. This physical mechanism is examined in detail through the wake topology analysis in Section 3.4.

The introduction of biomimetic grooves ( $N = 24$ ,  $Amp = 5\%$ ) consistently elevates both  $\overline{C_d}$  and  $C_{\ell,rms}$  relative to the corresponding smooth baseline at every aspect ratio, confirming that the optimised surface modification successfully inverts the conventional vibration-suppression role of such grooves. Three aspects of this groove contribution are particularly significant. First, at  $AR = 1.0$  the groove raises  $C_{\ell,rms}$  by 21.1% (from 0.4765 to 0.5768), representing the largest relative increment across all three aspect ratios. Second, at  $AR = 0.75$  the groove produces a 13.0% increment (from 0.6314 to 0.7134), yielding the highest absolute  $C_{\ell,rms}$  of any evaluated configuration. Third, at  $AR = 0.50$  the groove contribution diminishes sharply to only 7.0% (from 0.6126 to 0.6555). This monotonically decreasing groove effectiveness with increasing bluntness reveals a physical saturation: as the elliptical body becomes progressively shorter in the streamwise direction, the boundary layer available for groove interaction thins and shortens, reducing the capacity of the ridge-induced micro-vortices to coherently perturb the separating shear layer [13,47]. This behaviour suggests that the effectiveness of groove-induced perturbations is constrained by the available boundary-layer development length, which decreases with increasing geometric bluntness. The critical implication is that the highest absolute  $C_{\ell,rms} = 0.7134$  is achieved not at the maximum bluntness level but at the intermediate Grooved  $AR = 0.75$  configuration, identifying this geometry as the optimal configuration within the investigated parameter space.

To provide surface pressure evidence supporting the observed force coefficient trends, Fig. 8 presents the time-averaged mean pressure coefficient ( $\overline{C_p}$ ) distribution around the cylinder circumference for four representative configurations. The inset schematic defines the angular coordinate convention, with  $\theta = 0^\circ$  at the leading stagnation point and  $\theta = 180^\circ$  at the base region. Faint background traces represent instantaneous groove-induced pressure perturbations, while bold markers indicate the circumferentially averaged mean values.



**Figure 8:** Time-averaged mean pressure coefficient ( $\overline{C_p}$ ) distribution for baseline  $AR = 1.0$ , grooved  $AR = 1.0$ , grooved  $AR = 0.75$ , and grooved  $AR = 0.50$  at  $Re = 200$ . Faint traces indicate instantaneous groove-induced perturbations. Inset defines the angular coordinate convention.

Baseline  $AR = 1.0$  follows the canonical laminar profile with  $\overline{C_p} = 1.0$  at the stagnation point and base pressure of approximately  $-0.7$  at  $\theta = 180^\circ$ . The Grooved  $AR = 1.0$  configuration exhibits a moderately deeper suction envelope with organized periodic pressure perturbations associated with coherent shear-layer

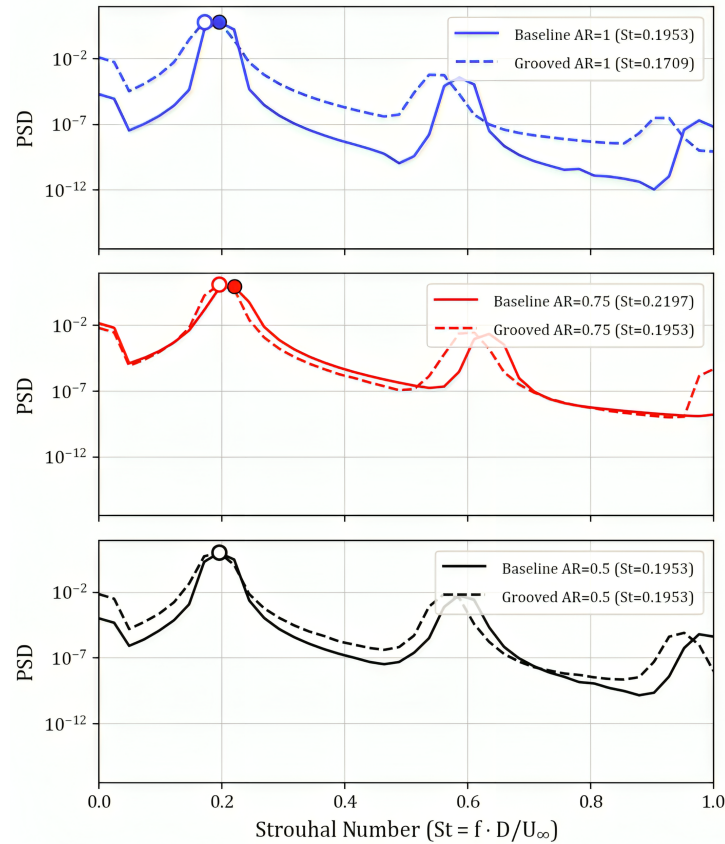


tripping by the groove ridges on the separating shear layer. At Grooved  $AR = 0.75$ , the suction amplifies substantially, with  $\overline{C_p}$  in the range of  $-1.6$  to  $-2.0$  across the leeward surface, and the groove-induced oscillations remain spatially organised and periodic, confirming coherent groove-shear-layer interaction at this bluntness level. This large and coherent pressure asymmetry between the upper and lower surfaces directly generates the highest  $C_{\ell,rms} = 0.7134$  of any evaluated configuration.

In contrast, Grooved  $AR = 0.50$  produces the most extreme suction peak, consistent with its highest  $\overline{C_d} = 1.987$ , yet the groove-induced perturbations become visibly irregular and incoherent in the separation regions. At this extreme bluntness, the boundary layer traverses the groove ridges too rapidly for the biomimetic tripping mechanism to establish organised micro-vortex structures, producing incoherent pressure fluctuations that less efficiently convert the high suction levels into coherent alternating lift. The  $\overline{C_p}$  distribution therefore provides an independent line of evidence confirming the Grooved  $AR = 0.75$  configuration as the aerodynamically optimal configuration among the evaluated cases.

### 3.2.2 Spectral Analysis and Strouhal Characteristics

Fig. 9 presents the Welch power spectral density (PSD) of the lift coefficient signal on a logarithmic scale for all six configurations, arranged as three panels each directly comparing the baseline (solid line) and grooved (dashed line) spectra at a common aspect ratio. All spectra are dominated by a single sharp peak at the Kármán shedding frequency, confirming the highly periodic, narrow-band character of the laminar wake across all evaluated geometries.



**Figure 9:** Power spectral density (PSD) of lift fluctuations comparing baseline and grooved configurations at  $AR = 1.0$ ,  $0.75$ , and  $0.50$  at  $Re = 200$ .



At  $AR = 1.0$  (upper panel), the groove induces a 12.5% downward shift in the dominant spectral peak from  $St = 0.1953$  to  $St = 0.1709$ . This is the largest Strouhal reduction observed across all configurations. The groove ridges promote early shear-layer detachment, which effectively shortens the vortex formation length and reduces the convection speed of the shed vortices relative to the free stream, lowering the dimensional shedding frequency. From a practical harvesting perspective, this frequency reduction translates directly into a lower natural frequency requirement for structural lock-in, which is advantageous for the design of soft, low-stiffness resonators suited to ultra-low-speed hydrokinetic deployment [7,8].

At  $AR = 0.75$  (middle panel), the groove produces a Strouhal normalisation from  $St = 0.2197$  (smooth) to  $St = 0.1953$ . This restoration of the shedding frequency to the circular cylinder reference value carries a significant practical advantage: a VIV micro-harvester designed and tuned for the standard circular cylinder geometry could in principle be deployed with the Grooved  $AR = 0.75$  body without any structural retuning of the spring-mass resonator, making this configuration especially attractive for scalable device implementation [1].

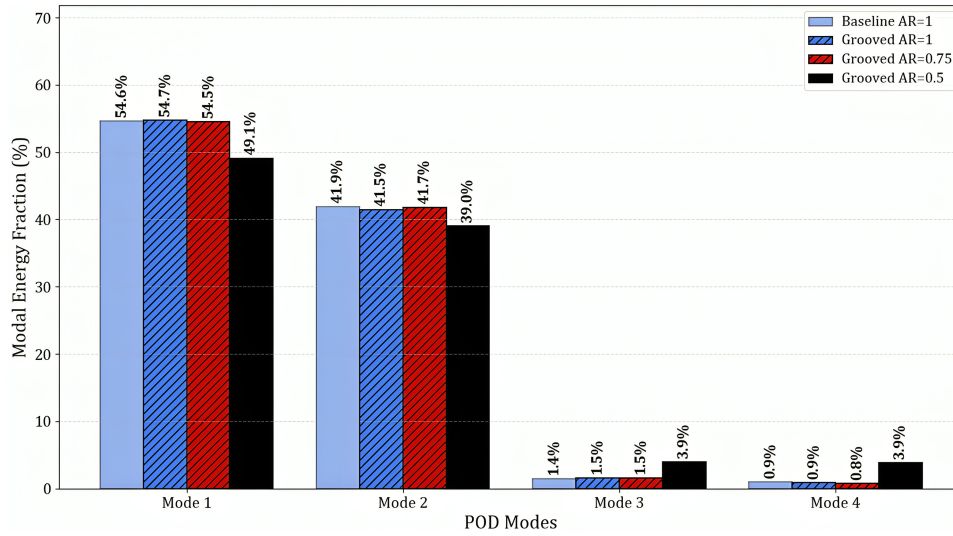
At  $AR = 0.50$  (lower panel), the Strouhal number remains at  $St = 0.1953$  in both the baseline and grooved cases, indicating that the shedding frequency at this extreme bluntness level is dominated by the geometry of the separated shear layers and is insensitive to the groove perturbation. In all panels, a secondary spectral peak is visible at approximately  $St = 0.59$  in the  $AR = 0.50$  and  $AR = 0.75$  configurations, corresponding to the second harmonic of the fundamental Kármán frequency. This feature is a well-established characteristic of laminar bluff-body wakes at  $Re = 200$  and is consistent with the self-interaction of the periodic vortex street in the near-wake region [40].

### 3.3 Modal Coherence and Structural Stability

#### 3.3.1 POD Modal Energy Distribution

Fig. 10 presents the POD modal energy fraction for the first four modes and the cumulative energy fraction for the same four representative configurations. The data provide a quantitative measure of wake coherence that informs the forcing quality of each configuration relevant to sustained VIV energy harvesting. For Baseline  $AR = 1.0$ , Mode 1 and Mode 2 together capture 96.5% of the total fluctuation energy ( $54.6 + 41.9\%$ ), with Modes 3 and 4 accounting for only 1.4% and 0.9% respectively. This near-perfect two-mode dominance confirms the textbook low-dimensional character of the laminar circular cylinder wake at  $Re = 200$ , in which virtually all aerodynamic fluctuation energy is organised into the conjugate Kármán mode pair. The Grooved  $AR = 1.0$  configuration maintains essentially identical modal coherence, with the first two modes capturing 96.2% ( $54.7 + 41.5\%$ ). This near-invariance confirms that the groove modification at  $AR = 1.0$  intensifies the aerodynamic excitation without disrupting the fundamental low-dimensional periodicity of the wake, consistent with the modest but consistent  $C_{\ell,rms}$  increment observed in Fig. 7.

At the optimum Grooved  $AR = 0.75$ , the first two modes together capture 96.2% ( $54.5 + 41.7\%$ ) of the total energy, demonstrating that the geometric synergy between the groove and the intermediate bluntness level preserves near-perfect modal coherence while simultaneously achieving the highest  $C_{\ell,rms}$  of any evaluated configuration. From an energy harvesting perspective, this result is highly significant: the amplified aerodynamic forcing of the Grooved  $AR = 0.75$  configuration is entirely organised into a single dominant periodic oscillation at the Strouhal frequency. A mechanical resonator coupled to this wake would therefore be expected to experience a reliable, narrow-band harmonic forcing favourable for energy extraction through the lock-in mechanism [5].



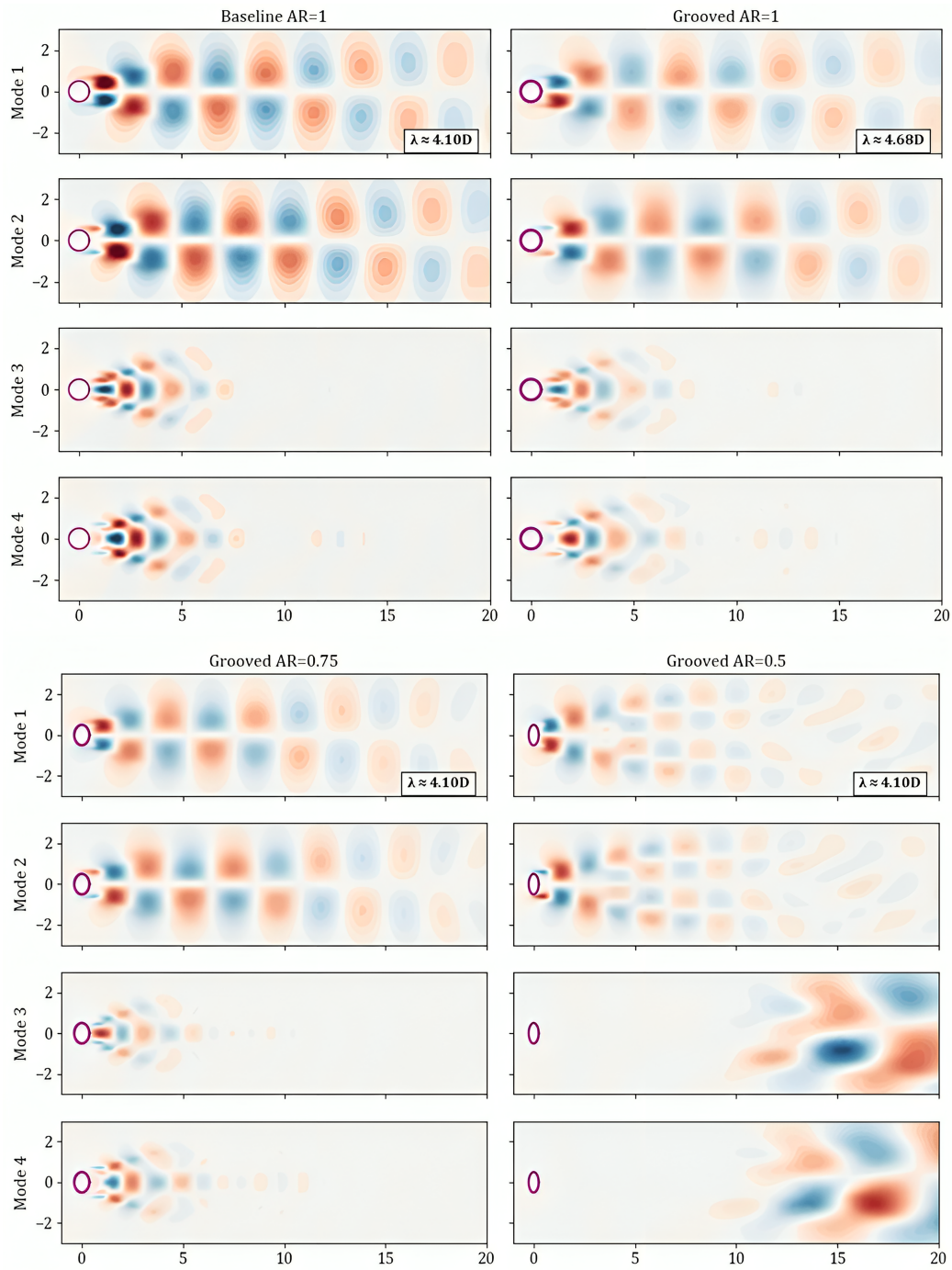
**Figure 10:** Proper orthogonal decomposition (POD) modal and cumulative energy distributions for the first four modes  $Re = 200$ .

A marked departure from this high-coherence regime occurs at Grooved  $AR = 0.50$ . The combined energy fraction of Modes 1 and 2 drops to 88.1% ( $49.1 + 39.0\%$ ), accompanied by a sharp anomalous increase in the energy scattered into higher-order structures: Modes 3 and 4 each carry 3.9% of the total energy, more than double their contribution in all other configurations. This spectral leakage into higher modes demonstrates that the extreme adverse pressure gradients associated with the  $AR = 0.50$  bluntness level overwhelm the organising influence of the biomimetic grooves, forcing the wake into a higher-dimensional, more complex dynamical state. As a consequence, the aerodynamic excitation at  $AR = 0.50$  loses the optimal narrow-band quality that is essential for efficient VIV energy conversion, fully elucidating why  $C_{\ell, rms}$  does not increase monotonically with further bluntness and confirming that the  $AR = 0.75$  configuration represents the optimum case in the present study.

### 3.3.2 POD Spatial Mode Structures

Fig. 11 presents the spatial contours of POD Modes 1 through 4 for the four evaluated configurations, providing a spatially resolved representation of the physical structures that carry the modal energy identified in the preceding subsection. The dominant vortex-street wavelength  $\lambda$ , annotated on the Mode 1 panels, is measured as the streamwise distance between two consecutive same-sign vorticity peaks. This wavelength is related to the shedding frequency through  $\lambda = U_c / f_s$ , where  $U_c$  is the mean convection velocity of the vortical structures. Introducing the Strouhal number definition yields  $\lambda/a = (U_c / U_\infty) / St$ . For laminar cylinder wakes at  $Re = 200$ , the convection velocity remains of the same order as the free-stream velocity ( $U_c$  in the range of  $0.7 - 0.9 U_\infty$ ) [48], so that  $\lambda$  scales approximately inversely with  $St$ . Accordingly, the wavelength variations across configurations are interpreted primarily in terms of relative changes in  $St$ .

For Baseline  $AR = 1.0$ , Modes 1 and 2 display the canonical alternating positive-negative vorticity patterns of the Kármán street, propagating coherently downstream with a dominant wavelength ( $\lambda$ ) of approximately  $4.10D$  that is directly related to the Strouhal shedding frequency through the convection velocity. Modes 3 and 4 exhibit low-amplitude spatial structures confined to the immediate near-wake, confirming their role as higher-harmonic corrections with negligible energetic significance.



**Figure 11:** Spatial contours of the first four POD modes for baseline  $AR = 1.0$ , grooved  $AR = 1.0$ , grooved  $AR = 0.75$ , and grooved  $AR = 0.50$   $Re = 200$ . The dominant vortex-street wavelength  $\lambda$  is annotated in the mode 1 panels.

The Grooved  $AR = 1.0$  configuration produces a noticeably longer dominant spatial wavelength of  $\lambda$  around  $4.68D$  in Modes 1 and 2, fully consistent with the lower Strouhal number ( $St = 0.1709$ ) induced by the groove modification. A lower shedding frequency at a given convection velocity corresponds directly to a larger spatial period between consecutive vortex cores, and the POD spatial structure provides independent geometric confirmation of the spectral shift observed in Fig. 9.

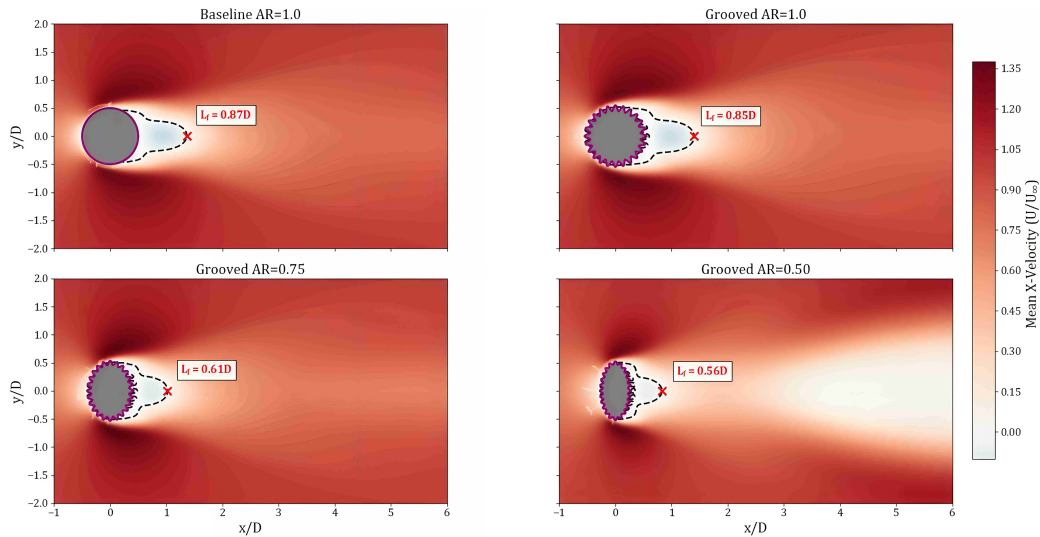
At Grooved  $AR = 0.75$ , the dominant wavelength returns to approximately  $4.10D$ , consistent with the Strouhal normalisation to  $St = 0.1953$  documented in Section 3.2. The Mode 1 and 2 structures at  $AR = 0.75$  display intensified near-field vorticity relative to both  $AR = 1.0$  cases, reflecting the amplified vortex roll-up associated with the shorter recirculation zone. Critically, the anti-symmetric Kármán pattern propagates coherently throughout the downstream domain with no visible fragmentation, confirming the sustained structural integrity of the wake at this optimal bluntness level.

In sharp contrast, the Grooved  $AR = 0.50$  configuration reveals a progressive spatial breakdown in Modes 1 and 2: while the anti-symmetric pattern is established in the near-wake, the structures rapidly break down into disjointed, irregular clusters as they convect downstream, losing spatial coherence well within the computational domain. More strikingly, Modes 3 and 4 at  $AR = 0.50$  exhibit massively extended spatial footprints reaching far into the downstream wake, directly visualising the incoherent multi-scale vortical structures that absorb the anomalous higher-mode energy fraction identified in Fig. 10. This spatial fragmentation confirms that the aerodynamic energy at  $AR = 0.50$  is partially dispersed into structures that cannot coherently excite a 1-DOF structural resonator, providing a physics-based explanation for the energy harvesting performance ceiling at this bluntness level.

### 3.4 Mean Wake Topology and Instantaneous Dynamics

#### 3.4.1 Mean Recirculation Zone

Fig. 12 presents the time-averaged streamwise velocity contours for four configurations that span the full parameter space of Stage II: Baseline  $AR = 1.0$ , Grooved  $AR = 1.0$ , Grooved  $AR = 0.75$ , and Grooved  $AR = 0.50$ . The recirculation zone length  $L_f$  is annotated on each panel as the downstream distance from the cylinder base to the point where the time-mean streamwise velocity first recovers to zero along the wake centreline.



**Figure 12:** Mean wake topology and recirculation zone lengths ( $L_f$ ) for the evaluated geometric configurations  $Re = 200$ .

For the smooth circular cylinder,  $L_f = 0.87D$ , consistent with the established literature for laminar circular cylinder wakes at  $Re = 200$  [39]. The introduction of biomimetic grooves at  $AR = 1.0$  marginally

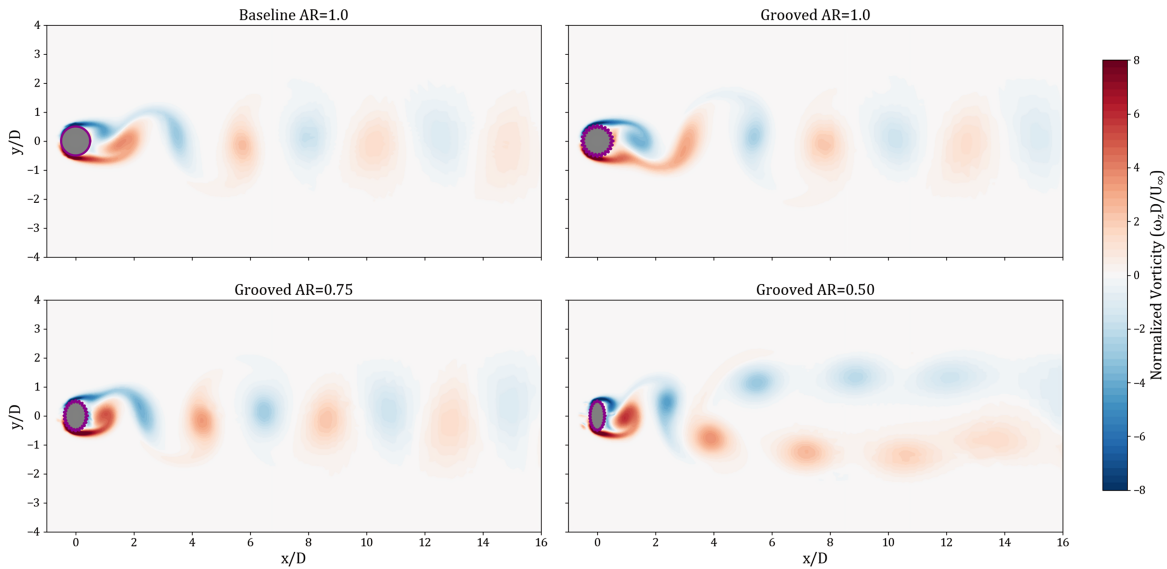


contracts the recirculation zone to  $L_f = 0.85D$ . This small but consistent reduction reflects the earlier shear-layer detachment promoted by the groove ridges, which slightly shortens the vortex formation region without fundamentally altering the global wake topology. This observation is consistent with prior findings for wavy cylinders in the laminar regime, in which surface undulations promote earlier separation through localised adverse pressure gradients without dramatically changing the time-averaged wake structure [13].

A fundamentally different topological regime emerges when the elliptical bluffness is introduced. At Grooved  $AR = 0.75$ , the combined effect of the shortened streamwise body and the groove perturbations drives a drastic contraction of the recirculation zone to  $L_f = 0.61D$ , representing a 30% reduction relative to the smooth circular baseline. This compact recirculation bubble implies that coherent vortex roll-up is completed significantly closer to the cylinder base. The vortices are therefore deposited into a high-intensity, near-body region that generates a correspondingly large alternating low-pressure suction force, directly driving the elevated  $C_{\ell, \text{rms}} = 0.7134$  of this configuration. At Grooved  $AR = 0.50$ , the recirculation zone further contracts to  $L_f = 0.56D$ , accompanied by a pronounced lateral broadening of the zero-velocity contour into a wide, flattened stagnation region. This topological change signals the onset of the saturation regime: the shear layers now detach so abruptly and symmetrically that the organised vortex roll-up process is partially disrupted, which accounts for the reduced  $C_{\ell, \text{rms}}$  relative to Grooved  $AR = 0.75$  despite the shorter  $L_f$ .

### 3.4.2 Instantaneous Vorticity Fields

Fig. 13 presents the instantaneous normalised vorticity contours for the same four configurations at a representative phase of the statistically steady shedding cycle. The qualitative evolution of the instantaneous wake structure across the four configurations provides direct visual evidence of the physical mechanisms identified in the force and topology data.



**Figure 13:** Instantaneous spanwise vorticity field at  $Re = 200$ , normalised as  $\omega_z^* = \omega_z D / U_\infty$ .

For Baseline  $AR = 1.0$ , the wake consists of a well-organised Kármán vortex street in which compact, alternating positive and negative vorticity cores convect smoothly downstream at regular spatial intervals, maintaining their structural integrity well into the far wake. The Grooved  $AR = 1.0$  configuration preserves this globally organised character but produces slightly more intensified and spatially compact vortex cores in

the near-wake region, consistent with the groove-induced acceleration of the roll-up process and the modest  $C_{\ell, \text{rms}}$  increment of 21.1%.

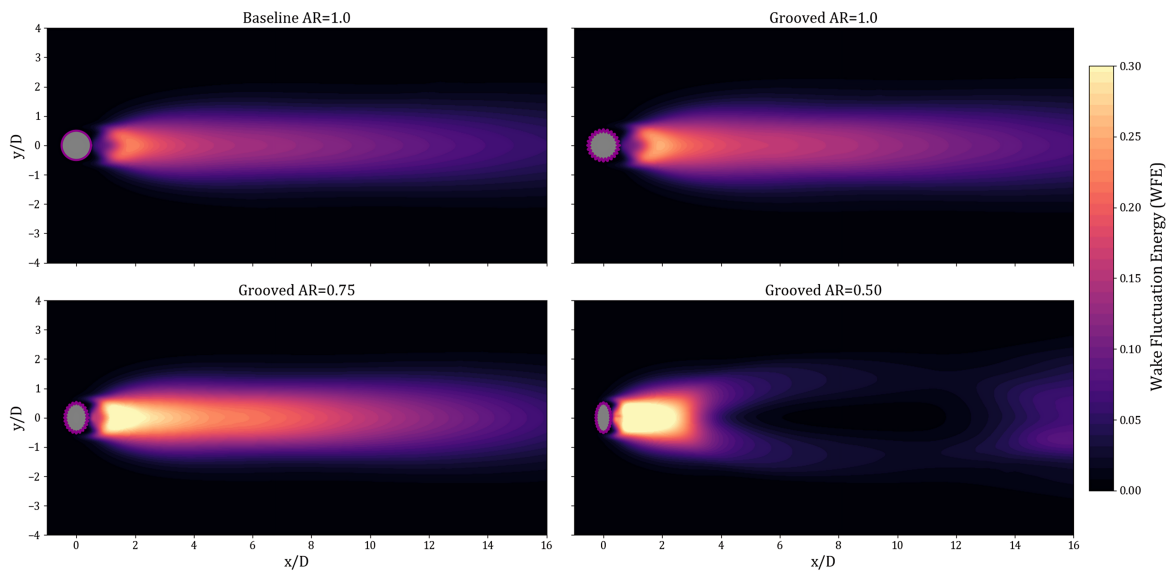
At the optimum Grooved  $AR = 0.75$ , the instantaneous vorticity field transforms markedly. The shear layers separate from the wider, shorter elliptical body and roll up into distinctly swollen, high-intensity vortex cores that form at a reduced downstream distance from the cylinder base. Despite the intensified vorticity magnitude, the shedding pattern retains a highly organised, anti-symmetric alternating character, confirming that the  $AR = 0.75$  bluffness level lies within the regime of coherent, narrow-band excitation required for efficient and sustained VIV lock-in. This combination of intensified forcing and retained structural coherence is the physical realisation of the geometric sweet spot identified in the force data.

At Grooved  $AR = 0.50$ , a qualitative breakdown in wake organisation becomes apparent. The vortex cores elongate and fragment as they convect downstream, losing the compact, well-defined structure observed in all higher aspect ratio configurations. The shear layers detach prematurely and fail to sustain fully coherent roll-up over the abbreviated streamwise body length, producing elongated, scattered vortical filaments rather than a clean alternating vortex pair. This structural fragmentation provides a direct physical explanation for the non-monotonic  $C_{\ell, \text{rms}}$  behaviour: at  $AR = 0.50$ , the bluffness is sufficient to shorten the recirculation zone and broaden the wake, but insufficient boundary-layer development length is available for the groove perturbations to organise the shear layer into the tight, high-intensity vortex cores that characterise the  $AR = 0.75$  optimum.

### 3.5 Energy-Oriented Wake Assessment

#### 3.5.1 Wake Fluctuation Energy Distribution

Fig. 14 presents the spatial distribution of the wake fluctuation energy ( $\text{WFE} = \frac{1}{2} \langle u'^2 + v'^2 \rangle / U_\infty^2$ ) for the four key Stage II configurations. Here WFE denotes the kinetic energy of the resolved periodic velocity fluctuations about the time mean, used purely as a fluctuation-energy diagnostic rather than a turbulence-model quantity. The WFE field quantifies the spatial density of the aerodynamic fluctuation energy in the wake and provides a direct spatial visualisation of the geometric amplification mechanism underlying the performance hierarchy.



**Figure 14:** Spatial distribution of wake fluctuation energy (WFE) in the wake region  $Re = 200$ .



For Baseline  $AR = 1.0$ , the WFE is distributed in elongated, parallel lobes extending far downstream along the shear-layer trajectories, with peak values concentrated approximately  $4D$ – $6D$  behind the cylinder base. The Grooved  $AR = 1.0$  configuration produces a slightly more compact and more intense WFE distribution compared to the smooth baseline, with the peak energy region displaced slightly upstream. This modest increase is consistent with the groove-induced acceleration of shear-layer roll-up and corresponds to the  $21.1\%C_{\ell,rms}$  increment identified in [Section 3.2](#).

At Grooved  $AR = 0.75$ , the WFE distribution undergoes a qualitative transformation. The high-intensity region concentrates into a wide bilobal structure immediately behind the cylinder base, with the peak WFE approximately two to three times higher than the Baseline  $AR = 1.0$  peak and located substantially closer to the body. This near-body intensification is a direct spatial consequence of the shortened recirculation zone ( $L_f = 0.61D$ ): coherent vortex roll-up is completed closer to the cylinder, depositing the fluctuation energy into a compact, high-density region that exerts a maximum alternating pressure force on the body. The lateral extent of the high-WFE lobes at  $AR = 0.75$  also broadens substantially relative to the circular cylinder cases, reflecting the wider separated shear layers produced by the vertically-oriented elliptical profile. Together, the near-body location, the elevated peak intensity, and the broader lateral extent of the WFE field at Grooved  $AR = 0.75$  provide the spatial energy signature of the aerodynamically optimum.

At Grooved  $AR = 0.50$ , the WFE peak moves even closer to the body ( $L_f = 0.56D$ ), and the wake broadens further in the lateral direction. However, the spatial organisation of the WFE field becomes less structured compared to  $AR = 0.75$ : the bilobal high-energy region diffuses more rapidly in the downstream direction, consistent with the vortex fragmentation and modal incoherence documented in [Section 3.3](#). This WFE diffusion means that a fraction of the available fluctuation energy at  $AR = 0.50$  is distributed into incoherent spatial scales that cannot be efficiently captured by a narrow-band structural resonator, providing the energy-field-level explanation for why Grooved  $AR = 0.50$  underperforms Grooved  $AR = 0.75$  despite greater geometric bluffness.

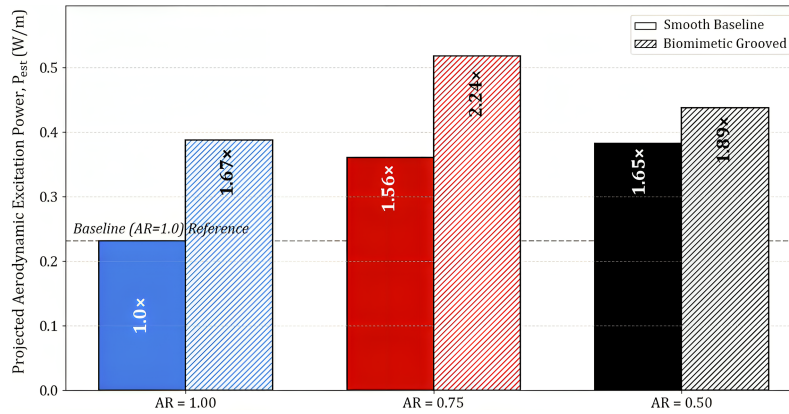
### 3.5.2 Projected Energy Harvesting Amplification

[Table 7](#) presents the complete aerodynamic and projected aerodynamic excitation metrics for all six Stage II configurations. The projected extractable power  $P_{est}$  is computed using the 1-DOF structural projection model ([Eq. \(8\)](#)) with representative structural parameters  $m^* = 2.0$  and  $\zeta = 0.05$ , which are representative of the low-mass-ratio and low-total-damping conditions typically employed in hydrokinetic VIV harvesters, where large-amplitude lock-in oscillations are commonly observed [\[49–51\]](#). Because the mass, damping ratio and natural frequency are held identical across all configurations, they cancel in every inter-configuration comparison, so that the amplification factor of any configuration relative to the smooth baseline depends only on the square of the ratio of their root-mean-square lift coefficients. The selected structural parameters therefore set only the absolute magnitude of  $P_{est}$  and have no influence on the relative ranking of the configurations.

[Fig. 15](#) illustrates the estimated harvested power and corresponding amplification factor for all six configurations relative to the smooth  $AR = 1.0$  baseline. The figure clearly reveals three distinct physical regimes. In the first regime, represented by the  $AR = 1.0$  pair, groove application alone raises  $P_{est}$  from  $0.2313$  to  $0.3873$  W/m (1.67-fold), demonstrating that the inverted biomimetic groove paradigm is effective even on the canonical circular geometry. In the second regime, the intermediate bluffness of  $AR = 0.75$  produces the optimum: Grooved  $AR = 0.75$  achieves  $P_{est} = 0.5184$  W/m, a 2.24-fold amplification that exceeds all other configurations. In the third regime, the maximum bluffness of  $AR = 0.50$  produces a diminishing return: despite the higher drag penalty ( $\overline{C_d} = 1.987$ ), the Grooved  $AR = 0.50$  achieves only 1.89-fold amplification, decisively below the  $AR = 0.75$  result.

**Table 7:** Summary of aerodynamic and projected aerodynamic excitation metrics  $Re = 200$ .

| Configuration        | Type    | $\overline{C_d}$ | $C_{\ell,rms}$ | St     | $P_{est}$ (W/m) | Ampl. |
|----------------------|---------|------------------|----------------|--------|-----------------|-------|
| Baseline $AR = 1.0$  | Smooth  | 1.333            | 0.4765         | 0.1953 | 0.2313          | 1.00× |
| Baseline $AR = 0.75$ | Smooth  | 1.663            | 0.6314         | 0.2197 | 0.3609          | 1.56× |
| Baseline $AR = 0.50$ | Smooth  | 1.891            | 0.6126         | 0.1953 | 0.3823          | 1.65× |
| Grooved $AR = 1.0$   | Grooved | 1.487            | 0.5768         | 0.1709 | 0.3873          | 1.67× |
| Grooved $AR = 0.50$  | Grooved | 1.987            | 0.6555         | 0.1953 | 0.4376          | 1.89× |
| Grooved $AR = 0.75$  | Grooved | 1.806            | 0.7134         | 0.1953 | 0.5184          | 2.24× |

**Figure 15:** Projected aerodynamic excitation power and normalized energy amplification, referenced to the smooth circular baseline ( $AR = 1.0$ ), across the evaluated configurations  $Re = 200$ .

The counter-intuitive underperformance of Grooved  $AR = 0.50$  relative to Grooved  $AR = 0.75$  is the central finding of this study and is fully explained by the convergent evidence from all five analysis tools. From the force data (Fig. 7), the groove  $C_{\ell,rms}$  increment collapses from 13.0% at  $AR = 0.75$  to 7.0% at  $AR = 0.50$ . From the spectral analysis (Fig. 9), the groove has no influence on the Strouhal number at  $AR = 0.50$ , indicating that the groove perturbation cannot modify the shedding dynamics at this bluntness level. From the mean wake topology (Fig. 12), the recirculation zone at  $AR = 0.50$  is so compressed ( $L_f = 0.56D$ ) that insufficient boundary-layer development length remains for the groove ridges to coherently trip the shear layer. From the instantaneous vorticity (Fig. 13), the vortex roll-up at  $AR = 0.50$  is spatially fragmented and structurally incoherent. From the POD analysis (Fig. 10), the two-mode energy fraction drops from 96.2% at  $AR = 0.75$  to 88.1% at  $AR = 0.50$ , and from the WFE maps (Fig. 14), the spatial energy at  $AR = 0.50$  diffuses downstream into incoherent scales unsuitable for narrow-band mechanical coupling. Together, these five lines of evidence form a physically coherent and self-consistent narrative: the Grooved  $AR = 0.75$  geometry represents the optimal balance point in a three-way competition between geometric bluntness for elevated force magnitude, groove-induced shear-layer tripping for enhanced coherence, and boundary-layer development length for sustained micro-vortex interaction.

This optimum is established among the three aspect ratios evaluated ( $AR = 1.0$ ,  $0.75$ , and  $0.50$ ). The non-monotonic variation of  $C_{\ell,rms}$  and  $P_{est}$ , which rise from  $AR = 1.0$  to a maximum at  $AR = 0.75$  and then fall at  $AR = 0.50$ , brackets an optimum in the neighbourhood of  $AR = 0.75$ , whose precise location would require a finer aspect-ratio sweep. Accordingly,  $AR = 0.75$  is reported as the best-performing configuration among the geometries tested rather than as a globally optimal aspect ratio.

To quantify the proposed boundary-layer interaction mechanism, the key wake and boundary-layer diagnostics are consolidated in Table 8 for the four configurations: the normalised recirculation length  $L_f/D$ , the base suction  $-\overline{C}_{p,\text{base}}$  (the magnitude of the time-mean pressure coefficient at the rear stagnation point), the boundary-layer thickness relative to the groove amplitude  $\delta/A_g$  (with  $A_g = D \cdot AR \cdot \text{Amp}$  and  $\delta$  from the laminar boundary-layer scaling), and the two-mode POD energy fraction  $E_{\text{POD}}$  as a measure of shedding coherence. While  $L_f/D$  contracts and  $\delta/A_g$  grows monotonically with bluffness, both  $C_{\ell,\text{rms}}$  and  $-\overline{C}_{p,\text{base}}$  peak at  $AR = 0.75$  and decline at  $AR = 0.50$ , where  $E_{\text{POD}}$  also drops sharply from 96.2% to 88.1%.

**Table 8:** Quantitative wake and boundary-layer diagnostics underlying the performance hierarchy at  $Re = 200$ .

| Configuration        | $C_{\ell,\text{rms}}$ | $L_f/D$ | $-\overline{C}_{p,\text{base}}$ | $\delta/A_g$ | $E_{\text{POD}}$ (%) |
|----------------------|-----------------------|---------|---------------------------------|--------------|----------------------|
| Baseline $AR = 1.00$ | 0.4765                | 0.87    | 0.911                           | –            | 96.5                 |
| Grooved $AR = 1.00$  | 0.5768                | 0.85    | 0.921                           | 5.0          | 96.2                 |
| Grooved $AR = 0.75$  | 0.7134                | 0.61    | 1.445                           | 5.8          | 96.2                 |
| Grooved $AR = 0.50$  | 0.6555                | 0.56    | 1.138                           | 7.1          | 88.1                 |

This non-monotonic response of the aerodynamic indicators to monotonically increasing geometric bluffness suggests that the groove-organised shedding saturates beyond  $AR = 0.75$ : further bluffness reduction to  $AR = 0.50$  does not strengthen it, identifying  $AR = 0.75$  as the best-performing configuration among those evaluated.

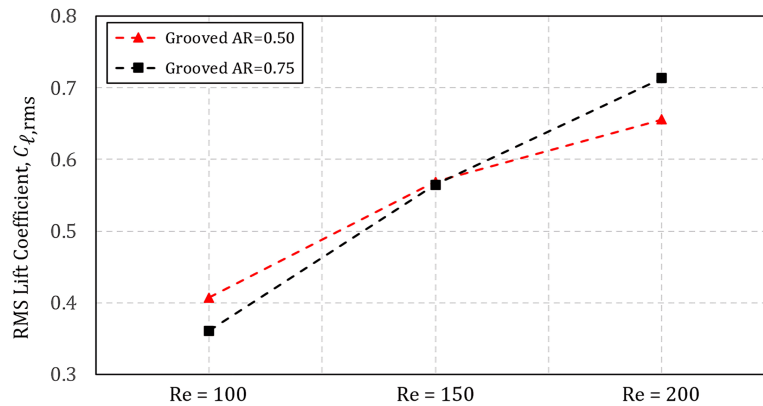
### 3.6 Effect of Reynolds Number on Geometric Optimum

The analysis presented in Sections 3.1–3.5 establishes Grooved  $AR = 0.75$  as the geometric optimum at  $Re = 200$ . A natural question arises as to whether this optimum persists across the laminar regime or whether it is confined to the specific Reynolds number examined. To address this question, the two Grooved configurations identified as the leading candidates for energy harvesting ( $AR = 0.75$  and  $AR = 0.50$ ) and their corresponding smooth counterparts are re-evaluated in Stage III at two additional Reynolds numbers,  $Re = 100$  and  $Re = 150$ , that span the lower half of the laminar deployment window relevant to ultra-low-speed hydrokinetic micro-harvesters. The resulting aerodynamic metrics and the corresponding groove contribution,  $\Delta C_{\ell,\text{rms}}$ , defined as the percentage increment of the grooved case over its smooth counterpart at the same aspect ratio and Reynolds number, are summarised in Table 9.

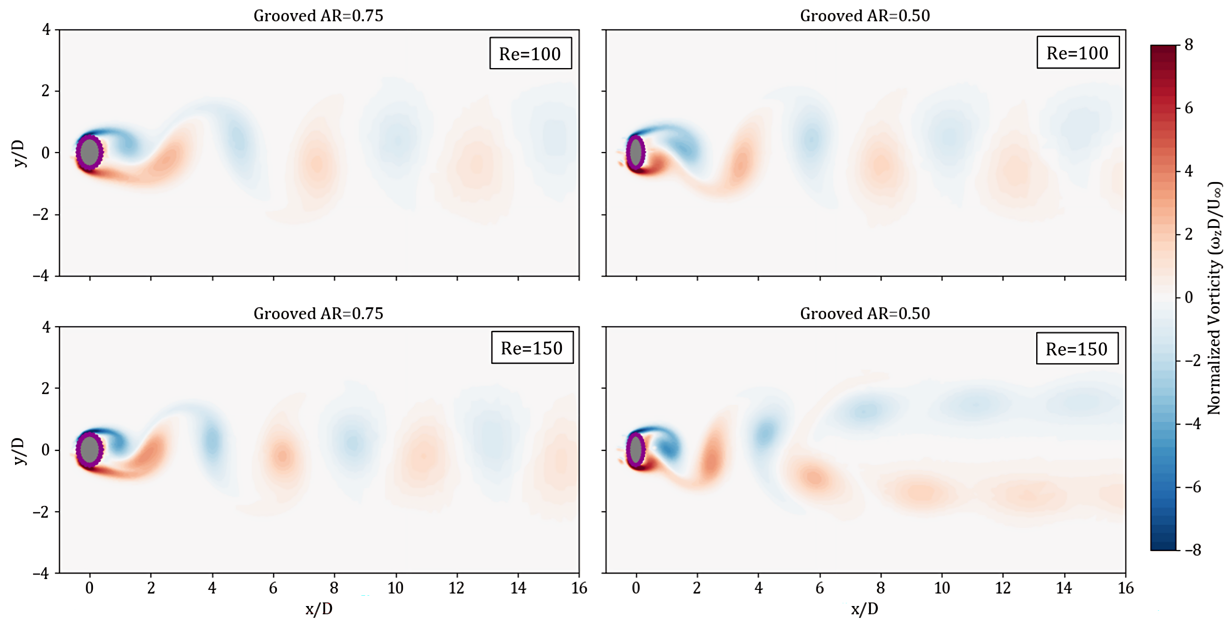
Two complementary observations emerge from Table 9. First, the groove contribution  $\Delta C_{\ell,\text{rms}}$  at  $AR = 0.75$  remains narrowly confined to the 13.0%–14.7% band across the full Reynolds range, whereas the corresponding contribution at  $AR = 0.50$  drops from 9.22% at  $Re = 100$  to 6.98% at  $Re = 150$  and 7.00% at  $Re = 200$ . The saturation mechanism governing the groove effectiveness at  $AR = 0.50$  is therefore not an artefact of the Reynolds number selected in the main analysis but a robust geometric feature of the laminar regime. Second, despite this uniformly stronger groove response at  $AR = 0.75$ , the *absolute*  $C_{\ell,\text{rms}}$  does not rank the two configurations identically at all Reynolds numbers. At  $Re = 100$ , the absolute excitation is higher for Grooved  $AR = 0.50$  (0.4074) than for Grooved  $AR = 0.75$  (0.3610); at  $Re = 150$ , the two configurations produce nearly identical values (0.5687 vs. 0.5645); and at  $Re = 200$ , the ordering reverses, with Grooved  $AR = 0.75$  (0.7134) clearly outperforming Grooved  $AR = 0.50$  (0.6555). This Reynolds-dependent ordering is visualised in Fig. 16.

**Table 9:** Effect of Reynolds number on aerodynamic excitation and groove contribution for the grooved  $AR = 0.75$  and grooved  $AR = 0.50$  configurations.

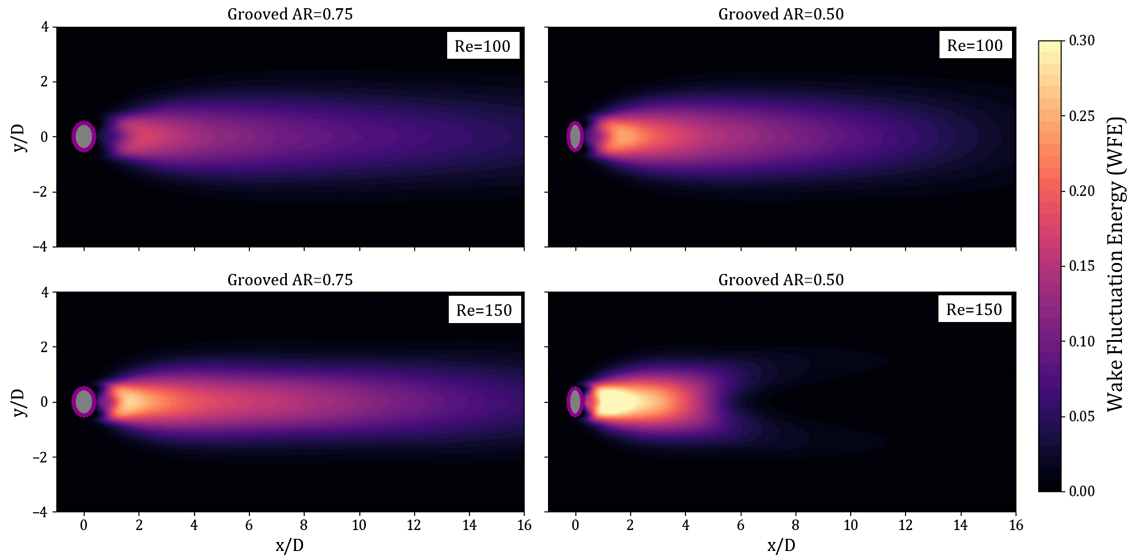
| Configuration       | Re  | $\overline{C_d}$ | $C_{\ell,rms}$ | St     | $P_{est}$ (W/m) | $\Delta C_{\ell,rms}$ (%) |
|---------------------|-----|------------------|----------------|--------|-----------------|---------------------------|
| Smooth $AR = 0.75$  | 100 | 1.5192           | 0.3148         | 0.1709 | 0.1153          | Ref.                      |
|                     | 150 | 1.5875           | 0.4952         | 0.1953 | 0.2498          | Ref.                      |
|                     | 200 | 1.6630           | 0.6314         | 0.2197 | 0.3609          | Ref.                      |
| Grooved $AR = 0.75$ | 100 | 1.6173           | 0.3610         | 0.1709 | 0.1517          | 14.68                     |
|                     | 150 | 1.7093           | 0.5645         | 0.1953 | 0.3246          | 13.99                     |
|                     | 200 | 1.8060           | 0.7134         | 0.1953 | 0.5184          | 12.99                     |
| Smooth $AR = 0.50$  | 100 | 1.8060           | 0.3730         | 0.1953 | 0.1417          | Ref.                      |
|                     | 150 | 1.8689           | 0.5316         | 0.1953 | 0.2878          | Ref.                      |
|                     | 200 | 1.8910           | 0.6126         | 0.1953 | 0.3823          | Ref.                      |
| Grooved $AR = 0.50$ | 100 | 1.8800           | 0.4074         | 0.1709 | 0.1932          | 9.22                      |
|                     | 150 | 1.9343           | 0.5687         | 0.1953 | 0.3295          | 6.98                      |
|                     | 200 | 1.9870           | 0.6555         | 0.1953 | 0.4376          | 7.00                      |

**Figure 16:** Root-mean-square lift coefficient ( $C_{\ell,rms}$ ) as a function of Reynolds number for grooved  $AR = 0.75$  and grooved  $AR = 0.50$ .

The physical mechanism underlying this crossover is clarified by the instantaneous vorticity and WFE distributions presented in Figs. 17 and 18. At  $Re = 100$  (upper rows), both configurations sustain a well-organised Kármán vortex street, with compact alternating vortex cores convecting smoothly downstream. Grooved  $AR = 0.50$  produces visibly larger and more intense vortex cores than Grooved  $AR = 0.75$ , consistent with its stronger absolute bluntness and higher  $C_{\ell,rms}$  at this Reynolds number. The corresponding WFE distribution at  $Re = 100$  shows a broader and moderately more energetic wake for Grooved  $AR = 0.50$ , fully consistent with the absolute force ranking. In this low-Reynolds regime, the boundary layer is sufficiently thick that the groove ridges interact coherently with the separating shear layer at both aspect ratios, and the unmodified bluntness advantage of  $AR = 0.50$  translates directly into a higher alternating lift magnitude.



**Figure 17:** Instantaneous normalised vorticity fields at  $Re = 100$  (upper row) and  $Re = 150$  (lower row) for grooved  $AR = 0.75$  and grooved  $AR = 0.50$ . The vorticity is normalised as  $\omega_z^* = \omega_z D / U_\infty$ .



**Figure 18:** Spatial distribution of wake fluctuation energy (WFE) at  $Re = 100$  (upper row) and  $Re = 150$  (lower row) for grooved  $AR = 0.75$  and grooved  $AR = 0.50$ . The corresponding  $Re = 200$  distributions are shown in [Fig. 14](#).

A qualitatively different picture emerges at  $Re = 150$ . Grooved  $AR = 0.75$  continues to exhibit the clean, alternating Kármán structure observed at  $Re = 100$ , with compact vortex cores and regular downstream convection. In contrast, the Grooved  $AR = 0.50$  wake begins to lose its structural integrity: the vortex cores elongate, the alternating spacing becomes irregular, and early signs of the spatial fragmentation documented at  $Re = 200$  in [Section 3.4](#) start to appear. The WFE field at  $Re = 150$  reflects this transition: although the two peak intensities are comparable, the  $AR = 0.50$  distribution exhibits a downstream diffusion tail that



is absent from the  $AR = 0.75$  case. These qualitative changes at  $Re = 150$ , together with the near-identical  $C_{\ell, \text{rms}}$  values of the two configurations, indicate the onset of the boundary-layer saturation mechanism at  $AR = 0.50$ , partially offsetting its bluntness advantage.

Beyond this point, the saturation mechanism becomes fully developed. At  $Re = 200$ , the wake at  $AR = 0.50$  is spatially fragmented, modally incoherent (cumulative two-mode energy fraction of 88.1%), and characterised by a diffused WFE field, as documented in [Sections 3.3–3.5](#). Simultaneously, the  $AR = 0.75$  configuration retains the compact near-body WFE footprint and the high modal coherence that together define the geometric optimum. The Reynolds-dependent crossover is therefore not a coincidence but a direct consequence of the progressive thinning of the laminar boundary layer with increasing  $Re$ : once the boundary layer becomes sufficiently thin, the compressed streamwise extent of the  $AR = 0.50$  body can no longer accommodate coherent groove-to-shear-layer interaction, and the groove effectiveness collapses relative to  $AR = 0.75$ .

This boundary-layer mechanism can be expressed more quantitatively. Using the classical laminar boundary-layer scaling  $\delta_{99}(x) = 5\sqrt{\nu x/U_{\infty}}$  [52] and taking the streamwise body extent as  $x = b = AR \cdot D$ , the normalized boundary-layer thickness is expected to vary approximately with  $\sqrt{AR/Re}$ . Therefore, the boundary layer for the  $AR = 0.50$  body is approximately 29% thinner at  $Re = 200$  than at  $Re = 100$  (since  $\sqrt{100/200} = 0.71$ ). As the boundary layer thins, the compressed streamwise extent of the  $AR = 0.50$  body provides progressively less development length for coherent interaction with the groove ridges, whose amplitude relative to  $D$  is fixed at  $Amp \cdot AR = 0.025$ . This trend is consistent with the measured collapse of the groove contribution at  $AR = 0.50$  from 9.22% at  $Re = 100$  to 7.0% at  $Re = 200$ , whereas the longer  $AR = 0.75$  body appears to retain a boundary layer sufficiently thick relative to the ridge scale to sustain coherent tripping, with its groove contribution remaining between 12.99% and 14.68% across the Reynolds range investigated in the present study.

The combined evidence from [Table 9](#), [Fig. 16](#), and the vorticity and WFE fields of [Figs. 17 and 18](#) leads to a clear and physically coherent conclusion. The saturation of the groove contribution at  $AR = 0.50$  is a robust feature of the laminar regime and manifests at every Reynolds number examined. However, the absolute geometric optimum for VIV energy harvesting is Reynolds-number dependent:  $AR = 0.50$  delivers the stronger excitation at  $Re = 100$ , the two configurations converge at  $Re = 150$ , and  $AR = 0.75$  takes over as the optimum at  $Re = 200$  and, by extrapolation of the saturation trend, throughout the upper half of the laminar regime. For the hydrokinetic micro-harvester deployment conditions that motivated the present study, in which the operating Reynolds number typically lies in the upper laminar range, the Grooved  $AR = 0.75$  configuration identified in the main analysis is therefore confirmed as the physically robust best-performing configuration among those evaluated.

#### 4 Conclusion

This study investigated the passive geometric amplification of aerodynamic excitation forces for VIV energy harvesting through a two-stage computational framework combining Taguchi L9 groove optimisation and fixed-cylinder laminar CFD. Six configurations spanning three smooth baselines and three optimally grooved elliptical cylinders at  $AR = 1.0, 0.75$ , and  $0.50$  were evaluated at  $Re = 200$  through aerodynamic force analysis, spectral characterisation, wake topology, instantaneous vorticity, POD modal decomposition, and a 1-DOF structural projection model. The analysis was subsequently extended to  $Re = 100$  and  $Re = 150$  to establish the robustness of the identified geometric optimum across the laminar deployment window.

The Taguchi L9 analysis conclusively identified groove amplitude as the dominant control parameter, with the influence of wave number remaining negligible within the evaluated range. The optimal groove ( $N = 24$ ,  $Amp = 5\%$ ) delivers a 21% increase in  $C_{\ell, \text{rms}}$  and a 12.5% downward Strouhal shift on the circular

baseline, broadening the structural lock-in range for hydrokinetic micro-harvester deployment at ultra-low flow velocities.

Contrary to the conventional role of biomimetic grooves as drag reduction and vibration suppression devices, the present study demonstrates that when biomimetic grooves are combined with an optimally bluff elliptical geometry, they can amplify coherent aerodynamic excitation and increase the projected aerodynamic excitation power relevant to VIV energy harvesting. The most significant and physically novel finding of the  $Re = 200$  analysis is the identification of a geometric sweet spot at  $AR = 0.75$ . The Grooved  $AR = 0.75$  configuration achieves the highest projected aerodynamic excitation power ( $P_{est} = 0.5184$  W/m), corresponding to a 2.24-fold amplification relative to the smooth circular baseline and outperforming the more aggressively bluff Grooved  $AR = 0.50$  configuration (1.89-fold). This counter-intuitive result is not a numerical artefact but a physically coherent outcome supported by five independent lines of evidence: the groove  $C_{\ell,rms}$  increment collapses from 13.0% to 7.0% as  $AR$  decreases from 0.75 to 0.50; groove-induced Strouhal modification disappears at  $AR = 0.50$ ; the recirculation zone at  $AR = 0.50$  is too compressed ( $L_f = 0.56D$ ) to permit coherent groove-shear-layer interaction; the instantaneous vortex roll-up at  $AR = 0.50$  fragments spatially; and the POD two-mode energy fraction drops from 96.2% ( $AR = 0.75$ ) to 88.1% ( $AR = 0.50$ ), with anomalous energy leakage into higher-order incoherent structures. Together, these observations confirm that at  $AR = 0.50$  the shortened streamwise body dimension suppresses the groove tripping mechanism, establishing  $AR = 0.75$  the best-performing interaction case among the evaluated aspect ratios at  $Re = 200$ .

The Reynolds-number sensitivity analysis conducted at  $Re = 100$  and  $150$  refines and generalises this finding. The groove contribution at  $AR = 0.75$  remains narrowly confined to the 12.99%–14.68% band across the full Reynolds range, whereas the corresponding contribution at  $AR = 0.50$  drops from 9.22% at  $Re = 100$  to approximately 7% at  $Re = 150$  and  $Re = 200$ . The boundary-layer saturation mechanism that suppresses the groove effectiveness at  $AR = 0.50$  is therefore a robust geometric feature of the laminar regime rather than a Reynolds-specific artefact. At the same time, the absolute geometric optimum is Reynolds-number dependent:  $AR = 0.50$  delivers the stronger excitation at  $Re = 100$  on account of its unmodified bluntness advantage in the thicker boundary-layer regime, the two configurations converge at  $Re = 150$ , and  $AR = 0.75$  emerges as the optimum at  $Re = 200$  once the boundary layer has thinned sufficiently for the saturation mechanism to be fully activated. This indicates that the geometric optimum shifts from  $AR = 0.50$  to  $AR = 0.75$  at  $Re = 150$ , corresponding to the onset of the boundary-layer saturation mechanism. For hydrokinetic micro-harvester deployment conditions in the upper laminar range, the Grooved  $AR = 0.75$  configuration is therefore confirmed as the physically robust geometric optimum.

A further structural advantage of the Grooved  $AR = 0.75$  configuration is the restoration of the Strouhal number from  $St = 0.2197$  (smooth  $AR = 0.75$ ) to  $St = 0.1953$  following groove application, aligning the shedding frequency with the circular cylinder reference value. A VIV harvester resonator tuned for the standard circular geometry can therefore be deployed directly with the Grooved  $AR = 0.75$  body without structural retuning, a practical advantage for scalable device implementation.

The present study is subject to several limitations. The simplified 1-DOF projection model uses constant mass and damping parameters and does not resolve the coupled fluid-structure dynamics. Future work should therefore extend the framework to fully coupled simulations that quantify lock-in, added mass, and reduced-velocity effects on the actual energy conversion efficiency. A finer aspect-ratio sweep near  $AR = 0.75$  and a co-optimisation of the groove parameters at each aspect ratio and Reynolds number would refine the identified geometric optimum. Three-dimensional simulations would clarify the geometric synergy and whether geometry-dependent differences in the transition threshold could shift the absolute optimum. A finer Reynolds-number sweep between  $Re = 150$  and  $Re = 200$  would localise the crossover Reynolds

number. Experimental validation in a water-channel facility would consolidate the practical viability of the proposed concept.

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## Nomenclature

The following symbols and abbreviations are used in this manuscript:

### Roman Symbols

|                         |                                                        |
|-------------------------|--------------------------------------------------------|
| $a$                     | Vertical dimension of the cylinder (cross-flow), m     |
| $A_g$                   | Groove amplitude ( $A_g = D AR Amp$ ), m               |
| $Amp$                   | Non-dimensional groove amplitude, %                    |
| $AR$                    | Aspect ratio, $b/a$                                    |
| $b$                     | Horizontal dimension of the cylinder (streamwise), m   |
| $\overline{C_p}$        | Mean pressure coefficient                              |
| $\overline{C_{p,base}}$ | Mean base pressure coefficient                         |
| $C_d$                   | Drag coefficient                                       |
| $\overline{C_d}$        | Time-averaged mean drag coefficient                    |
| $C_\ell$                | Instantaneous lift coefficient                         |
| $C_{\ell,rms}$          | Root-mean-square lift coefficient                      |
| $C_{p,est}$             | Estimated power coefficient                            |
| $D$                     | Reference length ( $D = a$ ), m                        |
| $E_{POD}$               | Two-mode POD energy fraction, %                        |
| $f_s$                   | Vortex shedding frequency, Hz                          |
| $F_L$                   | Instantaneous cross-flow lift force per unit span, N/m |
| $L$                     | Total perimeter of the base ellipse, m                 |
| $L_f$                   | Recirculation zone length, m                           |
| $m$                     | Effective oscillating system mass, kg                  |
| $m^*$                   | Mass ratio, $m/m_d$                                    |
| $N$                     | Integer wave number                                    |
| $P_{est}$               | Estimated extractable mechanical power, W/m            |
| $Re$                    | Reynolds number                                        |
| $St$                    | Strouhal number                                        |
| $T$                     | Integration interval over shedding cycles, s           |
| $u', v'$                | Velocity fluctuations about the time mean, m/s         |

|            |                                      |
|------------|--------------------------------------|
| $U_c$      | Mean vortex convection velocity, m/s |
| $U_\infty$ | Free-stream velocity, m/s            |

### Greek Symbols

|              |                                                         |
|--------------|---------------------------------------------------------|
| $\delta$     | Laminar boundary-layer thickness, m                     |
| $\Delta t$   | Time step, s                                            |
| $\zeta$      | Total structural-plus-electrical damping ratio          |
| $\theta$     | Angular position from the leading stagnation point, rad |
| $\lambda$    | Dominant vortex-street wavelength, m                    |
| $\nu$        | Kinematic viscosity, $\text{m}^2/\text{s}$              |
| $\rho$       | Fluid density, $\text{kg}/\text{m}^3$                   |
| $\omega_n$   | Natural angular frequency, rad/s                        |
| $\omega_z$   | Spanwise (out-of-plane) vorticity, $\text{s}^{-1}$      |
| $\omega_z^*$ | Normalised spanwise vorticity ( $\omega_z D/U_\infty$ ) |

### Abbreviations

|        |                                               |
|--------|-----------------------------------------------|
| 1-DOF  | One-Degree-of-Freedom                         |
| CFL    | Courant-Friedrichs-Lewy                       |
| FSI    | Fluid-Structure Interaction                   |
| PISO   | Pressure-Implicit with Splitting of Operators |
| POD    | Proper Orthogonal Decomposition               |
| PSD    | Power Spectral Density                        |
| TS     | Time Step Size                                |
| VIV    | Vortex-Induced Vibration                      |
| VIVACE | Vortex Induced Vibration Aquatic Clean Energy |
| WFE    | Wake Fluctuation Energy                       |

### References

- Bernitsas MM, Raghavan K, Ben-Simon Y, Garcia EMH. VIVACE (vortex induced vibration aquatic clean energy): a new concept in generation of clean and renewable energy from fluid flow. *J Offshore Mech Arct Eng*. 2008;130(4):041101. doi:10.1115/1.2957913.
- Zhao M. A review of recent studies on the control of vortex-induced vibration of circular cylinders. *Ocean Eng*. 2023;285:115389. doi:10.1016/j.oceaneng.2023.115389.
- Bakhtiar S, Khan FU, Fu H, Hajjaj AZ, Theodossiadis S. Fluid flow-based vibration energy harvesters: a critical review of state-of-the-art technologies. *Appl Sci*. 2024;14(23):11452. doi:10.3390/app142311452.
- Cao D, He J, Zeng H, Zhu Y, Chan SZ, Williams MR, et al. A review of oscillators in hydrokinetic energy harnessing through vortex-induced vibrations. *Fluids*. 2025;10(4):78. doi:10.3390/fluids10040078.
- Williamson CHK, Govardhan R. Vortex-induced vibrations. *Annu Rev Fluid Mech*. 2004;36(1):413–55. doi:10.1146/annurev.fluid.36.050802.122128.
- Sarpkaya T. A critical review of the intrinsic nature of vortex-induced vibrations. *J Fluids Struct*. 2004;19(4):389–447. doi:10.1016/j.jfluidstructs.2004.02.005.
- Khalak A, Williamson CHK. Motions, forces and mode transitions in vortex-induced vibrations at low mass-damping. *J Fluids Struct*. 1999;13(7–8):813–51. doi:10.1006/jfls.1999.0236.
- Wang J, Geng L, Ding L, Zhu H, Yurchenko D. The state-of-the-art review on energy harvesting from flow-induced vibrations. *Appl Energy*. 2020;267(15):114902. doi:10.1016/j.apenergy.2020.114902.
- Moradi MA, Mojra A. Flow and noise control of a cylinder using grooves filled with porous material. *Phys Fluids*. 2024;36(4):045134. doi:10.1063/5.0205125.
- Zhao F, Zeng L, Bai H, Alam MM, Wang Z, Dong Y, et al. Vortex-induced vibration of a sinusoidal wavy cylinder: the effect of wavelength. *Phys Fluids*. 2024;36(8):081909. doi:10.1063/5.0219753.

11. Talley S, Mungal G, Iaccarino G. Flow around cactus-shaped cylinders. In: Annual research briefs. Stanford, CA, USA: Center for Turbulence Research, Stanford University; 2001. p. 363–74.
12. Zdravkovich MM. Flow around circular cylinders. Vol. 1, Fundamentals. New York, NY, USA: Oxford University Press; 1997.
13. Lam K, Lin YF. Effects of wavelength and amplitude of a wavy cylinder in cross-flow at low Reynolds numbers. *J Fluid Mech.* 2009;620:195–220. doi:10.1017/S0022112008004217.
14. Babu P, Mahesh K. Aerodynamic loads on cactus-shaped cylinders at low Reynolds numbers. *Phys Fluids.* 2008;20(3):035112. doi:10.1063/1.2887982.
15. Talley S, Iaccarino G, Mungal G, Mansour NN. An experimental and computational investigation of flow past cacti. In: Annual research briefs. Stanford, CA, USA: Center for Turbulence Research, NASA Ames/Stanford University; 2001. p. 51–63.
16. Williamson CHK. Vortex dynamics in the cylinder wake. *Annu Rev Fluid Mech.* 1996;28(1):477–539. doi:10.1146/annurev.fl.28.010196.002401.
17. Shi X, Alam MM, Bai H. Wakes of elliptical cylinders at low Reynolds number. *Int J Heat Fluid Flow.* 2020;82(1):108553. doi:10.1016/j.ijheatfluidflow.2020.108553.
18. Park J, Kwon K, Choi H. Numerical solutions of flow past a circular cylinder at Reynolds numbers up to 160. *KSME Int J.* 1998;12(6):1200–5. doi:10.1007/BF02942594.
19. Jackson CP. A finite-element study of the onset of vortex shedding in flow past variously shaped bodies. *J Fluid Mech.* 1987;182:23–45. doi:10.1017/S0022112087002234.
20. Shi X, Alam MM, Zhu H, Ji C, Bai H, Sharifpur M. Flow three-dimensionality of wavy elliptic cylinder: vortex shedding bifurcation. *Ocean Eng.* 2024;301(1):117527. doi:10.1016/j.oceaneng.2024.117527.
21. Pradhan A, Arif MR, Afzal MS, Gazi AH. On the origin of forces in the wake of an elliptical cylinder at low Reynolds number. *Environ Fluid Mech.* 2022;22(6):1307–31. doi:10.1007/s10652-022-09892-z.
22. Kim J, Choi H. Distributed forcing of flow over a circular cylinder. *Phys Fluids.* 2005;17(3):033103. doi:10.1063/1.1850151.
23. Bearman PW. Vortex shedding from oscillating bluff bodies. *Annu Rev Fluid Mech.* 1984;16(1):195–222. doi:10.1146/annurev.fl.16.010184.001211.
24. Law YZ, Jaiman RK. Passive control of vortex-induced vibration by spanwise grooves. *J Fluids Struct.* 2018;78(4):297–315. doi:10.1016/j.jfluidstructs.2018.08.004.
25. Huang S. VIV suppression of a two-degree-of-freedom circular cylinder and drag reduction of a fixed circular cylinder by the use of helical grooves. *J Fluids Struct.* 2011;27(7):1124–33. doi:10.1016/j.jfluidstructs.2011.07.005.
26. Zhou B, Wang X, Guo W, Gho WM, Tan SK. Experimental study on flow past a circular cylinder with rough surface. *Ocean Eng.* 2015;109(Pt. 1):7–13. doi:10.1016/j.oceaneng.2015.08.062.
27. Han P, Huang Q, Pan G, Qin D, Wang W, Goncalves RT, et al. Optimal energy harvesting efficiency from vortex-induced vibration of a circular cylinder. *Ocean Eng.* 2023;282(2):114869. doi:10.1016/j.oceaneng.2023.114869.
28. Fershalov A, Elvin N, Orlandini P, Avros I, Liu Y. Harvesting sustainable energy through vortex-induced vibrations of finite length cylinder. *Phys Fluids.* 2025;37(4):044108. doi:10.1063/5.0260317.
29. Zhang M, Wu T, Xu F. Vortex-induced vibration of bridge decks: describing function-based model. *J Wind Eng Ind Aerodyn.* 2019;195(1368):104016. doi:10.1016/j.jweia.2019.104016.
30. Morse TL, Williamson CHK. Prediction of vortex-induced vibration response by employing controlled motion. *J Fluid Mech.* 2009;634:5–39. doi:10.1017/S0022112009990516.
31. Zhang M, Zhang C, Abdelkefi A, Yu H, Gaidai O, Qin X, et al. Piezoelectric energy harvesting from vortex-induced vibration of a circular cylinder: effect of Reynolds number. *Ocean Eng.* 2021;235(1):109378. doi:10.1016/j.oceaneng.2021.109378.
32. Barkley D, Henderson RD. Three-dimensional Floquet stability analysis of the wake of a circular cylinder. *J Fluid Mech.* 1996;322:215–41. doi:10.1017/S0022112096002777.
33. Hasheminejad SM, Jarrahi M. Numerical simulation of two dimensional vortex-induced vibrations of an elliptic cylinder at low Reynolds numbers. *Comput Fluids.* 2015;107(1):25–42. doi:10.1016/j.compfluid.2014.10.011.



34. Hu Y, Yuan H, Shu S, Niu X, Li M. An improved momentum exchanged-based immersed boundary–lattice Boltzmann method by using an iterative technique. *Comput Math Appl*. 2014;68(3):140–55. doi:10.1016/j.camwa.2014.05.013.
35. Rajani BN, Kandasamy A, Majumdar S. Numerical simulation of laminar flow past a circular cylinder. *Appl Math Model*. 2009;33(3):1228–47. doi:10.1016/j.apm.2008.01.017.
36. Celik Y. Surrogate-based robust design of asymmetric splitter plates for aerodynamic performance efficiency of flatback airfoils. *Phys Fluids*. 2026;38(4):047108. doi:10.1063/5.0325553.
37. Celik Y, Ingham D, Ma L, Kiziloglu BN, Pourkashanian M. Numerical analysis of unsteady performance of an aerofoil with surface openings under Darrieus motion. *Wind Struct*. 2026;42(3):329–55. doi:10.12989/was.2026.42.3.329.
38. Taira K, Brunton SL, Dawson STM, Rowley CW, Colonius T, McKeon BJ, et al. Modal analysis of fluid flows: an overview. *AIAA J*. 2017;55(12):4013–41. doi:10.2514/1.J056060.
39. Posdziech O, Grundmann R. A systematic approach to the numerical calculation of fundamental quantities of the two-dimensional flow over a circular cylinder. *J Fluids Struct*. 2007;23(3):479–99. doi:10.1016/j.jfluidstructs.2006.09.004.
40. Henderson RD. Nonlinear dynamics and pattern formation in turbulent wake transition. *J Fluid Mech*. 1997;352:65–112. doi:10.1017/S0022112097007465.
41. Williamson CHK. Oblique and parallel modes of vortex shedding in the wake of a circular cylinder at low Reynolds numbers. *J Fluid Mech*. 1989;206:579–627. doi:10.1017/S0022112089002429.
42. Taguchi G. System of experimental design: engineering methods to optimize quality and minimize costs. Vol. 1 & 2. White plains, NY, USA: UNIPUB/Kraus International Publications; 1987.
43. Erkan O, Ozkan M, Celik Y, Khalid MSU. Taguchi-based multi-factor analysis of self-starting behavior in vertical axis wind turbine farms. *Energy*. 2025;341(1–3):139495. doi:10.1016/j.energy.2025.139495.
44. Lumley JL. The structure of inhomogeneous turbulent flows. In: Yaglom AM, Tatarski VI, editors. *Atmospheric turbulence and radio wave propagation*. Moscow, Russia: Nauka; 1967. p. 166–78.
45. Berkooz G, Holmes P, Lumley JL. The proper orthogonal decomposition in the analysis of turbulent flows. *Annu Rev Fluid Mech*. 1993;25(1):539–75. doi:10.1146/annurev.fl.25.010193.002543.
46. Sirovich L. Turbulence and the dynamics of coherent structures. Part I: coherent structures. *Q Appl Math*. 1987;45(3):561–71. doi:10.1090/qam/910462.
47. Jie H, Liu YZ. Large eddy simulation of turbulent flow over a cactus-analogue grooved cylinder. *J Vis*. 2015;18(2):171–88. doi:10.1007/s12650-015-0294-x.
48. Lin JC, Hsieh SC. Convection velocity of vortex structures in the near wake of a circular cylinder. *J Eng Mech*. 2003;129(10):1108–18. doi:10.1061/(ASCE)0733-9399(2003)129:10(1108).
49. Sun H, Kim ES, Nowakowski G, Mauer E, Bernitsas MM. Effect of mass-ratio, damping, and stiffness on optimal hydrokinetic energy conversion of a single, rough cylinder in flow induced motions. *Renew Energy*. 2016;99(6):936–59. doi:10.1016/j.renene.2016.07.024.
50. Pal A, Soti AK. Power harvesting from VIV of rigidly-coupled cylinders in tandem arrangement. In: *Fluid mechanics and fluid power, Volume 5. Lecture notes in mechanical engineering*. Singapore: Springer; 2023. p. 621–30. doi:10.1007/978-981-19-7055-9\_53.
51. Barrero-Gil A, Pindado S, Avila S. Extracting energy from vortex-induced vibrations: a parametric study. *Appl Math Model*. 2012;36(7):3153–60. doi:10.1016/j.apm.2011.09.085.
52. Schlichting H, Gersten K. *Boundary-layer theory*. 9th ed. Berlin, Germany: Springer; 2017. doi:10.1007/978-3-662-52919-5.