

Generation mechanism of wake galloping in two staggered circular cylinders in view of hysteretic flow phenomena

Thu Dao¹; Tomomi Yagi, Ph.D.²; Kyohei Noguchi, Ph.D.³; Haruki Fukushima⁴; Gabriel Mohallem⁵; Tung Do⁶

¹Ph.D. Candidate, Dept. of Civil and Earth Resources Eng., Kyoto Univ., Kyoto daigaku-katsura, Nishikyo-ku, Kyoto, 615-8540, Japan (Corresponding author)
E-mail: dao.thu.r42@kyoto-u.jp

²Professor, Dept. of Civil and Earth Resources Eng., Kyoto Univ., Kyoto daigaku-katsura, Nishikyo-ku, Kyoto, 615-8540, Japan
E-mail: yagi.tomomi.7a@kyoto-u.ac.jp

³Assistant Professor, Dept. of Civil and Earth Resources Eng., Kyoto Univ., Kyoto daigaku-katsura, Nishikyo-ku, Kyoto, 615-8540, Japan
E-mail: noguchi.kyohei.7z@kyoto-u.ac.jp

⁴Former Graduate Student, Dept. of Civil and Earth Resources Eng., Kyoto Univ., Kyoto daigaku-katsura, Nishikyo-ku, Kyoto, 615-8540, Japan
E-mail: fukushima.haruki.73c@kyoto-u.jp

⁵ Former Student, Dept. of Civil and Earth Resources Eng., Kyoto Univ., Kyoto daigaku-katsura, Nishikyo-ku, Kyoto, 615-8540, Japan
Email: mohallemferreira.gabriel.m17@kyoto-u.jp

⁶Former Graduate Student, Dept. of Civil and Earth Resources Eng., Kyoto Univ., Kyoto daigaku-katsura, Nishikyo-ku, Kyoto, 615-8540, Japan
Email: tungcive1995@gmail.com

Abstract

A smooth cylinder placed near the wake of an upstream smooth cylinder is susceptible to galloping. This study experimentally investigated the generation mechanism of wake galloping in two staggered cylinders of equal diameter D based on the physical flow phenomena occurring in the oscillating downstream cylinder. The horizontal center-to-center distance of the two cylinders ranged from $3.0D$ to $3.5D$ and the vertical center-to-center distance was $0.3D$ to $0.5D$. As observed, the wind velocity range in the smooth cylinder was within the subcritical Reynolds number regime, and the unsteady responses at the downstream exhibited prominent hysteresis that evolved with the reduced velocity, and ultimately, triggered wake galloping at the critical reduced velocity. In particular, this hysteretic behavior is native to certain arrangements of the two cylinders and occurs because of the interaction between the downstream cylinder and the wake of the upstream cylinder. Moreover, the hysteresis-based discussion could identify the potential wake galloping vibrations in certain arrangements that are beyond the scope of the current investigation. With the application of 12 spiral protuberances on the surface of both cylinders, the hysteresis vanished and wake galloping was suppressed.

Keywords: Twin cylinders; Staggered cylinders; Wake-induced vibration; Wake galloping; Spiral protuberances; Hysteresis; Flutter derivative

1. Introduction

In bridge engineering, two parallel-cylindrical structures—stayed cables and hangers—are commonly used, and their fluid–structure interaction significantly varies from that of a single cylinder. A pioneering study by [Zdravkovich \(1987\)](#) classified these interactions into four interference flow regions: (i) wake interference, where the downstream cylinder is immersed in the wake of an upstream cylinder; (ii) proximity interference, where a cylinder equally impacts the other; (iii) combined proximity and wake interference, where the downstream cylinder is situated in the immediate wake region; and (iv) no-interference region. Since then, more sophisticated classifications have been proposed based on the experimental data (forces and surface pressure) and flow visualizations ([Alam and Meyer, 2013](#); [Gu and Sun, 1999](#); [Igarashi, 1981](#); [Sumner et al., 2000](#); [Zhou et al., 2009](#); [Zhou and Mahbub Alam, 2016](#)).

In addition, field observations of wake-induced vibrations (WIVs) have been reported ([Deng et al., 2021](#); [Hardy and Van Dyke, 1995](#); [Laursen et al., 2006](#); [Saito et al., 2001](#)), and various experimental and numerical studies have been published. [Bokaian and Geoola \(1984a, 1984b\)](#) studied the free-response oscillations of twin circular cylinders in tandem as well as in staggered arrangements inside a water channel by fixing one of the two cylinders to the side walls and setting the other cylinder free to oscillate in the cross-flow direction. Depending on the arrangement, they observed mostly vortex-induced vibrations (VIVs) in the upstream cylinder, and either only VIVs or galloping, or both VIVs and galloping (separately or in an interfered manner) in the downstream cylinder. They referred to the galloping in the downstream cylinder as “wake-induced galloping,” which is a motion-induced vibration compared to the flow-induced VIVs. In addition, [Zdravkovich \(1985\)](#) conducted free-vibration wind tunnel tests on two identical flexibly mounted rigid circular cylinders, which enabled 2DOF responses in both the cylinders. He reported that “fluid-elastic oscillations,” i.e., large-amplitude oscillations at high reduced velocities are analogous to the wake-induced galloping in [Bokaian and Geoola \(1984b\)](#), and they were mostly observed in the downstream cylinder in the wake interference region, including certain discrete arrangements in the proximity interference region. [Prasanth and Mittal \(2009\)](#) employed numerical simulations to study the oscillations of tandem and staggered twin cylinders in laminar flows. They revealed a resemblance between the upstream cylinder and a single cylinder in terms of VIVs, whereas the downstream cylinder experienced large-amplitude oscillations at almost all reduced velocities during testing. [Kim et al. \(2009a\)](#) performed free-vibration wind tunnel tests on two spring-supported cylinders with equal diameter D in tandem arrangements. They observed that the oscillating amplitude downstream was remarkably larger than that observed upstream as the center-to-center distance exceeded $3.7D$. [Alam and Kim \(2009\)](#) covered an extensive range of staggered arrangements of two identical spring-supported cylinders and identified seven major response patterns comprising combinations of VIVs and galloping in both cylinders. They observed that six of the seven patterns involved vibrations in the downstream cylinder. [Qin et al. \(2017\)](#) extended this discussion to cases of two tandem cylinders with unequal diameters. The aforementioned studies suggested that the downstream cylinder is more susceptible to WIVs compared to the upstream cylinder. More specifically, [Fontaine et al. \(2006\)](#) classified the oscillations in tandem risers as (i) VIVs and (ii) wake-induced oscillations. In this study, the WIVs are analogous to the latter and are distinct from VIVs. In particular, WIVs are categorized into wake

galloping (WG) and wake-induced flutter (WIF) based on the oscillation trajectories. WG is a predominantly vertical oscillation that occurs in the downstream cylinder as the two cylinders are relatively proximate (Tokoro et al., 2000; Fukushima et al., 2021). Conversely, two cylinders spaced at $10D-20D$ experience 2DOF WIF in various staggered arrangements (Cooper and Wardlaw, 1971; Fukushima et al., 2021).

The above-mentioned classification of WIVs is based on distinct generation mechanisms. The successful application of quasi-steady forces for modeling WIF in certain arrangements validates the applicability of the quasi-steady theory only when the downstream cylinder is situated exterior to the wake interference and proximity regions (Fukushima et al., 2021; Hémon, 1999; Price and Abdallah, 1990; Simpson, 1971a, 1971b). In these two regions, small variations in spacing within these regions could produce significantly distinct flow patterns and static forces in the stationary downstream cylinder (Shiraishi et al., 1986; Zdravkovich and Pridden, 1977). Although Assi et al. (2013) introduced the concept of “wake stiffness” to explain the aerodynamic force characteristics, the static forces were inadequate for characterizing the intricate interfering effects. Instead, they can be described in more detail based on the unsteady forces. Comprehensively, Gu and Sun (1999), Matsumoto et al. (1990), Qin et al. (2019), Ruscheweyh and Dielen (1992), Shiraishi et al. (1986), and Zdravkovich (1988) attributed WG to the “flow switching” phenomenon that reflects the intermittent variations of distinct flow patterns during oscillation. Assi et al. (2010) identified the mechanism of WG as self-excited oscillations, in which the motion-induced unsteady wake feeds the energy to the downstream cylinder and causes it to violently oscillate in the cross-flow direction. Fukushima et al. (2021), Sakaki et al. (2017), and Yagi et al. (2015) adopted the aerodynamic flutter derivatives proposed by Sarkar et al. (1994) and Scanlan and Tomko (1971) to model the unsteady aerodynamic forces. They detected WG onset based on a flutter derivative H_1^* that is a vertical damping term and explored WIF through flutter analyses. In addition, Yang et al. (2022) employed an empirical nonlinear model for unsteady WG forces by applying the three-step nonlinear least-squares fitting method introduced by Gao and Zhu (2017). Moreover, their approach identified the aerodynamic damping term as a key factor in WG. The above discussions on WIV onset were mostly concerned with the reduced velocities. However, the WIVs involved other parameters as well. Li and Ishihara (2021) numerically evaluated the impact of the mass and spacing ratios on WIVs. Additionally, the well-defined dependence of a single cylinder on Reynolds number in cross-flow posed the question of whether the Reynolds number (Re) would influence WIVs. Okajima (1979) realized the transcritical Re regime by imposing two types of surface roughness only on the upstream cylinder. He observed Re transition in the flow surrounding the two stationary tandem cylinders. For further details on the Re -dependence of two stationary cylinders, refer to Dubois and Andrianne (2022), Schewe and Jacobs (2019), and Zhou et al. (2009). By broadening the discussion of Re to the instability phenomena based on the Den Hartog criterion, Schewe and Jacobs (2019) raised the possibility of vertical instabilities in the subcritical, supercritical, and transcritical Re regimes; however, they did not provide concrete evidence of free-response vibrations.

Furthermore, the discussion of Re raised concern on the instabilities of parallel cylinders with surface modification, which reportedly underwent a transition at a lower Re compared to a smooth cylinder. Kim et al. (2009b) reported the aerodynamic performance of two tandem cylinders attached to trip wires in various

spacing scenarios. Their discussions majorly focused on VIVs, and they noted that the tripping wires could either suppress or excite the vibrations depending on their angular positions, which exhibited multiple flow separation/reattachment with vortex shedding patterns. Their flow visualization provided insights into the dissimilarity of the modified interfered wake from that in the case of two smooth cylinders. [Yagi et al. \(2011\)](#) initially introduced a cylinder attached with 12 spiral protuberances on its surface to target a lower aerodynamic drag force. They reported promising performance against rain-and-wind-induced vibrations and dry galloping. Subsequently, [Sakaki et al. \(2017\)](#) adopted this design and continued to investigate its performance against WIVs in two tandem arrangements with center-to-center distances of $3D$ and $7D$. In all cases, the free vibration tests revealed that the WG did not occur in the downstream cylinder with spiral protuberances. Moreover, [Sakaki et al. \(2017\)](#) suggested that the spanwise three-dimensionality of the flow field surrounding the modified cylinder that stabilized it against vibrations but they did not provide further discussion. [Li and Ishihara \(2021\)](#) employed two tandem cylinders with a center-to-center distance of $4D$, wherein both the cylinders were attached with four helical wires following the prototype proposed by [Ishihara and Li \(2020\)](#). Their simulations yielded positive total damping on the downstream cylinder, and thus, WG suppression. Although they attributed the suppression mechanism of the helical wires to their impact on the gap flow and wake, they did not elaborate on this.

This study focuses on WG in a staggered downstream cylinder located in the wake-interference region, considering its exposure and vulnerability toward WG compared to the upstream cylinder reported in the existing literature. The target arrangements of the cylinders are as follows: streamwise center-to-center distance of $3D - 3.5D$ and cross-flow distance of $0.3D - 0.5D$, which is within the WG-sensitive regime following the classification map of the WIV mechanism proposed by [Fukushima et al. \(2021\)](#). We aim to bridge the gap between the mathematical models of WG and the physical flow phenomena, which will cultivate a more intuitive understanding of WG and add depth to the discussion of the validity of each modelling approach. More importantly, it will enable a more comprehensive explanation on the suppression of WG by the modified cylinders. We conducted a series of wind tunnel tests, and the procedures are described in Section 2. The cylinder responses in the spring-supported free vibration tests are described in Section 3. Thereafter, detailed discussion on the various aspects of the free-vibration test results with the aid of mean aerodynamic pressure in the stationary condition and unsteady aerodynamic forces/pressure to clarify the WG mechanism are presented in Section 4. Ultimately, the findings are summarized in Section 5.

2. Experimental apparatus

In this study, the experiment was conducted in a wind tunnel at Kyoto University. The wind tunnel is a room-circuit Eiffel type with a test section of 1.0 m width and 1.8 m height. In particular, the flow-generating capacity was 30 m/s. All wind tunnel tests in the campaign were performed with a smooth incoming flow of velocity U [m/s] ranging from 1 to 14 m/s with the smallest interval of 0.1 m/s, corresponding to $Re = 0.3 - 6.3 \times 10^4$. The specified wind velocities are disclosed along with the results in the following sections.

The campaign included spring-supported free-vibration tests, unsteady aerodynamic force measurement tests, and pressure measurement tests. The arrangement of the two parallel cylinders used in these tests is illustrated in Fig. 1. As the WG in the downstream cylinder is the main focus of this study, we recorded only the responses of the downstream cylinder, and the upstream cylinder was fixed to the walls of the wind tunnel. As depicted in Fig. 1, let $(X/D, Y/D)$ denote the initial dimensionless position of the downstream cylinder relative to the fixed upstream cylinder in the no-wind condition, where D [m] denotes the diameter of the cylinder, $(W/D, S/D)$ represents the equilibrium position of the downstream cylinder after the occurrence of the wind-induced static displacement. $(\xi/D, \eta/D)$ indicates the instantaneous dimensionless displacement of the downstream cylinder with respect to its equilibrium position during oscillation, and $(x/D, y/D)$ represents the same parameter but with respect to the fixed upstream cylinder. Thus, $x/D = W/D + \xi/D$ and $y/D = S/D + \eta/D$.

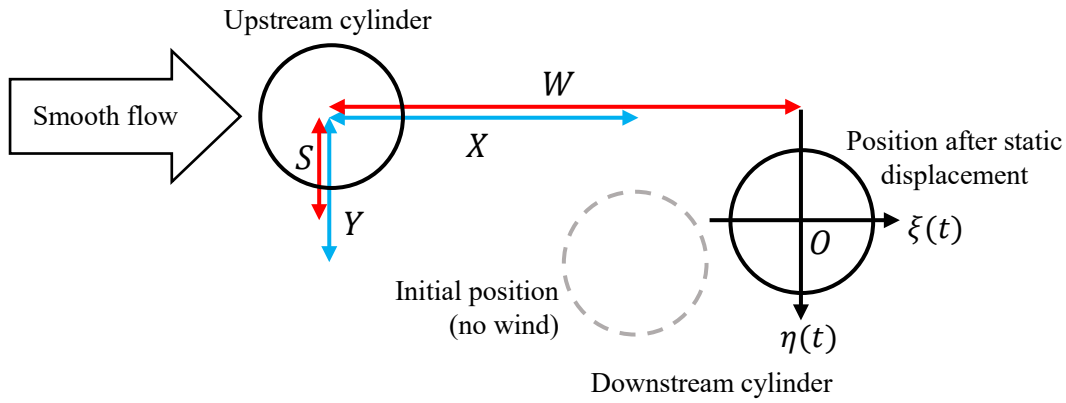


Fig. 1. Schematic arrangement of two cylinders and definitions of coordination axes.

All the test cylinders were aluminum pipes with lengths of $L = 900$ mm and two diameters: $D = 50$ mm and $D = 70$ mm with associated blockage ratios of 5% and 7%, respectively. To evaluate the performance of the modified cylinder with spiral protuberances, two types of cylinder surfaces were used in the experiment, namely, a smooth cylinder and a cylinder attached with 12 spiral protuberances (hereinafter, addressed as “spiral cylinder”). The specifications of the spiral cylinder were adopted from the previous studies of Sakaki et al. (2017) and Yagi et al. (2011). Specifically, the rubber protuberances had a rectangular cross-section of either 2.4 mm width by 1.5 mm height or 3.6 mm width by 2.1 mm height for pairing with $D = 50$ mm and $D = 70$ mm, respectively. This ensures the same dimension ratio of the protuberance to the cylinder, as in the original design proposed by Yagi et al. (2011). The protuberances were manually set on the surface of the smooth cylinder at a winding angle of $\gamma = 27^\circ$, as depicted in Fig. 2, where P [m] denotes the pitch of a spiral turn. The two types of surfaces comprised four arrangement combinations of two cylinders, as listed in Table 1. For instance, “spiral–circular” indicates the arrangement of a spiral cylinder upstream and a smooth cylinder downstream.

All displacements, forces, and pressure were digitally recorded using a data logger (GL7000; Graphtec, Japan) at a sampling frequency of 1 kHz. No specific cutoff frequency was used during data sampling outside the Nyquist frequency of 500 Hz. Upon checking the power spectral density of the signals, most of the signal

energy originated from (i) the motions of the cylinder in either the free vibration tests or the forced vibration tests, which were below 2 Hz, and (ii) vortex shedding, which occurred below 60 Hz at the maximum wind velocity. Therefore, all major aerodynamic information was well captured and preserved.

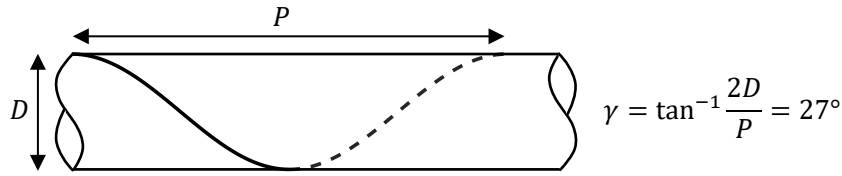


Fig. 2. Configuration of spiral protuberances.

Table 1

Four types of arrangements based on cylinder surfaces.

Arrangement	Upstream cylinder	Downstream cylinder
circular–circular	Smooth	Smooth
spiral–circular	With spiral protuberances	Smooth
circular–spiral	Smooth	With spiral protuberances
spiral–spiral	With spiral protuberances	With spiral protuberances

2.1. Spring-supported free vibration tests

The free-vibration tests used cylinders with $D = 50$ mm. The structural properties of the cylinders are listed in [Table 2](#), where the subscripts ξ and η denote the horizontal (along-the-flow) and vertical (cross-flow) directions, respectively. The model structural damping (i.e., the logarithmic decrement and Scruton number) was intentionally set at the lowest possible value to facilitate aerodynamic responses. Following the results of [Fukushima et al. \(2021\)](#), the downstream cylinder was initially located at $(X/D = 3.0, Y/D = 0.5)$ in which they observed free-response large amplitude oscillations in the downstream cylinder in the cross-flow direction for circular–circular configuration. The downstream cylinder was supported with four identical coil springs placed at each end to obtain an “X” shape. This setup enabled 2DOF oscillations with approximately identical horizontal and vertical natural frequencies as shown in [Table 2](#). The free-response oscillations of the downstream cylinder were captured using four laser displacement sensors (IL-300, Keyence, Japan) such that each end of the cylinder was mounted with two sensors, one of which was assigned to the vertical displacement, whereas the other measured the horizontal displacement.

Table 2

Structural properties of the downstream cylinders.

Equivalent mass		Natural frequency		Logarithmic decrement		Scruton number	
[kg/m]		[Hz]		[–]		[–]	
m_ξ	m_η	f_ξ	f_η	δ_ξ	δ_η	Sc_ξ	Sc_η
1.38	1.33	1.37	1.42	0.0054	0.0059	5.1	5.4

2.2. Unsteady aerodynamic force measurement tests (forced vibration tests)

The unsteady aerodynamic force measurements employed both 50- and 70-mm-diameter-type models to facilitate a wider range of reduced velocities. Unless the diameter is explicitly disclosed, all the results presented in Section 4 were evaluated from the 70-mm-diameter-type model.

Moreover, the downstream cylinder was mounted to the supporting frame, and was assigned with prescribed vertical harmonic oscillations $\eta(t)$ [m] using a motor (SD-400-11 A, Shimpo Industrial, Japan). The vibration frequency f_{vib} ranged from 0.673 to 2 Hz. The dimensionless double amplitude of the vibration was $2\eta_0/D = 0.4$. Both the downstream cylinder displacements and unsteady aerodynamic forces were simultaneously measured using two laser displacement sensors (IL-300, Keyence, Japan) and two load cells (LMC-3501-20 N, Nissho Electric Works, Japan) set at both ends. Overall, the duration of each measurement was 180 s. The post-processing of the measured unsteady aerodynamic forces comprised two steps: (i) dividing the time-series signal into separate cycles according to f_{vib} ; and (ii) ensemble-averaging these cycles into one representative cycle. This procedure not only successfully eliminated any singularities observed in the time series (i.e., the number of oscillating cycles at the lowest vibration frequency of 0.673 Hz was 121 cycles) but also removed the high frequency noise in the ensemble-averaged forces. The calculation of the flutter derivative H_1^* considers these ensemble-averaged unsteady forces, as expressed in Eq. (1):

$$H_1^* = -\frac{L_{\eta 0} \sin \psi_{L\eta}}{\rho (D/2)^2 \omega^2 \eta_0}, \quad (1)$$

where $L_{\eta 0}$ [N/m] denotes the amplitude of the ensemble-averaged unsteady aerodynamic lift force $L_{\eta}(t^*)$ [N/m], t^* [s] denotes the ensemble-averaged time instant, while t [s] indicates the real instantaneous time instant, $\psi_{L\eta}$ [deg.] represents the phase lag between the lift force $L_{\eta}(t^*)$ and displacement $\eta(t^*)$, ρ [kg/m³] denotes the air density, and $\omega = 2\pi f_{vib}$ [rad/s] represents the circular frequency. Additionally, to trace the flow phenomena during oscillation, the dimensionless unsteady lift $C_L(t^*)$ was calculated as follows:

$$C_L(t^*) = \frac{L_{\eta}(t^*)}{\frac{1}{2} \rho U^2 D} \quad (2)$$

It should be noted that the inertial force $F_{inertia}(t^*)$ [N/m] induced by the prescribed sinusoidal motions was excluded from the unsteady aerodynamic lift force $L_{\eta}(t^*)$ in Eqs. (2) and (3). The force on the downstream cylinder was obtained by conducting separate forced vibration tests on the cylinder at the target f_{vib} with no wind. The ensemble-averaged inertia $F_{inertia}(t^*)$ was obtained using the two-step post-processing described above. Finally, $F_{inertia}(t^*)$ was deducted from the ensemble-averaged unsteady forces obtained directly from the load cells to extract purely aerodynamic lift forces $L_{\eta}(t^*)$.

2.3. *Quasi-static force measurement tests*

As conducting aerostatic force measurement tests for various arrangements is time-intensive, quasi-static aerodynamic force measurement tests were utilized instead to obtain insights for the aerostatic force characteristics. The tests were vertical forced vibration tests conducted at $f_{vib} = 0.03$ Hz and $U = 12$ m/s which enabled extremely high reduced wind velocity of $U/fD = 5714$ to mimic the stationary condition. The setup and procedure were similar to those of the typical forced-vibration tests described in Section 2.2. The 50-mm-diameter cylinders were exclusively used for the tests. The equilibrium position of the downstream cylinder was located at $(X/D = 3.0, Y/D = 0.0)$, and the dimensionless double amplitude was $2\eta_0/D = 2.4$. The large double amplitude of 2.4 — the maximum capacity of our equipment — in the quasi-static force measurement tests was chosen to cover the largest possible range of vertical spacing scenarios. Additionally, quasi-static tests were also performed on the downstream cylinder at $(X/D = 3.0, Y/D = 1.0)$ with the same dimensionless double amplitude of 2.4 to verify the equilibrium position dependence of the forces, and thus potential motion-induced effects. The aerodynamic forces at $(X/D = 3.0, Y/D = 0.0)$ and $(X/D = 3.0, Y/D = 1.0)$ were well-matched where their vertical positions overlapped, which eliminated the equilibrium position dependence of the quasi-static forces; hence, the following discussions present only the quasi-static forces at $(X/D = 3.0, Y/D = 0.0)$. The extent to which these forces could represent the actual static forces is discussed in Section 4.2. The two-step signal processing procedure comprised ensemble-averaging of the periodic time-series signals, followed by a spatial averaging process through which the quasi-static lift at a specified position described the mean value of 50 data points both before and after it. For a sampling frequency of 1 kHz and $f_{vib} = 0.03$ Hz, the error of the spatial averaging was $y/D \pm 0.02D$.

2.4. *Aerostatic force measurement tests*

The aerostatic force measurement tests were conducted on the 70-mm-diameter-type cylinders at only three staggered arrangements ($X/D = 3.0, Y/D = [0.3; 0.5; 0.7]$). The setup and equipment were the same as those in the forced vibration test, except the downstream cylinder was securely fixed to the supporting frame. The duration of each measurement was 100 s, which was well-separated from the timescale of vortex shedding and thus was sufficient to yield representative time-averaged forces. The aerostatic lift coefficient $\bar{C}_L$ was obtained as follows:

$$\bar{C}_L = \frac{\bar{F}_L}{\frac{1}{2}\rho U^2 D}, \quad (3)$$

where $\bar{F}_L$ [N/m] is the time-averaged lift force per unit length.

2.5. Pressure measurement tests

In addition, a series of static and unsteady pressure measurement tests were conducted. The 70-mm-diameter pressure model is presented in Fig. 3. Twelve pressure taps were uniformly spaced along the circumference of the downstream cylinder to accommodate the surface protuberances between them. Although these coarsely placed pressure taps were unable to resolve the microscopic flow phenomena such as the flow separation and reattachment, they provided a glimpse at the schematic flow patterns surrounding the downstream cylinder. Furthermore, each pressure tap was connected to a multipoint-pressure sensor (ZOC17-OG/32Px-2.5 Pa, Ohte Giken Inc., Japan) using a 1.2-m-long vinyl tube. Subsequently, a multipoint pressure-measuring instrument (ZOC23AMP-A UNIT, Ohte Giken Inc., Japan) was used to record the surface pressure considering the variations in the reference static pressure inside the test section (obtained using a pitot tube placed under the models such that it did not disturb the flow field around the cylinders) and the readings from the pressure sensor. The pressure coefficient was evaluated as follows:

$$\bar{C}_p(\theta) = \frac{\bar{P}(\theta)}{\frac{1}{2}\rho U^2}, \quad (4)$$

where θ [deg.] denotes the angular position of the pressure tap, as defined in Fig. 3(b), and $\bar{P}(\theta)$ [Pa] represents the time-averaged measured pressure.

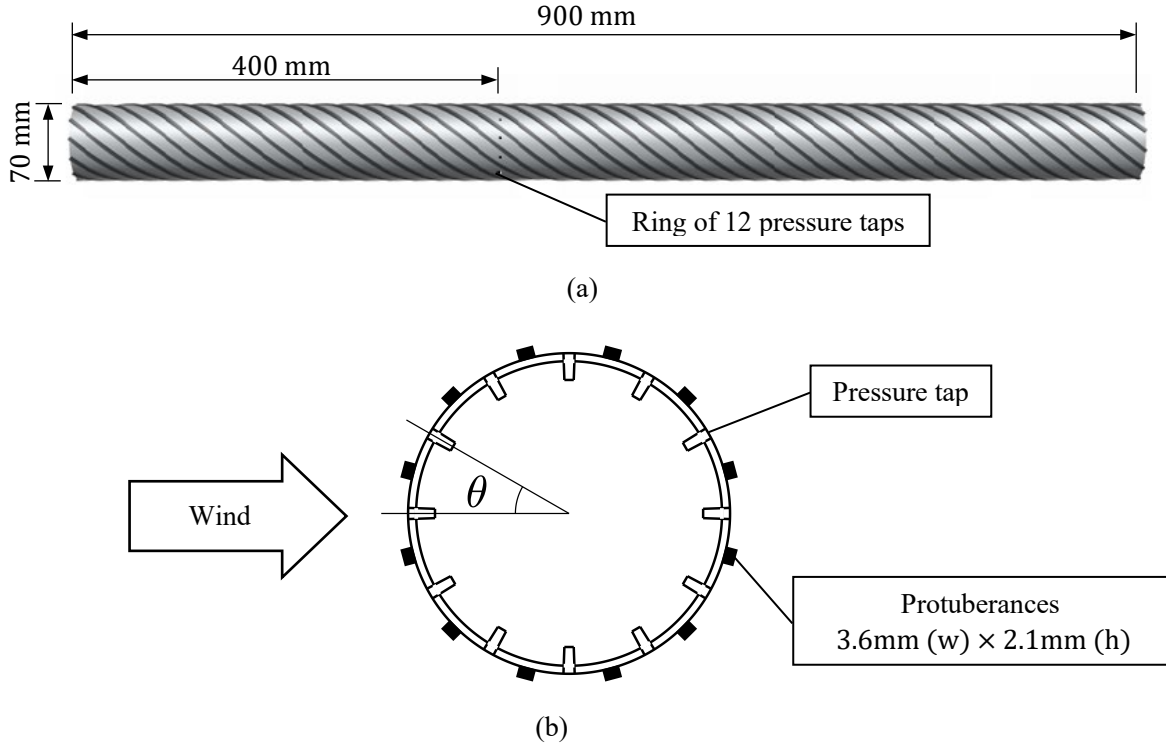


Fig. 3. Schematic of pressure model: (a) location of pressure ring; (b) arrangement of circumferential pressure taps.

The setup of the pressure measurement tests was similar to that of the unsteady aerodynamic force measurement tests (Section 2.2), where the upstream cylinder was fixed to the side walls and the downstream

cylinder was either fixed to measure the static pressure or harmonically oscillated to measure the unsteady pressure. In the unsteady pressure measurement tests, a time lag existed between the actual pressure response and the converted electrical signal. As observed, this time lag correlated with the length of the vinyl tube. Therefore, to specify the time lag, a phase-adjustment that utilized a pulsator at the downstream end of the wind tunnel was conducted to generate harmonic flow at f_{vib} . A 1.2-m-long vinyl tube was used to measure the flow-induced pressure, one end of which was set in the test section, while the other end was connected to a pressure sensor (ZOC17-OG/32Px-2.5 Pa, Ohte Giken Inc., Japan). The other reference signal was recorded using a semiconductor transducer (DD101K, Jtext Co., Japan). The transducer was placed at the same position as the vinyl tube in the test section and connected to the A/D converter via a pressure amplifier (AA6210, Jtext Co., Japan). Two signals from the vinyl tube and transducer were simultaneously recorded. The time lag was achieved by extracting the phase difference between them.

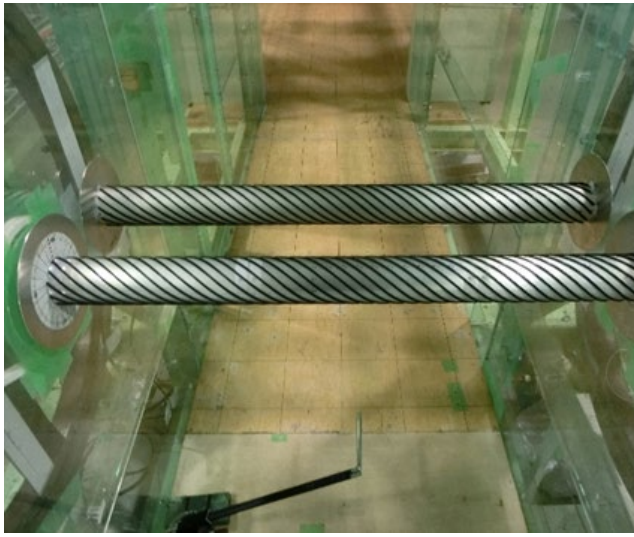


Fig. 4. Setup of parallel cylinders with spiral protuberances in pressure measurement tests.

3. Results

The amplitude responses of the downstream cylinder at $(X/D = 3.0, Y/D = 0.5)$ in the spring-supported free vibration tests are illustrated in Figs. 5–8. For circular–circular (Fig. 5) and spiral–circular (Fig. 6) configurations at $U/fD \geq 100$, even though the maximum horizontal amplitudes exceed the vertical ones, the oscillating patterns of the former are more likely buffeting rather than flutter — i.e., the horizontal amplitudes varied greatly, whereas the vertical amplitudes were stable. In particular, the circular–circular responses were confirmed to be vertical predominant by the trajectory traces of the downstream cylinder at $U = 12$ m/s presented by Fukushima et al. (2021). Therefore, the responses in Figs. 5 and 6 at $U/fD \geq 100$ are regarded as predominantly vertical oscillations, and f in the following discussion is denoted as f_η in Table 2, because we are interested in the vertical direction. Fukushima et al. (2021) identified such oscillations in the circular–circular configuration as WG. In this section, the term “WG” is used to refer to the oscillations in the spiral–circular configuration at $U/fD \geq 83$. In both cases, the WG amplitude diverged

at $U/fD \approx 200$. Conversely, no large-amplitude oscillations occurred in the circular–spiral (Fig. 7) or spiral–spiral (Fig. 8) configuration at $U/fD \geq 100$.

All four cases witnessed 2DOF oscillations at $U/fD < 100$. Fukushima et al. (2021) regarded such oscillations as 2DOF wake-induced flutter. The 2DOF oscillations in the spiral–circular case shared similarities with the circular–circular case, considering the amplitude ratio of the horizontal and vertical components and the reduced velocity range of occurrence at $20 \leq U/fD \leq 50$. In contrast, circular–spiral and spiral–spiral cases exhibited an increased amplitude of the 2DOF oscillations. Additionally, these 2DOF oscillations were extended over a wider range of reduced velocities, namely, $20 \leq U/fD \leq 90$ compared to those of circular–circular and spiral–circular. This unique phenomenon may be related to the aerodynamic characteristics of the spiral cylinder, constituted by the low $\bar{C}_D$ values in the subcritical Re regime and the suppression of Kármán vortices (Yagi et al., 2011). However, this theme is excluded from the scope of this discussion, considering only the WG at $U/fD \geq 100$.

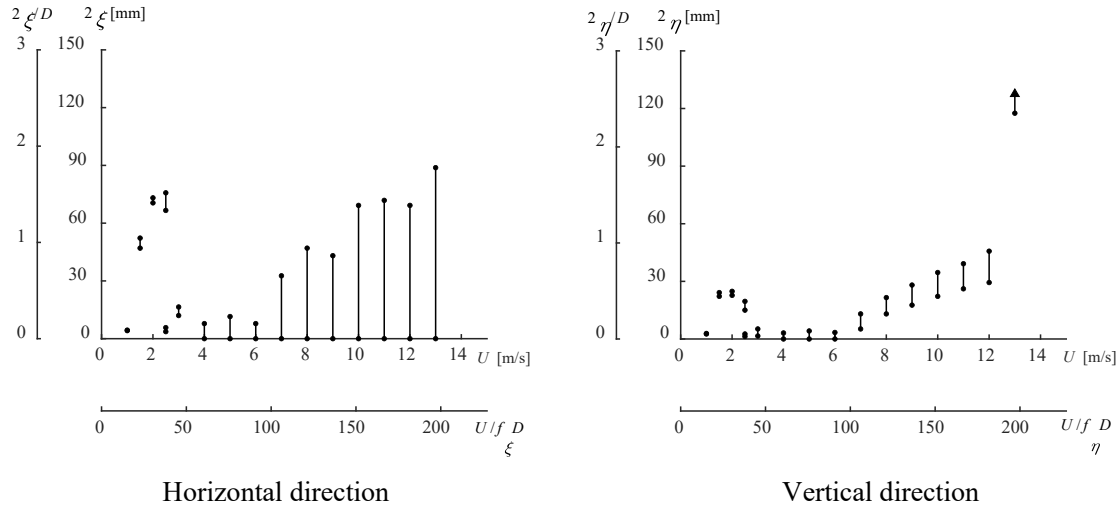


Fig. 5. Velocity–amplitude diagrams of circular–circular ($X/D = 3.0, Y/D = 0.5$) by Fukushima et al. (2021).

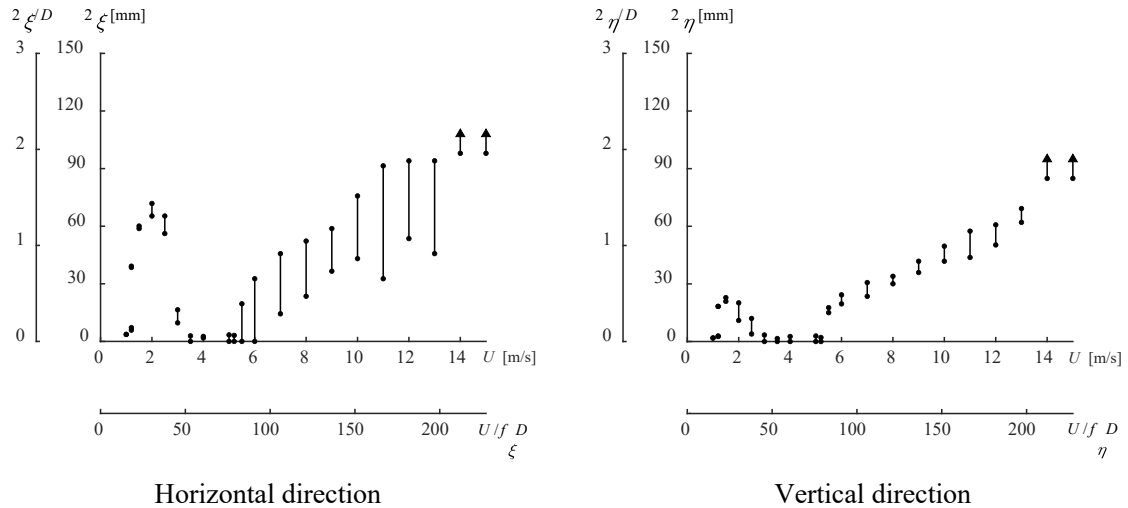


Fig. 6. Velocity–amplitude diagrams of spiral–circular ($X/D = 3.0, Y/D = 0.5$).

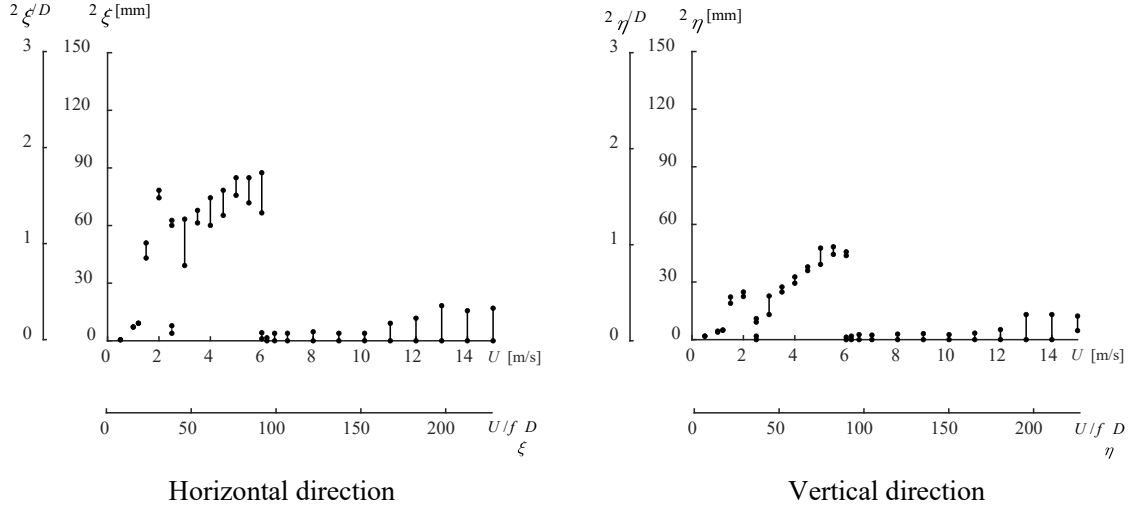


Fig. 7. Velocity–amplitude diagrams of circular–spiral ($X/D = 3.0, Y/D = 0.5$).

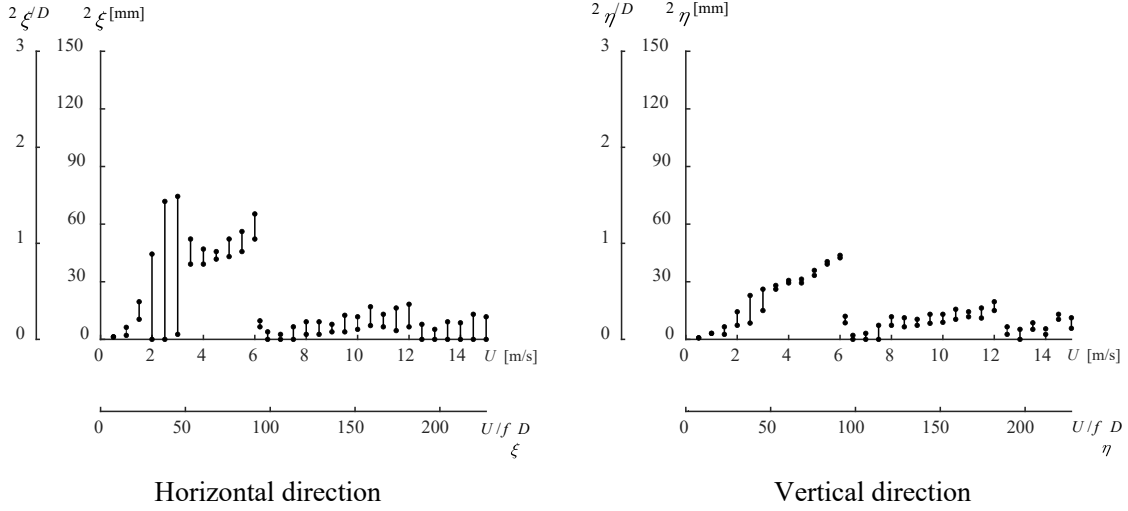


Fig. 8. Velocity–amplitude diagrams of spiral–spiral ($X/D = 3.0, Y/D = 0.5$).

In all cases, $U/fD \approx 100$ marked the threshold between the 2DOF wake-induced flutter and WG (where it existed). The static displacements of the downstream cylinders in the corresponding free-vibration responses are plotted in Fig. 9. For the circular–circular case, Fukushima et al. (2021) revealed that wind-induced static displacement initiated the “switch” from 2DOF oscillation to vertical flutter. The equilibrium position of the downstream smooth cylinder in the circular–circular case shifted to ($W/D = 3.0, S/D = 0.3$) at $U/fD = 91$ (Fig. 9(a)). Similarly, spiral–circular case experienced abrupt vertical static displacement to ($W/D = 3.0, S/D = 0.4$) immediately prior to the onset of WG (Fig. 9(b)) at $U/fD = 76$. However, the same did not hold for the circular–spiral (Fig. 9(c)) and spiral–spiral (Fig. 9(d)) cases. In detail, the equilibrium position dependence of the instabilities is explored in Section 4.

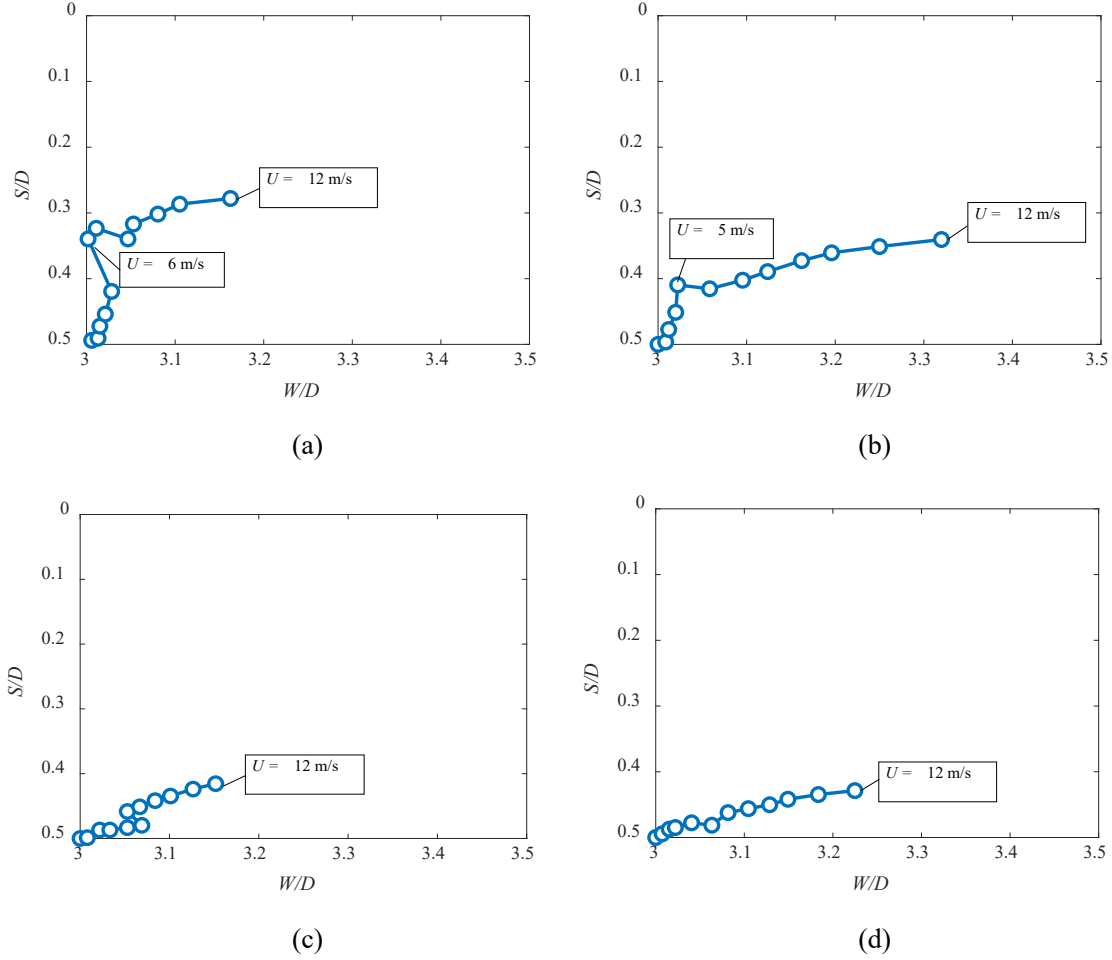


Fig. 9. Velocity-dependent equilibrium position of downstream cylinder at ($X/D = 3.0, Y/D = 0.5$). (a) circular–circular by [Fukushima et al. \(2021\)](#); (b) spiral–circular; (c) circular–spiral; (d) spiral–spiral.

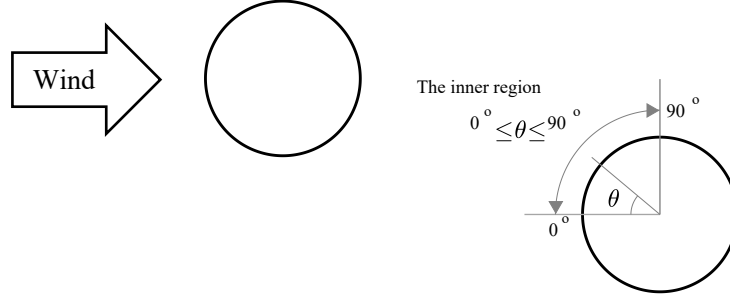
4. Discussion

Following Section 3, the mechanism of the WG observed in the free-vibration tests are discussed herein. Although WG in two closely spaced smooth cylinders primarily involves unsteady forces ([Fukushima et al., 2021](#); [Ruscheweyh and Dielen, 1992](#); [Shiraishi et al., 1986](#); [Zdravkovich, 1988](#)), the detailed analysis of the aerostatic force characteristics provides a baseline for further comparisons with the unsteady aerodynamic forces. Therefore, in Section 4.1, we discuss the static surface pressure on the downstream cylinders in all four types of arrangements. Thereafter, WG in the circular–circular case is explained in Section 4.2, after which the performance of the modified cylinder with 12 spiral protuberances against WG is explored in Section 4.3.

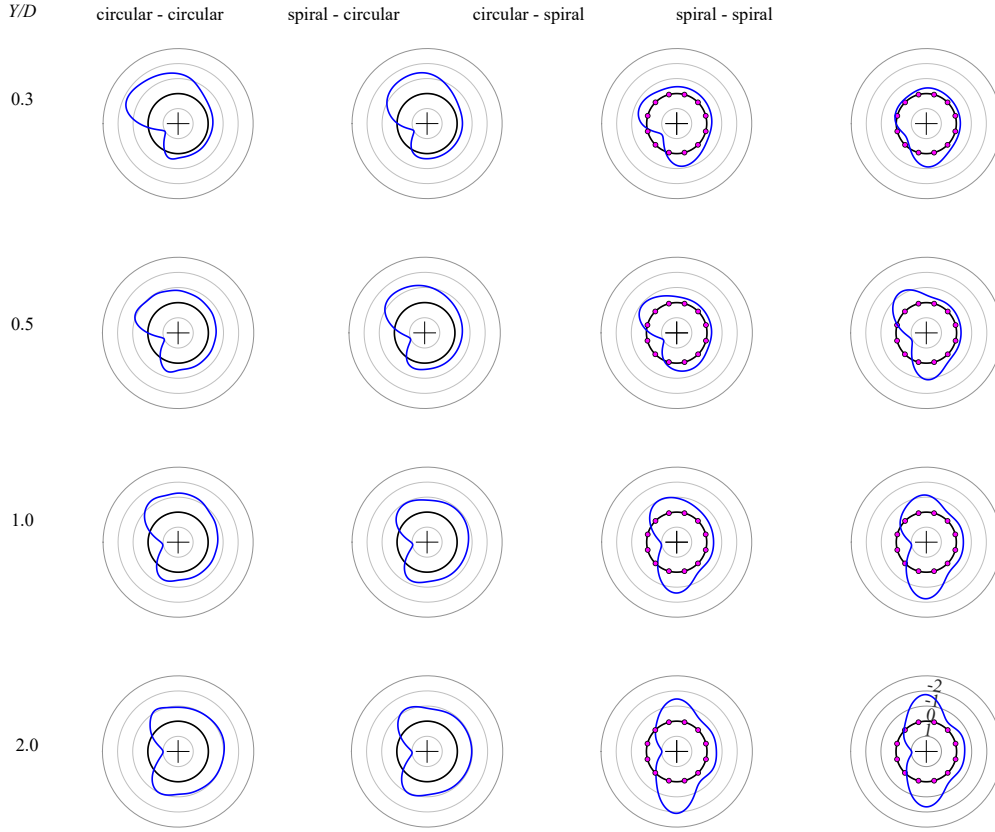
4.1. Position-dependent aerodynamic pressure in the stationary condition

For the four types of arrangements, the mean aerodynamic pressure distributions on the downstream cylinders at ($X/D = 3.0, Y/D = 0.3 - 2.0$) at $U = 12$ m/s are plotted in [Fig. 10\(b\)](#). The impact of wake interference on the downstream cylinder is implied by the non-zero angle of stagnation, which was observed

for $Y/D = 0.3$ and 0.5 . As the vertical distance between the two cylinders increased, the wake interference gradually ceased as the angle of stagnation reverted to zero, and the asymmetry of the mean pressure distributions around the stagnation line became less pronounced.



(a)



(b)

Fig. 10. (a) Definition of angular position; (b) Dimensionless mean surface pressure distributions $\bar{C}_p(\theta)$ on downstream cylinder at $(X/D = 3.0, Y/D = 0.3 - 2.0)$, $U = 12$ m/s.

The surface pressure gradient along the circumference of the cylinder is associated with the streamwise flow velocity according to the Navier–Stokes equations, which enables the visualization of the schematic flow patterns around the cylinder. In the circular–circular ($X/D = 3.0, Y/D = 0.3$), a steep favorable pressure gradient $\partial \bar{C}_p / \partial \theta < 0$ was observed at $0^\circ \leq \theta \leq 90^\circ$ (inner region defined in Fig. 10(a)) on the downstream

cylinder, which indicated an accelerated flow through the gap between the two cylinders, referred to as gap flow. As such, the gap flow and its resultant forces are sensitive to position, because they were already drastically diminished at $Y/D = 0.5$ until they seemingly disappeared at $Y/D = 2.0$. Similarly, the spiral–circular experienced gap flow at $Y/D = 0.3$ and 0.5 , although the velocity was not as high as in the circular–circular (because the favorable pressure gradient was not as steep).

Indicatively, the placement of spiral protuberances on the downstream cylinder surface can significantly suppress the gap flow. For the circular–circular and spiral–spiral cases ($X/D = 3.0, Y/D = 0.3$), the pressure gradient $\partial \bar{C}_p / \partial \theta$ in the inner region was remarkably flat, thereby signifying the absence of flow. As observed, a favorable pressure gradient developed only in the inner region of the downstream spiral cylinder at $Y/D = 0.5$.

Within the wake interference region ($X/D = 3.0, Y/D = 0.3-0.5$), the configuration of the downstream cylinder could more potentially influence the surrounding flow field than the upstream cylinder. Therefore, the effects of wake interference can be classified into the following two groups:

- (i) Downstream smooth cylinder (circular–circular and spiral–circular): strongest gap flow was observed at $Y/D \approx 0.3$, which rapidly ceased as the Y/D value increased. Although gap flow occurred in the inner region, the flow developed on the upper back of the cylinder at $90^\circ \leq \theta \leq 150^\circ$.
- (ii) Downstream spiral cylinder (circular–spiral and spiral–spiral): gap flow only started to form at $Y/D \approx 0.5$ and ceased as the Y/D value increased. Even in the presence of the gap flow, the upper back of the cylinder remained in the dead-air region with no flow.

The mean dimensionless static lift of the downstream cylinder at ($X/D = 3.0, Y/D = 0.3$ and 0.5) is plotted against the freestream velocity in Fig. 11. In the range of $U = 7 - 14$ m/s, the dimensionless forces change too subtly for Re -dependence, if quantified, to be significant. Therefore, the Re -dependence precludes from interest, and the scope of current discussion is limited to the reduced velocities.

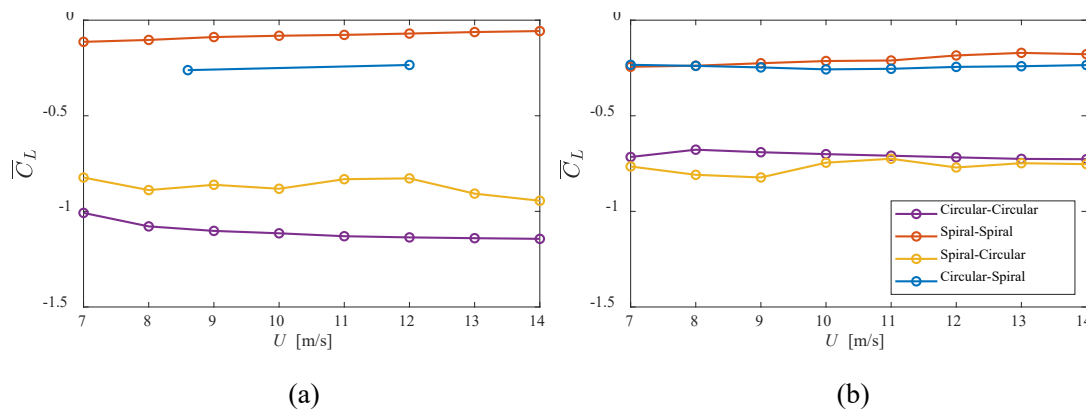


Fig. 11. Mean aerostatic lift coefficient of the downstream cylinder against freestream velocity at (a) ($X/D = 3.0, Y/D = 0.3$), and (b) ($X/D = 3.0, Y/D = 0.5$).

4.2. Wake galloping in parallel smooth cylinders

As observed in Fig. 9(a), WG in the circular–circular case is associated with a shifted equilibrium position at $(W/D = 3.0, S/D = 0.3)$. Therefore, this arrangement forms the focus of the following discussion.

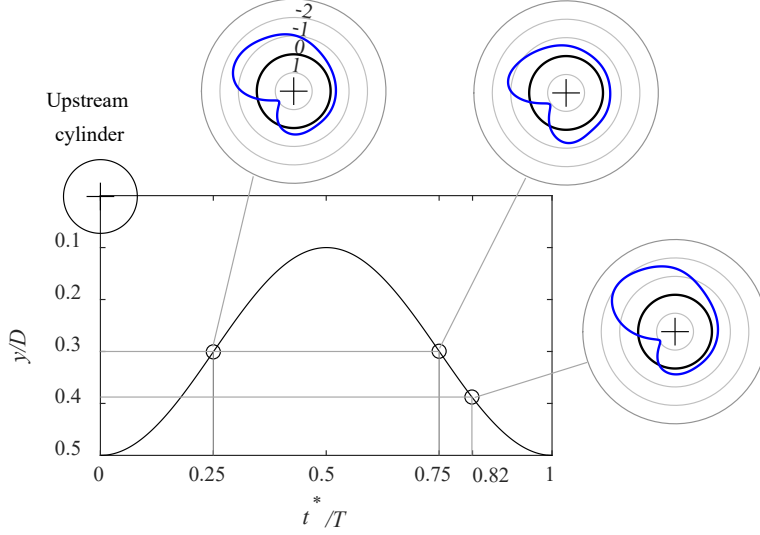


Fig. 12. Dimensionless instantaneous pressure distributions $C_p(\theta, t^*)$ at three instants in one ensemble-averaged cycle ($2\eta_0/D = 0.4$) in circular–circular ($W/D = 3.0, S/D = 0.3$) at $U/fD = 183$ ($U = 8.6$ m/s, $f_{vib} = 0.673$ Hz).

The vertical displacement of the downstream cylinder during a single ensemble-averaged cycle and the surface pressure distributions at three instantaneous positions are depicted in Fig. 12, which indicates the hysteresis from the dissimilar unsteady pressure distributions at two instants $t^*/T = 0.25$ and $t^*/T = 0.75$. The large negative pressure and steep favorable pressure gradient in the inner region signified a strong gap flow at $t^*/T = 0.25$ and $t^*/T = 0.82$ but not at $t^*/T = 0.75$, which foreshadows the non-quasi-static characteristic of the oscillation, despite the high reduced velocity.

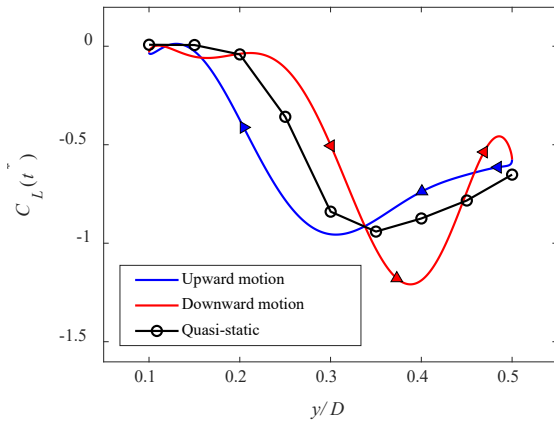


Fig. 13. Hysteresis loop of $C_L(t^*)$ at $U/fD = 183$ ($U = 8.6$ m/s, $f_{vib} = 0.673$ Hz) alongside quasi-static $\bar{C}_L$ at $U = 12$ m/s, $f_{vib} = 0.03$ Hz, circular–circular ($W/D = 3.0, S/D = 0.3$).

The hysteresis loop of the unsteady dimensionless lift $C_L(t^*)$, which was integrated from the surface pressure shown in Fig. 12, is presented in Fig. 13, where the blue and red branches represent the upward motion toward and downward motion away from the upstream cylinder, respectively. The negative peaks of $C_L(t^*)$ on the two branches correspond to the occurrences of gap flow at $t^*/T = 0.25$ and $t^*/T = 0.82$ (Fig. 12), respectively. Consequently, the gap flow produced a strong lift force directed toward the upstream cylinder, where $C_L(t^*) \approx 0$ indicates the immersion of the downstream cylinder in the wake of the upstream. Similar to the stationary condition, the position sensitivity of the flow phenomena was also evident in this case owing to the large amplitude of $C_L(t^*)$. This attribute is often referred to as the flow-switching phenomenon (between gap flow and outer flow) in previous studies (Gu and Sun, 1999; Matsumoto et al., 1990; Qin et al., 2019; Ruscheweyh and Dielen, 1992; Shiraishi et al., 1986; Zdravkovich, 1988). Both the branches strayed away from the quasi-static response such that the formations of gap flow were delayed in both the directions. This hysteresis plot encloses a non-zero bounded area. In principle, the bounded area represents the energy exchange during a single ensemble-averaged cycle following Eq. (5):

$$W^* = \frac{1}{D} \frac{1}{T} \int_0^T C_L(t^*) \dot{y} dt^* = \frac{1}{D} \oint C_L(t^*) dy, \quad (5)$$

where W^* denotes the dimensionless work done by the unsteady aerodynamic lift force. The clockwise direction of the loop indicates positive work yielding instability and *vice versa*. The hysteresis loop displayed in Fig. 13 produces a positive bounded area, which explains the WG mechanism.

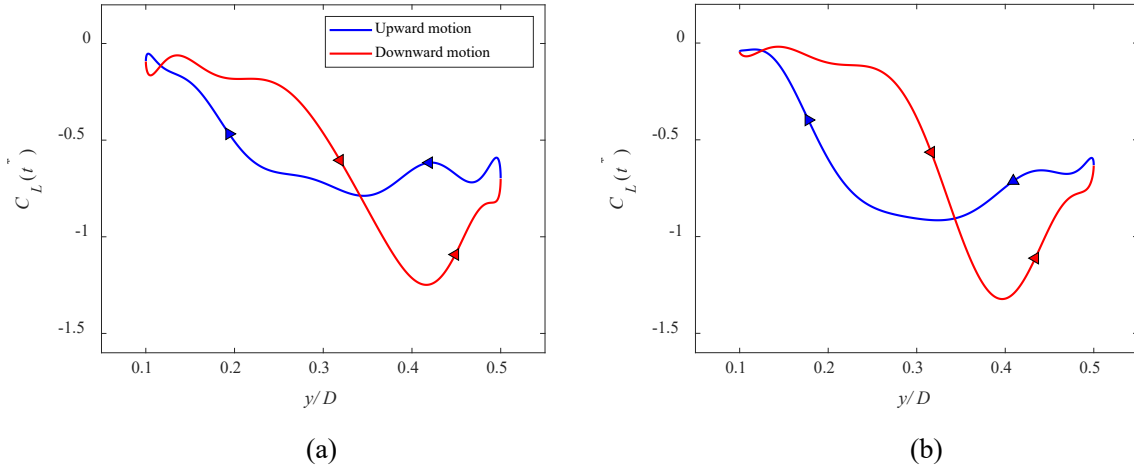


Fig. 14. Hysteresis loops of $C_L(t^*)$ in circular-circular ($W/D = 3.0, S/D = 0.3$) at (a) $U/fD = 100$ ($U=7$ m/s, $f_{vib}=1.00$ Hz); (b) $U/fD = 142$ ($U=10$ m/s, $f_{vib} = 1.00$ Hz).

The evolution of hysteresis between $U/fD = 100$ and 142 is illustrated in Fig. 14. The upward branch exhibited reduced velocity-dependence, where the gap flow was yet to completely develop at $U/fD = 100$. Upon reaching $U/fD \geq 142$, the reduced velocity-dependence of the phenomenon diminished, because the hysteresis loop generally sustained its shape to $U/fD = 183$ (Fig. 13), implying that WG is a hysteresis-predominant phenomenon. The hysteretic behavior is motion-induced, and theoretically, it should subside as the reduced velocity approaches infinity, which was the case for the quasi-static lift forces in this study (i.e., the upward and downward branches completely overlap). A direct comparison between the quasi-static

and true aerostatic forces showed they were mostly similar with one exception. The aerostatic forces acting on the downstream smooth cylinder in five staggered arrangements are plotted against the quasi-static forces in Fig. 15. While the quasi-static lift forces at $Y/D = 0.1, 0.5$, and 0.7 accurately estimated their aerostatic counterparts, the quasi-static test underestimated the aerostatic lift at $Y/D = 0.3$, i.e., the gap between the open circle and the solid marker plots, as shown in Fig. 15. The underestimation tendency seems exclusive to the wake interference region (i.e., $Y/D = 0.2 - 0.3$, as suggested in Fig. 15) and is yet to be clarified. Regardless, this observation did not challenge any of the previous discussion points, as the plots of the quasi-static and aerostatic forces exhibit generally similar tendencies, where the negative lift forces peak at $Y/D \approx 0.3$, as observed in Fig. 15.

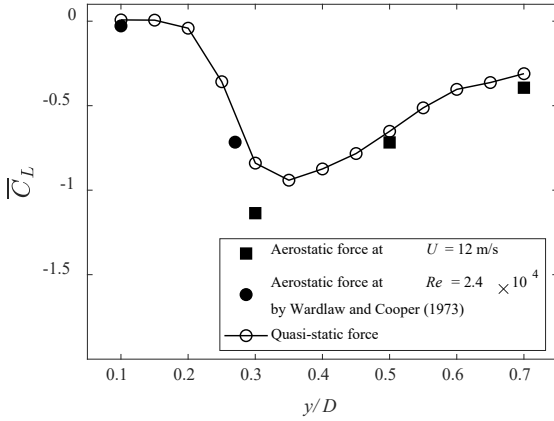


Fig. 15. Quasi-static $\bar{C}_L$ of circular–circular at $U = 12$ m/s, $f_{vib} = 0.03$ Hz, and truly aerostatic $\bar{C}_L$ compiled with measurements by Wardlaw and Cooper (1973).

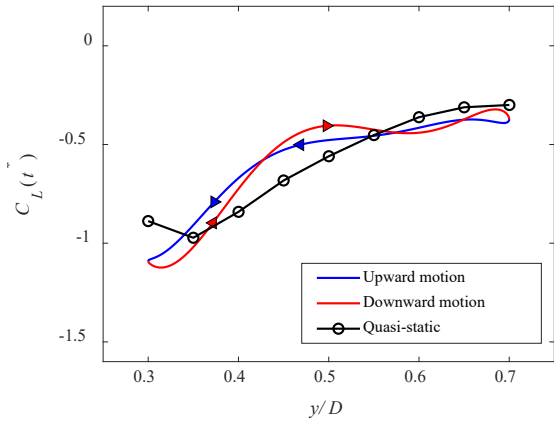


Fig. 16. Hysteresis loop of $C_L(t^*)$ at $U/fD = 183$ ($U = 8.6$ m/s, $f_{vib} = 0.673$ Hz) alongside quasi-static $\bar{C}_L$ at $U = 12$ m/s, $f_{vib} = 0.03$ Hz, circular–circular ($W/D = 3.0, S/D = 0.5$).

Although the hysteresis is reflected by the phase lag $\psi_{L\eta}$, such as in the calculation of H_1^* in Eq. (1), $\psi_{L\eta}$ should not be merely interpreted as a delay from the instantaneous response, but rather a mathematical representation of the non-quasi-static oscillation. Consequently, the H_1^* approach undermines the physics of the hysteresis observed in this case. Despite this outcome, the concept of H_1^* is mathematically sufficient to

describe the WG instability in the downstream cylinder. Fukushima et al. (2021) reported the compatibility between H_1^* and the response amplitudes, as portrayed in Fig. 5. Upon calculating H_1^* of the oscillating downstream cylinder in various equilibrium positions, Fukushima et al. (2021) determined the sensitivity of WG in circular–circular to the equilibrium position. A boundary was identified in which $S/D \geq 0.5$ yielded $H_1^* < 0$, whereas $S/D \leq 0.3 - 0.4$ (depending on the reduced velocities) yielded $H_1^* > 0$. The unsteady $C_L(t^*)$ of the downstream cylinder at $(W/D = 3.0, S/D = 0.5)$, $U/fD = 183$ is presented in Fig. 16. In this case, the enclosed area was negative and significantly smaller than that observed in Fig. 13. The major distinction between Figs. 13 and 16 is represented by the occurrence of the gap flow in an oscillation cycle. In $(W/D = 3.0, S/D = 0.5)$, the gap flow occurred once per cycle at $y/D \approx 0.3$, i.e., the uppermost position of the oscillation. Conversely, in $(W/D = 3.0, S/D = 0.3; \text{Fig. 13})$, the gap flow occurred twice per cycle owing to the advancement of the downstream cylinder into the upstream wake and the subsequent emergence from it. Qin et al. (2019) also observed in their free vibration tests of two tandem cylinders — $(W/D = 2.5 - 3.0, S/D = 0.0)$ — that the downstream cylinder underwent WG. Their finding reinforces the idea that the vibrations in the downstream cylinder arose from its alternating progression into and then out of the wake of the upstream cylinder. The hysteresis indicated the fluid–structure interaction between the wake and downstream cylinder. Matsumoto et al. (1990) and Shiraishi et al. (1986) observed similar hysteretic behavior in an oscillating downstream smooth cylinder in the vicinity of $(W/D = 3.0, S/D = 0.0)$. Their flow visualization unveiled that the motion of the downstream cylinder deformed the upstream cylinder wake from that of the stationary condition. In Fig. 13, as the downstream moved downward and farther from the wake, the wake moved along while encasing the downstream cylinder, which explains the extended period of $C_L(t^*) \approx 0$ at $y/D = 0.1 - 0.25$ on the downward branch. This delayed the formation of the gap flow. Conversely, as the downstream cylinder attempted to intrude the wake on the upward branch, the wake narrowed to resist the entrainment. This is illustrated in the instantaneous vortex shedding frequency of the oscillating downstream smooth cylinder, as depicted in Fig. 17, where the oscillation corresponds to Figs. 12 and 13. The two random consecutive cycles at $t = 54.3 - 57.3$ s in a 180-second-span unsteady pressure measurement are plotted in Fig. 17, wherein the similarity in the responses between these two cycles reflects their representativeness. Note that a single ensemble-averaged cycle is not used in this case, because the process would “erase” the trace of vortex shedding. More importantly, the upward motion $y/D = 0.5 - 0.25$ witnessed a rise in the Strouhal number ($St = 0.20 - 0.26$), especially in the inner region (Fig. 17(b)), because the downstream cylinder narrowed the wake of the upstream cylinder. Subsequently, as the downstream moved downward from $y/D = 0.1$ to 0.3 , it remained immersed in the mutual wake. The two cylinders experienced strong vortex shedding as a conjoined body at a lower frequency of $St \approx 0.15$ (Fig. 17(b) and (c)).

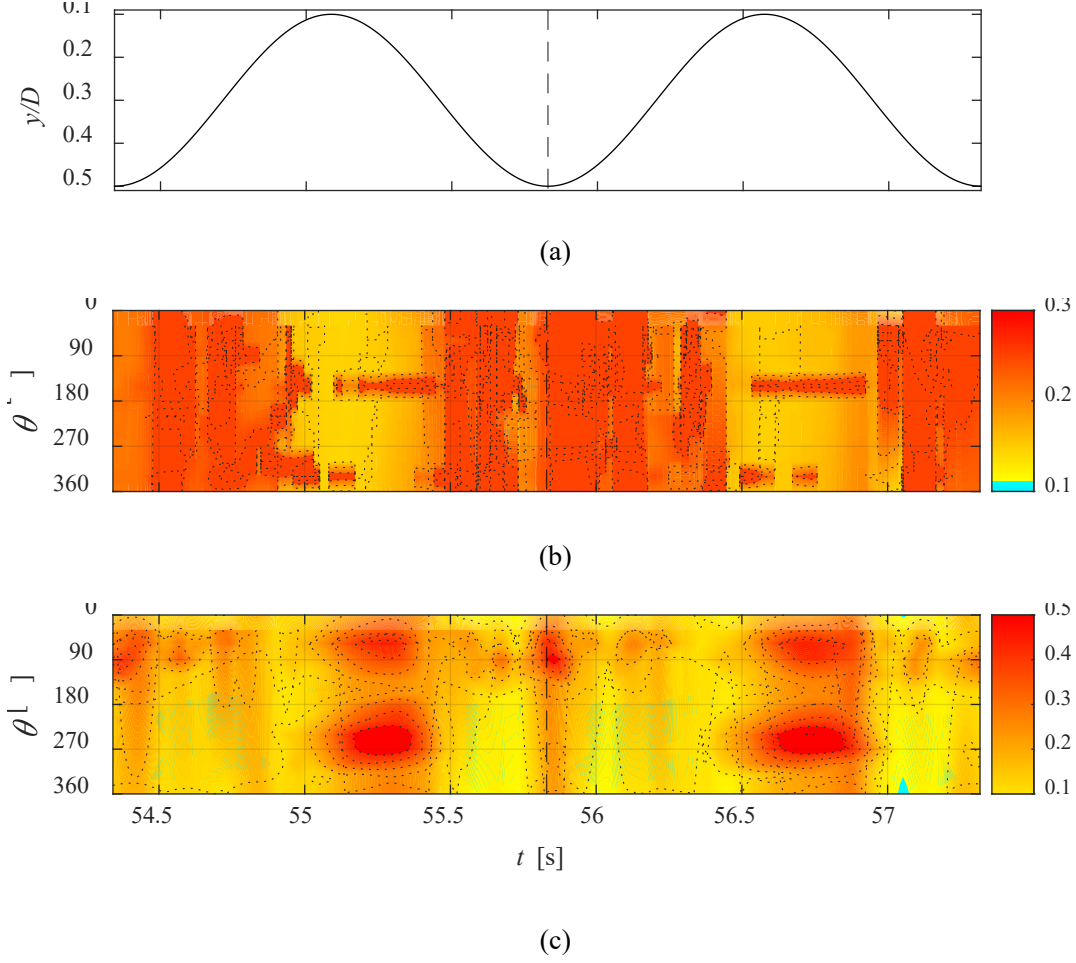


Fig. 17. Instantaneous vortex-shedding frequency of the downstream cylinder in circular-circular at $U/fD = 183$ ($U = 8.6$ m/s, $f_{vib} = 0.673$ Hz, $W/D = 3.0$, $S/D = 0.3$). (a) Instantaneous vertical displacement; (b) Instantaneous dimensionless vortex-shedding frequency $St(t)$; (c) Instantaneous amplitude of $C_p(t)$ at instantaneous $St(t)$.

In summary, the instability was not caused by the gap flow but by its manner of occurrence. The portrayal of such a manner is twofold: reduced velocity dependence and equilibrium position dependence, such that reduced velocity represents the viscosity-like effect, whereas the equilibrium position relates to the hysteresis. Both aerodynamic forces and flow experienced motion-induced hysteresis as a feedback system. However, the quasi-steady approach only considers the position and frozen velocity of the cylinder, but not its real-time velocity. Consequently, the quasi-steady theory disregards the hysteresis and fails to explain the WG in the wake interference region, as reported by [Fukushima et al. \(2021\)](#) and [Ruscheweyh and Dielen \(1992\)](#).

4.3. Suppression of wake galloping in parallel cylinders with spiral protuberances

This section discusses the performance of the modified cylinder with spiral protuberances in three types of arrangements: spiral-circular, circular-spiral, and spiral-spiral. These arrangements aim to clarify the influence of a spiral cylinder in upstream and downstream positions.

4.3.1. Cylinder with spiral protuberances on upstream side

Similar to the circular–circular case ($X/D = 3.0, Y/D = 0.5$), the static displacement occurred in spiral–circular ($X/D = 3.0, Y/D = 0.5$). On the verge of the WG at $U/fD = 83$, the new equilibrium position was located at ($W/D = 3.0, S/D = 0.4$) (Fig. 9(c)).

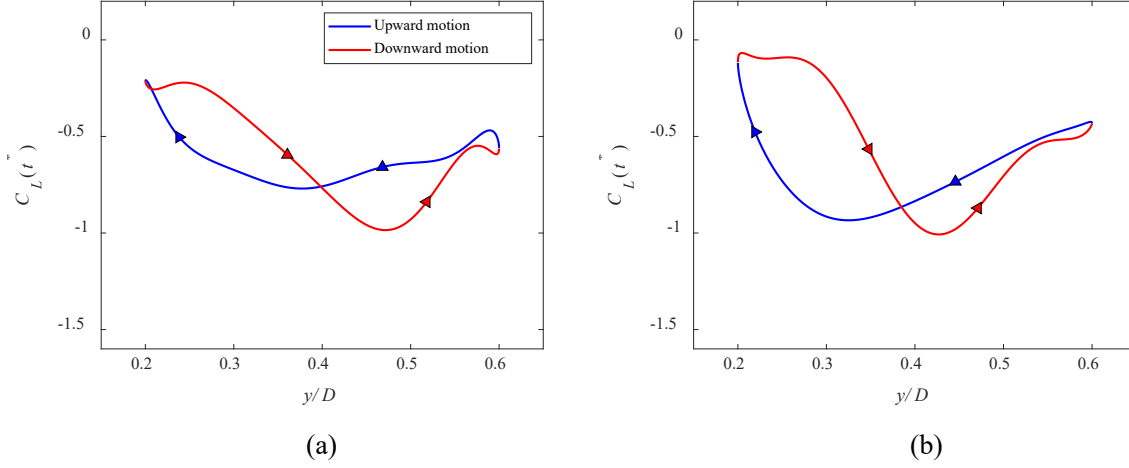


Fig. 18. Hysteresis loops of $C_L(t^*)$ in spiral–circular ($W/D = 3.0, S/D = 0.4$) ($D = 0.05$ m)
(a) $U/fD = 106$ ($U = 7$ m/s, $f_{vib} = 1.32$ Hz); (b) $U/fD = 182$ ($U = 12$ m/s, $f_{vib} = 1.32$ Hz).

The hysteresis loops of $C_L(t^*)$ in the spiral–circular case ($W/D = 3.0, S/D = 0.4$) at $U/fD = 106$ and 182 are illustrated in Fig. 18. Hysteretic features similar to the circular–circular case (Figs. 13 and 14) were observed, although the gap flow on the downward branch was not as strong as that (i.e., magnitude of $C_L(t^*)$ in Fig. 18 is smaller). Regarding the upstream spiral cylinder, the WG still occurred through the same mechanism as in circular–circular but only at a lower reduced velocity onset.

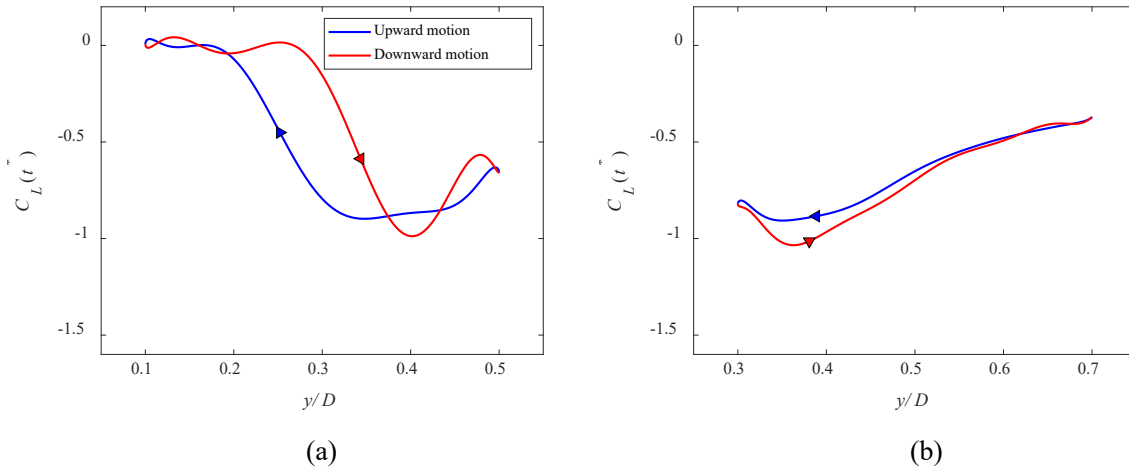


Fig. 19. Hysteresis loop of $C_L(t^*)$ in spiral–circular: (a) ($W/D = 3.0, S/D = 0.3$) at $U/fD = 182$ ($U = 8.6$ m/s, $f_{vib} = 0.673$ Hz); (b) ($W/D = 3.0, S/D = 0.5$) at $U/fD = 171$ ($U = 12$ m/s, $f_{vib} = 1.00$ Hz).

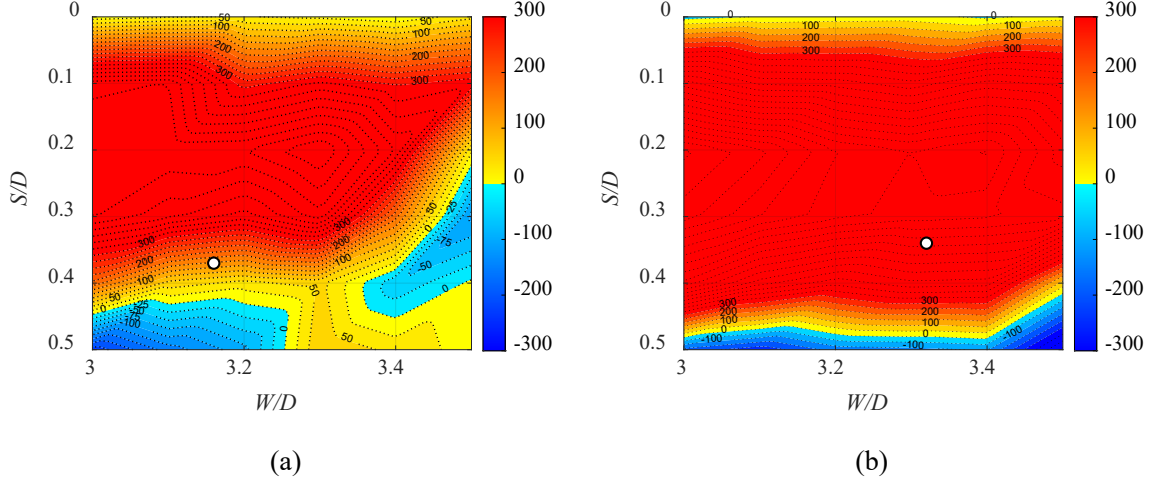


Fig. 20. Contour maps of H_1^* ($2\eta_0/D = 0.4$) in spiral–circular case, $D = 0.05$ m

● denotes shifted equilibrium position corresponding to Fig. 9(b).

(a) $U/fD = 136$ ($U = 9$ m/s, $f_{vib} = 1.32$ Hz); (b) $U/fD = 182$ ($U = 12$ m/s, $f_{vib} = 1.32$ Hz).

A single ensemble-averaged-cycle $C_L(t^*)$ at $(W/D = 3.0, S/D = 0.3)$ and $(W/D = 3.0, S/D = 0.5)$ is observed in Fig. 19, which is consistent with the results presented in Fig. 20 that plots the contour maps of H_1^* at various equilibrium positions under two distinct reduced velocities. As observed from Fig. 20, distinct boundaries existed between the negative and positive values of H_1^* at $S/D \approx 0.5$. Similar to the circular–circular case, evident hysteresis and instabilities were associated with the immersion of the downstream cylinder in the wake on its oscillation trajectory, which occurred at $S/D < 0.5$.

The growth of the positive H_1^* in the wake interference region velocity from $U/fD = 136$ to 182, as deduced from Fig. 20 (i.e., the dense contour lines within the red patches), is large, which is also the case for the H_1^* in circular–circular configurations as described by Fukushima et al. (2021). This observation may discourage increasing structural damping as a countermeasure against WG because it cannot overcome the constantly growing negative aerodynamic damping. Therefore, instead of mechanical and structural controls — e.g., enhancing structural damping and implementing external dampers — the following sections explore the potential of the spiral protuberances on the downstream cylinder as an effective aerodynamic control against WG.”

4.3.2. Cylinder with spiral protuberances on downstream side

Despite the static displacement (Fig. 9(c)), the circular–spiral ($X/D = 3.0, Y/D = 0.5$) remained stable at $U/fD \geq 100$ (Fig. 7). The contour maps of H_1^* for various spacing scenarios in the circular–spiral case are presented in Fig. 21. As observed, H_1^* was negative for all examined arrangements ($W/D = 3.0$ – $3.5, S/D = 0.0$ – 0.5).

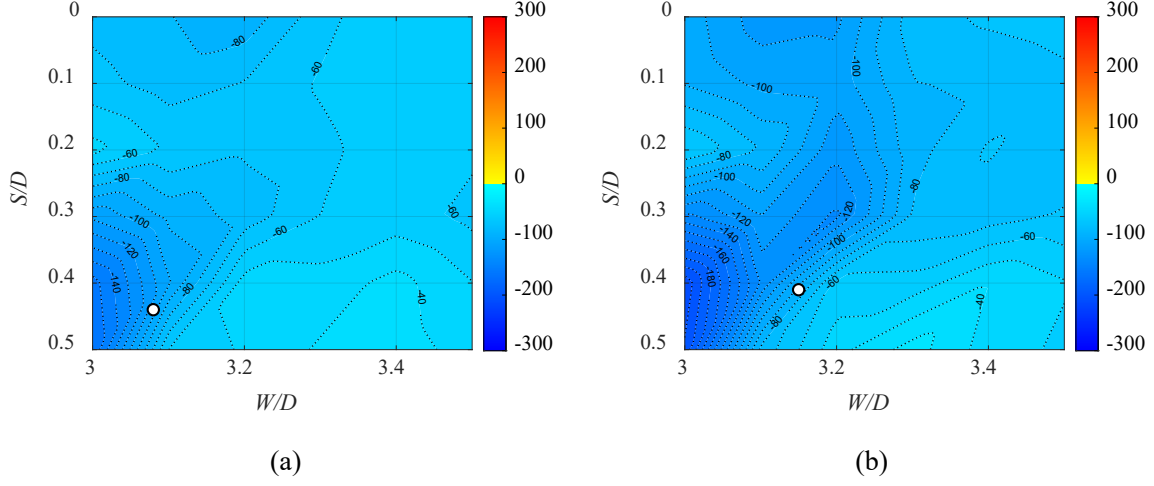


Fig. 21. Contour maps of H_1^* ($2\eta_0/D = 0.4$) in circular-spiral case, $D = 0.05$ m

● denotes shifted equilibrium position corresponding to Fig. 9(c).

(a) $U/fD = 136$ ($U = 9$ m/s, $f_{vib} = 1.32$ Hz); (b) $U/fD = 182$ ($U = 12$ m/s, $f_{vib} = 1.32$ Hz).

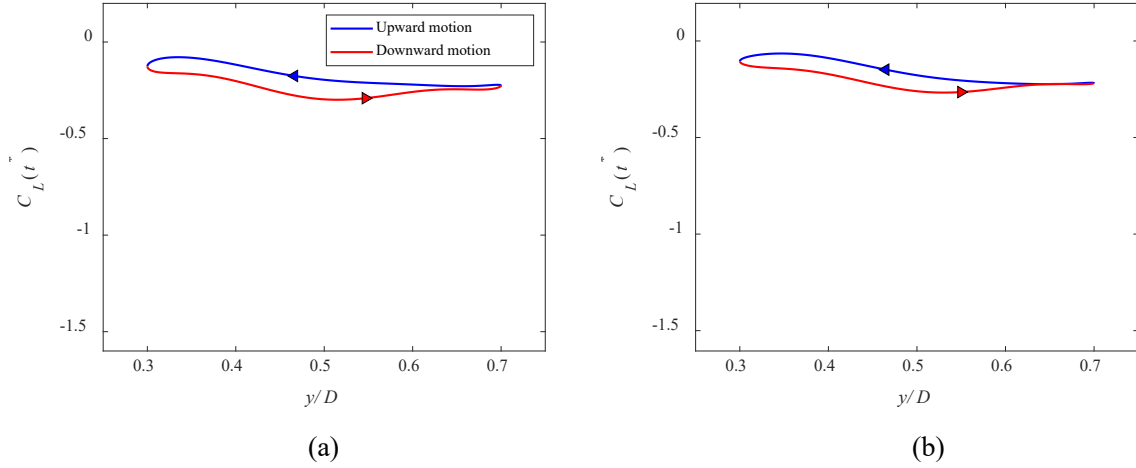


Fig. 22. Hysteresis loops of $C_L(t^*)$ in circular-spiral ($W/D = 3.0, S/D = 0.5$)

(a) $U/fD = 108$ ($U = 10$ m/s, $f_{vib} = 1.32$ Hz); (b) $U/fD = 152$ ($U = 14$ m/s, $f_{vib} = 1.32$ Hz).

Both hysteresis loops at $U/fD = 108$ and 152 in Fig. 22 yielded negatively bounded areas, thereby implying stability. The one-ensemble-averaged $C_L(t^*)$ presented in Fig. 22 cannot explicitly signify the occurrence of the gap flow or its existence during the oscillation.

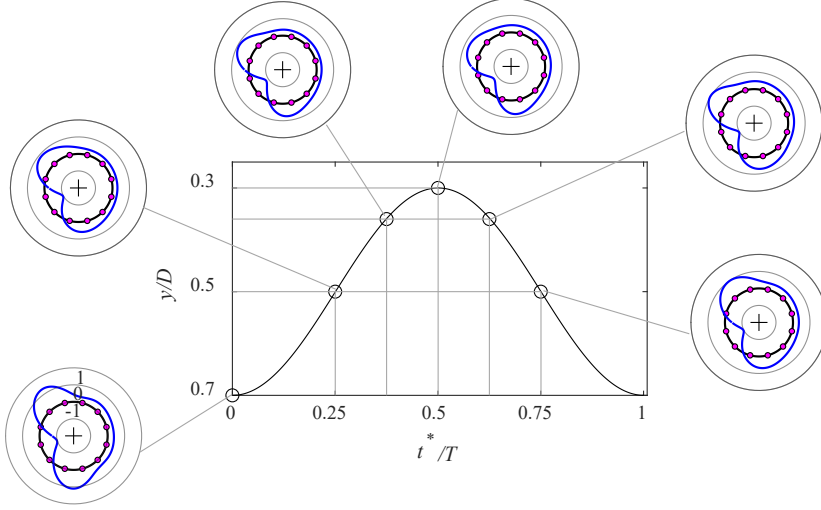


Fig. 23. Dimensionless instantaneous pressure distributions $C_p(\theta, t^*)$ in one ensemble-averaged cycle ($2\eta_0/D = 0.4$) in circular–spiral ($W/D = 3.0, S/D = 0.5$) at $U/fD = 183$ ($U = 8.6$ m/s, $f_{vib} = 0.673$ Hz).

The instantaneous pressure distributions on the downstream spiral cylinder at six instants in a single ensemble-averaged cycle of oscillation about ($W/D = 3.0, S/D = 0.5$) at $U/fD = 183$ are illustrated in Fig. 23, displaying two vital features. First, the almost constant pressure at $90^\circ \leq \theta \leq 150^\circ$ denotes the lack of flow on the upper back of the cylinder regardless of its position during the oscillation. This restrained the magnitude of unsteady lift $C_L(t^*)$ in a single cycle, as observed in Fig. 22. Moreover, this attribute was observed in the mean surface pressure described in Section 4.1 which results in the second feature: the unsteady $C_L(t^*)$ was seemingly quasi-static. Therefore, the WG suppression of the downstream spiral cylinder was twofold: reduction in the magnitude of the lift force and the diminished hysteresis.

4.3.3. Two parallel cylinders with spiral protuberances

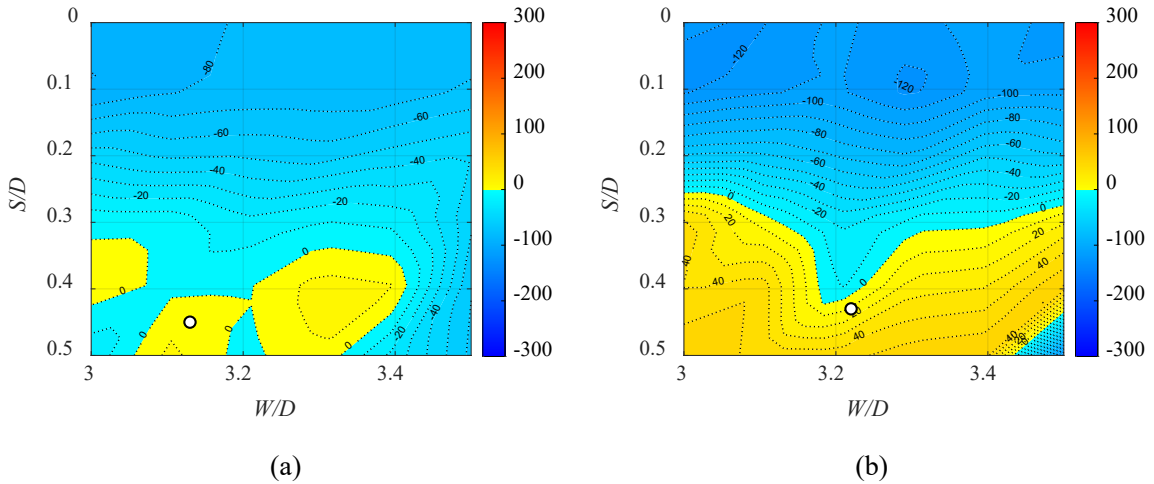


Fig. 24. Contour map of H_1^* ($2\eta_0/D = 0.4$) in spiral–spiral case, $D = 0.05$ m

● denotes shifted equilibrium position corresponding to Fig. 9(d).

(a) $U/fD = 136$ ($U = 9$ m/s, $f_{vib} = 1.32$ Hz); (b) $U/fD = 182$ ($U = 12$ m/s, $f_{vib} = 1.32$ Hz).

The contour maps of H_1^* for spiral–spiral case at ($W/D = 3.0-3.5, S/D = 0.0-0.3$) are plotted in Fig. 24, which indicates the position dependence of H_1^* in the spiral–spiral case. However, unlike the circular–circular (Fukushima et al., 2021) and spiral–circular case (Fig. 20), the order of magnitude of H_1^* in Fig. 24 was significantly smaller. The positive values of H_1^* were probably related to the small-amplitude oscillations observed in Fig. 8.

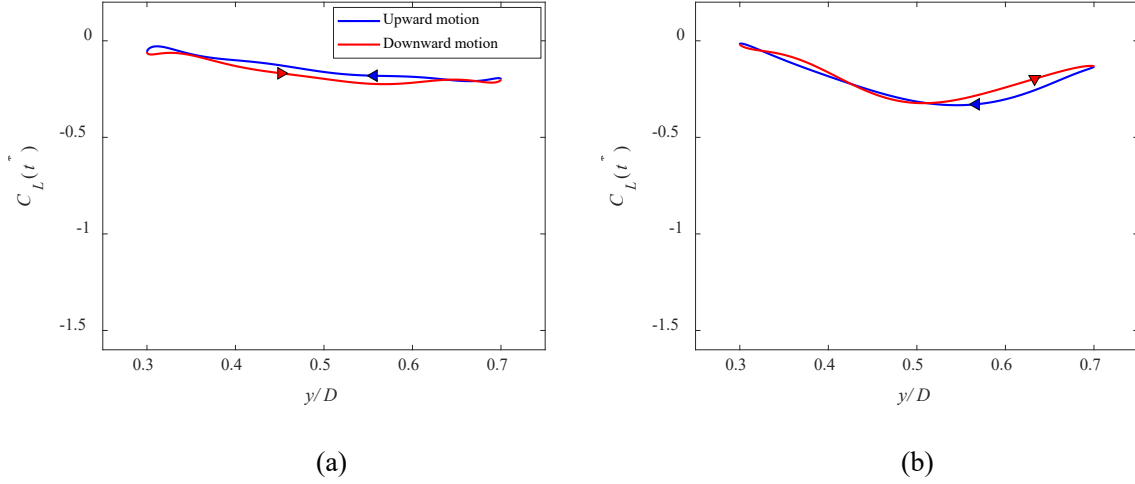


Fig. 25. Hysteresis loops of $C_L(t^*)$ in spiral–spiral ($W/D = 3.0, S/D = 0.5$)

(a) $U/fD = 130$ ($U = 12$ m/s, $f_{vib} = 1.32$ Hz); (b) $U/fD = 183$ ($U = 8.6$ m/s, $f_{vib} = 0.673$ Hz).

The hysteresis loops of the downstream cylinder in spiral–spiral at $U/fD = 130$ and 183 are presented in Fig. 25. The shape of the $C_L(t^*)$ loop varies according to the reduced velocity that portrays the reduced velocity-dependence of the unsteady lift force. However, the hysteresis of the unsteady lift was independent of the reduced velocity. Both the loops displayed in Fig. 25 yielded small bounded areas, either negative or positive. The hysteresis was further suppressed compared to the circular–spiral case presented in Fig. 22. Consequently, the aerodynamic damping, namely, H_1^* , was proximate to zero as observed in Fig. 24. In this case, positive H_1^* does not necessarily imply instability, especially considering the structural damping of the cylinder.

5. Conclusions

This study clarified the mechanism of wake galloping in the wake interference region, especially at $W/D = 3.0, S/D = 0.3 - 0.5$ under high reduced velocities $U/fD \geq 100$ for varying combinations of parallel smooth cylinders and cylinders with 12 spiral protuberances. The key findings are summarized as follows:

- 1) For two parallel smooth cylinders in the stationary condition, at ($X/D = 3.0, Y/D = 0.3-0.5$), the downstream cylinder experienced a strong lift force directed toward the upstream cylinder, which presumably correlated with gap flow. The aerodynamic lift force was sensitive to the vertical spacing of the two cylinders within the wake interference region, such that a small variation in the position produced significant variations in the lift force magnitudes. This position-dependent behavior produces a large-amplitude fluctuating lift in the oscillating downstream cylinder.

- 2) The flow-induced unsteady lift force on the downstream smooth cylinder during oscillation exhibited evident hysteretic behavior only at certain equilibrium positions, which involved the immersion of the downstream cylinder into the wake of the upstream cylinder. This explains the equilibrium position dependence of wake galloping. The hysteretic unsteady aerodynamic forces for two smooth cylinders exhibited reduced velocity-dependence in the reduced velocity regime below 150 but its velocity dependence grew less above a reduced velocity 150. More specifically, preceding the onset of wake galloping, the hysteresis favorably stabilized the downstream cylinder. However, the hysteresis and large-amplitude fluctuating lift joined forces to trigger the wake galloping as the reduced velocity increased. As the reduced velocity exceeded 150, the hysteresis was sustained owing to the fluid–structure interaction between the motion of the downstream cylinder and the wake of the upstream cylinder. Even at high reduced velocities, such interactions invalidate the application of the quasi-steady theory. The hysteresis-induced wake galloping mechanism holds within the wake interference region, where gap flow is most likely predominant. However, an extended discussion on other spacing scenarios requires further investigation.
- 3) The cylinder with spiral protuberances did not suppress the gap flow, but rather suppressed the impact of the gap flow on the aerodynamic forces. The gap flow on the modified cylinder did not elicit a strong lift in either the stationary condition or during oscillation. Although the gap flow was position-sensitive, the lift force was not. Consequently, the order of magnitude of the fluctuating lift was small in comparison to that of the smooth downstream cylinder.
- 4) Regardless of the equilibrium position, the hysteresis of the unsteady aerodynamic forces was drastically suppressed in the downstream cylinder with spiral protuberances, and the unsteady forces were almost quasi-static. A positive H_1^* in the downstream cylinder does not necessarily indicate instability but rather a nonlinear flow field. The combined action of the suppressed hysteresis and small-amplitude unsteady lift stabilized the downstream cylinder with spiral protuberances against wake galloping.
- 5) The downstream cylinder with spiral protuberances increased the amplitude of the 2DOF wake-induced flutter in the lower reduced-velocity regime under 100 probably because the aerodynamic properties of the modified cylinder might trigger instability. A more thorough investigation is required to verify this finding.

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