Closed-loop controlled vortex-airfoil interactions

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Closed-loop controlled interactions between an airfoil and impinging vortices were experimentally investigated. This work aims to minimize the fluctuating flow pressure ($p$) at the leading edge of the airfoil, which is a major source of the blade-vortex interaction noises commonly seen in rotocrafts. Piezoceramic actuators were used to create a local surface perturbation near the leading edge of the airfoil in order to alter the airfoil-vortex interaction. Two closed-loop control schemes were investigated, which deployed $p$ and the streamwise fluctuating flow velocity ($u$) as the feedback signal, respectively. As the control effect on $p$ was measured using a fast response pressure transducer, the oncoming vortical flow was monitored using a particle image velocimetry and a hot wire. It was found that the control scheme based on the feedback signal $u$ led to a pronounced impairment in the strength of oncoming vortices and meanwhile a maximum reduction in $p$ by 39%, outperforming the control scheme based on the feedback signal $p$. Physics behind the observations is discussed. © 2006 American Institute of Physics. [DOI: 10.1063/1.2189287]

I. INTRODUCTION

When a hard-surfaced body such as blade, foil, wedge, or fin is subjected to an oncoming vortical flow, the incident vortices may be distorted so rapidly that a sharp pressure rise is induced at the leading edge of the body. This pressure rise in turn causes the generation of an intense impulsive sound and subsequent radiation to the far field.1–5 This kind of noise, referred to as the blade-vortex interaction (BVI) noise, is commonly seen in engineering rotocrafts, e.g., helicopters, turbomachines, and fans. This noise can also cause an environmental concern and even directly jeopardize the competitiveness and even the usage of the industrial products such as hair driers, vacuum cleaners, computers, and telecom exchangers, which are associated with the rotocrafts. Therefore, the control of the BVI noise has recently been given an increasing attention in the literature.

Passive control methods have been extensively employed to reduce the BVI noise. Typical examples include modified blade tip shapes,6 the use of spoilers or vane wings,7 and flight path management.8,9 Requiring no external energy input, these methods change the dynamic behavior of the fluid-structure system via modifying its geometrical or physical parameters.

Active control techniques have also been explored in the past. Active control can be either an open- or closed-loop. Using an open-loop method, Brooks et al.10 simultaneously pitched the four blades of a hingeless BO-105 rotor up to 1.2° through electrohydraulic actuators at 3, 4, and 5 times of the rotor rotational frequency and managed to obtain a 6 dB reduction in BVI noise. Jackin et al.11 improved this technique, leading to the so-called individual blade control (IBC). IBC allowed the independent control of each blade of a helicopter with servohydraulic actuators, resulting in a reduction of the BVI noise by 12 dB. Chen et al.12 used surface-bounded piezoceramic actuators or fiber composites to twist an airfoil, achieving a 10 dB reduction in the BVI noise. Straub et al.13 applied piezoelectrically driven trailing edge flaps to all blades of a MD900 light utility helicopter. The tip vortices shed from a preceding blade were effectively disturbed by the flap oscillations so that the BVI noise dropped by 5 dB. Kaykayağlu14 changed interactions between upstream vortices and a downstream airfoil by oscillating the leading edge of the airfoil, which was activated by a variable-speed dc motor. The vortex strength and the BVI noise were effectively suppressed when the oscillation frequency of the leading edge coincided with the instability frequency of the vortex-airfoil system. Lee15 employed a bleeding technique, by blowing or sucking air through the porous leading edge of an airfoil, to perturb vortex-airfoil interaction, yielding a 30% reduction in the BVI noise in the near field.

A closed-loop control relies on a feedback signal from the controlled system to generate control actions. Most previous investigations were conducted numerically on the closed-loop control of the BVI noise. See Ariyur and Krstić16 and Swaminathan et al.17 for examples. Recently, Cheng et al.18 proposed a perturbation technique to control fluid-structure interactions, which proved to be very effective in altering the strength of vortices shed from a cylinder and subsequently the structural vibration.18,19 Inspired by that success, the present work extends the perturbation technique for a new application, i.e., modifying the blade-vortex interaction and subsequently suppressing the BVI noise.

A variety of methods for producing two-dimensional upstream vortices have been discussed by Wilder and Telionis.20 The simplest is to employ a cylinder to generate von Kármán vortices, whose interaction with a downstream blade may reflect the major characteristics of BVI.21 This method has been used by a number of researchers5,20,22 and is...
adopted presently. The objective of this work is to develop a closed-loop control system and to effectively suppress the fluctuating flow pressure $p$ at the airfoil leading edge in view of the fact that it is difficult to directly measure noise associated with fluid-structure interactions and that this pressure is responsible for the generation of the BVI noise.$^4,^{15}$ Two closed-loop control schemes, using $p$ and the streamwise fluctuating flow velocity $u$ for feedback signals, respectively, were deployed and compared. The control performances were assessed in terms of $p$ measured by a miniature pressure transducer. To understand the underlying physics, the control effects on the oncoming vortical flow were measured, simultaneously with $p$, using a hot wire and a particle image velocimetry (PIV), and the interactions between $u$, $p$ and the perturbation were examined in detail.

II. EXPERIMENTAL DETAILS

Experiments were conducted in a closed circuit wind tunnel, which has a 2.4-m-long square test section of 0.6 m X 0.6 m. Readers may refer to Zhou et al.$^{23}$ for more details about the tunnel. A circular cylinder made of stainless steel with a diameter $d=10$ mm and an NACA0012 airfoil with a chord length $c=150$ mm and a thickness $=18$ mm were horizontally mounted in tandem on the test section (Fig. 1). The cylinder and the leading edge of the airfoil were separated by $10d$, which was sufficient to prevent significant flow feedback induced by the airfoil.$^2$ The airfoil angle of attack was set at $0^\circ$. Measurements were conducted at a free-stream velocity $U_\infty=11$ m/s. The corresponding Reynolds numbers, $Re_d(U_\infty d/\nu)$, where $\nu$ is the kinematic viscosity) based on $d$
and Reₜ (=Uᵥc/ν) based on c were 7.3×10³ and 1.09×10³, respectively. The frequency fₑ of vortex shedding from the cylinder was about 220 Hz. The free-stream turbulence intensity was less than 0.4%.

Two curved piezoceramic actuators, so called THUNDER, which were 76 mm long and 2.8 mm wide, were embedded in a slot of 200 mm long, 3 mm wide, and 3 mm deep on the lower side of airfoil, which was 7 mm from the airfoil leading edge in the streamwise direction [Fig. 1(a)]. The THUNDER actuators (THin layer composite UNimorph piezoelectric Driver and sEnsoR), developed by the NASA Langley Research Center and produced by FACE International Corporation, deform out of plane under an excitation voltage. THUNDERs are characterized by many advantages such as high displacement, acceptable load capacity and small size. Typically, without any loading, the present actuator (THUNDER-11R) with a physical dimension of 76.2×2.54×0.74 mm can vibrate at a maximum displacement of about 2 mm and a frequency up to 2 kHz. The actuators were installed in a cantilever manner to create the maximum perturbation displacement in the lateral direction and thus the best control performance under the same excitation condition. The actuators and the walls of the slot around the actuators were well lubricated to minimize the contact friction. A thin piece of Mylar membrane, with superior strength, good heat resistance and insulation, was pasted on the top of the actuators to make a smooth airfoil surface (Fig. 1). Driven by the actuators, this membrane will oscillate to create a perturbation on the airfoil surface.

A cylindrical miniature electret pressure transducer (151-01 series, Tibbetts Ind.), with a sensitivity of 15.8 mV/Pa and a frequency response up to 10 kHz, was used to measure p at the airfoil leading edge, which provides both the feedback signal and a measure of the control performance. The cylindrical pressure transducer, with a diameter of 2.6 mm and a length of 7 mm, was embedded at the midspan of the airfoil, 1 mm from the airfoil leading edge and 1 mm below the x–z plane [Fig. 1(a)], where the intensity of p was relative high. The origin of the coordinate system, shown in Fig. 1(a), was defined at the cylinder center, with the x, y, and z along the streamwise, transverse, and spanwise directions, respectively. The two actuators were symmetrically located to the pressure transducer in the z direction [Fig. 1(a)]. The distance between the sensing element, i.e., the front end of the pressure transducer, and the cantilevered end of each actuator was 6, 5, and 4 mm in the x, y, and z directions, respectively.

As the pressure transducer and the actuators were very close to each other, it is crucial to ensure the output of the pressure transducer would not be affected by the disturbance generated by the actuators. A series of tests were carried out with the upstream cylinder removed. Under the same experimental conditions as used for present experiments, the actuators were excited by a sine wave of different combinations of the excitation frequency (fₑ) and voltage (Vₑ) from a signal generator. The fluctuating flow pressure (p) and the perturbation displacement (Yₑ) were simultaneously measured using the pressure transducer and the laser vibrometer, and then digitized for analysis. The number of the sampled points and the sampling time for each record were 20 000 and 10 s, respectively. The tests were divided into two parts. In the first part, Vₑ was fixed at 80 V and fₑ was set at 30, 80, 160, 220, and 300 Hz, respectively. In the second part, fₑ was fixed at 220 Hz and Vₑ was 10, 53, 80, 100, and 120 V, respectively. Note that the fₑ/Vₑ pair included 220 Hz/80 and 220 Hz/53 V, which were the working frequencies and voltages to be used in the present control schemes. The root mean square (rms) value of p and Yₑ, i.e., pₑrms and Yₑrms, under different fₑ and Vₑ are listed in Table I. It is evident that pₑrms is almost constant, irrespective of Yₑrms, indicating that although the actuators and the pressure transducer were close to each other, the outputs of the pressure transducer would not be influenced by the disturbance generated by the actuators.

Two 5 μm tungsten wires were placed at x/d=8, y/d=−1, z/d=0 (hot wire 1 in Fig. 1) and x/d=10, y/d=−1, z/d=−8 (hot wire 2 in Fig. 1), respectively, to measure the fluctuating flow velocity. Hot wires 1 and 2 were used to provide the feedback signal (u₁) and measure the vortical flow velocity (uₑ), respectively. The location of the hot wire 1 is important for the control performance. The vortical flow should not be affected by the BVI, given more than 0.5λₑ upstream from the airfoil leading edge, where λₑ is the vortex wavelength, estimated to be about 3.2d under the present Reₑ condition. Therefore, hot wire 1 needs to be placed at x/d<10−1.6=8.4 so that u₁ is able to provide the information on the upstream unperturbed vortical flow, without the contamination of the BVI noises, to warrant a good control performance. Furthermore, hot wire 1 was placed at y/d=−1 in order to minimize its possible disturbance on the downstream flow near the airfoil leading edge. The feedback signal p or u₁ was, after amplification, high-pass-filtered at a cutoff frequency of 200 Hz (or 0.18 if normalized by d and Uₑ) and then sent to a digital signal processor (DSP) controller fitted with 16 bit AD and DA converter. The converted analog signal was filtered again using a band-pass filter with a frequency range from 200 to 500 Hz (or from 0.18 to 0.45 if normalized) before amplification by a dual channel piezodriver amplifier (Trek PZD 700). The processed signal was then used to activate the actuators. The use of the two filters for the feed-forward and feedback passages was to remove noises from turbulence and electronics. The controller was implemented using a real-time system, dSPACE, which provided functions such as rapid control prototyping, production code generation, and hardware-in-the-loop tests. A DSP with SIMULINK function of MATLAB and software (ControlDesk 2.0) was used to sample and

<table>
<thead>
<tr>
<th>Experiment part 1</th>
<th>Experiment part 2</th>
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<tr>
<td>Vₑ (V)</td>
<td>80</td>
</tr>
<tr>
<td>fₑ (Hz)</td>
<td>30</td>
</tr>
<tr>
<td>Yₑrms (mm)</td>
<td>0.46</td>
</tr>
<tr>
<td>pₑrms (Pa)</td>
<td>22.4</td>
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</tbody>
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process the feedback signals. In addition, the perturbation displacement of the membrane on the top of the actuators was measured by a Polytec Series 3000 Dual Beam laser vibrometer. The streamwise and lateral fluctuating flow velocities near the airfoil leading edge were measured using a 5 μm tungsten X wire. The signals, be they used for monitoring or feedback purposes, were simultaneously conditioned and digitized using a 12 bit AD board at a sampling frequency of 3.5 kHz per channel. The duration of each record was about 20 s.

Both flow visualization and PIV measurements were conducted using Dantec standard PIV2100 system. Flow was seeded by smoke generated from Paraffin oil and was illuminated in the plane of mean shear by two new wave standard pulsed laser sources of a wavelength of 532 nm, each having a maximum energy output of 120 mJ. Digital particle images were taken using one charge coupled device camera (HiSense type 13, gain ×4, single frame for flow visualization or double frames for PIV, 1280×1024 pixels). A Dantec FlowMap Processor (PIV2100 type) was used to synchronize image taking and illumination. A wide-angle lens was used so that each image covered an area of 133 mm×105 mm of the flow field, i.e., x/d=0.35–13.65 and y/d=−5.25–5.25 for both flow visualization and PIV measurements. The longitudinal and lateral image magnifications were identical, i.e., 0.10 mm/pixel. In the image processing, 32 rectanglar interrogation areas were used. Each interrogation area or double frames for PIV, approximately 2.5 mm or 0.25 p

is then repeated on the basis of the optimum $A_{Y,p,\text{opt}}$ and $T_{Y,p,\text{opt}}$ to arrive at the final optimal combination of $A_{Y,m,\text{opt}}$ and $T_{Y,m,\text{opt}}$ for the closed-loop controller.

Figures 2 and 3 show the variation of $p$ with respect to $A_{Y,m}$ and $T_{Y,m}$ for the two control schemes, respectively. For $p$-control scheme, $A_{Y,p}$ was first adjusted with $T_{Y,p}$ fixed at 0 s [Fig. 2(a)]. Evidently, at $A_{Y,p}=3$, $p_{\text{rms}}/p_{\text{rms,nc}}$ is the lowest, where $p_{\text{rms,nc}}$ represents the rms value of $p$ without control. The unperturbed case is given in Figs. 2 and 3 by $p_{\text{rms}}/p_{\text{rms,nc}}=1$ and $A_{Y,p}=0$, as indicated by a dashed line. Then, $T_{Y,p}$ was varied within a vortex shedding cycle at $A_{Y,p}=3$ [Fig. 2(b)]. At $T_{Y,p}=0.001 52 s$, $p_{\text{rms}}/p_{\text{rms,nc}}$ displays its minimum, a 30.2% fall, and reaches the maximum at $T_{Y,p}=0.004 02 s$, a 4% amplification, compared with the unperturbed case. This time delay, i.e., 0.004 02–0.001 52 s, corresponds roughly to one half of the period of the vortex shedding, suggesting an anti-phased relation between the two extreme cases. With $T_{Y,p}$ set at 0.001 52 s, $A_{Y,p}$ was returned. The lowest $p_{\text{rms}}/p_{\text{rms,nc}}$ occurs again at $A_{Y,p}=3$ [Fig. 2(c)], where $p_{\text{rms}}/p_{\text{rms,nc}}$ is reduced by 30%, almost the same as that using $A_{Y,p}=3$ and $T_{Y,p}=0.001 52 s$ in Fig. 2(b). Further iterations performed failed to improve the control performance appreciably, implying that the results were already converged in the first three iterations, i.e., $A_{Y,p}=3$ and $T_{Y,p}=0.001 52 s$ were the optimum parameters of $p$-control scheme. The same optimization procedures were followed for $u$-control (Fig. 3) and $A_{Y,p}=1.5$ and $T_{Y,p}=0.001 77 s$ were found to be the optimum parameters. The whole tuning process finally led to an optimal configuration for each control scheme with the following parameters: $A_{Y,p}=3$, $T_{Y,p}=0.001 52 s$ for $p$-control; and $A_{Y,p}=1.5$, $T_{Y,p}=0.001 77 s$ for $u$-control. Unless otherwise stated, these parameters have been used to obtain the results discussed hereinafter.

III. PARAMETER OPTIMIZATION OF CLOSED-LOOP CONTROLLER

Two control schemes were investigated, referred to as $p$-control and $u$-control, using feedback signals from $p$ and $u$, respectively. Both control schemes aim at reducing $p$. This was achieved by manually tuning two parameters involved in the feedback controller, i.e., an amplitude gain coefficient ($A_{Y,m}$) and a time shift ($T_{Y,m}$) between the perturbation displacement $Y$ and the feedback signal $m$ (representing $p$ or $u$) of a closed-loop controller. The tuning process is to determine an optimum combination of $A_{Y,m}$ and $T_{Y,m}$, which makes the rms value, $p_{\text{rms}}$, of $p$ the minimum. The optimization procedure is as follows. First, vary $A_{Y,m}$ by keeping $T_{Y,m}=0 s$ to find a $A_{Y,m}$, yielding a minimum $p_{\text{rms}}$. Second, given $A_{Y,m}$, vary $T_{Y,m}$ within a range from 0 to 0.005 s to determine $T_{Y,m}$, under which $p_{\text{rms}}$ reaches the smallest. The reason for choosing the duration of 0.005 s is due to the dominance of $f_s (=220 Hz)$ in the signal responses. Thus, the optimum $T_{Y,m}$ can be determined for one complete cycle of the vortex shedding, i.e., $1/f_s (=0.005 s)$. The whole process

IV. CONTROL PERFORMANCE

Using the above-mentioned tuned controllers, each control scheme was individually assessed to evaluate the control performance in terms of reducing $p$. Figure 4 shows the typical time histories of the fluctuating pressure coefficient $C_p=\rho U_\infty^2/m$, with and without control, where $\rho$ is air density. Compared with the unperturbed case [Fig. 4(a)], the rms value of $C_p$ deceases by 30% for $p$-control [Fig. 4(b)] and by 39% for $u$-control [Fig. 4(c)].

The $p$ spectrum, $fE_p$ [Fig. 5(a)], displays a pronounced peak (7.45) at $f_s$ (in 4.44 (a reduction by 40%), suggesting the improvement in the energy of $p$ [Fig. 5(b)]. Yet, $u$-control leads to an even better performance, resulting in a reduction by 56% [Fig. 5(c)]. A more accurate method to estimate the energy of $p (E_pA_\Delta f)$ associated with $f_s$ is to integrate $E_p$ over a $-3 \text{ dB}$ bandwidth about $f_s$ and then is multiplied by $p_{\text{rms}}$. 

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FIG. 2. Dependence of the root mean square value \( \langle p_{\text{rms}} \rangle \) of the fluctuating flow pressure on the amplitude gain coefficient \( A_{Yp} \) or the time shift \( t_{Yp} \) between perturbation displacement \( Y_p \) and \( p \) under different control cases when \( p \)-control was deployed: (a) \( t_{Yp} = 0 \) s; (b) \( A_{Yp} = 3 \); and (c) \( t_{Yp} = 0.00152 \) s.

FIG. 3. Dependence of the root mean square value \( \langle p_{\text{rms}} \rangle \) of the fluctuating flow pressure on the amplitude gain coefficient \( A_{Yp} \) or the time shift \( t_{Yp} \) between perturbation displacement \( Y_p \) and fluctuating flow velocity \( u_1 \) under different control cases when \( u \)-control was deployed: (a) \( t_{Yp} = 0 \) s; (b) \( A_{Yp} = 1.5 \); and (c) \( t_{Ypu} = 0.00177 \) s.
Compared with the uncontrolled case, $E_{p,\Delta f}$ is effectively reduced by 52% for $p$-control and by 62% for $u$-control, seen in Table II.

Figure 6 shows the autocorrelation function, $R_{pp}(\tau)$, of $p$. The autocorrelation function of a signal $\alpha$, i.e., $R_{\alpha\alpha}(\tau)$, is defined as

$$R_{\alpha\alpha}(\tau) = \lim_{T \to \infty} \frac{1}{T} \int_0^T \alpha(t)\alpha(t+\tau)dt,$$

where $T=20$ s and $\tau$ represent the sampling time and time delay, respectively.\cite{28} Without control, $R_{pp}(\tau)$ [Fig. 6(a)] is essentially sinusoidal, displaying the same frequency as $f_c=220$ Hz, its amplitude reaching 0.4 (excluding those near $\tau=0$) and decaying very slowly. The observation indicates that the unperturbed $p$ is characterized by a strong periodicity associated with the oncoming Kármán vortices. With the control activated, however, $R_{pp}(\tau)$ [Figs. 6(b) and 6(c)] is significantly less periodic, with its amplitude decreased by roughly 50% in both cases. This loss in both the periodicity and the strength of $R_{pp}(\tau)$ shows the destructive effect of the perturbation on $p$.

The results demonstrate unequivocally the effectiveness of the present control technique on suppressing $p$ and thus the BVI noise. Further, $u$-control is superior to $p$-control. It should be mentioned that the control voltage $V_p$ required is approximately 80 and 53 V for $p$-control and $u$-control, respectively. The input energy $E$ to the actuators may be approximately given by $E=2\pi f_c C V_p^2$\cite{29} where $C(=4.16 \times 10^{-9}$ F) represents the capacitance of the actuator. $E$ is about 0.037 and 0.016 J for $p$-control and $u$-control, respectively. It may be concluded that $u$-control outperforms $p$-control despite less actuation energy.

**Table II.** Comparison in the energy reduction percentage of the fluctuating flow pressure ($p$) and the fluctuating streamwise flow velocity ($u$) at $f_c$ between the two control schemes.

<table>
<thead>
<tr>
<th>Energy</th>
<th>$p$-control</th>
<th>$u$-control</th>
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<tr>
<td>$E_{p,\Delta f}$</td>
<td>52% ↓</td>
<td>62% ↓</td>
</tr>
<tr>
<td>$E_{u,\Delta f}$</td>
<td>33% ↓</td>
<td>42% ↓</td>
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FIG. 4. Typical time histories of fluctuating pressure coefficient ($C_p$): (a) unperturbed; (b) $p$-control; and (c) $u$-control. The time origin is arbitrary.

FIG. 5. Weighted power spectrum of the fluctuating flow pressure ($p$): (a) unperturbed; (b) $p$-control; (c) $u$-control.

FIG. 6. The $p$-autocorrelation function $R_{pp}(\tau)$: (a) unperturbed; (b) $p$-control; and (c) $u$-control.
V. DISCUSSIONS

A. Perturbed oncoming vortical flow

It is of relevance to examine how the oncoming vortical flow responds to the control in order to improve our understanding of the underlying physics. Note that hot wire 2 was placed at \( x/d = 10 \) and \( y/d = -1 \), close to the airfoil leading edge, where intensive vortex-airfoil interactions occur.\(^{26}\) Figure 7 shows the weighted power spectra, i.e., \( fE_u \), of \( u_2 \) with and without the closed-loop control. Compared with the unperturbed case [Fig. 7(a)], the peak in \( fE_u \) at \( f_2 \) retreats by 25% and 32% for \( p \)-control [Fig. 7(b)] and \( u \)-control [Fig. 7(c)], respectively. The energy of \( u_2 \) around \( f_2 \), i.e., \( E_{u_2} \), calculated similarly to \( E_{p,\Delta t} \), exhibits a reduction by 33% for \( p \)-control and by 42% for \( u \)-control (Table II), suggesting a substantial impairment in the oncoming vortical structures. As a matter of fact, both the periodicity and the vortical structure strength have been attenuated once \( p \)- or \( u \)-control is introduced, as suggested by the \( u_2 \)-autocorrelation function \( R_{u_2}(\tau) \) (Fig. 8). The difference in the reduction percentage between \( p \)- and \( u \)-control further indicates the superiority of \( u \)-control over \( p \)-control.

The control does not simply influence the oncoming flow locally. Figures 9 and 10 present typical flow visualization photos and the PIV-measured iso-contours of spanwise vorticity, \( \omega_z = \omega_z / U_{\infty} \), respectively. Once hitting the airfoil leading edge, the oncoming Kármán vortices are rapidly distorted into elongated elliptical structures with their major axes parallel to the mean flow. Gursul and Rockwell\(^{26}\) attributed this deformation to the strain field around the airfoil leading edge. As shown in Figs. 9(b), 9(c), 10(b), and 10(c), the vorticity concentration region of the stretched vortices appears thinner, especially below the airfoil leading edge, which is near the perturbed surface, suggesting a reduced vortex strength. In fact, the averaged maximum spanwise vorticity (\( \omega_{z,\text{max}} \)) and circulation (\( \Gamma \)), estimated based on 25 PIV images, measured below the airfoil leading edge decrease by 21% and 31% for \( p \)-control, respectively, and by 28% and 40% for \( u \)-control, respectively, when compared with the unperturbed case. Each circulation \( \Gamma \) around a vortex is estimated by numerical integration of

\[
\Gamma = \int \frac{1}{2 \pi} \omega_z \Delta A \Delta t
\]

where \( \omega_z \) is spanwise vorticity over area \( \Delta A = \Delta x \Delta y \), \( \Delta x \) and \( \Delta y \) being the integral step along \( x \) and \( y \) directions, respectively. The magnitude \( |\omega_z| = 0.1 \), about 7% of \( \omega_{z,\text{max}} \), has been used as the cutoff level, which is the same as the level used by Cantwell and Coles.\(^{30}\)

Figure 11 shows the lateral distribution of fluctuating streamwise flow velocity \( u_{\text{rms}} \), i.e., the rms of \( u \), measured at \( x/d = 10 \) and \( y/d = -0.5, -1, -1.5, \) and \(-2 \) using a hot wire. The measured \( u_{\text{rms}} \) is normalized by its uncontrolled counterpart, i.e., \( u_{\text{rms,nc}} \). Under both control schemes, \( u_{\text{rms}} \) drops considerably, conforming to the observation from the spectra (Fig. 7). The maximum reduction occurs near the airfoil leading edge, where \( u_{\text{rms}} / u_{\text{rms,nc}} \) is 0.79 and 0.82 for \( u \)-control and \( p \)-control, respectively; the reduction is still appreciable, \( u_{\text{rms}} / u_{\text{rms,nc}} = 0.95 \), even at \( y/d = -2 \). The observation is internally consistent with flow visualization and PIV results. It may be concluded that the surface perturbation near the airfoil leading edge has made a pronounced modification on the oncoming flow field.

Strictly speaking, the BVI noise should be really estimated by pressure distributions along the front of the airfoil, but not just pressure at a point. However, the measurement of the pressure distribution around the airfoil leading edge is very difficult, if not impossible, because a large number of sensors are required. Under the bombardment of the oncoming vortices, the rise of the fluctuating flow pressure induced by the distortion of the vortices near the airfoil leading edge should

FIG. 7. Weighted power spectrum of the fluctuating flow velocity \( u_2 \): (a) unperturbed; (b) \( p \)-control; and (c) \( u \)-control.

FIG. 8. The \( u_2 \)-autocorrelation function \( R_{u_2}(\tau) \): (a) unperturbed; (b) \( p \)-control; and (c) \( u \)-control.
spread a certain spatial extent and be reasonably continuous, not a spike. This is supported by instantaneous flow visualization and PIV results (Figs. 9 and 10), which show that the interaction between the airfoil surface and incoming large-scale vortices occurs over almost the entire leading edge of the airfoil. This interaction determines to a great extent the fluctuating flow pressure $p$. Therefore, $p$ measured at one point should be able to provide a good indication of the average level of the pressure distribution over the airfoil leading edge.

**B. Perturbed vortex-airfoil interactions**

Insight may be gained into the perturbation vortex-airfoil interaction process by examining the spectral phases between simultaneously measured $u_1$, $u_2$, $p$, and $Y_p$ with and without control, i.e., $\Phi_{a_1a_2} = \tan^{-1}(Q_{a_1a_2}/Co_{a_1a_2})$, where $a_1$ and $a_2$ represent two signals and $Co_{a_1a_2}$ and $Q_{a_1a_2}$ stand for the co-spectrum and quadrature spectrum, respectively. The co-spectrum and quadrature spectrum, i.e., $Co_{a_1a_2}$ and $Q_{a_1a_2}$, are defined by

$$Co_{a_1a_2}(f) = 2 \int_{-\infty}^{\infty} R_{a_1a_2}(\tau) \cos 2\pi f \tau d\tau,$$  \hspace{1cm} (2)

$$Q_{a_1a_2}(f) = 2 \int_{-\infty}^{\infty} R_{a_1a_2}(\tau) \sin 2\pi f \tau d\tau$$  \hspace{1cm} (3)

(Ref. 28), where $\tau$ and $R_{a_1a_2}$ are time delay and cross-correlation function between $a_1$ and $a_2$, respectively. $R_{a_1a_2}$ is defined by

$$R_{a_1a_2}(\tau) = \lim_{T \to \infty} \frac{1}{T} \int_0^T a_1(t) a_2(t+\tau) dt$$  \hspace{1cm} (4)

(Ref. 28). The spectra were computed using a fast Fourier transform method.\textsuperscript{31} As an example, Fig. 12 shows the effect of the $u$-control scheme on the spectral phase $\Phi_{a_1u_1}$ between $u_2$ and $u_1$, where $u_1$ and $u_2$ were measured using hot wire 1 at $x/d=8$ and $y/d=-1$ and hot wire 2 at $x/d=10$ and $y/d=-1$. Without perturbation, $\Phi_{a_1u_1}$ [Fig. 12(a)] and also $\Phi_{a_1Y_p}$ at $f'_s$ are approximately zero, suggesting that the unperturbed $p$, $u_1$, and $u_2$ are all in-phase at the dominant vortex frequency. In the wake of a circular cylinder, the Kármán vortex wavelength is about $4.3d$ for the present Re. Since the hot wires 1 and 2 were longitudinally separated by $2d$ (Sec. II), about one-half of the wavelength, their measured $u_1$ and $u_2$ should be antiphased at $f'_s$ in the absence of the airfoil,
which may be inferred from Fig. 9(a) in Zhou et al.23 The present observation of the in-phased \( u_1 \) and \( u_2 \) at \( f_s' \) suggests that the incident vortical flow has been significantly modified as a result of the vortex-airfoil interaction.

Under \( u \)-control, \( \phi_{u/u_1} \) at \( f_s' \) is now about \(-0.7 \) [Fig. 12(b)]. However, \( \phi_{u/u_2} \) at \( f_s' \) remains zero. As mentioned earlier, the measurement location of \( u_1 \) was carefully selected so that \( u_1 \) would not be influenced by the vortex-airfoil interaction. It may be inferred that it is the perturbation that has modified the phase of \( u_2 \) with respect to \( u_1 \) at \( f_s' \). In the \( u \)-control, the time shift \( \tau_{u/u_1} \) between \( u_1 \) and \( u_2 \) was set to be 0.001 77 s, roughly corresponding to a phase shift \( \phi_{u/u_1} = 2.44 \) \((=0.001 77 \times 2\pi \times f_s, f_s = 220 \) Hz\) at \( f_s' \). Noting \( \phi_{u/u_1} = -0.7 \) at \( f_s' \), the spectral phase \( \phi_{u/u_2} \) between \( u_1 \) and \( u_2 \) at \( f_s' \) should be approximately \( \pi \) or antiphased. Similarly, \( \phi_{u/u_2} \) at \( f_s' \) should also be approximately \( -\pi \) under the \( p \)-control scheme. This is indeed confirmed by the result of \( \phi_{u/u_2} \), where \( \phi_{u/u_2} \) at \( f_s' \) is close to \( -\pi \) for both control schemes, indicating that the measurements are internally consistent.

In an investigation to control the flow-induced vibration on a laterally oscillating square cylinder, Cheng et al.18 found that the spectral phase between the lateral structural displacement \( Y \) and the streamwise fluctuating flow velocity \( u \) was approximately equivalent to that between the lateral cylinder oscillating velocity, \( \hat{Y} \), and the lateral flow velocity \( v \). The present case differs from theirs in that the airfoil leading edge is bombarded by incident vortices, instead of shedding vortices. Therefore, one experiment was carried out to investigate the relationship between flow and perturbation force. One movable \( X \) wire was used to measure the lateral fluctuating flow velocity \( v \) near the lower side of the airfoil leading edge, simultaneously with the airfoil surface perturbation velocity \( \hat{Y}_p \) measured using a laser vibrometer. The spectral phase \( \phi_{v/\hat{Y}_p} \) between \( v \) and \( \hat{Y}_p \) at \( f_s' \) (not shown) was close to \(-\pi \) for both \( p \)- and \( u \)-control schemes when the \( X \) wire was moved over \( x/d = 9.8 – 11 \) and \( y/d = -1.25 \) to \(-0.7 \), which was near the perturbed surface \((x/d = 10.7 \) and \( y/d = -0.5 \)). The airfoil surface perturbation velocity \( \hat{Y}_p \) is approximately in the lateral direction since the normal direction of the perturbation surface is about \( 87^\circ \) with respect to the longitudinal direction [Fig. 1(a)]. Note that, with the two actuators activated simultaneously, \( \hat{Y}_p \) is uniform along the spanwise direction. Considering the two dimensionality of flow and perturbation, \( \phi_{v/\hat{Y}_p} = -\pi \) at \( f_s' \) should hold along the spanwise direction. This phase relationship means the opposite or collided movements between the local airfoil surface perturbation and the local vortical flow, which exerts a significant influence on the whole unsteady vortex structure and subsequently weakens the vortex strength. This vortex strength reduction, presently by \( 31\% \) (Figs. 9 and 10), will in turn cause a weakened fluctuating flow pressure near the airfoil leading edge and subsequently its induced BVI noise because of their close link.3,15

One may surmise that the vortex-airfoil interaction could modify the spectral phase \( \phi_{u/u} \) between the streamwise and
uated with and without control. Thus obtained
approximately −π/2 in both cases (not shown), indicating
that the phase between u and v is not affected by control.

Therefore, the equivalence between Y_p−u and Y_p−v still holds presently in terms of the phase relationship.

As previously discussed, the unperturbed u_2 and p should be in-phased, i.e., Φ_{u,p}=0, at f'_s. This is indeed verified in Fig. 13(a). Once the p-control is applied, Φ_{u,p} may be derived by subtracting Φ_{u,p} from the spectral phase Φ_{u,p}.

Recall that the time shift t_y,p under the p-control is 0.001 52 s. Then Φ_{y,p} at f'_s can be approximately calculated by Φ_{y,p}=t_y,p×2π×f_s=2.1. Thus Φ_{u,p}=Φ_{y,p}−Φ_{y,p}=2.1
(−π) or −2π+2.1−(−π)≈−1. On the other hand, for the u-control, Φ_{u,p} at f'_s is the difference between Φ_{u,p}(=0.7) and Φ_{u,p}(=0). The previous analysis is indeed confirmed by Φ_{u,p} at f'_s calculated from the simultaneously measured u_2 and p [Figs. 13(b) and 13(c)].

The phase shift at f'_s between u_2 and p may reflect the vortex-airfoil interaction because u_2 contains the information on the distortion of the incident vortical flow near the airfoil leading edge, which is responsible for the generation of the fluctuating flow pressure, and subsequent the BVI noise. In the absence of perturbation, u_2 synchronized with p always over a range of frequencies about f'_s [Fig. 13(a)]. Once the control is introduced, Φ_{u,p} between u_2 and p at f'_s are changed [Figs. 13(b) and 13(c)], implying an altered vortex-airfoil interaction. The spectral coherence Coh_{u,p}=[(C_0^2
+Q_{u,p}^2)/E_{u,p}E_{p}] provides a measure of the degree of correlation between the Fourier components of u_2 and p. The peak in Coh_{u,p} at f'_s reaches about 0.37 without perturbation [Fig. 14(a)], suggesting a strong correlation between the oncoming vortices and airfoil. However, this peak recedes by 46% for the p-control [Fig. 14(b)] and by 60% for the u-control [Fig. 14(c)], that is, the oncoming vortices and the fluctuating flow pressure on the airfoil become weakly correlated.

FIG. 13. Spectral phase Φ_{u,p} between the fluctuating flow velocity (u_2) and the fluctuating flow pressure signal (p): (a) unperturbed; (b) perturbed under p-control; and (c) perturbed under u-control.

FIG. 14. Spectral coherence Coh_{u,p} between the fluctuating flow velocity (u_2) and the fluctuating flow pressure signal (p): (a) unperturbed; (b) p-control; and (c) u-control.
Based on the above-mentioned analyses, an interpretation for the impaired $p$ is now proposed. The impulsive fluctuating flow pressure and hence the BVI noise originate from the drastic distortion of the incident vortices, which is created by the vigorous interaction between vortices and airfoil leading edge. As demonstrated earlier, the closed-loop controlled surface perturbation $\tilde{Y}_p$ and $v$ associated with the incoming vortices are antiphased. This “collision” may act to reduce substantially the vortex strength, and hence the fluctuating flow pressure on the airfoil leading edge or the BVI noise.

Using the $u$-control scheme, the feedback signal is taken from the vortical flow upstream of the airfoil, which is the excitation source. Therefore, the effect of the control action is to modify directly the flow excitation and subsequently $p$. On the other hand, the $p$-control scheme uses $p$, i.e., the passive response of the vortex-airfoil interaction, as the feedback signal, instead of the excitation source. Consequently, the control performance is less effective than the $u$-control scheme, even though the input energy is nearly 2.3 times that used in the $u$-control scheme.

VI. CONCLUSIONS

Closed-loop control of the vortex-airfoil interaction has been experimentally investigated with a view to suppress the BVI noise. Two control schemes were investigated and compared, leading to the following conclusions.

1. The proposed closed-loop control technique is effective to suppress the pressure rise, generated from vortex-airfoil interactions, at the leading edge of the airfoil. The choice of the feedback signal may have considerable effect on the control performance. The $u$-control scheme reduced the rms value of $p$ by 39% whereas $p$-control scheme reduced that by 30%. The difference is ascribed to the fact that the $u$ signal is linked to the excitation source of the noise, whereas the $p$ signal reflects the passive response of the vortex-airfoil interaction. The observation points to a crucial role of the feedback signal played in the closed-loop control of the BVI noise.

2. In a successful control, an antiphased relationship occurs between the controlled perturbation velocity and the lateral fluctuating flow velocity associated with incident vortices. This antiphase relationship significantly impairs the oncoming vortex strength and vortex-airfoil interaction, resulting in a remarkable reduction in the fluctuating flow pressure around the airfoil leading edge and subsequent the BVI noise.

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