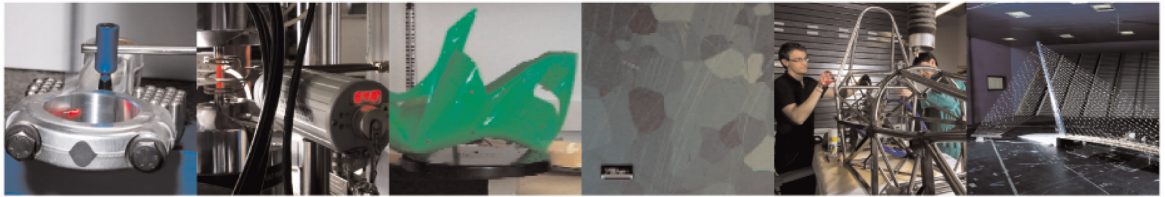




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Effect of the low-frequency turbulence on the aeroelastic response of a long-span bridge in wind tunnel

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Effect of the low-frequency turbulence on the aeroelastic response of a long-span bridge in wind tunnel

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Abstract

1 The influence of the low frequency turbulence components on the buffeting response
2 of long span bridges was studied through experimental tests performed on a full bridge
3 aeroelastic model in the wind tunnel of the Politecnico di Milano, using an active turbu-
4 lence generator producing a correlated deterministic harmonic turbulence. The experi-
5 mental evidence underlined the nonlinear effect of the low frequency incoming turbulence
6 on the dynamic resonant response of the structure at higher frequencies.

7 Numerical simulations are used to explain the bridge behavior considering the vari-
8 ation of the aeroelastic properties of the bridge with the instantaneous angle of attack
9 and reduced velocity. Even though wind tunnel experiment uses an oversimplified wind
10 spectrum with an intentionally high correlation along the main span, it helps to un-
11 derstand the nonlinear interaction between the low frequency and the high frequency
12 buffeting response on a full bridge.

13 *Key words:*

14 wind tunnel, aeroelastic model, long span bridge, band superposition, buffeting,
15 aeroelasticity

16 1. Introduction

17 In bridges aerodynamics, modeling the non-linearities of wind loads acting on decks is
18 still an open issue. International research groups started recently to compare the results
19 of the most widely used numerical approaches in long span bridge design (Diana et al.,
20 2019a,b) to compute the aeroelastic response of the structure. Most of these approaches

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21 rely on linearized numerical models to compute the aerodynamic forces acting on bridge
22 elements (mainly the deck), considering small variation of the relative angle of attack
23 between incoming wind and bridge deck around the static position taken by the structure
24 under the action of mean wind speed.

25 While this linearization hypothesis is valid when the contribution of the deck rotation
26 to the variation of the instantaneous angle of attack is considered (a good aerodynamic
27 design aims to small vibrations), large variations of angle of attack can be anyhow pro-
28 duced by the turbulent fluctuations of the incoming wind velocity. This consideration
29 is widely agreed in bridge aerodynamics and it is supported by full scale measurements
30 (e.g. Fenerci and Øiseth (2018); Bocciolone et al. (1992); Hui et al. (2009)). Modern
31 full-scale experimental techniques (e.g. LIDARS, see Cheynet et al. (2017a,b); Mikkelsen
32 et al. (2017)), adopted in recent full scale monitoring, allow today to better investigate
33 the characteristics of the real wind blowing on long span bridges confirming the pres-
34 ence of large variation of the vertical component of the wind velocity and thus of the
35 instantaneous angle of attack on the deck.

36 Looking at the spectrum of the turbulent incoming wind and to the physics of the
37 Atmospheric Boundary Layer, the largest variations of the turbulent wind velocity com-
38 ponents are due to low frequency contributions of the large scale eddies and this consid-
39 eration is at the base of numerical methods to compute bridge buffeting response called
40 “Band superposition” methods (Diana et al., 1995; Chen and Kareem, 2001). These
41 approaches aim at reproducing with different numerical modeling the “Low Frequency”
42 (LF) and the “High Frequency” (HF) part of the aeroelastic response. The adoption
43 of different numerical models to compute the aerodynamic forces in the two frequency
44 ranges is based on the assumption that the aerodynamic forces dependency on reduced
45 velocity (V^*) is weak at LF, where large variations of the instantaneous angle of attack
46 occur, while it is stronger at HF where the variation of the instantaneous angle of attack
47 is small.

48 Quasi-Steady Theory approaches (Diana et al., 1995; Chen and Kareem, 2001) proved
49 to be effective numerical models to reproduce the aerodynamic forces dependency on the

50 angle of attack when V^* dependency is weak and they are used to compute the LF part
51 of the aeroelastic response. Usually the angle of attack dependency is strongly nonlinear
52 for the drag force for all the bridge deck typologies and it is increasingly nonlinear also
53 for the lift force and for the aerodynamic moment, in particular if multi-box deck sections
54 are considered (Diana et al., 2008). Simulation of the HF range is performed separately
55 from the LF range using conventional linear methods based on flutter derivatives and
56 admittance functions in time domain where aerodynamic coefficients are updated at each
57 time step using the instantaneous LF angle of attack.

58 Time domain models are required to model the interaction between the LF and HF
59 aeroelastic response since the LF variation of the angle of attack is time dependent.
60 Taking into account the large V^* dependency in the HF range, this is a challenging task
61 for time domain models and research is still active on this topic. Volterra series methods
62 (Wu and Kareem, 2014; Carassale et al., 2014) and rheological models (Diana et al.,
63 2013a) are two recent approaches to deal with this problem. An application of these
64 methods in the full range of V^* and angle of attack is limited by the need of specific
65 experimental wind tunnel tests to identify the numerical model parameters.

66 Rheological models identification for instance is based on aerodynamic hysteresis
67 loops and the model is proposed to overcome Quasi Steady Theory limitation and to
68 try to model the dependency of the aerodynamic forces upon both angle of attack and
69 reduced velocity in the LF and HF range (Diana et al., 2010, 2013a).

70 Independently from the applied numerical modeling, Band-Superposition methods
71 are today more used in research than in practical bridge design where LF-HF interac-
72 tion is usually neglected. Nevertheless the evolution of long span bridges towards more
73 complex aerodynamic solutions (longer spans, multi-box decks, environmental effects,..)
74 and the availability of more performing experimental facilities and larger computation
75 power help to better investigate the aeroelastic problem and the interaction between LF
76 and HF contributions and to develop better numerical models to take into account these
77 effects also on conventional bridge solutions like the one investigated in this paper.

78 At present, investigations on LF and HF range interaction are usually performed by

79 means of wind tunnel tests on sectional deck models using turbulence active generation
80 with both forced motion or elastically suspended set-ups (Diana et al., 2010, 2013b; Ma
81 et al., 2013) , and no evidence is present in literature on full aeroelastic models. In this
82 paper, we present the experimental results of a wind tunnel campaign performed on an
83 aeroelastic model of long span-bridge (1:220 scale) aimed at highlighting the effects of LF
84 incoming turbulence on the dynamic HF response of the structure. To this end, an active
85 turbulence generator was used to generate a correlated deterministic harmonic turbu-
86 lence. The active turbulent generator is able to produce a multi-harmonic perturbation
87 of the flow with a strongly correlated vertical component of the wind velocity.

88 The perturbation consists in the superposition of single harmonics in order to simplify
89 the incoming turbulent wind. The usual wind tunnel tests performed on full bridge
90 aeroelastic model with the passive reproduction of the whole turbulent wind spectrum
91 does not allow to appreciate the non linear interaction since the contributions of all the
92 harmonics is overlapped.

93 In this case, the response of the structure was recorded under two different incoming
94 flow conditions:

- 95 a) HF turbulence component only;
- 96 b) superposition of HF and LF turbulence components.

97 Comparing the high frequency responses of the bridge in the two different cases, it
98 is possible to highlight the interaction effects of the LF incoming turbulence that can
99 be analyzed and explained using numerical models. Indeed, numerical simulations are
100 performed in order to explain the different aeroelastic behavior of the bridge: initially an
101 eigenvalue-eigenvectors analysis of the aeroelastic system is performed to show the effect
102 of the dependence of aerodynamic forces on the angle of attack and reduced velocity.
103 Furthermore, a Band Superposition simulation in time domain is performed to highlight
104 the method capability to reproduce the strongly non-linear aeroelastic response.

105 **2. Experimental setup**

106 *2.1. Aeroelastic model of the full bridge*

107 The bridge studied in the present research is the Izmit Bay Bridge, a three spans
108 suspension bridge with a main central span of 1550 m and two side spans of 566 m.
109 Each tower is a 252 m high steel structure having two crossbeams connecting the two
110 tower legs at the middle level and at the top. Towers foundations are placed on the
111 gravel bed, at 40 m below the water level. Main cables are deviated at the side span
112 piers and anchored at the cable anchor blocks. The bridge deck is a classical streamlined
113 single box (with a three-lane dual carriageway with guardrails), 36.4 m wide and 4.75 m
114 deep, having 2.8 m wide inspection walkway with parapets on both sides. The general
115 arrangement of the bridge and the deck cross-section are shown in Figure 1. Aeroelastic
116 tests were performed on an aeroelastic model of the full bridge in the Boundary Layer
117 Wind Tunnel of Politecnico di Milano. The model was realized in a 1:220 geometrical
118 scale, using Froude similarity (Diana et al., 2013b; Argentini et al., 2016).

119 *2.2. Aerodynamic coefficients*

120 Static aerodynamic coefficients and torsional flutter derivatives were measured for
121 the deck. The aerodynamic static coefficients were measured on a sectional model, 1.5 m
122 long, with the same geometrical scale of the full-bridge to match the Reynolds number
123 of the aeroelastic tests. The sectional model was mounted inside two vertical flat plates
124 to guarantee a bi-dimensional flow. Outside the flat plates, the model was supported by
125 two six-axis force balances. Vertical and transverse motions were constrained, while the
126 rotation was imposed on both sides by means of an electric motor.

127 Since large angles of attack were expected in the aeroelastic tests presented in this
128 study, aerodynamic coefficients were measured as a function of a wide range of mean
129 angles of attack α : static coefficients from -10 deg to +10 deg, with step 2 deg; flutter
130 derivatives coefficients from -6 to +6 deg, with step 2 deg.

Considering the sign conventions reported in Figure 2, static drag, lift and moment,

acting on the deck section per unit length, are defined as:

$$F_D = \frac{1}{2}\rho BU^2 C_D(\alpha) \quad (1)$$

$$F_L = \frac{1}{2}\rho BU^2 C_L(\alpha) \quad (2)$$

$$M = \frac{1}{2}\rho B^2 U^2 C_M(\alpha) \quad (3)$$

131 where $C_{D,L,M}$ are the static aerodynamic coefficients, ρ is air density, B is the deck
 132 chord, U is the mean wind speed, α is the angle of attack. The static force coefficients
 133 are reported in Figure 3a), and they have classical values and slopes of closed-box deck
 134 sections.

The self-excited lift and moment per unit length, acting on the deck, related to the torsional motion θ can be defined as (using the definition in (Zasso, 1996):

$$L_{se} = \frac{1}{2}\rho U^2 B \left(-h_2^* \frac{B\dot{\theta}}{U} + h_3^* \theta \right) \quad (4)$$

$$M_{se} = \frac{1}{2}\rho U^2 B^2 \left(-a_2^* \frac{B\dot{\theta}}{U} + a_3^* \theta \right) \quad (5)$$

135 where a_i^*, h_i^* are the flutter derivative coefficients, function of reduced velocity $V^* = \frac{U}{fB}$
 136 (f frequency of motion) and mean angle of attack α .

137 Flutter derivatives were measured with the same setup and model, using a forced
 138 motion technique (Diana et al., 2004), and they are reported in Figure 3b), for the
 139 reduced velocity $V^* = 11.7$, as a function of the mean angle of attack. The reason why
 140 this specific reduced velocity is shown will be explained in the next Sections.

141 However, we can introduce a first comment about the value of a_2^* coefficient: using
 142 the definition in Eq.(5), $a_2^* > 0$ means positive torsional aerodynamic damping, while
 143 $a_2^* < 0$ means negative damping; looking at the values reported in Figure 3b), it is
 144 possible to notice that $a_2^* > 0.4$ for mean values of α in the range between -6 and +4
 145 deg, while its value decreases to 0.1 for $\alpha = 6$ deg, therefore the aerodynamic damping
 146 is largely reduced if α exceeds +4 deg. This specific trend will be used to explain some
 147 experimental findings in the following Sections.

148 Vertical and horizontal unsteady aerodynamic coefficients were not measured, and,
 149 in the numerical simulations, their values were inferred from quasi-steady theory.

150 *2.3. Active turbulence generator*

151 An active turbulence generator, sketched in Figure 4, was used to generate harmonic
152 wind waves (Diana et al., 2013a). The generator, 4 m wide and placed 7 m upwind
153 the model, is composed by a vertical array of 10 NACA 0012 airfoils with a chord of
154 0.2 m each). All the airfoils were driven synchronously by two brushless motors giving
155 a pitching motion according to a user-defined motion law in terms of frequency and
156 amplitude. No atmospheric boundary layer is reproduced during the aeroelastic tests
157 and the airfoils deflect the incoming smooth wind causing a 4 m wide coherent wave.
158 This wave excite most of the main span of the bridge, whose length is 5 m, while the
159 total length of the bridge is 12 m (Figure 4b)).

160 The flow was measured along the main span, one chord upwind the leading edge of
161 the deck, by means of three 4-holes cobra probes, able to measure the instantaneous
162 vertical and horizontal wind components.

163 By varying the frequency and amplitude of the airfoils oscillation, it is possible to
164 study different turbulent wind conditions. In particular, it is possible to:

- 165 1. generate only LF fluctuations of w , that in turn generates LF variations of the wind
166 angle β_{LF} . Consequently a LF fluctuation of the angle of attack $\alpha_{LF} = \beta_{LF} + \theta_{LF}$
167 (see Figure 2) forces quasi-steadily the bridge, being θ_{LF} the LF torsional response
168 of the deck.
- 169 2. generate only HF fluctuations, usually forcing the bridge in resonance, in order to
170 highlight the aeroelastic response.
- 171 3. generate both HF and LF fluctuations to check if the aeroelastic effects are linear
172 and superposition principle holds, or if nonlinear effects are present and the HF
173 response is influenced by the LF fluctuations.

174 A picture of the experimental setup is shown in Figure 5.

175 *2.4. Bridge dynamic properties*

176 Since the turbulence generator is placed in the middle of the main span, the vibration
177 modes forced by the coherent fluctuations are mainly the symmetrical ones with respect

178 to the center of the bridge.

179 During the wind tunnel test campaign also the flutter instability of the bridge was
180 studied, resulting in a critical speed of 5.53 m/s and a flutter frequency of 2.57 Hz in
181 model scale (full-scale: 82 m/s and 0.173 Hz),(Argentini et al., 2016).

182 The structural modes mainly involved in the flutter are the first and the fifth vertical
183 bending and the first torsional one, named respectively 1V, 5V, and 1T. The experi-
184 mental natural frequencies of these modes are 1.31 Hz, 2.87 Hz and 3.84 Hz respectively
185 (full-scale: 0.0885 Hz, 0.1934 Hz, and 0.2592 Hz), and their mode-shapes (taken from a
186 finite element model) are reported in Figure 6.

187 3. Experimental results

188 Two different wind tunnel tests were performed to investigate the effects of LF co-
189 herent fluctuations of the incoming turbulent wind on the HF aeroelastic response of the
190 bridge:

191 **Case a)** : single-harmonic vertical turbulence component with a HF content at 2.57 Hz
192 (reduced velocity $V^* = V/(fB) = 11.7$) and small amplitude ($\beta_{HF}=1$ deg), repre-
193 senting a simplified contribution of the high frequency part of the wind spectrum;

194 **Case b)** : double-harmonic vertical turbulence component with HF content at 2.57 Hz
195 with small amplitude ($\beta_{HF} = 1$ deg) plus LF content at 0.1 Hz (reduced velocity
196 $V^* = 303$) with amplitude $\beta_{LF} = 2$ deg, representing simplified simulation of the
197 wind spectrum with frequency content both at LF and HF.

198 A mean wind speed of 5 m/s was chosen in both cases, in order to have a strong
199 aerodynamic coupling between modes: indeed, the HF forcing at 2.57 Hz is a frequency
200 near the torsional frequency of the aeroelastic model at a mean wind speed of 5 m/s.

201 3.1. Generated flow

202 The angles of attack for both cases, measured at deck height by the multi-hole probe
203 at mid-span, are shown in Figure 7 in terms of spectra. In Case a), as expected, the flow

204 is characterized by a constant horizontal mean velocity component with a vertical HF
205 velocity component at 2.57 Hz. In Case b), the LF content is clearly visible, and in the
206 HF some sub- and super- harmonics are present, probably due to floor effect. However,
207 in the authors' opinion, this boundary effect can be neglected with regard to the results
208 presented in this paper.

209 In the Supplementary data, the reader can find a video recorded during Case b)
210 tests where it is possible to observe the two overlapped wavelengths of the LF and HF
211 components in the active generator and their effect on the response the bridge.

212 *3.2. Bridge aeroelastic response*

213 Figure 8 shows the recorded vertical accelerations at mid-span, measured with on-
214 board MEMS accelerometers, overlapped to the LF time history of the angle of attack
215 for both cases. Comparing the time histories of the aeroelastic response (red lines) in
216 Case b) with the one in Case a), it is possible to observe that the HF dynamic response
217 is strongly dependent on the LF incoming turbulence.

218 In particular, at the maximum positive LF angles of attack generated by the incoming
219 turbulence the deck response amplitude is more than twice the reference Case a). This
220 result shows that, although in the two cases the mean speed and the HF contents are
221 almost the same, the bridge reacts in two very different ways depending on the LF
222 instantaneous angle of attack.

223 This behavior can be explained looking at the dependence of the unsteady aerody-
224 namic coefficients upon the mean angle of attack. Considering that the static rotation of
225 the deck is 2 degrees nose up at 5 m/s, the angle of attack in Case b) oscillates between
226 -4 and 6 degrees (see Fig. 8b). On the other hand the mean wind speed is steady, this
227 means that the reduced velocity does not change between the two cases, and it is possible
228 to study the trend of the flutter derivatives as a function of the angle of attack, as shown
229 in Figure 2b for $V^* = 11.7$.

230 The aerodynamic coefficients that are more influenced by the angle of attack are
231 a_2^* and h_2^* . Specifically a_2^* , that is the coefficient related to the aerodynamic torsional

232 damping, has a decreasing trend for positive angles of attack, and it reaches nearly 0
233 at +6 deg, as previously already commented. The larger dynamic response observed
234 at $\alpha_{LF} > 4$ deg can be therefore related to the total damping of the bridge model
235 (structural plus aerodynamic) due to the dependency of aerodynamic coefficients upon
236 the angle of attack.

237 To support this hypothesis we have to suppose that the aerodynamic coefficients
238 depends upon the LF angle of attack, and not only on the mean angle of attack. In the
239 following section, different numerical models are used to support this explanation and
240 to show the need to consider the effect of α_{LF} on the HF aeroelastic response.

241 4. Numerical results

242 Two different kind of numerical simulations were performed to study the experi-
243 mental behavior: a simple eigenvalue-eigenvector analysis, and a more complex Band-
244 Superposition analysis.

245 4.1. Eigenvalue-eigenvector analysis

246 Starting from the results of the experimental campaign, a numerical study was car-
247 ried out in order to investigate the effect of different mean angles of attack on the
248 aeroelastic coupling of the bridge, and specifically on its eigenvalues/eigenvectors. The
249 used algorithm is based on multi-modal equations and it solves the eigenvalue problem
250 at different wind speeds, considering smooth flow conditions (Argentini et al., 2014).
251 The three symmetric modes reported in Figure 6 were used in the simulations since they
252 are the most important modes for the symmetric flutter instability.

253 In Figure 9a), the computed numerical static rotation of the deck, as a function of
254 the mean wind speed is shown: we can note that at 5 m/s the mean static rotation of
255 the deck at midspan is $\theta_{ST} = 2$ deg.

256 In Figure 9b), the damping evolution of the 3-mode system (1V-5V-1T) is reported
257 as a function of the wind speed; we consider smooth flow conditions, so the angle of

258 attack α is the deck rotation θ_{ST} . We notice that the lowest damping at 5 m/s is the
259 one of “1T” mode, with damping ratio value of about 0.04.

260 Starting from this case, other simulations were run, changing the value of the angle
261 of attack by summing to θ_{ST} a value of +4, +2, -2, and -6 deg, and the obtained results
262 are shown in Figure 10, in comparison with the reference case $\alpha = \theta_{ST}$.

263 On the one hand, it is clearly visible that the “1T” mode, at 5 m/s of wind speed
264 and at angles of attack $\theta_{ST} - 6^\circ$ and $\theta_{ST} - 2^\circ$ has the same total damping ratio of the
265 reference case ($\approx 4.5\%$). On the other hand, the simulation with $\theta_{ST} + 2^\circ$ shows a smaller
266 damping ratio ($\approx 1\%$), while at $\theta_{ST} + 4^\circ$ total damping is negative, meaning bridge in
267 flutter instability. This means that the variation of the angle of attack, constant in these
268 simulations and variable at LF in wind tunnel tests, might shift the eigenvalues of the
269 system from a stable condition to a less stable, or even unstable, condition.

270 These simulations are not intended to compute the aeroelastic behavior for different
271 static angles of attack, but to study what can occur when a β_{LF} produces an instan-
272 taneous angle of attack α_{LF} , slowly ranging from -4 to +6 deg, as in the experimental
273 tests.

274 To be thorough, in Figure 11 the magnitudes and the phases of the eigenvectors at
275 5 m/s of the mode $1T$ are shown for all these simulations. From the comparison, the
276 eigenvectors are very similar, therefore we can confirm that the different total damping
277 is linked to the direct aerodynamic damping a_2^* and not to a different coupling of the
278 structural (no-wind) mode shapes.

279 *4.2. Band Superposition simulation*

280 The behavior of the bridge has also been simulated with a Band-Superposition model
281 applied to the full bridge, in order to take into account the β_{LF} .

282 The Band Superposition procedure, which is here briefly summarized, consists of
283 four main steps:

- 284 1. LF response computation
- 285 2. HF response computation

286 3. sum of LF and HF response

287 For the considered case, the LF vertical wind speed component is mono-harmonic at 0.1
288 Hz ($V^* = 303$) with amplitude $\beta = 2$ deg. The LF computation is simulated using a
289 nonlinear corrected quasi-steady theory (e.g. Diana et al. (1995)).

290 The HF band solution can be simulated with a rheological numerical model (Diana
291 et al., 2013a) or with a multi-band approach (Diana et al., 2005). Both approaches model
292 HF forces with parameters that are modulated by the instantaneous LF angle of attack,
293 α_{LF} : self-induced and buffeting forces are computed independently and their effect are
294 summed up exploiting the superposition hypothesis around the low-frequency angle of
295 attack. In the considered test (Case b)) the central part of the mid-span is forced by
296 a turbulent vertical wind component at 2.57 Hz ($V^* = 11.7$) with amplitude $\beta_{HF} = 1$
297 deg, plus a $\beta_{LF} = 2$ deg at 0.1 Hz ($V^* = 303$), while the lateral spans are forced by a
298 laminar wind.

299 For this forcing conditions, a multi-band model is used for the numerical simulations,
300 because only a single reduced velocity is present in the high-frequency range and there-
301 fore it is easily implemented, since flutter derivatives are considered at a fixed reduced
302 velocity and function only of α_{LF} . Figure 12 shows the comparison between the simulated
303 and the experimental time history of the torsional acceleration at the mid-span section,
304 and the time history of the experimental α_{LF} , β_{LF} , and θ_{LF} .

305 We can highlight that the main effects of the LF angle of attack is well reproduced:
306 in particular, there is an amplification of the response for positive α_{LF} , and a reduction
307 for negative α_{LF} ; the dependency on the α_{LF} in the response is caught, even if the
308 amplitude of the numerical response has some differences: in particular, it is larger for
309 negative α_{LF} , but the maximum vibration levels, for the equivalent acceleration at deck
310 edge, are in both cases about 3 m/s^2 .

311 To analyze the differences in the experimental-numerical comparison, we should re-
312 member that the variation of α_{LF} lead to a variation of total damping between negative
313 and positive values, according to results obtained at constant mean angle of attack.
314 However, in this test case, the system is always in transient-state conditions and the

315 aerodynamics could not be fully described by the flutter derivatives that represent a
316 steady-state condition around a specific mean angle of attack: further investigations on
317 this topic are suggested for future research studies.

318 5. Conclusions

319 The research presented in this paper provides a further contribution in the investi-
320 gation of non-linear effects in the aeroelastic response of long span bridges.

321 The artificial over-simplified wind scenario composed by single harmonics in the ver-
322 tical component of the incoming wind velocity acting with almost full spatial coherence
323 on the central part of the full bridge aeroelastic model is intentionally used to study the
324 interaction between LF and HF aeroelastic response of the structure. It is confirmed
325 that the HF response of the bridge is modified by the large fluctuations of the angle of
326 attack induced by the incoming wind.

327 A numerical analysis of the aeroelastic effects shows very well how the HF response is
328 not only due to the aerodynamic force dependency on V^* , but also on their dependency
329 on the instantaneous angle of attack.

330 Even for a very common deck section like the conventional single box deck section of
331 the bridge considered in this study, large variation of the flutter derivatives aerodynamic
332 coefficients occur, if different angle of attack are considered. Since the variation of the
333 instantaneous angle of attack can be produced by the LF turbulent fluctuations of the
334 incoming wind, aerodynamic forces can vary largely inducing non linear effects in the
335 structural response.

336 Band superposition approaches can reproduce the observed aeroelastic behavior of
337 the bridge under the condition to have the flutter derivatives coefficients for the required
338 V^* range and for a large set of angles of attack. This is not a usual condition since
339 flutter derivatives are commonly measured either only at a mean angle of attack equal
340 to zero or in a small range of angles of attack around zero. For the considered case the
341 maximum instantaneous angle of attack is for instance 6 deg.

342 Even if the wind spectrum was over-simplified, reproducing only two frequency com-
343 ponents at LF and HF, and even if the spatial correlation is extremely large along the
344 central span, the structural dynamics of the model and the aerodynamic coefficients of
345 the deck are representative of real long span bridge. Therefore the dynamic response of
346 the bridge is the result of both the aeroelastic coupling of structural modes and of the
347 buffeting excitation.

348 This test-case is another evidence that supports the need for a characterization of
349 the deck unsteady aerodynamics in a large range of angles of attack also at low reduced
350 velocities. Further research should be devoted to extend this methodology to a full-
351 spectrum incoming turbulent wind, to assess how low-frequency fluctuations of the wind
352 affect the aeroelastic response of structures in the atmospheric boundary layer.

353 6. Figures

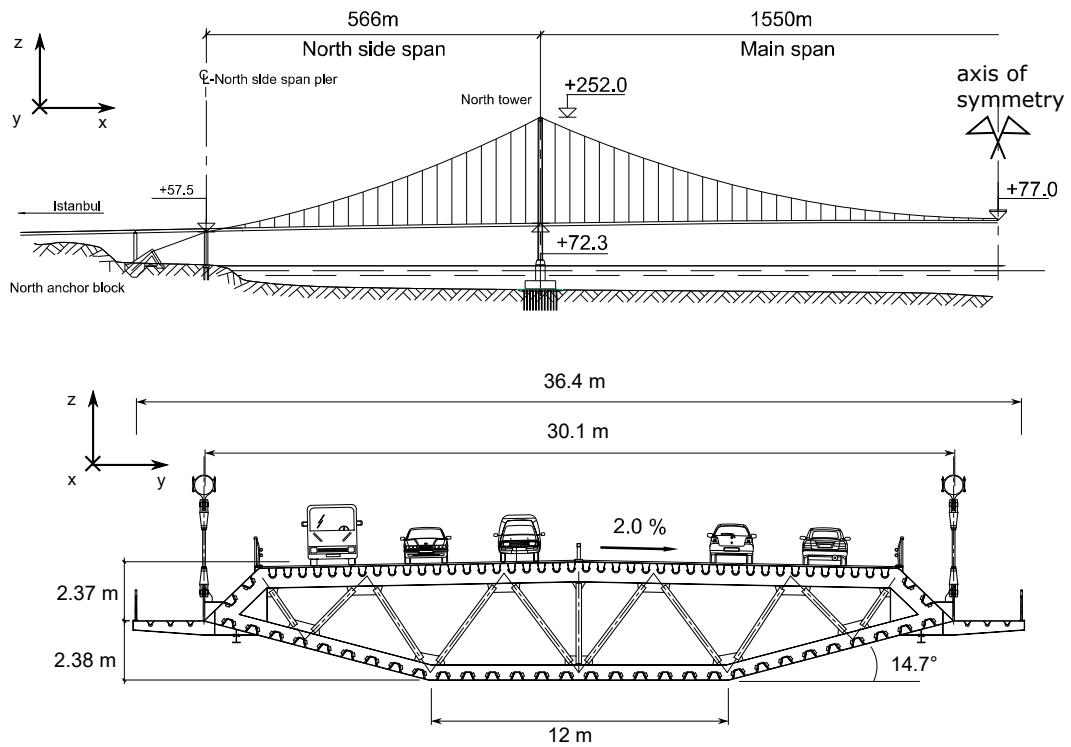


Figure 1: General arrangement of the bridge (the bridge is symmetric and only half of the complete bridge is shown) and typical deck cross-section

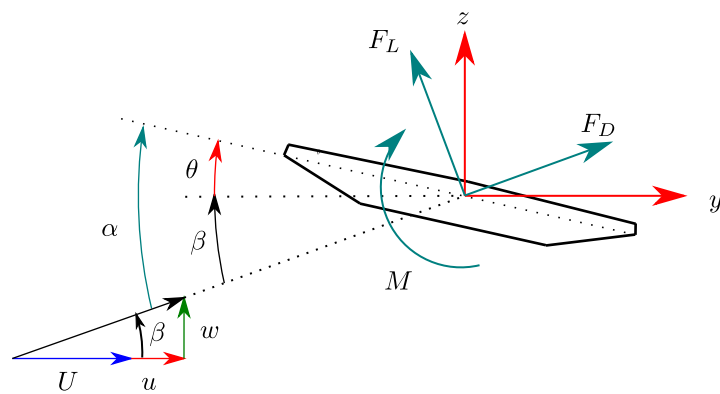


Figure 2: Sign conventions for forces (lift F_L , drag F_D , moment M), displacements (lateral y , vertical z , rotation θ), and wind velocity (turbulent vertical speed w and horizontal u)

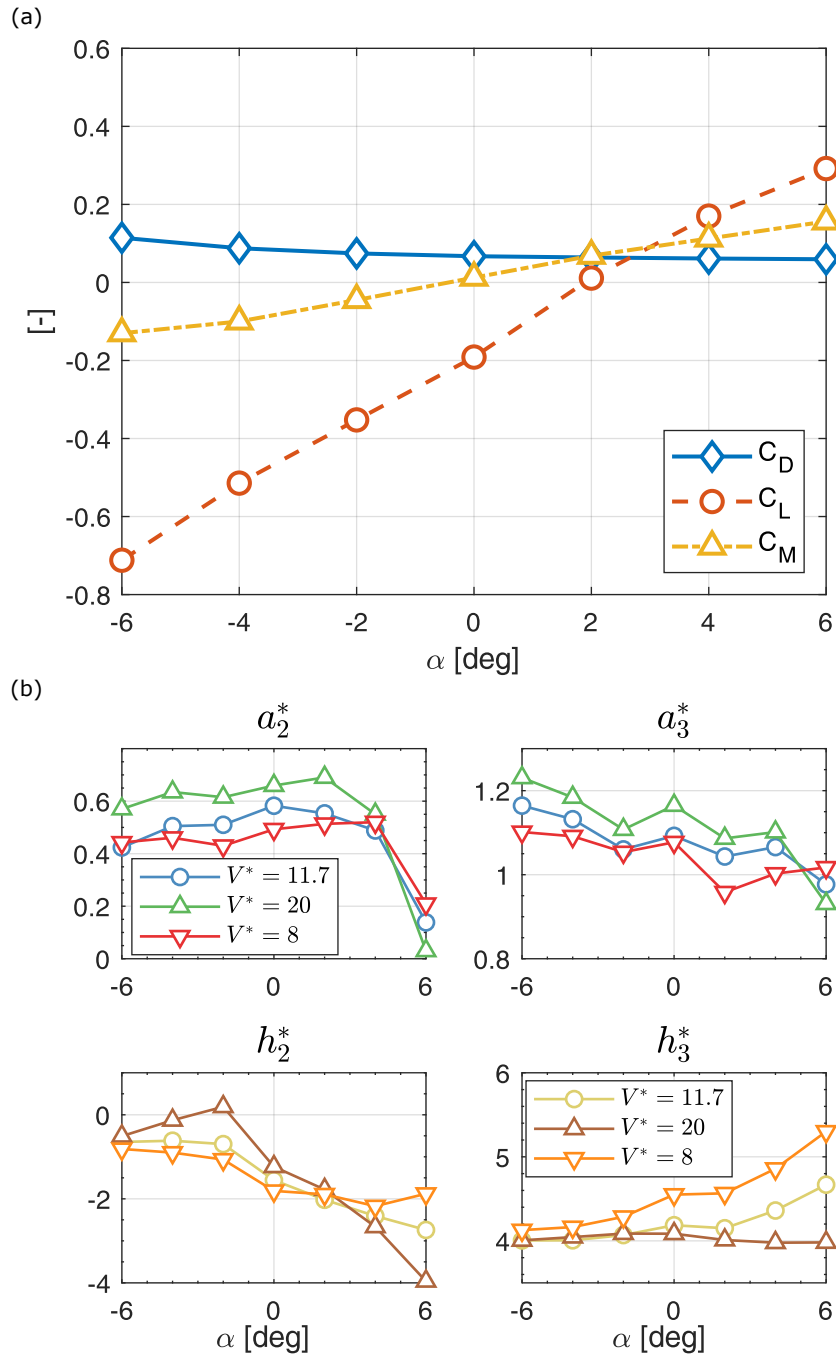


Figure 3: a) Stationary aerodynamic coefficients; b) torsional flutter derivatives a_2^* , a_3^* , h_2^* , h_3^* , as function of the angle of attack at $V^* = [8, 11.7, 20]$

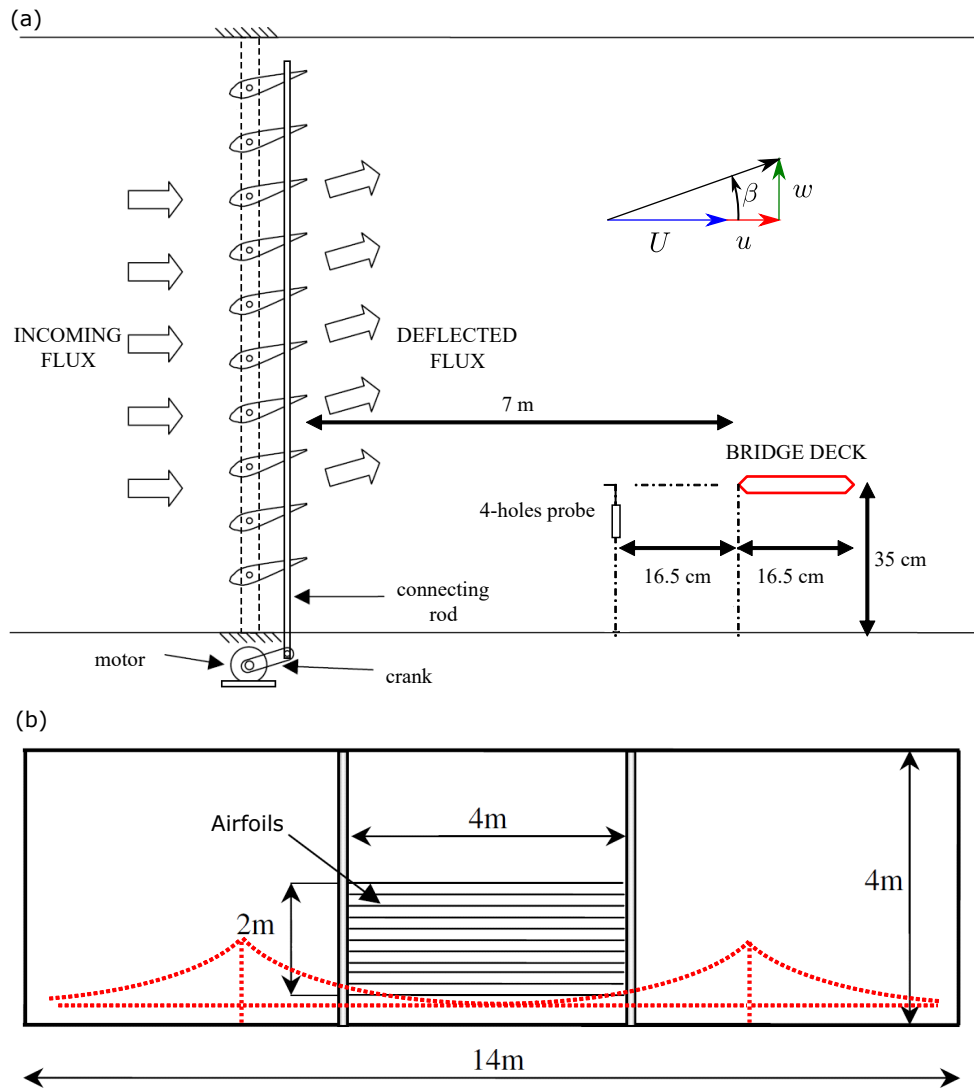


Figure 4: Active turbulence generator: a) side view; b) frontal view

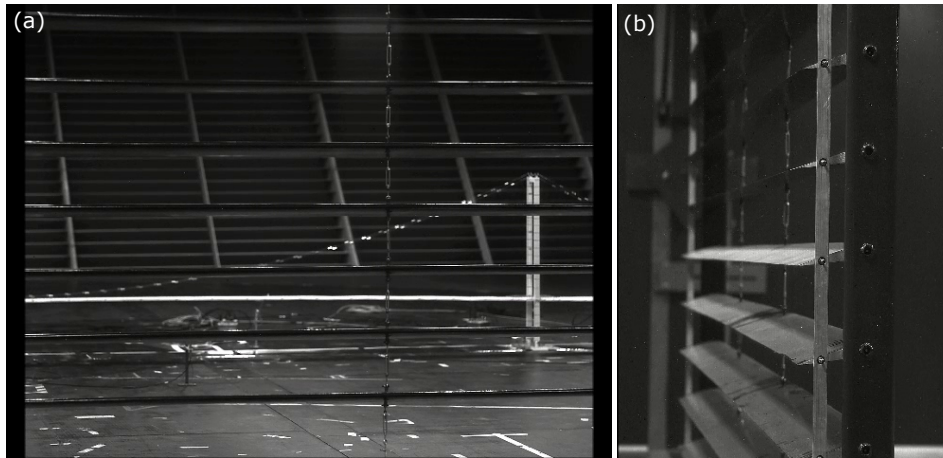


Figure 5: Pictures of the experimental setup. a) active turbulence generator with view of the bridge model in the background; b) details of the airfoils

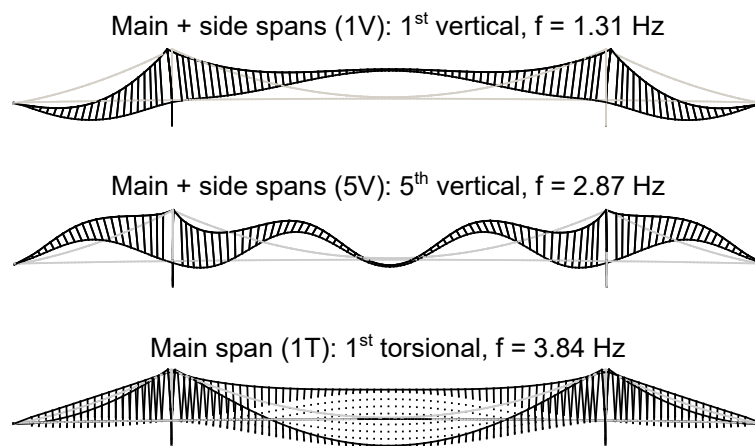


Figure 6: Modal shapes of the first and fifth vertical bending modes (1V and 5V) and of the first torsional mode (1T). f structural modal frequency

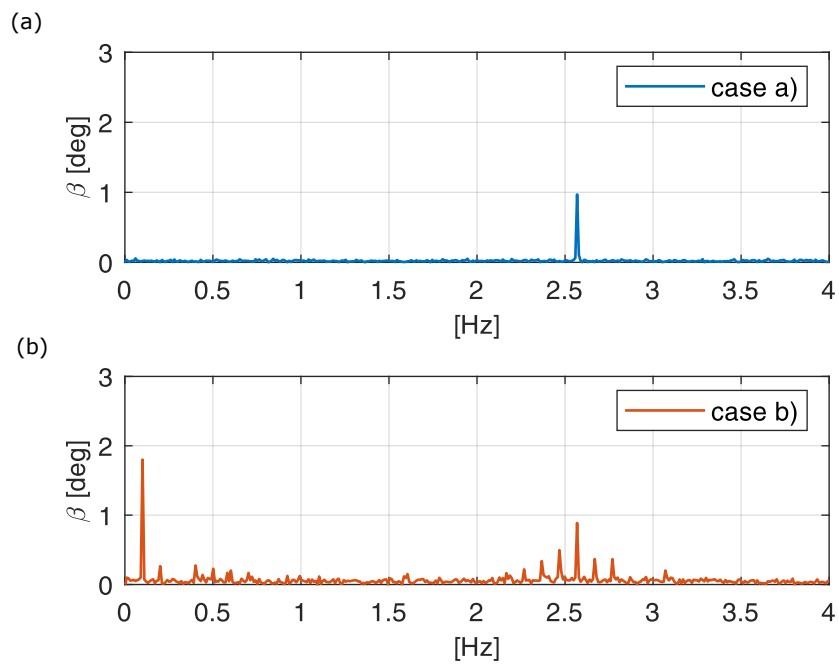


Figure 7: Incoming turbulent wind angle β for Case a) and Case b)

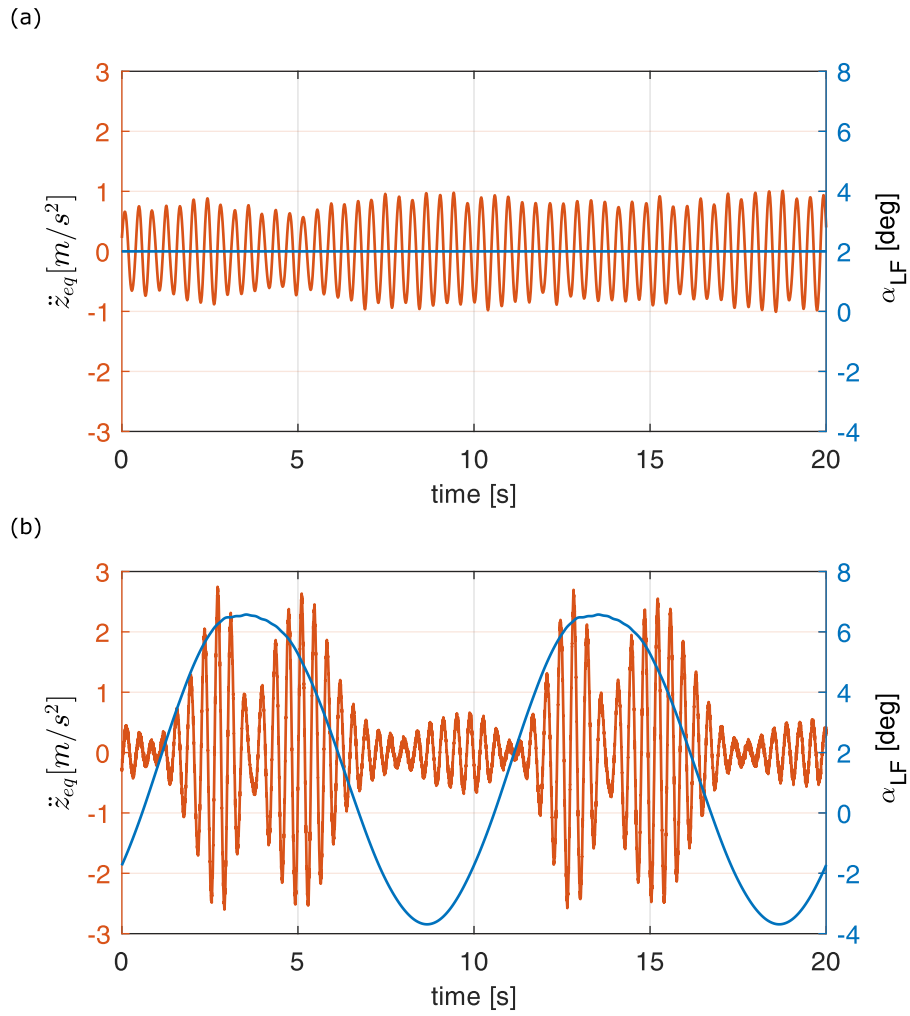


Figure 8: Comparison between torsional response in Case a) and in Case b): mid-span equivalent torsional accelerations at deck edge (red line, left y-axis) and LF angle of attack (blue line, right y-axis)

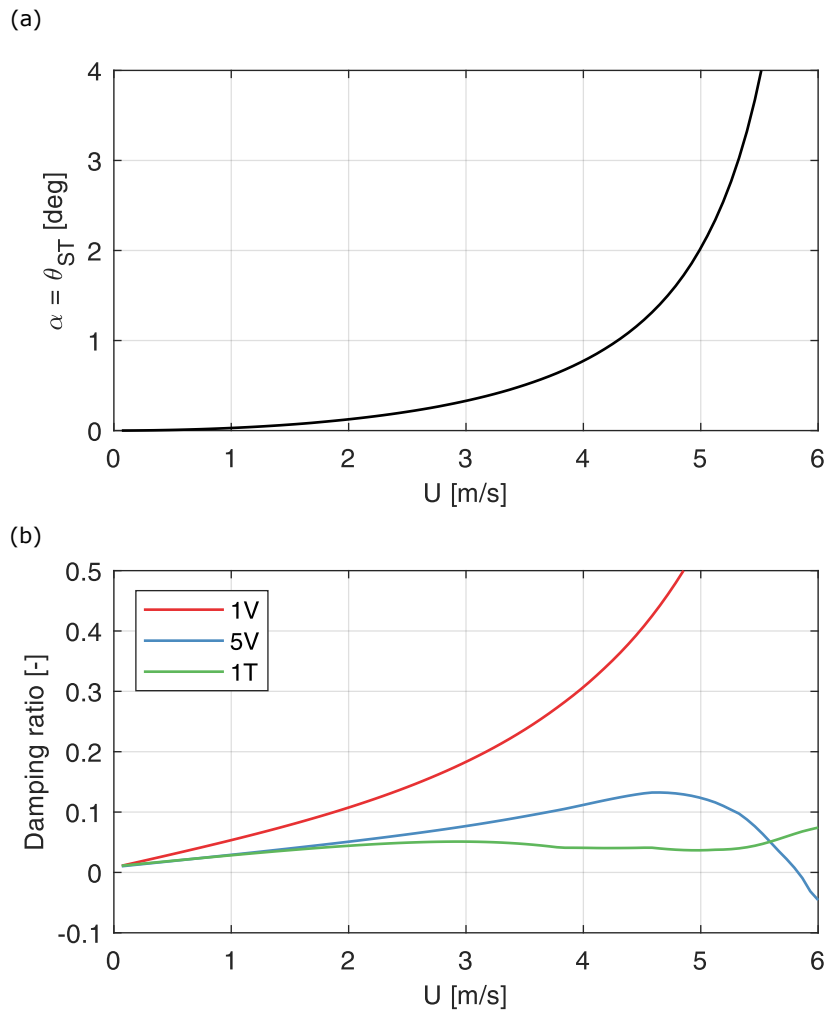


Figure 9: (a) static deck rotation at mid-span as a function of the incoming wind speed; (b) trend of the modal damping ratios as a function of the incoming wind speed for the considered modes (1V, 5V, 1T)

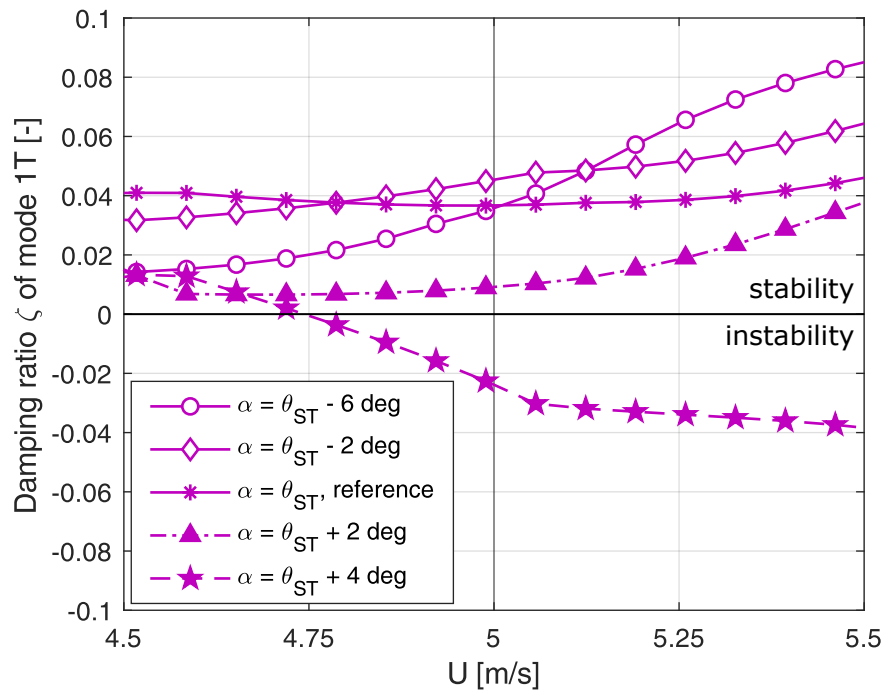


Figure 10: Trend of the modal damping ratio of mode “1T” as a function of the incoming wind speed, for different mean angles of attack α ranging from $\theta_{ST} - 6$ deg to $\theta_{ST} + 4$ deg

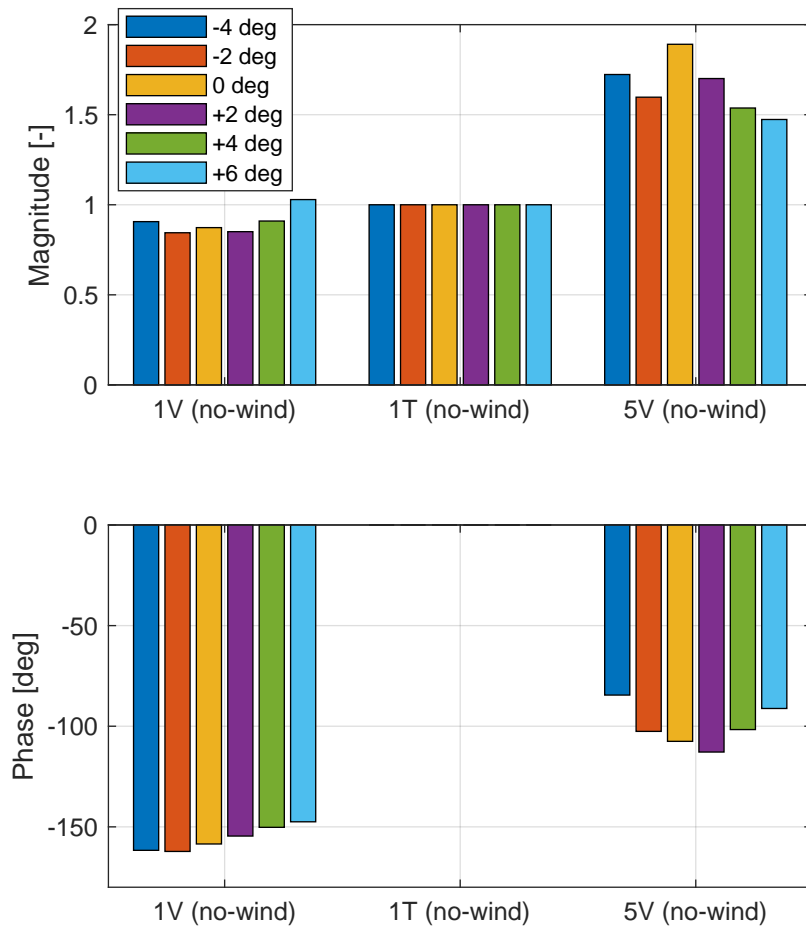


Figure 11: Magnitude and phase of the eigenvector of the mode "1T" at 5 m/s, for different mean angles of attack ranging from -4 to +6 deg

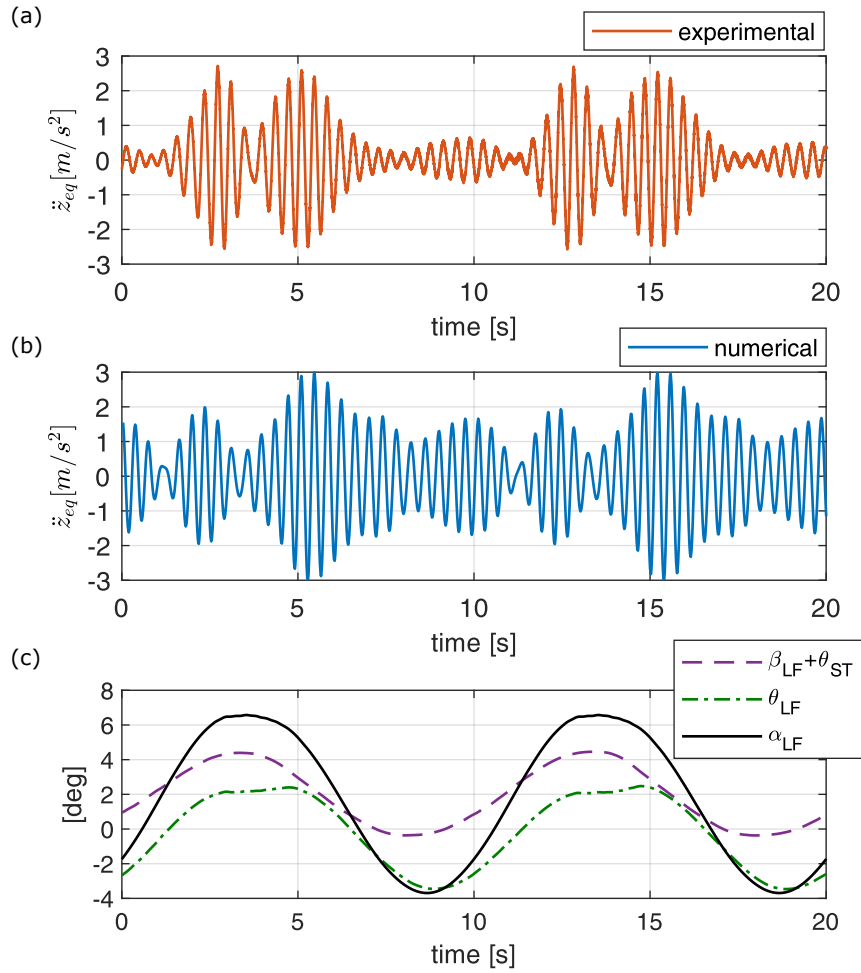


Figure 12: (a) and (b) Comparison of numerical and experimental torsional accelerations, in terms of equivalent torsional accelerations at deck edge \ddot{z}_{eq} . (c) Experimental LF angles: deck rotation θ_{LF} , wind angle β_{LF} , and total angle of attack $\alpha_{LF} = \theta_{LF} + \beta_{LF} + \theta_{ST}$.

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