Emanuele Mattiello

Passive-Dynamic Wind Tunnel Tests of the Øresund Bridge Cables

Master's Thesis, July 2012
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This report was prepared at Technical University of Denmark - Department of Civil Engineering.

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"There is no doubt that it is around the family and the home that all the greatest virtues, the most dominating virtues of human society, are created, strengthened and maintained."

- Winston Churchill -

"In every conceivable manner, the family is link to our past, bridge to our future."

- Alex Haley -

...to my family,
for the person I am
Preface

This report is the result of the master’s thesis activity conducted at DTU.Byg to obtain the Double Degree title Laurea Specialistica in Ingegneria Edile - recupero e conservazione and Master of Science in Civil Engineering - M.Sc. Civil at the Università degli Studi di Padova (UniPd) and Technical University of Denmark (DTU), respectively. The double degree title is the conclusion of the three years long European programme called T.I.M.E. - Top Industrial Managers for Europe.

During the project I have enjoyed the benefit of joining the Civil Engineering Structural Dynamics (CESDyn) Group under competent supervision of Professor Christos T. Georgakis (DTU), to whom I would like to express my sincere gratitude for his qualified direction, rewarding guidance and support, giving me the opportunity to get a glimpse of the researcher’s world.

I would also like to give a particular thanks to the Ph.D. students of the CESDyn Group, who have been extremely helpful in all regards.

Without the access to the unique testing facility established by DTU.Byg, Femern Belt and FORCE Technology, i.e. the Climatic Wind Tunnel situated at FORCE Technology, Kgs. Lyngby, Denmark, the realisation of the project would not have been possible. Through the experimental work, which has been carried out here, I have gained a deeper understanding of the flow and vibration phenomena related to bridge cables and 'hands on' experience with experimental scientific work.

The special course 'Introduction to Wind Tunnel Testing in Civil Engineering' followed before starting the project has served as an introduction to the experimental investigations performed in the wind tunnel and Professor Holger H. Koss (DTU) has been kind to answer my questions and provide me with valuable inputs.

The entire project was carried out together with the Danish student Mads Beedholm Eriksen. A series of stimulating debates and challenging comparison of views pushed us to a keen interest on the investigated topic and on structural aerodynamics more broadly.

Special thanks is also to Professor Carlo Pellegrino (UniPd) for his supervision on the project and to Professor Paolo Salandin (UniPd) for his active collaboration as my coordinator for the T.I.M.E. programme.

Finally I would like to express all my gratitude to family and friends for their patience, understanding and support throughout this project and the entire period abroad at DTU, Denmark.
Abstract

Large amplitude vibrations have been reported from the Øresund Bridge especially concomitant with rain events. A novel study of the aerodynamic damping of the filleted twin-cable has been carried out through passive-dynamic wind tunnel tests.

A specific inclined-yawed cable model configuration has been selected based on the critical cable-wind angles related to large amplitude vibration observations from full-scale monitoring. In order to estimate the aerodynamic damping a series of tests in dry and simulated rain conditions have been performed in the Climatic Wind Tunnel at FORCE Technology, Kgs. Lyngby, Denmark.

The results are compared with damping values determined from the application of quasi-steady analytical models using force coefficients from static wind tunnel tests. An additional comparison is performed with results available from full-scale monitoring. The results are in accordance in the subcritical Reynolds number region, while the correlation in the critical region is not completely validated, due to the limitations of the test set-up.

The gained experiences are finally presented for the use in future testing activities with the purpose of improving the performance of passive-dynamic tests.
Sommario

Vibrazioni di notevole ampiezza degli stralli dell’Øresund Bridge sono state spesso riportate, specialmente in presenza di pioggia. Un nuovo studio sullo smorzamento aerodinamico dei cavi gemelli con doppio filetto elicoidale è stato intrapreso attraverso una serie di test dinamici-passivi in galleria del vento.

Una specifica configurazione inclinata del modello dei cavi è stata selezionata sulla base degli angoli critici cavi-vento determinati dall’osservazione di ampie vibrazioni degli stralli monitorati. Una campagna di test in condizioni asciutte e con pioggia simulata per determinare lo smorzamento aerodinamico è stata condotta nella galleria del vento climatica, denominata CWT, presso l’istituto FORCE Technology, Kgs. Lyngby, Danimarca.

I risultati dei test sono stati confrontati con i valori di smorzamento ottenuti da modelli analitici basati sull’ipotesi di quasi stazionarietà. A tal scopo sono stati utilizzati coefficienti di forza disponibili da precedenti test statici in galleria del vento dello stesso modello testato dinamicamente. Un ulteriore confronto è stato fatto con i dati disponibili dall’attività di monitoraggio del ponte. Quanto ottenuto dalle comparazioni fatte dimostra un generale accordo nei valori di smorzamento nella regione subcritica del numero di Reynolds. Il confronto nella regione critica è invece non completamente convalidato data la minore affidabilità dei dati ottenuti dai test dinamici a causa dei limiti del set-up adottato.

L’esperienza acquisita durante lo svolgimento del progetto è infine presentata come supporto per future attività sperimentali, finalizzate al miglioramento di test dinamici-passivi in galleria del vento.
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# Nomenclature

## Acronyms

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<tr>
<th>Acronym</th>
<th>Description</th>
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<tbody>
<tr>
<td>1-DoF</td>
<td>Single-degree-of-freedom systems</td>
</tr>
<tr>
<td>2-DoF</td>
<td>Two-degree-of-freedom systems</td>
</tr>
<tr>
<td>3-DoF</td>
<td>Three-degree-of-freedom systems</td>
</tr>
<tr>
<td>BNC</td>
<td>Bayonet Neill-Concelman connector</td>
</tr>
<tr>
<td>CLP</td>
<td>Power Wind/Wave Tunnel Facility at Hong Kong University</td>
</tr>
<tr>
<td>CSTB</td>
<td>Centre Scientifique et Technique du Bâtiment</td>
</tr>
<tr>
<td>CWT</td>
<td>Climatic Wind Tunnel located at 'FORCE Technology'</td>
</tr>
<tr>
<td>DAQ</td>
<td>Data Acquisition unit</td>
</tr>
<tr>
<td>FFT</td>
<td>Fast Fourier Transform</td>
</tr>
<tr>
<td>FHWA</td>
<td>US Federal Highway Administration</td>
</tr>
<tr>
<td>HDPE</td>
<td>High-Density Polyethylene</td>
</tr>
<tr>
<td>HSS</td>
<td>Hollow Structural Sections</td>
</tr>
<tr>
<td>IFFT</td>
<td>Inverse Fast Fourier Transform</td>
</tr>
<tr>
<td>NaN</td>
<td>Not a number</td>
</tr>
<tr>
<td>NRC</td>
<td>National Research Council of Canada</td>
</tr>
<tr>
<td>OLS</td>
<td>Ordinary least-squares regression</td>
</tr>
<tr>
<td>OMA</td>
<td>Operational Modal Analysis</td>
</tr>
<tr>
<td>PTI</td>
<td>Post-Tensioning Institute</td>
</tr>
<tr>
<td>PVC</td>
<td>Polyvinyl chloride tubes</td>
</tr>
<tr>
<td>RPM</td>
<td>Revolutions per minute</td>
</tr>
<tr>
<td>RTO</td>
<td>Regression through the origin</td>
</tr>
</tbody>
</table>
Greek symbols

\( \alpha \) Angle of attack \([\,^\circ]\)
\( \alpha_{inst} \) Correction factor for Cobra Probe/Pitot tube readings \([-]\)
\( \alpha_{pos} \) Correction factor for model position/reference position readings \([-]\)

\( \beta \) Horizontal yaw angle \([\,^\circ]\)

\( \chi \) Maskell’s empirical blockage factor \([-]\)

\( \delta \) Logarithmic decrement \([-]\)

\( \epsilon_{rel} \) Relative error \([\%]\)

\( \lambda_{geom} \) Prototype to model scaling coefficient \([-]\)

\( \mu \) Dynamic air viscosity \([Pa \cdot s]\)

\( \nu \) Kinematic viscosity \([m^2/s]\)

\( \omega_0 \) Natural angular/circular frequency \([Hz]\)

\( \omega_d \) Damped angular/circular frequency \([Hz]\)

\( \phi \) Relative wind-cable angle \([\,^\circ]\)

\( \rho \) Air density \([kg/m^3]\)

\( \sigma \) Standard deviation \([-]\)

\( \theta \) Cable inclination \([\,^\circ]\)

\( \zeta \) Damping ratio \([\%]\)

\( \zeta_{eq} \) Equivalent viscous damping ratio \([\%]\)

\( \Delta_{mass} \) Differential mass acquisition via log transducer \([kg]\)

Roman symbols

\( \ddot{x}(t) \) Acceleration time history \([m/s^2]\)

\( \dot{x}(t) \) Velocity time history \([m/s]\)

\( acc_{lim} \) Acceleration limit \([m/s^2]\)

\( b_k \) Blockage coefficient \([\%]\)

\( c \) Viscous damping parameter \([Ns/m]\)

\( e \) Euler-Mascheroni constant \([Hz]\)

\( f(t) \) Mass normalized load time history \([N/kg]\)

\( f(t) \) Mass normalized load time history \([N/kg]\)
\( f_0 \) Natural frequency \([\text{Hz}]\)
\( f_1 \) Lower bandwidth frequency \([\text{Hz}]\)
\( f_2 \) Higher bandwidth frequency \([\text{Hz}]\)
\( f_i \) Frequency of considered mode \([\text{Hz}]\)
\( f_s \) Sampling frequency \([\text{Hz}]\)
\( f_v \) Vortex shedding frequency \([\text{Hz}]\)
\( f_{\text{max}} \) Largest frequency in the band of interest \([\text{Hz}]\)
\( f_{\text{Ny}} \) Nyquist frequency \([\text{Hz}]\)
\( f_{\text{peak}} \) Frequency of the peak amplitude in the discrete spectrum \([\text{Hz}]\)
\( f_{\text{res}} \) Maximum achievable frequency resolution \([\text{Hz}]\)
\( g \) Resonant peak factor \([-\text{]}\)
\( h \) Length of the model 'seen' by the wind \([m]\)
\( in \) Initial point of the time interval for damping calculation \([s]\)
\( j \) Final point of the time interval for damping calculation \([s]\)
\( k \) Stiffness coefficient \([\text{N/m}]\)
\( l \) Length of the body exposed to the wind \([m]\)
\( l_{\text{vel}} \) Cross-length of measured wind velocity profile \([m]\)
\( m \) Mass per unit length \([\text{kg/m}]\)
\( p_v \) Actual vapour pressure \([\text{mbar}]\)
\( p_{\text{atm}} \) Atmospheric pressure \([\text{mbar}]\)
\( p_{sb} \) Dynamic pressure correction due to solid blockage \([\text{Pa}]\)
\( p_{\text{tot}} \) Total dynamic pressure correction \([\text{Pa}]\)
\( p_{wb} \) Dynamic pressure correction due to wake blockage \([\text{Pa}]\)
\( u(t) \) Longitudinal turbulence component \([\text{m/s}]\)
\( v(t) \) Lateral turbulence component \([\text{m/s}]\)
\( w(t) \) Vertical turbulence component \([\text{m/s}]\)
\( w_{\text{CWT}} \) Width of CWT test section \([m]\)
\( x(t) \) Displacement time history \([m]\)
\( x_{\text{amp}} \) Amplitude of a signal \([m]\)
\( x_{\text{per}} \) Power of a signal \([\text{W}]\)
<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
<th>Units</th>
</tr>
</thead>
<tbody>
<tr>
<td>B1</td>
<td>Rotational matrix no. 1</td>
<td>[-]</td>
</tr>
<tr>
<td>B2</td>
<td>Rotational matrix no. 2</td>
<td>[-]</td>
</tr>
<tr>
<td>B3</td>
<td>Rotational matrix no. 3</td>
<td>[-]</td>
</tr>
<tr>
<td>C(_{F\phi})</td>
<td>Force coefficient derivative matrix in cable-wind plane orientation</td>
<td>[-]</td>
</tr>
<tr>
<td>C</td>
<td>Damping matrix</td>
<td>[-]</td>
</tr>
<tr>
<td>C(_{F\phi})</td>
<td>Force coefficient matrix in cable-wind plane orientation</td>
<td>[-]</td>
</tr>
<tr>
<td>K</td>
<td>Stiffness matrix</td>
<td>[kg/m]</td>
</tr>
<tr>
<td>M</td>
<td>Mass per unit length matrix</td>
<td>[kg/m]</td>
</tr>
<tr>
<td>A(_{cross})</td>
<td>Model cross sectional area</td>
<td>[m(^2)]</td>
</tr>
<tr>
<td>A(_{CWT})</td>
<td>Cross section area of the testing chamber of CWT</td>
<td>[m(^2)]</td>
</tr>
<tr>
<td>A(_{model})</td>
<td>Cable model frontal area in the wind direction</td>
<td>[m(^2)]</td>
</tr>
<tr>
<td>A(_{ref})</td>
<td>Reference area of the model for force coefficients calculation</td>
<td>[m(^2)]</td>
</tr>
<tr>
<td>AS</td>
<td>Aspect ratio</td>
<td>[-]</td>
</tr>
<tr>
<td>C(_D)</td>
<td>Drag force coefficient</td>
<td>[-]</td>
</tr>
<tr>
<td>C(_L)</td>
<td>Lift force coefficient</td>
<td>[-]</td>
</tr>
<tr>
<td>C(_{D-Maskell})</td>
<td>Corrected Maskell drag coefficient</td>
<td>[-]</td>
</tr>
<tr>
<td>C(_{D-swbc})</td>
<td>Corrected drag coefficient for solid and wake blockage</td>
<td>[-]</td>
</tr>
<tr>
<td>C(_{D-u})</td>
<td>Uncorrected drag coefficient</td>
<td>[-]</td>
</tr>
<tr>
<td>C(_{D-wbc})</td>
<td>Corrected drag coefficient for wake blockage</td>
<td>[-]</td>
</tr>
<tr>
<td>D</td>
<td>Cable diameter</td>
<td>[m]</td>
</tr>
<tr>
<td>E(_a)</td>
<td>Vapour pressure</td>
<td>[mbar]</td>
</tr>
<tr>
<td>F(_t)</td>
<td>Load time history</td>
<td>[N]</td>
</tr>
<tr>
<td>F(_D)</td>
<td>Drag force</td>
<td>[N]</td>
</tr>
<tr>
<td>F(_L)</td>
<td>Lift force</td>
<td>[N]</td>
</tr>
<tr>
<td>F(_y)</td>
<td>Damping force generated by body motion in wind action</td>
<td>[N]</td>
</tr>
<tr>
<td>F(_r)</td>
<td>'Froude' number</td>
<td>[-]</td>
</tr>
<tr>
<td>G</td>
<td>Gust factor</td>
<td>[-]</td>
</tr>
<tr>
<td>H(_L)</td>
<td>Horizontal displacement acquisition with left-side laser</td>
<td>[mm]</td>
</tr>
<tr>
<td>H(_R)</td>
<td>Horizontal displacement acquisition with right-side laser</td>
<td>[mm]</td>
</tr>
<tr>
<td>I(_u)</td>
<td>Turbulence intensity in the flow direction</td>
<td>[%]</td>
</tr>
</tbody>
</table>
\( M \) Total mass \([\text{kg}]\)
\( N \) Total number of sampled data \([-]\)
\( P_{14} \) Dynamic relative pressure of test section \([\text{Pa}]\)
\( P_{15} \) Static relative pressure of test section \([\text{Pa}]\)
\( R_a \) Surface roughness indicator \([\text{nm}]\)
\( Re \) Reynolds number \([-]\)
\( RH \) Relative humidity inside the test section \([\%]\)
\( S \) Centre-to-centre spacing \([\text{m}]\)
\( Sc \) Scruton number \([-]\)
\( St \) Strouhal number \([-]\)
\( T \) Air temperature inside the test section \([\text{oC}]\)
\( T_0 \) Natural period \([\text{s}]\)
\( T_{tot} \) Total logging time \([\text{s}]\)
\( U \) Mean wind speed in the horizontal direction \([\text{m/s}]\)
\( U(t) \) Total velocity component in the direction of the mean wind \([\text{m/s}]\)
\( U_R \) Relative wind velocity \([\text{m/s}]\)
\( U_r \) Reduced velocity \([-]\)
\( U_{crit} \) Critical velocity at which instability occurs \([\text{m/s}]\)
\( V(t) \) Total velocity component in the lateral horizontal direction \([\text{m/s}]\)
\( V_L \) Vertical displacement acquisition with left-side laser \([\text{mm}]\)
\( V_R \) Vertical displacement acquisition with right-side laser \([\text{mm}]\)
\( W(t) \) Total velocity component in the vertical direction \([\text{m/s}]\)
\( Z \) Non-dimensional damping parameter \([-]\)

**Subscripts**

- \( a \) Refers to the aerodynamic contribution
- \( s \) Refers to the structural contribution
- \( v \) Refers to the direction of cylinder velocity
- \( w \) Refers to water
- \( in-plane \) Refers to in-plane direction
- \( out-of-plane \) Refers to out-of-plane direction
Refers to drag
Refers to lift
Refers to relative wind direction

Mathematical symbols

\(+\) Convolution
\(\mathcal{F}\) Fourier transform
\(\mathcal{H}\) Hilbert transform
\(\langle\ldots\rangle\) Mean value of ...
\(\pm\) Plus or minus
\(\propto\) Proportional to
\(\Re\) Real part
\(|\ldots|\) Absolute value of ...
\(f(\ldots)\) Function of ...
Chapter 1

Introduction

The Øresund Bridge cables have suffered from large amplitude vibrations since the last part of the construction phase and intensive work and retrofitting of additional dampers have been necessary to suppress the vibrations to an acceptable limit.

In this introductory section a description of the Øresund Bridge and the vibration problems is given, followed by the objective and outline of this master’s thesis.

1.1 The Øresund Bridge

The Øresund Bridge (Øresundsbro Konsortiet, 2012) is a part of the Øresund Link which connects Copenhagen, Denmark, and Malmö, Sweden. With a total length of 7845 m it is the longest road and rail bridge in Europe. The high bridge is a cable-stayed bridge with a main span of 490 m, a navigational clearance height of 55 m and the pylons reach 204 m above mean sea level. The high bridge is shown in Figure 1.1.

The cable system, provided by ‘Freyssinet Group’, consists of 160 stay cables in total (80 stay cable pairs) with a maximum length of 262 m. Each cable is made as a bundle of 68-73 strands. The cables are arranged in two parallel harp configurations with 30° inclination angle from the horizontal and 20 m between the anchorages. The cables span from the pylon to the bridge deck and they are aligned with the bracing of the deck truss structure. The stay cables on the Øresund Bridge are arranged in a twin-cable configuration, where the stays are connected two by two and are sharing supports. Connectors are added between the cables for every 100 m to improve stability.

The cables are covered by black HDPE tubes, which have a diameter of 250 mm each. A 2.1 mm high double helical fillet, which is intended to work as an aerodynamic countermeasure mainly to prevent rain-wind induced vibrations (RWIV), is applied to the tubes. The helical fillets are seen in Figure 1.2. According to Larose and Smitt (1999) it was already decided at an early stage in the detailed design of the superstructure to use the double helical fillet, similar to the fillet used on the Pont de Normandie in France with cables diameter of 160 mm.
1.1. The Øresund Bridge

Figure 1.1: High bridge and western approach bridge with Amager in the background, photo by Christian Ringbæk

The cable diameter is relatively large compared to other cable-stayed bridges. That is explained by the high stiffness requirement needed to support the railroad. A series of wind tunnel tests was carried out by the 'Danish Maritime Institute' (today 'FORCETechnology') to verify the effectiveness of this countermeasure for the 250mm diameter twin-cable. The distance between the stays in the twin configuration was designed to be $0.670\,m$, cf. (de Sá Caetano, 2007, p. 129).

1.1.1 Large Amplitude Vibrations on the Øresund Bridge

Already before the bridge was opened in 2000 large amplitude vibrations were reported. It was decided to install hydraulic dampers at the lower anchorages of the longest cables and no large stay cable vibrations were reported until December 2001, when the permanent monitoring system detected a large amplitude incident. More incidents were detected in the early spring 2002, cf. (de Sá Caetano, 2007). After visual inspection of the dampers, showing severe damages, the dampers were repaired in summer 2002, cf. (Øresundsbro Konsortiet, 2002). The damper system was found to be inadequate for the bridge, so a new type of dampers were tested in winter 2003-2004, cf. (Øresundsbro Konsortiet, 2003), while several episodes of large cable vibrations occurred.

Due to snow accumulation on the cables and wind speeds close to $14\,m/s$ vibration amplitudes up to $3m$ for a cable with a damper of the former type were reported on February 24, 2004, while a cable with the new type of damper reached a maximum vibrations amplitude of about $0.5m$, cf. (de Sá Caetano, 2007). Comprehensive repair work in autumn 2004 resulted in the installation of a new damper type developed by the cable contractor 'Freyssinet Group' in consultation with Øresunsbron and Sundlink, cf. (Øresundsbro Konsortiet, 2004).
### Introduction

#### 1.1. The Øresund Bridge

At the anchorages viscous radial dampers were installed on the longest and second longest cable pairs and, additionally equipped by tuned mass dampers, cf. (Lesjöfors AB, 2012b). Illustrations of the dampers can be seen in Figure 1.2

![Viscous radial dampers at anchorage](image1.png) ![Tuned mass dampers at cable midspan](image2.png)

**Figure 1.2:** Dampers on the Øresund Bridge twin stay cables, cf. (Gimsing and Georgakis, 2012)

Figure 1.3 shows the pylons and the cables head on from the roadway. Note that the photo was taken before the retrofit was carried out in 2003, where additional tuned mass dampers were installed on the longest cables.

According to the analysis of vibration occurrences by de Sá Caetano (2007): "Correlation between weather conditions and large amplitude vibrations indicated that the major source of vibrations was related to rain/wind action or galloping caused by aggregation of snow or ice in the stays."

Rain-wind induced vibrations are accounting for the majority of the reported and measured cases of cable vibrations, cf. e.g. (de Sá Caetano, 2007; Gimsing and Georgakis, 2012), and several field observations cf. e.g. (Hikami and Shiraishi, 1988; Main and Jones, 1999; Ni et al., 2007; Zuo and Jones, 2010) and wind tunnel tests cf. e.g. (Cosentino et al., 2003; Flamand, 1994; Larose and Smitt, 1999; Matsumoto et al., 2003; Zhan et al., 2008) have been carried out.

The observations show that the vibrations generally occur at specific conditions. Moderate rain intensity, specific angle of attack and limited wind velocities are normal conditions when RWIV are reported, but vibrations have also been reported at intensive rainfall, cf. (de Sá Caetano, 2007; Main and Jones, 1999).

The mechanism is described further in section 2.2 among other instability mechanisms.

#### 1.1.2 Research and Investigations

A collaborative research project between 'DTU.Byg', 'Femern Bælt', 'Sund & Bælt' and 'FORCE Technology' has been funded by 'Femern Bælt A/S' and 'Storebælt A/S', cf. (Koss, 2009), with the aim to investigate how weather conditions affect bridge cables and provide design guidelines for the mitigation of wind induced vibrations of cables on long-span cable-supported bridges.

A new wind tunnel specially designed for testing bridge cables in different weather conditions has been established in 2008. Its design specifications are reported in
1.2 Thesis Objective

The aim of the project was to analyse the monitored configurations and reproduce the conditions for the Øresund Bridge cables, which may lead to instability via tests of a twin-cable model at reduced scale in the CWT. This was done by performing a series of passive-dynamic tests in order to determine the aerodynamic damping at different flow and meteorological conditions. The latter were divided into dry and wet conditions to be able to evaluate the effects of the presence of water rivulets on the twin-cable aerodynamics. The results in terms of aerodynamic damping ratio were compared with results from full-scale monitoring of the Øresund Bridge, cf. (Acampora, 2011) and with the application of theoretical quasi-steady models, cf. (Macdonald and Larose, 2006, 2008a) using aerodynamic force coefficients from static tests.

1.3 Approach

In order to reach the goal of the project a structured approach has been developed. The followed procedure consisting in five steps is outlined below:

- Research and documentation
- Monitoring data analysis
1.3. Approach

- Experimental campaign
- Analytical work
- Comparison

Hereafter every point is presented and briefly described. The complete description for each task is then elaborated in the subsequent chapters.

1.3.1 Research and Documentation

The very first task in every research project is the documentation on literature about the topic, its state of the art and on-going developments. Firstly it is vital to go to the basis of the problems. Secondly it is possible to narrow down the attention onto the specificity of the project. Therefore a substantial documentation activity was performed soon at the beginning of the project. Wind engineering and dynamics of structures were considered as starting point. Then the focus was moved into the fluid-dynamic sphere with attention on flow around circular cylinder and twin-cylinder configurations. After that, various vibration mechanisms were reviewed with particular emphasis on rain-wind induced vibrations, which are likely the most evident and dominant vibration phenomenon occurring on cable-stayed bridges. Furthermore measures to control the vibrations were considered. This was followed by the study of the aerodynamic damping, its significance and of methods to estimate it. The described procedure is reported in chapter 2.

1.3.2 Monitoring Data Analysis

As the project title says, the focus of the thesis is on cables of a real structure, the Øresund Bridge. A monitoring system was established in 2009, cf. (Acampora, 2011). It was decided to use data from this full-scale monitoring activity to select few specific geometrical configurations of the actual stays to be reproduced at reduced scale in the wind tunnel. In particular, cables declining in the wind, with large registered amplitudes over a long monitored period, and for various meteorological conditions were considered in the analysis. Cable characteristics in term of natural frequency of vibrations and damping were used as reference to design the scaled model and compare results. The monitoring system and documentation of the study of full-scale measurements are presented in chapter 3.

1.3.3 Passive-Dynamic Tests

Parallel to the previously mentioned tasks, a study about the experimental methods for wind tunnel tests was conducted. Firstly practical experience in the CWT at 'FORCE Technology' was conducted to get familiar with the testing facility. Secondly testing procedures were reviewed to determine the most suitable one for the purpose of the project. Wind tunnel tests are generally conducted with a static or a dynamic approach. The experimental campaign performed in this thesis project follows the passive-dynamic procedure, as described in Georgakis et al. (2009). Details about the
1.3. Approach

testing facility, the experimental method and performed activities are presented in chapter 4.

1.3.4 Analytical Quasi-Steady Approach

The experimental campaign was accompanied by a theoretical work based on the study and consequent application of analytical models for the prediction of cable instabilities. All considered models use the quasi-steady approach for which quasi-steady theory is assumed applicable. Models, cf. (Macdonald and Larose, 2006, 2008a), were input with force coefficients from static tests, to output aerodynamic damping estimations. Description of the various theoretical models and their application is depicted in chapter 5.

1.3.5 Comparison

The last part of the project consisted of a comparison between results of aerodynamic damping from the three different sources, already presented in the previous parts. Those were full-scale monitoring, experimental work in the wind tunnel and analytical models. An appraisal between results differences, limits and/or constrains in the use of these three methods was also included. The task is presented in chapter 6.
Cable Aerodynamics

The present chapter illustrates the research and documentation activity performed as the first step for the master’s project, as introduced in section 1.3.1. First the flow around and vibration phenomena for a twin-cable configuration are described. Then some solutions to limit vibrations are illustrated. Finally damping for a single-degree-of-freedom system, aerodynamic damping and techniques for its estimation are presented.

2.1 Flow Around Two Cylinders

The flow around the Øresund Bridge cables is not only governed by the theory of flow around circular cylinders. The special twin-cable configuration imply that the presence of two cylinders whose interaction may affect the flow, must be considered.

In this section the flow around two cylinders is described. The cylinders, which are considered as parallel, plain, circular and identical, are positioned in steady cross-flow, where the flow approaches the cylinders perpendicularly.

Note that the actual cables are not perfectly circular, but may be slightly oval due to manufacturing, bridge erection operations and ageing. Besides that, scratches and extruded labelling modifying the surface roughness or acting as separation triggers and not least the presence of the helical fillets, change the aerodynamic properties of the cable. Furthermore, the flow does not approach the cables perpendicularly, but different cable-wind angles and axial flow should be considered for a full description.

Finally the steady flow is a theoretical assumption which may not be sufficient for a complete description of the phenomena of a vibrating body.

The flow is often described by the dimensionless quantity Reynolds number, which expresses the ratio of inertial forces to viscous forces. For small values of $Re$ the viscous forces are dominating and for larger values the inertial forces become dominating. Reynolds number is defined as:

$$Re = \frac{UD}{\nu}$$  \hspace{1cm} (2.1)
where $D$ is a characteristic dimension of the considered body e.g. the diameter of a cable, $U$ is the flow velocity and $\nu$ is the kinematic viscosity of the fluid, which for air at standard atmospheric pressure and $20^\circ C$ is $1.51 \times 10^{-5} \, \text{m}^2/\text{s}$. Reynolds number is often used as similarity parameter for experimental cases, which allows a comparison of flow characteristics and fluid dynamic behaviours of bodies at different conditions and to characterize various flow regimes.

The flow behaviour of a twin configuration depends on how the cylinders are positioned relative to each other. Zdravkovich (2003) distinguishes between three simple arrangements of the two cylinders: side-by-side, tandem and staggered, which are sketched in Figure 2.1.

![Figure 2.1: (a) side-by-side, (b) tandem, (c) and staggered, twin-cylinder arrangements](image)

For two cylinders placed relatively far from each other the flow around and the forces on the cylinders are the same as for the single cylinder. Synchronization of the vortex shedding may occur, though, cf. (Sumner, 2010). The flow around two cylinders differs from the behaviour of the single cylinder, when the two cylinders come closer. The different interference regions may be identified in:

- Wake interference; the flow around the downstream cylinder is affected by the wake of the upstream cylinder.
- Proximity interference; the cylinders are close to each other whereby the flow around one cylinder affects the other.
- Combined proximity and wake interference; this represents an overlap of the proximity and the wake interference.

Mainly the side-by-side arrangement was considered in the present project due to the actual bridge cable configuration, which means that the proximity interference region is dominating. The remaining regions are e.g. considered when the angle of attack-dependency is considered in the quasi-static model used to determine the aerodynamic damping, cf. chapter 5. Therefore three interference flow regimes are presented for this arrangement depending on the ratio between the transverse centre-to-centre spacing, $S$, and the cylinder diameter, $D$.

- For $1<S/D<1.1$ a single vortex street is formed behind both cylinders, which work as a single bluff body. The small gap between the cylinders only allows a weak flow, which suppresses vortex shedding at the gap in the same manner as the ‘near-wall effect’, cf. (Sumner, 2010, p. 869). Zdravkovich (2003) calls this single bluff body behaviour the ‘single vortex street regime’. 
• For 1.1-1.2 < S/D < 2-2.2 two wakes are formed behind the cylinders - a narrow and a wide wake. The flow through the gap is directed (biased) towards the narrow wake. The flow is bi-stable and may switch to either side. This is the so called 'biased flow regime'.

• For 2-2.2 < S/D < 4-5 the two wakes are equal in size and the vortex shedding is synchronized. The out-of-phase coupling dominates and produces two mirrored vortex streets in the 'coupled wakes regime'.

The interference regimes for the side-by-side arrangement are sketched in Figure 2.2.

![Figure 2.2: Flow patterns for side-by-side arrangements: (a) Single vortex street regime with single bluff body behaviour, (b) biased flow regime, (c) the coupled wakes regime, where parallel vortex streets are dominating](image)

The centre-to-centre spacing of the Øresund Bridge cables is 670 mm, as mentioned in the introduction, cf. 1.1, which gives a spacing ratio of S/D = 2.68. In a cross-flow test with the cable configuration of the Øresund Bridge the flow pattern falls within the coupled wakes regime. Hence the gap between the cylinders is sufficiently large for a parallel vortex street to form. Both cylinders undergo vortex shedding at the same frequency, either out-of-phase or in-phase. The behaviour of the cylinders is closer to the single cylinder, but the proximity interference effects may lead to various modes of synchronization, anti-phase and in-phase, in the vortex formation and shedding processes. The two vortex streets lead to complex vortex street interaction in the combined wake of the cylinders.

A flow visualization example from a cross-flow test of a twin cylinder model is shown in Figure 2.3.

### 2.2 Wind Induced Cable Vibration Mechanisms

Wind induced cable vibrations can occur due to buffeting or vortex-shedding, but in most cases the larger amplitude vibrations are caused by dry galloping or galloping in the presence of rain, sleet, snow or ice. The different types of wind induced cable vibrations are related to changes in aerodynamic forces. This section contains a description of different vibration mechanisms, which are induced by the wind. At last, the section will focus on rain-wind induced vibrations, which most likely account for 95% of the reported and measured case events on actual bridges, cf. (Gimsing and Georgakis, 2012, p. 545) and (de Sá Caetano, 2007, p. 39).
2.2. Wind Induced Cable Vibration Mechanisms

Other vibration mechanisms such as cable-structure interaction or other external loadings may also be relevant regarding cable vibrations, but are not within the scope of the project.

2.2.1 Buffeting

Wind gusts due to the turbulence in the wind cause buffeting of cables. The wind speed is generally characterised by three time dependent velocity components, e.g. cf. (de Sá Caetano, 2007; Holmes, 2001):

\[
\begin{align*}
U(t) &= U + u(t) \\
V(t) &= v(t) \\
W(t) &= w(t)
\end{align*}
\]  

(2.2)

where \( U \) is the mean wind velocity, and \( u(t) \), \( v(t) \) and \( w(t) \) are the fluctuating components in the Cartesian orthogonal directions.

An analysis of wind buffeting was firstly developed by Davenport (1961, 1962, 1963). The principles of buffeting analysis are basically unchanged, cf. (Macdonald, 2003). The amplitudes caused by buffeting are generally small compared to other vibration phenomena, but may be sufficiently large to cause structural fatigue damages.

2.2.2 Vortex Induced Vibrations

Vortex induced vibrations are caused by regular von Kármán vortex shedding on alternating sides of a cable, as illustrated in Figure 2.4, which induce an alternating load perpendicular to the mean flow direction. This results in an oscillating lift force.

Vibrations due to vortex shedding are normally characterised by small amplitudes, but they may become large, when the vortex shedding frequency is close or equal to the structural eigenfrequency for one of the lower modes of the cable - commonly called \textit{lock-in} or \textit{synchronisation}. This resonant excitation can cause large displacements transverse to the wind direction.
2.2. Wind Induced Cable Vibration Mechanisms

For a cylinder with diameter $D$ immersed in a steady flow with flow velocity $U$, the vortex shedding frequency is often described by a dimensionless parameter, the ‘Strouhal’ number defined as:

$$St = \frac{f_v \cdot D}{U} \quad (2.3)$$

In the subcritical regime it is practically constant about a value of $St \approx 0.2$ for circular cylinders, cf. (Sumer and Fredsøe, 2006, p. 10).

Knowing the eigenfrequencies of the cable makes it possible to predict the wind speed, at which vortex shedding causes a resonant excitation, and thereby ensure that the lock-in region does not coincide with the design wind speeds.

2.2.3 Vortex Induced Vibrations at High Wind Velocity

Vortex Induced vibrations of cables at high reduced wind velocity is a phenomena, which has been introduced by Matsumoto et al. (2001). It is a complex, three-dimensional phenomenon caused by the presence of an axial flow along the cable and enhanced by the presence of rain, cf. (de Sá Caetano, 2007).

The reduced velocity is defined as:

$$U_r = \frac{U_{\text{crit}}}{f_0 \cdot D} \quad (2.4)$$

where $U_{\text{crit}}$ is the critical velocity at which instability, related to a specific mechanism, occurs.

The phenomenon has been observed and studied by many e.g Main and Jones (1999), Matsumoto et al. (2001), Cheng et al. (2003) and Cheng et al. (2008a).

The two-dimensional von Kármán vortex shedding interacts with the axial flow and amplified vortices are created. The vortex shedding frequency of the amplified vortices is typically one third of the conventional frequency, meaning that every third vortex is amplified.

2.2.4 Galloping

Galloping is a vibration mechanisms, which requires an initial transverse motion of the cable with respect to the mean wind direction. The transverse cable motion corresponds to a change in the relative angle of attack $\alpha$, which induces a transverse
2.2. Wind Induced Cable Vibration Mechanisms

load component, cf. Figure 2.5. Galloping occurs when this load coincides with the motion of the cable and thereby amplifies the response, which may result in large amplitude vibrations.

![Figure 2.5: Galloping](image)

Hartog (1932) was the first to present a stability criterion for cross-flow oscillations. A D-shaped cross section was used to illustrate the mechanism. A decrease in the drag coefficient and a negative slope of the lift coefficient may lead to galloping instability. Ice-coated cables may also suffer from galloping due to the deformed shape of the cross section, generating a change in the aerodynamic forces.

### 2.2.5 Dry Inclined Cable Galloping

Galloping-like vibrations of inclined cables on cable-stayed bridges have been observed in dry condition, cf. (Matsumoto et al., 2001; Zuo and Jones, 2009, 2010), hence the name. This mechanism was reproduced in wind tunnel by Saito et al. (1994). Saito proposed also a stability criterion adopted by FHWA (2007).

According to the general understanding, the similarity with conventional galloping is apparent, but Cheng et al. (2008a,b) report that the presence of the axial flow on the leeward side of the inclined cable should not be neglected. The mechanisms of dry inclined cable galloping have not been fully comprehended yet, and requires further studies as Matsumoto et al. (2010) say. The recently published work by Raeesi et al. (2012) reveals that a breakdown of the regular von Kármán vortex shedding combined with negative aerodynamic damping are possible onset conditions for the galloping of dry inclined cables on real bridges.

### 2.2.6 Wake Induced Vibrations

When the cable is positioned downstream relative to another body, wake induced vibrations may be dominating and lead to instability.

The turbulence of the wake may be random, but e.g. in case of conventional vortex shedding from the upstream body the wake may induce large vibrations, if the vortex shedding frequency and the eigenfrequency of the cable coincide, hence a vortex street induces buffeting resulting in instability similar to the lock-in due to vortex shedding.

In the case of the Øresund Bridge, the distance of 670mm between the twin cables was suggested in the design phase, after wind tunnel testing, to avoid instability due to wake galloping, cf. (de Sá Caetano, 2007, p. 129). Possible wake interactions e.g. pylons-cables and between cables belonging to the two different stay planes were not considered when analysing monitoring data of the Øresund Bridge.
2.2.7 Rain-Wind Induced Vibrations

RWIV are believed to be the result of a complex non-linear interaction between the wind load on the cable and axial water rivulets formed by the rain, which run down the cable. RWIV are normally observed to happen in the wind velocity range $5 - 18 \text{ m/s}$. Lower wind velocities are believed not to produce enough energy to excite the cable, while at higher velocities the water is blown off the cable, so the rivulet is not sustained, cf. (Gimsing and Georgakis, 2012). RWIV are often observed for cables declining in the wind direction, but that is not always the case as Main and Jones (1999) said.

Usually two rivulets are formed, one running along the top windward side and a bigger rivulet along the bottom leeward side of the cable, sketched in Figure 2.6, leading to an asymmetric cross section and therefore a variation in aerodynamic forces on the cable. Note that depending on the balance of gravitational, aerodynamic and surface capillarity forces, cf. (de Sá Caetano, 2007, p. 39), it may be hard for the upper rivulet to form.

![Figure 2.6: Cross section of bridge cable with upper and lower water rivulets](image)

The rivulets trigger the flow separation and thereby modify the pressure distribution compared to the pressure around a normal circular cylinder, cf. (Cosentino et al., 2003). A decrease on the drag coefficient and a negative slope of the lift coefficient may lead to negative aerodynamic damping and eventually instability, if the total damping becomes negative. Hereinafter the cable has started to vibrate, the rivulets tend to oscillate circumferentially, cf. (de Sá Caetano, 2007, p. 40). This may enhance the vibrations. According to studies by Flamand (1994) the existence of 'fake' rivulets was not enough to cause instability, while actual water rivulets led to instability for a PE tube covered with soot to make the surface not water-repellent.

2.3 Vibration Control Systems

Different approaches are available to suppress or control the vibrations. 'Structural' control and 'mechanical' control are introduced, after which 'aerodynamic' counter-measures are presented.

Cable systems on the larger bridges are typically characterised by low mass and stiffness. 'Structural' control solutions indeed operate on mass and stiffness. One way to improve the performance of cable systems is e.g. to add cross-ties. This method has been used frequently as temporary solution during construction or permanently to suppress vibrations and increase stability. Ties were used on the Øresund Bridge to
2.3. Vibration Control Systems

avoid large vibrations for the cables with the former inadequate damper type, while
testing new damper types.

The other solution used on the Øresund Bridge consists of ‘mechanical’ control -
damping devices such as tuned mass dampers, liquid dampers, etc. These are widely
used on bridge cables which suffer from vibrations, since external dampers can be
applied after construction of the bridge, but this may be an expensive solution on
the long run, cf. the Øresund Bridge case reported in section 1.1.1, and the dampers
must be maintained properly to guarantee sufficient damping throughout their entire
lifetime.

The intention of ‘aerodynamic’ control systems or solutions, in contrast to ‘structural’
and ‘mechanical’ control is to change the aerodynamic properties of the cable system
through shape and surface modifications. As a matter of fact, shape modification of
cables is limited, but surface modifications are widely used, cf. (Kleissl and Georgakis,
2011).

The most used solutions for surface modification of stays in cable-stayed bridges can
be ascribed to two categories: cables wound with single or multiple helical fillets and
stays with pattern-induced surfaces. Solutions with fillets are widely used in Europe
and North America. These surface modifications are e.g. used on the Øresund Bridge,
Pont de Normandie, Cooper River Bridge, cf. Figure 2.7(a), to name a few. Pattern-
indented surfaces are mainly used in Japan, South Korea, China and Asia in general.
Well known examples are the Sutong Bridge and the Tatara Bridge. Figure 2.7(b)
shows the cables of the latter bridge with the modified surface. Helical fillets have
the ability to disrupt the water rivulets responsible for RWIV and generate a variable
flow separation line on the cable axis, mitigating the drag crisis. Pattern-induced
surfaces instead, inhibit the formation of rivulets and stabilize the flows separating
on the cable surface, shifting the drag crisis to lower $Re$ number, cf. (Kleissl and
Georgakis, 2012). On the Øresund Bridge a solution with double helical fillet has
been chosen as aerodynamic countermeasure against the RWIV and its effectiveness
was investigated by Larose and Smitt (1999) during the design phase.

Focusing on the stay surface, roughness of the cable covering plays an important role
too. As the roughness is increased the drag curve i.e. drag force coefficient as a
function of $Re$ number changes, cf. (Matteoni and Georgakis, 2012). The increasing
roughness induces an earlier transition to turbulence in the boundary layer, which
results in a drag curve shifted towards the lower range of $Re$. This means that the
drag crisis occurs at lower wind velocities for the same cable geometry. Furthermore
the drag crisis is not as abrupt as it is for the smooth surface, cf. (Sumer and Fredsøe,
2006, p. 47), which reduces the probability for galloping instability described in
section 2.2.4.

Surface modification is often a compromise, since the price is an increased drag in the
supercritical range, which is particularly relevant for long span cable stayed bridges
due to the significant wind load on the cable system.
2.4 Damping

When looking at structures, damping is of course one of the key parameters determining structural properties together with mass and stiffness. Therefore, here a section is specifically dedicated to damping, as it is a central node for the thesis scope. Firstly, a brief theoretical derivation of damping is presented. Secondly, damping in bridge cables is described and finally some methods for its estimation are reported.

2.4.1 Single-Degree-of-Freedom Systems

The dynamic response of a linear single-degree-of-freedom system is described by the second order differential equation, the equation of motion:

\[ M \ddot{x}(t) + c \dot{x}(t) + kx(t) = F(t) \]  \hspace{1cm} (2.5)

with mass \( M \), stiffness \( k \) and the load history \( F(t) \). Damping is introduced through the viscous damping parameter \( c \), which is proportional to the velocity. Dividing with the mass and introducing the natural angular frequency \( \omega_0 \) and the damping ratio \( \zeta \), the normalized equation of motion becomes:

\[ \ddot{x}(t) + 2\zeta \omega_0 \dot{x}(t) + \omega_0^2 x(t) = f(t) \]  \hspace{1cm} (2.6)

where

\[ \omega_0 = \sqrt{\frac{k}{M}} \quad , \quad \zeta = \frac{c}{2 \sqrt{kM}} = \frac{c}{\epsilon_{cr}} \quad , \quad f(t) = \frac{F(t)}{M} \]  \hspace{1cm} (2.7)
2.4. Damping

The damping ratio denotes the ratio between viscous damping $c$ of the system and its critical damping $c_{cr}$.

The natural period of the system representing the time required for the undamped system to complete one cycle of free vibration is given by:

$$ T_0 = \frac{2\pi}{\omega_0} \quad (2.8) $$

and the natural cyclic frequency, which in the present work is just referred to as the natural frequency, is defined as:

$$ f_0 = \frac{1}{T_0} = \frac{\omega_0}{2\pi} \quad (2.9) $$

**Underdamped Free Vibrations**

For an underdamped system with $0 < \zeta < 1$ the natural angular frequency is:

$$ \omega_d = \omega_0 \sqrt{1 - \zeta^2} \quad (2.10) $$

The lower damped frequency corresponds to a longer time period of the cycles of the damped vibration. For values $\zeta << 1$ the term $\zeta^2$ and thereby the difference between the natural angular frequency of the undamped and the underdamped system becomes negligible in many cases, (Chopra, 2007, p. 51). This is often the case for slender and flexible structure elements like bridge stay cables.

2.4.2 Bridge Cables Damping

A very important property of a bridge cable regarding vibrations is damping, which depends on mass, stiffness, structural construction and aerodynamic properties. The damping of long cable stays is normally very low and additional measures must be considered to avoid undesired large amplitude vibrations. The total damping of bridge cable consists of different contributions, which can be divided into three categories:

- The inherent damping is mainly induced by friction between the internal elements of the cable such as the steel wires and the HDPE sheaths covering the strands.
- Additional mechanical damping e.g. from external dash-pot dampers, mass tuned dampers, etc., cf. section 1.1.1.
- Aerodynamic damping from the pressure differences induced by the flow around the cable. Note that if the aerodynamic damping becomes negative it acts in favour of the vibration.

The sum of contributing inherent and mechanical damping is defined as total structural damping, from here on.

A structure moving in air, such as an oscillating bridge cable, experiences some forces tending to damp the vibration due to the body-air interaction. They are called aerodynamic forces and in steady air they mainly depend on the viscosity of air, as Davenport (1962) firstly pointed out. If the body is moving, e.g. due to gusty wind actions,
fluctuating components of drag and lift are induced counteracting the motion itself. For many engineering structures this aerodynamic damping, which may be either positive or negative, is usually not large especially compared with mechanical damping, cf. (Davenport, 1962). But for light and flexible structures, e.g. line like structures, aerodynamic damping becomes a significant factor. Especially when negative, it could lead to instability situations if its absolute value overcomes the structural damping, cf. (Davenport, 1983).

By means of Operational Modal Analysis (OMA), Acampora and Georgakis (2011b) determined the total damping of the Øresund Bridge cables from full-scale monitoring. The aerodynamic damping of the stays was found by subtracting the structural from the total one, cf. section 3.3.

Similarly, the aerodynamic damping for passive-dynamic wind tunnel tests was determined as the difference between the total damping and the damping measured at zero wind velocity i.e. when the interaction between the moving cable and the still air is negligible.

### 2.4.3 Free-Vibration Decay Method

The most simple and frequently used method to determine the damping of a system is to consider the change in displacement amplitude per oscillation. The damping ratio, $\zeta$, of an underdamped system in the time domain is found by using the logarithmic decrement, $\delta$, which is the natural logarithm of the amplitude ratio of any two successive peaks, cf. (Chopra, 2007; Clough and Penzien, 2003; Inman, 2009).

$$\delta = \frac{1}{n} \ln \left( \frac{x(t)}{x(t + nT_d)} \right)$$ \hspace{1cm} (2.11)

where $n$ is the number oscillations between the two successive positive peaks and $T_d$ is the period of the damped oscillation.

The damping ratio expressed by the logarithmic decrement is given by:

$$\zeta = \frac{\delta}{\sqrt{4\pi^2 + \delta^2}}$$ \hspace{1cm} (2.12)

The response of a damped structure characterized by the damping ratio $\zeta$ is illustrated in Figure 2.8. The envelope of the underdamped free vibration response time history is defined as:

$$x(t) = x_0 \cdot e^{-\zeta \omega_0 t}$$ \hspace{1cm} (2.13)

As mentioned by Clough and Penzien (2003) a major advantage of the free-vibration decay method is that the vibrations can be initiated by any convenient source and only the relative displacement amplitudes need be measured. Thereby equipment and instrumentation requirements are minimal. With a purely linear viscous damping behaviour the application of equation (2.12) by any set of successive peaks yield the same damping ratio. However, the behaviour of most real systems is not perfectly linear. E.g. the damping ratio is often found to be amplitude dependent, i.e. using $n$ consecutive cycles in the earlier part of the free-vibration response yield a different
2.4. Damping

\[ T_d = \frac{2\pi}{\omega_d} \]

\[ x(t) = x_0 e^{-\zeta \omega_0 t} \]

**Figure 2.8:** Damped response

A damping ratio than that of from \( n \) consecutive cycles for a later part of the response, where the amplitude has decreased considerably.

This consideration is important when evaluating the damping ratio with the free-vibration decay method. In fact, non-linear damping was actually observed during the conduction of passive-dynamic tests with the twin-cable model.

### 2.4.4 Other Methods

An alternative method of determining the damping ratio called 'half-power method' is based on frequency response, where the width of the peak is used to determine the damping ratio, cf. (Clough and Penzien, 2003; Olmos and Roesset, 2010).

\[ \zeta = \frac{f_2 - f_1}{2f_0} \]

(2.14)

where \( f_0 \) is the resonance frequency corresponding to the peak amplitude, and \( f_1 \) and \( f_2 \) are the frequencies at \( 1/\sqrt{2} \) of the peak amplitude. An advantage of this method is that it makes possible to determine the damping without knowing the static displacement. A disadvantage of this method is that the peak of the frequency response for a system with low damping e.g. < 0.5\% becomes narrow, which makes it necessary to use smaller intervals for the discrete frequencies i.e. higher frequency resolution, cf. (de Sá Caetano, 2007, p. 184), to obtain the desired accuracy. This will require a longer logging time, which means more data with the same sample frequency. Due to the previous considerations the half-power method was not applied within the present project.

Several other methods exist for the determination of the damping of a system. An important one is the so called 'resonance energy loss per cycle' method, cf. (Clough and Penzien, 2003). The system is loaded with a known input force and the resulting displacement is measured. The applied force is varying harmonically with a frequency equal to \( \omega_0 \). So the (damping) force-displacement curve can be built. For linear viscous system the resulting plot is an ellipse. In non-linear viscous system the area beneath the curve still represents the total energy input per cycle and therefore the equivalent viscous damping ratio \( \zeta_{eq} \) can be evaluated, having the same amount of energy loss as the reference system, i.e. a linear viscous system. Issues regarding the
application of this methodology are related to the complex instrumentation required to control and measure the input loading. Also it is not possible to have a direct vision of the motion of the model subjected to another external loading, which for the present project is the wind action. Thus, this procedure was not considered further.

Another approach is based on the P-\(\Delta\) effects. Mansuri (2009) describes that the P-\(\Delta\) effects reduce by increasing the damping ratio. As a matter of fact, this method is mainly suitable for multi-DoF systems as multi-storey buildings, hence not implemented within this project.
2.4. Damping
Chapter 3

Full Scale Monitoring

The Øresund Bridge is monitored with a fixed system installed in 2009 and accomplished in January 2010 by ‘GeoSIG Ltd’. The system records oscillations of the cables and meteorological conditions.

In this section the configuration of the monitored cables, to be investigated by wind tunnel tests is selected and vibration events are illustrated. The results from full-scale monitoring at the Øresund Bridge are presented in terms of aerodynamic damping, which was estimated by Acampora and Georgakis (2011b).

3.1 Monitoring System

The Øresund Bridge fixed monitoring system was positioned during 2009 and made operative in January 2010 by ‘GeoSIG Ltd’. GeoSIG Ltd (2012) reports that the monitoring system comprises 105 channels for environmental and structural data, data acquisition and processing centre. Environmental data include atmospheric humidity, temperature and pressure. According to Acampora (2011), accelerations in the in-plane and out-of-plane directions are registered by tri-axial accelerometers AC-53 (GeoSIG Ltd, 2012), with 200mg/V sensitivity. Ultrasonic anemometers HD2003 (DeltaOHM Srl, 2012a) can measure wind speeds up to 60m/s with a resolution of 0.01m/s. They are placed at the top of the south-east pylon, on the south side of the deck mid-span and between the two longest cable pairs west of the mentioned pylon. A rain-gauge HD2013D (DeltaOHM Srl, 2012b) is located near the anemometer at the pylon top to collect rainfall data with a resolution of 0.2mm. The monitoring system samples at a frequency 30Hz for all channels using National Instruments Compact RIO.

Important monitoring system outputs describing the behaviour of the stays can be listed as:

- Maximum global cable displacements perpendicular to the cable at mid-span and quarter-span.
3.2. Cable Configuration

- 1-minutes mean wind directions during vibrations event.
- 1-minutes mean wind speed during the event of vibrations.
- Mean frequencies of cables over the monitored period.
- Structural damping expressed as logarithmic decrement.
- Meteorological conditions in terms of wind speed, precipitation, temperature, etc.

3.2 Cable Configuration

Within the monitored period from 12/01/2010 to 30/06/2011, cf. (Acampora, 2011), four different configurations of the monitoring system have been arranged. In particular accelerometers have been placed in various positions to monitor the oscillations of different cables.

One particular configuration was considered when realizing the wind tunnel model, aiming for a consistent comparison between full-scale and reduced-scale. It is obvious that the selected configuration must be representative for the investigated problem. The so called Configuration 4 was selected because it covered a long period - from January to June 2012 - compared to the others.

The instrument arrangement for Configuration 4 is illustrated in Figure 3.1. Here it is seen that the first and third longest cables of the eastern main span (southern cable plane) and western side span (northern cable plane) are equipped with accelerometers. Anemometers and rain gauges are placed both at the bridge deck and at the top of the south-eastern pylon.

The cables '8M' and '8S' were selected among the monitored cables in Configuration 4 for the investigation of aerodynamic damping by wind tunnel test due to the following considerations.

The cables '8M' and '8S' are the third longest cables of the bridge, hence the longest
3.2. Cable Configuration

cables where the additional tuned mass dampers are not installed. The cables were therefore assumed to be most prone to vibrations due to the length and the low structural damping.

Possible rain-wind induced vibrations were also considered. In fact, maximum displacements of the cables ‘8M’ and ‘8S’ for dry and wet conditions are registered to be 0.08 – 0.12 m respectively. Figure 3.2 shows in a polar plot the recorded vibrations in dry (red crosses) and wet (blue stars) conditions. The 0° degree indicates the North, the dashed blue line the bridge axis.

![Figure 3.2: Distribution of maximum cable displacements [m] vs wind direction [°], cf. (Acampora, 2011)](image)

Figure 3.3 showing the maximum cable displacement vs wind speed indicates that the largest cable vibrations in the presence of rain occur at wind velocities around 13 – 15 m/s, as pointed out by the yellow marks.

![Figure 3.3: Maximum cable displacements [m] vs wind speed [m/s], cf. (Acampora, 2011)](image)

Furthermore, it was chosen to neglect the events, where the vibrations may have been due to wake effects. This refers to events related to wind directions along the bridge axis and crossing both cable planes. The extreme events, which have not been
affected by these disturbances have been observed at approximately 180° for '8M' and 0° for '8S'. The yellow oval-shaped circles in Figure 3.2 highlight these extreme events. They have been reported for the cable declining in the wind (θ = 30° for all cables) with a horizontal yaw of about β = 70°, which is within the angle range for that RWIV occur the most as reported by Gimsing and Georgakis (2012).

The relative cable-wind angle, φ, describing the angle between the cable and the wind, is defined as:

\[ \phi = \cos^{-1}(\cos \beta \cos \theta) \]  

(3.1)

For the selected configuration the relative cable-wind angle was found to be \( \phi = \cos^{-1}(\cos(70°) \cos(30°)) = 72.8° \).

The definitions of the different angles describing the cable configuration, inclination, yaw and relative yaw angle, are illustrated in Figure 3.4.

Table 3.1 presents the outputs from the monitoring system for cable '8M' and '8S' during the considered period and configuration. Note that the frequencies corresponding to first mode vibrations are slightly detuned. This is due to the cable sag in the in-plane direction.

Table 3.1: Monitoring outputs for Configuration 4 - cables '8M' and '8S', cf. (Acampora, 2011)

<table>
<thead>
<tr>
<th>Monitoring output</th>
<th>Value for '8M'</th>
<th>Value for '8S'</th>
</tr>
</thead>
<tbody>
<tr>
<td>In-plane frequency</td>
<td>0.573Hz</td>
<td>0.572Hz</td>
</tr>
<tr>
<td>Out-of-plane frequency</td>
<td>0.559Hz</td>
<td>0.565Hz</td>
</tr>
<tr>
<td>Maximum cable displacement</td>
<td>0.08m</td>
<td>0.12m</td>
</tr>
<tr>
<td>Structural damping ratio</td>
<td>3.5%</td>
<td>3.4%</td>
</tr>
</tbody>
</table>
3.3 Monitoring Results

The results from the monitoring in terms of aerodynamic damping ratio have been estimated by Acampora and Georgakis (2011b) applying the operational modal analysis (OMA) using the commercial software platform 'ARTeMIS'. Hereby the overall mean damping of the cable for a series of vibration records have been determined for specific wind velocities and wind directions. In the specific case reported in Figure 3.5 wind speeds are up to $15\text{m/s}$ and the considered direction is the normal $\pm 5^\circ$, to the twin cables vertical plain, i.e. cross-flow. The aerodynamic damping has been determined by subtracting the known structural damping.

Figure 3.5: Aerodynamic damping from full-scale monitoring of cable ‘8M’-Configuration 4 in dry and wet conditions, cf. (Acampora and Georgakis, 2011b)

Figure 3.5 shows a comparison in terms of aerodynamic damping in the out-of-plane oscillation direction between dry and wet conditions. It is worth to mention the drop in $\zeta_a$ for the dry condition. This drop appears larger than the one normally observed for single cylinders, cf. (Acampora and Georgakis, 2011b). The wet condition shows that drop in $\zeta_a$ starts at a lower $Re$ while the minimum value happens at the same Reynolds number. Even though $\zeta_a$ for the wet case is becoming negative between $1.3 \times 10^5 < Re < 2 \times 10^5$, no large amplitude vibrations have been registered by Acampora and Georgakis (2011a). That is because the $Sc$ number defined in (4.3) was always positive and larger than five, cf. (Acampora and Georgakis, 2011b, Figure 7), as a consequence of the very high level of structural damping of the Øresund Bridge cables.
3.3. Monitoring Results
Chapter 4

Experimental Work

In this chapter, the experimental work performed in the wind tunnel is described including wind tunnel specifications, selected testing method, description of the twin-cable model, scaling, dynamic rig, test set-ups, data acquisition, test performance and data processing. Lastly, the obtained results are presented and discussed.

4.1 Test Preparation

4.1.1 Wind Tunnel Specifications

The wind tunnel where the experimental investigations were performed is the Climatic Wind Tunnel (CWT) at ‘FORCE Technology’, Kgs. Lyngby. It is a closed-circuit wind tunnel, which is dedicated to testing of structural cables. The test chamber has a cross-section of $2m \times 2m$ and is $5m$ long, which allows for full-scale models of cables up to approximately $200mm$ in diameter in cross-flow with a relative blockage area ratio of $10\%$. An overview of the CWT is provided in Figure 4.1

![Figure 4.1: Principle sketch of the CWT (left), and sectional cut through the test section of the wind tunnel (right)](image)

The control of precipitation and temperature demands a water tight system and
4.1. Test Preparation

Experimental Work

A cooling unit. The cooling system of the CWT allows minimum air temperature of about $-5^\circ C$ at maximum speed. Specific measurements about flow turbulence intensities were performed in January 2010 by 'FORCE Technology'. Results show that at the mid-section of the testing chamber the turbulence intensity is $0.8 - 0.9\%$ in the flow direction and about $0.5\%$ for the lateral and vertical flow components. Measurements refer to normal condition of the wind tunnel, i.e. without any extra grid or structure to increase the flow turbulence.

General specifications of the CWT are summarized in Table 4.1.

<table>
<thead>
<tr>
<th>Property/dimension</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Test section inner cross-section</td>
<td>$2.0m \times 2.0m$</td>
</tr>
<tr>
<td>Test section length</td>
<td>$5.0m$</td>
</tr>
<tr>
<td>Maximum air speed in turbulent flow</td>
<td>$30m/s$</td>
</tr>
<tr>
<td>Cloud air vapour density</td>
<td>$0.4g/m^3$</td>
</tr>
<tr>
<td>Minimum air temperature at max. speed</td>
<td>$-5^\circ C$</td>
</tr>
</tbody>
</table>

4.1.2 Testing Methods

There are generally two major approaches of conducting wind tunnel tests on bridge cables: static and dynamic approaches.

In static tests the model ends are fixed to the ceiling and floor or walls of the facility. Measures of pressure, force and moment coefficient are the common direct output of such test methodology. Further investigations, e.g. aerodynamic damping calculation, can be made assuming quasi-steady theory.

Dynamic tests are primarily different from static ones because of the way the model is connected to the test chamber. The model moves during the tests and direct outputs can be data about displacements, velocities or accelerations. The most evident advantage compared to the static approach is the visualization of the behaviour of the model in terms of its own motion. If correctly setup, the model can accurately simulate vibration phenomena of full-scale structures. The dynamic approach is also an important tool for the calibration, validation and interpretation of results from static tests. Therefore it should always be a part of a complete test campaign.

Two methods are suitable when conducting dynamic wind tunnel tests: active-dynamic and passive-dynamic.

The first method requires an external driving force, which actively imposes a known motion to the model, e.g. an engine inducing a harmonic excitation, cf. van Gils Hansen (2008).

The aerodynamic forces which the flow generates on the model are found by subtracting the inertial forces from the driving forces for a particular motion, as described by Georgakis et al. (2009). This method is relevant when investigating the forces acting on a cable subjected to a particular vibration mechanism that is simulated by the external forcing.

In the second method, passive-dynamic, the model is mounted on a spring rig and therefore free to move in the wind, as mentioned by Georgakis et al. (2009). This method is useful when investigating the aerodynamic behaviour of cables under vary-
ing excitation mechanisms. Vibration characteristics can be seen directly for varying wind velocities, directions, turbulence intensities, different cable-wind angles and meteorological conditions. For these reasons, the last method i.e. passive-dynamic, was adopted for the conduction of wind tunnel tests within the scope of this project.

4.1.3 Twin-Cable Model

For the purpose of the project, the exact twin-cable arrangement of the Øresund Bridge was reproduced in the model used for the tests in the wind tunnel. Therefore the side-by-side layout of the bridge stays was considered in designing and realizing the model.

This section presents the model which was used to perform the experimental work in the wind tunnel including dimensions, materials, surface roughness and treatment due to surface tension requirements.

The use of a wind tunnel facility, in this case the CWT, implied some limitations in the model size because of the blockage effect. The ratio between the area of the model ‘seen’ by the wind and the cross section area of the tunnel, i.e. \(4m^2\) as reported in table 4.1, namely blockage ratio is defined as:

\[
b_k = \frac{A_{\text{model}}}{A_{\text{CWT}}} \tag{4.1}
\]

Results of wind tunnel tests can be considered reliable if blockage correction is applied, for values of \(b_k < 10 - 15%\) as a rule of thumb, cf. (ESDU 80024, 1980). As a consequence of this, the dimensions of the model were scaled down by a factor of \(\lambda_{\text{geom}} = 0.44\), i.e. the diameter of the cable is 110\(mm\) compared to the prototype size of 250\(mm\). The resulting blockage coefficient for a twin-cable model with \(D = 0.11m\) installed in cross-flow was equal to \(b_k = 11%\).

The model was scaled compared to full-scale cables and thus measurements on the model were subjected to scaling laws. Those were governed by the ‘Froude’ similarity because of the invariability of gravity, as explained by Koss (2012). Fundamental and dependent physical quantities were defined as a function of \(\lambda_{\text{geom}}\) and reported in Table 4.2 for a general overview. Derivation of the used scaling coefficients and details about the adopted scaling rules are reported in Appendix A. Note that the model mass did not follow the scaling law according to the ‘Froude’ similarity, as illustrated in section 4.1.5.

Another important scaling factor was referring to damping. It depended on the mass-frequency parameter ratio between model and prototype cables. Further details about scaled damping are reported in section 6.2.

Full-scale cables were modelled by means of commercial PVC pipes, which represented the external covering of the bridge stays. The model pipes had the outer diameter equal to 110\(mm\), a thickness of about 6\(mm\) and a length of 3050\(mm\).

4.1.3.1 Surface Roughness

The original surface of the PVC tubes was extremely slick, so the surface was treated with sand paper of type ‘P240’ in order to simulate prototype cables surface aspect,
Table 4.2: Scaling coefficients

<table>
<thead>
<tr>
<th>Physical quantity</th>
<th>Prototype</th>
<th>Model</th>
</tr>
</thead>
<tbody>
<tr>
<td>Acceleration</td>
<td>1</td>
<td>1</td>
</tr>
<tr>
<td>Length</td>
<td>1</td>
<td>$\lambda_{geom} = 0.44$</td>
</tr>
<tr>
<td>Time</td>
<td>1</td>
<td>$\sqrt{\lambda_{geom}} = 0.6633$</td>
</tr>
<tr>
<td>Frequency</td>
<td>1</td>
<td>$1/\sqrt{\lambda_{geom}} = 1.51$</td>
</tr>
<tr>
<td>Area</td>
<td>1</td>
<td>$\lambda^2_{geom} = 0.1936$</td>
</tr>
<tr>
<td>Velocity</td>
<td>1</td>
<td>$\sqrt{\lambda_{geom}} = 0.6633$</td>
</tr>
<tr>
<td>Force</td>
<td>1</td>
<td>$\lambda^3_{geom} = 0.085$</td>
</tr>
</tbody>
</table>

anticipate the drag crisis at lower Reynolds number, cf. (Sumer and Fredsøe, 2006, p. 47) and (Matteoni and Georgakis, 2011; Zdravkovich, 2003), because of the limiting upper velocity of the wind tunnel facility, and to increase the wettability of the tubes, which is important when conducting wet tests, cf. (Larose and Smitt, 1999).

The obtained surface roughness was evaluated with an optical microscope, cf. (Alicona, 2012).

Six gummy samples of the pipe surface were picked with technique and tools from (Struers, 2012). Hereafter 2-D optical pictures, were taken by the high resolution camera incorporated in the 'Alicona' and analysed by the microscope itself. Pictures of the six samples are shown in Figure 4.2. The microscope obtained a 3-D set of data from the 2-D pictures, which were used to determine an average roughness measure along four trajectories per sample. The roughness average was defined as:

$$R_a = \frac{\sum_{i=1}^{n} |y_i^2|}{n}$$  (4.2)

where $n$ is the number of data points and $|y_i^2|$ is the module of the distance of the $i^{th}$ data point from the base line. For the cable model the surface roughness variation was found to be between $R_a = 290 - 470 \text{nm}$ with an average value of $R_a = 390 \text{nm}$. Figure 4.3 shows an example of a 3-D reconstruction of the surface aspect from the data set acquired by the 'Alicona' microscope.

4.1.3.2 Double Helical Fillet

The plastic covering of full-scale cables of the Øresund Bridge is fitted with a double helical fillet 2.1$\text{mm}$ high and a pitch angle of 55°, with the purpose to mitigate the vibrations provoked by the interaction between rain and wind as reported by Larose and Smitt (1999). In order to be consistent when realizing the cables model, even the fillet had to be reproduced and scaled. Water droplets are not changing passing from full-scale to model, but the excess of water on the model cable surfaces compared to the one on prototype stays can be assumed to be blown off by the wind; geometrical scaling of the fillet was then considered appropriate. Therefore an electric wire of approximately 0.6$\text{mm}$ in diameter was used to that purpose and attached to the pipe surface by means of a double sided 1$\text{mm}$ wide tape. The orientation of the fillet in full-scale cables is counter-clockwise scrolling the pipes from top to bottom. For the model a clockwise orientation of the fillets, cf. e.g. Figure 4.4(a), was applied to
simulate the selected cable configuration from full-scale monitoring, since the test set-up was mirrored, as explained in 4.1.5. It means that the critical configuration from monitoring for the declining cables with a yaw angle $\beta = 70^\circ$ was reproduced in the wind tunnel with a negative yaw angle $\beta = -70^\circ$, because of geometrical restrictions in the CWT. Therefore the fillets were applied contrariwise to reproduce the same flow-structure interaction.

### 4.1.3.3 End Connections

The prototype cables on the Øresund Bridge are arranged in a side-by-side configuration with a centre to centre distance of 670mm, cf. (Larose and Smitt, 1999). Model pipes were kept at the right spacing i.e. 294.8mm by means of the triangular shaped
end connection especially designed for the set-up and shown in Figure 4.4.

Figure 4.4: Triangular shaped end connections of the twin-cable wind tunnel model

It consisted of four elements jointed together: two aluminium flanges inserted into the pipes for about 10 cm to keep their ends rigid, an aluminium flat plate connecting the two tubes with the right spacing, two triangular steel elements to stabilize and reinforce the connection and a rectangular flat steel bar for the attachment of springs and threaded bars, which also increased the torsional stiffness of the overall model itself. All joints were bolted in order to facilitate adjustments and reuse. Only the connection between the 'L' elements constituting the triangles were welded. The PVC pipes were simply screwed onto the aluminium flanges with four vines per end. The overall dimensions of the triangular metal end were within 30 cm by 30 cm. The triangular frame was used at the top end as support for the detecting plexiglass plates of the lasers displacement transducers employed to record the motion of the model while testing, cf. Figure 4.4(a). On the lower end instead, laser plates were fixed on the threaded bars extension. They were made to separate the anchorage points of the out-of-plane springs from the in-plane spring, cf. Figure 4.4(b).

4.1.4 Dynamic Rig

Wind tunnel tests with the twin-cable model were performed using the passive-dynamic method, as previously described in sections 1.3.3 and 4.1.2. Therefore it was necessary to build a rig to support the springs connected to the model. As one could imagine there exist several manners to realize such a rig. Hereafter some examples of dynamic rigs used in others wind tunnel facilities are briefly presented and the rig used in the CWT described.
Dynamic Rig at Velux A/S

A 1-DoF rig was designed and used at the Velux A/S wind tunnel in Østbirk, Denmark, for testing a model of the Øresund Bridge cables. The full rig was located inside the test chamber, and it allowed for the movement of only one cable while the other cable was placed just to simulate the surrounding flow field, as indicated by Larose and Smitt (1999). Figure 4.5 shows the dynamic rig used at Velux A/S.

![Sketch of the rig](image1)
![Rig with the twin-cable model](image2)

**Figure 4.5:** Dynamic rig at Velux A/S for tests on the twin-cable model of the Øresund Bridge, cf. (Larose and Smitt, 1999)

Dynamic Rig at CSTB

At the climatic wind tunnel of CSTB situated in Nantes, France, a suspension system of springs and thin cables fixed to a steel frame was used for investigating rain-wind induced vibrations. In this case the overall frame supported both the top and the bottom rig and it was placed totally inside the test section. Figure 4.6 illustrates the dynamic rig used at CSTB.

![General arrangement](image3)
![Rig with a cable model](image4)

**Figure 4.6:** Dynamic rig at CSTB for tests on inclined cables, cf. (Cosentino et al., 2003)
4.1. Test Preparation

Experimental Work

Dynamic Rig at NRC

At National Research Council (NRC) of Canada, tests on inclined cables were performed using a dynamic rig divided in two: an upper rig placed out of the test section and a bottom rig located inside the testing chamber. The two parts were independent from each other. A steel frame supported the springs which were set in the two main orthogonal directions, perpendicular to the axis of the model. The supporting frame also allowed for adjustments in the spring stiffness in order to control the model frequencies in the two directions. Figure 4.7 shows the top and bottom rigs.

![Figure 4.7: Dynamic rig at NRC Canada for tests on inclined cables, cf. (FHWA, 2007)](image)

Dynamic Rig at CLP

Zhan et al. (2008) developed in Honk Kong a new concept of dynamic rig for testing rain-wind induced vibrations on inclined-yawed cables, to better simulate the dynamic behaviour of actual stays. The cable model was pulled at its ends by pre-tensioned springs which could be adjusted in length to reach the desired natural frequency. The equilibrating force of the cable model actually came from a tension force, as in full-scale prototype cables. This new set-up was found to be convenient for simulation of large amplitude vibrations, but its use not validated by others yet. Figure 4.8 shows the new dynamic rig used at CLP Wind Tunnel in Honk Kong.

![Figure 4.8: Dynamic rig at CLP Wind Tunnel at the Hong Kong University of Science and Technology, cf. (Zhan et al., 2008)](image)
4.1.4.1 Dynamic Rig at CWT

The dynamic rig, which was used in this project allowed two degrees of freedom. A single in-plane spring pointing upwards and two out-of-plane springs were connected to the model at each end. The springs were arranged as illustrated in Figure 4.9.

![Figure 4.9: Conceptual sketch of the dynamic spring rig used at CWT](image)

The supporting structure for the dynamic rig at CWT was originally designed by Leif W. Smitt based on the experience from the tests at the Velux A/S wind tunnel. The main difference compared to the previously presented cases was on the dimensions of the wind tunnel cross-section, which is $4m^2$ at the CWT as reported in Table 4.1. Therefore there was no room for allocating the rig inside the test section without causing flow disturbances and increasing the blockage. Thus, in this case the metal frame of the rig was built around the chamber and supported the springs which were located outside the test-section. This particular configuration implied openings in the walls of the tunnel to let the model ends be connected to the rig, cf. Figure 4.10(a) and Figure 4.14.

The rig was in fact a combination of two separate and independent rigs, one per model end. This allowed for numerous geometrical configurations of the model in several set-ups as described later in section 4.1.5, but on the other hand, adjusting the rig components to a new configuration required double the time than for a rig fully connected to one frame. The rig at CWT consisted of one or two transversal beams - depending on the needs - placed on the top of the chamber or below its floor supporting one vertical arm per side. The beams were Square HSS (Hollow Structural Sections) 100x100x5mm, while the vertical arms were circular hollow pipes 60x5mm. The latter were carrying secondary pipes 60x5mm and 40x3mm arranged in a cross looking shape. The bigger pipe acted as the cross trunk while the smaller ones attached on it worked as the cross arms. The trunk of the cross could be rotated arbitrarily with respect to the vertical pipes thanks to a special fitting so that the plane of the cross could be selected depending on the wanted inclination of the model. All those elements were connected via special high-strength light steel fittings similar to the one used for scaffolding joints, cf. Figure 4.10. Finally other high-strength light steel fittings attached to the cross elements served as end supports of the springs which were connected directly to the model, as described in section 4.1.3.

Obviously the purpose of a dynamic rig was to let the model vibrate while conducting...
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tests. The motion was limited by the rig configuration and restricted to the allowed
degrees of freedom. Those were the translations in the two principal orthogonal
directions, in-plane and out-of-plane. RWIV usually happen in the in-plane direction,
as described by Cosentino et al. (2003), but the rig of the CWT was intended as a two-
degree-of-freedom rig. That was because the monitored vibrations did not only occur
in the in-plane direction, cf. (Acampora, 2011), and because of the slightly detuned
cable frequencies in the two orthogonal directions due to the cables sag, inducing
different vibration characteristics in the two main planes. Vibration characteristics
mainly referred to the natural frequencies of the stays. The in-plane spring and
the two out-of-plane springs per end of the dynamic rig had to reproduce the exact
vibration characteristics, conveniently scaled in the respective planes, as described in
section 4.1.3. Pre-tensioned springs were used for the model and calculated according
to the data provided by the producer (Lesjöfors AB, 2012a). The spring sizes were
determined in terms of number of coils, outer diameter and thread diameter among
the available commercial products in order to reach the wanted frequencies and allow
for one diameter vibration amplitude in both directions. The out-of-plane springs had
to permit larger deformations due to the drag displacement. This extra displacement
was found to reach about 40cm at maximum wind tunnel speed, from dynamic test
attempt at CWT on twin-cable configuration in cross-flow.

Furthermore, it was necessary to account for the interaction between the in-plane and
the out-of-plane stiffness. The out-of-plane springs were contributing to the in-plane
stiffness together with the in-plane spring and vice versa. An extra contribution to
the stiffness of the system - just on the upper end - came from the chain carrying the
axial component of the model weight in the inclined set-ups, cf. section 4.1.5. That
effect was considered for the selection of the springs of the upper rig in the inclined
set-ups. Because of the spring mass, stiffness tolerances and interactions in the 2-DoF
motion, the springs were attached to the relative support through specially designed
fittings, which could be screwed on and off the springs changing their effective length
and thereby stiffness, in order to reach the wanted vibration frequencies of the model,
with accuracy.

Note that the designed rig entailed a limitation regarding acceleration, because only
one in-plane (vertical) spring was used at each end. This means that the model only
would be able to move downwards due to gravity. Hence the upper limit for the
acceleration of the motion had to be equal to the gravitational acceleration. This
limit is lower for an inclined model set-up.

The third degree of freedom, the axial rotational, was not considered in the project,
but it should be considered in future works when e.g. ice accretions on the cables
surfaces will make the torsional mode relevant.

Another issue related to the twin-cable model affected the present rig. It regarded the
motion of the two cables which could be independent or not. If independent motion
of the two cables was wanted a very complex spring supporting system would have
been required.

This was not achievable due to the limitation in time of the project. Larose and
Smitt (1999) used a rig in which just one of the cable was allowed to move for testing
the twin configuration at the Velux A/S wind tunnel. Here the model was designed
in such a way that the two cables were forced to move together. The springs were
attached to the triangle end support vertex i.e. model axis, and the two cables rigidly
connected to each other by an aluminium plate, cf. 4.1.3 and Figure 4.4.

Some images of the rig in the adopted set-ups described in section 4.1.5, are presented
in Figure 4.11. They show the flexibility of the dynamic rig system in reproducing
several geometrical configurations.

4.1.5 Test Set-Ups

Geometrical configurations for testing the model were selected based on the informa-
tion from the monitoring of the Øresund Bridge as described previously in chapter 3,
cf. (Acampora, 2011). The critical configuration to investigate was found to be the
one for cables ‘8S’ and ‘8M’ on the side span and mid-span respectively, with the same
inclination $\theta = 30^\circ$ and yaw angle $\beta = 70^\circ$. The intention of these wind tunnel tests
was to compare the results with data from other sources i.e. monitoring and theory.
There were considered three different set-ups, as shown in Table 4.3. The cross-flow
set-up was intended as a basic case of reference. The inclined set-up was considered
because it represents the Øresund stays inclination with the wind orthogonal to the
them. The latter set-up, inclined-yawed, reproduced the worst monitored cable con-
figuration within the considered period. The term set-up referred to the combination

<table>
<thead>
<tr>
<th>Set-up</th>
<th>Inclination $\theta$</th>
<th>Yaw angle $\beta$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Cross-flow</td>
<td>0$^\circ$</td>
<td>90$^\circ$</td>
</tr>
<tr>
<td>Inclined</td>
<td>30$^\circ$</td>
<td>90$^\circ$</td>
</tr>
<tr>
<td>Inclined-yawed</td>
<td>30$^\circ$</td>
<td>70$^\circ$</td>
</tr>
</tbody>
</table>

of the model and the dynamic rig arranged in a way that the model axis was oriented
according to the selected $\theta$ and $\beta$, and the springs plane (or cross plane) orthogonal to
it, so that the springs could simulate directly the vibration characteristics of the twin
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Experimental Work

cables. To foster vibrations a lot of effort was putted in trying to reduce the Scruton number $Sc$ in (4.3), keeping the mass of the moving parts of the set-ups low; PVC tubes and end connections first, springs, their fittings and laser detecting plates later. The $Sc$ number is a dimensionless aerodynamic stability parameter. The lower it is the higher the propensity of a cable to vibrate due to galloping or vortex-shedding, cf. (Gimsing and Georgakis, 2012).

$$Sc = \frac{m \cdot \zeta}{\rho \cdot D^2}$$ (4.3)

where $m$ is the cable mass per unit length, $\zeta$ the damping ratio, $\rho$ the air density and $D$ the diameter. $Sc$ number is in practice an important coefficient because it gives an estimate of the size of a cable in design phases. In fact, $Sc > 10$ for circular plain cylinder and $Sc > 5$ for cables with helical fillets or dimples are recommended from both the US Federal Highway Administration (FHWA) and the Post-Tensioning Institute (PTI) to avoid RWIV and dry-inclined galloping, cf. (Gimsing and Georgakis, 2012).

Cross-flow set-up

The first set-up was the reference case with the model simply mounted perpendicular to the flow with $0^\circ$ of inclination from the horizontal plain. Therefore the in-plane
direction coincided with the actual vertical one and the out-of-plane with the horizontal along-wind orientation. The cross-flow set-up was used to first calibrate the data acquisition system and validate the selection of springs, which was determinant for reproducing the actual twin-cable frequencies on the scaled model. For this set-up the rig ends were perfectly symmetric to the tunnel centre-line and the model as well. The latter was extended out of the wall of the tunnel testing section through openings. They were large enough to allow the maximum permitted vibrations of one diameter (11 cm) in the vertical direction and one diameter plus the maximum expected mean drag displacement (about 20−24 cm). As one could see, the wall openings were large, inducing flow disturbances which are investigated in section 4.1.7. Figure 4.12 presents the cross-flow set-up. A test attempt for the calibration and adjustment of the acquisition system and flow visualizations were performed using this set-up. Results of dynamic tests in cross-flow are not presented here though.

Inclined set-up

The inclined set-up reproduced the actual inclination, namely \( \theta = 30^\circ \) from horizontal, of all stays of the Øresund Bridge with a cable-wind angle \( \beta = 90^\circ \). The work performed with this set-up mainly consisted of practising on the inclined geometrical configuration, the same for this set-up and the following inclined-yawed one. The model was centred with respect to the chamber and the lower end was defined as the left one looking at the cables from downstream. This is illustrated in Figure 4.13, which shows the inclined set-up. This orientation was important for the application of the double helical fillet, cf. section 4.1.9 in order to reproduce the same prototype stays aspect. The upper rig end was fixed to a transversal beam placed on the top of the tunnel while the lower end rig to a beam fixed underneath the tunnel floor. Compared to the previous set-up, the cross springs plane was simply rotated 30° from the vertical direction so that it resulted perpendicular to the model axis. The model length of 2560 mm was determined so that it got very close to the tunnel structure without hitting it while allowing one diameter vertical vibrations.
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An issue derived from the model inclination was the prevention of the axial translation of the model due to its self-weight component. To account for that a steel chain was aligned with the model axis and fixed to the wall of the room containing the tunnel, cf. Figure 4.15. The wall end of the chain was actually fixed to a rail so that the anchorage position could be adjusted when changing the wind speed and therefore the mean drag so that the chain was always lined up with the model axis. The chain was also integrated with a cable puller to always guarantee the tension in the chain and facilitate adaptation in length if needed. To estimate the mass of the water on the model tested under simulated rain condition, a force transducer with four strain gauges configured as a full bridge was arranged. The instrument was not further used for testing because of the too high resolution and sensitivity of the sensor to the varying temperature and relative humidity. No actual dynamic tests were performed with the here described set-up.

Figure 4.13: Inclined set-up viewed from downstream

Inclined-yawed set-up

The last set-up was the one modelling the configuration of stays '8S' and '8M' found to be the most susceptible to vibrations in the monitored period from January to September 2011 as reported by Acampora (2011). Because the yaw angle was smaller than 90° the model would almost hit the tunnel structure if extended through the wall openings on both sides. Therefore it was decided to place the lower end close to the cross section edge and extend the pipes out of the tunnel at the upper end; PVC tubes were 3050mm long instead of 2560mm as for the previous set-ups. Figure 4.14 illustrates the inclined-yawed set-up.

Note that the model was still symmetric about its axis because the end connections between the tubes and the springs were similar for both ends. Only the threaded bars connecting the triangular fitting vertex to the horizontal springs were longer for the lower end due to the necessity to place the downstream horizontal arm of the lower end rig totally outside the tunnel, cf. Figure 4.16(b). In order to reduce the coupling between the considered degrees of freedom to a level of minimum, springs both in
the in-plane and out-of-plane directions had to be connected to the model axis. Long threads of the lower end were experienced to bend upwards at their free end due to the self-weight of the model. Therefore springs acting in the in-plane direction were placed close to the end triangular fitting connecting the two pipes which was designed to provide the necessary rigidity. At the other end the threads were kept as in the previous set-ups in order to maintain the effective length of the steel chain at about 0.5 m limiting changes in frequencies due to its presence.

4.1.6 Rain-Simulation System

One of the main goals for the present project was the comparison in terms of aerodynamic damping in different weather conditions, namely dry and wet, cf. section 1.2. The second condition is the one responsible for the RWIV phenomena largely observed on real bridges, cf. (Gimsing and Georgakis, 2012, p. 546) and simulated in wind tunnel experiments, cf. e.g. (Cosentino et al., 2003; Flamand, 1994; Larose and
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4.1.1. Test Preparation

Inclined-yawed set-up end connections

Smitt, 1999; Matsumoto et al., 2003; Zhan et al., 2008). This phenomenon was firstly observed by Hardy and Bourdon (1980) and then identified by Hikami and Shiraishi (1988). In order to perform tests in wet condition, a meteorological condition with rain had to be reproduced in the test section of the wind tunnel. The first problem encountered in arranging such a system was the scaling. Rain and water droplets on surfaces were difficult to scale down in size. Consequently, the rain event itself was believed not suitable for a proper simulation in CWT. Focus was then put onto the reproduction of the effects of rain events on prototype cables, i.e. formation of water rivulets and wetting of the cable surfaces. Water rivulets play a major role in the behaviour of flexible line like structures changing the original characteristics of the body because they lead to a fictitious modification of the cross-section which is now ‘seen’ as asymmetric by the wind, cf. section 2.2.7 and (de Sã Caetano, 2007). In the dynamic tests performed at CWT rivulets were triggered and initiated by the water coming out from little flexible pipes attached to the top end of each of the two model cables outside the test section. Kick-start water injectors were placed two per cable, cf. Figure 4.17(a,b), with the purpose of simulating both the lower and upper rivulets occurring on prototype cables, as reported by Gimsing and Georgakis (2012, p. 546), for cable-wind angle $45^\circ < \phi < 90^\circ$. The amount of water released by the injectors was controlled while testing in wet conditions, according to the wind speed so that the water issued was the same on the two modelled stays and enough for the rivulet formation. Any surplus of water to the right quantity for the rivulet formation was anyhow blown off by the wind action.

It was observed during test attempts and rain system preparation that the water rivulet on the upper wind-ward side of the two cables was not capable of forming. That was a direct consequence of the cable scaling which led to a larger curvature compared to full-scale cables which were observed capable of sustain an upper rivulet with the same cable-wind angle. Furthermore, the two lower rivulets, although forming, were smaller than that on full-size stays - for the same reason - and not capable to run all the way along the model from the kick-start to the lower end. They were disappearing at half of the model length. Therefore it was decided to add two water sprays - ‘garden’ like type - placed upstream inside the test section to fed with water the lower rivulets while running down the cables, cf. Figure 4.17(c). The sprays were located at about 50cm from the wall of the tunnel in order to cover half of the model each. The water
Experimental Work

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(a) Water rivulet initiators  (b) Rivulet kick-start

(c) Water sprays

Figure 4.17: Rain simulation system at CWT facility at 'FORCE Technology'

sprayed on the cables was regulated in quantity, orientation and intensity according to the changing wind velocity in order for the water droplets to always reach and uniformly cover the respective portion of the model. The water released by the sprays could be described as light rain. The small droplets were transported by the wind wetting the cable surfaces homogeneously and making the lower rivulets constant in size on the entire model extension.

The described two parallel systems for rain effects simulation were supplied through common hoses from the water mains. One hose was connected directly to the water supply and then split into two branches, supplying the water injectors and the sprays respectively. The two hoses feeding the two parallel systems were integrated with regulator valves before further splits to provide water to the four kick-start pipes and the two sprays respectively.

4.1.6.1 Surface Treatment

Experience in the wind tunnel showed that the surface tension of the cable was a very important parameter for the formation of the water rivulet on the cable. Comparing a prototype HDPE full-scale bridge cable with the present scaled model with PVC pipes, the surface appearance was clearly diverse, cf. Figure 4.18. The prototype cable was characterized by micro-regular prominences and dimples while model cables appeared
more smooth at sight and to the touch.

The PVC pipes had a higher surface tension than the original HDPE cable covering. Furthermore model cables had a larger curvature than that of prototype samples. It was tested that the behaviour of water on their surface, i.e. their wettability was very different. In fact, at the tested cable geometrical configuration with inclination \( \theta = 30^\circ \) and yaw angle \( \beta = 70^\circ \) the water was blown off the PVC pipes before any water rivulet was formed, while on the original HDPE cable covering the lower rivulet was easily forming.

To increase the wettability of the test model a polyvinyl alcohol solution was applied to the pipes to change the surface tension and thereby increase the reproducibility of the water behaviour on actual stays, cf. (Larose and Smitt, 1999). Further details about surface treatment are reported in Appendix B. The result was a clear formation of the lower rivulet at a wide range of velocities.

Figure 4.18: Close up of prototype and model cable surfaces

Figure 4.19: Comparison in terms of wettability of twin-cable model before and after surface treatment
4.1.7 Flow Profiles

Flow measurements were carried out in the empty wind tunnel to investigate the effects of the panel openings on the velocity at the model location and on the turbulence intensity at the opening location.

The first flow measurements output was a velocity correction factor to link the velocity at the position of the model with the reference velocity measured by the Pitot tube placed 3.8m upstream, cf. section 4.3.1. This was done by measuring the velocity at the model position and at the reference position and comparing the two values. A velocity profile along the model axis was established to be able to take the decrease in velocity at the boundaries of the flow into account, when determining the velocity at the model position.

The turbulence level was also investigated to study the influence of the panel openings. The idea of this investigation was mainly to know how much the turbulence intensity was changing at the panel openings compared to the centre of the tunnel, where the turbulence intensity was approximately 1% as reported in section 4.1.1. The turbulence intensity for the velocity component in the flow direction is determined as:

\[ I_u = \frac{\sigma_u}{U} \]  

(4.4)

where \( \sigma_u \) is the standard deviation and \( U \) is the mean value of the velocity of the axial flow.

Thus, besides a turbulence profile along the model axis, additional measurements were performed in the flow direction along each of the panel openings. This was done to investigate how the flow changed in the flow direction at those particular locations. Furthermore, the model could be exposed to different flow conditions depending on its position, i.e. the displacement due to drag, through the panel opening.

A Cobra Probe, which is a four-hole pressure probe, was used to determine the velocities in the three orthogonal components: axial (U), lateral (V) and vertical (W) for three different fan revolutions, namely 500, 900 and 1300RPM, which represent the velocity range of the wind tunnel (0 – 1500RPM). The probe works in a range 2 – 100m/s with an acceptance cone of ±45°, cf. (Cobra Probe, 2011).

4.1.7.1 Velocity Profile at Model Position

The velocity profiles were determined at the position of the model, meaning that 37 measurement points along the inclined-yawed line simulating the model axis from one panel opening to the other one were sampled with a sampling time of 60s and a sampling frequency of 2048Hz. In Figure 4.20 it is shown how the Cobra Probe was placed along a twine indicating the model axis. The twine was removed during the measurements.

The velocity profiles for the mean values \( U \) for each velocity are shown in Figure 4.21. Vertical black lines denote the wind tunnel test section limits. The profiles are enlarged at the boundaries, where the measurement points were positioned closer to each other to describe the effect at the boundaries more detailed. Note that the velocity did not reach zero at the boundary of the wind tunnel, but a positive mean velocity.
was still registered until approximately 50mm further outside the tunnel. This also defined the effective width of the test section, $l_{vel} = 2.1\text{m}$, which was used to determine flow dependent quantities such as force coefficients. Furthermore, velocities lower than $2\text{m/s}$ were not reliable values due to the range of the Cobra Probe, but are included in Figure 4.21 to give an idea of the zero velocity position.

At 7 selected points - marked with stars in the figures - the sample time was increased up to 180s for more extensive statistical studies of the flow, e.g. for establishing spectra, etc. It was chosen not to investigate this further in the project, but the data are available for future use.

In order to determine one reference value for the flow velocity at the model position at a specific $RPM$, an integration of the velocity profile was performed dividing the result with the effective length, $l_{vel} = 2.1\text{m}$. 
4.1.7.2 Correction Factors

Given that flow measurements were carried out using a Cobra Probe and not a Pitot tube, which was the reference instrument, a correction factor, $\alpha_{\text{inst}}$, was determined. The factor linked the velocity measured by the Cobra Probe, $U_{\text{cobra,ref}}$, with the velocity measured by the Pitot tube, $U_{\text{pitot,ref}}$, at the same position. In this way the direct ratio between the readings of the two instruments was found. Two repetitions for the three fan revolutions were performed to determine this link. First with the Cobra Probe placed 100 mm to one side and then placed 100 mm to the opposite side of the Pitot tube, as shown in Figure 4.22.

![Figure 4.22: Velocity measurements at reference position with Cobra Probe and Pitot tube](image)

The correction factor, $\alpha_{\text{inst}}$, between the two instruments was determined from linear regression of the data from the velocity determined by the Pitot tube and the velocity determined by the Cobra Probe, as illustrated in Figure 4.23(a).

Likewise, the correction factor, $\alpha_{\text{pos}}$, for the position was determined from linear regression, cf. Figure 4.23(b), of the data from the velocity determined by the Pitot tube at the reference position and a corrected velocity for the velocity at the model position determined as $U_{\text{pitot,model}} = \alpha_{\text{inst}} \cdot U_{\text{cobra,model}}$.

The Regression Through the Origin (RTO) method was used to perform the data point interpolation. It is derived from the common ordinary least-squares (OLS) regression method, cf. (Eisenhauer, 2008), which is often used in statistics. RTO was chosen because the origin was assumed to be part of the set of measurements data, i.e. both instruments should output zero in still air. The regression was computed according to:

$$\alpha = \frac{\sum x_i \cdot y_i}{\sum x_i^2}$$

where $\alpha$ is the estimated slope of the regression line, $x_i$ and $y_i$ are abscissa and ordinate of the $i^{th}$ data point, respectively.

The correction factors corresponding to the inclinations of the regression curves were
determined to:

\[ \alpha_{inst} = \frac{U_{cobra,ref}}{U_{pitot,ref}} = 1.012 \quad (4.6) \]
\[ \alpha_{pos} = \frac{U_{pitot,model}}{U_{pitot,ref}} = 0.962 \quad (4.7) \]

This meant that the Cobra Probe registered slightly higher velocities than the Pitot tube, and that the velocity at the model was lower than at the reference position. As mentioned, \( \alpha_{pos} \) also accounted for the decrease in velocity at the boundaries, which were clearly testified in the velocity profiles.

![Scaling factor due to instruments](image1.png)

![Scaling factor due to position](image2.png)

**Figure 4.23:** Linear regression for velocity measurements at reference position

### 4.1.7.3 Turbulence Profile at Model Position

The standard deviation of the velocity was determined and is given in Figure 4.24. It shows a clear increase in the fluctuating component closer to the panel openings. The standard deviation was decreasing outside the tunnel boundaries.

![Standard deviation of velocity](image3.png)

**Figure 4.24:** Standard deviation of velocity \( u(t) \) for 500, 900 and 1300RPM along the model axis
The turbulence intensity determined from (4.4) and illustrated in Figure 4.25, was 0.6 – 0.7% at the centre line. At the panel openings it was clearly amplified reaching 33 – 34% crossing the left boundary and 25 – 28% at the right boundary.

4.1.7.4 Flow Profiles at Panel Openings

To investigate how the velocity and turbulence was changing in the flow direction close to the wind tunnel boundaries, yet another 21 samples were performed at each of the panel openings. The wall openings were approximately 90cm by 90cm in size, cf. Figure 4.20. The sampling time was selected to 120s and the sampling frequency to 2048Hz. The measurements were performed inside the test section along a straight line 30mm from the wind tunnel walls in the along wind direction. Figure 4.25 shows pictures of the Cobra Probe positioned at the two panel opening.

The velocity profiles for the upper end and the lower end side are shown in Figure 4.27(a) and (b), respectively. The magnitudes differed slightly between the two sides, but this may not only be due to differences in the flow, but also to small imprecisions in positioning the Cobra Probe at the same distance to the boundary. As illustrated in Figure 4.21 the velocity decreased significantly within few millimetres.
Either way, the more interesting observation was the variation in velocity along the panel opening. From Figure 4.27 it is clear that the velocity was decreased along the opening in the flow direction, i.e. the model was affected differently, when it was positioned further downstream. This was for instance the case, when the drag force became larger and the model moved downstream.

![Graph showing velocity variation](image)

**Figure 4.27:** Mean velocity for 500, 900 and 1300 RPM at panel openings with the wind coming from the left

The turbulence intensity was increasing along the opening. This is shown in Figure 4.28. Vertical black lines denote the opening limits. The turbulence intensity was around three times higher in the last part of the opening compared to the first one, hence the model was exposed to a much more turbulent flow, when it was positioned further downstream.

![Graph showing turbulence intensity variation](image)

**Figure 4.28:** Turbulence intensity for 500, 900 and 1300 RPM at panel openings with the wind coming from the left

The observations made above showed that the changes in velocity and turbulence differed from the ordinary flow profiles for a wind tunnel with a completely closed test section, cf. Appendix C.
4.1.8 Data Acquisition

A fundamental aspect in conducting experimental tests is the data acquisition system, which records, collects, converts and stores signals from the various instruments. It has to be robust and reliable in order to translate the exact phenomena occurring on the model into treatable data and keep the noise coming from the electric network supplying the system, wiring and instrument interferences to a level on minimum. The systems here adopted included several components which are presented and described in the next paragraphs.

Displacements time-histories of the twin-cable model were recorded by four laser displacement transducers fixed to the rig structure. Detection of the laser spot was guaranteed through plexiglass plates with a white reflective plastic covering orthogonal to laser bins fixed at the two end connections as previously described in 4.1.3.3. The two plates on each side were perpendicular to each other so that vibrations in the two main planes could be recorded by the two relative transducers. The used lasers, produced by WayCon Positionsmesstechnik (2012) were LAS-T-250 for the in-plane vibrations and LAS-T-500 for the out-of-plane. The difference between the two sensors was the recording range and the resolution: 50 – 300\,mm and 0.02 – 0.35\,mm for the first one and 100 – 600\,mm and 0.03 – 0.6\,mm for the second one. Despite small differences in the resolution, recordings from the two laser types could be treated in the same way. In fact, resolution discrepancies were not relevant compared to the vibration amplitude involved which were one-two orders of magnitude larger. Recording range was selected of two different types because of the expected larger displacement amplitude in the out-of-plane direction due to the increasing drag while augmenting the wind speed. Wiring for the four lasers was constitute by a cable per instrument which was split in two parts at one end; one attached to the power supply unit and one connected to the recording unit through a BNC plug as illustrated in Figure 4.29.

On the inclined set-ups where the rivulet formation could be effectively reproduced, the steel chain supporting the model weight in the axial direction was integrated with a force transducers with four strain gauges configured as a full bridge, to get information about the actual tension force on the cable i.e. weight of the model and its difference between dry and wet conditions. The log transducer was connected to the registering unit with an electric wire with ethernet connection, cf. Figure 4.29. Because of the high resolution of the force transducer the reference force value registered by the instrument with the model standing still at zero wind velocity was fluctuating from day to day or even within the same day due of the changing room temperature. Thus, data from the force transducer were not considered reliable for the prefixed goal.

Both lasers and force transducer were connected to a common docking unit via 'National Instruments' modules, namely a NI9215 with BNC for the four lasers and a NI9237 supporting ethernet plugs for the force transducer. Another module NI9215 with BNC was attached as well and received the inputs from reference instrumentation i.e. temperature, relative humidity and pressure data.

The docking station was a compact DAQ unit, the NiCDAQ-9172, by 'National Instruments'. It supported input from eight modules. In this case just the first three were occupied by the module with reference data, the module connected to the lasers and the one linked to the force transducer, in the order. Figure 4.29 shows the docking unit with the three connected modules. The dock was simply plugged to the electric
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Input from the instruments, converging into the docking unit, were managed and stored on a hard drive through a specially designed programme in a 'LabVIEW' environment (short for Laboratory Virtual Instrumentation Engineering Workbench) developed by 'National Instruments' too. The programme was built up with a graphical language and it organized the receiving data in voltage from the instruments. Data were registered simultaneously from each connected instrument and arranged in such a way that a time history file was produced every time the system was registering. A summary file was also produced; it contained a list of averaged outputs from every time history-run. Numbers registered in the time history files and summary files were not volt, but converted into the proper unit by the conversion parameters implemented in the 'Labview' programme. Conversion parameters were found for every instrument via calibration as reported in Appendix D. The list of parameters stored by the 'LabVIEW' routine are shown in Table 4.4.

<table>
<thead>
<tr>
<th>Recorded parameters</th>
</tr>
</thead>
<tbody>
<tr>
<td>Run/Time V R H R H L Δmass P14 P15 T RH</td>
</tr>
<tr>
<td>unit - / mm mm mm mm mm kg Pa Pa °C %</td>
</tr>
</tbody>
</table>

The programme named 'Laser_v1.3.vi' also managed the control of the logging. From its main control window it was possible to launch a run, stop it and have a graphical presentation of the recording signals on real time. This was particularly useful when conducting the tests because any potential deviation in the recording was immediately visible. Figure 4.30 reports a screen-shot of the user friendly control window of the 'LabVIEW' routine.
4.1.9 Test Programme

Several experimental tests in the inclined-yawed set-up, cf. section 4.1.5, were conducted in CWT located at 'FORCE Technology' in order to reproduce the critical condition of full-scale cables, obtained from the analysis of monitoring data, as described earlier in sections 3.2 and 3.3. Therefore a test programme was built to define, prioritize and organize the tests to be performed, according to the time schedule of the wind tunnel. The programme is pictured in Table 4.5. The condition referred to the presence of simulated rain together with the wind as external action on the twin-cable model. Test activities accounted also for the time necessary for the test preparation i.e. adjustment of dynamic rig and rain simulation system, and calibration of the set-ups. The latter was a delicate phase which took time but was essential for the subsequent data processing. That phase included the weighting of the model and the relative springs, calibration of springs, lasers displacement transducer, determination of model stiffness and frequencies in the two main orthogonal directions. Performed calibrations are further detailed in Annex E.

4.2 Test Performance

Dynamic tests at CWT were performed following a specific procedure figured out during the previous test attempts. The latter were discovered of major importance because they allowed a better understanding of the practical issues and limitations to deal with when conducting the actual tests on the inclined-yawed set-up. Hereinafter this procedure is described in details.

The first step was the installation of the rig, of the model and the adjustment of the springs. Spring length was determined in order to get the requested stiffness and therefore model frequencies. Springs were allocated in a cross configuration, in-plane and out-of-plane, and pre-pulled in order to allow for the static displacement, i.e.
4.2. Test Performance

Table 4.5: Test programme for experimental campaign at CWT

<table>
<thead>
<tr>
<th>Activity</th>
<th>Condition</th>
<th>Day</th>
</tr>
</thead>
<tbody>
<tr>
<td>Set-up preparation</td>
<td>Dry</td>
<td>May 25\textsuperscript{th} 2012</td>
</tr>
<tr>
<td>Set-up preparation</td>
<td>Dry</td>
<td>May 26\textsuperscript{th} 2012</td>
</tr>
<tr>
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<td>May 27\textsuperscript{th} 2012</td>
</tr>
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<td>May 28\textsuperscript{th} 2012</td>
</tr>
<tr>
<td>Dynamic test 850-1500 RPM</td>
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<td>May 29\textsuperscript{th} 2012</td>
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<td>May 31\textsuperscript{st} 2012</td>
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<tr>
<td>Dynamic test 0-900 RPM</td>
<td>Wet</td>
<td>June 1\textsuperscript{st} 2012</td>
</tr>
<tr>
<td>Dynamic test 900-1500 RPM</td>
<td>Wet</td>
<td>June 2\textsuperscript{nd} 2012</td>
</tr>
<tr>
<td>Photographic documentation</td>
<td>Dry/Wet</td>
<td>June 3\textsuperscript{rd} 2012</td>
</tr>
<tr>
<td>Flow visualization</td>
<td>Dry</td>
<td>June 4\textsuperscript{th} 2012</td>
</tr>
</tbody>
</table>

static drag and lift components. It was discovered of vital importance to keep the in-plane springs and the axial chain preventing the axial translation of the model always aligned with the cable longitudinal axis. That was because of the necessity of maintaining the motion of the model decoupled in the two allowed degrees of freedom. This consideration led to the need of moving the upper anchorage of the in-plane springs and the axial chain every step in speed, because of the changing drag static component. The adjustment of the springs was obtained by shifting the upper hook onto a special designed fitting, cf. Figure 4.10(b). The position of the axial chain was instead adapted by sliding its end fitting onto an aluminium rail, cf. Figure 4.15(a). Naturally it was difficult to ensure for all velocities the same alignment of the mentioned set-up components to the model axis, because of a pre-drilled hook positions on the spring fitting with a spacing of about 8mm.

It was decided to perform three sweeps from zero to maximum speed. The first one was the registration for 180s of the so called ‘free motion’ of the model excited only by the wind action. This sweep was used to identify the behaviour of the twin cables for different velocities and determine the amplitude of the vibrations due only to the gustiness of the wind for different velocities. The second and the third sweeps were instead performed introducing to the model an external manual excitation to empathize the in-plane and out-of-plane responses respectively. These two sweeps were used for the determination of the aerodynamic damping of the model of the two stays. For every step in velocity the external excitation was applied five times registering the response for 60s. Several repetitions were performed to check the repeatability of the results and to allow for statistical treatment of the acquired data and thus better the quality of the final results leading to a description of the phenomena which was closer to reality. Practically the 60s log was initiated just before the application of the external excitation in order to register it all. This solution was found to be important e.g. at high speed where small amplitude external excitation due to geometry issues and high level of damping were producing short time record applicable for damping determination, cf. section 4.3 for further details. Regarding the external manual excitation it is worth it to state that it was mainly adopted to facilitate the practical test performance. The apparent not reliability of this methodology compared to e.g. mechanical external excitation was disproved by the data processing.
4.2. Test Performance

on the time histories which showed a uniform excitation between the two model ends for all test repetitions. Furthermore this manual technique is successfully actuated in practice as reported by de Sá Caetano (2007, p. 182). Obviously, externally disturbed excitations were disregarded in the data processing and substitute by extra records acquired during the testing phase.

In conducting wet tests the three sweeps were combined together as a unique sweep in velocity from 0 to maximum speed. At each step in velocity the three excitations - free, in-plane and out-of-plane - were registered with the same logging time and sample frequency as for dry tests. The choice of this alternative procedure was induced by the difficult and time consuming adjustments of the rain simulation system. Therefore for a specific velocity all tests logging were performed after the proper configuration of the water system, avoiding possible uneven rain condition from the repetition of the three sweeps at different times.

Due to restriction in space i.e. limitation in the maximum length of the horizontal rig arms carrying the out-of-plane springs, the latter had consequently a confined pre-tensioning range. This fact had rendered the initial pre-tensioning of the springs not sufficient to cope with the maximum in-wind static displacement. Therefore at certain velocities in the speed sweep it was necessary to interrupt the tests and made a modification in the geometrical configuration of the horizontal springs. Anchor fittings of the spring were slid against the wind direction along the arms and the pre-tensioning of the springs increased thanks to the new space generated along the arms by the sliding operation. Of course at zero velocity the springs were not perpendicular to each other any more but once the last tested velocity was re-reached the orthogonality between the springs was re-established due to the static drag; it was then possible to proceed the tests increasing the speed further. For dry tests it was necessary to shift the model two times after 850RPM (about 16.9m/s) and 1100RPM (about 21.8m/s). For wet tests the model was shifted again two times to reach the full speed after 900RPM (about 17.8m/s) and 1200RPM (about 23.8m/s). Shift of the twin-cable model in wet tests occurred at higher velocities than in dry tests because of the reduced drag static component in the rain-simulated condition, cf. Figure 4.34 in section 4.4.1.

4.2.1 Sample Rate

It is well known that a continuous signal can be represented by a sequence of discrete values, through the process of periodic sampling. The discrete data points are exact values of the continuous sequence, but in order to preserve its information content they have to be sampled with a proper rate, $f_s$; therefore particular emphasis was put into a selection of an appropriate sampling frequency. Distinctive feature of discrete sequences is the signal ambiguity in the frequency domain, cf. (Inman, 2009; Lyons, 2001) so that there is a periodic replica of the continuous signal spectrum with every repetition centred at $n \cdot f_s$, where $n$ is any integer number. When those replica are intersected to each other ‘aliasing’ occurs. This problem can be avoided by keeping the sampling rate $f_s > 2 \cdot f_{max}$ with $f_{max}$ being the maximum frequency of the band of interest. The previous relationship is known as the ‘Nyquist’ criterion. This is a fundamental principle in signal processing and all optimization criteria to estimate the best $f_s$ for a certain sampling operation are derived from it, cf. (Lyons, 2001).
4.3. Data Processing

For the purpose of the thesis, the frequency band of interest was very narrow (about 0.2 Hz) and centred about 0.861 – 0.857 Hz for in-plane and out-of-plane directions respectively. Therefore $f_s$ could have been set e.g. at some ten Hz. Nevertheless, in order to have a sufficiently fine definition of the responses in the time domain, the sample rate was set equal to $f_s = 512$ Hz.

The sampling time was chosen to keep a high frequency resolution for a better description of the frequency content around the natural frequencies of the model, i.e. band of interest. The best frequency resolution is achievable when all sampled data are considered in calculating the Fourier transform. In this case the frequency resolution is defined as:

$$f_{res} = \frac{f_s}{N} = \frac{f_s}{T_{tot} \cdot f_s} = \frac{1}{T_{tot}}$$  \hspace{1cm} (4.8)

where $N$ is the total number of sampled data obtained as the logging time $T_{tot}$ times the sample rate $f_s$. Therefore the logging time for in-plane and out-of-plane excitation tests was set to $T_{tot} = 60s$ leading to a frequency resolution of $f_{res} = 0.0167$ Hz.

4.3 Data Processing

The processing of the data, which were obtained from the passive-dynamic wind tunnel tests, is described in this section. Output data from the acquisition system mainly referred to displacement time histories apart from the indispensable reference data such as temperature, relative humidity and differential dynamic pressure. The displacement data were converted from volt to millimetre already by the 'LabView' routine 'Laser_v1.3.vi' where the calibration constants of the lasers were inserted in the acquisition programme, cf. appendix D.

For both dry and wet tests the same processing procedure was undertaken. Firstly data from the 'free motion' records were analysed. Secondly data from in-plane and out-of-plane excitation were considered. Reference data from the 'free motion' loggings were calculated accounting for the zero reading at zero wind velocity. Mean displacements i.e. static components of every displacement response, were determined afterwards for every single speed, leading to the construction of static displacement vectors as function of $Re$. A filter was applied to the displacement time histories and their standard deviation determined after the performance of spectral analysis. Data from in-plane and out-of-planeexcitations were treated in a similar way compare to the ones from 'free motion'. The filtered time histories instead, were used here for the determination of damping. A logarithmic fitting was applied to the envelope function of each displacement time history within a specifically determined range. Damping was then calculated via the so called 'free-vibration decay method', cf. section 2.4.3. After working on every single time history static displacement vectors and total damping vectors as a function of $Re$ were constructed for the 'free motion' recording and the five repetitions of in-plane and out-of-plane excitations. Displacements allowed calculation of forces and force coefficients while aerodynamic damping was isolated as a contribution to the total one, from both dry and wet test conditions. The following parts describe more in depth the various steps of the data processing just illustrated.
4.3 Data Processing

4.3.1 Reference Data

The first step in the data processing was the evaluation of the so called 'reference data', performed for each of the three excitation mode i.e. free, in-plane and out-of-plane. This processing step was devoted to the determination of the parameters as a function of which, force coefficients and damping would be calculated. Input data were atmospheric pressure \( p_{\text{atm}} \) registered day by day from an analog barometer located close to the CWT at 'FORCE Technology', temperature \( T \) and relative humidity \( RH \) of the test chamber recorded by two dedicated probes positioned at the ceiling of the wind tunnel 1.2m downstream the twin-cable model position. The last input parameter was the differential dynamic pressure \( P_{14} \) - between inside and outside the test section - which was determined through a Pitot tube placed at the tunnel ceiling 3.8m upstream the twin-cable model. \( T, RH \) and \( P_{14} \) were recorded with the same \( f_s \) as that of laser displacement transducers by the acquisition system as reported in section 4.1.8.

The vapour pressure and the actual vapour pressure were calculated by (4.9) and (4.10) respectively with \( c_0 = 6.1078 \), \( c_1 = 7.5 \) and \( c_2 = 237.3 \).

\[
E_s = c_0 \cdot 10^{c_1 \frac{T}{273.15}} \quad (4.9)
\]

\[
p_v = E_s \cdot \frac{RH}{100} \quad (4.10)
\]

Air density could then be determined from the atmospheric pressure and \( p_v \) according to:

\[
\rho = \frac{p_{\text{atm}} \cdot 100}{287.05 \cdot (T + 273.15)} \cdot \left( 1 - \frac{0.378 \cdot p_v \cdot 100}{p_{\text{atm}} \cdot 100} \right) \quad (4.11)
\]

The flow velocity was calculated reversing the classical definition of dynamic pressure in:

\[
P_{14} = \frac{1}{2} \cdot \rho \cdot U^2 \quad (4.12)
\]

\[
U = \sqrt{\frac{2 \cdot P_{14}}{\rho}} \quad (4.13)
\]

Kinematic viscosity of the air was defined according to:

\[
\nu = -1.1555 \times 10^{-14} \cdot (T + 273.15)^3 + 9.5728 \times 10^{-11} \cdot (T + 273.15)^2 + 3.7604 \times 10^{-8} \cdot (T + 273.15) - 3.4484 \times 10^{-6} \quad (4.14)
\]

(4.14) led to the definition of the Reynolds number, already presented in (2.1). It is important to state and clarify which was the body characteristic dimension for the definition of \( Re \). From here on it was set to be equal to \( D \), being the diameter of one single cylinder of the two in the twin-cable model, cf. e.g. (Alam et al., 2003; Huera-Huarte and Gharib, 2011; Sumner, 2010; Zdravkovich, 2003). It is important to underline the necessity to use \( Re \) as a reference parameter when presenting results from wind tunnel test like aerodynamic damping and force coefficients as in the present work. The dimensionless parameter \( Re \) allows for comparison between results from different test campaigns and field/full-scale observations, accounting for potential differences in model sizes and range of tested velocities.
4.3. Data Processing

Note that the wind velocity in (4.13) determined from every test repetition, was corrected with the factor $\alpha_{\text{pos}}$ for the calculation of $Re$ and later for the determination of the force coefficients, cf. section 4.3.4. $\alpha_{\text{pos}}$ defined in cf. section 4.1.7.2 accounts for the difference between the Pitot tube reading and the actual wind velocity experienced by the model determined via Cobra Probe measurements, cf. section 4.1.7.

Finally the dynamic viscosity of the air was defined as:

$$\mu = \nu \cdot \rho \quad (4.15)$$

4.3.2 Spectral Analysis

Spectral analysis of the recorded time series was the second performed step in the data processing. The purpose of this analysis was the evaluation of the frequency content of the logged signal. Representation of the latter in frequency domain shows the distribution of the energy from the load i.e. wind and external excitations, in terms of model displacement response at different frequencies. In other words the frequency domain shows the spectral component or frequency content of the signal i.e. model displacement response, cf. (Brandt, 2011).

Assuming a linear system the frequency components of the measured signal, represented by a non-periodic function, can be decomposed into a series of harmonic functions or Fourier series as shown in:

$$x(t) = a_0 + \sum_{n=1}^{\infty} a_n \cdot \cos(n2\pi f_0 t) + \sum_{n=1}^{\infty} b_n \cdot \sin(n2\pi f_0 t) \quad (4.16)$$

where

$$T_0 = 1/f_0$$

$$a_0 = \frac{1}{T_0} \cdot \int_{t_0}^{t_0+T_0} x(t) \, dt$$

$$a_n = \frac{2}{T_0} \cdot \int_{t_0}^{t_0+T_0} x(t) \cdot \cos(n2\pi f_0 t) \, dt, \quad n = 1, 2, ...$$

$$b_n = \frac{2}{T_0} \cdot \int_{t_0}^{t_0+T_0} x(t) \cdot \sin(n2\pi f_0 t) \, dt, \quad n = 1, 2, ...$$

The Fourier transform is defined in exponential or Euler form as:

$$X(f) = \mathcal{F} [x(t)] = \int_{-\infty}^{+\infty} x(t) e^{-i2\pi f_0 t} \, dt \quad (4.17)$$

The Fourier transform of the function representing the signal contains all same information as the original function, but in the frequency domain instead of time domain. This means that the original function can be completely reconstructed by the inverse Fourier transform defined as:

$$x(t) = \mathcal{F}^{-1} [X(f)] = \int_{-\infty}^{+\infty} X(f) e^{i2\pi f_0 t} \, df \quad (4.18)$$
4.3. Data Processing

To determine the Fourier transform of the measured signal the built-in MatLab function 'fft' (Fast Fourier Transform, FFT) was used. It returns the discrete Fourier transform computed with a fast Fourier transform algorithm.

A frequency spectrum consists of the collection of the peak responses of each harmonic function of the signal (magnitude of corresponding harmonic function) as a function of the frequency indeed. Figure 4.31 shows a recorded displacement time history and its spectrum. The two figures represent the same signal, in this case it is the 3rd repetition record of the in-plane model excitation at 800RMP - about 15.9m/s in dry condition.

![Displacement response of the model to in-plane excitation represented in time and frequency domain](image)

**Figure 4.31:** Displacement response of the model to in-plane excitation represented in time and frequency domain

The spectrum plotted in Figure 4.31(b) indicates that the model was responding to the external excitation with a very narrow frequency range around $f_0$ i.e. the model was mainly excited in one mode only. The calculated peak in the spectrum corresponding to the damped frequency of the system, $f_{\text{peak}} = 0.86669\,\text{Hz}$ was close to the natural frequency of the model found to be $f_0 = 0.861\,\text{Hz}$ for the in-plane direction. The discrepancy between the two values was due to the frequency resolution, the fact that treated signals are by nature discrete and the effect of damping. The spectrum also shows how other frequencies than those around $f_0$ were part of the original recorded signal. This is an undesired part of the signal, often called noise, which is present in the original response. The treatment of signal noise will be presented in section 4.3.3.

The spectrum is limited to a frequency range between $0 - 5\,\text{Hz}$ where the largest responses were registered. Additionally the spectrum is symmetric about the Nyquist frequency, $f_{\text{Ny}} = f_s/2 = 256\,\text{Hz}$. It means that mirrored peaks are found close to the end of the complete frequency range i.e. $0 - f_s$.

The peaks in the spectrum indicate the harmonic functions with the highest response and thereby where the energy of the vibrations is concentrated. Usually the power - energy per time - spectrum is used. It shows how the power of the signal is distributed within given frequency bins. The power of a signal is proportional to its amplitude squared as indicated in:

$$x_{\text{pwr}}(n) \propto x_{\text{amp}}(n)^2 \quad (4.19)$$

Because signals with smaller differences in amplitude have larger differences in their
4.3. Data Processing

relative powers, it is easier to recognize natural modes from the noise, i.e. peaks are more evident, cf. (Lyons, 2001). However, in case of the spectrum shown in Figure 4.31(b) the secondary axis represents the amplitude of the harmonic function. In this way it is easier to relate the frequency spectrum to the relative filtered time history representing only one or a narrow band of frequencies. In other words the relation between frequency and time domains is directly established by means of amplitude magnitude.

4.3.3 Filtering

In order to isolate the desired frequency content corresponding to the natural frequency of the model in the excited direction, it was necessary to clean up the signal through filtering. Filtering is one of the main and powerful tools in the signal processing field. Numerous filtering techniques and algorithms are developed and available. Examples of the most common and used filters are Chebyshev type I, type II and Butterworth filters to name a few. They can all be both infinite impulse response (IIR) or finite impulse response (FIR) filters, cf. (Lyons, 2001). The here most interesting characteristic of filters is their definition in terms of frequency pass. Low pass filters leave the signal undisturbed below a defined frequency value, while high pass filters work the way around. The combination of the two is a band pass filter where a specific frequency range is selected from the original spectrum. The cut-off is the gap in the frequency axis between the untreated signal and the filtered one. It can be more or less wide depending on the filter algorithm.

Another characteristic of filters is that they act on a digital spectrum of a discrete signal which images the original continuous signal. Discrete spectrum of signals are by themselves periodic and thus defined between $\pm \infty$ even though the discrete signal in the time domain can be finite in time. Therefore the application of digital filters always generates some discrepancies between original and filtered signals at the extremes of the time domain. That is why, in the testing phase, the logging of the model response was initiated slightly before the manual excitation, in order not to lose any information about the damping of the signal which was, once filtered, deformed at the limits of the logging time window.

Eventually, in the developed data processing an alternative filtering approach was chosen, as suggested by van Gils Hansen (2008), in order to simplify the design of the filter itself and better control its effects on the filtered time histories. The frequencies of the undesired part of the signal were simply set to an extremely low value - here set equal to $1 \times 10^{-10}$ - leaving a narrow undisturbed frequency range between the cut-off frequencies. Note that the Fourier transform of the signal is mirrored around the Nyquist frequency as stated in section 4.3.2, meaning that the filter-band was applied symmetrically about the centre of the frequency range. The application of this filtering technique is illustrated in Figure 4.32(b), where the filtered frequency spectrum is compared to the original spectrum. It is evident that the peak was left undisturbed, while the rest of the signal was filtered out. The lower and upper cut-off frequencies were in this case defined as $f_{\text{peak}} \pm 0.15Hz$ and likewise for the mirrored part of the frequency range. $f_{\text{peak}}$ was set equal to $f_i$ leading to frequencies range of interest of $0.711 - 1.011Hz$ and $0.707 - 1.007$ for the in-plane and out-of plane degrees of freedom respectively.
4.3. Data Processing

The filtered time history of the displacement was determined by applying the inverse of the Fourier transform. This was done by applying the MatLab function 'ifft' on the filtered Fourier transform analogous to 'fft'.

The function returned the filtered time history of the displacement, which is shown in Figure 4.32(a). By comparison with the original time history in Figure 4.31(a) and back-plotted in Figure 4.32(a) it is seen that isolated and non-symmetric amplitude peaks were filtered out because the overall signal was obtained by superposition of harmonic function with characteristic frequencies very close to each other.

\[
\begin{align*}
\text{(a) Signal in time domain} & \quad \text{(b) Signal in frequency domain}
\end{align*}
\]

\[
\begin{align*}
\text{Displacement, [mm]} & \\
\text{original in-plane time history} & \\
\text{filtered in-plane time history} & \\
\text{frequency, [Hz]} & \\
\text{Spectrum of original signal} & \\
\text{Spectrum of 1st mode} & \\
\text{frequency, [Hz]} & \\
\end{align*}
\]

**Figure 4.32:** Original and filtered displacement response of the model to in-plane excitation represented in time and frequency domain

4.3.4 Calculation of Force Coefficients

Force coefficients were the first output result from the data processing. Drag and lift mean components were determined for the three types of excitation i.e. free, in-plane and out-of-plane, and for the two tested weather conditions from the mean displacement vectors. The latter were multiplied by the respective modal stiffness, \( k \), depending on the considered direction as indicated in (4.20) to calculate the total drag and lift forces experienced by the model in the dynamic rig.

\[
\begin{align*}
F_D &= \bar{x} \cdot k_{out-of-plane} \quad (4.20a) \\
F_L &= \bar{y} \cdot k_{in-plane} \quad (4.20b)
\end{align*}
\]

where \( \bar{x} \) and \( \bar{y} \) are the mean displacements of the out-of-plane and in-plane displacement time histories respectively.

Consequently force coefficients were calculated from total forces according to:

\[
\begin{align*}
C_D &= \frac{F_D}{\frac{1}{2} \rho U^2 A_{ref}} \quad (4.21a) \\
C_L &= \frac{F_L}{\frac{1}{2} \rho U^2 A_{ref}} \quad (4.21b)
\end{align*}
\]
4.3. Data Processing

where $A_{ref}$ is the reference area of the body - area 'seen' by the wind - here calculated as $A_{ref} = l \cdot 2D$, $l$ being the actual length of the model hit by the wind, cf. (Blevins, 2001, p. 308). $l$ was calculated as $l = l_{rel} / (\sin(\beta) \cdot \cos(\theta)) = 2.46m$ where $l_{rel} = 2.1m$ was the effective width of the tunnel cross section used for the velocity profile calculations, cf. section 4.1.7.1. Note that here the reference width of the body was set equal to $2 \cdot D$, which is the same dimension used for the evaluation of the blockage coefficient $b_k$ introduced in section 4.1.3. Even though blockage coefficients for the dynamically tested geometrical configuration was $b_k = 2D \cdot w_{CWT} / \cos(\theta) / A_{CWT} = 12.6\%$, which is slightly above the suggested value for the applicability of common correction methods, cf. (ESDU 80024, 1980), the drag coefficient was blockage corrected with the 'Maskell III' method based on the theory of Maskell (1965). The implementation of the blockage correction is reported in Appendix G.

4.3.5 Calculation of Damping

Damping of the twin-cable model tested dynamically in CWT was determined in two specific orthogonal directions, defined in-plane and out-of-plane considering the model and not the wind direction as reference. Damping ratios were calculated separately for the two directions from the in-plane and out-of-plane model excitation test repetitions, for both dry and wet conditions. The damping estimation was based on the free-vibration decay method illustrated in section 2.4.3, applied on the filtered time histories, cf. section 4.3.3. The envelope of the filtered record was calculated by means of the Hilbert transform in (4.22). First, the MatLab function 'hilbert' was used to calculate the analytic signal. Second, the envelope was determined as the absolute value of the analytic signal, cf. (Brandt, 2011).

$$\mathcal{H}[x(t)] = \int_{-\infty}^{+\infty} \frac{x(u)}{\pi(t-u)} \, du = x(t) * \frac{1}{\pi t} \quad (4.22)$$

where * indicates convolution. The Hilbert transform is defined between $\pm \infty$ as indicated in (4.22) but computed on a finite period of time. As a consequence, computational errors are introduced at the extremes of the calculation window as for the Fourier transform. Values of the Hilbert transform at the limits of the time interval tend to infinite and therefore do not represent the envelope of the series.

The filtered records represented the response of one mode only, the one characterized by the narrow frequency range left undisturbed by the designed filter. The overall response consisted of two parts; the first one was dominated by the model transient response to the external manual excitation while in the second part the response due to the stochastic wind gustiness was dominating. The two parts are always present in the global response in total time window but one of the two may be more evident than the other in a specific time sub-range or in the total time range, depending on the velocity. As a matter of fact, damping was determined considering only the transient part of the response. The amplitude width of the signal within which the response was ruled by the turbulent component of the wind action was here called 'buffeting limit'. This buffeting limit was derived from the 'free motion' tests. The standard deviation or root mean square of each filtered time series, which was considered as a zero mean Gaussian process, was calculated according to:

$$\sigma = \sqrt{\frac{\sum_{i=1}^{N} (x_i - \bar{x})^2}{N}} \quad (4.23)$$
To account statistically for the occurrence of peaks in the time histories, the resonant peak factor, cf. (EN 1991-1-4, (B.4)), defined in (4.24) was used.

\[ g = \sqrt{2 \ln f_i \cdot T} + \frac{e}{\sqrt{2 \ln f_i \cdot T}} \]  

(4.24)

where \( e = 0.5772... \) is the Euler-Mascheroni constant, \( f_i \) is the frequency of the considered mode and \( T = 180 \) s was the period of observation. The 'buffeting limit' was defined as the peak factor of the process, cf. Floris and Iseppi (1998). It is defined in (4.25), and accounted for approximately 99.7%, because \( g \approx 3 \), of the sample population assuming a normal Gaussian distribution.

\[ G = g \cdot \sigma \]  

(4.25)

A representative time interval had to be defined from the 60 s time window of the filtered displacement series. The initial point, \( i_n \), of the interval was set equal to the time corresponding to the max positive amplitude plus one second. Computational error due to filtering and envelope calculation were avoided because the manually induced motion was generated few seconds after the log was initiated as reported in section 4.2. Max positive amplitude normally referred to the manually applied excitation. In few cases it corresponded to other excitation sources e.g. wake buffeting due to turbulent flow at the panel openings at high velocity, happening later in time after the manual excitation was damped out. In order not to consider other peaks than the one corresponding to the initial manual excitation, the initial time was restricted to be lower than 30 s. In cases where this condition was not respected no damping calculation was performed; hence the corresponding value was set in MatLab equal to 'NaN', i.e. not a number. The final point, \( j \), of the time interval used for damping calculation was set equal to 55 s or the time corresponding to the intersection point between the 'buffeting limit' function i.e. \( x(t) = G \) and the envelope of the time history, whichever was smaller. A precondition for the damping calculation was the condition for \( G \) to be less than the maximum of the envelope of the displacement record within the range \( i_n - 55 \) s. It means that any manually applied excitation smaller in amplitude compared to the response due to buffeting was not considered for damping calculation.

The envelope function within the time interval \( i_n - j \) was approximated with a natural logarithm function for a direct determination of the logarithmic decrement and then damping as reported in section 2.4.3. The logarithmic function was applied firstly to the entire \( i_n - j \) range and secondly to four equal subintervals each of them lasting a fourth of the original one, to check out the linearity or amplitude dependency of damping. Two values of damping were then considered: one from the 'full' time range interval and one from the 'segmented' time range interval. In particular, the second one was defined as the mean value between the four obtained from the log fit of each of the four subintervals. Figure 4.33 depicts the just described procedure for damping calculation. The shown time history corresponds to the third repetition of the in-plane model excitation at 800RMP - about 15.9m/s in dry condition.

This calculation procedure was applied to all time displacement histories, from all five repetitions. Selected time histories from the first repetition corresponding to 0 – 300 – 600 – 900 – 1200 – 1500RPM for the in-plane and out-of-plane directions, and for both dry and wet conditions are reported in appendix F. It contains plots of
the original and filtered histories and the application of the logarithmic fitting in the representative time interval.

Damping for a specific velocity or equivalently $Re$ was finally determined as the average between the values corresponding to the five repetitions disregarding the maximum and the minimum values due to the large statistical variability of estimated damping values.

### 4.3.5.1 Aerodynamic Damping

Finally the aerodynamic damping was determined according to (4.26) by subtracting the structural damping from the total damping calculated as indicated in section 4.3.5. The structural damping is determined using the same procedure but for time series at zero wind velocity.

$$\zeta_a = \zeta - \zeta_s \quad (4.26)$$

Note that the structural damping subtracted from the total to get the aerodynamic one was actually the damping of the dynamic rig itself. This damping has not to be confused with the structural damping of prototype cables. Anyway the intention here was not to reproduce the structural damping of the prototype stays, but instead to keep the 'structural' damping of the model low to emphasize the pure aerodynamic contribution.

### 4.4 Test Results

#### 4.4.1 Force Coefficients

Force coefficients obtained from the passive-dynamic wind tunnel tests on the inclined-yawed twin-cable model with double helical fillet, are reported in Figure 4.34. It con-
4.4. Test Results

tains a comparison between force coefficients from the two different tested conditions, dry and wet.

![Graphs showing force coefficients](image)

**Figure 4.34:** Force coefficients from dynamic tests of the inclined-yawed twin-cable model with double helical fillet

Looking at Figure 4.34(a), the two drag curves follow similar trends. They presented a constant value until the critical $Re$ range was reached, about $1.5 \times 10^5$ for the dry condition and $1.7 \times 10^5$ for the wet one, where they started to decrease. It is important to underline lower values of $C_D$ in the wet case. That was because, as reported by de Sá Caetano (2007, p. 39), the loss of symmetry of the cables cross section due to the presence of water rivulets causes a variation in the aerodynamic forces. Comparison with literature of the dry case validated the trustworthiness of outputs from dynamic tests. Zdravkovich (2003, p. 1025) shows that drag coefficient of side-by-side twin cylinders with a spacing ratio of approximately $2.6 - 2.7$ oscillates between around $1.2 - 1.3$ for $0.6 \times 10^5 < Re < 1.6 \times 10^5$. In (ESDU 84015, 1998) instead is reported that in the subcritical range i.e. $0.2 \times 10^5 < Re < 2 \times 10^5$, $C_D \approx 1.4$. No drag coefficients for wet cases are reported in the studied literature.

Considering Figure 4.34(b), the lift curve in the dry case stayed around zero value before an increasing took place at $Re \approx 1.5 \times 10^5$, for which the drag crisis started. The wet case behaved similarly increasing after $Re \approx 1.7 \times 10^5$. Note that lift force was defined positive upwards. The initial increase in the lift curve up to $C_L \approx 0.4$ in the wet test from negative values was linkable to the progressive increase in the amount of water on the model surfaces. In other words, in the low velocity range rivulets were increasing in size till their maximum occurring at approximately $6 - 8 m/s$ equivalent to $Re \approx 0.5 \times 10^5$. The drop down in lift from 0.4 to 0.15 occurring from $Re \approx 0.8 \times 10^5$ corresponding to about $11m/s$ was observed to happen in concurrence with the initiation of an increasingly oscillating motion of the rivulets on the cable surfaces, cf. Figure 4.37(b). The instantaneous change in their position could be attributed to be responsible for the changing lift at nearly constant drag values. Lift coefficients from passive-dynamic tests agreed well with the one found from literature. According to Zdravkovich (2003, p. 1025) the lift varies around $0.04 - 0.15$, while ESDU 84015 (1998) reports a variation between $0.04 - 0.08 \pm 0.05$ for the range $0.2 \times 10^5 < Re < 3 \times 10^5$. No lift coefficients for wet cases are reported in the studied literature.
4.4. Test Results

A larger variability of force coefficients between the three performed test repetitions was experienced for $Re < 0.5 \times 10^5$, as shown in Figure 4.34. Small displacements and velocities could be potentially the cause for large deviations in the force coefficients. Therefore, more test repetitions are necessary for a better estimation of $C_D$ and $C_L$ in that $Re$ range.

4.4.2 Aerodynamic Damping

In this section the main result of passive-dynamic tests is presented. It consisted of aerodynamic damping calculations, for the two tested conditions dry and wet, in two orthogonal directions defined out-of-plane and in-plane. The latter belonged to the plain containing the two cylinder axes, and the other one was perpendicular to it. Directions and reference plains were the same as for the calculation of force coefficients.

Figure 4.35, shows aerodynamic damping results in ‘scaled model’ order of magnitude. In the two sub-plots a comparison between the dry and the wet cases is reported for the out-of-plane and in-plane directions respectively. Extreme values obtained from the calculations are shaded in the plots. That is because they did not represent correctly the aerodynamic damping of the model. In fact, the turbulence intensity was increasing with the distance from the wall edge going along the opening in the wind direction, as presented in section 4.1.7 and shown in Figure 4.28. As a consequence, an increased uncontrolled motion of the model was observed when the model was crossing the wall openings far from the wall edges due to static displacement.

In Figure 4.35(a) the out-of-plane direction is considered. A quasi-uniform increasing trend until $Re \approx 1.5 \times 10^5$ is observed. Maximum value of $\zeta_a \approx 4.5\%$. After that point the drag crisis started, as shown previously in Figure 4.34(a). That clearly affected the aerodynamic damping which was reduced or at least did not increase any more. Unfortunately the quality of the aerodynamic damping data in the critical $Re$ range were not sufficient to depict a behaviour. On the other end, comparing the dry and the wet cases, the condition with simulated rain manifested a lower $\zeta_a$ in the full tested $Re$ range. The maximum value in the wet case was reduced to $\zeta_a \approx 3.6\%$.

![Figure 4.35](image-url)
4.4. Test Results

Looking at Figure 4.35(b) there is not a clear distinction between the dry and the wet cases. In-plane estimations of $\zeta_a$ were considered entirely reliable for the wet condition whilst for the dry one some data points had to be disregarded. The trend of the dry in-plane aerodynamic damping was then difficult to trace in the critical $Re$ range. In the wet case instead it was observed that after about $Re \approx 1.5 \times 10^5$ the damping did not increase further. For both the tested conditions maximum values were $\zeta_a \approx 2.5\%$. Finally, considering the sub-critical $Re$ range $\zeta_{a,\text{in-plane}}$ was approximately half of the value in the orthogonal direction, $\zeta_{a,\text{out-of-plane}}$, as normally described by theoretical models, cf. chapter 5.

4.4.3 Additional Results

Below additional results from the conduction of passive-dynamic tests in CWT are reported. They comprise additional information that can be used for later experimental campaigns in CWT or for further investigations on the same topic.

**Wind speed vs RPM**

An interesting ‘secondary’ result was the determination of the wind velocity against the fan revolutions per minute $RPM$ at the two different tested condition. It was known from previous experiment at the CWT that the wind speed as a function of the fan cycles assumes a linear trend. But this fact was not demonstrated for wet cases yet. Figure 4.36 shows the output of this investigation. The three test repetitions for both conditions are plotted and a liner regression with intercept set to zero was found. Results confirmed the thesis about the linearity of the velocity in the dry case. The same linear trend was observed in wet conditions. Furthermore the two conditions were seen to be identical, as expected. Therefore the calibrated Pitot tube could be said to be unaffected by changing environmental conditions in the test chamber.

![Figure 4.36: Wind speed vs fan revolutions per minute](image)

The relative error between the two interpolated curves, corresponding to the wind speed vs RPM function for the two cases was found to be:

$$\epsilon_{rel} = \frac{\Delta U_{wet} - \Delta U_{dry}}{\Delta U_{dry}} = -0.81\%$$
4.4. Test Results

Weight of the water

One of the original intentions for the experimental campaign was the estimation of the weight of the water distributed on the cable surfaces when testing with simulated rain. To this purpose, a force transducer with four strain gauges configured as a full bridge was installed as part of the acquisition system. As described in section 4.1.8, due to resolution issues it was not possible to utilize the gauge. The weight of the water, at zero velocity, was estimated based on an alternative and indirect approach accounting for the registered change in frequency between the dry and the wet case. The calculation procedure is illustrated in Appendix E.

The weight of the water for the in-plane and out-of-plane oscillation was found to be:

\[ M_{w(\text{in-plane})} = 0.478 \text{Kg}, \quad M_{w(\text{out-of-plane})} = 0.532 \text{Kg} \]

These data constitute a rough estimation of the weight of the water because rivulets were observed to change in size with the velocity, as documented in Figure 4.37. Rivulets were increasing until about 6–8 m/s, cf. Figure 4.37(a), due to the swollen rivulets, maintaining a constant value until approximately 22 m/s after which they were observed to start diminishing in size due to the large blowing force, cf. Figure 4.37(c).

\[ 250 \text{RPM} \approx 4.9 \text{m/s} \]
\[ 750 \text{RPM} \approx 14.9 \text{m/s} \]
\[ 1250 \text{RPM} \approx 24.9 \text{m/s} \]

Figure 4.37: Change in water rivulets size at different wind velocities during the conduction of wet passive-dynamic tests
Peak Acceleration

To ensure that the acceleration limit described in section 4.1.4.1 had not been exceeded, the peak acceleration was determined. Note that the peak acceleration should not exceed the component of the gravitational acceleration, which for \( \theta = 30^\circ \) is determined as:

\[
acc_{\text{lim}} = 9.80665 \text{m/s}^2 \cdot \cos \theta \approx 8.50 \text{m/s}^2
\]

The largest acceleration has been determined for the in-plane motion of the dynamic tests deriving the acceleration response from the displacement time series. In Figure 4.38 the displacement and acceleration response of the selected run is shown. The acceleration limit is marked with a dotted line to show, that the limit has not been exceeded.

If the limit were exceeded, which means that a maximum acceleration larger than the component of the gravitational acceleration occurred, it would have been necessary to use another in-plane spring, pointing downwards, in the dynamic rig.
4.4. Test Results
Chapter 5

Theoretical Models

The major part of the theoretical work in this project dealt with theoretical models to describe aerodynamic damping. The basic idea was to apply parameters such as force coefficients and geometrical configuration data as inclination, yaw and angle of attack to analytical models available in literature to predict the proneness to instability. In this chapter several models dealing with different vibration mechanisms are described, from classical instability criteria to newer formulations of unified models developed by Macdonald and Larose (2006, 2008a,b).

5.1 Aerodynamic Damping for Single-degree-of-freedom Systems

Models for prediction of instability of single-degree-of-freedom systems are presented in the subsequent sections, from simplified models considering one specific direction only, to a unified model accounting for an arbitrary vibrations direction.

5.1.1 Den Hartog Criterion

A criterion for galloping instability was the first instability criterion developed for cross-flow vibrations of 1-DoF systems. The criterion was first presented by Hartog (1932) to explain large vibrations of iced electricity transmission lines and later published in (Hartog, 1956), where the criterion was derived considering a bluff body, e.g. a cylindrical cable, exposed to a normal wind. It is assumed that the body has an initial downward directed motion, which means that the force acting on the body both has a drag and a lift component in the direction of the relative wind velocity and perpendicular to it, respectively, cf. Figure 5.1. The angle of attack $\alpha$ is defined as the angle between the wind and relative wind directions.

The total damping force $F_y$ - opposite oriented respect to the motion - is determined
5.1. Aerodynamic Damping for Single-degree-of-freedom Systems

Theoretical Models

Figure 5.1: Sketch of drag and lift components generated by the relative flow

in (5.1) as combination of drag and lift in the direction of the motion \( \dot{y} \).

\[
F_y = F_L \cos(\alpha) + F_D \sin(\alpha) \tag{5.1}
\]

The system is unstable when:

\[
\frac{\partial F_y}{\partial \alpha} < 0
\]

This means that the upward oriented force increases for a negative angle of attack and decreases for a positive angle of attack, i.e., an alternating force is acting on the body enhancing the motion. The differentiation of (5.1) with respect to \( \alpha \) leads to:

\[
\frac{\partial F_y}{\partial \alpha} = \frac{\partial F_L}{\partial \alpha} \cos \alpha - F_L \sin \alpha + \frac{\partial F_D}{\partial \alpha} \sin \alpha + F_D \cos \alpha
\]

\[
= \sin \alpha \left( -F_L + \frac{\partial F_D}{\partial \alpha} \right) + \cos \alpha \left( \frac{\partial F_L}{\partial \alpha} + F_D \right) \tag{5.2}
\]

For small values of \( \alpha \) the sine term can be neglected and the cosine term equals the unity. Furthermore, the forces \( F_i = \frac{1}{2} C_i \rho DU^2 \) can be expressed by means of force coefficients, leading to the well-known Den Hartog instability criterion in:

\[
C_D + \frac{\partial C_L}{\partial \alpha} < 0 \tag{5.3}
\]

where \( C_D \) and \( C_L \) are the drag and lift coefficients, respectively.

5.1.2 Conventional Quasi-Steady Aerodynamic Damping

Davenport (1963) derived expressions for the aerodynamic damping of a structure in terms of the logarithmic decrement for the \( n^{th} \) vibration mode. The structure was assumed to be essentially horizontal and with a uniform mass per unit length, vibrating in the wind.

Alternatively, an expression for conventional aerodynamic damping in the flow direction is derived here by considering a body exposed to a steady flow normal to the body in terms of damping ratio. The expression linking logarithmic decrement and damping ratio is (2.12).

The drag force exerted on the body, for \( \dot{x} \ll U \), is given by:

\[
F_D = C_D \frac{1}{2} \rho Dl(U - \dot{x})^2 \simeq C_D \frac{1}{2} \rho DlU^2 - C_D \rho DlU \dot{x} \tag{5.4}
\]
where $C_D$ is the drag coefficient, $D$ is a characteristic dimension of the body, $l$ is the length of the body, $\rho$ is the air density, $U$ is the mean flow velocity and $\dot{x}$ is the velocity of the body in the flow direction.

The second term in (5.4) represents the aerodynamic damping, which is seen by establishing the equation of motion (2.5). In this case the total damping term becomes:

$$c_\dot{x} = c_s \dot{x} + C_D \rho D l U \dot{x} = c_s \dot{x} + c_a \dot{x}$$  \hspace{1cm} (5.5)

where $c_s$ is the structural damping parameter and $c_a = C_D \rho D U$ is the aerodynamic damping parameter.

The aerodynamic damping ratio is determined from (2.7) by inserting $c_a$.

$$\zeta_a = \frac{c_a}{2 \sqrt{k_M}} = \frac{C_D \rho D l U}{4 \pi f_0 M} = \frac{C_D \rho D U}{4 \pi f_0 m}$$  \hspace{1cm} (5.6)

For vibrations in the cross-flow direction the aerodynamic damping is approximated to:

$$\zeta_{a,y} = \frac{\rho D U}{4 \pi f_n m} \frac{\partial C_L}{\partial \alpha}$$  \hspace{1cm} (5.7)

Note that these expressions only are valid for constant $C_D$ as function of $Re$, i.e. in the subcritical $Re$ range.

### 5.1.3 Drag Crisis

In the critical $Re$ range the drastic drop in $C_D$, known as drag crisis, may lead to unstable vibrations in the direction of the flow. An expression, equivalent to the Den Hartog galloping instability criterion, is given for the drag crisis:

$$2C_D + \frac{\partial C_D}{\partial Re} Re < 0$$  \hspace{1cm} (5.8)

This was proposed by Currie and Turnbull (1987) for marine piles as Macdonald and Larose (2006) report.

### 5.1.4 Unified 1-DoF Model

A unified 1-DoF model has been recently derived by Macdonald and Larose (2006) to determine the quasi-steady aerodynamic damping for a cylinder with arbitrary cross section in a steady flow. The model is valid for small amplitude vibrations in any plane, considering both inclination and yaw angle and includes as special cases, models such as Den Hartog galloping, Drag Crisis, conventional quasi-steady aerodynamic damping as well as dry inclined cable galloping.

The aerodynamic damping can be either positive or negative, cf. e.g. (Davenport, 1983). If negative, it indicates that sufficient counteracting structural damping is necessary to avoid instability.

Macdonald and Larose (2006) consider a cylinder experiencing a flow with velocity $U$ at an angle $\phi$ relative to the cylinder axis in the cable-wind plane, cf. Figure 3.4 in
section 3.2. Assuming an initial motion in the plane normal to the cable axis at angle \( \alpha \) to the cable-wind plane and with velocity \( v \), the magnitude of the relative velocity is:

\[
U_R = \sqrt{U^2 - 2Uv \sin \phi \cos \alpha + v^2}
\]  

(5.9)

The angle of attack, \( \alpha \), is defined as the angle between the normal direction of the wind and the motion of the cable. \( \alpha \) is defined positive clockwise. As an example, considering the twin-cable model tested dynamically in CWT, in the out-of-plane motion i.e. motion perpendicular to the twin-cable plane, the angle of attack is \( \alpha = 0^\circ \) for a normal flow. Likewise, considering the in-plane motion, i.e. motion in the twin-cable plane, \( \alpha = 270^\circ \) for a normal flow, cf. Figure 5.2.

![Figure 5.2: Definition sketch of angle of attack \( \alpha \) for (a) the in-plane motion and (b) the out-of-plane motion of the twin-cable model, with cable motion denoted by \( \hat{y} \) and \( \hat{x} \) respectively](image)

The angle between the drag force and the direction of the cylinder motion, i.e. the relative angle of attack, is given by:

\[
\alpha_R = \alpha + \alpha_D, \quad \alpha_D = \tan^{-1} \left( \frac{v \sin \alpha}{U \sin \phi - v \cos \alpha} \right)
\]  

(5.10)

The drag and lift forces, per unit length, generated by the relative wind flow are:

\[
F_D = \frac{1}{2} \rho U_R^2 D C_D
\]  

(5.11)

\[
F_L = \frac{1}{2} \rho U_R^2 D C_L
\]  

(5.12)

The component of the resultant force acting in the same direction as the velocity of the cylinder \( v \), is:

\[
F_v = \frac{1}{2} \rho U_R^2 D (C_D \cos(\alpha + \alpha_D) - C_L \sin(\alpha + \alpha_D))
\]  

(5.13)

The non-linear damping term, from the equation of motion of the system, cf. section 2.4.1 is a function of the velocity since it is based on the quasi-steady approach. The equivalent linear aerodynamic damping ratio for small amplitude vibrations in a given mode \( n \), is expressed by:

\[
\zeta_a = -\frac{1}{2\omega_n m} \left. \frac{dF_v}{dv} \right|_{v=0}
\]  

(5.14)

where \( m \) is the mass per unit length and \( \omega_n \) is the undamped natural angular frequency for \( n^{th} \) mode.
The linear aerodynamic damping ratio is obtained by substituting (5.13) into (5.14), noting that $U = U_R$ and $\alpha_D = 0$ for $v = 0$, from the Taylor series expansion of the resultant force $F_v$ in the cable motion direction approximated to the first order term. Assuming that the force coefficients are function of $Re_R$, $\alpha_R$ and $\phi_R$ only, the subscript $R$ may be dropped.

The final general expression for the aerodynamic damping, based on quasi-steady theory, for small amplitude vibrations in any given plane, derived in Appendix A of (Macdonald and Larose, 2006), becomes:

$$
\zeta_a = \frac{\mu Re}{4m\omega_n} \cos \alpha \left\{ \cos \alpha \left[ C_D \left( \sin \phi + \tan^2 \alpha \frac{\sin \phi}{\sin \phi} \right) + \frac{\partial C_D}{\partial Re} Re \sin \phi + \frac{\partial C_D}{\partial \phi} \cos \phi - \frac{\partial C_D}{\partial \alpha} \tan \alpha \frac{\sin \phi}{\sin \phi} \right] \right. \\
- \sin \alpha \left[ C_L \left( 2 \sin \phi - \frac{1}{\sin \phi} \right) + \frac{\partial C_L}{\partial Re} Re \sin \phi + \frac{\partial C_L}{\partial \phi} \cos \phi - \frac{\partial C_L}{\partial \alpha} \tan \alpha \frac{\sin \phi}{\sin \phi} \right] \right\} 
$$

(5.15)

This expression includes all of the previously mentioned instability criteria, rewriting the $Re$ definition in (2.1) as $\mu Re = \rho Du$.

The Den Hartog galloping criterion is, in the absence of structural damping, equivalent to (5.15) for a cross-flow oscillation with the flow normal to the body, i.e. $\phi = 90^\circ$ and $\alpha = 90^\circ$, which implies that the aerodynamic damping considering this special case is given by:

$$
\zeta_a = \frac{\mu Re}{4m\omega_n} \left( C_D + \frac{\partial C_L}{\partial \alpha} \right) 
$$

(5.16)

The drag crisis criterion corresponds to a normal flow, but with oscillations in the flow direction, i.e. $\phi = 90^\circ$ and $\alpha = 0^\circ$. This reduction of the full expression results in:

$$
\zeta_a = \frac{\mu Re}{4m\omega_n} \left( 2C_D + \frac{\partial C_D}{\partial Re} Re \right) 
$$

(5.17)

The expression for the conventional quasi-steady aerodynamic damping (5.6) is obtained by further reducing (5.17) by setting $\frac{\partial C_D}{\partial Re} = 0$ and, as for the drag crisis, $\phi = 90^\circ$ and $\alpha = 0^\circ$.

Macdonald and Larose (2006) assume that the change in lift force with the angle of attack can be neglected for a circular cylinder, $\frac{\partial C_L}{\partial \alpha} = 0$, which leads to an expression for the aerodynamic damping for cross-flow oscillations, expressed by the drag coefficient, which is half of the aerodynamic damping for oscillations in the flow direction. This is still only valid in the subcritical $Re$ range.

Note that force coefficients for actual bridge cables determined by wind tunnel tests should not necessarily be assumed as invariant with the angle of attack, $\alpha$, cf. (Matteoni and Georgakis, 2011). Wind tunnel tests for a short bridge cable models in cross-flow with a non-uniform surface roughness and shape show significant variations in the force coefficients, which may be important for theoretical models and section tests. As a matter of fact, these variations may not be important when averaged over the entire length of a prototype bridge stay.
5.2 Aerodynamic Damping for Two-degree-of-freedom Systems

Macdonald and Larose (2008a) expanded their unified model from (Macdonald and Larose, 2006) to include 2-DoF systems. First, a general formulation and its relative solution has been presented for a perfectly tuned system, which means that the frequencies of the 2-DoF are assumed to be equal. This is valid e.g. for vertical cable like hangers on suspension bridges. For inclined cables, e.g. cables on cable-stayed bridges, changes in natural frequencies due to static sag are not noteworthy for even in-plane modes and the out-of-plane modes, while odd in-plane vibration modes may undergo significant increase in frequencies. Second, a model for detuned systems has been presented in the companion paper (Macdonald and Larose, 2008b). It is shown, that for more than a few percent detuning of the natural frequencies, the structural damping required to prevent galloping vibrations tends towards solutions for the uncoupled 1-DoF systems.

The analytical work of this project specifically referred to the evaluation of aerodynamic damping and drag/lift instabilities. Therefore, the 2-DoF model presented in (Macdonald and Larose, 2008a) was applied. The two different frequencies were considered in the calculation of the respective aerodynamic damping terms to account for the twin-cable detuned system. The estimation of the minimum level of structural damping necessary to prevent instabilities presented in the second part of Macdonald and Larose (2006, 2008a,b)’s work, was not considered here.

5.2.1 Unified 2-DoF Model

The component of the resulting force acting in the same direction as the velocity of the cylinder, equivalent to (5.13), is given by:

\[ F_x = \frac{1}{2} \rho U_R^2 D (C_D \cos(\alpha + \alpha_D) - C_L \sin(\alpha + \alpha_D)) \]  

(5.18)

Similarly, the component of the force in the orthogonal direction is given by:

\[ F_y = \frac{1}{2} \rho U_R^2 D (C_D \sin(\alpha + \alpha_D) - C_L \cos(\alpha + \alpha_D)) \]  

(5.19)

![Figure 5.3: Definition sketch of angle of attack α for the 2-DoF model](image)

The angle of attack for the 2-DoF model is again defined as the angle between the flow direction and the direction of the motion indicated by \( \dot{x} \). Note that two orthogonal
motion component are now considered, \( \dot{x} \) and \( \dot{y} \). A sketch of the definition is shown in Figure 5.3.

The forces provide a non-linear damping term in the equation of motion for the cylinder, which for small vibrations are determined as for the 1-DoF model considered in section 5.1.4. The linearised dynamic components of the aerodynamic forces can be described with the aerodynamic damping matrix:

\[
C_a = \begin{bmatrix} c_{a,xx} & c_{a,xy} \\ c_{a,yx} & c_{a,yy} \end{bmatrix} = - \begin{bmatrix} \frac{\partial F_x}{\partial x} & \frac{\partial F_x}{\partial y} \\ \frac{\partial F_y}{\partial x} & \frac{\partial F_y}{\partial y} \end{bmatrix}
\]

\[
\dot{x} = \dot{y} = 0
\]

(5.20)

where \( c_{a,xx} \) corresponds to the aerodynamic damping term for the 1-DoF model with \( \alpha = 0^\circ \) when the flow is normal to the plane containing the two cables axes in the dynamically tested set-up. Likewise, \( c_{a,yy} \) corresponds to the aerodynamic damping term for the 1-DoF model with \( \alpha = 270^\circ \) indicating the in-plane motion.

By calculating the matrix terms in (5.20) as for the 1-DoF model in (Macdonald and Larose, 2006), the full 2-DoF aerodynamic damping matrix per unit length of the cylinder can be written as:

\[
C_a = \frac{\mu Re}{2} GB_1 + C_{F\phi} B_2
\]

(5.21)

where

\[
G = \begin{bmatrix} g(C_D) & -g(C_D) \\ g(C_L) & g(C_D) \end{bmatrix}, \quad C_{F\phi} = \frac{1}{\sin \phi} \begin{bmatrix} C_D & C_L \\ C_L & C_D \end{bmatrix}, \quad C_{F\phi}' = \frac{1}{\sin \phi} \begin{bmatrix} C_D' & C_L' \\ C_L' & C_D' \end{bmatrix},
\]

\[
B_1 = \begin{bmatrix} \cos^2 \alpha & \sin \alpha \cos \alpha \\ \sin \alpha \cos \alpha & \sin^2 \alpha \end{bmatrix}, \quad B_2 = \begin{bmatrix} -\sin \alpha \cos \alpha & \cos^2 \alpha \\ -\sin^2 \alpha & \sin \alpha \cos \alpha \end{bmatrix},
\]

\[
g(C_F) = C_F \left( 2 \sin \phi - \frac{1}{\sin \phi} \right) + \frac{\partial C_F}{\partial Re} Re \sin \phi + \frac{\partial C_F}{\partial \alpha} \cos \alpha
\]

with \( C_F' = \frac{\partial C_F}{\partial \alpha} \) and \( C_F = C_D \) or \( C_L \).

By rearranging (5.21) the aerodynamic damping matrix can be written in the alternative form:

\[
C_a = \frac{\mu Re}{4} \left\{ (G - J)B_3 + (H + J) \right\}
\]

(5.22)

where

\[
J = \frac{1}{\sin \phi} \begin{bmatrix} C_D' & C_L' \\ -C_D' & C_L' \end{bmatrix}, \quad B_3 = \begin{bmatrix} \cos 2\alpha & \sin 2\alpha \\ \sin 2\alpha & -\cos 2\alpha \end{bmatrix},
\]

\[
H = \begin{bmatrix} h(C_D') & -h(C_L) \\ h(C_L) & h(C_D') \end{bmatrix}, \quad h(C_F) = g(C_F) + \frac{2C_F}{\sin \phi}
\]

The symmetric term \((G - J)B_3\) and the skew-symmetric term \((H + J)\) in (5.2.1) can be used to describe and estimate the transfer of energy between the 2-DoF, cf. (Macdonald and Larose, 2008a).

In order to obtain a better description of the damping of a cable system, the 2-DoF model is suggested to use especially in cases were detuning between the degrees of
5.3. Results from Static Test

freedom is present. That was the case for the twin-cable system investigated in the project. In the application of the 2-DoF model the aerodynamic damping ratio in its matrix form was defined as:

\[
\zeta_a = \begin{bmatrix}
\frac{c_{a,xx}}{2m_x\omega_x} & \frac{c_{a,xy}}{2m_y\omega_y} \\
\frac{c_{a,yx}}{2m_x\omega_x} & \frac{c_{a,yy}}{2m_y\omega_y}
\end{bmatrix}
\]  \hspace{1cm} (5.23)

The diagonal terms described the damping in the two directions as the 1-DoF model. The term \(\zeta_a(1,2)\) describes the damping in out-of-plane direction for a motion in the in-plane direction, while the term \(\zeta_a(2,1)\) does the opposite.

It should be noted that the 2-DoF model does not take into account the torsional motion or any rotational component. Therefore, a further expansion of the model with 3-DoF has been developed by Gjelstrup and Georgakis (2011) including the rotational degree of freedom about the cable axis. However, the last mentioned 3-DoF was not applied in this project because the adopted dynamic rig was designed as a 2-DoF system only.

5.3 Results from Static Test

The aerodynamic damping was calculated applying the models described in sections 5.1 and 5.2 using force coefficients from static tests. The available data were force coefficients determined from a series of different tests.

5.3.1 Force Coefficients

Since data from test of an inclined-yawed twin-cable model with helical fillets were not available, it was chosen to consider the force coefficients from three different test series: a cross-flow test of a plain twin-cable model, a cross-flow test of a twin-cable model with double helical fillet and a test with an inclined-yawed plain twin-cable model. All tests had been performed by Antonio Acampora and Giulia Matteoni, Ph.D. students of the CESDyn group, in dry conditions in the CWT with a model, which was similar to the one described in section 4.1.3.

For the vertical cross-flow tests, i.e. \(\beta = 90^\circ, \theta = 90^\circ\) and \(\phi = 90^\circ\), the twin-cable model had been installed between floor and ceiling in the wind tunnel. A photo of the model mounted in the wind tunnel is shown in Figure 5.4(a).

The inclined-yawed twin-cable model had been tested in the same configuration as the passive-dynamic tests set-up, i.e. \(\beta = 70^\circ, \theta = 30^\circ\) thus \(\phi = 72.3^\circ\). The model is shown in Figure 5.4(b).

Results illustrated in Figure 5.5, include force coefficients for a plain twin-cable model and twin-cable model with double helical fillet.

Drag coefficients for the two tested set-ups without helical fillet seemed to follow the same trend, but a reduction in magnitude of \(C_D\) was evident in the inclined-yawed case. That was because the force coefficients in all cases refer to the model system i.e. drag was defined normal to the plain containing the axes of the two cylinders and the lift belonged to the mentioned plain oriented perpendicular to the drag.
5.3. Results from Static Test

As a consequence of the adopted reference system, $C_D$ of the inclined-yawed set-up strictly should be called normal component of $C_D$. The total $C_D$ defined in the flow direction was not possible to determine, because the axial force component registered by the force transducers comprised an extra unknown compressive force due to the suction of the non-rigid wall panels originated by the possible non-zero differential static pressure at the test section. Therefore the inclined-yawed case was expected to give a lower drag coefficient, as finally demonstrated in Figure 5.5(a). The drag coefficient for the cables with the double helical fillet was instead seen to be lower in the subcritical $Re$ range and the drag crisis occurring from $Re \approx 1.3 \times 10^5$, was less distinct compared to the plain cables. These observations corresponded to the results of Kleissl and Georgakis (2012), where full-scale single HDPE tubes had been tested with and without helical fillet. Kleissl and Georgakis (2012) explains that the more smooth transition to critical flow most likely is due to the ability of the fillet to generate a variable separation line along the length of the cable.

![Figure 5.4: Set-ups for static tests on twin-cable model with and without double helical fillet](image)

![Figure 5.5: Force coefficients from static test for a twin-cable model with and without double helical fillets](image)

Despite the fact that the increase in lift at the critical $Re$ range was more evident for the plain cable, which may be related to the just mentioned difference in drag
5.3. Results from Static Test

coefficient between plain and filleted cables, lift curves stayed at low value around a zero mean, cf. Figure 5.5(b). Nevertheless, lift fluctuation played an important role in the estimation of the aerodynamic damping.

5.3.2 Aerodynamic Damping

The aerodynamic damping was determined for all three sets of force coefficients using the analytical models described earlier in the chapter, cf. sections 5.1 and 5.2.

Both 1-DoF and 2-DoF models were applied to determine the aerodynamic damping for the motion in each direction. This was done to show that the 2-DoF model simply contains the unified 1-DoF model. Note that the 2-DoF model also contains coupling terms, which link the two directions.

The aerodynamic damping for motion in the out-of-plane direction was additionally compared to the special case considering the drag crisis, which is defined in (5.17). Similarly, the aerodynamic damping for the motion in the in-plane direction was compared to the case considering the Den Hartog galloping instability criterion, which is defined in (5.16). These comparisons were made to study the difference between the models based on the classical instability criteria and the unified models, which e.g. also take the relative yaw angle $\phi$ into account.

The aerodynamic damping calculation, which was based on the force coefficients from the static cross-flow test with the plain twin-cable model, is presented in Figure 5.6 for both out-of-plane ($\zeta_{a,xx}$) and in-plane ($\zeta_{a,yy}$). It is seen that the Drag Crisis, 1-DoF unified model and 2-DoF unified model are very similar. In fact, the Drag Crisis differed slightly from the others because of the force coefficients dependencies on $\alpha$ and $\phi$ were neglected, while the 1-DoF and 2-DoF models naturally gave the same result.

The out-of-plane aerodynamic damping increased linearly until $Re \approx 1.3 \times 10^5$, followed by a drastic drop with negative values for $Re > 2.0 \times 10^5$. For plain cylinders this may lead to instability, if the structural damping is not sufficiently large. The drag coefficient governed the out-of-plane aerodynamic damping. The combination of lower value and abrupt drop, cf. Figure 5.5, resulted in a fast decreasing damping.

Also the three models for the in-plane aerodynamic damping took the same pattern and values. Results from the 1-DoF and 2-DoF models were equal, while the model
5.3. Results from Static Test

based on Den Hartog’s observations focusing on the effect of change in lift force as a function of angle of attack, slightly deviated from the other models in the critical \( Re \) range. The in-plane aerodynamic damping also increased linearly until \( Re \approx 1.6 \times 10^5 \) followed by stable or slightly decreasing values. It was noted that the aerodynamic damping values in the subcritical \( Re \) range were around half of the out-of-plane values.

The aerodynamic damping determined with force coefficients from test with the twin-cable model with helical fillets, cf. Figure 5.7, increased linearly until \( Re \approx 1.4 \times 10^5 \), after which it stabilised and decreased slightly. For \( Re > 2.0 \times 10^5 \) the linear increase continued. The aerodynamic damping for the in-plane motion was increasing for the entire considered \( Re \) range. A linear slope was observed until \( Re \approx 1.5 \times 10^5 \). Hereinafter the damping tended to increase slower.

![Figure 5.7: Aerodynamic damping for twin-cable model with double helical fillet tested in cross-flow](image)

For the plain inclined-yawed twin-cable model test, the aerodynamic damping is shown in Figure 5.8. For the out-of-plane motion, an increase in aerodynamic damping was seen until \( Re \approx 1.1 \times 10^5 \). The value dropped afterwards and negative values were observed from \( Re > 1.8 \times 10^5 \). The aerodynamic damping for the in-plane motion reached the maximum value at \( Re \approx 1.5 \times 10^5 \). For higher \( Re \) the damping decreased slightly.

![Figure 5.8: Aerodynamic damping for inclined-yawed plain twin-cable](image)

From the three tests it was clear that the different applied models gave analogous results for the two directions. Therefore, it would have been sufficient to determine the aerodynamic damping using the unified 2-DoF model, which also contains the
5.3. Results from Static Test

coupling terms between the out-of-plane and in-plane directions, that better picture
the damping of a detuned system.
In Figure 5.9 the results from the all three tests are plotted together. Besides the
aerodynamic damping of the motion in the two directions, the coupling terms are also
plotted to give the full description. They are the cross terms of the damping ratio
matrix in (5.23).

![Graph](image)

**Figure 5.9:** Aerodynamic damping determined with the unified 2-DoF model for
all three static tests

It was noticed that the two tests with the plain cables followed the same trend, but
that the inclined-yawed case presented aerodynamic damping values slightly lower in
magnitude and there was a small shift in $Re$, meaning that the decrease in aerody-
namic damping happened at lower values of $Re$ for the inclined-yawed test. Though,
the drop was not as dramatic as it was for the plain twin-cable model tested in cross-
flow.
The largest difference was, that there was no significant drop in aerodynamic damping
for the motion in any of the two directions for values larger than $Re \approx 1.3 \times 10^5$. The
curves levelled off instead.
The coupling terms determined with the 2-DoF model, were close to zero for $Re < 
1.3 \times 10^5$. This means that there was no considerable coupling between the damping
of the motion of the two directions. But for $Re > 1.3 \times 10^5$ the $yx$-term deviated from
zero indicating a coupling, i.e. a transfer of energy from one DoF to the other. In
other words a large influence on the aerodynamic damping in the in-plane direction
was caused by the motion in the out-of-plane direction.
For the case of cross-flow test of the plain twin-cable model, values started to increase
at $Re \approx 1.5 \times 10^5$. After a slight increase, a large drop resulting in large negative
values was noticed, cf. Table 5.1.
5.3. Results from Static Test

Table 5.1: Aerodynamic damping from analytical 2-DoF model

<table>
<thead>
<tr>
<th>Reynolds number</th>
<th>Out-of-plane 1.0 x 10^5</th>
<th>2.1 x 10^5</th>
<th>In-plane 1.0 x 10^5</th>
<th>2.1 x 10^5</th>
</tr>
</thead>
<tbody>
<tr>
<td>Plain cross-flow model</td>
<td>0.31%</td>
<td>-0.13 %</td>
<td>0.17%</td>
<td>0.22%</td>
</tr>
<tr>
<td>Filleted cross-flow model</td>
<td>0.28%</td>
<td>0.33%</td>
<td>0.15%</td>
<td>0.26%</td>
</tr>
<tr>
<td>Plain inclined-yawed model</td>
<td>0.25%</td>
<td>-0.09 %</td>
<td>0.12%</td>
<td>0.13%</td>
</tr>
</tbody>
</table>

In contrast to the yx, the xy-term was almost constant in the entire range. Only a slight increase for Re > 2.0 x 10^5 was noted.

For the inclined-yawed case the picture was the same, but the shift in Re made it divert from zero from Re ≈ 1.3 x 10^5 and the negative drop was not so dramatic. Actually the term became positive again after the drop at Re ≈ 1.9 x 10^5.

Also for the model with fillets the yx-term deviated from zero for Re > 1.6 x 10^5.

The comparison showed that the presence of the helical fillets seemed to have a positive influence on the aerodynamic damping avoiding negative values, in this case. This was related to the more smooth drag curve, cf. Figure 5.5(a).
5.3. Results from Static Test
In this chapter the results from the experimental work are evaluated and compared to data obtained from related research activities performed by Ph.D. students of the CESDyn group. First, the force coefficients which were determined from the mean displacement of the twin-cable model in section 4.4, are compared to the force coefficients determined from static wind tunnel tests. Second, the results from the passive-dynamic test in terms of aerodynamic damping are compared to the results from quasi-steady models using static force coefficients and from full-scale monitoring of the Øresund Bridge. Before the direct comparison was made, the passive-dynamic tests results were scaled to represent the full-scale case.

6.1 Comparison of Force Coefficients

The first result of the comparative analysis concerned force coefficients obtained from dynamic and static tests. Drag and lift curves, already presented in Figure 4.34, are here reported together with curves from static tests of the same twin-cable model with double helical fillet in a cross-flow configuration, though. The latter set-up was chosen for the comparison, out of the three test series available, cf. Figure 5.5 in section 5.3, because it was assumed to have the most similar aerodynamic behaviour to the dynamically tested model. In fact, the presence of the double helical fillet is causing slower flow transition compared to the plain case as reported by Kleissl and Georgakis (2012). Furthermore the same authors report that the fillet is obstructing the axial flow in inclined-yawed cases. For those reasons, it was chosen to compare results from models with the same aerodynamic shape, even though tested under diverse geometrical configuration. As a consequence, differences between the two results could be most likely attributed to the presence of the axial flow in the inclined set-up. Also diversities in the set-up arrangements between dynamic and static experiments might have been a potential source of result discrepancies. This mainly referred to the partially open test section of the wind tunnel when performing passive-dynamic tests as well as the possible non quasi-steady characterization of the flow around the
moving cylinders in the dynamic tests. Openings were causing a decreasing in the flow velocity at the model location, as documented in section 4.1.7. The force coefficient comparison is depicted in Figure 6.1. Note that the dynamic wet test outputs were not reported because static tests had been conducted only in dry condition.

Figure 6.1(a) illustrates drag force coefficients. They were defined in both cases normal to the two cables axes plain, as mentioned previously. Therefore the normal component of the drag was considered. Results showed a certain similarity in the drag transition. The critical Re range was defined from about $1.5 \times 10^5$ where drag started to decrease constantly. All in all, dynamic drag appeared to be about 20% larger than static values, likely because of the reasons mentioned above.

Lift coefficients are presented in Figure 6.1(b). A remarkable similarity was observed between the results from the two tests. One only difference was denoted after $Re \approx 2 \times 10^5$ where the dynamic lift proceeded with the precedent trend while the static one deviated and started to reduce.

**Figure 6.1:** Force coefficients from dynamic tests on inclined-yawed set-up and static tests on cross-flow set-up of the twin-cable model with double helical fillet

6.2 Scaling of Aerodynamic Damping from Dynamic Test

Force coefficients and Reynolds number are dimensionless parameters, which were not affected by the geometric scaling of twin-cable model.

Nevertheless, despite the damping ratio determined from passive-dynamic tests is a dimensionless parameter too, it had to be scaled properly to compare with full-scale values. That was because $\zeta_a$ is governed by the mass-frequency parameter. The explanation is presented in the following and a scaling factor is determined.

Assuming that the force coefficients are representative for both the scaled model and the full-scale prototype cable and that the density and kinematic viscosity of the air are the same in the two cases, the only left quantities affecting the aerodynamic damping are masses and frequencies. All the considered models describing the aerodynamic
damping, reported in chapter 5, take the form:

\[ \zeta_a = \frac{\rho DU}{2m\omega} \cdot f(Re, CD, CL, \alpha, \phi) \]  \hspace{1cm} (6.1)

It indicates that \( \zeta_a \) is a function of \( Re \), force coefficients, and the angles \( \alpha \) and \( \phi \). By substituting \( DU = \nu Re \), according to (2.1), the expression (6.1) becomes:

\[ \zeta_a = \frac{\rho \nu Re}{2m\omega} \cdot f(Re, CD, CL, \alpha, \phi) \]  \hspace{1cm} (6.2)

From (6.2) it is clear that the only two variables are the mass \( m \) and the frequency \( \omega \), the product of which is here denoted mass-frequency parameter. Applying the assumptions mentioned above, the ratio, \( \lambda_{\zeta_a} \), between the aerodynamic damping determined from wind tunnel test and the full-scale value is:

\[ \lambda_{\zeta_a} = \frac{\zeta_a, test}{\zeta_a, full-scale} = \frac{\frac{\rho \nu Re}{2m_{test}\omega_{test}} \cdot f(Re, CD, CL, \alpha, \phi)}{\frac{\rho \nu Re}{2m_{full-scale}\omega_{full-scale}} \cdot f(Re, CD, CL, \alpha, \phi)} \]  \hspace{1cm} (6.3)

which can be reduced to:

\[ \lambda_{\zeta_a} = \frac{\zeta_a, test}{\zeta_a, full-scale} = \frac{m_{full-scale} \cdot \omega_{full-scale}}{m_{test} \cdot \omega_{test}} \]  \hspace{1cm} (6.4)

Hence, the aerodynamic damping scaling factor was defined as the ratio between the mass-frequency parameter of the full-scale and the scaled tests, respectively. The scaling coefficient was determined for both out-of-plane and in-plane vibrations from (6.4). This resulted in:

\[ \lambda_{\zeta_a-(out-of-plane)} = 11.15 \]
\[ \lambda_{\zeta_a-(in-plane)} = 11.32 \]

### 6.3 Comparison of Aerodynamic Damping

The first aerodynamic damping comparison was between results from the performed passive-dynamic tests in CWT, cf. section 4.4.2 and the results of the application of the 2-DoF analytical model developed by Macdonald and Larose (2008a,b) using force coefficients determined from static tests, cf. section 5.3. Only the tested dry condition was reported since available data from static tests did not include wet condition. Solely the two damping matrix diagonal terms were considered, since the damping from passive-dynamic tests was estimated in the two orthogonal directions. Note that the cross diagonal damping terms were not calculated from passive-dynamic tests because the coupling between the two DoF was not only due to the detuned frequencies but also to the mutual interaction between the springs of the rig for the oscillating model.

The comparison is illustrated in Figure 6.2.

Figure 6.2(a) shows the data concerning the out-of-plane direction. A good correlation between the two curves was depicted in the subcritical range i.e. \( Re < 1.5 \times 10^5 \). After
6.3. Comparison of Aerodynamic Damping

that the reliability of the dynamic curve decreased, as described in section 4.4.2 but for the values around $Re \approx 1.8 \times 10^5$ an agreement with the theoretical curve could be found again. In general values of aerodynamic damping appeared to be $\zeta_a < 0.4\%$, scaled to prototype values.

The in-plane orientation is instead considered in Figure 6.2(b). The theoretical trend of the damping was generally well reproduced by the estimation obtained from testing the model dynamically. Therefore it could be said that the theoretical model described accurately the aerodynamic behaviour of scaled stays dynamically tested in the wind tunnel. Value of damping were contained approximately to $\zeta_a < 0.25\%$, scaled to prototype values.

![Graph](image)

**Figure 6.2:** Aerodynamic damping from dynamic tests on inclined-yawed set-up and analytical 2-DoF model using static force coefficients

The second comparative analysis for aerodynamic damping regarded dynamic tests data versus full-scale monitoring data. The latter results had been directly obtained for the instrumentation installed on the Øresund Bridge cables, through the activity of Acampora and Georgakis (2011a,b). Data of damping available for both dry and wet conditions but only for the out-of-plane direction were already presented in section 3.3. The comparison is illustrated in Figure 6.3.

The dry case is considered first. As shown in Figure 6.3(a), the trend in the subcritical range i.e. for approximately $Re < 1.5 \times 10^5$, was in agreement with monitoring data. Small deviations were expected because of differences in surface roughness and turbulence flow level between wind tunnel tests and the actual conditions on the bridge. In the critical range the damping from monitoring seems to decrease, regaining the initial trend after $Re \approx 1.8 \times 10^5$. Dynamic tests did not report complete data in the critical $Re$ range but agreed with the monitoring curve for values around $Re \approx 1.8 \times 10^5$.

Figure 6.3(b) shows instead damping for the wet condition i.e. under simulated rain in CWT and actual rain events out of the real bridge. Again it was noted that in the subcritical range the two curves matched well. In the critical range aerodynamic damping from monitoring experiences a significant drop until $\zeta_a \approx -0.25\%$, after which it regains the positivity, following the pattern reported also for the monitored dry case. However, the dynamic test results did not show such a large drop. This could be explained by the not complete reproduction in the wind tunnel of the
6.3. Comparison of Aerodynamic Damping

![Graphs showing aerodynamic damping in dry and wet conditions.](a) $\zeta_a$ in dry condition  (b) $\zeta_a$ in wet condition

Figure 6.3: Aerodynamic damping from dynamic tests on inclined-yawed set-up and full-scale monitoring of the Øresund Bridge cables

Actual conditions on the bridge responsible for negative aerodynamic damping values. The simulation of these conditions require further studies for a more descriptive characterization of the phenomena in case of rain events.
6.3. Comparison of Aerodynamic Damping
Final Considerations

The final considerations regarding the performed activity in the wind tunnel and the subsequent data processing are now reported. With this, the author has the intention to testify the experience gained throughout the conduction of the project and make it available for later studies. Suggestions for future research oriented to a deeper understanding of the phenomena investigated are reported as well.

7.1 Experience from Wind Tunnel Tests

7.1.1 Model Preparation

The experimental part of the thesis work started with the preparation of the twin-cable model and the set-up, designed as a reproduction of the actual configuration of the Øresund Bridge stays at reduced scale, cf. section 4.1.3. It was chosen to adopt PVC pipes of 110mm in diameter and 5.3mm thick. The adopted size generated an important blockage ratio which had to be considered for the calculation of the drag curve. The combination of pipe material and thickness made the tubes rigid enough on their own. So that, inner stiffening components such as the aluminium pipes did not need to be employed in conducting dynamic tests.

The original roughness of the PVC pipes had to be modified to simulate the actual stay surface properties. Other surface roughness levels than the one defined in this case, potentially could give different result, e.g. anticipation or postponement of the drag crisis in the $Re$ scale. Working in this way it would be possible to relate the roughness levels at full-scale and reduced scale causing the same aerodynamic behaviour. The same scaling issues were met in the application of the double helical fillet to the model surfaces. Qualitatively speaking, it seemed that the fillet size adopted was reasonable for the reproduction of full-size behaviour, but further studies of this would berecommendable, for instance confronting directly a full-size cable section with a scaled one.
7.1.2 2-DoF Spring Rig

The 2-DoF rig adopted for the experimental campaign in the CWT allowed translational movements in two orthogonal directions, cf. section 4.1.4, while the rotational component should be prevented. As a matter of fact, the latter implied some practical arrangements that became clear when the model was mounted on the rig. The main issue was about maintaining the alignment of the in-plane springs with the model axis while varying the wind speed and thus the mean static displacement of the model. An alternative solution to the one adopted here, could be the use of a shifting rig to compensate for the static movement of the model. In particular, the out-of-plane springs could be back-slid while increasing the wind velocity to maintain the in-plane springs aligned with the model axis. In this way there would be no relative static movement between the model and the wall panels. This solution might also be potentially valid in the out-of-plane direction even though the springs most likely had to be diverse to each other as it was the case here. This difference in size and length, due to the necessity to account for the static drag, occasionally happened to generate a sort of coupling between the two degrees of freedom. That was due to the different angles from the vertical plane of the out-of-plane spring forces acting on the model moving in the in-plane direction. Coupled motions were also observed when the in-plane springs were not anchored perfectly on the axis line of the model. This minor detail was found to be of extreme importance to obtain a decoupled motion of the two degrees of freedom, excited one at a time.

The inclined configuration of the model forced the adoption of a solution to contain the gravity component along the model axis, namely a metal chain, cf. section 4.1.5. Passive-dynamic tests were conducted using a short chain for the prevention of the motion in the model axis direction, because of size limitation in the room hosting the CWT. The effect of this fitting was the generation of pendulum motion of the model. Therefore the manual excitation applied to enhance the modal response had to be applied with a difference in amplitude between the two ends in order to maintain this pendulum like oscillations. This behaviour might have influenced the damping of model vibrations which were observed to be amplitude dependent at high wind velocities.

7.1.3 Rain System

The effectiveness of using two parallel systems for the rain simulation, cf. section 4.1.6, i.e. kick-start pipes as rivulets initiators and water sprays to wet the model surface and feed the rivulets running down the pipes with water, was validated while testing. The position of the rivulet initiators had to be defined in situ. It was important to regulate the water flow from the pipes in order to have the same output on the two pipes of the twin-cable model, placed in a side-by-side arrangement. The location of the water sprays could be anywhere in the test section, at the tunnel floor in the tested case. The very essential thing was to control and regulate the water intensity with the changing wind speed in order to keep the model surfaces uniformly wet all over the speed sweep.

A consequence of the use of PVC pipes, even though treated with sand paper, was the different surface tension of the model pipes compared to prototype cables. Therefore
7.1 Experience from Wind Tunnel Tests

Surface treatment with a polyvinyl coating solution had to be considered, cf. section 4.1.6.1. It was a practical solution already used in practice (Larose and Smitt, 1999), which allowed for the conduction of proper wet tests, i.e. the water behaviour on the tube surface was better reproduced in the wind tunnel.

7.1.4 Model Dynamics

The manual model excitation for emphasising only one mode at a time, was a potential source of uneven dynamic stimulation of the model. This technique is used in practice on full-scale bridges, cf. (de Sá Caetano, 2007, p. 182). More accurate or controlled excitation systems could be adopted for later set-ups, which also would account for the possible pendulum type motion.

In designing the model the target was to keep its mass on a minimum level, cf. section 4.1.5, i.e. minimum $Sc$ number, to amplify the pure aerodynamic behaviour. As a result, large amplitude vibrations of the model under the wind action alone were registered, leading to a high buffeting limit and thereby a decrease in the usable time interval for damping estimation. In general it was necessary to make sure that the initial excitation was significantly larger than the buffeting limit to ensure valid results applying the same method of damping estimation, adopted in this project.

For a better distinction between aerodynamic damping of the motion imposed by the external source and the buffeting response, one could possibly analyse the effect on the vibration response of varying the model mass, hence its Scruton number. It is clear though, that spring rig stiffness has to be controlled as soon as the model mass is changed, if the same modal frequencies are wanted.

7.1.5 Tunnel Wall Openings

The tunnel wall openings clearly changed the flow conditions in the test chamber compared to completely closed situations, as demonstrated in section 4.1.7. The fact that the model passed through the openings at different distances from the wall edge because of the changing static displacement, made the model be subjected to different end conditions throughout the wind velocity sweeps. As discussed in section 4.4.2, this was related to the uncertainty in the damping evaluation for some velocities in the high speed range.

Therefore a good improvement for future investigation of similar type would be to keep the model position compared to the wall edges constant while testing, either by rearranging the opening geometry at different speeds or using a sort of sliding rig to account for the static displacement due to the wind action, as already mentioned in section 7.1.2.
7.2 Experience from Data Processing

7.2.1 Reference Data

The determination of reference data has been presented in section 4.3.1. The activity of data processing had revealed that the electric net or electric wiring for the instruments of the data acquisition system might affect the furnesses reading. Noise peaks appeared on the reference histories and therefore statistical interpretation of the outputs had to be used. For instance, referring to the temperature reading, if the actual mean value would have stayed constant during the logging time, the mode value estimation could have been performed to estimate the actual mean and get rid of noise peaks at the same time. However, when the cooling unit of the CWT was activated the mean actual temperature could sensible change within the sample time, so that the statistical mean was likely more accurate for the estimation of the actual mean. These sudden jumps in temperature, more evident at high relative humidity level, were difficult to identify from the time histories. They were limited by making some fast sweeps in velocity in the wind tunnel with the cooling unit activated before the start of the actual test programme.

Another essential factor noticed from the analysis of the reference histories was the strict requirement for the calibrated Pitot tube in the CWT to stay dry all the time even in wet testing condition, because the water presence on and inside the probe hole would spoil the pressure reading at the furness. That was simply because the used Pitot tube did not include a drainage system like aircraft Pitot tubes do.

7.2.2 Force Coefficient

The determination of aerodynamic forces and force coefficients from passive-dynamic tests was based on the force-displacement link, through stiffness, as described in section 4.3.4. A part from the necessity of an accurate estimation of the model stiffness, it was important to account for any intentional or accidental change in the position of the laser displacement transducers. Acquisition sensors should not be touched while testing, but this could be unavoidable as e.g. when model peak displacements overcame the instrument reading range. Thus, documentation of any kind of disturbances regarding the laser displacement transducers, and the repetition of sampling before and after a change in their position, made the analysis and interpretation of the data more clear and precise.

As the test campaign was conducted with a partially open test section, the flow in the tunnel testing area was observed to be different from that of a closed section, cf. section 4.1.7. Therefore when calculating quantities such as $Re$ and force coefficients, the change in velocity and in the effective length of the model ‘seen’ by the wind had to be updated accounting for the actual characteristics of the test section. On the other hand, the flow obstruction generated by the model was the same as if it would have been tested statically. That was the reason for which a blockage correction procedure had to be applied for the correction of the drag coefficient.
7.2.3 Aerodynamic Damping

Section 4.3.5 describes the procedure adopted for the evaluation of aerodynamic damping from the recorded time histories. From the selected approach some points are worth to be highlighted.

First, the filtering process was very important and essential for a good data analysis. At low wind velocities the model response did not seem to suffer from noise or unwanted modes but for consistency the same signal filter was suggested to be employed to all time histories throughout the whole speed range. Naturally digital filtering introduced some anomalies in the treated time histories at the limits of the time window. Therefore it was important to exclude the use of data at the extremes in the determination of the wanted parameters. About the noise level, i.e. gusty wind induced response, the selection of instruments with a proper resolution was very essential, cf. (Brandt, 2011), but it had to be considered in any case in the histories analysis, disregarding values beneath it. Adjusting the model $Sc$ could be beneficial to this purpose, i.e. the limitation of the buffeting induced response.

Second, the assumption of a linear vibration decay over time was adopted here, even though the processing of test outputs made the amplitude dependency and the non-linearity of damping evident for high wind speeds, i.e. above approximately $15 m/s$. Therefore, for a complete description of the damping trend an alternative method could be considered instead of the linear logarithmic interpolation. However, if the target was to investigate how the damping was affected by variable wind action an average qualitative estimation was accepted and practically usable e.g. for design purposes.

Last, the 2-DoF dynamic rig adopted in the CWT for this passive-dynamic test series, was designed to allow for one diameter maximum amplitude displacements. Above this limit sensors could still register data, but the latter had not to be considered as reliable in the data processing. That was because large amplitudes introduced second order effects, non-linear behaviour of springs that might change the vibration characteristics of the model and trigger mode coupling. This was of course a limitation which had to be considered if dependable results were wanted.
7.3 Future Work

The analysis of the results from the passive-dynamic tests shows that there is space to improve the reproduction of the conditions for the Øresund Bridge cables, which may lead to instability. In particular, research should be directed to a deeper understanding of the aerodynamic damping in the critical range. Parallel to this, effort should be put into the investigation of the negative aerodynamic damping monitored on the full-scale stays in presence of rain events.

The documented master’s thesis work had made the author conscious about the vastness of the field he was introduced to. The use of passive-dynamic test technique, never used before in the CWT, generated a series of stimulating debates between the author and his project mate, with the supervisor and members of the CESDyn research group. Those reflections were oriented to exploit the new developed dynamic rig to further progress with the understanding of aerodynamic behaviour of structures.

Some of the discussed topics are suggested and highlighted in the following list:

- Design of a 3-DoF dynamic rig to include the rotational degree of freedom.
- Full application of advanced 2-DoF and 3-DoF analytical models including force coefficient derivatives, cable-wind angle and angle of attack dependency.
- Development of parallel static-dynamic test campaign and of the use of full-scale monitoring for a more complete and detailed comparison between aerodynamic damping from these three sources.
- Further dynamic wind tunnel investigations of cables under simulated rain condition and with ice accretions.
Conclusions

Passive-dynamic wind tunnel tests of a scaled twin-cable model have been performed to evaluate the effects of flow and meteorological conditions, which may lead to instability.

The results from tests are estimated in terms of force coefficients and aerodynamic damping for the two tested conditions dry and wet. The drag coefficient in the dry case is found to assume a maximum value of about 1.3 in the subcritical $Re$ range. This value is reduced by approximately 20\% in the wet case. The wet lift coefficient is found to diverge significantly from the dry case. The behaviour of the lift in the wet case may be linked to the change in size and position of the rivulets.

The aerodynamic damping ratio is determined for two orthogonal directions for both dry and wet conditions. A nearly linear increase is observed until $Re \approx 1.5 \times 10^5$ for the two conditions and planes. The aerodynamic damping ratio for the out-of-plane direction reaches a maximum $\zeta_a \approx 4.5\%$ in dry condition. The wet case shows slightly smaller values with a maximum $\zeta_a \approx 3.6\%$. The aerodynamic damping for the two conditions in the in-plane direction are similar to each other with a maximum $\zeta_a \approx 2.5\%$. This agrees with the expectations, saying the in-plane damping to be around half of the out-of-plane in the subcritical range. Note that these values refer to the scaled model. A mass-frequency dependent scaling factor has been applied to obtain representative results for the full-scale cables.

A comparison between force coefficients determined from passive-dynamic and static tests shows a good trend agreement between the two. Though, a significant difference in magnitude of about 20\% for the drag coefficients is registered. Comparing the aerodynamic damping from the passive-dynamic tests and the application of analytical models using static force coefficients, the reliability of the analytical models to describe the dynamically determined aerodynamic damping is made evident. The comparison is only performed for the dry condition, since data from wet tests were not available.

The comparative analysis between the passive-dynamic tests and full-scale damping estimation is also documented. Both weather conditions are considered solely for the out-of-plane direction. The dry case shows a similar pattern between the two curves with slightly larger values from the monitoring source. Maximum values are around 0.5\% and 0.4\% for the monitored and scaled model cases respectively, at $Re \approx 1.3 \times 10^5$. The drop and the successive increase in $\zeta_a$ of the full-scale data
Conclusion

cannot be confirmed by the test data because of the increasing uncertainties of the latter beyond the subcritical Re range. Similarly, for $\zeta_a$ in wet condition the agreement between the curves is outlined in the subcritical range, while the large decrease of full-scale damping to negative values is not observed for the damping determined through passive-dynamic tests.

All in all, in the subcritical Re range the aerodynamic damping determined from passive-dynamic tests corresponds well to the damping determined from analytical models and from monitoring in dry condition. At higher Re the estimation is less reliable due to the limitations of the test set-up and dynamic rig. Regarding the wet condition, only data from monitoring have been available for comparison. Further work is necessary to better the damping estimation from passive-dynamic tests in the critical Re range and to increase the knowledge about the influence on the aerodynamic behaviour in presence of rivulets.


Bibliography


Appendix A

Model Scaling

Due to geometrical and size restrictions of the used wind tunnel facility, it was decided to conduct the passive-dynamic tests on a reduced scale model of the twin-cable configuration of the Øresund Bridge. In doing so the 'Froude' similarity was considered because of the invariability of gravity between prototype and test conditions, i.e. $\lambda_{acc} = 1$. The ratio between inertial forces and gravity forces had to be kept the same when scaling down the model. The 'Froude' number is defined as:

$$ F_r = \frac{U}{\sqrt{g \cdot l}} $$  \hspace{1cm} (A.1)

where $U$ is the mean velocity, $g$ is the gravity acceleration and $l$ a characteristic body dimension.

As reported in section 4.1.3, scaling factors $\lambda$ were defined as the ratio between the model quantity and the prototype/full-scale quantity. The geometric scaling factor was then defined as $\lambda_{geom} = \frac{l_{model}}{l_{prot}}$. From (A.1) the following was derived:

$$ \lambda_{geom} = \frac{l_{model}}{l_{prot}} = \left( \frac{u_{model}}{u_{prot}} \right)^2 $$  \hspace{1cm} (A.2)

Therefore the velocity scaling factor $\lambda_u$ was defined as:

$$ \lambda_u = \sqrt{\lambda_{geom}} $$  \hspace{1cm} (A.3)

In the same way, by the definition of time $t = \frac{l}{u}$, the time scaling factor $\lambda_t$ was:

$$ \lambda_t = \lambda_u = \sqrt{\lambda_{geom}} $$  \hspace{1cm} (A.4)

For the frequency the scaling factor $\lambda_{freq}$ resulted:

$$ \lambda_{freq} = 1/\sqrt{\lambda_{geom}} $$  \hspace{1cm} (A.5)
It is important to state that the last scaling factor, $\lambda_{freq}$, was particularly important together with $\lambda_{geom}$ for the characterization of the model properties. The geometric scaling factor was strictly observed for the actual construction of the model. Only the double helical fillet was not scaled exactly according to this law because variation of the twin-cable aerodynamics was already introduced by the increased curvature of the model pipes. The frequency scaling factor was instead adopted in the reproduction of modal properties of the twin-cable model in two main orthogonal directions. The springs mounted on of the 2-DoF rig reproduce the vibration characteristics of the 1st mode only.

The mass, instead, was not scaled according to any law, but simply reduced as much as could, to reduce $Sc$ and emphasize the model aerodynamic response. This procedure could be said scaling with 'relaxed similarity' according to (Koss, 2012). A mass-frequency scaling ratio was considered for the comparison in terms of aerodynamic damping between the tested model and full-scale data, cf. section 6.2.

Naturally, all others physical quantities could be determined from the fundamental unit scaling laws, i.e. $\lambda_{geom}$, $\lambda_t$ and $\lambda_{acc} = 1$, following the same procedure derived above.
Appendix B

Surface Treatment

Behaviour of water on prototype cables surface was an important factor to consider in preparing the model for tests in wet condition. The model surfaces were treated with sand paper, as described in section 4.1.3.1, but the modification of the surface roughness was not sufficient to reproduce a wettability similar to the one of a prototype cable. Therefore, in order to increase the wettability of the model surfaces it was necessary to operate in changing surface tension properties and thereby the behaviour of the water on the pipe surfaces. Based on the experience from (Larose and Smitt, 1999) it was decides to apply a polyvinyl alcohol solution. The solution consisting of 5% in weight of Gohsenol KP-08R commercialized by ’Nippon Gohsei’ dissolved in 95% pure ethanol, cf. Figure B.1, was simply applied to the model with a soft brush before testing. The coating was applied with a few hours interval of effective testing, since its effect decreased after intensive wet testing.

Figure B.1: Polyvinyl alcohol solution for improving cable wettability
An illustration of the effect of the polyvinyl alcohol coating is given in Figure B.2 and Figure 4.19, where the coating was applied to the upper cable leaving the lower cable untreated. Water was sprayed on the cables for comparison. It is seen how the water was repelled by the untreated lower cable and smaller droplets were formed, while the water was distributed in a fine layer over a wider surface of the treated upper cable.

**Figure B.2:** Response of water to the application of polyvinyl alcohol coating; treated upper cable and untreated lower cable
Flow profiles for Climatic Wind Tunnel with totally closed test section established by ‘FORCE Technology’ are presented here as flow documentation.

Figure C.1: Flow profiles for the CWT with closed test section
Appendix D

Calibration of Instruments

Table D.1 reports calibration constants from voltage to the specific unit for the instruments used in the data acquisition systems for passive-dynamic tests of the twin-cable model in CWT at 'FORCE Technology'. Calibration factors of reference data, i.e. pressures, temperature and relative humidity, were determined for the probes located at the specific position in CWT described in 4.3.1. On the contrary, conversion factors for laser transducers were directly taken from the producer specification, cf. (WayCon Positionsmesstechnik, 2012), and verified before testing.

<table>
<thead>
<tr>
<th>Acquired quantity</th>
<th>Conversion formula</th>
<th>Final unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>Dynamic differential pressure</td>
<td>$P_{14} = \text{Volt} \cdot 99.9552$</td>
<td>Pa</td>
</tr>
<tr>
<td>Static differential pressure</td>
<td>$P_{15} = \text{Volt} \cdot 100.0199$</td>
<td>Pa</td>
</tr>
<tr>
<td>Temperature</td>
<td>$T = \text{Volt} \cdot 9 + 20$</td>
<td>$^\circ\text{C}$</td>
</tr>
<tr>
<td>Relative humidity</td>
<td>$RH = \text{Volt} \cdot 10$</td>
<td>%</td>
</tr>
<tr>
<td>Vertical laser - right side</td>
<td>$V_{\text{right}} = \text{Volt} \cdot 25 + 50$</td>
<td>mm</td>
</tr>
<tr>
<td>Vertical laser - left side</td>
<td>$V_{\text{left}} = \text{Volt} \cdot 25 + 50$</td>
<td>mm</td>
</tr>
<tr>
<td>Horizontal laser - right side</td>
<td>$H_{\text{right}} = \text{Volt} \cdot 50 + 100$</td>
<td>mm</td>
</tr>
<tr>
<td>Horizontal laser - left side</td>
<td>$H_{\text{left}} = \text{Volt} \cdot 50 + 100$</td>
<td>mm</td>
</tr>
</tbody>
</table>
Before conducting any test the model was calibrated tuning the springs of the 2-DoF rig and its own structural characteristics i.e. mass and frequencies determined. The target objective was to attribute to the model the proper natural frequencies in the two main plain so that values determined from full-scale monitoring, cf. Table 3.1, were scaled according to the 'Froude' similarity as explained in section 4.1.3. Frequencies were directly calculated from the displacement time histories considering the number of cycles over time. In-plane frequency was calculated accounting for the five model excitations at zero wind velocity in the in-plane direction; the same was performed for the orthogonal direction, out-of-plane. Naturally, the frequency directly calculated following this procedure was the frequency of a damped system, and not its natural one. Despite this fact, the natural frequency could be back-calculated as:

\[
\omega_d = \omega_0 \cdot \sqrt{1 - \zeta_s^2}
\]  

(E.1)

The following tables report results of natural frequencies calculation of the twin-cable model tested dynamically in CWT. Both conditions dry and wet were considered. Note that reported values for \(\zeta_s\) are related to the model and they do not represent full-scale values. Tables E.1-E.4 clearly show that \(\omega_0\) and \(\omega_d\) or \(f_0\) and \(f_d\) are the same in all tested cases because of the extremely low values of the rig damping \(\zeta_s\).

**Table E.1: Frequency calculation - Dry condition - In-plane direction**

<table>
<thead>
<tr>
<th>Repetition</th>
<th>(f_d)</th>
<th>(\zeta_s)</th>
<th>(f_0)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1(^{st})</td>
<td>0.86138 Hz</td>
<td>0.002079</td>
<td>0.861382 Hz</td>
</tr>
<tr>
<td>2(^{nd})</td>
<td>0.86066 Hz</td>
<td>0.002173</td>
<td>0.860662 Hz</td>
</tr>
<tr>
<td>3(^{rd})</td>
<td>0.86138 Hz</td>
<td>0.002063</td>
<td>0.861382 Hz</td>
</tr>
<tr>
<td>4(^{th})</td>
<td>0.86138 Hz</td>
<td>0.002059</td>
<td>0.861382 Hz</td>
</tr>
<tr>
<td>5(^{th})</td>
<td>0.86066 Hz</td>
<td>0.002133</td>
<td>0.860662 Hz</td>
</tr>
<tr>
<td><strong>Average</strong></td>
<td><strong>0.861 Hz</strong></td>
<td><strong>0.210%</strong></td>
<td><strong>0.861 Hz</strong></td>
</tr>
</tbody>
</table>
Table E.2: Frequency calculation - Dry condition - Out-of-plane direction

<table>
<thead>
<tr>
<th>Repetition</th>
<th>$f_d$</th>
<th>$\zeta_s$</th>
<th>$f_0$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1$^{st}$</td>
<td>0.85702Hz</td>
<td>0.003514</td>
<td>0.857025Hz</td>
</tr>
<tr>
<td>2$^{nd}$</td>
<td>0.85784Hz</td>
<td>0.003366</td>
<td>0.857845Hz</td>
</tr>
<tr>
<td>3$^{rd}$</td>
<td>0.85702Hz</td>
<td>0.003255</td>
<td>0.857025Hz</td>
</tr>
<tr>
<td>4$^{th}$</td>
<td>0.85495Hz</td>
<td>0.003189</td>
<td>0.854956Hz</td>
</tr>
<tr>
<td>5$^{th}$</td>
<td>0.85784Hz</td>
<td>0.003189</td>
<td>0.857844Hz</td>
</tr>
<tr>
<td>Average</td>
<td>0.857Hz</td>
<td>0.339%</td>
<td>0.857Hz</td>
</tr>
</tbody>
</table>

Table E.3: Frequency calculation - Wet condition - In-plane direction

<table>
<thead>
<tr>
<th>Repetition</th>
<th>$f_d$</th>
<th>$\zeta_s$</th>
<th>$f_0$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1$^{st}$</td>
<td>0.85324Hz</td>
<td>0.002148</td>
<td>0.853242Hz</td>
</tr>
<tr>
<td>2$^{nd}$</td>
<td>0.85417Hz</td>
<td>0.002047</td>
<td>0.854172Hz</td>
</tr>
<tr>
<td>3$^{rd}$</td>
<td>0.85417Hz</td>
<td>0.002072</td>
<td>0.854172Hz</td>
</tr>
<tr>
<td>4$^{th}$</td>
<td>0.85377Hz</td>
<td>0.002055</td>
<td>0.853772Hz</td>
</tr>
<tr>
<td>5$^{th}$</td>
<td>0.85417Hz</td>
<td>0.001996</td>
<td>0.854172Hz</td>
</tr>
<tr>
<td>Average</td>
<td>0.854Hz</td>
<td>0.206%</td>
<td>0.854Hz</td>
</tr>
</tbody>
</table>

Table E.4: Frequency calculation - Wet condition - Out-of-plane direction

<table>
<thead>
<tr>
<th>Repetition</th>
<th>$f_d$</th>
<th>$\zeta_s$</th>
<th>$f_0$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1$^{st}$</td>
<td>0.84890Hz</td>
<td>0.002939</td>
<td>0.848904Hz</td>
</tr>
<tr>
<td>2$^{nd}$</td>
<td>0.84851Hz</td>
<td>0.002608</td>
<td>0.848513Hz</td>
</tr>
<tr>
<td>3$^{rd}$</td>
<td>0.84951Hz</td>
<td>0.002377</td>
<td>0.849512Hz</td>
</tr>
<tr>
<td>4$^{th}$</td>
<td>0.84851Hz</td>
<td>0.002435</td>
<td>0.848513Hz</td>
</tr>
<tr>
<td>5$^{th}$</td>
<td>0.84951Hz</td>
<td>0.002403</td>
<td>0.849512Hz</td>
</tr>
<tr>
<td>Average</td>
<td>0.849Hz</td>
<td>0.255%</td>
<td>0.849Hz</td>
</tr>
</tbody>
</table>

As stated before, cf. appendix A, mass of the model was not scaled according to the 'Froude' similarity law, because effort was directed in reducing the mass as much as possible to enhance the pure aerodynamic behaviour or model response, keeping the 'Scruton' number $Sc$ defined in (4.3) to the lower possible value. The weight of the model considering PVC tubes, triangular end connections and threaded bars for the attachment to the rig was found to be $M_{tub} = 26.2kg$. A precision scale was adopted to estimate the weight of the rig springs together with their relative fittings. Results in term of spring weight are reported in Table E.5.

The total mass of the model was calculated considering the contribution from tubes and springs. Note that different spring masses were contributing to the overall model mass when it was moving in the two perpendicular directions. For values of hanging mass over spring mass larger than three, the contribution of the spring mass to the overall moving mass was reduced to one third, cf. (Yost, 2002). Of course, for this to be valid, springs has to be linear-elastic. A linearity check of the springs was performed at 'DTU.Byg' laboratory. Springs were installed one by one on a testing
Table E.5: Dynamic rig spring weights

<table>
<thead>
<tr>
<th>Spring position</th>
<th>Left side [kg]</th>
<th>Right side [kg]</th>
</tr>
</thead>
<tbody>
<tr>
<td>In-plane</td>
<td>1.166</td>
<td>1.425</td>
</tr>
<tr>
<td>Out-of-plane (upstream)</td>
<td>0.691</td>
<td>0.704</td>
</tr>
<tr>
<td>Out-of-plane (downstream)</td>
<td>0.510</td>
<td>0.507</td>
</tr>
</tbody>
</table>

pulling machine normally used for tensile tests, and a controlled force was applied and constantly increased. A linear increase in the displacement was observed in all cases denoting non-dependency of spring stiffness on the force or displacement imposed. Therefore two model masses, one for the in-plane and the other for the out-of-plane oscillations were determined as (E.2) accounting for a third of the spring mass in the computed direction and half of the spring mass in the opposite direction.

\[
M_{\text{in-plane}} = M_{\text{tub}} + \frac{1}{3} M_{\text{sp(in-plane)}} + \frac{1}{2} M_{\text{sp(out-of-plane)}} = 28.27 kg \quad (E.2a)
\]

\[
M_{\text{out-of-plane}} = M_{\text{tub}} + \frac{1}{2} M_{\text{sp(in-plane)}} + \frac{1}{3} M_{\text{sp(out-of-plane)}} = 28.30 kg \quad (E.2b)
\]

Based on the results of frequency and mass calculations it was possible to compute the stiffness of the model installed on the dynamic rig, using (E.3). Stiffness calculations were based only on frequencies from the dry condition, because the reduction in frequency in the wet case was simply due to the weight of the water added to the model and not to a change in its stiffness.

\[
k_{\text{in-plane}} = 4\pi^2 \cdot f_{\text{in-plane}}^2 \cdot M_{\text{in-plane}} = 827.535 N/m \quad (E.3a)
\]

\[
k_{\text{out-of-plane}} = 4\pi^2 \cdot f_{\text{out-of-plane}}^2 \cdot M_{\text{out-of-plane}} = 820.438 N/m \quad (E.3b)
\]

Based on the hypothesis illustrated above i.e. the added water in wet tests was causing a change in frequency, the amount of water in terms of mass was estimated as:

\[
M_{w(\text{in-plane})} = \frac{k_{\text{in-plane}}}{4\pi^2 \cdot f_{\text{wet(in-plane)}}^2} - M_{\text{in-plane}} = 0.478 Kg \quad (E.4a)
\]

\[
M_{w(\text{out-of-plane})} = \frac{k_{\text{out-of-plane}}}{4\pi^2 \cdot f_{\text{wet(out-of-plane)}}^2} - M_{\text{out-of-plane}} = 0.532 Kg \quad (E.4b)
\]

Note that the mass of the water referred to the condition zero wind velocity, where the model surface was wet and a lower rivulet formed on both pipes, but not along the whole length. The quantity of water was changing with the velocity, but the calculation of its mass was no longer performed due the increasing uncertainty on the methodology adopted for zero velocity. In fact, a coupling between the two modes was observed and damping estimation became less precise at higher wind speeds.
A selection of representative displacement responses for both investigated dry and wet conditions is presented in the following. The first test repetition for the velocities $U = 0 \text{RPM} = 0\text{m/s}$, $U = 300\text{RPM} \approx 5.9\text{m/s}$, $U = 600\text{RPM} \approx 11.9\text{m/s}$, $U = 900\text{RPM} \approx 17.8\text{m/s}$, $U = 1200\text{RPM} \approx 23.8\text{m/s}$ and $U = 1500\text{RPM} \approx 29.7\text{m/s}$ is considered.

The displacement responses are presented in the order:

- In-plane motion - dry condition.
- Out-of-plane motion - dry condition.
- In-plane motion - wet condition.
- Out-of-plane motion - wet condition.
F.1 In-plane motion - dry condition

Figure F.1: Displacement responses for in-plane motion at $U = 0 \text{ RPM} = 0 \text{ m/s}$ in dry condition

Figure F.2: Displacement responses for in-plane motion at $U = 300 \text{ RPM} \approx 5.9 \text{ m/s}$ in dry condition

Figure F.3: Displacement responses for in-plane motion at $U = 600 \text{ RPM} \approx 11.9 \text{ m/s}$ in dry condition
F.1. In-plane motion - dry condition

Figure F.4: Displacement responses for in-plane motion at $U = 900\text{RPM} \approx 17.8\text{m/s}$ in dry condition

Figure F.5: Displacement responses for in-plane motion at $U = 1200\text{RPM} \approx 23.8\text{m/s}$ in dry condition

Figure F.6: Displacement responses for in-plane motion at $U = 1500\text{RPM} \approx 29.7\text{m/s}$ in dry condition

Note that the initial excitation did not exceed the buffeting limit for the first repetition at $U = 1500\text{RPM} \approx 29.7\text{m/s}$. Therefore, the damping estimation was not performed, as illustrated in Figure F.6.
F.2 Out-of-plane motion - dry condition

**Figure F.7:** Displacement responses for out-of-plane motion at $U = 0\, \text{RPM} = 0\, \text{m/s}$ in dry condition

**Figure F.8:** Displacement responses for out-of-plane motion at $U = 300\, \text{RPM} \approx 5.9\, \text{m/s}$ in dry condition

**Figure F.9:** Displacement responses for out-of-plane motion at $U = 600\, \text{RPM} \approx 11.9\, \text{m/s}$ in dry condition
F.2. Out-of-plane motion - dry condition

Figure F.10: Displacement responses for out-of-plane motion at $U = 900\text{RPM} \approx 17.8\text{m/s}$ in dry condition

Figure F.11: Displacement responses for out-of-plane motion at $U = 1200\text{RPM} \approx 23.8\text{m/s}$ in dry condition

Figure F.12: Displacement responses for out-of-plane motion at $U = 1500\text{RPM} \approx 29.7\text{m/s}$ in dry condition

Note that the initial excitation for the first repetition at $U = 1500\text{RPM} \approx 29.7\text{m/s}$ actually did exceed the buffeting limit, even though this is not immediately seen from Figure F.12. The reason is that the damping was calculated for a very narrow range just above the buffeting limit. This becomes visible by zooming in the plot.
F.3 In-plane motion - wet condition

Figure F.13: Displacement responses for in-plane motion at $U = 0\text{RPM} = 0\text{m/s}$ in wet condition

Figure F.14: Displacement responses for in-plane motion at $U = 300\text{RPM} \approx 5.9\text{m/s}$ in wet condition

Figure F.15: Displacement responses for in-plane motion at $U = 600\text{RPM} \approx 11.9\text{m/s}$ in wet condition
F.3. In-plane motion - wet condition

Figure F.16: Displacement responses for in-plane motion at $U = 900\text{RPM} \approx 17.8m/s$ in wet condition

Figure F.17: Displacement responses for in-plane motion at $U = 1200\text{RPM} \approx 23.8m/s$ in wet condition

Figure F.18: Displacement responses for in-plane motion at $U = 1500\text{RPM} \approx 29.7m/s$ in wet condition
F.4 Out-of-plane motion - wet condition

Figure F.19: Displacement responses for out-of-plane motion at $U = 0 RPM = 0m/s$ in wet condition

Figure F.20: Displacement responses for out-of-plane motion at $U = 300 RPM \approx 5.9 m/s$ in wet condition

Figure F.21: Displacement responses for out-of-plane motion at $U = 600 RPM \approx 11.9 m/s$ in wet condition
F.4. Out-of-plane motion - wet condition

Figure F.22: Displacement responses for out-of-plane motion at $U = 900\text{RPM} \approx 17.8 \text{m/s}$ in wet condition

Figure F.23: Displacement responses for out-of-plane motion at $U = 1200\text{RPM} \approx 23.8 \text{m/s}$ in wet condition

Figure F.24: Displacement responses for out-of-plane motion at $U = 1500\text{RPM} \approx 29.7 \text{m/s}$ in wet condition
F.4. Out-of-plane motion - wet condition
In the following, the implementation of the used blockage correction for drag force coefficient is presented. It was derived from the theory of Maskell (1965) for static tests closed wind tunnel. Cooper et al. (1999) improved blockage corrections for closed wind tunnels including methods when dealing with open test section. Hackett and Cooper (2001) implemented the so called ‘Maskell III’ method which was adopted here. Note that blockage correction methods do not include moving structure-fluid interaction and aero-elastic effects which may be important in dynamic tests. Nevertheless, forces and then force coefficients were calculated based on registered static mean displacements of the model mounted on a dynamic rig. Therefore the analogy with static wind tunnel tests, regarding blockage correction, was considered realistic.

The implementation of the ‘Maskell III’ method required few input parameters which mainly referred to the test set-up geometry. Model height or length, width and thickness were initially inserted. Height was set equal to \( h = w_{CWT}/\cos(\theta) = 2.3 \text{m} \), cf. section 4.3.4, while width and thickness both corresponded to two cable diameters \( 2D \). Model frontal area was then \( A_{model} = 0.506m^2 \) and test section area was \( A_{CWT} = 4m^2 \) as reported in Table 4.1. The aspect ratio was defined as \( AS = 2D/h \).

Firstly the wake blockage correction was applied. Maskell’s empirical blockage factor was defined as:

\[
\chi = \vartheta \cdot \frac{A_{model}}{A_{CWT}} \cdot C_{D-u}
\]

where \( \vartheta = 0.96 + 1.94 \cdot e^{-0.06 \cdot AR} \) and \( C_{D-u} \) the drag force coefficient blockage uncorrected.

The original Maskell correction was defined as:

\[
C_{D-\text{Maskell}} = \frac{C_{D-u}}{1 + \chi}
\]

\[
\Delta_{CDM} = C_{D-u} \cdot \left( \frac{1}{1 + \chi} + \frac{1}{2\chi} \right) \cdot \left( 1 - (1 + 4\chi)^{0.5} \right)
\]
Dynamic pressure correction due to wake blockage was set to:

\[ p_{wb} = 1 + \vartheta \cdot \frac{A_{\text{model}}}{A_{\text{CWT}}} \cdot (C_{D-\text{Maskell}} - \Delta_{CDM}) \]  
\[ (G.3) \]

The corrected drag coefficient due to wake blockage was then established equal to:

\[ C_{D-wbc} = \frac{C_{D-u}}{1 + \vartheta \cdot \frac{A_{\text{model}}}{A_{\text{CWT}}} \cdot (C_{D-\text{Maskell}} - \Delta_{CDM}) + \Delta_{CDM}} \]  
\[ (G.4) \]

Secondly solid blockage correction had to be considered, once wake blockage correction was computed. In particular two dimensional solid blockage correction was considered through the coefficients:

\[ k_{2s} = \frac{\pi^2}{12}, \quad \beta = 1 \]  
\[ (G.5a) \]

\[ \lambda_{2s} = \frac{2 \cdot A_{\text{cross}}}{\pi \cdot t \cdot c} \cdot \left( \frac{c}{t} + 1.2 \cdot \beta \right) \]  
\[ (G.5b) \]

\[ \epsilon_{2s} = \frac{k_{2s}}{\beta^3} \cdot \left( \frac{t}{h_{\text{CWT}}} \right)^2 \cdot \lambda_{2s} \]  
\[ (G.5c) \]

where the model thickness was set to \( t = D \), the model chord was \( c = 2D \) and model cross section was \( A_{\text{cross}} = 2 \cdot D^2 \pi / 4 \).

Dynamic pressure correction due to solid blockage was defined as:

\[ p_{sb} = (1 + \epsilon_{2s})^2 \]  
\[ (G.6) \]

The corrected dynamic pressure considering wake and solid blockage was then:

\[ p_{\text{tot}} = p_{wb} + p_{sb} - 1 \]  
\[ (G.7) \]

Finally the drag coefficient corrected for both solid and wake blockage was expressed by:

\[ C_{D-swb} = \frac{C_{D-u}}{p_{\text{tot}} + \Delta_{CDM}} \]  
\[ (G.8) \]