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Experimental testing and validation of a full-scale high-bandwidth morphing aileron demonstrator

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Abstract

To meet increasingly stringent sustainability requirements, the aeronautical sector is exploring novel aerodynamic configurations to reduce emissions. This thesis was developed within the European HERWINGT project and investigates the use of morphing ailerons. These devices aim to reduce parasitic drag and decrease actuation forces, while simultaneously offering potential for active control applications.

The research focuses on a full scale demonstrator equipped with deformable S-glass fibre skins, driven by electromagnetic motors via Scott-Russell mechanisms. The core work supports prototype development alongside experimental validation and testing. The internal friction of the actuation system was initially characterised using a Coulomb-viscous model before being integrated into the control logic. Subsequently, ground test campaigns were conducted.

Static testing demonstrated that the prototype successfully achieves the target aerodynamic profiles in agreement with numerical models. Furthermore, static actuation forces isolated from friction components exhibited strong consistency with numerical predictions. Dynamic tests performed up to 10 Hz identified the presence of a natural mode within the frequency band of interest. This mode allows for a reduction in actuation force compared to maintaining static deflections. Additionally, the morphing surface demonstrated the ability to follow the required deflection command without delays, confirming the suitability of the architecture for dynamic applications.

Finally, the wind tunnel campaign is presented. Alongside the test setup, the text reports preliminary results concerning aerodynamics and forces under load, concluding with the future developments of the research.

Keywords: Morphing ailerons, Compliant structures, Experimental validation, Ground tests, Wind tunnel tests.

Abstract in lingua italiana

Per soddisfare i requisiti sempre più stringenti di sostenibilità, il settore aeronautico sta attivamente esplorando nuove configurazioni aerodinamiche per ridurre le emissioni. Questa tesi è stata sviluppata nell'ambito del progetto europeo HERWINGT e indaga l'utilizzo di alettoni morphing. Tali dispositivi mirano a ridurre la resistenza parassita e a diminuire le forze di attuazione, offrendo al contempo nuove prospettive per le applicazioni di controllo attivo.

La ricerca si concentra su un dimostratore in scala reale, dotato di rivestimenti deformabili in fibra di vetro S, azionati da motori elettromagnetici tramite meccanismi di Scott-Russell. Il progetto di tesi consiste nel supportare lo sviluppo del prototipo, parallelamente alla validazione e alle prove sperimentali. L'attrito interno del sistema di attuazione è stato inizialmente caratterizzato mediante un modello Coulomb-viscoso, per poi essere integrato nella logica di controllo. Successivamente, sono state condotte campagne di prove a terra in assenza di vento.

Le prove statiche hanno dimostrato che il prototipo raggiunge con successo i profili aerodinamici obiettivo, in accordo con i modelli numerici. Inoltre, le forze di attuazione statica, al netto delle componenti di attrito, hanno mostrato un'ottima coerenza con le previsioni numeriche. Le prove dinamiche, eseguite fino a 10 Hz, hanno individuato la presenza di un modo proprio nella banda di interesse, che consente di ridurre la forza di attuazione rispetto al mantenimento delle deflessioni statiche. Inoltre, la superficie morphing ha dimostrato la capacità di seguire il comando di deflessione richiesto senza ritardi, confermando l'idoneità dell'architettura per le applicazioni dinamiche.

Infine, vengono presentate le campagne in galleria del vento. Oltre all'assetto di prova, si riportano i risultati preliminari su aerodinamica e forze sotto carico, concludendo con gli sviluppi futuri della ricerca.

Parole chiave: Alettoni morphing, Strutture deformabili, Validazione sperimentale, Prove a terra, Prove in galleria del vento.

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List of Symbols

Variable	Description	Unit
A	Oscillation amplitude	[m]
a_a	Actual acceleration	[m/s ²]
C_d	Drag coefficient	[-]
C_l	Lift coefficient	[-]
dt	Time step for signal interpolation	[s]
F_{aer}	Aerodynamic force component along the actuator axis	[N]
F_c	Coulomb friction force	[N]
F_{el}	Elastic restoring force	[N]
F_f	Friction force	[N]
F_{fund}	Fundamental amplitude of the friction force	[N]
F_g	Gravitational force component	[N]
F_{in}	Inertial force	[N]
F_m	Motor force	[N]
$F_{m, est.}$	Estimated motor force	[N]
$F_{m, wf}$	Friction-deducted motor force	[N]
F_s	Actuation force required to slide the lower skin	[N]
F_v	Viscous friction coefficient	[N/(m/s)]

Variable	Description	Unit
f	Excitation frequency	[Hz]
g	Gravitational acceleration	[m/s ²]
h	Altitude	[m]
k	Reduced frequency	[-]
L_1	Length of Scott-Russell rod 1	[m]
M	Mach number	[-]
m	Slope of the linear regression	[N/(m/s)]
m_{rod}	Mass of each single rod	[kg]
m_{slider}	Mass of the moving part of the motor	[kg]
q	Intercept of the linear regression	[N]
R^2	Coefficient of determination	[-]
T_g	Glass transition temperature	[°C]
t	Time	[s]
u_q	Linear displacement of the lower aileron skin measured optically	[m]
V	Maximum velocity amplitude	[m/s]
v	Velocity	[m/s]
v_a	Actual velocity	[m/s]
v_d	Demand velocity	[m/s]
V_{fund}	Fundamental amplitude of the velocity	[m/s]
w	Out-of-plane displacement (DIC contour plot)	[m]
x_a	Actual motor position / stroke	[m]
x_b, y_b, z_b	Body axes	[-]
x_d	Demand motor position / stroke	[m]

Variable	Description	Unit
x_q	Actual linear displacement of the motor slider measured optically	[m]
$z_{tip,q}$	Out-of-plane displacement of the wing-box tip measured optically	[m]
α	Angle of attack	[°]
$\Delta\beta$	Variation of the boat-tail angle	[°]
Δx_A	Horizontal displacement of point A in the Scott-Russell mechanism	[m]
Δy_C	Vertical displacement of point C in the Scott-Russell mechanism	[m]
δ_a	Actual aileron rotation	[°]
δ_d	Demand aileron rotation	[°]
δ_q	Effective rotation angle measured optically	[°]
δ	Equivalent trailing-edge rotation	[°]
θ	Angle between rod and shaft axis	[°]
θ_0	Angle between rod 2 and shaft axis in neutral position	[°]
ω	Angular frequency	[rad/s]

List of Abbreviations

Acronym	Meaning
ACTE	Adaptive Compliant Trailing Edge
AFRL	Air Force Research Laboratory
AFTI	Advanced Fighter Technology Integration
ATED	Adaptive Trailing Edge Device
BNC	Bayonet Neill–Concelman
CAD	Computer-Aided Design
CAEP	Committee on Aviation Environmental Protection
CFD	Computational Fluid Dynamics
CFRP	Carbon Fibre-Reinforced Polymers
CO ₂	Carbon Dioxide
CST	Class/Shape function Transformation
DAC	Digital-to-Analogue Converter
DIC	Digital Image Correlation
DOE	Design of Experiments
EAPs	Electroactive Polymers
EASA	European Union Aviation Safety Agency
EMA	Electro-Mechanical Actuator
EMCs	Elastomeric Matrix Composites

Acronym	Meaning
EMI	Electromagnetic Interference
EU27+EFTA	European Union and European Free Trade Association
FEA	Finite Element Analysis
FEM	Finite Element Method
FF	Feed-Forward
FFT	Fast Fourier Transform
FishBAC	Fish Bone Active Camber
FMCs	Flexible Matrix Composites
FRF	Frequency Response Function
FRPs	Fibre-Reinforced Polymers
GFRP	Glass Fibre-Reinforced Polymers
GVT	Ground Vibration Tests
GVPM	Galleria del Vento Politecnico di Milano
HERA	Hybrid-Electric Regional Aircraft
HERWINGT	Hybrid Electric Regional Wing Integration Novel Green Technologies
HMI	Human-Machine Interface
ICAO	International Civil Aviation Organization
IPCC	Intergovernmental Panel on Climate Change
LCA	Life Cycle Assessment
LED	Light-Emitting Diode
MACW	Mission Adaptive Compliant Wing
MAW	Mission Adaptive Wing
MES	More Electric Systems

Acronym	Meaning
NASA	National Aeronautics and Space Administration
NO _x	Nitrogen Oxides
PID	Proportional-Integral-Derivative
PIV	Particle Image Velocimetry
PLC	Programmable Logic Controller
PSU	Power Supply Unit
PZTs	Piezoelectric Materials
RANS	Reynolds-Averaged Navier-Stokes
RBF	Radial Basis Functions
SAF	Sustainable Aviation Fuels
SARISTU	Smart Intelligent Aircraft Structures
SHM	Structural Health Monitoring
SMA _s	Shape Memory Alloys
SMP _s	Shape Memory Polymers
SMS	Smart Morphing & Sensing
SoA	State-of-the-Art
SO _x	Sulfur Oxides
STO	Safe Torque Off
TRIC	Translation Induced Camber
TRL	Technology Readiness Level
USAF	United States Air Force
VAC	Volts Alternating Current
VDC	Volts Direct Current

Acronym	Meaning
WMO	World Meteorological Organization

1 | Introduction

The strong push for sustainable aviation is driving a major technological leap in aerospace engineering. As conventional aerodynamic configurations approach their theoretical limits, the industry is turning towards disruptive technologies capable of redefining flight efficiency. Among these, morphing structures stand out as one of the most promising solutions for next-generation aircraft. This work falls within this innovative framework, specifically contributing to the European Union funded project Hybrid Electric Regional Wing Integration Novel Green Technologies (HERWINGT) [2, 19]. This thesis contributes to the manufacturing and assembly phases of a full-scale morphing aileron prototype, as well as to its experimental validation and ground testing activities, including the preparation and preliminary analyses of the wind tunnel test campaign.

1.1. Context and motivation

To fully understand the relevance of the developed prototype, it is essential to outline the current aeronautical context. This section reviews the stringent ecological targets driving modern research on morphing elements and examines the evolution of control surface design. By analysing the inherent drawbacks of traditional hinged mechanisms, the discussion will highlight why continuous-shape adaptations are currently investigated as the future standard for flight control.

1.1.1. Environmental challenges and aviation sustainability

The relentless pursuit of efficiency and sustainability in the aviation sector is driven by a complex interplay of environmental, economic, and technological factors. This transition is guided by ambitious regulatory frameworks and long-term visions, such as the European Green Deal [20] and Flightpath 2050 [21], alongside major research and innovation programs like the Clean Aviation Joint Undertaking. These initiatives aim to transform aviation by establishing collective global objectives to mitigate the sector's footprint.

The urgency of this paradigm shift is underscored by recent reports from the Intergovern-

mental Panel on Climate Change (IPCC), World Meteorological Organization (WMO), and Copernicus. These studies highlight rapid and widespread climate changes, revealing that Europe is currently warming at twice the global average rate, making it the fastest-warming continent [18]. In response to this undeniable reality, the International Civil Aviation Organization (ICAO) targets net-zero carbon emissions for international aviation by 2050, alongside an intermediate goal of reducing CO₂ emissions by 5% by 2030 through the expansion of Sustainable Aviation Fuels (SAF) and clean energy initiatives.

Although aviation accounted for approximately 2.5% of global CO₂ emissions in 2023, and represented 12% of total transport greenhouse gas emissions alongside 4% of all greenhouse gas emissions within the EU27+EFTA (European Union and European Free Trade Association countries) area in 2022, its overall climate impact is significantly more severe. The sector's true environmental footprint is a combination of both CO₂ and non-CO₂ emissions, including nitrogen oxides (NO_x), particulate matter, sulphur oxides (SO_x), water vapour, and contrail-cirrus formation.

In addition to climate neutrality and the reduction of chemical pollutants, another major challenge addressed by European visions like Flightpath 2050 is acoustic pollution. Coupled with the projected long-term growth of air traffic volumes, expected to reach up to 13.8 million flights in Europe by 2050 (as presented in Figure 1.1), both environmental and noise pollution are poised to worsen significantly without immediate mitigation. To address this compounded threat, ICAO's Committee on Aviation Environmental Protection (CAEP) agreed in early 2025 on stricter certification standards specifically targeting aircraft noise, alongside engine emissions. These stringent constraints will be decisive for the design of new aircraft and for contributing to future sustainability goals.

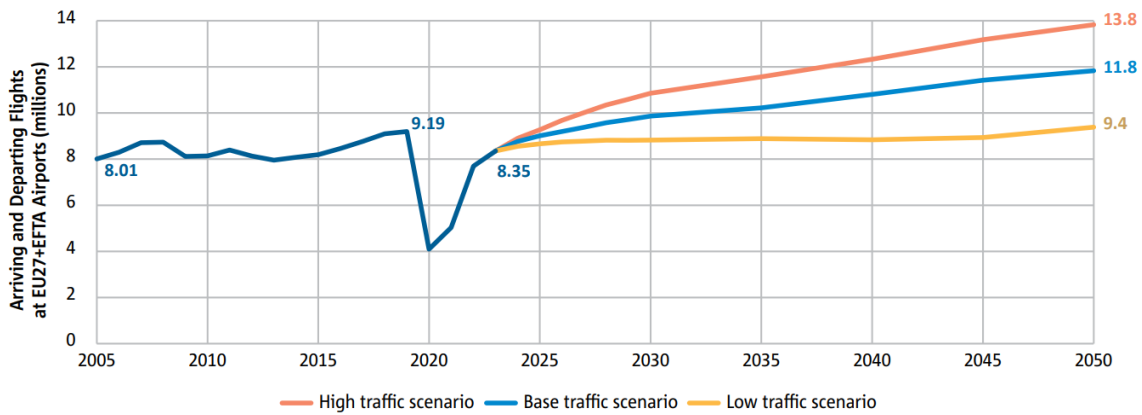


Figure 1.1: Historical and forecasted flight departures and arrivals in the EU27+EFTA region [18].

However, aerial vehicles are highly complex machines, and mitigating their environmental impact requires multi-objective approaches that tackle aircraft design from various interdependent disciplines. While the development of lightweight structures and the integration of hybrid-electric propulsion systems represent fundamental steps, maximising aerodynamic efficiency remains a mandatory priority. A highly effective way to achieve this is by minimising the overall aerodynamic drag acting on the vehicle, which translates directly into lower fuel consumption and emissions.

Since conventional aerodynamic configurations are approaching their theoretical optimisation limits, engineers are pushing the boundaries of wing design. One of the primary strategies currently being investigated to minimise induced drag is the adoption of ultra-high aspect ratio wings. Consequently, discovering new aerodynamic configurations capable of adapting to varying flight conditions, managing structural loads efficiently, and reducing overall resistance is currently driving the paradigm shift for newly designed airframes.

1.1.2. Limitations of conventional hinged ailerons

Despite this push for revolutionary wing designs, the fundamental mechanical architecture used to control flight dynamics, such as conventional hinged ailerons, has remained largely unchanged for decades.

The ailerons are aerodynamic control surfaces located along the trailing edges near the wingtips. They provide lateral control of the aircraft, with spoilers contributing on many aircraft, enabling rotation about the longitudinal axis [37]. To produce a roll, the ailerons are deflected asymmetrically: one aileron moves downward, increasing local camber and lift, while the other moves upward, reducing the local lift. This difference in lift along the wingspan generates a rolling moment about the longitudinal axis, inducing the roll rate necessary to execute the manoeuvre [31].

From a geometrical and constructional point of view, ailerons are identical to plain flaps, with the substantial difference that they can also be deflected upward. They consist of rigid surfaces connected to the wing structure through structural hinges. To maximise the roll moment arm and increase control effectiveness (control power), they are positioned as close as possible to the wingtips. Typically, their spanwise extent covers 20%–30% of the total wingspan, while their chord (aileron depth) represents approximately 15%–25% of the local wing chord.

The sizing and effectiveness of ailerons are dictated by airworthiness regulations. For example, the European certification EASA CS 25.147(d) evaluates roll capability, requiring

that a transport aircraft be able to reverse a turn from a 30° bank angle on one side to a 30° bank angle on the opposite side in no more than 11 s [22].

Although conventional hinged control surfaces have demonstrated high reliability and certification maturity, their discrete mechanical architecture introduces intrinsic limitations that become increasingly critical for next-generation high-efficiency aircraft.

Before detailing these aspects, it is important to note that conventional control surfaces are optimised for specific operating conditions, leading to suboptimal aerodynamic performance across off-design flight regimes.

The main drawbacks can be grouped into four main categories:

- **Aerodynamic discontinuities:** the deflection of a rigid hinged control surface introduces geometric discontinuities at the interface between the fixed wing structure and the movable element. During actuation, unavoidable gaps and abrupt curvature variations are generated along the trailing edge. These geometric irregularities disturb the local boundary layer development, promoting premature flow separation and the formation of recirculation regions. At moderate to high deflection angles (typically up to $\pm 30^\circ$), the flow may exhibit abrupt stall behaviour, characterised by a sudden loss of lift linearity and a rapid increase in profile drag. This aerodynamic degradation directly compromises control effectiveness, reducing roll authority precisely under conditions where higher aerodynamic loads and control power are required [4, 12, 15, 44].
- **Actuation and hinge moment penalties:** the rigid rotation of a control surface in high-speed airflow generates substantial aerodynamic hinge moments. The actuation system must overcome not only these aerodynamic loads but also the inertial loads associated with the mass of the surface itself. Because loads are transferred through discrete hinge points, local structural reinforcement is necessary, increasing weight and complexity [24]. Consequently, hydraulic or electromechanical actuators must be sized to accommodate peak hinge moments, leading to oversized systems, higher energy consumption, and an overall weight penalty that negatively impacts aircraft efficiency.
- **Structural integration constraints:** as future transport aircraft adopt high aspect ratio wings to minimise induced drag, the internal volume available in the outboard sections is reduced. The mechanical architecture required for conventional hinged rotation (including hinge brackets, load transfer fittings, bearings, and actuator interfaces) demands significant spatial allocation and concentrated structural

reinforcements. Integrating these bulky mechanisms within extremely thin wing profiles is geometrically challenging, often necessitating external fairings or structural compromises that introduce parasitic drag and weight penalties.

- **Aeroacoustic penalties:** the geometric gaps and exposed edges associated with hinged mechanisms act as sources of vorticity and broadband aerodynamic noise. The separated and highly turbulent flow generated near these discontinuities contributes significantly to airframe noise, particularly also during approach and landing phases. In the framework of increasingly stringent environmental noise regulations, the acoustic penalty of discontinuous control surfaces represents a non-negligible technological constraint.

These intrinsic limitations highlight the need for alternative control architectures capable of providing smooth aerodynamic shaping, distributed load transfer, and reduced actuation penalties: requirements that motivate the exploration of morphing technologies.

1.1.3. Morphing in aviation

An important technology under investigation in aircraft design is the morphing concept. Originally, this solution was proposed to improve aircraft aerodynamic performance during take-off, landing, and cruise; however, it is now also under investigation for flutter suppression and load alleviation [9, 26, 45]. This term has Greek origins: it derives from the ancient Greek *metamórphōsis*, composed of *metá* (indicating "change" or "beyond") and *morphē* (meaning "shape" or "form") [29].

The concept of morphing is manifold. However, in the aeronautical field, it can be defined as a set of technologies that allow an aerial vehicle to dynamically modify its shape, surface characteristics, or structure in real-time to optimally adapt to different flight conditions and environments [3, 7].

Morphing technology is deeply inspired by the natural world, emulating birds that continuously change their wing shapes not only to achieve configurations that minimise energy consumption during flight, but also to rapidly transition from an efficient cruise to aggressive manoeuvring and precision descents, even in complex environments [7].

Whereas a conventional aircraft features a rigid geometry, inherently a design compromise optimised for a single flight phase but suboptimal for others, a morphing aircraft is capable of altering its configuration to maximise aerodynamic efficiency, manoeuvrability, and range throughout the entire flight envelope [13, 29].

This is achieved by adjusting the wing geometry, the fuselage shape, or the control sur-

faces. Such adaptations favourably modify the aircraft's aerodynamic and stability characteristics, ensuring seamless transitions between different flight modes and providing enhanced operational flexibility.

Technically speaking, even the aeronautical systems employed for decades, such as flaps, slats, ailerons, and retractable landing gears, fall within the concept of morphing, as they enable the aircraft to change its shape according to the flight condition. However, these elements fall under the definition of discrete morphing [3, 41], as they rely on rigid surfaces actuated by traditional hinges and kinematics. These mechanisms introduce unavoidable discontinuities on the aerodynamic surfaces; furthermore, this type of discrete morphing typically serves a single function.

In the modern aerospace context, the term morphing has taken on a different connotation: today, it refers to the so-called "continuous morphing". This involves the implementation of smooth and continuous shape variations, free of external joints, and capable of performing multiple functions simultaneously. This is achieved through the use of smart materials, compliant structures, and flexible skins capable of deforming without compromising their ability to withstand aerodynamic loads.

Although continuous morphing is currently considered a disruptive technology for next-generation aircraft, the fundamental concept is actually how aviation began. The very first powered aircraft, the 1903 Wright Flyer, relied on a primitive form of continuous morphing (wing warping) to achieve roll control. It was only the subsequent historical demand for higher cruise speeds and heavier payloads that forced a transition towards rigid structural paradigms, shifting shape adaptation towards the discrete, sub-optimal hinged surfaces used today [7].

In modern literature [7, 39], there is a consensus to no longer consider discrete, hinged systems as true morphing. Today, the term is reserved exclusively for continuous morphing, which is characterised by radical and seamless deformations.

Another important classification worth mentioning within the category of continuous morphing is the distinction between distributed and localised morphing, as proposed by [29]. Essentially, these technologies can be applied at different scales, depending on the extent of the transformation:

- **Distributed Morphing:** this category encompasses extensive and coordinated transformations that simultaneously involve multiple aircraft components, rendering the entire vehicle adaptable to complex environments. This domain can be classified into two main categories:

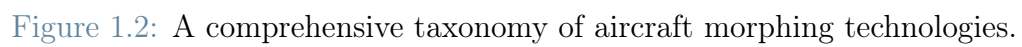
- **Bio-mimetic morphing:** directly inspired by animal flight, such as that of birds and insects, this approach includes flapping wing architectures, active folding wing mechanisms, and flexible multi-articulated skeletal structures. These emulate avian movements to ensure fine control during unpredictable or turbulent flight conditions.
- **Complete airframe morphing:** this category diverges from pure biological imitation to focus on the macroscopic and coordinated transformation of the entire vehicle. It primarily concerns fully reconfigurable airframes or multi-rotor systems capable of radically altering their global shape in mid-flight to maximise operational versatility and structural resilience. This enables the vehicle to absorb impacts, withstand adverse weather conditions, and navigate effectively in hostile or heavily confined environments.
- **Localised Morphing:** this approach focuses on component-level modifications designed to improve aerodynamic efficiency and flight control without overhauling the entire aircraft architecture. It is mainly divided into two areas:
 - **Tail morphing:** the application of shape-changing concepts to the tail surfaces in order to govern pitch and yaw. It includes mechanisms such as varying the incidence of the tailplanes or the tail span to enhance longitudinal stability, manoeuvrability, and directional control.
 - **Wing morphing:** the real-time application of deformable technologies to the wing structure. This encompasses continuous variations in planform, airfoil camber, and control surfaces, with the objective of enhancing manoeuvrability, performance and adaptability to various flight phases, reducing structural loads, whilst concurrently improving fuel efficiency and reducing noise emissions.

As depicted in the previous list, *wing morphing* represents the most extensively researched domain within localised applications, given its direct impact on the aircraft's aerodynamic efficiency, stability, and control.

To systematically categorise the complex mechanisms that alter the wing's geometry, this work adopts a comprehensive classification that merges two prominent literature paradigms. By combining the traditional geometric approach, based on the plane of application [7, 29], with the modern classification based on dimensional scales and realisation modes [13], wing morphing technologies can be broadly subdivided into the following functional categories:

- **Wing planform alteration (macro-scale):** this category involves macroscopic modifications to the 2D geometry of the wing (when viewed from above). It includes variations in wing span, chord length, and sweep angle. These macro-deformations are primarily implemented to optimise the aircraft for fundamentally different flight regimes. For example, modifications to the wing span could increase lift in low-speed flight, whilst changing the wing sweep angle could reduce drag at high speeds.
- **Out-of-plane morphing (macro-scale):** this refers to macroscopic movements that displace the wing structure outside its original horizontal 2D plane. It comprises alterations such as twist, dihedral, and span-wise bending. Twist morphing, for instance, dynamically alters the angle of attack along the span, which is highly effective for active roll control, maintaining lateral stability, optimising lift distribution, and alleviating gust loads without relying on conventional hinged ailerons.
- **Airfoil adjustment (meso-scale):** unlike planform alterations, this approach focuses on modifying the 2D cross-sectional profile of the wing without changing its macroscopic footprint. It primarily involves variable thickness and variable camber mechanisms. By smoothly altering the camber of the airfoil through compliant structures and flexible skins, the wing can optimise local pressure distributions and improve overall aerodynamic performance. A practical application within this category is *wing control surface morphing*, which focuses on replacing traditional discrete high-lift and control devices (such as flaps, ailerons, and slats) with continuous, adaptive structures. These morphing architectures allow for precise and continuous deformation of the airfoil geometry at the leading and trailing edges, enabling smooth camber variation. This leads to gapless, fluid movements that provide better roll control, improved lift during take-off and landing, and optimised drag reduction compared to conventional hinged mechanisms.
- **Active flow control (micro-scale):** from a morphing perspective, this entails micro-scale deformations of the wing's local hardware, or the modification of its local "virtual shape", to manipulate the boundary layer. Techniques such as active skin vibration, synthetic jets, adaptive shock control bumps, active variable stiffness, and circulation control via regulated air blowing are employed to control flow separation, increase lift, reduce drag and noise, and mitigate shock waves [13, 29].

For ease of reading, a conceptual map summarising the entire aircraft morphing taxonomy is presented in Figure 1.2; while, focusing specifically on the wing, a visual representation of its morphing paradigms is provided in Figure 1.3.



Within this comprehensive framework, the present thesis is specifically positioned in the domain of *airfoil adjustment*, focusing on trailing-edge camber morphing. The core objective is to investigate and validate continuous, full-scale morphing ailerons designed to replace traditional hinged control surfaces, enabling smooth airfoil shape variations through flexible skins. This novel architecture aims to overcome the intrinsic disadvantages of conventional rigid mechanisms previously outlined in Section 1.1.2.

1.1.4. The HERWINGT project

Within the context of continuous morphing research, the present thesis work is carried out in the framework of the European Union funded project Hybrid Electric Regional Wing Integration Novel Green Technologies (HERWINGT) [2, 19], supported by the Clean Aviation Joint Undertaking.

The overall goal of the HERWINGT project is to develop an innovative wing, ideal for the future Hybrid-Electric Regional Aircraft (HERA) with a maximum capacity of 100 seats and a range of 500 to 1000 km. It aims to develop, validate, and demonstrate architectures, concepts, structures, and technologies targeting improvements in aerodynamic efficiency, lightweight structural solutions, and hybrid-electric propulsion systems. All the proposed technical solutions are fully aligned with the target of reducing emissions.

The primary challenge is to demonstrate a regional aircraft featuring technologies mature enough for a 2035 launch. This aircraft should incorporate hybrid-electric propulsion, supported by drop-in fuels or hydrogen. Specifically, the anticipated benefit is a 50% fuel burn reduction at the aircraft level, and a 90% net emission reduction in total, taking the fuel effect into account.

The 50% reduction at the aircraft level is expected to be achieved in three different ways [19]:

- Aerodynamic improvements: developing State-of-the-Art (SoA) wing configurations and improved aerodynamics to reduce drag, enabling a 15% fuel burn reduction at the wing component level.
- Weight reduction: decreasing the wing structure weight by 20% compared to a 2020 SoA wing, utilising novel material technologies, an advanced wing structure, and more integrated systems.
- Green integration: making the wing compatible with new hybrid-electric propulsion methods, including batteries, hydrogen, and SAF.

Among its broader deliverables, the project focuses on the generation of digital twins alongside a comprehensive Life Cycle Assessment (LCA). These outputs cover individual components, subsystems, and the complete wing assembly, ensuring seamless alignment with the digital architecture of the reference aircraft. Furthermore, HERWINGT establishes a strategic pathway leading to a full-scale wing demonstration and its eventual inaugural flight.

To achieve these targets, the project focuses on several technologies [2]:

- Advanced multifunctional materials: selecting novel aeronautical materials that allow for reducing weight, facilitating highly integrated structures and enabling out-of-autoclave manufacturing processes.
- Innovative structural architectures: developing new concepts that highly integrate different structural elements to minimise (or eliminate) the reliance on joints and bonding.
- Structural Health Monitoring (SHM): implementing SHM systems to ensure structural integrity and anticipate damage, allowing for thinner designs and consequently lower structural mass.
- More Electric Systems (MES) integration: incorporating systems such as induction-based ice protection directly into the structure, thus minimising both weight and aerodynamic drag penalties.
- Future fuel compatibility: advancing sensors, sealants, and material technologies to ensure that future fuel systems are compatible with broad SAF specifications.
- Aerodynamic drag reduction: exploiting high-aspect-ratio wings, *morphing control surfaces* governed by optimised flight control laws, and improved wing laminarity.

These technologies are planned to reach a Technology Readiness Level (TRL) of 5 through a comprehensive set of physical and virtual demonstrators, to ensure strong certification prospects.

Rather than manufacturing a full wing immediately, the project develops and tests 15 specific demonstrators. These include flaps, leading edges with integrated ice protection systems, SAF tanks, structural health sensors, and *morphing wings* capable of changing shape in-flight to optimise aerodynamics [19].

Within this context, Politecnico di Milano is responsible for the design and experimental validation of two distinct morphing devices:

- A full-scale morphing aileron: designed to replace the conventional hinged aileron, this device is the core subject of the present thesis. Its primary objective is to successfully demonstrate the manufacturing and operational feasibility of this concept at full scale. It aims to enhance aerodynamic performance by providing smooth curvature variations that delay flow separation, while reducing the required actuation effort compared to conventional surfaces, thereby increasing structural efficiency. This also allows for the installation of smaller and lighter actuators. Another ambitious goal is to demonstrate high-bandwidth capability (up to 10 Hz) with reduced actuation effort compared to static deflection, thereby showing the potential applicability of the concept to active control functions such as gust load alleviation and flutter suppression. These advantages are of primary importance, as a lighter and more aerodynamically efficient component directly contributes to pollutant emission reductions. The smoother curvature also leads to a decrease in aerodynamic noise.
- A morphing droop nose: widely studied in a preceding project (Clean Sky 2) [16]. In the current HERWINGT project, fatigue tests will be conducted to analyse its behaviour under cyclic loads.

1.2. Thesis outline

The purpose of this section is to outline the scope and the primary objectives of the present thesis (Section 1.2.1). Furthermore, it provides a general overview of the organisation of the work, thereby assisting the reader in navigating the document (Section 1.2.2).

1.2.1. Research scope and objectives

While numerical simulations and computational models are fundamental during the preliminary design phase, the physical realisation and the behaviour of a full-scale morphing structure introduce complex operational variables that cannot be completely captured numerically and necessitate rigorous experimental validation.

The core objective of this thesis is to support the research team in bridging the gap between the numerical design environment and the physical implementation of the morphing aileron demonstrator developed within HERWINGT project. The experimental activity focuses on a 2.5 m span wing demonstrator, composed of two independently actuated glass-fibre ailerons. This is representative of the outer wing section of the HERA aircraft. By participating in the assembly and testing of this physical prototype, the research aims to experimentally validate the computational targets and assess the technological maturity

of the proposed compliant architecture.

Specifically, the experimental campaign is driven by the following primary objectives:

- Hardware integration and functional validation: assembling the physical prototype and its internal kinematic architecture. This phase ensures that the electromechanical linear actuators and the transmission mechanisms can successfully drive the continuous S-glass fibre skin without inducing structural failures or mechanical binding.
- Friction modelling and control optimisation: experimentally measuring the internal mechanical friction generated by the kinematic mechanism. Characterising these dissipative forces is necessary to develop a mathematical friction model, which is subsequently embedded into the motor control logic to ensure precise position tracking.
- Shape validation and static load assessment without aerodynamic load: utilising an optical motion capture system (Qualisys®) to reconstruct the actual morphed profiles and evaluate the static actuation forces required to reach the imposed deflections. This allows for a direct geometric comparison with the theoretical targets and numerical results, establishing the mathematical relationships (direct and inverse laws) between the actuator stroke and the equivalent trailing edge rotation.
- Dynamic characterisation without aerodynamic load: assessing the dynamic response of the system across a 0 to 10 Hz frequency bandwidth to verify its suitability for high-frequency applications, such as dynamic control or active load alleviation. This phase investigates whether the aileron can accurately follow the target shapes during rapid dynamic actuation at various frequencies and oscillation amplitudes, assessing any associated phase lag or amplitude amplification. A key goal is to empirically demonstrate the reduction in required actuation force induced by the system's structural resonance.
- Wind tunnel testing: verifying that the structural, kinematic, and dynamic performances previously validated in the laboratory are maintained under actual wind loads, alongside the analysis of the aerodynamic properties. In this thesis, the preliminary experimental results are presented, specifically focusing on the steady aerodynamic characterisation of the model and the evaluation of the actuation forces under aerodynamic loading.

1.2.2. Thesis structure

The thesis is logically structured to guide the reader through the theoretical background, the prototype design and development, and the experimental validation phases:

- Chapter 1 provides an overview of the environmental context surrounding modern aviation, offering a general introduction to the morphing concept and the European HERWINGT project framing this research. Additionally, it outlines the primary limitations associated with conventional flight control surfaces.
- Chapter 2 provides an overview of the SoA in morphing trailing edges, exploring the core engineering challenges and the advanced materials driving the transition from discrete to continuous shape-changing architectures. It also reviews global developments in morphing aileron technologies and previous research conducted at Politecnico di Milano, including past wind tunnel testing campaigns on morphing structures.
- Chapter 3 summarises the prior multi-disciplinary design and numerical validation of the specific morphing aileron demonstrator developed within the HERWINGT project [11]. It outlines the aero-structural optimisation process conducted by the research team prior to this thesis, which generated the target shapes and the compliant structure optimisation that led to the final numerical model. The chapter also lists the primary technological objectives the proposed device aims to achieve, establishing the theoretical baseline for the experimental activities.
- Chapter 4 transitions to the physical prototype, describing the hardware setup of the experimental ground test rig. It details the composite structure, the linear electromagnetic actuators, the pneumatic braking system, and the software control interfaces.
- Chapter 5 focuses on the identification of the experimental friction model. It explains the frequency-domain identification procedure used to isolate the Coulomb and viscous friction coefficients, which are essential for precise motion control. Furthermore, the derived model is validated through a direct comparison with experimental measurements.
- Chapter 6 presents the static and quasi-static ground tests. Utilising a motion capture system, this chapter validates the geometric fidelity of the morphed shapes and defines the fundamental kinematic relationships of the mechanism. Moreover, it evaluates the actuation forces required to reach the various deflections, comparing the experimental measurements with the numerical model predictions.

- Chapter 7 investigates the dynamic behaviour of the system. Through sine sweep analyses, it characterises the actuation bandwidth and evaluates the tracking accuracy for both the displacement of the motor shaft and the rotation of the aileron, specifically in terms of magnitude response and phase shift. Furthermore, it demonstrates the actuation force reduction phenomenon induced by the excitation of an aileron's structural eigenmode located within the operational bandwidth.
- Chapter 8 describes the wind tunnel facility and the experimental setup established for the testing campaign. Furthermore, it presents the initial experimental results, specifically focusing on the steady aerodynamic characterisation of the model and the evaluation of the actuation forces under aerodynamic loading.
- Finally, Chapter 9 summarises the key findings of the experimental activities, reflecting on the achieved objectives and identifying potential avenues for future research and development.

2 | Morphing trailing edges: technologies and case studies

Despite the well-known potential benefits of morphing, as outlined in Section 1.1.3, the application of these technologies remains challenging today, particularly at the scale of a full-sized aircraft. For this reason, researchers are dedicating significant effort to advancing this field.

The aim of this chapter is to provide the reader with an overview of the main challenges associated with morphing, as well as how current technologies and materials can help overcome these difficulties. This will allow the reader to better understand the model studied in this thesis and the current directions of research concerning morphing trailing edges.

2.1. Challenges in morphing implementation

The practical implementation of morphing technologies is constrained by several key engineering challenges. These primarily involve trade-offs between aerodynamic performance and structural requirements, as well as increased system complexity. In modern literature, it is acknowledged that although morphing can bring several advantages, it typically leads to penalties in terms of weight, mechanical complexity, and cost [7]. These inherent limitations, often coupled with severe certification hurdles, currently hinder the widespread industrial adoption of morphing control surfaces.

A central issue stems from a fundamental mechanical contradiction in the design of the skin. The morphing skin must exhibit low in-plane stiffness to allow shape changes with minimal actuation energy. At the same time, it must possess high out-of-plane stiffness to withstand aerodynamic loads (high bending stiffness), without undergoing unacceptable sagging, buckling, or causing internal mechanisms to bind [1, 41]. This structural conflict is commonly referred to as the *morphing paradox* in the literature and presents challenges in material selection [6].

A second major challenge related to morphing is linked to *weight and actuation penalties* [7]. Unlike conventional hinged rigid surfaces, achieving a desired morphed shape requires overcoming the elastic resistance (structural strain energy) of the material itself, alongside aerodynamic, inertial, and gravitational loads. This requires higher actuation forces and power consumption, particularly if continuous "holding power" is needed to maintain the configuration against the airflow (unless the material features shape-locking capabilities, such as with SMPs) [40]. Consequently, this leads to bulkier motors and heavier structures that are difficult to integrate, potentially offsetting the aerodynamic fuel savings achieved during flight.

Furthermore, current actuation technologies (including pneumatic, hydraulic, and piezo-electric actuators) often lack the optimal power-to-weight ratio and dynamic responsiveness required for full-scale morphing applications. Therefore, to fully capitalise on the advantages of morphing, developing reliable and efficient actuation systems capable of precise control is imperative. Addressing these hurdles requires further research into integrating multifunctional actuators and developing hybrid systems that leverage the strengths of different technologies [29].

Another significant concern is *structural integrity and durability*. Maintaining these characteristics over time under repeated morphing cycles, varying aerodynamic loads, and harsh environmental conditions is challenging. The continuous deformation of the structure can induce severe stress and fatigue, ultimately leading to material degradation or failure. Consequently, morphing materials and internal mechanisms must exhibit high fatigue resistance, and long-term environmental stability [29].

This physical challenge is strictly tied to the *reliability and certification* of morphing technologies. Unlike conventional hinged control surfaces, which rely on mature, discrete, and easily certifiable kinematics, continuous morphing structures face severe certification requirements [34]. Because the aeronautical industry demands high safety standards, the integration of these novel technologies requires exhaustive testing across diverse operational conditions. Furthermore, it is imperative to develop redundant structural architectures and sophisticated fault-tolerant control systems capable of real-time reconfiguration, in the event of failures [13].

A further critical challenge is *system integration and cost-effectiveness* [29]. Integrating morphing technologies into existing aerospace systems without requiring major structural redesigns is a significant difficulty. It is therefore necessary to carry out multidisciplinary studies to ensure compatibility with current aircraft designs and established operational procedures. In addition, high initial investments and complex manufacturing processes

still limit the widespread adoption of these technologies in the aerospace sector. To remain competitive in commercial aviation, it is important to develop scalable solutions and modular morphing components that can be easily retrofitted into existing platforms. This would help reduce production costs and achieve economies of scale [13, 29].

Finally, *flight control and aeroelasticity* pose extreme challenges. The continuous shape adaptation alters the aircraft's aerodynamic centre and mass distribution in real time, creating complex and non-linear dynamics. As a result, control systems must be highly reliable to safely manage these fluctuating loads and transient structural responses to prevent failures. Additionally, careful attention must be paid to complex aeroelastic phenomena that can arise due to the flexible nature of morphing structures [13].

2.2. Morphing materials

As depicted in Section 2.1, the design of a morphing system requires the resolution of conflicting engineering requirements, primarily driven by the skin's morphing paradox. To achieve a well-executed design, it is necessary to adopt a multidisciplinary approach right from the preliminary design phase, seamlessly integrating the domains of material science, aerodynamics, and mechanical engineering [1]. To overcome implementation challenges, designers must carefully select the most suitable materials and technologies for the specific application. The ultimate goal is to develop a system that balances flexibility, load-bearing capability, and overall reliability, while minimising weight and mechanical complexity [7]. Specifically, this requires the effective integration of external flexible skins with internal structures capable of concurrently performing both actuation and load-bearing tasks.

Depending on their functional role within the morphing architecture, materials can be broadly classified into two main categories: *passive materials* and *active smart materials*, which will be discussed in detail in Section 2.2.2.

This section aims to present an overview of the materials and technologies explored for morphing applications, highlighting the essential properties for deformable aerodynamic skins as well as for internal actuation and structural elements.

Subsequently, Section 2.3 will demonstrate how these elements are integrated to develop advanced technological systems, such as morphing trailing edges.

2.2.1. Materials and actuation properties for flexible skins and internal systems in morphing wings

To achieve effective morphing, materials and structures must satisfy opposing demands depending on the specific component: flexible skins must exhibit high elasticity to undergo large deformations for shape change, whereas internal actuation systems must provide high stiffness and generate sufficient force to drive the morphing process while resisting aerodynamic loads.

These desired properties are primarily dictated by the intrinsic characteristics of the base materials, but they can also be enhanced or tailored through engineered structural architectures (essentially a geometric reorganisation of the material), such as corrugated laminates, as briefly presented in Section 2.2.2.

The primary mechanical properties typically required for flexible morphing skins include the following [1]:

- High elasticity and multiaxial deformability: the polymeric material must withstand large deformations and return to its original shape without suffering permanent damage or breaking.
- Stiffness (low in-plane and high out-of-plane): as described in Section 2.1, materials adopted for the wing skin must have a low in-plane stiffness to allow the wing to stretch easily, enabling it to morph as required. At the same time, high out-of-plane stiffness ensures that the structure does not bend or deform under aerodynamic pressure during flight.
- High mechanical strength: high mechanical strength is crucial for morphing wings to endure bending, twisting, and stretching forces without failing, maintaining structural integrity throughout the morphing process for safe operations.
- High fracture toughness: due to the significant cyclic strains and stresses involved in the morphing process, the material must possess high fracture toughness to resist crack propagation and prevent tearing under loads. This also contributes to improved fatigue performance by slowing crack growth.
- Low hysteresis: morphing materials must not dissipate excessive energy in the form of heat during cyclic deformations. Low hysteresis ensures that the skin operates efficiently and returns precisely to its un-morphed configuration with minimal energy losses, a crucial factor for preserving the aircraft's overall performance.
- High durability, resilience, and environmental resistance: materials must withstand

cyclic and dynamic loading while maintaining their mechanical integrity under environmental stresses such as temperature fluctuations, moisture, and UV radiation.

- Low density (lightweight): in aerospace applications, reducing weight is essential; selected materials should be significantly lighter than traditional metals to improve fuel efficiency and reduce operating costs.

While both the external skin and the internal system (comprising the actuation mechanism and the load-bearing structure) share the fundamental necessity to deform to enable shape changes, their primary design focus differs significantly, as previously discussed. For the skin, the emphasis lies on high conformability and multiaxial flexibility. For the internal substructure and actuation mechanisms, instead, the attention shifts primarily toward actuation force and load-bearing capacity.

The internal system, which increasingly combines structural elements and motors into a single integrated architecture, must forcefully guide the deformation against external aerodynamic pressures and inherent structural stiffness, while safely sustaining the flight loads. To achieve a balance between the flexibility required for shape-changing, structural strength, and weight reduction, a specific set of functional requirements must be met. These apply to any internal driving mechanism: this holds true whether the system relies on conventional electromechanical actuators driving a rigid kinematic system constrained to the skin, or on smart materials (such as shape memory alloys, piezoceramics, and active polymers) capable of acting simultaneously as integrated "artificial muscles" and primary load-bearing members.

Considering both actuation and structural functions as a unified system, these core requirements include [7, 25, 29, 41, 44]:

- High actuation force and specific work: active materials or actuators must generate sufficient force to overcome both the external aerodynamic loads and the stiffness of the compliant morphing skin.
- High-bandwidth and fast response time: rapid actuation is required for flight control applications, manoeuvrability, and active load alleviation, while slower responses may be sufficient for cruise optimisation.
- Structural strength and load-bearing capability: the internal architecture and actuation mechanisms must safely withstand the loads during all flight conditions without undergoing structural deformations or failure.
- Anisotropic stiffness (tailored compliance): the internal substructure must exhibit anisotropic mechanical properties. For instance, in camber morphing applications,

the structure requires low chordwise bending stiffness to allow shape changes with minimal energy input, but high spanwise bending stiffness to carry bending and shear loads.

- Efficient load transfer and skin support: the internal substructure must provide adequate support to the flexible skin. This is essential to resolve and transfer the distributed aerodynamic pressure loads from the aerodynamic surface into the main load-carrying elements, preventing undesired deflections and preserving the aerodynamic profile of the airfoil under flight loads.
- High durability and fatigue resistance: since internal structures and actuation mechanisms undergo continuous cyclic loading during shape changes and flight, they must resist fatigue to ensure a long operational lifespan and preserve structural integrity, even under adverse environmental conditions.
- Aeroelastic stability and torsional stiffness: despite the required flexibility for morphing, the internal architecture must maintain adequate global stiffness across all morphed configurations to prevent aeroelastic instabilities.
- Compactness and low weight: actuators and internal mechanisms must fit within the constrained internal volume of the wing (e.g., the trailing edge) while minimising mass, which has historically been one of the major limitations of morphing concepts.
- Locking capability (holding power): the system must be able to maintain the morphed configuration under flight loads without requiring continuous energy input. This requirement becomes particularly crucial for configuration-type changes, where a specific morphed shape must be maintained for a considerable period of time (e.g., during prolonged cruise segments). Without locking features, the actuators would have to constantly withstand the full aerodynamic pressure, leading to excessive power consumption.

Overall, the successful realisation of a morphing wing requires structural integration of compliant skins with high-energy-density actuation systems and load-bearing materials. Ideally, the ultimate goal would be to develop a single material or structural architecture capable of simultaneously fulfilling all these mechanical and functional requirements to provide the desired performance and reliability during flight.

2.2.2. Classification of morphing materials

As highlighted by the aforementioned requirements (Section 2.2.1), traditional rigid materials, such as aluminium alloys or conventional composites, are often inadequate for

morphing applications, as they cannot undergo large, continuous deformations without failing [1]. Consequently, the realisation of morphing wings relies on the adoption of advanced polymers and smart materials, which are capable of withstanding multiaxial strains while retaining their structural integrity. Depending on their response to external stimuli and their specific functional role within the morphing architecture, these materials are generally classified into two main categories. The first category comprises *passive materials*. Generally, they are employed either as external flexible aerodynamic skins (transferring aerodynamic loads to the internal actuating substructure) or as compliant wing cores [1]. The second category includes *active smart materials*; depending on the selected design, these materials can be utilised as dedicated solid-state actuators to drive the shape change, or they can be fully integrated into the structural architecture to simultaneously fulfil both aero-structural and actuation tasks [7].

A brief list of passive-materials could be presented [1, 9, 25, 34, 41, 44]:

- Pure elastomers: these are highly flexible polymers, such as silicone, natural rubber (latex), and polyurethanes, which are capable of undergoing extreme elastic elongations without breaking. They are typically employed to provide seamless aerodynamic coverage, offering multiaxial degrees of freedom and excellent environmental resilience. However, their low stiffness and high Poisson's ratio cause them to contract laterally (a phenomenon known as necking-in) when stretched, making them highly susceptible to aeroelastic instabilities and sagging or buckling under flight loads. To mitigate this issue, pure elastomers generally require *prestretching* (pre-tensioning) prior to installation. This process stores elastic energy within the material, significantly increasing its out-of-plane stiffness to maintain surface smoothness during morphing operations.
- Elastomeric Matrix Composites (EMCs) or Flexible Matrix Composites (FMCs): these materials have been developed specifically to overcome the structural limitations of pure elastomers. The concept relies on embedding high-stiffness continuous fibres, such as carbon or Kevlar, into a hyper-elastic matrix. By strategically orienting these fibres, the composite achieves extreme mechanical anisotropy and a near-zero spanwise Poisson's ratio. This tailored behaviour allows the skin to retain a high degree of flexibility in the morphing direction, comparable to that of unreinforced elastomers, thus keeping the required actuation forces very low. Simultaneously, the composite architecture (via specific fibre orientations or skin pre-tensioning) provides the high mechanical resistance needed to withstand out-of-plane aerodynamic pressures and transfer the loads to the internal substructure.

- *Tailored conventional composites* (FRPs - Fibre-Reinforced Polymers): these include traditional polymer matrix composites reinforced with glass (GFRP) or carbon (CFRP) fibres. Although intrinsically rigid, when engineered with extremely thin laminates, optimised stacking sequences, or tailored thickness distributions, these passive materials can provide the flexibility required for shape adaptation. Simultaneously, they maintain high strength and load-bearing capabilities. This specific material category has been selected for the morphing skin of the HER-WINGT demonstrator investigated in this thesis, which employs an S-glass fibre fabric (a high-strength structural glass fibre).
- *Auxiliary passive fillers and compliant cores*: this category encompasses highly deformable materials, such as elastomeric and polyurethane foams, utilised as lightweight internal fillers or compliant elements within the wing skeleton. Their primary function is to provide continuous support to the aerodynamic skin in order to prevent local instabilities, while maintaining low stiffness to facilitate smooth shape transitions.

It is worth noting that the mechanical behaviour of these materials is often governed not only by their intrinsic properties, but also by their structural architecture. For this reason, they are frequently organised into specialised geometric configurations, such as honeycomb cores, corrugated laminates, or auxetic metamaterials. Specifically, cellular honeycomb cores are employed to provide high out-of-plane stiffness and lightweight structural support, and can be tailored to exhibit a near-zero Poisson's ratio. Corrugated laminates provide high mechanical anisotropy, yielding high bending stiffness against aerodynamic pressure while remaining compliant in the morphing direction. Finally, auxetic metamaterials, characterised by a negative Poisson's ratio, exhibit lateral expansion under tensile loading, facilitating complex three-dimensional shape changes [1, 25, 29].

While these approaches enable a high degree of passive adaptability through structural design, they still rely on external actuation systems to induce deformation. For this reason, increasing attention has been directed toward *active smart materials*, which offer the potential for a higher level of functional integration. As previously introduced, active smart materials are investigated in the field of morphing aircraft due to their potential for multifunctional integration. By combining structural and actuation capabilities within a single material system, they can, in principle, fulfil aerodynamic, load-bearing, and actuation functions simultaneously. This integrated approach could significantly reduce the overall weight and mechanical complexity of the wing. Alongside this highly advanced technological challenge, they are also being studied as internal "artificial muscles" embedded within the structure to drive external flexible skins. Based on the specific stimuli they

respond to, the most prominent active materials investigated for morphing architectures can be summarised as follows [1, 7, 39]:

- **Electroactive Polymers (EAPs):** these polymers are capable of undergoing significant deformation when subjected to an electric field. They can achieve large strains and fast response times while remaining lightweight, although they generate relatively low actuation forces. However, their application in full-scale morphing wings is limited by several drawbacks. In particular, their low force output restricts their ability to induce significant shape changes under high aerodynamic loads. Additionally, the high-voltage power supply required to achieve large deformations, together with their poor environmental durability (due to exposure to moisture, UV radiation, and other environmental factors) increases system complexity and raises safety concerns.
- **Shape Memory Polymers (SMPs):** these are smart polymers capable of altering their elastic modulus in response to temperature variations. The required thermal activation can be achieved through external heating, or internally via Joule heating (by integrating conductive reinforcements), electromagnetic activation, or optical and chemical stimuli. When heated above their glass transition temperature (T_g), they transition from a rigid glassy state to a flexible rubbery state, becoming easily deformable and capable of accommodating large multiaxial strains. Once cooled below T_g in the deformed configuration, they recover their initial rigidity and “freeze” into the morphed shape. This variable-stiffness behaviour allows the structure to lock the morphed configuration without requiring continuous energy input, addressing the holding power requirement. However, SMPs typically exhibit slow response times, are difficult to control or program precisely, and provide limited actuation force.
- **Piezoelectric Materials (PZTs):** these are ceramic materials that deform when subjected to an electric field. They are capable of producing very high output forces and exhibit a high-frequency bandwidth, although they are limited to low actuation strains. Due to their fast response times, they are well-suited for active vibration control, aeroelastic flutter control, and for micro-adjustments of the trailing edge.
- **Shape Memory Alloys (SMAs):** these metal alloys (e.g., Nitinol) undergo a solid-state phase transition when heated or cooled, allowing them to recover a predefined shape while generating high actuation forces and high energy density. A key limitation is that the material behaviour is temperature-dependent: the actuation state is maintained only within a given temperature range defined by the phase transfor-

mation temperatures. Additionally, due to their thermally driven actuation, they exhibit relatively slow response times and are therefore best suited for low-frequency applications.

To conclude this classification, it is important to highlight a key architectural distinction regarding how these active materials are integrated into the morphing wing structure. Due to their material properties, SMAs and PZTs, being metallic and ceramic in nature, face limitations when used as external skins: PZTs are fragile and limited to small actuation strains, while SMAs exhibit slow dynamic responses and would require significant power to heat and maintain large surfaces. Consequently, they are predominantly used as internal actuation mechanisms to drive separate compliant skin components [39, 41]. Conversely, polymer-based active materials, such as SMPs and EAPs, possess the elasticity required to undergo large, continuous multiaxial deformations. Therefore, they are investigated for direct implementation as active aerodynamic skins, forming a seamless external surface capable of adapting the airfoil profile while interacting with the external airflow [1].

However, while active smart materials and complex elastomers represent promising academic solutions, current literature highlights that this technological advantage is not yet mature enough for practical, full-scale implementation. Industrial application, in fact, is limited by challenges related to actuation force, bandwidth, durability, and severe aeronautical certification, and an overall low Technology Readiness Level (TRL). Consequently, their application as a unified skin-actuator system remains largely confined to the proof-of-concept stage [1].

Therefore, for large-scale commercial applications such as the morphing aileron developed within the HERWINGT project (whose experimental evaluation constitutes the core of this thesis), the most promising and viable industrial approach currently relies on the integration of *passive composite skins* driven by high-bandwidth electromechanical or *electromagnetic actuators*. This structural configuration guarantees the necessary actuation force and high dynamic response (e.g., up to 10 Hz) required for active control applications [11].

Building upon these foundational material concepts, Section 2.3 presents an overview of the SoA morphing architectures and technological solutions proposed by researchers for airfoil adjustment. It illustrates how the previously discussed materials are implemented in practice to modify the overall airfoil profile. This includes advanced structural concepts developed for both the leading edge (e.g., morphing droop nose) and the trailing edge.

2.3. State of the Art in morphing airfoil adjustment technologies

To fully contextualise the morphing aileron demonstrator developed within HERWINGT project, it is essential to review the most relevant SoA projects focused on airfoil adjustment, analysing their structural approaches, actuation mechanisms, and their achieved Technology Readiness Level (TRL).

It is worth noting that applying morphing concepts to larger aircraft significantly increases the complexity of the design process. The adaptive structure must strike a delicate balance: it requires enough elasticity to allow for shape transitions, while demanding sufficient stiffness to withstand in-service flight loads. As a result, despite the abundance of theoretical and experimental studies on small aircraft, there is still a noticeable lack of experimentally validated morphing architectures suitable for commercial aircraft [5, 7, 34].

Consequently, this overview focuses mainly on large-scale models. Particular attention is given to technologies developed around the concept of an external skin driven by an internal actuation system; these represent pragmatic engineering solutions that are more readily applicable to full-scale aircraft. Furthermore, this discussion will not concentrate on the detailed design methodologies of the various technologies (such as aerodynamic analyses, structural optimisations, or numerical simulations). Rather, the objective is to describe their operational principles, highlight their main advantages and drawbacks, and outline the experimental test campaigns conducted to assess their achieved TRL.

2.3.1. A historical pioneer: the MAW project

One of the first fundamental projects in this field is the Mission Adaptive Wing (MAW), an historical programme from the 1980s carried out on the Advanced Fighter Technology Integration (AFTI) F-111 aircraft by Boeing in collaboration with NASA and the USAF. This project is considered a pioneer from which subsequent developments in wing morphing and camber change concepts originated, and it is particularly important because it was tested on a flying demonstrator, reaching an estimated TRL of about 6–7. The objective was to achieve an optimal wing camber across different flight regimes, from subsonic to supersonic, while also reducing the drag rise phenomenon.

The *MAW* employed flexible fibreglass for the upper wing surface, both at the leading and trailing edge regions. The lower wing surface was not continuous; instead, it was segmented to create a discontinuity that allowed overlapping metallic panels to slide over

each other in order to accommodate chord-wise length variations during wing cambering.

The external skin was actuated by an electro-hydraulic system that drove a complex network of internal rigid linkages and hinges connected to the outer surface. However, this solution was limiting, since the internal structure was not originally designed to be bent or twisted. The hydraulic actuators had to be significantly oversized in order to overcome the resistance of the rigid linkage system, which suffers from drawbacks such as wear, backlash, and friction in the joints, all of which increase the required actuation force.

The aerodynamic benefits were successfully demonstrated, with an increase in aerodynamic efficiency of about 20%, an increase in range of approximately 20%–30%, and a 15% increase in wing airload at constant bending moment compared to the equivalent conventional fixed-wing aircraft. However, the presence of heavy actuation systems offset the aerodynamic gains achieved. The tests and the innovative concept itself encouraged designers to move toward compliant structures that can be shaped and morphed in flight [7, 16, 41]. A schematic representation of the MAW airfoil cross-section and the AFTI/F-111A aircraft configuration is shown in Figure 2.1.



Figure 2.1: (a) shows the MAW airfoil cross-section in its deformed, cambered configuration [41]; (b) shows a modified General Dynamics AFTI/F-111A Aardvark aircraft equipped with MAW during flight [30].

2.3.2. Bio-inspired compliant structures: the FishBAC concept

Investigated by Woods and Friswell (2012–2014) and driven by the criteria of simplicity, reliability, and control authority, the Fish Bone Active Camber (FishBAC) concept

was developed as a biologically inspired compliant structure to achieve large, continuous changes in airfoil camber and aerodynamic properties [43].

The *FishBAC* concept is built around a highly anisotropic compliant structural core with a pre-tensioned EMC skin: both core and skin are designed to exhibit near-zero Poisson's ratio, with very low chordwise and high spanwise bending stiffness. The concept mimics the skeletal structure of a fish, combining a compliant chordwise bending beam (spine) with transverse ribs (stringers) connected to an external EMC skin surface, actuated through a tendon-driven antagonistic mechanism. A rigid leading-edge D-spar carries the primary structural loads.

The stringers support the skin at a constant distance from the spine, maintain the airfoil thickness during morphing, transmit pressure loads into the structure, and significantly increase the spanwise bending stiffness. The pre-tensioned elastomeric skin accommodates large deformations while maintaining a smooth aerodynamic surface and preventing buckling [43, 44].

Wind tunnel tests demonstrated lift-to-drag ratio gains of approximately 20%–25% over the range of angles of attack between $\alpha = 5^\circ$ and $\alpha = 10^\circ$, compared to a NACA 0012 airfoil with a trailing-edge flap [44]. However, from an engineering standpoint, there are some challenges, including the need to maintain proper tendon tension. Based on its current level of validation, the technology can be classified at approximately TRL 4. Figure 2.2 illustrates the overall FishBAC architecture and its tendon-driven actuation system.

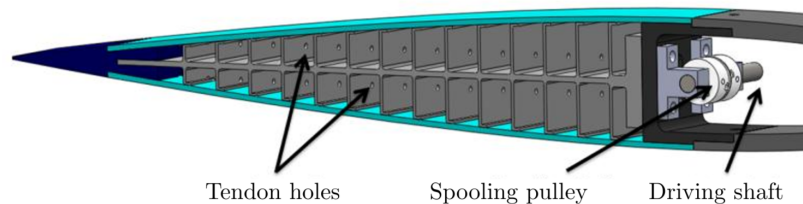


Figure 2.2: FishBAC prototype: details of tendon attachment (adapted from [43]).

2.3.3. Industrial maturity and flight testing: MACW and ACTE

To highlight the achievement of an advanced TRL for compliant aerodynamic surface technologies, it is useful to refer to the main demonstrator efforts led by FlexSys Inc. in collaboration with U.S. aeronautical institutions. In particular, the Mission Adaptive Compliant Wing (MACW), developed with support from the Air Force Research Laboratory (AFRL), and the subsequent Adaptive Compliant Trailing Edge (ACTE) programme, conducted by FlexSys Inc. in collaboration with NASA and AFRL, in which the FlexFoilTM compliant trailing-edge technology was developed and flight-tested, represent two key demonstrators for the validation of morphing surfaces with continuous deformation in relevant operational environments. Both projects have reached a high level of technological maturity, estimated at TRL 6–7, having been successfully demonstrated in flight on experimental aircraft platforms.

The *MACW* concept is based on the use of compliant structures designed through topology optimisation algorithms, capable of deforming globally as a single continuous mechanism. The deformation is distributed across the entire structure, reducing stress concentrations and enabling variable aerodynamic configurations with reduced actuation energy requirements. From a structural standpoint, these structures are optimised to allow deformation while resisting external aerodynamic loads [24].

MACW model was mounted to the underbelly of the White Knight aircraft. Measurements on the wing upper surface confirmed laminar flow extension up to 60% chord at high operating lift conditions. Even at $M = 0.55$ and 40 000 ft, the test airfoil behaved as predicted by computational models and demonstrated stable operation without restrictions or failures [24].

The study also reported potential fuel savings of up to 15% for aircraft equipped with MACW, emphasising the reduced actuation force and power requirements compared to conventional flaps [24, 38].

Despite its advantages, the MACW concept requires a complex multidisciplinary optimisation process that may involve several iterative cycles to converge to a feasible solution [24]. In addition, aspects related to fatigue loading, long-term structural durability, and certification requirements must be carefully addressed during the design and validation process.

FlexSys Inc. further advanced its technology in *ACTE* project. As MACW demonstrator, FlexFoilTM is based on the principle of distributed compliance. By exploiting the elasticity of its internal materials, the structure can bend to vary the trailing edge deflection (from

-9° to $+40^\circ$) and twist along the wingspan at up to $30^\circ/\text{s}$ for gust load alleviation, while maintaining a smooth and uninterrupted aerodynamic surface [26].

Between 2014 and 2017, several flight test campaigns were conducted on a modified Gulfstream III business jet, where the conventional high-lift flaps were replaced by 19 ft span-wise FlexFoilTM control surfaces on each wing, including 2 ft wide compliant fairings at each end. A representation of the ACTE flight test aircraft and the FlexFoilTM system is shown in Figure 2.3.

It should be noted that the flight test articles were not equipped with actuators for in-flight shape changes; instead, the control surfaces were mechanically locked at specific deflections on the ground prior to each flight, while dynamic actuation capabilities were successfully demonstrated on a dedicated ground test article.

The ACTE system operated up to $M = 0.85$ and altitudes of 40 000 ft, accumulating more than 70 flight hours without any structural failures and demonstrating robust performance under loads beyond typical operational limits for commercial aircraft. Flight tests also confirmed a noise reduction of up to 30% during take-off and landing phases compared to the baseline aircraft, as well as an estimated drag reduction of 5%–12% in extended-range configurations [23, 26, 38]. However, active deformation introduces trade-offs, including increased actuation power demand (10%–20% higher than conventional flaps) and a moderate weight penalty of approximately 4% [26].



(a)



(b)

Figure 2.3: (a) shows the modified Gulfstream III aircraft used for ACTE flight testing; (b) shows a close-up of the compliant trailing edge (FlexFoilTM) system [26].

2.3.4. Sliding skin concept: TRIC and SmartX

A fundamental technology sharing the sliding skin principle with the morphing ailerons developed within the HERWINGT project is the Translation Induced Camber (*TRIC*) developed through the European FP7 CHANGE project. This concept is relatively simple and compact; by utilising the sliding of a fibreglass bottom skin, driven by an actuator pair composed of conventional fast, high-torque rotary servos, it achieves a combination of cross-sectional warping and skin bending. Specifically, this combination is what enables the morphing capabilities: equal actuation on both ends induces camber morphing, while differential actuation induces twist morphing. However, the main disadvantage of the original TRIC concept is that the lift distribution cannot be influenced locally with a single morphing surface controlled by a single actuator pair. This limitation inhibits the use of the morphing mechanism for multi-objective flight control [28].

To overcome this limitation, the concept was further developed within the SmartX project [28, 32] to address simultaneous goals such as gust and manoeuvre load alleviation, flutter suppression, and drag minimisation. The *SmartX-Alpha* demonstrator, also constructed primarily using fibreglass to ensure structural flexibility, introduces a distributed and modular morphing architecture comprising six independent modules, as illustrated in Figure 2.4. These modules are interconnected by elastomeric skin segments made of silicone, enabling seamless active span-wise twist and camber morphing. Consequently, the lift distribution along the wingspan can be varied locally and actively without incurring the additional drag penalties associated with gaps between control surfaces.

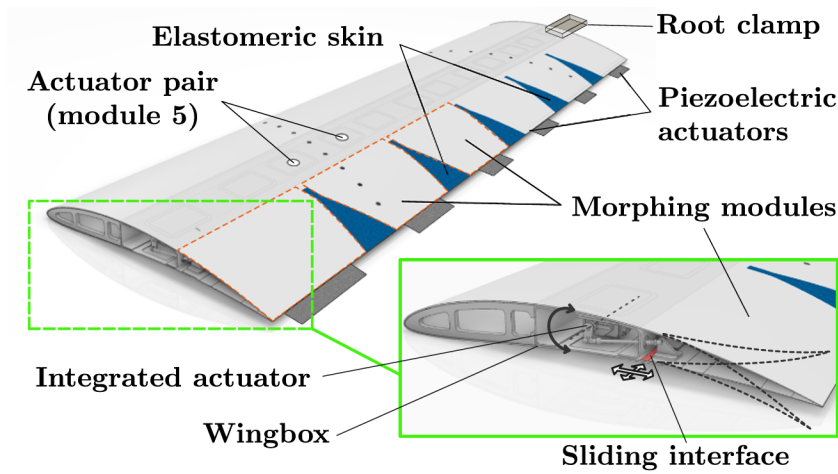


Figure 2.4: Schematic representation of the SmartX-Alpha demonstrator, highlighting the distributed modular layout and the internal actuation mechanism [28].

To validate the design of the static target morphing shapes, Digital Image Correlation (DIC) measurements were conducted on the top and bottom surfaces of the morphing modules. This validation was performed without applied aerodynamic loads in both the experimental setup and the numerical simulations. During these tests, a linear trend was observed between the actuator input rotation and the resulting trailing-edge tip deflection during symmetric upward and downward actuation, aligning well with the numerical simulation. However, the validation study revealed a significant sensitivity to backlash in the sliding interface and an added structural stiffness due to the silicone elastomer. As a result, the experimental peak deflections were lower than anticipated by the initial model [28]. To mitigate this issue, a sensor-based compensator operating within a closed-loop controller is proposed. By relying on accurate real-time position measurements from integrated sensors, such as fibre optic shape sensors, the system can dynamically correct for these mechanical losses.

In addition to static testing, wind tunnel experiments were conducted to validate the system's dynamic load alleviation capabilities, as shown in Figure 2.5. Piezoelectric flow sensors mounted directly on the wing were utilised to measure boundary layer oscillations, while trailing-edge piezoelectric actuators acted as high-frequency aeroelastic controls for gust alleviation and flutter suppression. Currently, the research team is advancing this technology through a new iteration, *SmartX-Neo*. This upcoming version features a more lightweight carbon-fibre construction, an increased aspect ratio, and an expanded array of 10 independent trailing-edge flaps [32]. Given these successful dynamic wind tunnel validations and the ongoing prototype developments, the technology can currently be estimated at a TRL of 4 to 5.



Figure 2.5: The SmartX-Alpha physical demonstrator mounted in the open jet wind tunnel at TU Delft [32].

2.3.5. European Clean Sky 2 demonstrators

To meet the stringent environmental targets for next-generation aviation, the European Union has invested in sustainable aeronautics through large-scale, multidisciplinary research programmes. Through these programmes, the morphing concept has been widely studied, up to the most recent framework programme: Clean Aviation, under which the HERWINGT project is being developed.

One of the most recent major projects, which is essential to mention, particularly regarding morphing technologies, is Clean Sky 2. Within this programme, two technologies must be cited for their role in the subsequent development of the HERWINGT morphing technology.

The first is the *multifunctional flap*, designed by the University of Naples "Federico II" in collaboration with Siemens, and intended to be mounted on Airgreen2, a 90-seat regional transport aircraft. This technology can be seen as the natural continuation of the Adaptive Trailing Edge Device (ATED) developed in the European programme SARISTU [14] (Smart Intelligent Aircraft Structures, 2011–2015). This project also took inspiration from the Canadian morphing camber aileron CRIAQ-MDO505 [8, 34] for the segmented skin.

In particular, the same design team involved in the advanced demonstration of the Clean Sky 2 morphing flap was responsible for the structural design and validation of the two morphing devices cited above.

This multifunctional flap was created with the idea of replacing a traditional double-slotted flap with a multimodal architecture that dynamically varies its camber.

The developed technology utilises a "fairingless" deployment mechanism entirely housed within the wing, offering significant benefits in terms of weight and aerodynamic drag reduction. To comply with strict industrial requirements, the system relies on an architecture based on conventional mechanical systems. This arrangement is characterised by metallic components in relative motion (referred to as "finger-like" robotic ribs) that drive an external segmented skin. Contrary to what was proposed by Flexsys Inc., the flap camber variation does not require any structural elements to be bent to enable shape morphing; this clearly represents a great advantage in terms of overall system robustness and fatigue life [17, 34].

The device was engineered as a multi-modal flap capable of operating in three distinct morphing configurations:

- Overall camber morphing: designed to enhance high-lift performances during take-off and landing by increasing the maximum attainable lift coefficient and the stall angle;
- High-speed tab deflection: employed during cruise conditions, this mode locks the main rib segments and actuates only the terminal portion (tab) of the flap to maximise aerodynamic efficiency at high speeds;
- Differential twisting: a span-wise twist applied to redistribute and alleviate structural loads.

Thanks to the flap's adaptivity across these different flight phases, the aerodynamic efficiency of the reference aircraft saw a 5% increase compared to a conventional wing, yielding a proportional reduction in fuel burned per flight and pollutant emissions [34].

From the perspective of industrial advancement, the architecture is highly efficient: it utilises a "through-shaft" transmission line requiring only six localised actuators to drive the entire flap, which measures approximately 5 m in span for the full-scale demonstrator. Furthermore, system reliability is ensured through a fail-safe approach based on permanent magnetic brakes. In the event of electrical power loss or a failure in the morphing functionality, the architecture automatically locks; under these circumstances, the system continues to operate as a conventional rigid flap, with no relevant effects on aircraft operations or flight safety [17, 34].

The second technology that must be cited within the Clean Sky 2 framework is the full-scale morphing droop nose, developed by Politecnico di Milano. Designed for the Airgreen2 regional aircraft, this device aims to delay wing stall during high-lift conditions while preserving a seamless, gap-less natural laminar flow profile during cruise. To achieve this, the device employs a composite compliant structure. From a material perspective, it effectively combines internal rib bodies made of high-strength 7075 aluminium alloy (Ergal), with a flexible compliant skin made of S-glass fibre fabric and epoxy resin. The successful implementation and testing of these specific materials at a full-scale industrial level strongly legitimises their use for compliant morphing applications. Furthermore, the experimental validation of this prototype is of fundamental importance to this work, as it utilises a non-intrusive optical tracking setup identical to the one adopted in this thesis. To evaluate the quality of the morphing shape and its ability to maintain this shape under aerodynamic loads, researchers employed a Qualisys® motion capture system equipped with 8 Miquis M3 cameras. As shown in Figure 2.6, 155 spherical reflective markers were distributed over the skin's outer surface, with a higher concentration at the leading edge where the curvature variation is greatest.



Figure 2.6: Experimental setup of the full-scale morphing droop nose developed by Politecnico di Milano, highlighting the reflective markers used for the Qualisys® tracking [16].

Since the cameras only track discrete points, the continuous 3D surface of the droop nose was mathematically reconstructed using a procedure based on Radial Basis Functions (RBF). This method interpolates the displacements of the markers onto a finer numerical grid, allowing for a highly accurate comparison with the target models. Ultimately, the experimental tests successfully demonstrated that the compliant structure could reliably achieve the target aerodynamic shape under landing loads with negligible deviations. This outcome not only proves the effectiveness of the selected composite materials, but also validates the combined use of Qualisys® tracking and RBF mathematical reconstruction as a robust methodology for analysing morphing structures [16, 17]. Following these successful full-scale ground and structural validations, both the multifunctional flap and the morphing droop nose technologies have achieved an estimated TRL between 5 and 6.

2.3.6. Morphing wind tunnel tests at PoliMi

In the context of the experimental validation of large-scale adaptive high-lift surfaces, a precedent of primary importance is represented by the multi-disciplinary Smart Morphing & Sensing (SMS) project [5], funded under the Horizon 2020 framework programme. This project deserves special mention in the present discussion since the experimental campaign was conducted at the same facility (the Politecnico di Milano Wind Tunnel, GVPM) used for the tests described in this thesis, thus addressing analogous technological and safety challenges [5].

The objective of the *SMS* wind-tunnel test campaign was to evaluate the effectiveness of

the different morphed shapes that could be provided by the trailing-edge flap to improve the aerodynamic performance of the wing section. In particular, the main parameters evaluated were the wing lift and aerodynamic efficiency in both landing and take-off configurations. The tested model consisted of a large-scale prototype (75%–80% scale relative to the outboard region of the main wing of an Airbus A320 class aircraft), featuring a span of 4 m and a total chord in take-off configuration equal to 2.7 m. The wing model features a framed structure, with a structural skeleton and a non-loaded skin. The skeleton is composed of two steel spars that extend along the full span of the model and ten aluminium ribs transferring the aerodynamic loads from the skin to the spars. The morphing flap was fastened to the wing structure by a mechanical interface allowing the positioning of the flap in take-off and landing configurations [5].

Due to the considerable size of the model and the resulting high degree of wind-tunnel blockage within the test section, the authors deemed it necessary to perform a preliminary fluid-dynamic compatibility study. Using a 1 : 9 scaled model of the wind tunnel itself, it was verified that the high flow deflections induced by the wing would not cause the diffuser to stall. The outcome of this investigation confirmed the absence of massive separation, although the high blockage resulted in a 20% reduction of the maximum achievable upstream free-stream velocity, thereby limiting the full-scale model tests to a maximum velocity of 40 m/s.

An additional methodological aspect of relevance to the present discussion concerned the pre-test structural and safety analysis. Given the kinematic complexity of the articulated flap, an eigenvalue modal analysis was carried out to prevent the onset of aeroelastic instabilities. The analysis revealed a low modal density (defined as the number of resonant modes of the structure within a given frequency band) alongside a clear separation between the first mode (associated with the morphing deformation at approximately 48 Hz) and subsequent modes (above 100 Hz), confirming the structural robustness of the design.

The experimental campaign adopted a multi-instrumental approach. Owing to the unfeasibility of using the GVPM strain-gauge balance (which was unsuitable for withstanding the aerodynamic loads expected for a model of such dimensions at high speeds), the aerodynamic performance was evaluated by integrating measurements from 141 static pressure taps arranged along the mid-span section. These were complemented by a Particle Image Velocimetry (PIV) survey to investigate the flow field over the upper surface region of the flap.

The approach adopted to quantify the actual deformation of the structure under aerodynamic loading is of particular methodological interest. To verify that performance was not

biased by excessive aeroelastic deformations, the authors employed a non-intrusive optical measurement technique utilising 8 Qualisys® Miquis M3 cameras. This motion-capture system enabled the precise reconstruction of the relative surface displacements, revealing that the aerodynamic loads induced vertical displacements at the flap trailing edge on the order of few percent of the flap chord, which nonetheless exerted a non-negligible effect on the overall aerodynamic performance of the wing [5]. The wing model installed in the test section, alongside a detail of the reflective markers employed for the shape acquisition, is shown in Figure 2.7.

The overall results of the campaign demonstrated that the greatest benefits derived from these geometries were achieved particularly in the landing configuration. In this phase, the morphed shape generated a 4% increase in lift and, thanks to a reduction in pressure drag resulting from minimised flow separation, provided an 8% increase in wing aerodynamic efficiency compared to the baseline configuration. This demonstrates the significant potential of morphing technology for civil transport aircraft, confirming that required maximum lift coefficients can be attained with lower flap extensions, thereby yielding subsequent mechanical advantages [5].



Figure 2.7: (a) shows the SMS model in GVPM facility; (b) shows a close-up of the reflective markers for shape acquisition [5].

3 | Design and numerical validation of the demonstrator

In order to demonstrate the feasibility of morphing aileron technology and its applicability on a full-scale aircraft, an extensive study process is required.

As presented in Chapter 2, several studies have demonstrated the advantages of morphing technologies, but few have proven their applicability on a large scale. Therefore, it is not enough to select a configuration that merely satisfies all the morphing challenges listed in Section 2.1; it is necessary to find a configuration that can be practically applied at a full scale.

The design phase of such a complex device required a multi-disciplinary design optimisation process, able to combine aerodynamic performance, kinematic and structural requirements, and actuation aspects related to the compliant structure concept on which the morphing device is based.

The aim of this chapter is to provide a description of the morphing aileron configuration and the objectives of the technological demonstrator. It should be explicitly noted that the entire aero-structural design, alongside the virtual testing phases, was conducted prior to the current thesis work. Therefore, this chapter summarises the main outcomes previously published in [11]. Specifically, it details the kinematic architecture, the multi-disciplinary design phase, and the results of the virtual static and dynamic tests to present the obtained optimal solution and provide a numerical baseline for the expected experimental outcomes. Building upon this theoretical and numerical framework, the physical prototype and its individual components will be thoroughly detailed in Chapter 4.

3.1. Introduction to the experimental prototype

As briefly outlined among the general project objectives in Section 1.1.4, the morphing aileron concept is engineered to overcome the aerodynamic and structural limitations of conventional control surfaces in both static and dynamic functionality. In particular, the

main objectives that this demonstrator aims to achieve are [11]:

- **Seamless aerodynamic surface:** provide an external surface that is completely gapless, eliminating the discontinuities typical of traditional ailerons to improve aerodynamic efficiency and reduce parasitic drag. Concurrently, the morphing device must match or exceed the aerodynamic performance of a conventional hinged aileron by providing equivalent increments in the lift coefficient across a range of flight conditions, thereby ensuring at least the same roll control authority. Specifically, it must achieve a lift increment equivalent to a reference deflection of $\pm 30^\circ$ of a conventional aileron at design manoeuvring speed, corresponding to $M = 0.29$ at sea level. This reference condition assumes an aircraft angle of attack of 2° in the aircraft reference system, with the local airfoil geometry including a local wing twist of 0.9° .
- **Reduced actuation effort:** thanks to its continuous shape and smoother curvature, the required lift variation can be obtained with a smaller overall deflection. Besides promoting greater aerodynamic efficiency, this leads to reduced actuation power requirements. Because the deflection is smaller, the actuation system has to counteract less aerodynamic work and overcome a lower structural elastic resistance, while performing a shorter kinematic stroke. In fact, a core project goal is to design a structure and kinematic system optimised to minimise these actuation efforts.
- **Structural efficiency:** another fundamental goal is to optimise the structural efficiency, minimising the overall weight while ensuring the structure is stiff enough to carry the required flight loads. The material must be distributed intelligently to solve the morphing paradox: the structure requires low in-plane stiffness to be easily deformed with minimal energy, but simultaneously high out-of-plane bending stiffness to withstand external aerodynamic pressures without unwanted deformations. By optimally distributing and reducing the material through dedicated compliant structure optimisation, actuation forces are minimised while required structural stiffness is maintained. This reduction in actuation effort yields a secondary weight-saving benefit, enabling the installation of more compact and lighter motors.
- **High-bandwidth dynamic performance:** the device is designed to operate with a bandwidth of up to 10 Hz. An ambitious goal is to demonstrate its viability for active control applications, such as gust load alleviation or flutter suppression. To facilitate this, the aileron is designed as a dynamic resonant system: by tuning its first natural vibration mode to fall within the target operational bandwidth, the required dynamic actuation forces are significantly reduced, further allowing for the

use of smaller actuators.

- **Certification-oriented design:** last but not least, the design is conceived with the aeronautical certification process in mind. In other words, the selection of materials and the kinematic architecture of the actuation mechanism have been tailored to facilitate eventual validation by certification authorities.

The morphing device is designed to be installed on a high-aspect-ratio strut-braced wing, an innovative configuration selected to boost aerodynamic efficiency and minimise fuel consumption in next-generation HERA. In the full-scale aircraft, the wing architecture incorporates three independent aileron devices covering a total span of five metres. However, due to the physical space and operational constraints of the wind tunnel facility, the actual technological demonstrator is limited to the outermost two metres of the wing span, comprising two independent morphing aileron modules. Furthermore, because the wind tunnel is limited to a maximum Mach number of 0.15, which is lower than the actual flight design conditions, a specific sizing modification was applied to the experimental prototype. To avoid oversizing the actuation system with respect to the actual aerodynamic loads expected during the test campaign, a 27% thickness reduction was implemented across the entire compliant structure. This structural adjustment successfully preserves the target morphing shape capabilities without compromising the quality of the external aerodynamic surface. Figure 3.1 shows the 3D model of the complete aircraft wing and the 3D model of the demonstrator attached to the wing box.

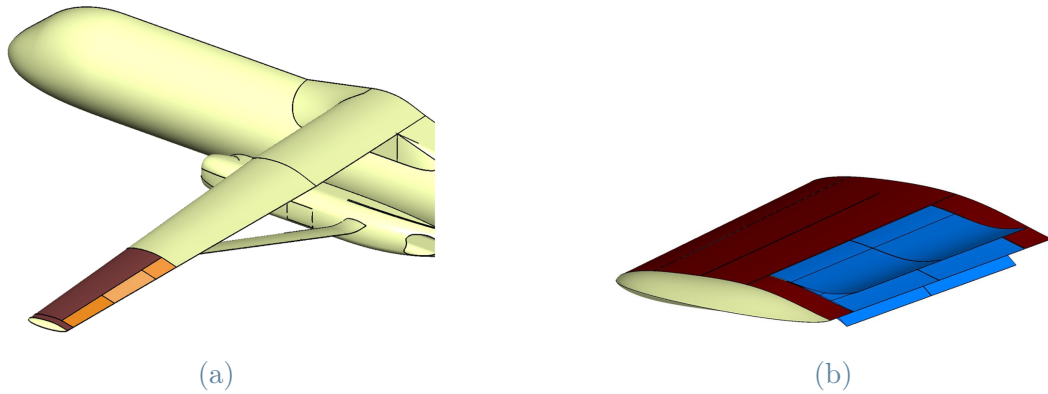


Figure 3.1: (a) shows the complete aircraft wing with the three real scale ailerons, while (b) shows the demonstrator designed for wind tunnel testing [11].

3.2. 3D model architecture and functionality

The demonstrator features two independent morphing ailerons, each spanning one metre, which are mounted onto a dummy wing-box (developed by Hellenic Aerospace Industry). They operate on the same functional principle but move independently, driven by separate electromagnetic linear actuators that displace the lower skin via two Scott-Russell linkages per aileron. The complete wind tunnel model spans 2.5 metres due to the inclusion of passive end pieces, which minimize aerodynamic tip effects on the active surfaces. Additionally, the wing model features a tapered planform, resulting in a variable chord length along the span. Figure 3.2 shows the 3D CAD model of the complete final structure, positioned vertically, viewed from both the lower (pressure) and upper (suction) sides to better illustrate the final shape of the model. The vertical configuration was chosen to facilitate mounting on the wind tunnel balance, given the considerable weight of the structure, which is approximately 350 kg.

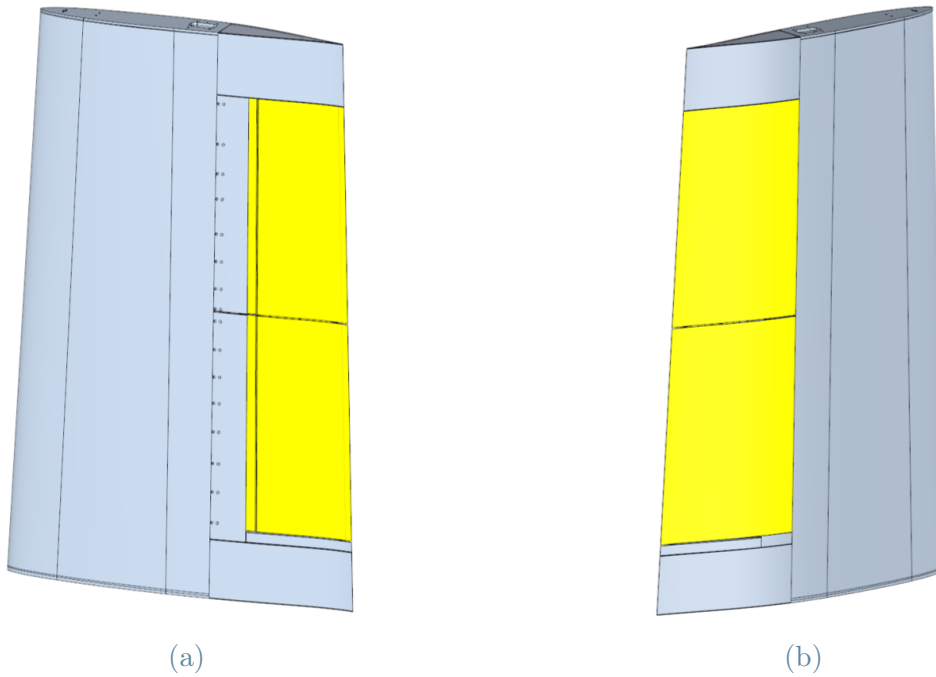


Figure 3.2: Vertical configuration of the 3D CAD model of the complete morphing demonstrator, showing (a) the pressure side and (b) the suction side.

The operation of the individual morphing aileron is based on the deformation of a compliant external skin made of S-glass fibre, which deforms under the forces generated by the linear actuator. The linear actuator consists of a main body, fixed to the rear spar (located at 60% of the chord) via an interface plate, drives a slider moving in the spanwise

direction. The two ends of this slider are connected to two rods, which allow the spanwise linear motion generated by the motor to be converted into a controlled displacement along the chord. Figure 3.3 provides a view from the upper side of the prototype, illustrating the position of the two electromagnetic motors and the direction of actuation, alongside a detailed close-up of a single actuator unit that highlights the fixed actuator body and the connection of the slider ends to their respective rods.

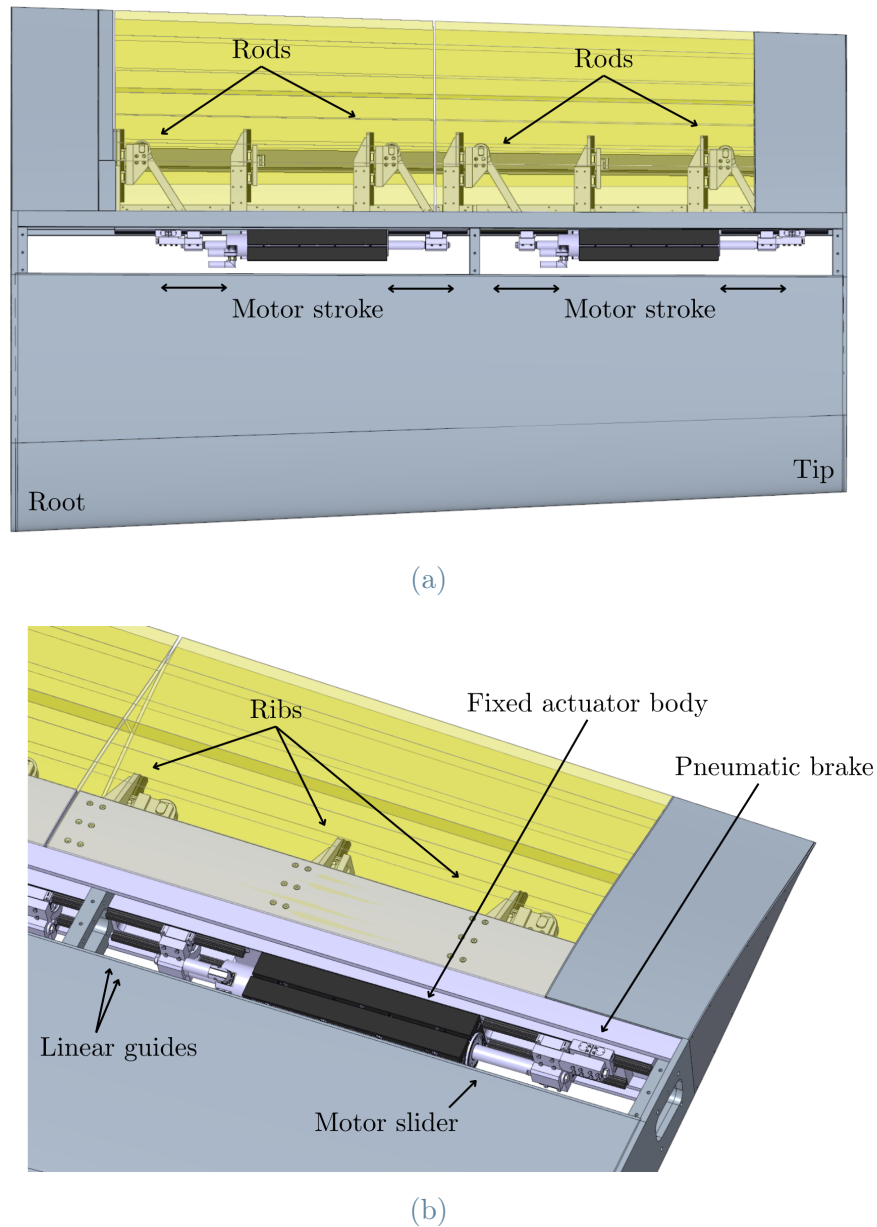


Figure 3.3: (a) Uncovered upper side view of the prototype, detailing the complete internal kinematic system, motor actuation direction, and rod connections. (b) Detailed close-up of a single electromagnetic actuator unit, identifying key components.

The opposite ends of the two rods are constrained to two linear guides installed on the ribs. These guides constrain the movement to the chordwise direction, ensuring low-friction motion while simultaneously supporting the aerodynamic loads acting on the skin. The lower skin of the morphing aileron connects to these rod ends, forcing it to move in the chordwise direction (along the tangential direction of the airfoil surface to accommodate the deformation). By applying pull or push forces to the lower skin through this mechanism, the entire structure bends to generate downward or upward deflected shapes, respectively.

The connection, located at 70% of the chord, is not made directly to the fibreglass of the aileron, but rather through a spanwise carbon-fibre stringer and three sliders. As detailed in Figure 3.4, this arrangement allows the loads (originating from both the actuation system and the aerodynamics) to be better distributed along the spanwise direction, ensuring a smooth shape transition and preventing anticlastic distortion. Specifically, the figure highlights the carbon stringer running along the entire span and the three attached sliders, which connect the aileron to linear guides sliding in the chordwise direction. The two outermost sliders are directly linked to the rods transmitting the motor's motion, whereas the central one serves exclusively to prevent out-of-plane bulging along the span, being connected solely to its linear guide.

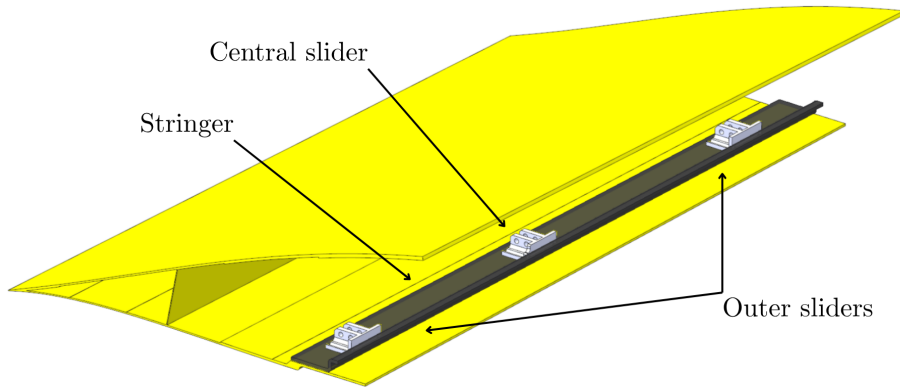


Figure 3.4: Detailed view of the aileron highlighting the spanwise carbon stringer, the two outer sliders, and the central slider.

The upper skin is clamped at 70% of the chord via ribs supporting the non-morphing region between the rear spar and the morphing device, so, the deformable portion of

the aileron begins at a length equal to 70% of the chord. Moreover, shape variation is facilitated by an internal flexible spar connecting the upper and lower skins. This also provides structural integrity and preserves the airfoil thickness while successfully resisting axial stretching during deformation. Additionally, the shape variation is supported by an optimised skin thickness distribution in the chordwise direction, which simultaneously helps to sustain the aerodynamic loads. A detailed cross-sectional view highlighting both this variable thickness skin and the internal compliant spar is provided in Figure 3.5.

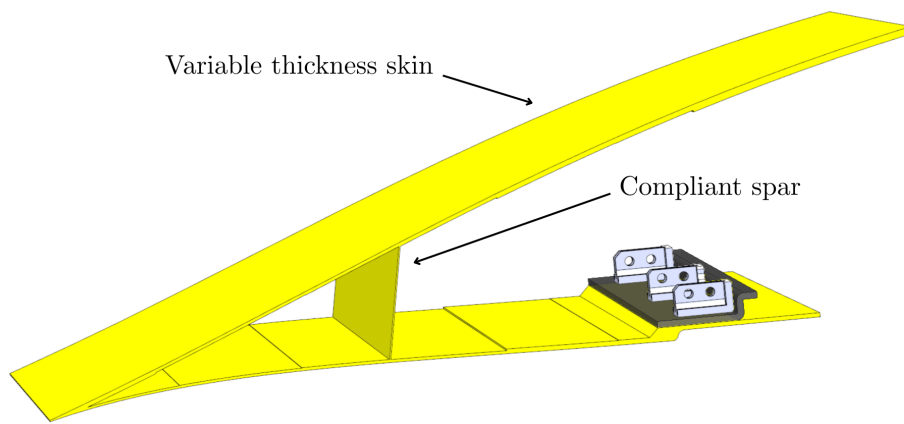


Figure 3.5: Cross-sectional view of the aileron, highlighting the internal compliant spar and the variable thickness skin.

Figure 3.6 displays the aileron with a level of transparency that allows these internal details to be seen (the opposite ends of the rods constrained to slide along the linear guides, and the connection to the aileron stringer). In addition, the same figure shows how the sliding skin principle is exploited for the lower surface: a small panel is used over which the lower skin of the aileron moves, effectively closing any gap as the aileron transitions from minimum to maximum deflection.

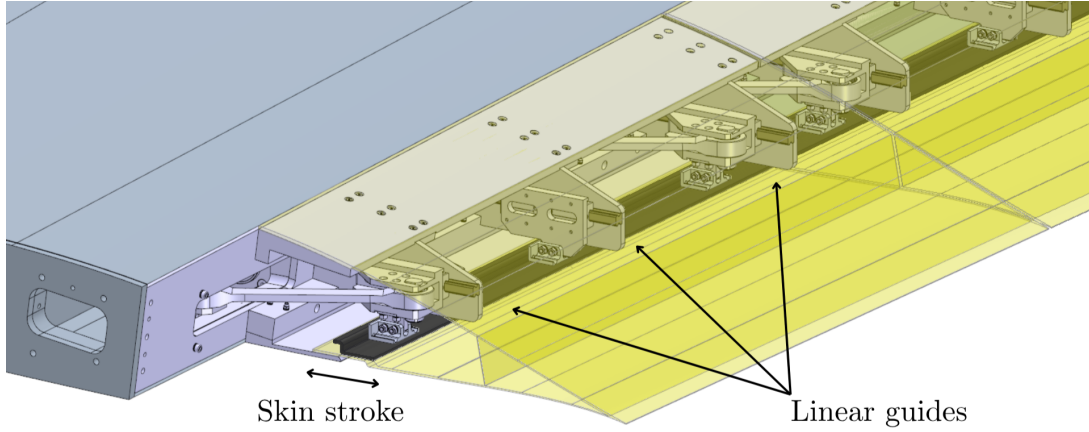


Figure 3.6: Detailed view of the prototype with a semi-transparent aileron. The internal kinematics, including the slider-rod constraints along the linear guides, are visible, alongside the sliding skin mechanism on the lower surface.

3.3. Design of the Scott-Russell mechanism

To fully understand how the force is transmitted from the motor to the aileron's lower skin, it is essential to detail the functioning of the Scott-Russell mechanism, upon which the current actuation system is based.

Specifically, two such mechanisms are coupled with each motor. These kinematic linkages translate the spanwise linear motion, represented in Figure 3.7 as the horizontal displacement Δx_A of point A (where one end of the slider, rod 1, and the spanwise linear guide are constrained together), into the chordwise motion of the lower skin, represented as the vertical displacement Δy_C of point C. Point C serves as the connection node between the rod 1, the lower skin, and the chordwise linear guide.

The relationship between Δy_C and Δx_A is non-linear, governed by the following kinematic equation:

$$\Delta y_C = \sqrt{4 L_1^2 - (2 L_1 \cos \theta_0 + \Delta x_A)^2} - 2 L_1 \sin \theta_0 \quad (3.1)$$

where θ_0 is the angle between rod 2 and the shaft axis direction in the neutral position of the linkage (i.e., when the morphing aileron is in its undeformed configuration).

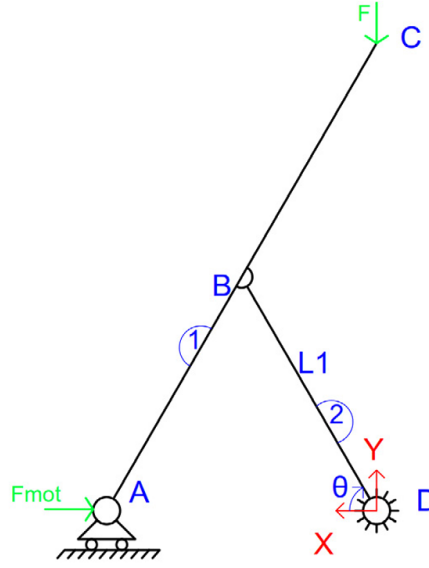


Figure 3.7: Scott-Russell linkage that connects the actuator to the morphing device lower skin [15].

Regarding force transmission, the relationship between the reaction force of the deformed aileron at point C (F_s) and the actuation force exerted by the motor at point A (F_m) is defined as:

$$F_m = \frac{F_s}{\tan \theta} \quad (3.2)$$

A single actuator could potentially be connected to multiple Scott-Russell mechanisms at various spanwise locations (for instance, by placing a single motor at the wing root), thereby minimising both the number of actuators and the overall system inertia. However, for this demonstrator, as previously shown in Figure 3.3, a localised configuration was chosen where the slider actuates the mechanisms directly at its two respective ends.

It should be noted that the traditional Scott-Russell linkage was mechanically simplified in this setup by removing rod 2 and installing a direct linear guide at point C.

Another advantage of this specific linkage is its ability to resist undesired lateral loads, which are absorbed by the main bearings at point D. This robustness protects the electromagnetic motor from unexpected operational scenarios.

In particular, this proposed kinematic solution, proves to be highly effective for the current morphing aileron design. The spanwise actuation system adopted in this work requires a lower force for upward deflections and a comparable force for downward deflections when

compared to a morphing solution based on a chordwise actuation force applied directly to the skin.

This asymmetry is directly caused by the trigonometric non-linearity of the linkage: the relationship between the slider displacement and the resulting aileron rotation is non-linear. Consequently, positive aileron deflections require shorter motor strokes but generate higher peak forces, whereas negative deflections demand longer strokes but involve lower forces. Due to this specific mechanical behaviour, the proposed spanwise actuation presents a unique advantage if applied to aerodynamic surfaces that deflect in a single direction, such as a morphing flap. In such scenarios, the mechanism could be tailored to operate within the stroke range that demands the lowest actuation effort.

However, a limitation of this kinematic configuration is its inability to accommodate wing twist deformations. In such cases, the proposed compliant structure would need to be paired with a different, distributed actuation architecture capable of independent local control.

3.4. Design and numerical validation of the morphing ailerons

Having detailed the physical architecture and the kinematic mechanisms of the morphing demonstrator, it is fundamental to introduce the computational framework that led to this final configuration. Although the primary focus of this thesis remains the experimental assembly and testing of the physical prototype, this section introduces the multi-disciplinary design optimisation and the subsequent 3D numerical validation processes. Providing this theoretical background is crucial for understanding the complexity involved in sizing such a compliant device, and it establishes a numerical baseline against which the experimental results will be compared. For an exhaustive description of the numerical methodology, the reader is referred to [11].

3.4.1. Multi-disciplinary design optimisation

The design optimisation process was initially performed on a single 2D reference airfoil section. Specifically, the cross-section chosen as the baseline for the optimisation was the root section of the inner aileron, located 2 metres from the wing tip. Once the optimal 2D design was obtained for this reference section, the procedure was extended to other positions along the span to reconstruct the complete 3D geometry of the device, aiming to achieve a nearly constant deflection. In this spanwise expansion, the optimised parameters

were directly applied to the inner aileron of the demonstrator (from 2 m to 1 m from the tip). Conversely, a scaling factor of 0.92 was introduced for the outer aileron section (from 1 m to the tip) to maintain a uniform actuation force requirement per unit stroke and prevent the shorter tip region from becoming excessively stiff.

Specifically, this section focuses on the 2D design phase and highlights the numerical advantages of the morphing concept over a conventional hinged aileron. In Section 3.4.2, further numerical assessments conducted on the 3D geometry will be presented, aimed at verifying the geometric fidelity, structural strength, actuation loads, and dynamic response of the complete device.

Fundamentally, the design of this reference 2D profile was carried out in two distinct stages. The first level of the HERWINGT design framework consists of an *aero-structural shape optimisation*. Its goal is to define the optimal external geometry of the morphing aileron before establishing its internal architecture. To limit the number of variables and ensure the physical feasibility of the deformation, the geometry is parametrised using the Class/Shape function Transformation (CST) mathematical method. This approach controls shape variations primarily through two variables: the equivalent trailing-edge rotation δ and the variation of the boat-tail angle $\Delta\beta$ (both illustrated in Figure 3.8). Geometrically, the vertex of the equivalent trailing-edge rotation δ is located at the centre of the inscribed circle tangent to both the upper and lower surfaces, at the exact chordwise location where the morphing deformation begins.

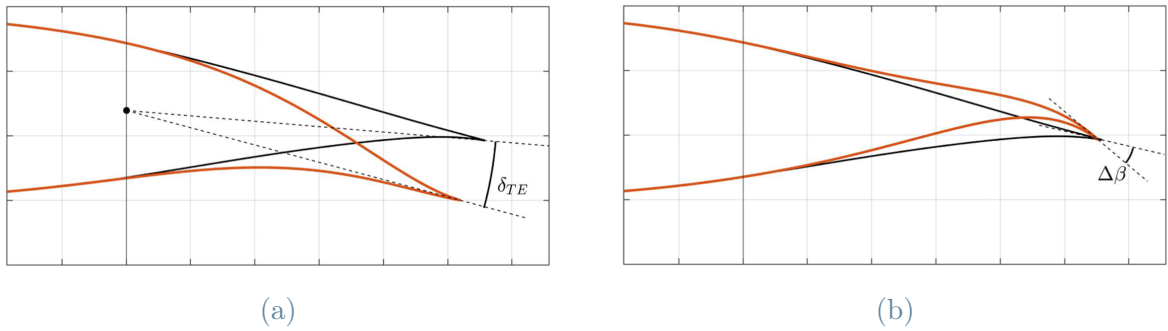


Figure 3.8: Aerodynamic shape parametrisation using the CST method, visually defining (a) the equivalent trailing-edge deflection δ and (b) the boat-tail angle variation $\Delta\beta$, with the vertical line marking the deformation start at 70% chord [11].

The optimisation is set up as a multi-objective problem aimed at simultaneously minimising the aerodynamic drag coefficient (C_d) and the actuation force required to slide the lower skin (F_s). The algorithm operates under aerodynamic and structural constraints:

the morphed shape must provide a lift coefficient at least equivalent to that of a conventional aileron deflected by $\pm 30^\circ$ at the design manoeuvring speed. Furthermore, it must leave the wing-box region unaltered, whilst avoiding axial strain of the upper skin and excessive curvature variations to prevent material failure. To reduce the computational effort, the process avoids evaluating every single shape with direct fluid dynamic computations; instead, it leverages a Design of Experiments (DOE) approach coupled with RANS CFD simulations to build mathematical response surfaces. The final outcome of this phase is a Pareto front, from which the designers extract the definitive target shapes. The selection prioritises configurations capable of minimising the effort required to slide the lower skin (which is directly linked to the actuator effort required by the motor through a kinematic relation), while ensuring a clear aerodynamic improvement over the baseline aileron.

The selected target shapes are characterised by the optimal variables $\delta = 14^\circ$ and $\Delta\beta = 25^\circ$ for the downward deflection, and $\delta = -15^\circ$ and $\Delta\beta = -27^\circ$ for the upward deflection. These correspond to lower skin strokes of -35.5 mm and 37.9 mm, respectively.

The second level of the design process, referred to as *compliant structure optimisation*, focuses on the synthesis of the aileron's internal structure according to the principle of distributed compliance. Starting from the optimal shapes calculated previously, the objective is to design a compliant architecture capable of deforming to accurately replicate those geometries. This synthesis is performed using a multi-objective genetic algorithm coupled with non-linear finite element analyses (FEA). The algorithm acts simultaneously on topological and sizing variables: it determines the number and arrangement of the internal flexible spars connecting the skins (defined as beam existence and destination in Figure 3.9), and optimises the thickness distribution of both the external skin and the internal spars (defined as boundary size and beam size in Figure 3.9).

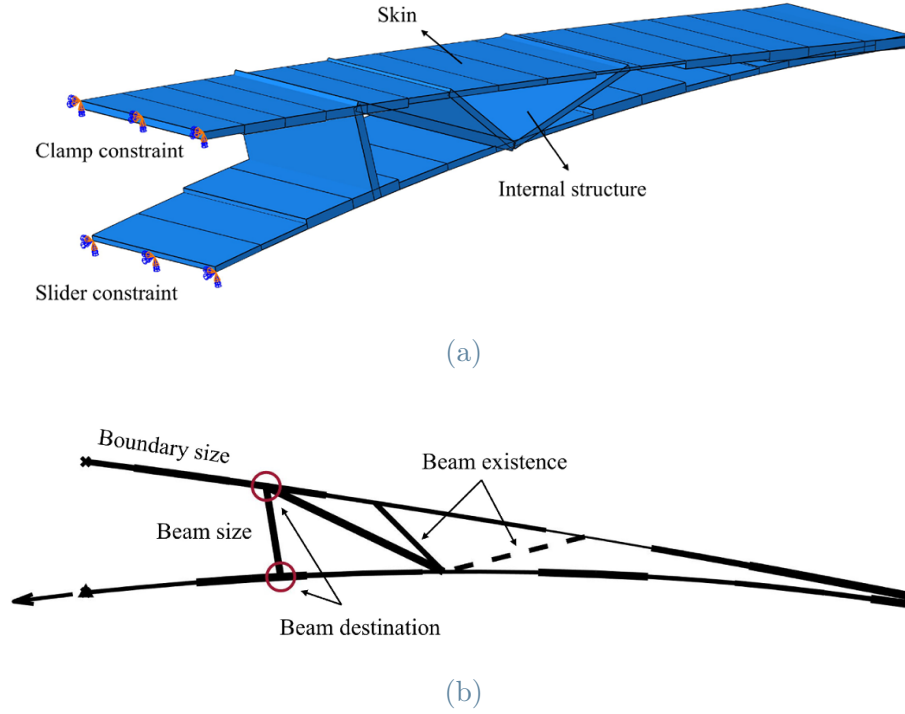


Figure 3.9: (a) and (b) show the finite element model and optimization variables for the structural design of the morphing aileron [11].

This problem is formulated to balance conflicting requirements. From a kinematic perspective, it minimises the least-square geometric error between the deformed actuated structure (computed via non-linear FEA in Abaqus®) and the aerodynamic target shapes (maximum upward and downward deflections) under their corresponding aerodynamic loads at manoeuvring speed ($M = 0.29$, $h = 0$ m, $\alpha = 8^\circ$ for positive deflection, $\alpha = -4^\circ$ for negative deflection), ensuring the necessary flexibility for actuation. From a structural perspective, it minimises the passive deformation of the unactuated configuration under the most critical flight condition loads ($M = 0.45$, $h = 0$ m and $\alpha = 4^\circ$), guaranteeing the required stiffness to withstand aerodynamic forces without the need for continuous power to maintain the position.

Furthermore, to ensure the morphing aileron's dynamic behaviour aligns with a 10 Hz actuation bandwidth, two distinct boundary conditions were imposed to achieve both actuation effort reduction and control tracking precision. On one hand, to allow the structure to act as a dynamic resonant system when the lower skin is free to slide, the natural frequency is tuned within or slightly above the target bandwidth, to exploit structural resonance to reduce the dynamic actuation force compared to static requirements. On the other hand, to prevent the compliant device from acting as a low-pass filter,

which would delay the control response, the natural frequency computed with locked actuation (fixed lower skin) is constrained to be well above the operational bandwidth (e.g., > 50 Hz). This distinction ensures that the structure remains dynamically faster than the actuation, allowing the aileron to follow fast control commands accurately and in phase.

From the resulting Pareto front, the optimal topology is extracted, upon which a rigorous local sizing optimisation using a sequential quadratic programming algorithm is executed. This last phase further refines the thicknesses to reduce the peak actuation force required from the motor, while preserving the aerodynamic quality of the final shape.

Figure 3.10 shows the comparison between target shapes obtained in the aero-structural shape optimisation and the deformed shapes obtained at the end of the compliant structure optimisation, for downward and upward deflection.

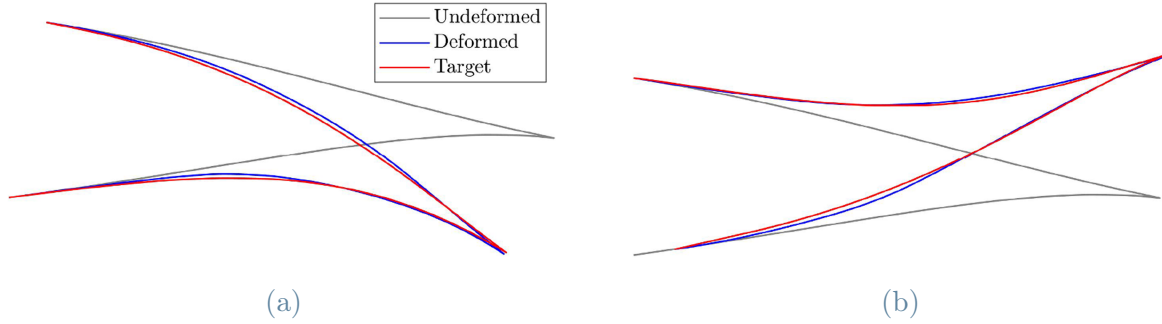


Figure 3.10: (a) downward and (b) upward deflection comparison between target and deformed shapes [11].

At the end of this design process, maximum strain constraints were assessed (as seen in Figure 3.11), even though preliminary analytical estimates indicated that the strain levels required for the target curvature would be non-critical.

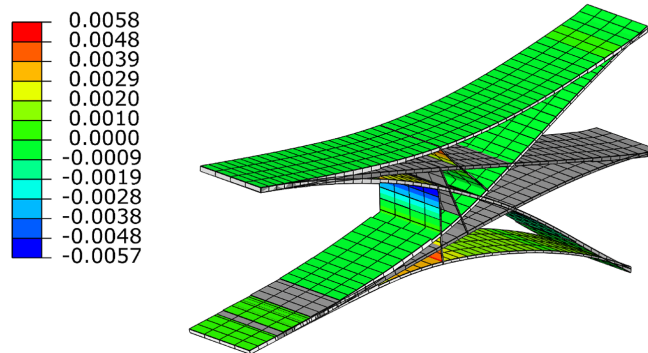


Figure 3.11: Max principal strain of the final configurations [11].

Subsequently, a modal analysis conducted on this design confirmed the effectiveness of the structural synthesis: when evaluated with the lower skin free to slide, the first natural frequency is 10.9 Hz, successfully satisfying the dynamic constraint of the optimisation. Conversely, when the lower skin is fixed, the frequency rises significantly to 51.5 Hz, ensuring that the structural response remains perfectly in phase with the actuation command throughout the entire operational bandwidth of interest.

At the design manoeuvring speed, the morphing aileron prototype numerically demonstrated compliance with the target lift coefficient, which was required to be equal to or greater than that of the conventional hinged aileron. For positive deflections, the morphing device achieved a higher C_l , whereas it yielded an equivalent value in the case of negative deflections. Additionally, the morphing configuration exhibited a significantly lower aerodynamic drag, decreasing by approximately an order of magnitude for positive deflections at an identical C_l value.

The assessment of the actuation forces was carried out through dedicated finite element analyses. For the morphing aileron, the total force demanded from the actuator comprises two distinct elements: a structural requirement, needed to deform the compliant architecture, and an aerodynamic requirement, necessary to withstand the external fluid pressures. The structural portion is quantified as the force required to achieve the target morphed configuration in the absence of aerodynamic loads. Subsequently, the aerodynamic portion is isolated by subtracting this purely structural effort from the total actuation force computed under load. Conversely, the actuation effort for the traditional hinged aileron is governed solely by aerodynamic forces. This is evaluated by calculating the hinge moment of the rigid surface during its rotation. Such an evaluation assumes the implementation of a fully internal kinematic linkage, given that any external modification altering the aerodynamic profile of the reference wing are not permitted.

Despite the presence of an additional structural contribution, the overall actuation force required for the morphing solution remains lower than that of the hinged configuration. Specifically, for downward deflections, this total force is reduced by approximately 30%. This is due to the morphing aileron's ability to reach the target lift coefficient with a smaller trailing-edge rotation compared to the large rigid deflection demanded by a conventional surface.

3.4.2. Three-dimensional numerical validation

The 2D design was propagated into a 3D finite element model representing the two-metre span of the aileron. As introduced in Section 3.4.1, this spanwise expansion incorporated

a scaling factor for the outermost 1-metre section.

Then, subsequent numerical analyses on the complete model were conducted to validate the 2D results.

The optimal morphing shapes corresponding to maximum downward and upward deflection were provided to Leonardo S.p.A. for aircraft-level CFD RANS simulations to assess the aerodynamic benefits of the morphing aileron. The resulting surface pressure distributions were used to derive the 3D aerodynamic loads for the structural verification.

Static structural verifications, performed in Abaqus®, confirmed the safety of the full-scale device, yielding a maximum Tsai-Hill failure index of 0.32 for the downward deflected configuration under ultimate load at manoeuvring speed with $\alpha = 15^\circ$ (corresponding to a safety margin of 2.1, which confirms that the structural strength requirements are satisfied), alongside a buckling safety margin of 13 in the dive speed condition.

In terms of geometric fidelity, the deformed surfaces maintain a continuous profile without any localised distortions. The discrepancy between these resultant profiles and the initial target shapes is negligible. This demonstrates that the structure can successfully mimic the idealised 3D shapes generated via the CST parametrisation, even when applied to a finite-span and tapered configuration. Moreover, the overall impact of aerodynamic loads on the final morphed geometry remains marginal. In Figure 3.12, a comparison between the target shapes and the finite element deformed geometries (with and without the aerodynamic loads) is presented.

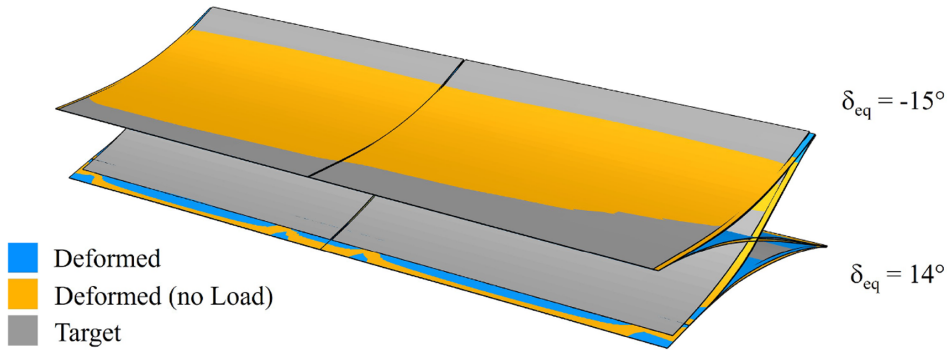


Figure 3.12: Comparison between 3D target shapes and finite element deformed shapes with and without aerodynamic loads [11].

Alongside these geometric assessments, the required actuation forces were also computed, showing consistency with the baseline 2D results.

To evaluate the dynamic characteristics of the system, specific modal analyses were conducted. When the actuation points are fixed, the first vibration mode reaches 56.5 Hz. Such a high value safely prevents any unwanted dynamic interaction with the intended 10 Hz operational bandwidth. Conversely, a subsequent evaluation performed with the lower skin allowed to slide freely revealed a first mode frequency of 12.6 Hz, meeting the 10 Hz requirement.

To understand how the structure behaves in motion, virtual dynamic tests were carried out on the 3D model. The estimated mass of the actuation system, which is 6.4 kg, was integrated into the model, reducing the natural frequency in the free command configuration. To mimic the laboratory dry tests that will be detailed later in this thesis, no aerodynamic loads were applied during these simulations. A displacement-controlled analysis was performed by imposing a wave-like movement with a 20 mm amplitude. A range of frequencies was tested, increasing the speed linearly from 0 to 12 Hz over a 24 s period. During the simulation, the required force started to decrease, reaching a lowest point before rising again. This minimum effort occurred at 10 Hz, marking an anti-resonance peak. At this specific frequency, the natural flexibility of the 3D structure acts in synergy with the actuation, which reduces the required motor force. This result supports the idea of using a dynamic resonant system. A phase graph also showed a clear phase inversion around this anti-resonance point, resulting in an out-of-phase response that fully matched the trends observed in the time histories.

Overall, within the normal working range of 0 to 10 Hz, moving the flexible structure dynamically takes less effort than holding it still in a static position. While these specific results belong to a system tuned to a 10 Hz frequency, tuning it anywhere between 5 and 10 Hz would still keep the dynamic forces lower than the static ones. Nevertheless, the actuator stroke and the out-of-plane displacement of the trailing edge remained in phase throughout the entire test, confirming the absence of any command inversion. The structural dynamics are considerably faster than the actuation and do not introduce any delay, as demonstrated by the high fixed-command first mode frequency.

Finally, the physical tests on the demonstrator will be essential to accurately measure the damping behaviour of the system, as this feature is very difficult to calculate using only numerical simulations.

However, as previously introduced in Section 3.1, the entire compliant structure of the experimental prototype underwent a further 27% thickness reduction to adapt to the lower aerodynamic loads achievable in the wind tunnel facility. Consequently, this overall reduction in stiffness led to a decrease in the first resonant frequency of the model com-

pared to the full-scale design target of 10 Hz. Specifically, the first modal frequency of the individual ailerons dropped to approximately 8.9 Hz. It must be noted that this represents an initial reference value for the wind tunnel model, evaluated without including the actuator mass.

Figures 3.4 and 3.5 present the final optimised configuration of the inner aileron, scaled for the wind tunnel tests. These figures show that the final assembly consists of a flexible skin characterised by a variable thickness in the chordwise direction, supported by a single internal compliant spar. The outer aileron shares this exact structural topology and external shape, with the only distinction being the scaled distribution of its thicknesses.

4 | Experimental ground test rig: hardware setup and system architecture

This chapter marks the transition from the numerical design phase to the practical experimental activities conducted for this research. The main objective is to provide a clear and complete overview of the physical demonstrator. The text explains the working principles of the prototype and details its main components, covering both the physical hardware mounted on the test rig and the software interfaces required to communicate with and control the actuation system. To keep the focus strictly on the functionality and operation of the device, the manufacturing and assembly procedures are intentionally omitted.

Outlining the experimental setup is essential to prepare the reader for the testing phases discussed later in the thesis. A solid understanding of the hardware and control architecture is, in fact, necessary to fully follow the subsequent experimental analyses.

Figure 4.1 illustrates the assembled physical prototype in its laboratory testing configuration. As shown, the demonstrator is secured to the laboratory floor through its root rib, which is the exact structural interface that will later connect the model to the wind tunnel balance. Notably, the model is also constrained at the top rib. This upper constraint was introduced as a safety measure to adequately support the structure during preliminary high-frequency dynamic tests. While the wind tunnel setup will strictly rely on the single bottom attachment, adding the top constraint during ground tests does not compromise the validity of the results. The experimental focus at this stage was exclusively on the dynamic response of the morphing ailerons themselves, rather than the global dynamic behaviour of the complete wing assembly.

Additionally, for logistical reasons, the 3D-printed passive end-piece at the root, designed to minimise aerodynamic tip effects on the active surfaces, is absent in this laboratory setup, although it will be properly integrated prior to the wind tunnel campaign. The visual representation clearly displays the main dummy wing-box and the two independent

morphing ailerons. Furthermore, the external covers over the actuation bays have been intentionally removed. This exposes the internal hardware, providing a clear view of the electromagnetic linear actuators and the associated mechanical components.



Figure 4.1: Physical prototype of the morphing aileron demonstrator, showing (a) the pressure side and (b) the suction side.

To provide a structured breakdown of the experimental assembly, the remainder of this chapter is organised into three dedicated sections:

- Section 4.1 details the primary actuation hardware and mechanical components installed on the test rig, focusing on the linear motors, the braking systems, and the cooling units.
- Section 4.2 outlines the power supply architecture and the electrical cabinet layout that houses the servo-drives responsible for low-level motor management.
- Section 4.3 describes the control software environment and the Human-Machine Interface (HMI), explaining the distinct operational modalities used to drive the system (specifically the manual and automatic modes).

4.1. Actuation hardware and mechanical setup

This section is dedicated to the physical architecture mounted directly onto the experimental bench. The discussion is strictly confined to the mechanical hardware and the actuation units.

4.1.1. S-glass fibre morphing ailerons

Starting with the fundamental element of the project: the two morphing aileron segments (Figure 4.2). Manufactured by ELIGIO RE FRASCHINI S.p.A., the external compliant skin is constructed from an S-glass fibre EE302S ER450 38% composite fabric, the mechanical properties of which are detailed in Table 4.1. To effectively transfer the mechanical power from the actuators to the deformable surface, a dedicated carbon-fibre stringer is bonded to the lower skin of each aileron. This stringer features a distinct "S" cross-section, specifically designed to increase the bending stiffness of the lower skin. This structural interface distributes the chordwise motion along the entire wingspan by transferring actuation loads primarily through shear forces.



Figure 4.2: The two unassembled segments of the S-glass fibre morphing ailerons.

Figure 4.3 provides a detailed view of this assembly. It highlights the bonding between the carbon stringer and the aileron's lower skin, where a temporary positioning screw was used to ensure precise alignment during the adhesive curing process. Furthermore, the

three metallic sliders are secured to the carbon stringer using a robust combination of structural adhesive and safety screws.

As previously described in Section 3.2, the two outermost sliders on each aileron act as the primary interface with the actuation system: they are directly connected to the two Scott-Russell kinematic mechanisms that transmit the motor's mechanical power. Conversely, the central slider is not driven by the actuator; it connects exclusively to a linear guide to prevent anticlastic distortion and out-of-plane bulging along the span.

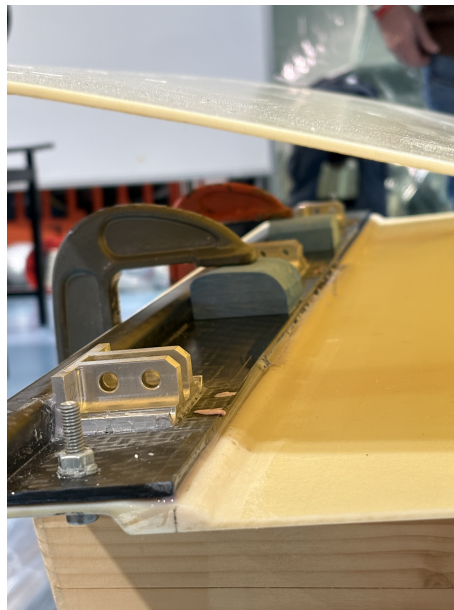


Figure 4.3: Detailed view of the carbon-fibre stringer bonded to the lower skin, with the metallic sliders secured via structural adhesive and safety screws.

Interestingly, during the preliminary testing phase, this central constraint was found to over-stiffen the outer aileron, needlessly increasing the required actuation forces (even though its structural thickness had already been reduced during the design phase). Although both ailerons were initially designed to exhibit similar stiffness profiles, experimental observations revealed that the slightly larger inner aileron behaved as comparatively less stiff in practice. Consequently, the central slider's connection to the linear guide was maintained solely on the inner aileron, while it was removed from the outer one to optimise the overall actuation effort.

Material property	Test method	Value	Unit
0° Tensile modulus	ASTM D3039	28.34	[GPa]
0° Tensile strength		687	[MPa]
0° Poisson's ratio		0.16	[-]
0° Elongation at failure		3.05	[%]
90° Tensile modulus	ASTM D3039	27.1	[GPa]
90° Tensile strength		580	[MPa]
90° Poisson's ratio		0.11	[-]
90° Elongation at failure		-	[%]
0° Compressive modulus	ASTM D6641	29.0	[GPa]
90° Compressive modulus		28.1	[GPa]
0° Compressive strength	SACMA SRM 1R-94	540	[MPa]
90° Compressive strength		505	[MPa]
In Plane Shear modulus	ASTM D3518	3.98	[GPa]
In Plane Shear strength at failure		97.5	[MPa]
In Plane Shear strength at 5%		-	[MPa]
Cured Ply Thickness		0.271	[mm]
Laminate cured density		1.78	[g/cm ³]
Fibre density		2.46	[g/cm ³]

Table 4.1: Mechanical properties of the cured EE302S ER450 38% S-glass fibre fabric material [9].

4.1.2. Electromagnetic linear motors

The second fundamental element of the morphing aileron prototype is the core system responsible for generating the force required to actuate the structure: the electromagnetic linear motor (LinMot® PS10-70x400U-BL-QJ 0150-1294).

The final demonstrator incorporates two such motors, one for each aileron segment. It is

important to note that these components do not represent the final flight-ready solution; they were selected exclusively for laboratory and wind tunnel testing. Indeed, within the broader scope of the HERWINGT project, Collins Aerospace is specifically tasked with selecting a dedicated Electro-Mechanical Actuator (EMA) for the operational morphing aileron.

Unlike traditional geared motors, which rely on mechanical transmissions to move a component, this is a linear induction electric motor. The term "electromagnetic" indicates that the linear translation of the shaft is generated directly by the interaction between the internal stator coils and the shaft (slider) itself. The defining characteristic of this operating principle is that the actual force exerted by the motor is strictly proportional to the absorbed electrical current.

Physically, the motor consists of a main body (the stator containing the coils) and a metallic through-shaft (the slider) that moves linearly. A notable technical detail is that the shaft features welded ends, which must not enter too deeply into the electromagnetic field generated by the coils at the end of the stroke to avoid interference.

Due to space constraints, the two motors (one for each 1-metre aileron segment) are installed parallel to the rear spar, completely housed within the wing-box. The motor base is secured to the wing structure using a mounting flange and threaded joints, which are anchored to a metal bracket to provide a sufficiently rigid constraint against dynamic vibrations. The selected flange allows for the installation of two cooling fans per motor, facilitating heat exchange with the surrounding air. Figure 4.4 shows the stator, the slider, and the selected metallic mounting flange.



Figure 4.4: (a) Stator and slider; (b) metallic mounting flange [27].

This motor is capable of generating a maximum peak force of 2720 N when powered at 3x400 VAC, as implemented in the current laboratory setup. A technological peculiarity of these actuators is that holding a specific static position generates more localized heat compared to executing continuous dynamic movements. Consequently, especially for prolonged static tests, the motors are equipped with cooling fans to actively dissipate this

thermal load. Figure 4.5 displays the four cooling fans, complete with their protective safety grids, prior to their installation on the motor flanges, intended to be mounted in pairs on each actuator.



Figure 4.5: The four cooling fans, equipped with protective safety grids, shown prior to installation.

These specific motors were chosen because, unlike geared alternatives, they can execute high-frequency dynamic movements, meeting the 10 Hz operational requirement of the project, while their maximum linear stroke of 150 mm still guarantees the travel necessary to properly deform the ailerons up to the maximum design deflections.

Furthermore, a major advantage of these motors is their integrated measurement instrumentation, which allows the control system to monitor position, force, and temperature in real-time. The specific built-in tools include:

- **Incremental linear encoder:** the integrated position sensor operates without any physical contact. Specifically, it continuously measures the exact position (stroke) of the motor shaft relative to a zero reference, known as the "HOME" condition. This signal is sent directly to the drive (the electronic device controlling the motor) via dedicated feedback cables. The main benefit of this integrated tool is that it completely eliminates the need to install external displacement sensors or encoders to measure structural deformation. This measurement is crucial for the PID (Proportional, Integral, Derivative) control loop, whose parameters were precisely tuned during laboratory procedures.
- **Indirect force measurement:** operating via current (without a physical load cell on the shaft, but as an intrinsic measurement capability derived from electromag-

netism and drive electronics), the motor-drive system effectively functions as a dynamometer. The drive computes the applied force by measuring the instantaneous electrical current drawn by the motor. Given the known linear and proportional relationship between current and force (approximately 80 N/A, as shown in the datasheet [27]), the system always knows the exact force being applied. This allows engineers to record the actuation loads required to deflect the morphing aileron during both slow static and rapid dynamic manoeuvres without relying on additional force sensors.

- **Winding temperature sensors:** the performance of electromagnetic induction motors is highly dependent on their thermal profile. Therefore, the motor integrates sensors to measure the instantaneous temperature of the three internal electrical windings (phases U, V, and W). This thermal data is transmitted to the control panel, allowing operators to read it in real-time on the Human-Machine Interface HMI screen to prevent overheating.

Figure 4.6 provides a view from inside the wing-box, displaying the motor body mounted on the spar via the flange. The image shows the linear guides along which the slider travels, and partially reveals the connection between the motor shaft extremities and the two connecting rods of the Scott-Russell mechanism.



Figure 4.6: Installation details of the linear actuator, viewed from inside the wing-box, highlighting the linear sliding guides on the motor side.

For further technical details and the complete specifications of the linear actuator, the reader is referred to Table 4.2.

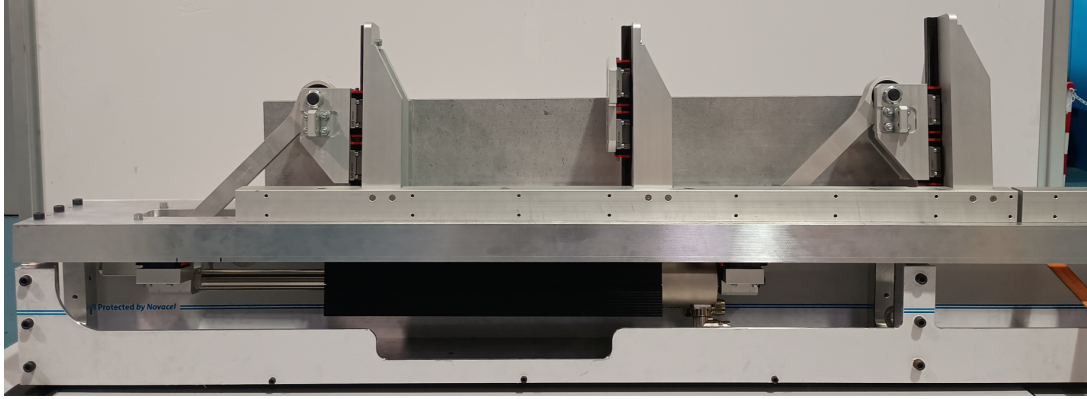
Category	Parameter	Value	Unit
Stroke	Max. Stroke	150	[mm]
Force	Max. Force @ 1x230VAC	2150	[N]
	Max. Force @ 3x400VAC	2720	[N]
	Max. Cont. Force (Passive / Fan / Fluid)	320 / 500 / 890	[N]
	Max. Border Force relative	100	[%]
	Force Constant 1	80	[N/A _{pk}]
	Force Constant 2	113	[N/A _{rms}]
Velocity	Max. Velocity @ 1x230VAC	2.2	[m/s]
	Max. Velocity @ 3x400VAC	3.9	[m/s]
Position Detection	Repeatability	±0.05	[mm]
	Linearity	±0.45	[%]
Electrical Data	Max. Current @ 1x230VAC	26.8 / 18.9	[A _{pk} / A _{rms}]
	Max. Current @ 3x400VAC	33.9 / 23.9	[A _{pk} / A _{rms}]
	Max. Cont. Current 1 (Passive / Fan / Fluid)	4 / 6.2 / 11	[A _{pk}]
	Max. Cont. Current 2 (Passive / Fan / Fluid)	2.9 / 4.4 / 7.9	[A _{rms}]
Thermal Data	Max. Winding Temperature (Sensor)	90	[°C]
	Thermal Resistance (Passive / Fan / Fluid)	0.52 / 0.22 / 0.068	[°K/W]
	Thermal Time Constant (Passive / Fan / Fluid)	2100 / 500 / 100	[s]
Mechanical Data	Slider Length	690	[mm]
	Slider Mass	3240	[g]

Table 4.2: Technical specifications of the LinMot® PS10-70x400U-BL-QJ 0150-1294 linear actuator [27].

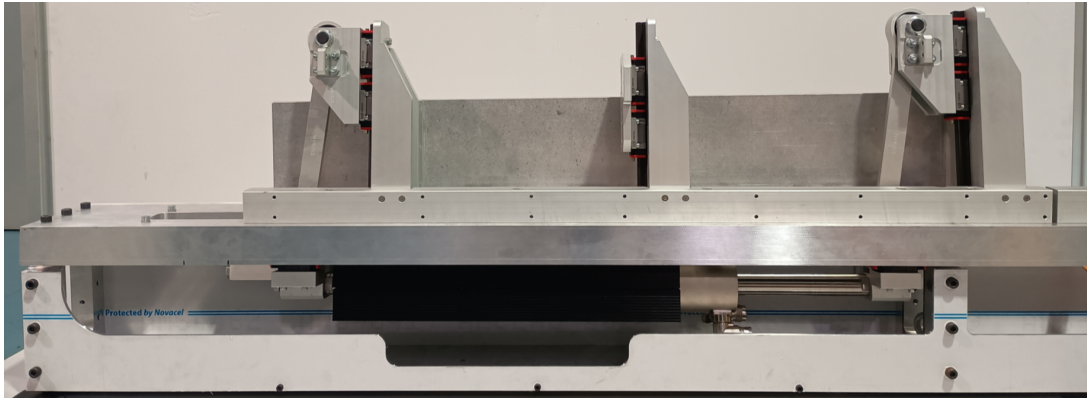
4.1.3. Actuation mechanism

As the motor translates parallel to the wingspan, the ends of its shaft connect to a linkage system featuring two Scott-Russell mechanisms per motor alongside linear guides. This kinematic chain effectively converts the spanwise motion of the actuator into the chordwise sliding of the lower fibreglass skin, generating the required morphing deformation.

Figure 4.7 illustrates the actuation system, capturing the motor, the connecting rods, and the attachment points where the three lower skin sliders are secured. The images specifically present the mechanism at its extreme operational limits for maximum positive and maximum negative deflection. This visual representation clearly shows how the slider linkages translate along the chordwise direction.



(a) Mechanism in position for the maximum positive deflection (downward).



(b) Mechanism in position for the maximum negative deflection (upward).

Figure 4.7: Two Scott-Russell mechanisms driving a single aileron, shown in the maximum positive (a) and maximum negative (b) deflection configurations. Both are viewed from the suction side with the aileron removed for visibility [9].

In addition, Figure 4.8 provides a detailed perspective of a ball bearing block. The specific component in the figure connects one extremity of the Scott-Russell rod to the chordwise sliding linear guide. These recirculating ball guides were selected to fulfil two simultaneous roles within the assembly:

- **Friction reduction:** they guarantee a smooth sliding movement with minimal mechanical resistance. Friction constitutes a considerable factor within the over-

all balance of forces. Therefore, reducing this resistance is necessary to decrease the required actuation effort and mitigate any hysteresis phenomena affecting the compliant structure.

- **Load management:** these linear bearings are also responsible for withstanding part of the aerodynamic loads exerted on the lower skin of the aileron. They safely transfer these external forces directly to the wing box structure, ensuring that no additional load is transmitted to the motor.

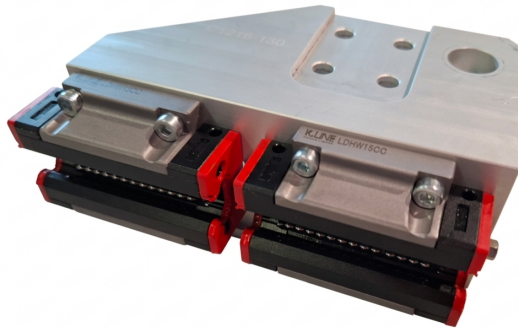


Figure 4.8: View of the recirculating ball bearing blocks used for the linear guides.

4.1.4. Pneumatic brakes

Mounted on the linear guides on the actuator side, the pneumatic brakes play a fundamental role in the experimental setup. Because the morphing skin is an elastic structure and is subject to aerodynamic loads, maintaining a deformed shape requires continuous mechanical effort. If the linear motors were to hold these static deflections for prolonged periods, they would draw excessive current and overheat. To prevent this, the pneumatic brakes engage a few seconds after the aileron reaches its target angle, locking the system in place and allowing the motors to safely reduce their power consumption during static testing.

Figure 4.9 illustrates the selected pneumatic component, specifically the MKS1501A model manufactured by Zimmer Group. These devices operate on a mechanically normally closed principle: in the complete absence of air pressure, an internal spring clamps the brake onto the guide rail, providing a baseline holding force of approximately 400 N. To release the brake and permit movement, pressurised air is supplied to port A, which compresses the internal spring.

To counteract the demanding load conditions, a holding power greater than the baseline spring force was required. The brake can operate in a standard configuration, where

closure relies entirely on the spring and port B is simply fitted with an exhaust filter. However, this experimental setup explicitly exploits the manufacturer's PLUS mode. When braking is commanded, rather than just releasing the pressure, pressurised air is actively routed into port B. This pneumatic pressure acts in tandem with the internal spring, boosting the overall holding force to 1300 N. Consequently, this braking capacity allowed to install only a single brake per actuation system, optimising both space and weight. The operating pressure for both ports ranges between 5.5 bar and 6.5 bar.

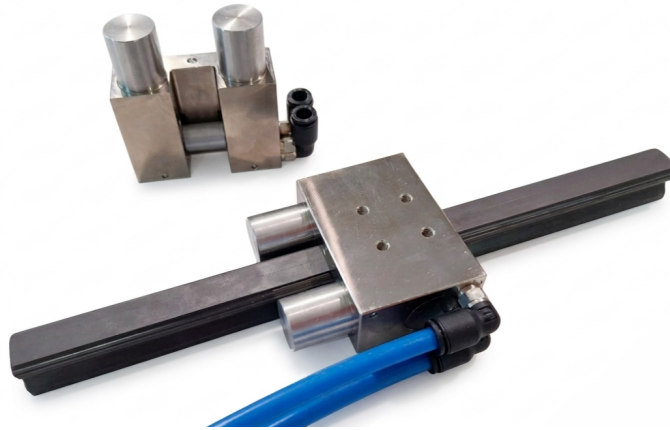


Figure 4.9: View of the pneumatic brakes installed on the linear guides.

Airflow distribution to the brakes is managed by single-solenoid, spring-return electrovalves (model VUVG-L14-M52-MT-G18-1P3, supplied by Festo S.r.l.), shown in Figure 4.10. In their resting state (unpowered), the valve directs air to port A, keeping the brake open to allow motor translation. Once the control unit commands a static hold, the solenoid is energised. The valve then switches, exhausting port A and simultaneously directing pressurised air into port B. This engages the PLUS mode, locking the guide securely. As highlighted in the figure, the assembly includes the main valve body, the air inlet, the two delivery outlets routed to the brake, and the silencer filters installed on the exhaust ports.



Figure 4.10: Electro-valve used to control the pneumatic brakes

4.2. Power supply and servo-drive cabinet

The electrical power management and low-level control architecture are governed by a dedicated electrical cabinet, developed by BLU TECHNOLOGY S.r.l., which acts as the central node connecting the power supply network, the PC control interface, and the physical subsystems of the facility.

The cabinet receives power from the mains (three-phase 400 VAC, 32 A max) and distributes it to power the electromagnetic motors, the motor cooling system, and the pneumatic components (solenoid valves).

The following key components are housed inside the cabinet:

- **24 VDC Power supply unit (PSU):** a module dedicated to converting the mains voltage into a 24 VDC supply. This is necessary to power the logic boards of the servo-drives and sensors, but also auxiliary components such as the relays, solenoid valves, and the cooling fans for both the cabinet and the motors.
- **Servo-drives:** the core of the control system consists of two LinMot® E1400 series drives, one for each linear motor. These modules directly receive the three-phase 400 VAC supply for their power stage. Internally, a dedicated microcontroller processes the control signals from the user via the Human-Machine Interface (HMI) and the position data from the linear encoders. This closes the control loop (using PID logic) and delivers the optimal currents to the motor phases. Additionally, the drives manage the pneumatic brakes.

- **Safety electronics:** electromechanical relays designed to physically and instantaneously cut off the power supply to the drives if the emergency stop is triggered via the mushroom push-button.
- **Line filters:** passive components required to suppress high-frequency Electro-Magnetic Interference (EMI) generated by the drives, ensuring a clean power supply and preventing disturbances.

The electrical cabinet also acts as a communication interface between the PC software (HMI) and the hardware. Specifically, the connections to the cabinet, are divided into:

- **Input connections:**
 - The three-phase 400 VAC, 32 A power cable.
 - An Ethernet port, connected directly to the PC and the servo-drives, used for drive parametrisation and for sending standard position commands. This mode will be referred to as "MANUAL" in Section 4.3.
 - Two BNC connectors (one per axis) linked to the servo-drives, which receive a differential analogue signal ranging between -10 V and +10 V. This signal is generated by a Digital-to-Analogue Converter (DAC) connected to the PC, translating motion profiles (such as frequency sweeps or sine waves) developed in LabVIEW®. This real-time control mode will be referred to as "AUTO" in Section 4.3.
- **Output and feedback connections:**
 - Power cables to the two linear motors.
 - Feedback signal cables, which carry the data read by the integrated motor encoders back to the cabinet for closed-loop position control.
 - Low-voltage auxiliary lines for controlling two FESTO S.p.A. solenoid valves (used to manage the compressed air for the motor brakes) and for powering the motor cooling fans.

The overall cabinet is shown in Figure 4.11.

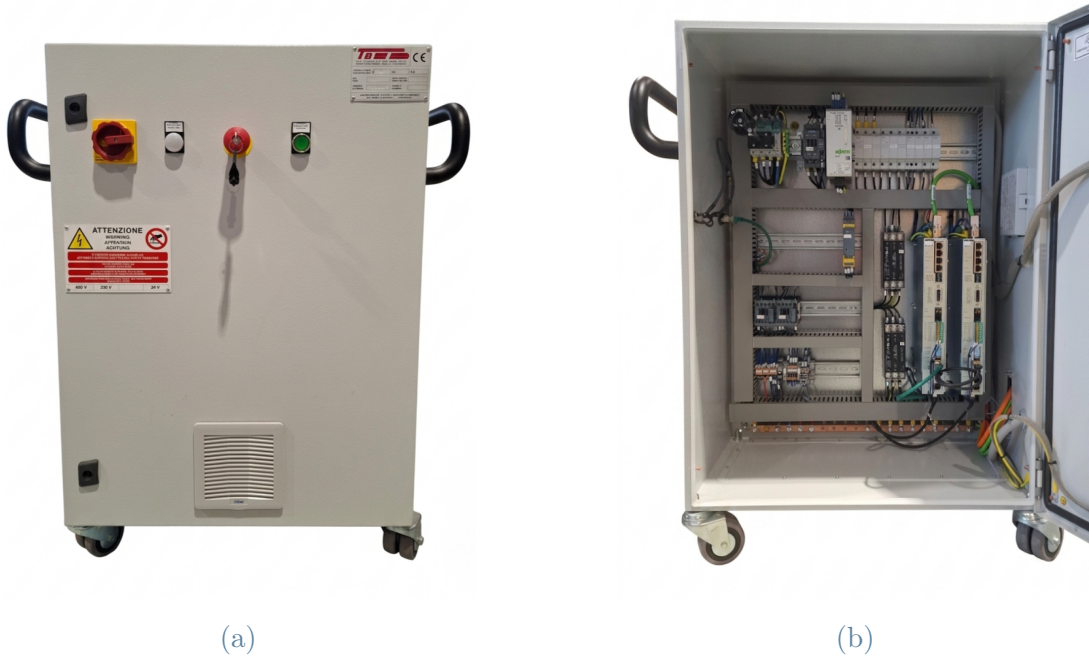


Figure 4.11: (a) external view of the cabinet, featuring the mushroom push-button to disable the servo-drives and the green button to enable them; (b) internal view of the cabinet, featuring the servo-drives and other electrical components.

4.3. Control interface and operational modes

This section outlines the software architecture designed to manage the experimental facility. The overall control strategy relies on two distinct but interconnected software environments, each dedicated to specific operational tasks: a HMI for system management and manual actuation, and a LabVIEW® interface for imposing complex dynamic profiles (automatic actuation).

4.3.1. Human-Machine Interface

To control the motors, it is necessary to communicate the desired reference signal to the servo-drives, enabling them to appropriately manage the power delivered to the linear actuators. For this purpose, BLU TECHNOLOGY S.r.l. has developed a HMI to facilitate interaction with the servo-drives, as shown in Figure 4.12.



Figure 4.12: HMI main dashboard for the servo-drives.

The software layout is divided into two main sections: one for the inner aileron (AX.1) and one for the outer aileron (AX.2).

Taking AX.1 as an example, the first panel displays the actual position and rotation of the aileron, measured in millimetres and degrees, respectively.

These values are referenced to the "Home" position, which corresponds to the 0° deflection of the aileron. It is assumed that this zero position remains consistent across different test sessions; therefore, a dedicated procedure has been developed to set it accurately and repeatedly upon every system start-up.

Upon start-up, the interface defaults to "OFF" mode. To initialise the system, the operator must switch to "MANUAL" mode, enable the drives, and perform the homing operation to define the 0° position. This procedure must be executed at every system reboot to provide the position encoder with a zero reference.

The "actual position" is measured directly by the motor encoder, with positive values indicating a downward deflection and negative values representing an upward deflection relative to the zero position. The term "actual" distinguishes this measurement from the "demand" (or target) position, which represents the ideal setpoint the motor is expected to reach.

Conversely, the "actual rotation" is derived using a polynomial equation implemented in the HMI and obtained via the Qualisys® motion capture system (as detailed in Section 6.2.2). This mathematical relationship, defined as the inverse law, converts the linear displacement in millimetres into the equivalent angular rotation of the aileron.

Moving further down the AX.1 panel, the interface displays the real-time temperatures of the individual motor phases (U, V, and W) in [°C], as recorded by the built-in thermal sensors.

Below this, a field allows the user to set the "Maximum allowable motor current" in [mA]. For safety reasons, this limit can be adjusted in real time during testing. As previously described in Section 4.1.2, the current is directly proportional to the exerted force ($1\text{ A} = 80\text{ N}$). A dedicated field also displays the demand current, which represents the current target required for the specified motion.

A dedicated section manages system alarms and features an "Alarm Reset" button. For instance, if the motor exceeds the maximum current threshold, the drives are automatically disabled, and an error message is displayed on the interface. To resume normal operations, the user must clear the fault by pressing this reset button.

Additionally, the interface includes a "Set Home Here" button to establish the reference position and an "Abort Movement" button to immediately halt any ongoing command.

Further down, four indicator LEDs provide the following system status updates:

- "STO": drive enabled (emergency stop disengaged and power stage active).
- "Is Homed": indicates that the home reference position has been successfully set.
- "Op. Enabled": lights up when the motor is actively powered (both during movement and in static conditions if the "Keep Axis Enabled" flag is checked).
- "Brake": displays the status of the electromechanical brakes (activated once the motor reaches its target position).

The system features three primary control modes: "MANUAL", "AUTO", and "OFF".

- "OFF" mode: the motor current and pneumatic brakes are deactivated, allowing the

ailerons to move freely. This is the default state upon system start-up.

- **"MANUAL"** mode: primarily used for static testing and the homing procedure. In this mode, the user can input a desired deflection angle and a specific movement speed. The motor then reaches this target position using a PID control loop. Once the position is achieved, the motor brakes engage to hold the aileron stationary. Although the input is provided in degrees, the system uses a secondary polynomial equation (termed the direct law) to calculate the equivalent displacement in millimetres. This mathematical relationship was established by comparing the actual airfoil shapes, reconstructed via the Qualisys® system, with the actual position in millimetres (as detailed in Section 6.2.2).
- **"AUTO"** mode: through the LabVIEW® software, this mode forces the motor to track a specific dynamic signal. The reference profile is declared in LabVIEW® and transmitted as an analogue signal via a DAC. This function is specifically designed to execute continuous motion profiles, such as sine waves or frequency sweeps.

When **"MANUAL"** mode is selected, an additional set of controls appears below the mode selection buttons. These features enable digital motor control via the Ethernet connection and are illustrated in Figure 4.13:

- **"JOG"** motion (**"forward"/"backward"**) with the desired speed (set in the **"JOG Speed"** field in [mm/s]).
- **"Execute Movement"**: triggers motion towards a target position (set in degrees) at a specified speed (in [mm/s]).
- **"Go Back Home"**: returns the motor to the established 0° reference position.
- **"Keep Axis Enabled"**: ensures the motor remains powered during stationary phases. In this configuration, when the system arrives in position, the brakes are engaged, and the position is actively maintained by both the motor force and the pneumatic braking system.

"Arrived in Pos.": a status flag indicating that the motor has reached its target position and the pneumatic brakes have successfully engaged."

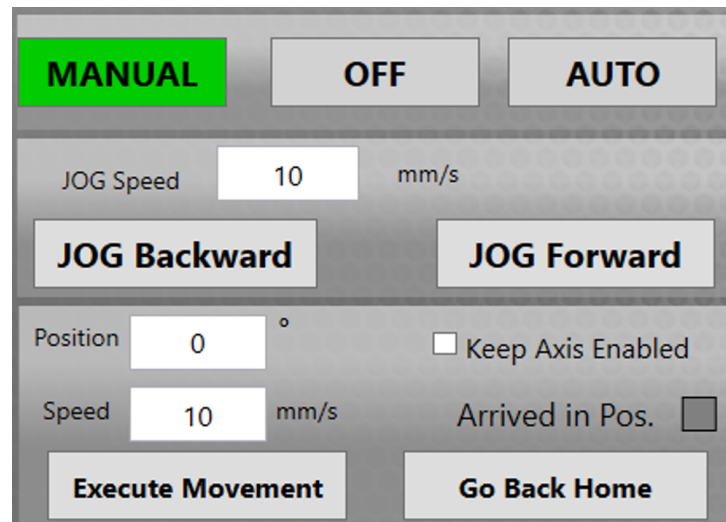


Figure 4.13: Manual mode functionalities.

Conversely, selecting "AUTO" mode for analogue control hides these manual features. The operator must then switch to the LabVIEW® environment (Section 4.3.2) to communicate with the signal generator, which subsequently outputs the desired analogue profile to the relevant servo-drive, and then to the motor.

In the control panel associated with the second axis ("AX.2"), two additional buttons are available for signal-imposed control. "AUTO STD (AI2)" forces the motion to follow the analogue input from the BNC connector of Drive 2, whereas "AUTO REF (AI1)" links the motion to the input from the BNC connector of Drive 1. This configuration provides the flexibility to drive both motors synchronously with the same reference signal or independently, as required.

At the bottom of the interface, the "Analog In Linmot (V)" window displays the instantaneous voltage of the applied analogue signal.

Remaining fields, such as "Analog in (Bit) BNC", "Setpoint (°)", and "Setpoint (mm)", are legacy features from a previous HMI version and are no longer relevant to current operations.

A global "ALL AXIS OFF" button is also provided lower down in the interface, allowing the operator to safely disable both motors simultaneously.

Finally, a data logging module, which can be utilised in both "AUTO" and "MANUAL" modes, allows the user to export a text file containing a defined number of samples for each motor. All signals are acquired at a frequency of 250 Hz. The system records the following variables:

- t in [ms]: the time vector.
- x_d [mm]: demand position.
- x_a [mm]: actual position.
- δ_d [°]: demand rotation, calculated by the demand position through the inverse polynomial law.
- δ_a [°]: actual rotation, calculated by the actual position through the inverse polynomial law.
- v_d [mm/s]: demand velocity.
- v_a [mm/s]: actual velocity.
- F_m [N]: motor force.

To streamline the post-processing of these exported text files, a custom MATLAB[®] routine named "exid" was developed. This function is specifically designed to parse the raw outputs, clean the data by addressing any potential acquisition jitter, and resample the signals to the theoretical 250 Hz acquisition frequency. Furthermore, it compiles the extracted variables into a structured format. A comprehensive description of this data pre-processing function, alongside its source code, is provided in Appendix A.

The "Communications" panel indicates the connection status with the "PLC" (a legacy feature no longer in use), "AX.1", and "AX.2". A red indicator warns of a communication failure. Lastly, the "EXIT" button terminates the connection between the HMI and the control cabinet.

4.3.2. Analogue signal generation for "AUTO" mode

To command the motors in "AUTO" mode, a dedicated LabVIEW[®] virtual instrument has been developed. The fundamental logic of this software relies on generating digital signal arrays and transmitting them to a hardware signal generator. This device acts as a DAC, translating the digital packets into two independent, continuous analogue voltage signals. These signals are physically routed to the servo-drives via two distinct BNC connectors, one for each motor.

The user interface, divided into specific functional blocks, allows the operator to configure both the hardware communication and the kinematic profiles. The main dashboard is shown in Figure 4.14.

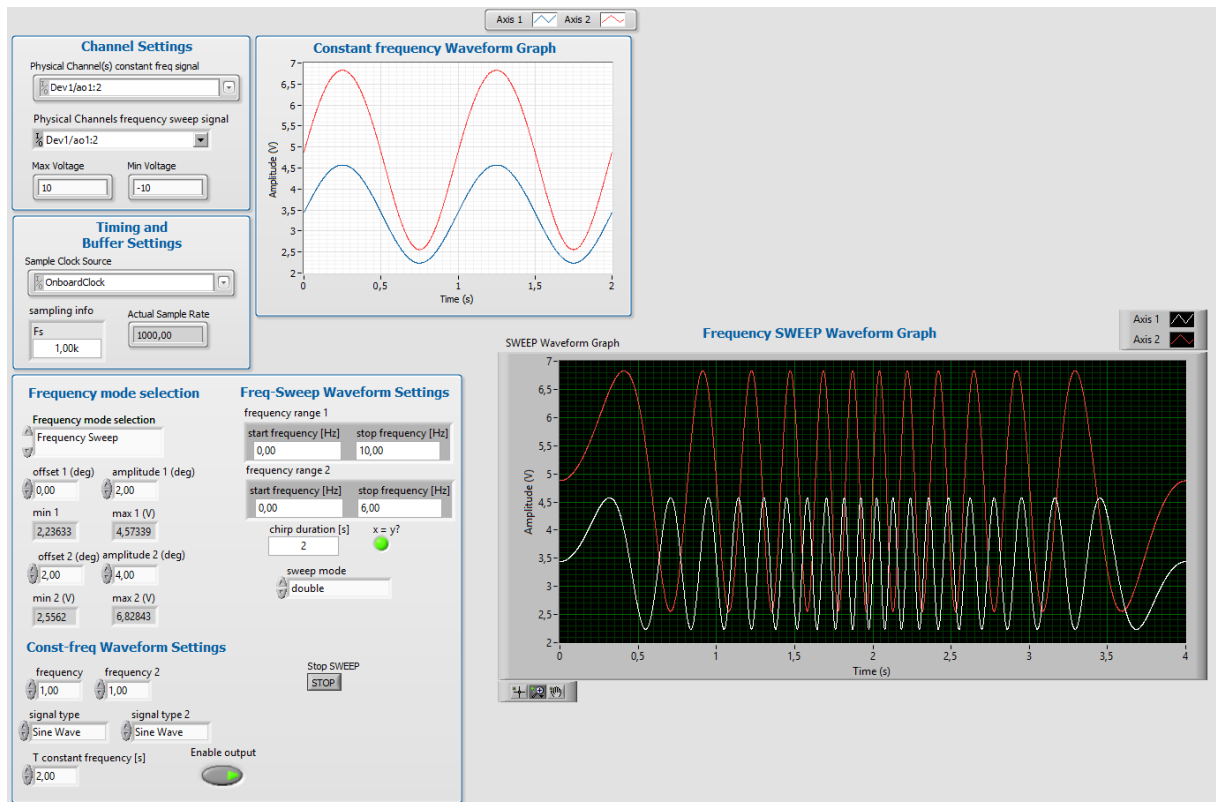


Figure 4.14: LabVIEW® front panel developed for the "AUTO" mode control.

In the "Channel Settings" panel, the specific hardware outputs are defined. Both the "Physical Channel(s) constant freq signal" and the "Physical Channels frequency sweep signal" are assigned to the Dev1/ao1:2 port, properly addressing the two required analogue channels. The voltage limits are constrained by the "Max Voltage" and "Min Voltage" fields, set to 10 and -10 respectively, to match the operational ± 10 V input range of the servo-drives.

The "Timing and Buffer Settings" section regulates the signal generation rate, using the hardware's "OnboardClock" as the synchronization source. The operator defines the required sampling frequency in the "sampling info Fs" field (set to 1000 Hz).

The core of the interface is dedicated to the motion profiles. The desired control mode is chosen via the "Frequency mode selection" drop-down menu, which allows toggling between a constant frequency output and a frequency sweep operation. Regardless of the chosen mode, the operator must define the fundamental spatial parameters for each axis: "offset 1 (deg)", "amplitude 1 (deg)", "offset 2 (deg)", and "amplitude 2 (deg)" (where 1 stands for the inner aileron, and 2 for the outer one). Based on

these angular inputs and the direct polynomial laws, the software automatically calculates the corresponding analogue voltage that will be generated by the DAC. The indicator fields such as "min 1 (V)" and "max 1 (V)" report the maximum and minimum output voltages.

Depending on the selected mode, two different setting panels are utilised:

- **Constant frequency mode:** the operator uses the "Const-freq Waveform Settings" panel to specify the desired frequencies ("frequency" and "frequency 2") and select the waveform type ("signal type" and "signal type 2", e.g., sine wave). The overall duration of the test is defined in the "T constant frequency [s]" field.
- **Frequency sweep mode:** the "Freq-Sweep Waveform Settings" panel is utilised to define dynamic tests. The operator defines a "start frequency [Hz]" and a "stop frequency [Hz]" for each motor. The "chirp duration [s]" field dictates the time required to complete the frequency transition. The "sweep mode" is set to "double"; this configuration forces the frequency to increase to its maximum value and then smoothly return to zero. This specific logic prevents abrupt motor halts at high operational frequencies. A dedicated "Stop SWEEP" button is also provided to manually interrupt this specific cycle.

To safely execute the motion, the user must activate the "Enable output" toggle button, an additional interlock implemented to prevent accidental signal generation.

Finally, the interface provides visual feedback through two dedicated plots: the "Constant frequency Waveform Graph" and the "Frequency SWEEP Waveform Graph". These graphs plot the theoretical voltage output for both axes, allowing the operator to verify the commanded motion before it reaches the actuators.

It is worth noting a specific operational characteristic managed by the LabVIEW® environment. As detailed in Section 3.3, the actuation system relies on a modified Scott-Russell mechanism, which is characterised by a trigonometric non-linearity. Consequently, while the operator defines purely symmetrical angular targets in the interface (for example, a continuous oscillation between $+5^\circ$ and -5°), the physical linear stroke required by the motor to reach these rotations is asymmetrical. Specifically, positive aileron deflections require shorter motor strokes, whereas negative deflections demand longer strokes.

To handle this mechanical behaviour, the control software compensates for the asymmetry through the embedded polynomial relationships. The system automatically converts the symmetric angular demand into an asymmetric millimetre displacement profile. This

calculated displacement is subsequently scaled linearly into the $\pm 10\text{ V}$ analogue control signal sent to the servo-drive. As a result, the software outputs an asymmetrical voltage waveform that precisely forces the motor to produce a symmetrical physical rotation of the morphing surface.

5 | Friction force evaluation

This chapter details the procedure adopted to evaluate and model the friction forces acting on the two aileron actuation systems. A proper characterisation of friction is, in fact, fundamental to guarantee reliable control performance and to interpret the dynamic response of the system correctly. The friction model plays a crucial role in the overall dynamics, as its coefficients must be included among the parameters of the motor controller to ensure accurate angle tracking during the experimental analyses.

To achieve this, the following sections describe the methodology used to derive a mathematical model for the friction forces of both mechanisms. Furthermore, an overview of the motor control logic is provided. Understanding the underlying control architecture is essential to clarify the rationale behind the selection of the adopted friction model, as well as to identify exactly where the derived coefficients are implemented within the motor setup parameters.

5.1. Motor control architecture

The position control of the LinMot[®] motors used in this system relies on a closed-loop architecture driven by a standard Proportional-Integral-Derivative (PID) controller.

The Proportional (P) action performs a linear calculation based on the tracking error, defined as the instantaneous deviation between the target position and the actual motor position. The corrective force applied by the motor is computed by multiplying this error by a fixed parameter, known as the P gain. Consequently, the corrective action is directly proportional to the distance from the target: the closer the motor is to the desired position, the smaller the applied corrective force becomes.

The Integral (I) control utilises the same position error, but it integrates this value over time. At each processing cycle, the controller adds the current error to the sum accumulated in previous steps. This total accumulated error is then multiplied by the I gain parameter to compute the corrective force applied by the motor. The integral action continues to build up this force until the position deviation is reduced exactly to zero,

making its primary objective the elimination of any steady-state tracking error. However, since it relies on the accumulation of past errors, the dynamic response of this control term is inherently slow.

The Derivative (D) control evaluates the difference between the demand velocity and the actual velocity of the system. This velocity error is then multiplied by the D gain parameter to compute the respective corrective force. Similar to the P control, this mechanism exhibits a highly dynamic intervention.

A previous study concerning these coefficients was conducted in [9], where the parameters were fixed at $P = 7.5 \text{ A/mm}$, $D = 10 \text{ A/(m/s)}$, and $I = 10 \text{ A/(mm s)}$. This configuration provided the best overall response across the 0–10 Hz frequency range.

Figure 5.1 presents an overview of the control structure for the position control system.

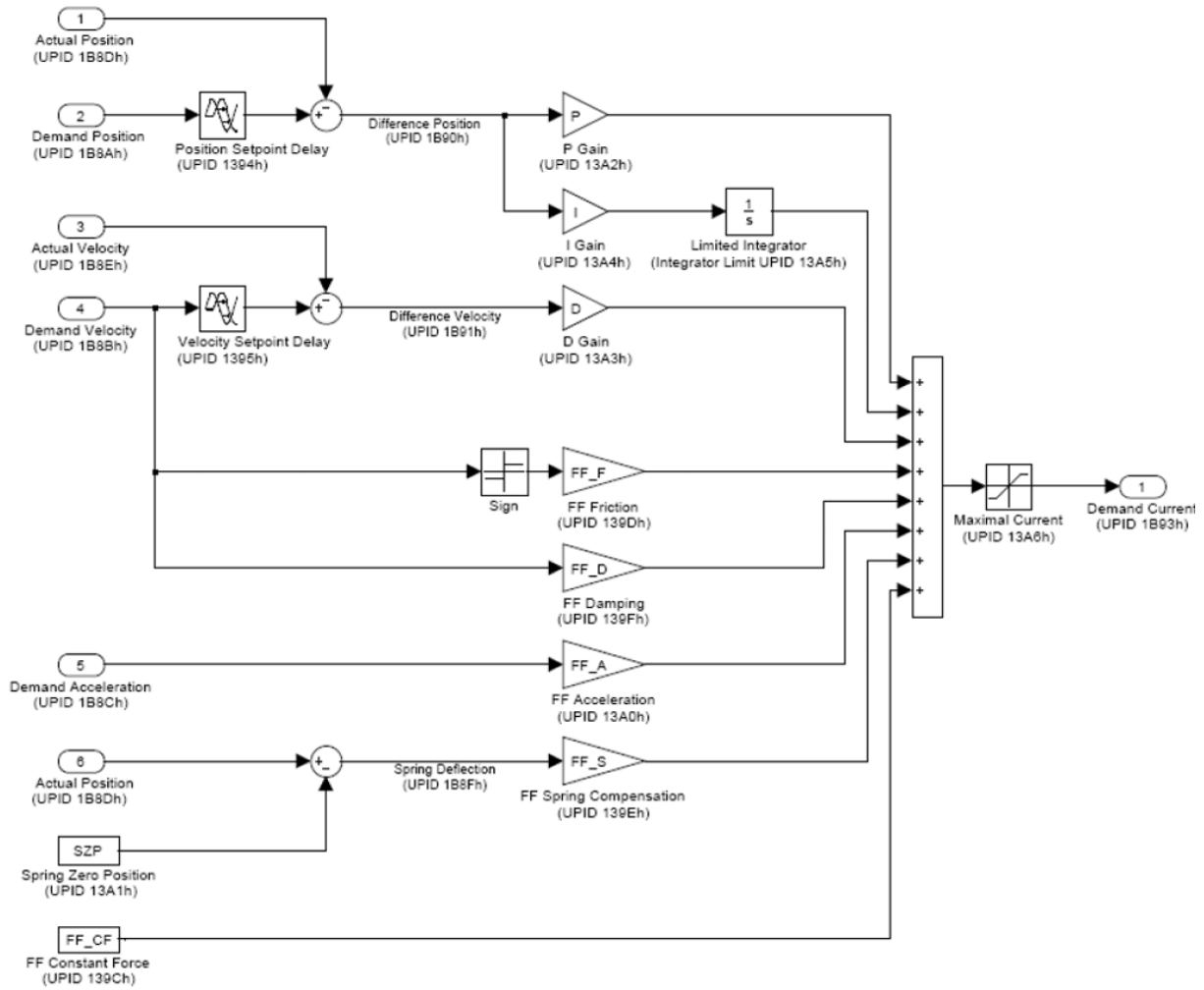


Figure 5.1: Control structure of the position control system [27].

To overcome the limitations of purely error-based control, the LinMot® drive architecture integrates an open-loop compensation logic alongside the PID, known as Feed-Forward (FF). This technique pre-calculates the effort required to overcome known, predictable physical forces. It achieves this by adding a compensatory current directly to the command signal sent to the motor.

Consequently, these predefined disturbances do not need to be managed by the PID controller loop. The main advantage of this pre-filtering approach is its rapid response. Because the pre-filters act independently from the PID error correction, they compensate for physical loads directly, simplifying the tuning process of the entire control system.

LinMot® servo controllers employ the following pre-filter parameters:

- **"FF Friction"**: a pre-filter designed to compensate for the known, constant friction within the mechanical system (Coulomb friction). This force is independent of the velocity magnitude but depends on its direction (sign).
- **"FF Damping"**: a pre-filter used to compensate for damping effects, such as viscous friction, which scales proportionally with the sliding velocity.
- **"FF Acceleration"**: computes the extra force required to move the load during acceleration phases. For this specific application, this parameter is intentionally set to zero. As demonstrated by [9], enabling this pre-filter in a highly dynamic setup introduces severe acoustic noise and unstable behaviour. Due to the high accelerations required by the motion profile, a non-zero value forces the drive to continuously output excessive current peaks, even during steady-state conditions where the motor should hold its static position. This dynamic overshoot causes rapid thermal build-up and risks premature mechanical wear, without providing any tangible improvement to the actual tracking performance.
- **"FF Spring Compensation"**: compensates for the presence of mechanical springs. This parameter is ignored in the current setup, as the physical architecture does not include any elastic elements of this type.
- **"FF Constant Force"**: a pre-filter applied to counteract fixed load masses, typically used in vertical setups. This compensation is actively utilised in the present system because the motor operates in a vertical orientation, requiring a continuous baseline force to balance the weight of the slider and the connecting rods resting upon it.

This chapter focuses specifically on the experimental determination of the pre-filter values associated with Coulomb and viscous friction, namely the **"FF Friction"** and **"FF Damping"** parameters.

5.2. Friction model: harmonic approximation for oscillatory regimes

The theoretical reference model adopted within the control architecture is the classical Coulomb–viscous friction model, analytically expressed as:

$$F_f(v) = F_c \operatorname{sgn}(v) + F_v v \quad (5.1)$$

This formulation separates the dissipative contributions into two distinct terms:

- Coulomb friction: a constant-magnitude force dependent purely on the sign of the velocity. In the controller settings (Figure 5.1), this is implemented as the "FF Friction" component.
- Viscous friction: a contribution proportional to the actual velocity v . This is implemented as the "FF Damping" parameter.

While this model is theoretically straightforward for unidirectional continuous motion, the actuation system operates in an oscillatory regime. Under these conditions, the friction force is not purely harmonic. Specifically, in the mid-frequency range (1–10 Hz), hysteresis and frequency-dependent effects can emerge. To establish a practical and robust parameter identification procedure, an equivalent harmonic approximation is introduced.

Assuming a commanded sinusoidal position trajectory:

$$x(t) = A \sin(\omega t) \quad (5.2)$$

the corresponding theoretical velocity is:

$$v(t) = V \cos(\omega t) \quad (5.3)$$

where the velocity amplitude is $V = A \omega$, and $\omega = 2\pi f$ is the angular frequency associated with the excitation frequency f .

Under this harmonic excitation, the two fundamental dissipative components can be analysed in the frequency domain:

- The non-linear Coulomb component, $F_c \operatorname{sgn}(v(t))$, governed by $\operatorname{sgn}(\cos(\omega t))$, behaves as a square wave alternating between $+1$ and -1 . When expanded into a

Fourier series, its fundamental harmonic component is in phase with the velocity and has an amplitude equal to $\frac{4}{\pi}F_c$.

- The linear viscous component, $F_v v(t)$, generates a harmonic term at the excitation frequency with an amplitude equal to $F_v V$.

By superimposing these two contributions, the theoretical amplitude of the first harmonic of the friction force, $F_{f,fund}$, evaluated at the angular frequency ω , can be expressed as:

$$F_{f,fund}(\omega) = F_v V + \frac{4}{\pi} F_c \quad (5.4)$$

This linear relationship between the fundamental friction force amplitude $F_{f,fund}$ and the velocity amplitude V is the basis of the experimental identification procedure. By conducting tests at various velocity amplitudes V (achieved by varying either the oscillation amplitude A or the frequency f), the fundamental force amplitude $F_{f,fund}$ can be extracted from the measured data via Fast Fourier Transform (FFT) analysis.

A linear regression of the form $y = m x + q$ is then applied to the experimental dataset. The slope of the fitted line (m) directly provides the viscous coefficient F_v , while from the intercept (q) the Coulomb term F_c can be calculated as:

$$F_c = q \frac{\pi}{4} \quad (5.5)$$

Despite its practical effectiveness, this modelling and identification approach presents specific limitations that must be acknowledged:

- Assumption of linear viscous behaviour: the procedure relies on a linear regression framework. If the viscous coefficient F_v were to exhibit a strong dependence on the excitation frequency, the linearity would fail, potentially requiring piecewise identification across distinct frequency bands [10]. Therefore, the validity of this linear trend must be verified experimentally within the target 1–10 Hz range.
- Absence of static friction (stiction) modelling: the adopted compensation scheme does not explicitly account for the static friction threshold required to initiate motion. At very small oscillation amplitudes (e.g., below $\pm 1^\circ$, corresponding to approximately 8 mm of motor stroke), the applied motor force might not fully overcome this static threshold, leading to measurements that do not accurately represent the purely dynamic friction model. Consequently, the experimental campaign is restricted to amplitudes large enough to guarantee a fully developed dynamic regime.

- Frequency boundaries: the harmonic approximation is specifically tailored for the intermediate 1–10 Hz band. At very low frequencies (< 1 Hz), the classical Coulomb–viscous model remains directly applicable in the time domain without needing harmonic approximations. Conversely, at frequencies above 10 Hz, the proposed model loses accuracy as complex structural dynamics and frequency-dependent damping become the dominant dissipative phenomena.

5.3. Data acquisition: dynamic model of the actuation system

In the present study, the derivation of the friction model for the actuation system is based on a dynamic force balance. By analysing the equilibrium of all forces acting on the actuator, it is possible to isolate and characterise the friction contribution within the overall system dynamics. The linear actuator must generate a force that balances the sum of the following contributions:

$$F_m = F_f + F_{aer} + F_{el} + F_{in} + F_g \quad (5.6)$$

where:

- F_m is the force generated by the motor, which must counteract all the other contributions;
- F_f is the friction force;
- F_{aer} is the projection onto the actuator axis of the aerodynamic loads acting on the aileron;
- F_{el} represents the elastic restoring force of the aileron structure;
- F_{in} is the inertial force associated with the moving masses;
- F_g is the gravitational contribution of the system components.

The above equation represents the dynamic force balance projected along the actuator axis. The motor force F_m is generated by the linear actuator and transmitted to the mechanism, while the remaining terms represent equivalent resistive contributions opposing the actuator motion. Each term corresponds to the projection, along the actuator axis, of the physical forces acting on the mechanical system.

The static gravitational component is accounted for within the "FF Constant Force"

control parameter, as described in Section 5.1. Therefore, depending on the configuration of the experimental setup, this parameter is adjusted to consider both the magnitude and the direction of the applied gravitational load.

Furthermore, to simplify the analysis, the experimental tests for friction identification were conducted without the aileron installed and in the absence of aerodynamic loads. Under these conditions, the elastic and aerodynamic contributions are zero:

$$F_{el} = 0; \quad F_{aer} = 0 \quad (5.7)$$

It should be noted that this simplification introduces a certain approximation into the model. Under real operating conditions, the presence of the aileron and aerodynamic loads may generate forces perpendicular to the linear guides. These transverse loads can depend on the aileron rotation angle and may increase the normal reaction forces acting on the guides, potentially leading to higher friction levels. However, such effects are neglected at this stage of the study in order to isolate and characterise the intrinsic friction behaviour of the actuation mechanism.

5.3.1. Operating conditions

For practical positioning reasons, the experimental campaign was conducted with the wing in a horizontal configuration, as presented in Figure 5.2.

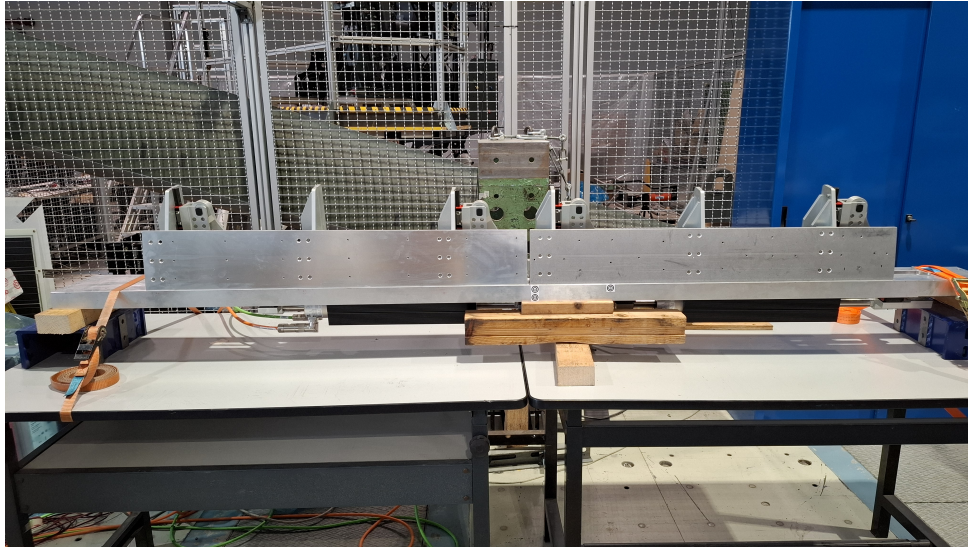


Figure 5.2: Horizontal configuration of the wing, including the actuation mechanisms of both ailerons, view from the suction-side.

In both the vertical and horizontal configurations, the gravitational forces produce components normal to the linear guide rails and to the motor axis. These normal components differ between the two orientations and could affect the friction forces. However, in both cases, these components are considered negligible, and no significant impact on the identified friction parameters is expected.

5.3.2. Data acquisition and preprocessing

The collected data are those obtained by the HMI (Section 4.3.1). For this analysis, the following quantities are of interest:

- $F_m(t)$: motor force, required to evaluate the total force balance;
- $v_a(t)$: actual velocity, used to evaluate the real velocity oscillation amplitude;
- $a_a(t)$: actual acceleration, derived as the numerical gradient of $v_a(t)$ with respect to time, and used to calculate the inertial forces of the moving masses.

Knowing these quantities makes it possible to evaluate the friction force by rearranging Equation (5.6), taking into account the correct sign of each force. The inertial forces act opposite to the motion. Meanwhile, the gravitational force of the mechanism rods is subtracted when the mechanism moves in the positive direction, since it assists the motor; conversely, when the mechanism moves in the negative direction, gravity opposes the motion and therefore requires additional motor force:

$$F_f(t) = F_m(t) - (m_{slider} + 2m_{rod})a_a(t) + 2m_{rod}g \quad (5.8)$$

Where:

- $m_{slider} = 5.5 \text{ kg}$ is the mass of the moving part of the motor;
- $m_{rod} = 0.5 \text{ kg}$ is the mass of each single rod;
- $a_a(t)$ is the actual acceleration of the combined mass (slider and rods);
- g is the gravitational acceleration.

Figure 5.3 presents a detailed view of the suction-side actuation system in the horizontal configuration, illustrating the system dynamics. Specifically, the positive x-axis points to the left; therefore, the weight of the rods assists the motor during motion in the positive direction.

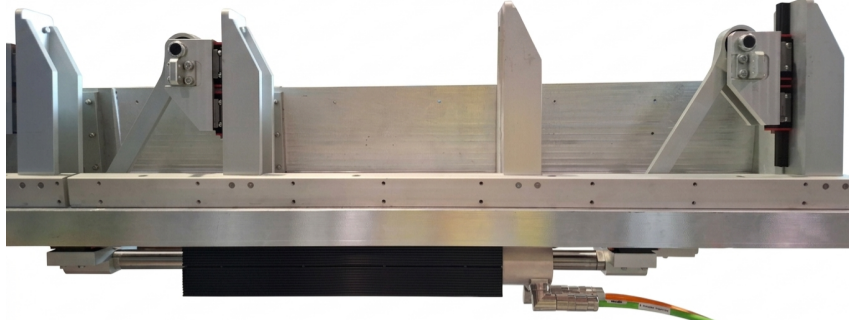


Figure 5.3: Pressure-side view of the actuation mechanism.

In reality, the gravitational force of the mechanism rods acting on the motor depends on the angle between the rods and the motor axis, which changes continuously with the motor position. For simplicity, however, this force is considered constant along the motor axis for both the horizontal and vertical configurations.

5.3.3. Signal preprocessing

The raw data acquired from the HMI must be appropriately pre-processed. First, to eliminate transient effects related to the start and end of the motion, the signals are cropped, isolating only the steady-state oscillatory phase. The time vector is then zeroed at the beginning of this valid observation window.

Since numerical differentiation and the Fast Fourier Transform (FFT) require a strictly constant sampling time, a spline interpolation is applied to the time series to enforce a uniform time step dt . Once the velocity signal $v_a(t)$ is regularised, the actual acceleration $a_a(t)$ is computed as its numerical gradient. Finally, the residual dynamic friction force $F_f(t)$ acting on the system over time is calculated using Equation (5.8).

5.3.4. Data processing: frequency analysis

In a real mechanical system, the actual execution of a commanded sinusoidal trajectory is affected by tracking errors. Consequently, rather than estimating the maximum velocity analytically as $A\omega$, the fundamental velocity amplitude is extracted directly by applying the FFT to the measured actual velocity signal $v_a(t)$.

Before applying the FFT, the mean value is subtracted from both the velocity and the friction force signals.

From the resulting amplitude spectra, the peak values corresponding to the fundamental

driving frequency of each test are extracted. These peaks are the true fundamental amplitudes: V_{fund} for the velocity (which represents the maximum oscillation amplitude and serves as the experimental counterpart to the theoretical V), and $F_{f, fund}$ for the friction force.

5.3.5. Parameters identification procedure

Following the extraction of the fundamental amplitudes for each experimental test (characterised by different combinations of commanded amplitude and frequency), a dataset of $(V_{fund}, F_{f, fund})$ pairs is obtained. According to the harmonic approximation of the Coulomb and viscous friction model, the fundamental amplitude of the friction force is linearly related to the fundamental amplitude of the velocity:

$$F_{f, fund} = F_v V_{fund} + \frac{4}{\pi} F_c \quad (5.9)$$

By plotting the experimental pairs and performing a linear regression of the form $y = mx + q$, the friction parameters can be directly identified. Specifically, the slope m corresponds to the viscous friction coefficient F_v , while the intercept q is related to the Coulomb friction F_c , as previously described in Section 5.2.

5.4. Results and validation

This section presents the outcomes of the experimental campaign. First, the friction parameters extracted through the frequency-domain analysis are detailed for both aileron actuation systems. Subsequently, the derived Coulomb–viscous model is validated in the time domain by comparing the theoretical motor force against the actual experimental measurements across different operating conditions.

5.4.1. Identified parameters

By applying the previously described linear regression methodology to the experimental datasets, the friction coefficients for both actuation systems were estimated. The resulting regression plots are reported in Figure 5.4 and Figure 5.5 for Axis 1 and Axis 2, respectively.

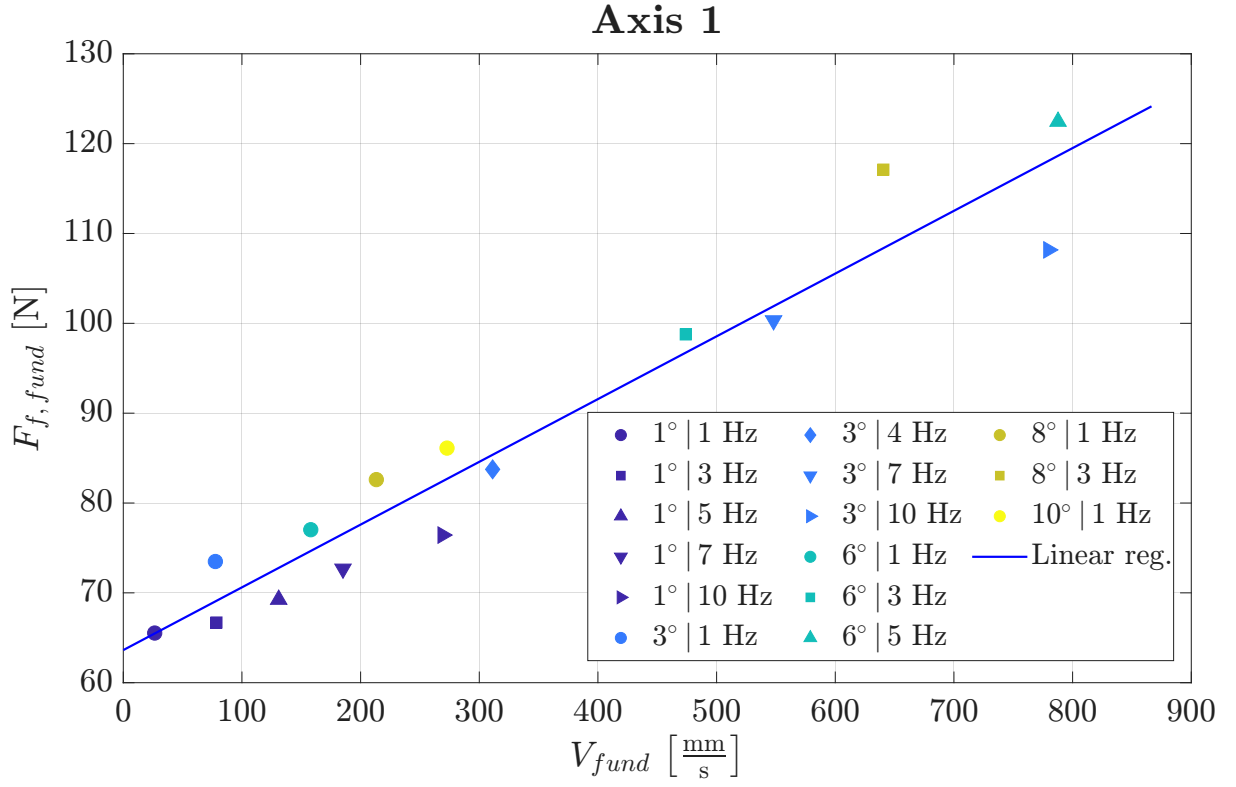


Figure 5.4: Inner aileron: linear regression of the fundamental friction force against fundamental velocity.

For Axis 1, the identified parameters are:

$$F_c = 49.9748 \text{ N}; \quad F_v = 69.8445 \frac{\text{N}}{\text{m/s}} \quad (5.10)$$

with a correlation coefficient $R^2 = 0.9322$.

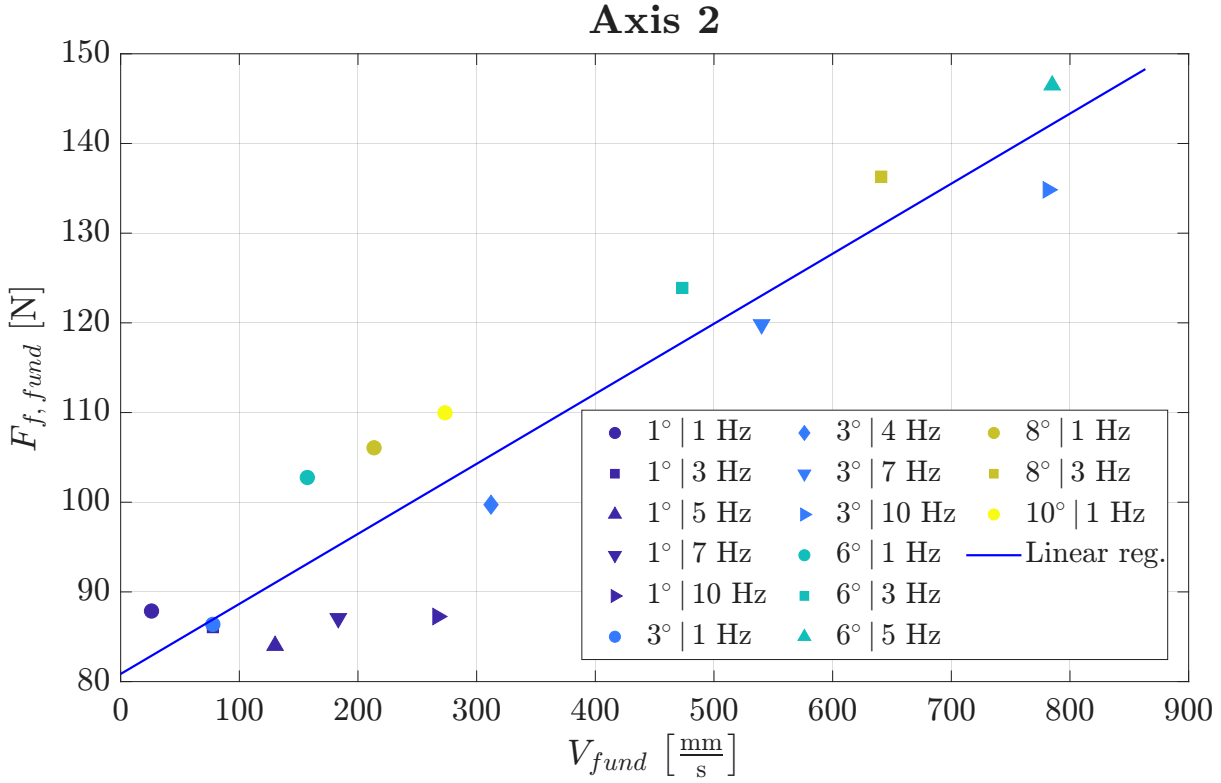


Figure 5.5: Outer aileron: linear regression of the fundamental friction force against fundamental velocity.

For Axis 2, the identified parameters are:

$$F_c = 63.5011 \text{ N}; \quad F_v = 78.0988 \frac{\text{N}}{\text{m/s}} \quad (5.11)$$

with a correlation coefficient $R^2 = 0.8802$.

5.4.2. Model vs experimental comparison

To verify the accuracy of the estimated parameters, the predicted system response is compared against the measured data. Based on Equation (5.8) the total estimated motor force $F_{m, est.}(t)$ required to drive the mechanism can be reconstructed as:

$$F_{m, est.}(t) = F_v v_a(t) + F_c \text{sgn}(v_a(t)) + (m_{slider} + 2 m_{rod}) a_a(t) - 2 m_{rod} g \quad (5.12)$$

This analytical model is then compared against the actual motor force $F_m(t)$ measured

during the experimental tests.

The validation results for various operating conditions are presented from Figure 5.6 to Figure 5.9. These plots illustrate the comparison between the actual measured motor force and the force estimated by the model, employing the identified F_c and F_v values.

Although the figures refer specifically to Axis 1, Axis 2 exhibits an analogous behaviour. The comparisons confirm that the model correctly reconstructs both the dynamics and the amplitude of the actual force, thereby validating the proposed friction parameter identification procedure.

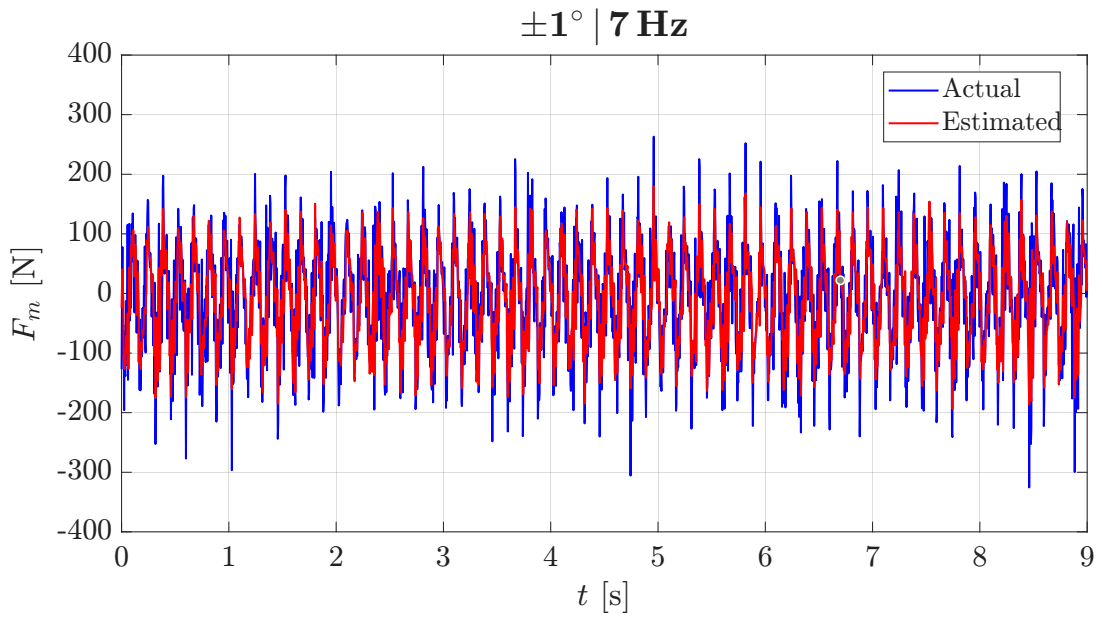


Figure 5.6: Validation of the friction model for Axis 1: estimated vs actual force during small-deflection, high-frequency oscillations.

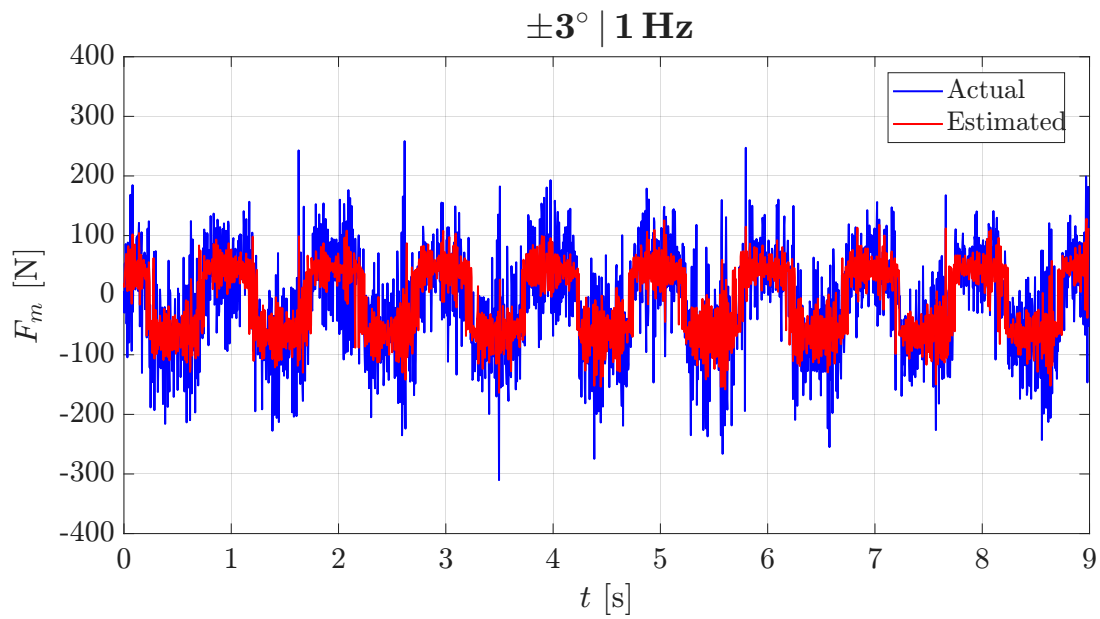


Figure 5.7: Validation of the friction model for Axis 1: estimated vs actual force during medium-deflection, low-frequency oscillations.

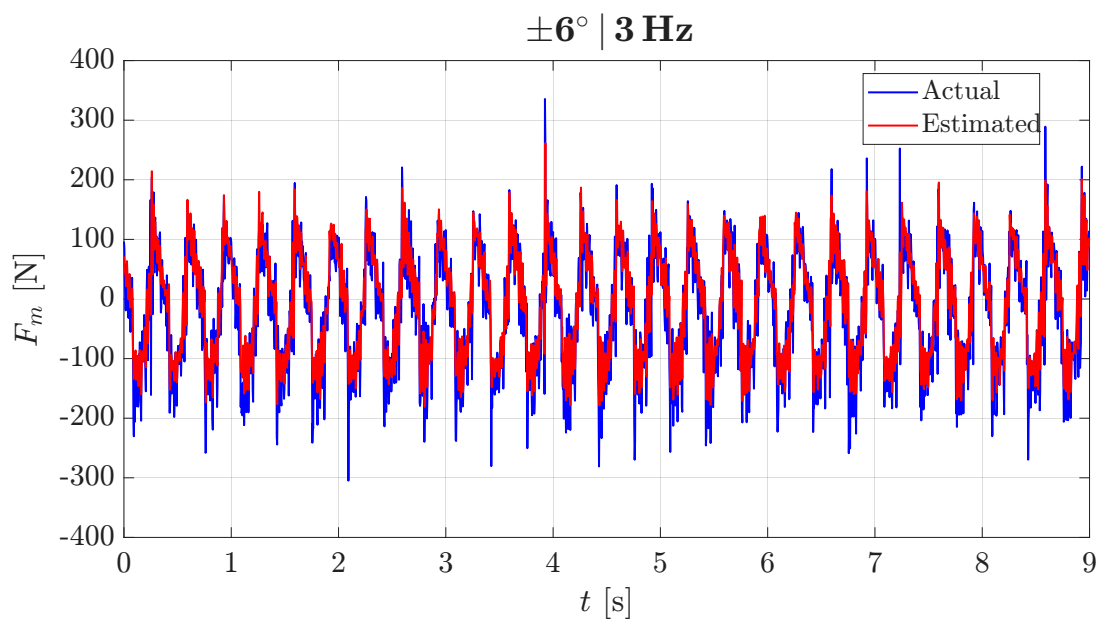


Figure 5.8: Validation of the friction model for Axis 1: estimated vs actual force during medium-deflection, intermediate-frequency oscillations.

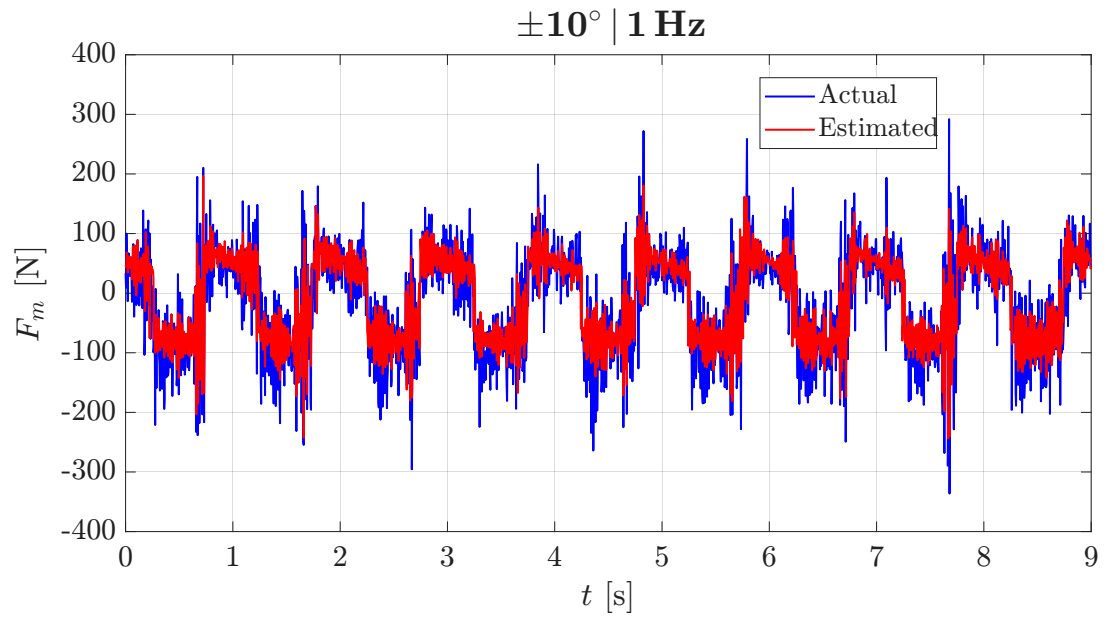


Figure 5.9: Validation of the friction model for Axis 1: estimated vs actual force during large-deflection, low-frequency oscillations.

6 | Static ground tests

The broader experimental campaign conducted on the prototype aims to validate its shape-morphing capabilities and characterise both its static and dynamic behaviour under wind-off conditions (i.e., without aerodynamic loads). While the dynamic response is the subject of Chapter 7, the present chapter focuses on the static and quasi-static ground testing phases.

The model was returned to the vertical configuration and its assembly was finalised, as compared to the setup shown in Figure 5.2. Furthermore, to isolate the structural response of the ailerons and minimise interference or global deformations from the dummy wing-box, all ground tests were conducted with the demonstrator constrained at both the root and the tip. Although these fully clamped boundary conditions do not perfectly replicate the operational environment of the wind tunnel, they do not negatively affect the static shape measurements. Additionally, these rigid constraints prove advantageous for the subsequent dynamic testing, as they reduce the global oscillation of the entire demonstrator, allowing for a clearer observation of the aileron's isolated kinematics.

The static and quasi-static tests involved holding the ailerons at fixed deflections or actuating them at very low speeds. The physical shapes achieved by the prototype are reconstructed and compared against two sets of reference profiles: the "target" shapes, derived from the *aero-structural shape optimisation*, and the "numerical" shapes, obtained from the *compliant structure optimisation* models. These reference profiles were previously detailed in Section 3.4.1 and illustrated in Figure 3.10.

A primary objective of these static tests is the formulation of the direct and inverse polynomial relationships governing the system's kinematics. The direct law converts the target rotation angle, provided by the user via the HMI, into the required linear motor stroke (in millimetres) relative to the home position. Conversely, the inverse law translates the actual motor displacement, read by the linear position encoder, back into degrees to provide real-time angular feedback on the HMI. These experimental relationships are subsequently compared with the theoretical kinematic curves obtained through FEM analysis.

Following the geometric and kinematic characterisation, the study evaluates the steady-

state force required by the motor to reach and hold the aileron at specific deflection angles.

Data acquisition for the shape reconstruction relies on a Qualisys® motion capture system. Using a network of cameras and reflective markers, this optical system tracks and digitises the physical model in 3D space. As will be detailed in Chapter 7, the same Qualisys® setup is also employed during the dynamic testing phase to measure the actual aileron rotation over time. This tracking helps identify any deviations between the motor slider displacement (x_a) and the true structural response, such as phase delays, attenuation or amplification caused by the structural dynamics of the aileron, or hysteresis effects.

Figure 6.1 illustrates the experimental test rig and the camera arrangement relevant to the measurement campaigns discussed in both this chapter and Chapter 7.



Figure 6.1: Experimental setup for the ground tests using the Qualisys® motion capture acquisition system. (a) and (b) illustrate different views of the arrangement.

6.1. Motion capture system

To track the deformation of the morphing ailerons, an optical motion capture system (Qualisys®) relying on passive reflective markers was employed. A total of 72 markers were distributed across the external surfaces of the devices. Specifically, they were arranged to map the chordwise deformed profile at three distinct spanwise stations per aileron.

A laser level was used during the setup phase to ensure precise alignment of the markers along the selected sections, as shown in Figure 6.2.



Figure 6.2: Alignment of the reflective markers using a laser level to accurately track the chordwise sections.

For each monitored section, 5 markers were attached to the lower skin (pressure side) and 6 to the upper skin (suction side). To accurately capture the trailing edge position, a split-marker configuration was adopted: half of a marker was placed on the upper surface and the other half on the lower surface.

The 3D coordinates acquired by 8 Miquis M3 cameras (presented in Figure 6.3) allowed for the extraction of the 2D sectional geometries at various deflection angles. These experimental profiles were then compared against the target aerodynamic shapes and the numerical results to assess the system's accuracy.



Figure 6.3: Qualisys® Miquis M3 cameras used for the experimental campaign.

Alongside the geometric reconstruction, the system was configured to track specific kinematic variables during both static and dynamic tests. To achieve this, three additional markers were introduced: one on each motor slider and one on the wing-box near the tip.

The primary parameters extracted from the Qualisys® tracking are:

- x_q : the actual linear displacement of the motor slider measured optically. This is physically equivalent to x_a (the stroke read by the motor encoder). Acquiring x_q is essential for post-processing, as it allows for the time-synchronisation of the optical data with the motor logs via cross-correlation, correcting any phase delays between the two independent acquisition systems.
- δ_q : the effective rotation angle of the aileron, derived from the spatial coordinates of the trailing edge extracted from the acquired geometric shapes. In static or quasi-static conditions, this value closely matches the theoretical angle δ_a computed via the inverse polynomial relationship from the actual motor stroke x_a . However, during dynamic tests, δ_q captures the true structural response, highlighting any dynamic attenuation or phase delay introduced by the aileron's compliance.
- u_q : the linear displacement of the lower aileron skin. This parameter is extracted from the first marker positioned on the pressure side, which is located near the spanwise line corresponding to the connection points of the actuating rods.
- $z_{tip,q}$: the out-of-plane displacement of the wing-box tip. This marker monitors any unwanted global bending or oscillation of the constrained structure induced by the moving ailerons.

To match the resolution of the HMI, the sampling frequency of the motion capture setup was set to 100 Hz for the static analyses and increased to 250 Hz for the dynamic tests.

In parallel with the optical measurements, all motor parameters were continuously logged via the HMI, as detailed in Section 4.3.1.

Finally, it is worth noting that while the Qualisys® setup is highly effective for these laboratory ground tests, it has been replaced by Digital Image Correlation (DIC) technology during the wind tunnel campaigns.

6.1.1. Calibration procedure and airfoil shape reconstruction

A rigorous calibration procedure was required to define the three-dimensional global coordinate system and establish the accurate measurement volume.

Prior to the actual calibration, the setup phase involved the manual adjustment of the focus and aperture on the eight cameras to ensure optimal marker detection. Furthermore, a software masking operation was performed to exclude any static reflections or background light sources (outliers) from the cameras' field of view.

Once the optical setup was completed, the spatial calibration was executed to determine the position and orientation of each camera relative to the global coordinate system. This process relied on the specific calibration tools illustrated in Figure 6.4. An L-shaped reference frame, equipped with four markers, was initially placed to define the absolute origin and the orientation of the measurement software axes.

It is important to clarify that the final body axes used for the geometric assessment (x_b , y_b , and z_b) were obtained later during the post-processing phase. A rigid rototranslation was applied to the raw tracking data to align the experimental coordinates with the reference frame of the numerical and target models, setting the x_b axis parallel to the chordwise direction, the y_b axis along the main spar (spanwise direction), with the z_b axis completing the right-handed coordinate system.

Subsequently, a calibration wand, featuring two markers separated by a precisely known reference distance, was continuously swept throughout the physical measurement space. Tracking the known distance between the wand's markers allows the software to compute the relative distances and the focal parameters of the cameras.

To ensure the algorithm correctly identified only the calibration tools, it was necessary to temporarily cover all the markers already positioned on the test article during this phase.

Following this procedure, the spatial positions of the camera optical centres were successfully determined, achieving an average residual error of approximately 1 mm.

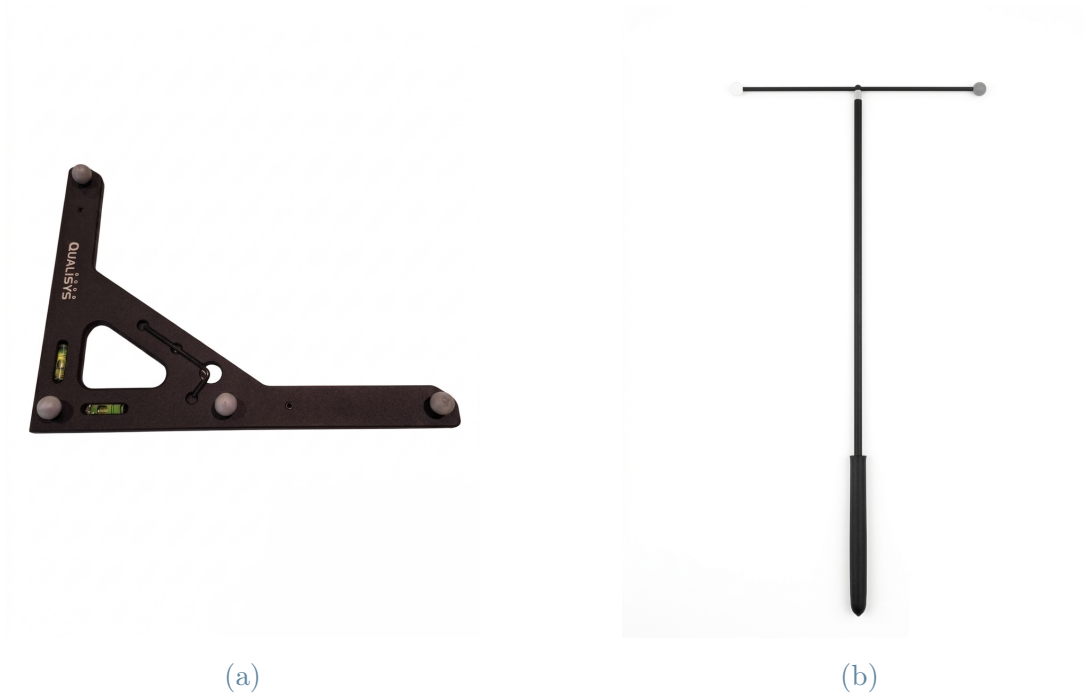


Figure 6.4: Hardware used for spatial calibration: (a) the L-shaped reference frame used to define the coordinate system; (b) the calibration wand used to establish the measurement volume.

A dedicated procedure was developed to reconstruct the six different aerodynamic profiles from the marker coordinates. Initially, two separate interpolating splines were generated for each spanwise section: one connecting the markers on the suction side and another connecting those on the pressure side. Importantly, both splines converge and terminate at the exact same point, sharing the trailing edge split-marker.

While these initial splines yielded two distinct curves for the upper and lower surfaces, they passed directly through the optical centroids of the markers, thus failing to represent the true external skin of the airfoil. To correct this offset, a normal translation was applied to each marker coordinate (excluding the trailing edge marker, which defines the tip). Specifically, the points were shifted inward, perpendicularly to the local tangent of the initial spline, by a distance equal to half the marker's radius.

Finally, a secondary spline interpolation was performed on these corrected spatial points.

This process successfully generated the real external boundaries of both the upper and lower surfaces, defining the precise airfoil shapes for both static and dynamic experimental validation.

6.2. Experimental results

To assess the morphing capabilities of the prototype, the initial test examined the static, undeformed shape to verify the manufacturing quality. Once it was confirmed that the baseline shape closely matched the theoretical model, it is necessary to recall how the equivalent deflection angle is defined before investigating the maximum positive and negative deflections.

Throughout these analyses, the equivalent deflection angle δ is defined as the angle between two intersecting rays, as illustrated in Figure 3.8a. Their common vertex lies at the centre of the inscribed circle tangent to both the upper and lower surfaces, located at the chordwise station where morphing deformation initiates (i.e., at 70% of the chord in the undeformed configuration). The two rays originate from this vertex and pass through the trailing edge point in the undeformed and deformed configurations, respectively.

A quasi-static deflection was performed from the neutral 0° position to the maximum and minimum deflections achievable by the actuation system. This continuous tracking enables the precise determination of the true aileron rotation at any given instant during post-processing, allowing for the identification of the shapes that most closely resemble the target deflections.

Beyond confirming operational limits and geometric accuracy, this quasi-static analysis correlates the required linear motor stroke to specific deflection angles. Extracting this kinematic dataset allows for the establishment of the direct and inverse polynomial laws. As previously discussed in Section 4.3.1, these polynomial functions are integrated into the HMI to provide precise closed-loop control and real-time angular feedback to the operator.

Building upon these kinematic relationships, the study subsequently investigates the mechanical effort required from the linear actuators to reach and sustain specific deflections. A cyclic testing approach was adopted to analyse the actuation forces. Both motors executed a continuous full cycle from the minimum deflection up to the maximum, and back down again. This cyclic profiling captures the hysteretic effects induced by internal friction, providing a dataset to validate the physical models detailed in Chapter 5.

Finally, a numerical-experimental comparison of these actuation forces was conducted. During the physical ground tests, the total force measured by the motor encompasses structural elastic restoring forces, internal friction, and gravitational loads. Aerodynamic loads are absent under these wind-off conditions, and inertial forces are negligible given the extremely low actuation speeds adopted for these quasi-static tests (approximately

2 mm/s). Conversely, the purely numerical model accounts solely for elastic stiffness and component weight, inherently excluding friction.

6.2.1. Experimental shape validation

Although the shape evaluation covered all six marker-equipped sections, data from the outermost section of the inner aileron (the third row of markers from the bottom) is presented here for clarity.

The initial analysis focused on evaluating the undeformed profile to verify the geometric accuracy of the manufactured baseline configuration. Figure 6.5 compares the experimental undeformed aileron shape against the theoretical baseline target. It is important to clarify that in this initial undeformed state, the idealised target shape and the structural numerical model are identical, serving as a single, common reference baseline. This contrasts with the deformed states, where the numerical structural predictions exhibit a minor offset compared to the target profiles.

This comparison confirms that the physical prototype accurately reproduces the intended ideal profile before any actuation occurs.

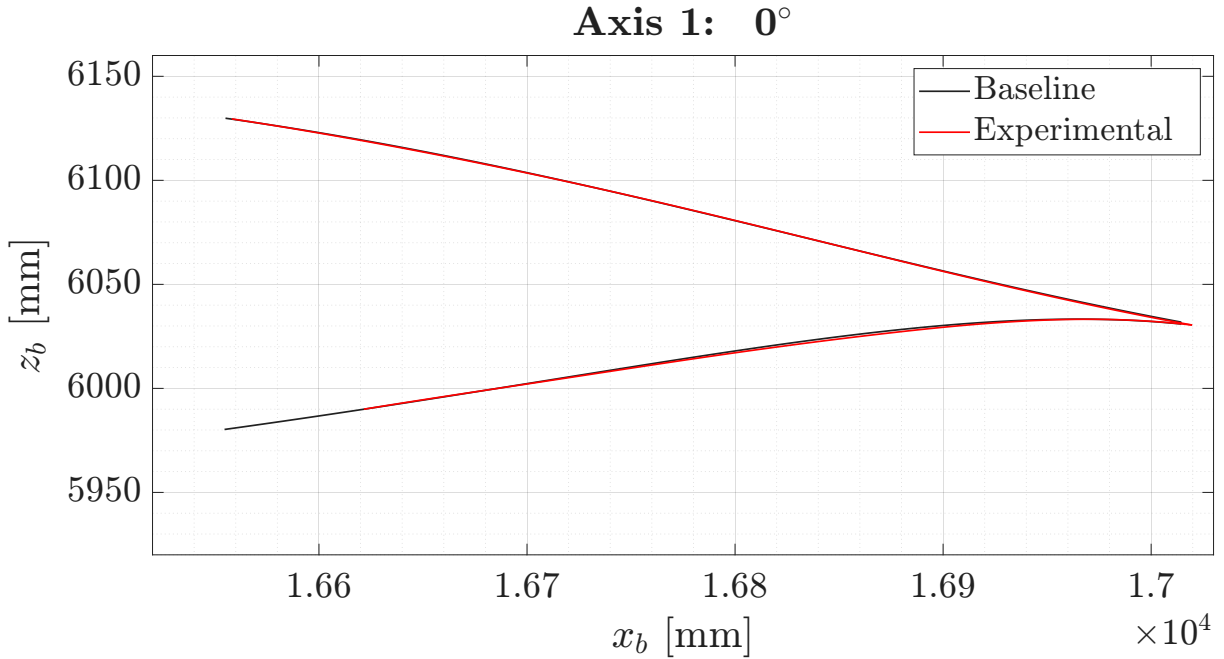
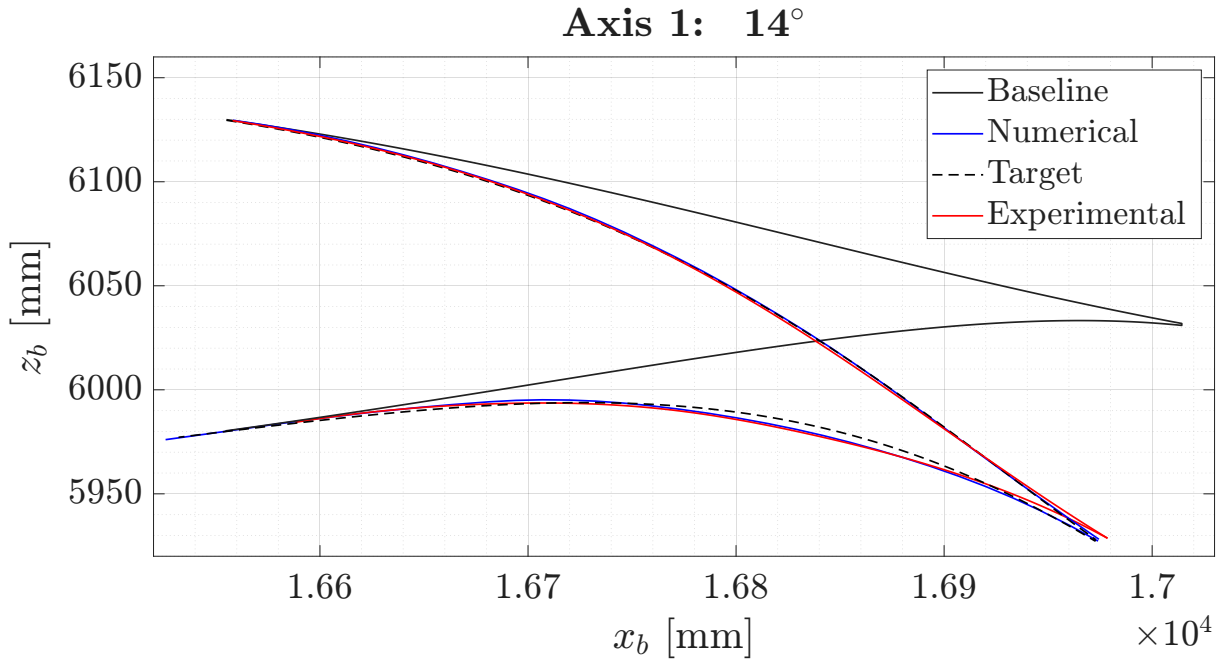


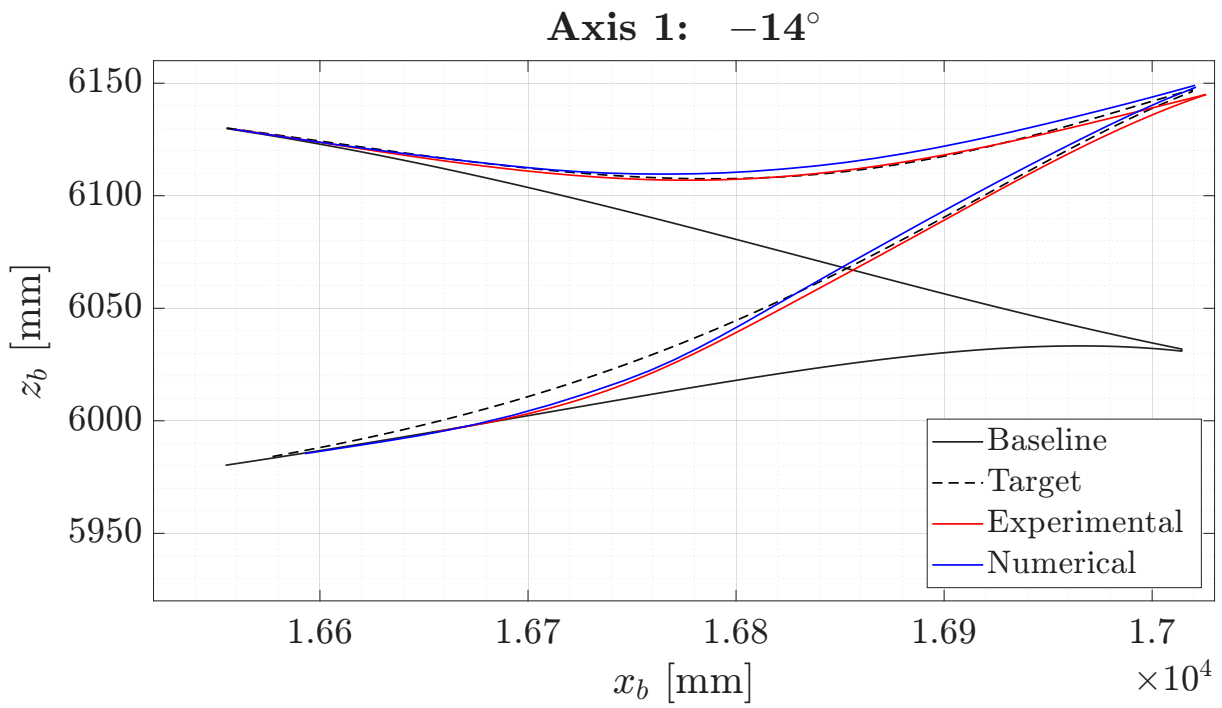
Figure 6.5: Comparison between the undeformed experimental aileron profile and the baseline (target and numerical models, which are equal in this undeformed condition).

Subsequently, the focus shifted to the extreme deflections. The mechanism was driven to its stroke limits to evaluate its final position relative to the targets. Figure 6.6 displays the configurations that most closely match these desired deflections. While the target (described in Section 3.4.1) was set to -15° and 14° , the physical aileron deformed slightly less than anticipated for a given stroke. Consequently, when the motor reached its maximum negative extension, the achieved angle was slightly less than -15° . Therefore, a moderately lower negative target was adopted, whilst the positive target remained unchanged.

Figure 6.6 compares the experimental shapes at maximum positive and negative deflections with both the target shapes and the structural simulations derived from the numerical model.



(a)



(b)

Figure 6.6: Experimental shape evaluation: (a) maximum positive deflection and (b) maximum negative deflection, compared alongside the target and numerical shapes.

The results demonstrate that the prototype successfully reproduces the desired shapes, showing good overall agreement with the target and the numerical prediction shapes. Furthermore, the experimental data confirms the compliant structure's ability to smoothly and continuously reach extreme deflections across the entire morphing region, preserving its structural integrity.

The most noticeable deviations occur near the trailing-edge region. Here, the structure is highly sensitive to manufacturing tolerances and behaves more rigidly than expected. Nevertheless, these minor discrepancies do not significantly impair the prototype's ability to achieve the target shapes. Figure 6.7 displays the experimental model at maximum and minimum deflections.

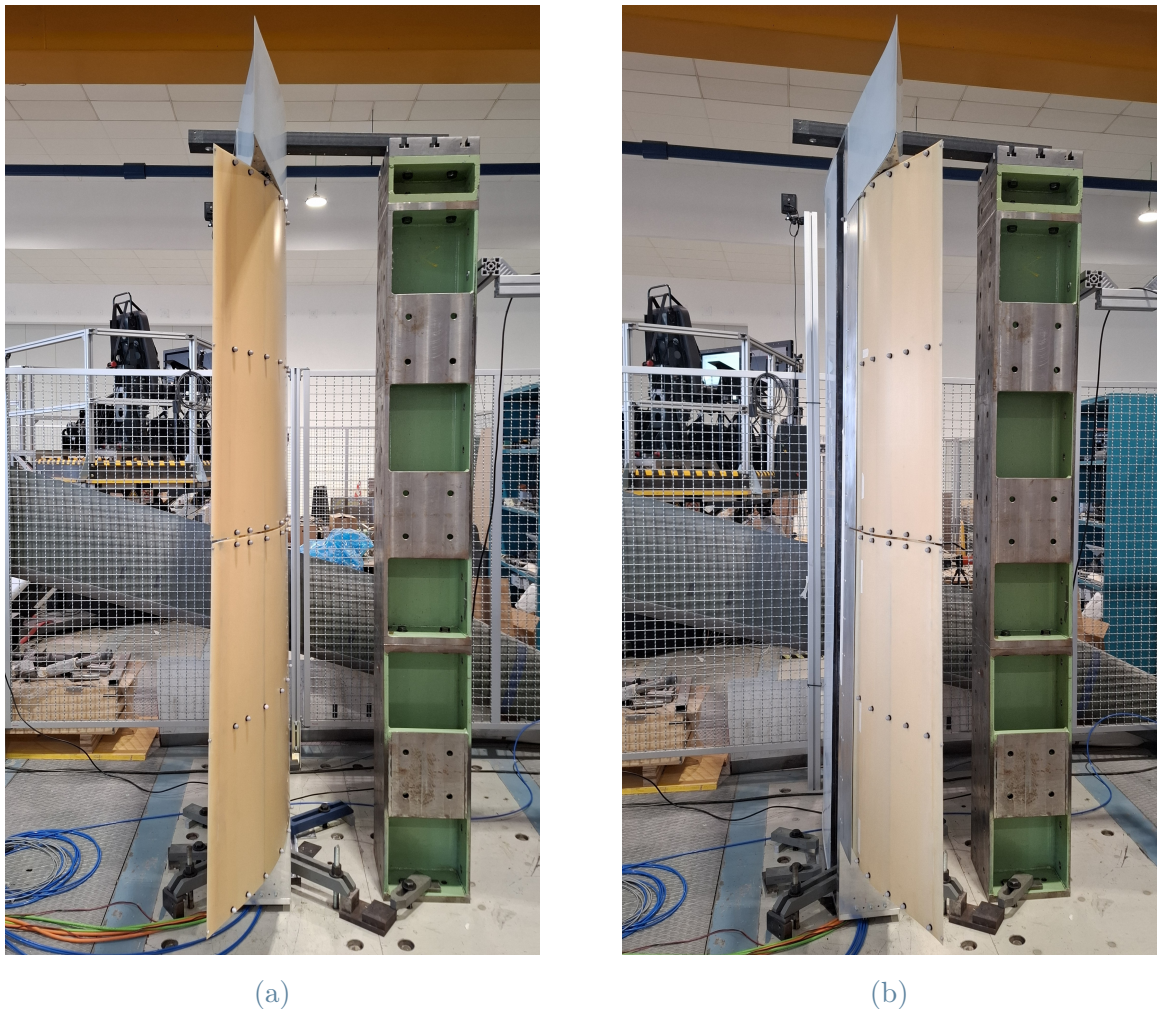


Figure 6.7: Physical morphing aileron prototype under test: (a) maximum positive deflection and (b) maximum negative deflection.

6.2.2. Deflection laws

By executing a quasi-static movement (which allows the dynamic transient effects to be neglected) up to the extreme deflections, and simultaneously recording the motor's actual position data, it was possible to map the relationship between motor stroke and equivalent rotation for both Axis 1 (Figure 6.8) and Axis 2 (Figure 6.9).

This experimental dataset facilitated the derivation of repeatable polynomial laws suitable for HMI implementation. Specifically, the direct law translates the desired aileron rotation (in degrees) into the required motor stroke (in millimetres). This allows the end-user to simply input a target angle, and the interface automatically calculates the corresponding motor linear position. Conversely, the inverse law calculates the rotation angle based on the current actuator position x_a , as measured by the motor's encoder, providing the interface with real-time angular feedback.

It is essential to note that deflections (δ_a) derived solely from the actual actuator position (x_a) may not be accurate during high-frequency dynamic testing, where the aileron's structural dynamics may become significant. Under such conditions, an external measurement tool is required to read the rotation directly.

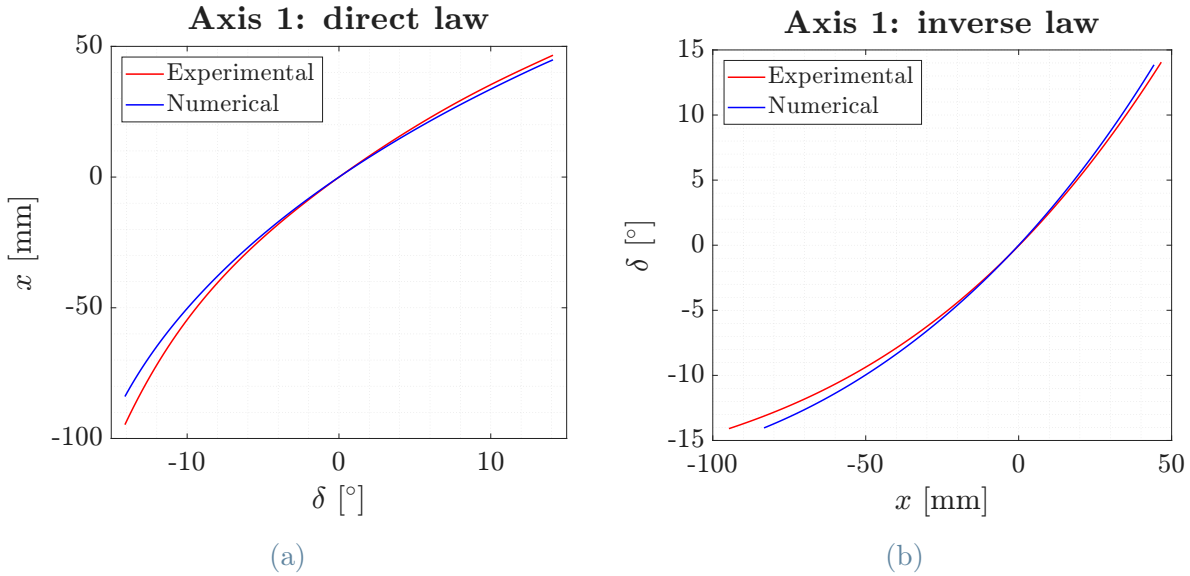


Figure 6.8: Axis 1: (a) direct law; (b) inverse law.

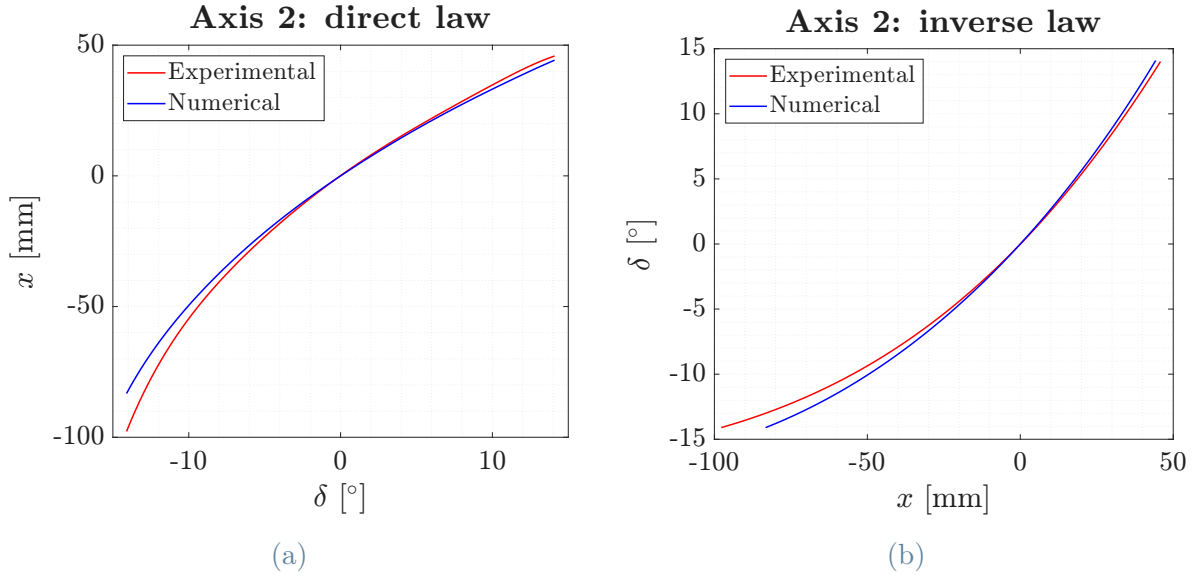


Figure 6.9: Axis 2: (a) direct law; (b) inverse law.

6.2.3. Static actuation loads

Finally, the ultimate objective of the static tests was to determine the actuation forces required to achieve specific deflections, enabling a direct comparison with the numerical model. As previously stated, the system was operated cyclically in a quasi-static manner, sweeping from the maximum deflection of 14° to the minimum of -14° , and then returning to the maximum.

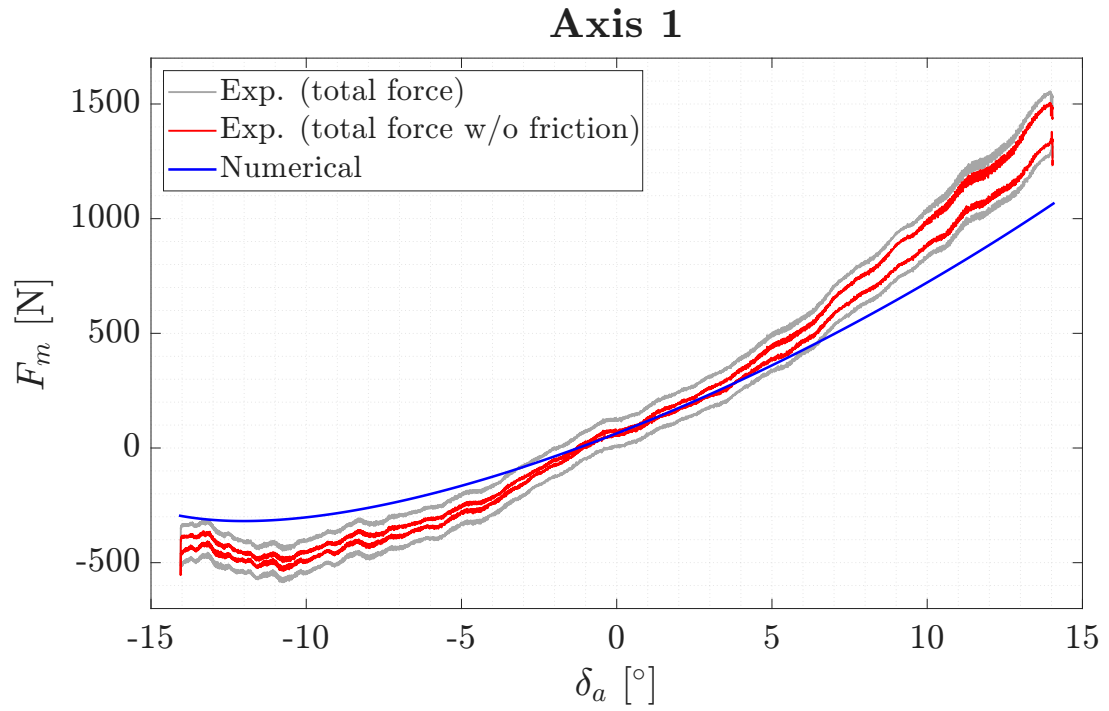
The static tests revealed a non-linear relationship between actuation force and deflection, a result expected from the system's kinematics. However, unlike the numerical model, which only accounts for elastic and gravitational forces (the latter simply shifting the curves vertically), the experimental data exhibit a pronounced hysteresis loop caused by friction.

Because mechanical friction opposes the direction of motion, it adds to the required actuation force when the motor drives the structure away from its neutral position. Conversely, during the return phases towards zero deflection, friction counteracts the elastic restoring force, assisting the motor and resulting in a lower total required force. As noted, inertial and aerodynamic loads are absent due to the wind-off, quasi-static testing conditions.

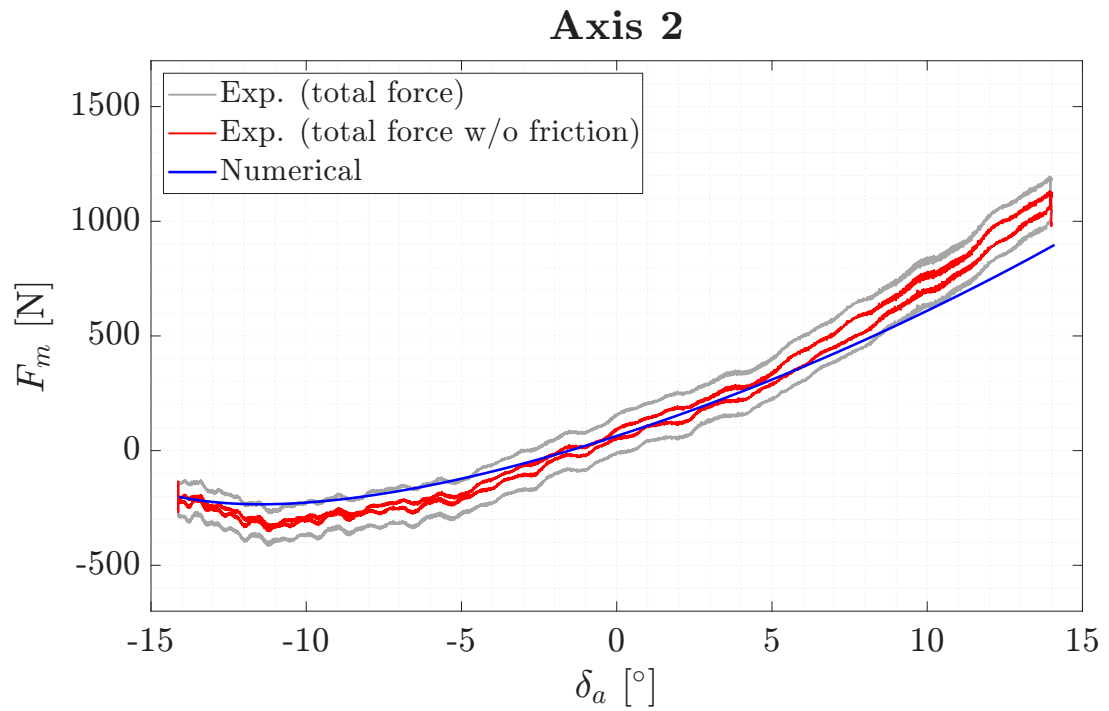
Figure 6.10 presents the actuation loads for both axes. The plots also display the motor force after mathematically subtracting the friction component using the models developed in Section 5.4.1. Ideally, these friction-compensated curves should perfectly overlap. The discrepancies occur because the current friction model does not fully capture the

transverse forces the aileron exerts on the linear guides at varying deflections, which increase the actual friction forces. Consequently, the previously developed friction models marginally underestimate the frictional loads at certain points in the cycle.

Despite this, the friction model is considered reliable, as it reduces the hysteresis observed in the experimental cyclic data. This analysis confirms that whilst friction forces are by no means negligible, the friction-compensated force-rotation curves align well with the structural behaviour predicted by the numerical model.



(a)



(b)

Figure 6.10: Static actuation load analysis over a full continuous cycle for (a) Axis 1 and (b) Axis 2.

7 | Dynamic ground tests

The objective of the dynamic ground tests is to characterise the dynamic behaviour of the morphing aileron prototype, demonstrating the capability of both the actuator and the compliant structure to accurately track commanded inputs. A fundamental goal is to define the system response to specific input signals, evaluating the phase delay and amplitude attenuation or amplification across the entire assembly, from the commanded motor displacement through to the actual aileron rotation.

This serves to verify the alignment with numerical expectations and to assess the structural capacity to withstand the high actuation frequencies envisioned in the design.

Furthermore, the experiments aim to demonstrate that at specific actuation frequencies, the force required by the actuation system is lower than under static conditions due to the presence of a structural eigenmode of the aileron within the bandwidth of interest.

The experimental setup remained identical to the one detailed in Chapter 6, featuring the prototype mounted vertically and constrained at both the root and tip sections.

Dynamic tests were primarily conducted by exciting the system with sine sweep signals. A frequency range of 0 to 10 Hz, compatible with the design bandwidth, was utilised to evaluate the frequency domain response. To thoroughly investigate the system, frequency sweeps at various amplitudes were conducted to determine whether the response exhibited any non-linear frequency dependence.

The parameters described in Section 6.1 were recorded using the Qualisys[®] motion capture system, and the HMI data were also acquired. A sampling frequency of 250 Hz was selected for the Qualisys[®] system to match the HMI sampling frequency and ensure the relevant dynamic phenomena were accurately captured. The acquired signals were then cross-correlated using the actual stroke position measurements obtained from the two distinct acquisition systems to temporally align the datasets, as outlined in Section 6.1.

The recorded measurements were subsequently analysed in both the time and frequency domains. Time history analyses evaluated the transient response, whilst frequency domain analyses identified the Frequency Response Functions (FRFs) between the actuation input

and the acquired output quantities.

Finally, during these tests, the motor saturation limits, specifically the maximum allowable velocity and acceleration, were configured to ensure actuation without excessive delays whilst remaining appropriately low to prevent structural damage.

7.1. Data preprocessing

The collected data underwent preprocessing, which included temporal truncation to restrict the analysis window strictly to the sweep duration. High-noise signals, specifically force and velocity, were filtered to reduce high-frequency oscillations. Position data remained unfiltered, as their initial quality was deemed sufficient for analysis.

Prior to the filtering process, all measured quantities were detrended by removing their mean values in order to eliminate static offsets and the corresponding zero-frequency component. The cut-off frequency was adapted according to the commanded excitation frequency in order to preserve the relevant signal content without significant attenuation. For each test condition, the cut-off frequency was calculated as 1.5 times the target frequency plus an additional 10 Hz margin. A zero-phase forward and reverse digital filtering technique was applied to eliminate any temporal phase delay introduced by the filter. The resulting velocity signal, showing the attenuation of high-frequency noise, is illustrated in Figure 7.1.

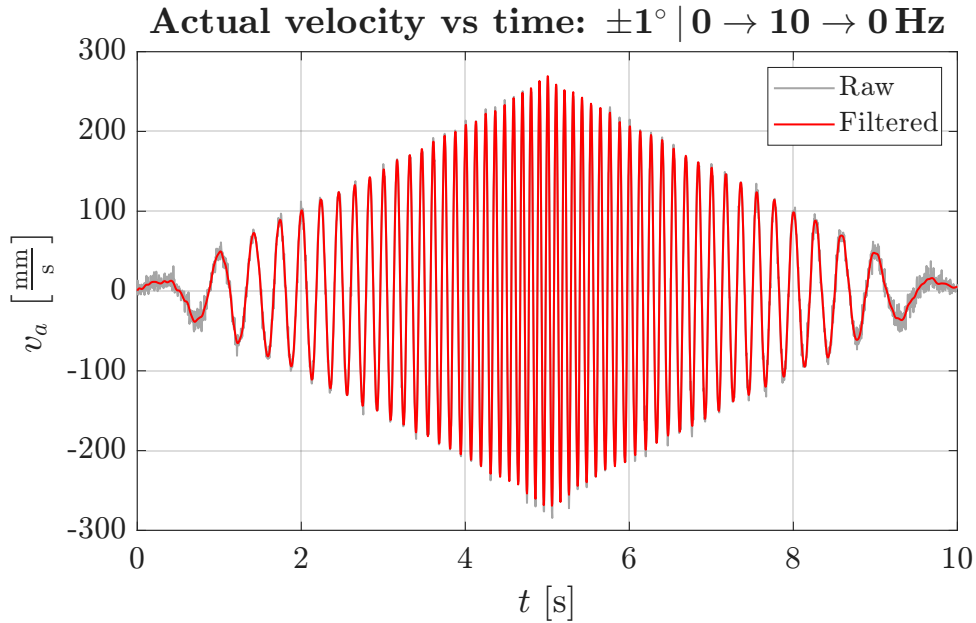


Figure 7.1: Comparison between the raw and filtered velocity signals.

With regard to the actuation force, the mean value removal eliminates the contribution associated with gravitational loading. Furthermore, the friction force component was mathematically subtracted using the coefficients derived in Chapter 5, yielding the friction-deducted motor force ($F_{m,wf}$, where "wf" stands for "without friction").

This correction was introduced for two main reasons. First, mechanical friction tends to mask the structural anti-resonance peak, making the dynamic behaviour of the compliant structure more difficult to identify. Second, it allows the experimental results to be compared directly with the dynamic numerical simulations, which do not account for friction effects. The influence of friction on the identification of the resonance peak, and the consequent need to analyse the friction-deducted force, will be discussed in detail in Section 7.2.1.

The resulting motor force signal, after mean value removal and filtering, is shown in Figure 7.2. Note that, in this graph, the friction force component has not been subtracted from the measured signal.

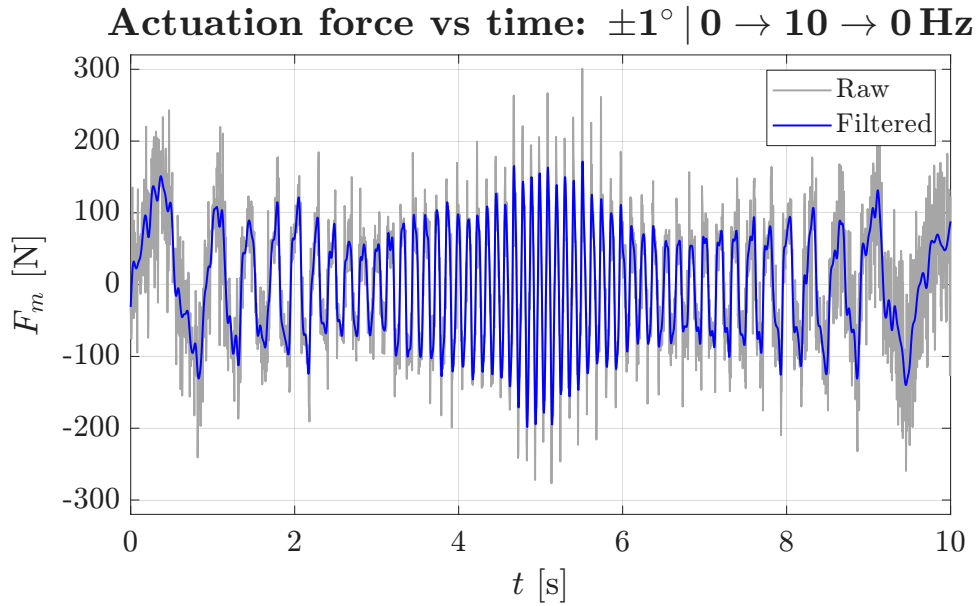


Figure 7.2: Comparison between the raw and filtered force measurements.

Following the preprocessing stage, the dynamic behaviour of the system was evaluated by estimating the FRFs to analyse how key variables, such as the actual motor position and the required actuation force, vary with frequency with respect to their reference signals.

The FRFs were estimated using the integrated MATLAB[®] function `tfestimate`, which relies on Welch's method. To mitigate measurement noise and obtain stable response

curves, the time histories were divided into 2-second segments, providing a frequency resolution of 0.5 Hz. A Hann window was applied to each segment to reduce spectral leakage. Furthermore, a 50% overlap factor was applied to these time windows to evaluate the frequency content of each segment before averaging at each discrete frequency. This averaging procedure across overlapping windows significantly reduces the variance of the FRF estimates [42].

7.2. Experimental results

This section presents the outcomes of the dynamic testing campaign conducted on the morphing aileron prototype. To provide a clear overview of its performance, the discussion is divided into two main areas.

The first part investigates the required actuation forces, specifically analysing the load reduction phenomenon associated with the system anti-resonance, and demonstrating that friction forces contribute to hindering this effort reduction.

The second part evaluates the overall dynamic response, focusing on the capability of the assembly to accurately track the commanded kinematic inputs across the operational frequency range.

For these preliminary analyses, the maximum frequencies (up to 10 Hz) were evaluated with deflections restricted to $\pm 1.5^\circ$. As these were initial tests, larger oscillations up to $\pm 3^\circ$ were investigated at a reduced maximum frequency of 7 Hz for safety reasons. Future developments will aim to reach the full 10 Hz target with larger oscillations of $\pm 5^\circ$.

The results will primarily display the data obtained for axis 1 under a reference condition of $\pm 1^\circ$, sweeping from 0 Hz to 10 Hz and back to 0 Hz. The behaviour observed on the other axis did not show any significant differences.

7.2.1. Dynamic actuation loads

The initial analysis evaluated the morphing device subjected to a linear sine sweep signal (from 0 Hz to 10 Hz and back to 0 Hz, with an amplitude of $\pm 1^\circ$, referred to as double mode in Section 4.3.2) to understand the behaviour of the required actuation force across the frequency spectrum.

As shown in Figure 7.3, the amplitude of the actuation force decreases when the system is excited in the 5–6 Hz frequency range. The visibility of this reduction is dependent on the friction component. Although a minor decrease can be observed in the total motor force

(F_m), the effect is masked by friction. Conversely, the reduction is far more pronounced in the friction-deducted signal ($F_{m,wf}$).

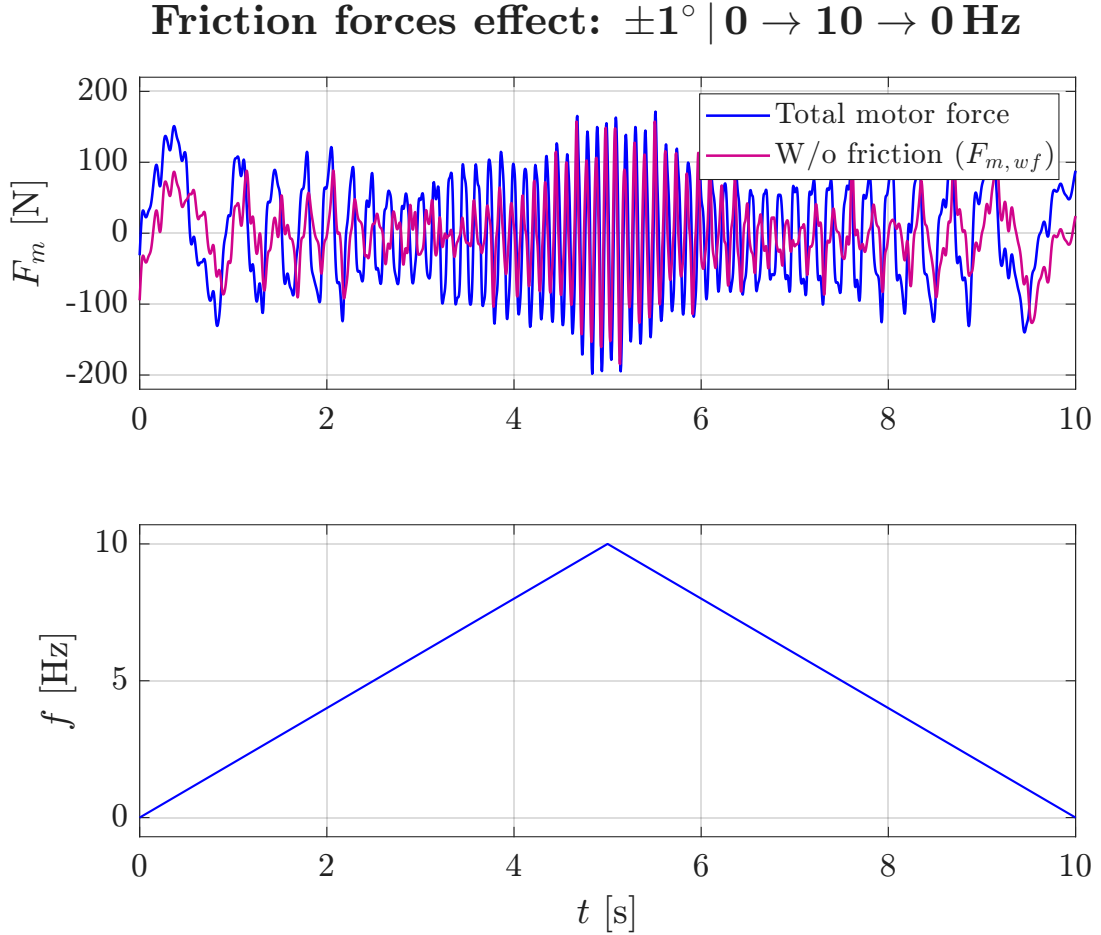


Figure 7.3: Comparison between the total motor force (F_m) and the friction-deducted motor force ($F_{m,wf}$) under a chirp excitation signal.

To further justify the methodological choice of removing the friction force prior to evaluating the system dynamics, Figure 7.4 illustrates the FRF of the required force per unit displacement computed using the total uncompensated motor force (where friction is retained). This graph reports various input amplitudes tested up to an oscillation frequency of 7 Hz.

The FRF was computed using the actual measured displacement rather than the commanded signal to isolate the pure mechanical dynamics of the structure from the influence of the control system.

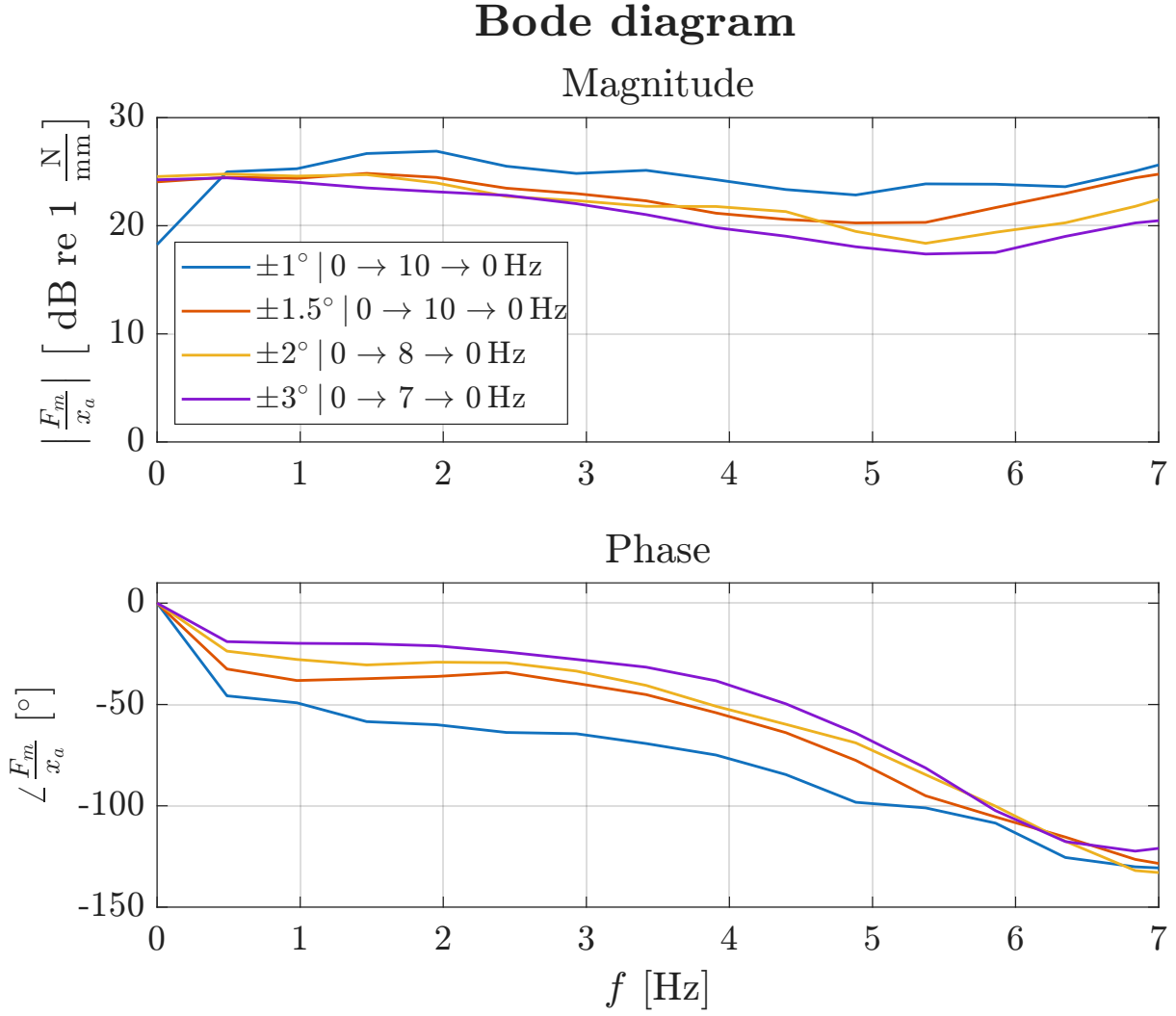


Figure 7.4: Frequency Response Function of the required actuation force (including friction) per unit displacement, at various oscillation amplitudes.

As evident from the graph, when friction is not removed, the anti-resonance peak becomes indistinguishable. This occurs because the mechanical friction within the sliding guides and the kinematic chain introduces a non-linear damping effect into the overall system. This dissipative force flattens the frequency response amplitude and alters the phase behaviour, masking the force-reduction phenomenon.

Consequently, friction removal was essential to reveal the anti-resonant behaviour of the compliant structure, while also aligning the experimental data with the numerical models.

Once the friction is deducted, the underlying dynamic response emerges. Figure 7.5 presents the FRF of the friction-deducted motor force per unit actual displacement across

various oscillation amplitudes.

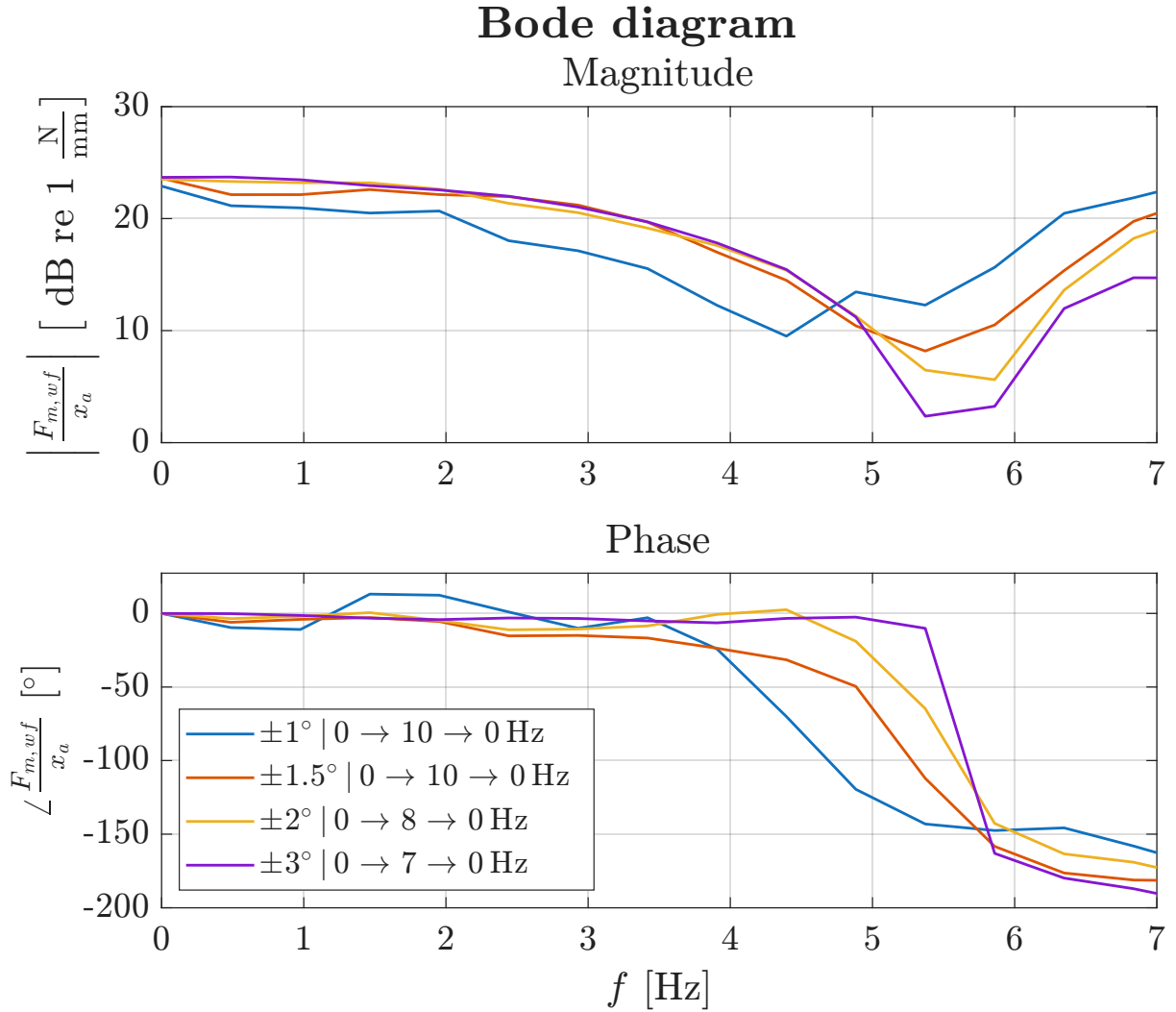


Figure 7.5: Frequency Response Function of the required actuation force (without friction) per unit displacement, at various oscillation amplitudes.

This graph indicates that the anti-resonance peak falls within the 5–6 Hz range, regardless of the input amplitude. Thanks to this characteristic, there is a specific frequency band in which the sum of the structural and inertial contributions is lower than the static elastic values. Therefore, the system ensures a reduction in the required actuation force relative to the static case across multiple dynamic operating amplitudes.

This beneficial behaviour aligns with the outcomes of the modal analyses and numerical simulations discussed in Section 3.4.2, even if occurring at a slightly lower frequency in

the physical prototype. The effect is further supported by the corresponding phase shift between force and position, which occurs exactly at this frequency range.

Finally, to definitively confirm that this load reduction is generated by the aileron itself, Figure 7.6 displays the FRF between the friction-deducted motor force and the actual displacement, evaluated both with and without the aileron installed.

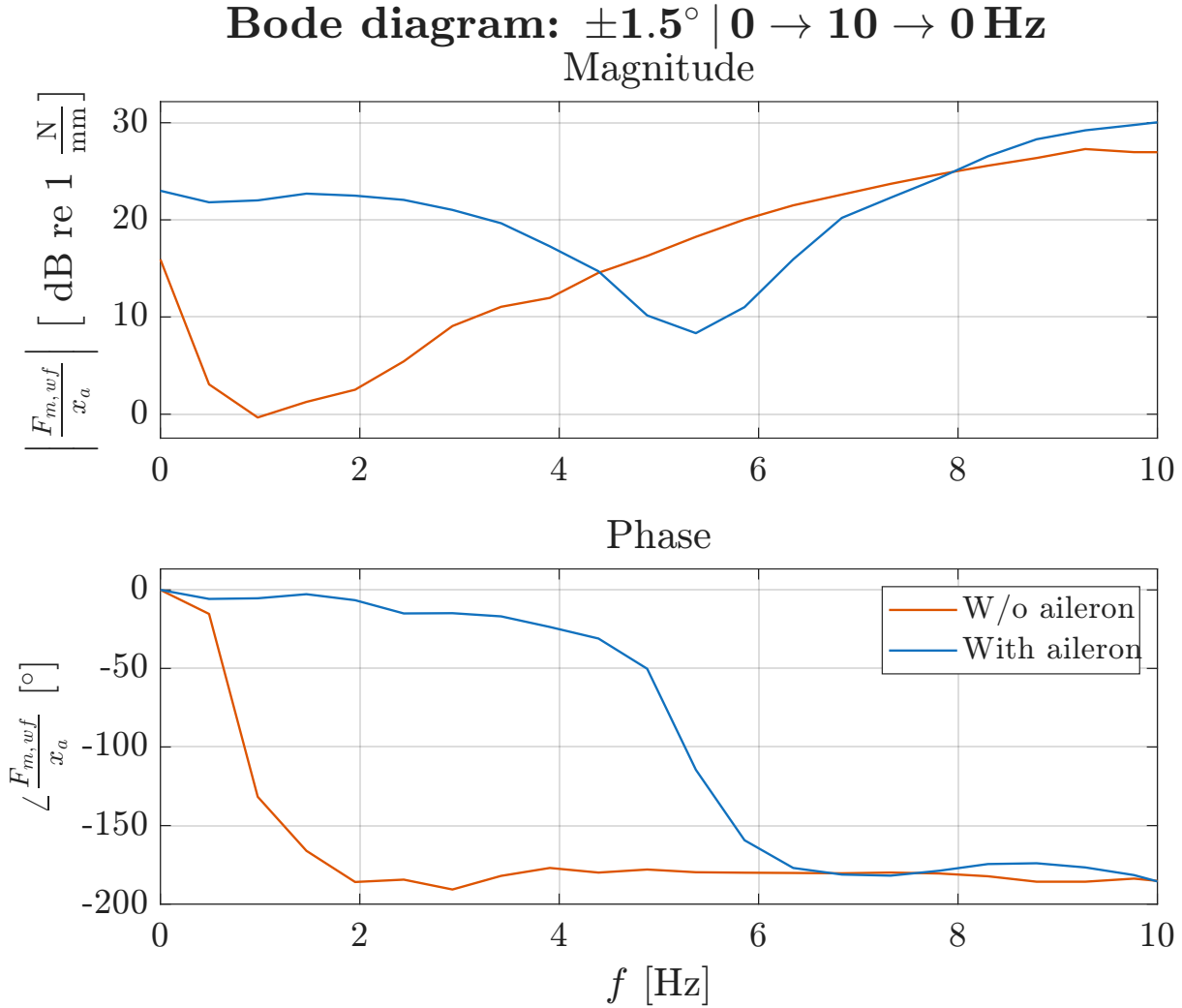


Figure 7.6: Frequency Response Function of the required actuation force (without friction) per unit displacement, comparing the bare actuator to the fully assembled system.

The curve representing the fully assembled configuration reveals an anti-resonance peak at 5.4 Hz. Conversely, the FRF of the bare actuator exhibits a steady increase in magnitude across that region without any notches, demonstrating that this reduction in actuation load is due to the dynamic coupling with the compliant aileron structure.

7.2.2. System dynamic response

Having demonstrated that the required actuation force per unit displacement decreases around a specific frequency, the analysis shifts to evaluating the system's tracking performance across the frequency band of interest.

Figure 7.7 presents the time histories of the main data acquired through the HMI and the Qualisys[®] system. The plots illustrate the friction-deducted motor force ($F_{m,wf}$) alongside the actual rotation measured by Qualisys[®] (δ_q). Additionally, the actual (x_a) and desired (x_d) stroke displacements recorded by the HMI are compared, as well as the desired rotation (δ_d) against the actual one. The frequency profile over the observation period is also plotted.

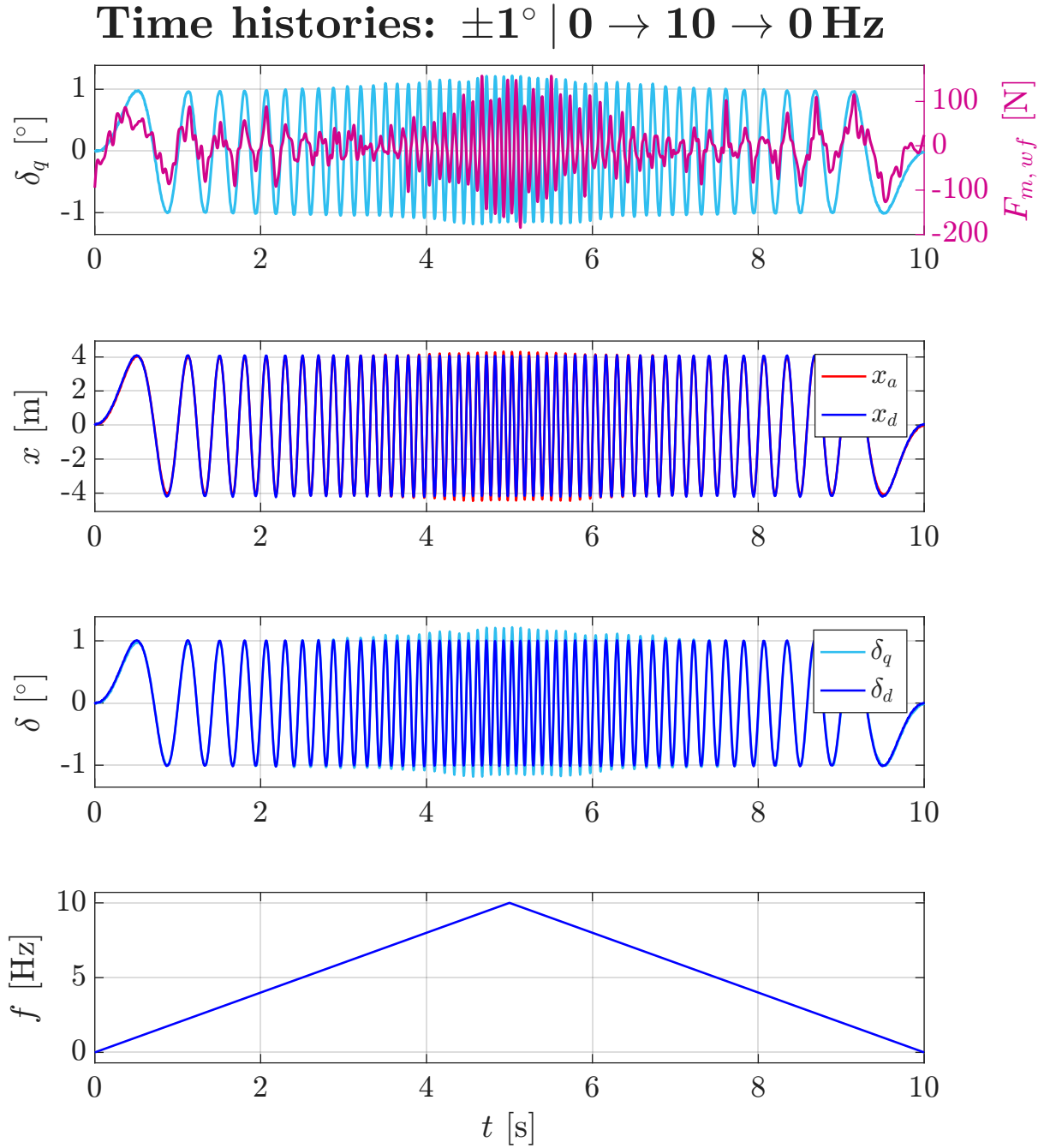


Figure 7.7: Time histories of the system dynamic response under a dual chirp excitation signal.

The time histories reveal that the required friction-deducted force amplitude decreases during the initial 2.5s (corresponding to approximately 5 Hz) before increasing again towards the 10 Hz peak. Notably, the friction-deducted force and the actual rotation are in phase at the beginning and end of the test but shift to an opposite phase condition in

the central region above 5–6 Hz, as also demonstrated by Figure 7.5. Furthermore, the actual and desired displacements remain in phase, with the actual displacement exhibiting a slight amplification. This behaviour is reflected in the rotation data.

To better understand these phenomena, FRFs were analysed. The first evaluated FRF compares the actual stroke position (x_a) to the desired stroke position (x_d), as shown in Figure 7.8. Across the entire frequency range of interest, the amplitude of the transfer function remains practically at 0 dB, while the phase shift is almost null.

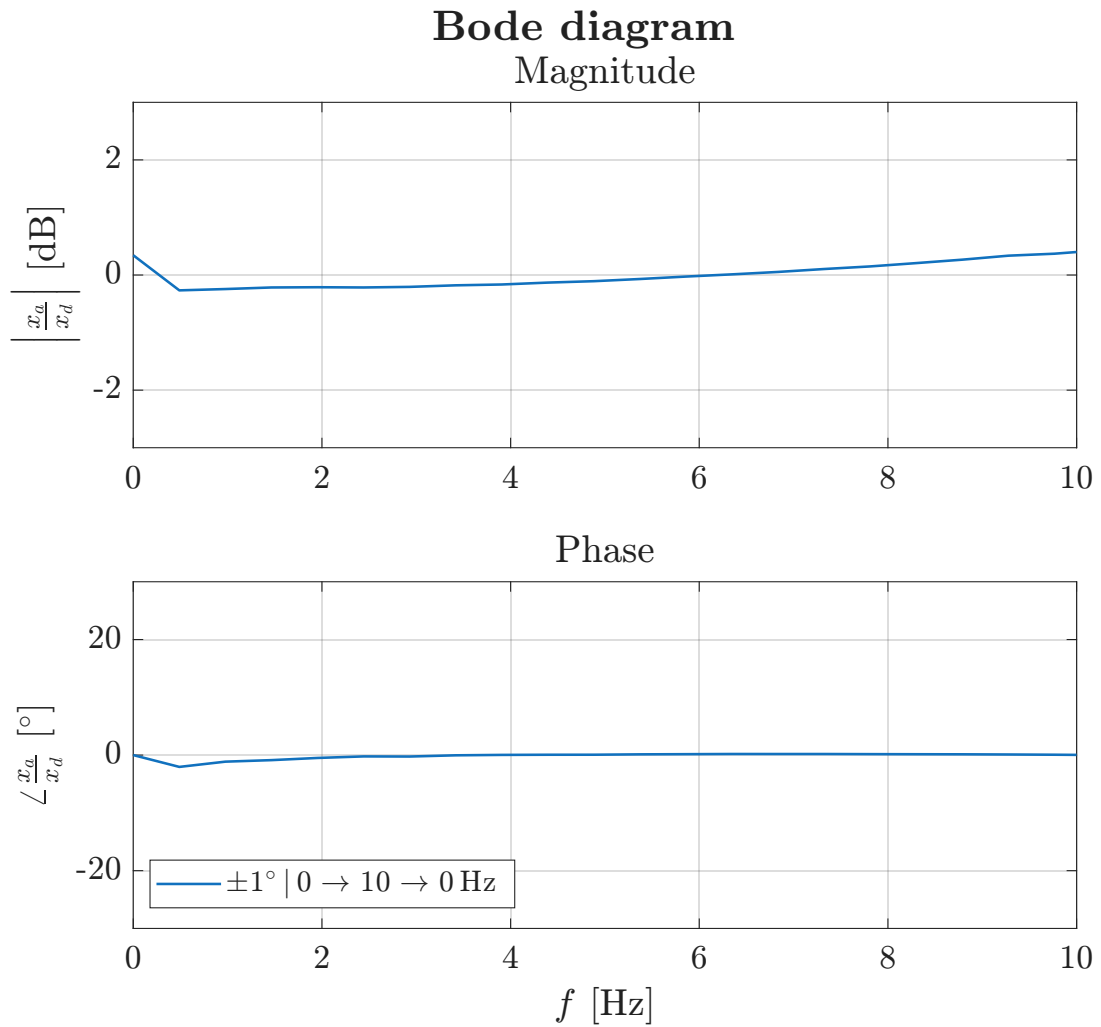


Figure 7.8: Frequency Response Function comparing the actual and desired motor stroke positions.

Figure 7.9 represents a similar analysis conducted for the ratio between the actual (δ_q) and desired rotations (δ_d). The overall system gradually amplifies the commanded input as the frequency increases, reaching an amplitude of approximately 1 dB at 10 Hz. This signal

amplification stems partially from the dynamic response of the actuator (Figure 7.8) and partially from the intrinsic dynamics of the compliant structure. The phase shift remains zero throughout the operational frequency range.

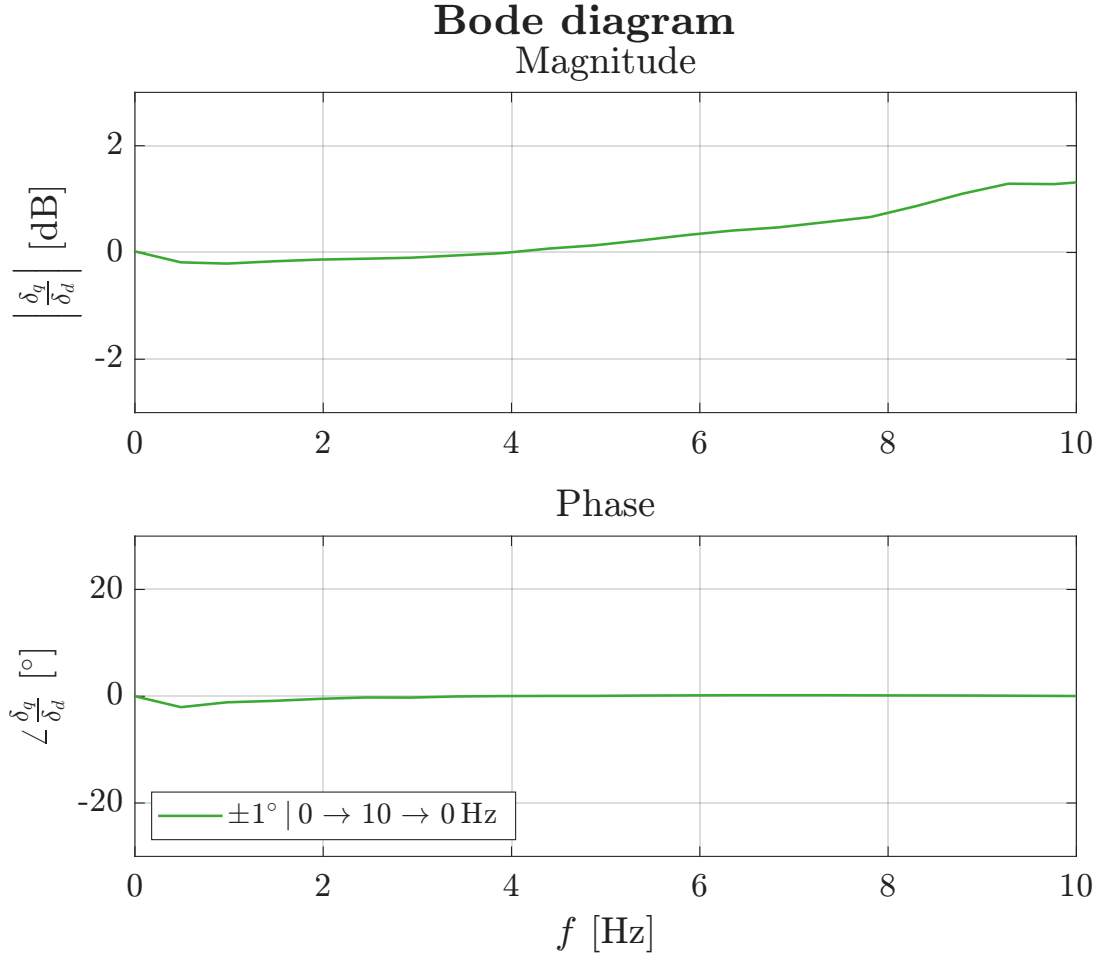


Figure 7.9: Frequency Response Function comparing the actual and desired aileron rotations.

No dynamic instabilities or undesirable oscillatory behaviours were detected within the investigated range of amplitudes and frequencies.

For completeness, Figure 7.10 shows a comparison between the actual and desired positions for amplitudes exceeding $\pm 1^\circ$. This clearly demonstrates that the system behaviour remains essentially identical despite the different tested amplitudes.

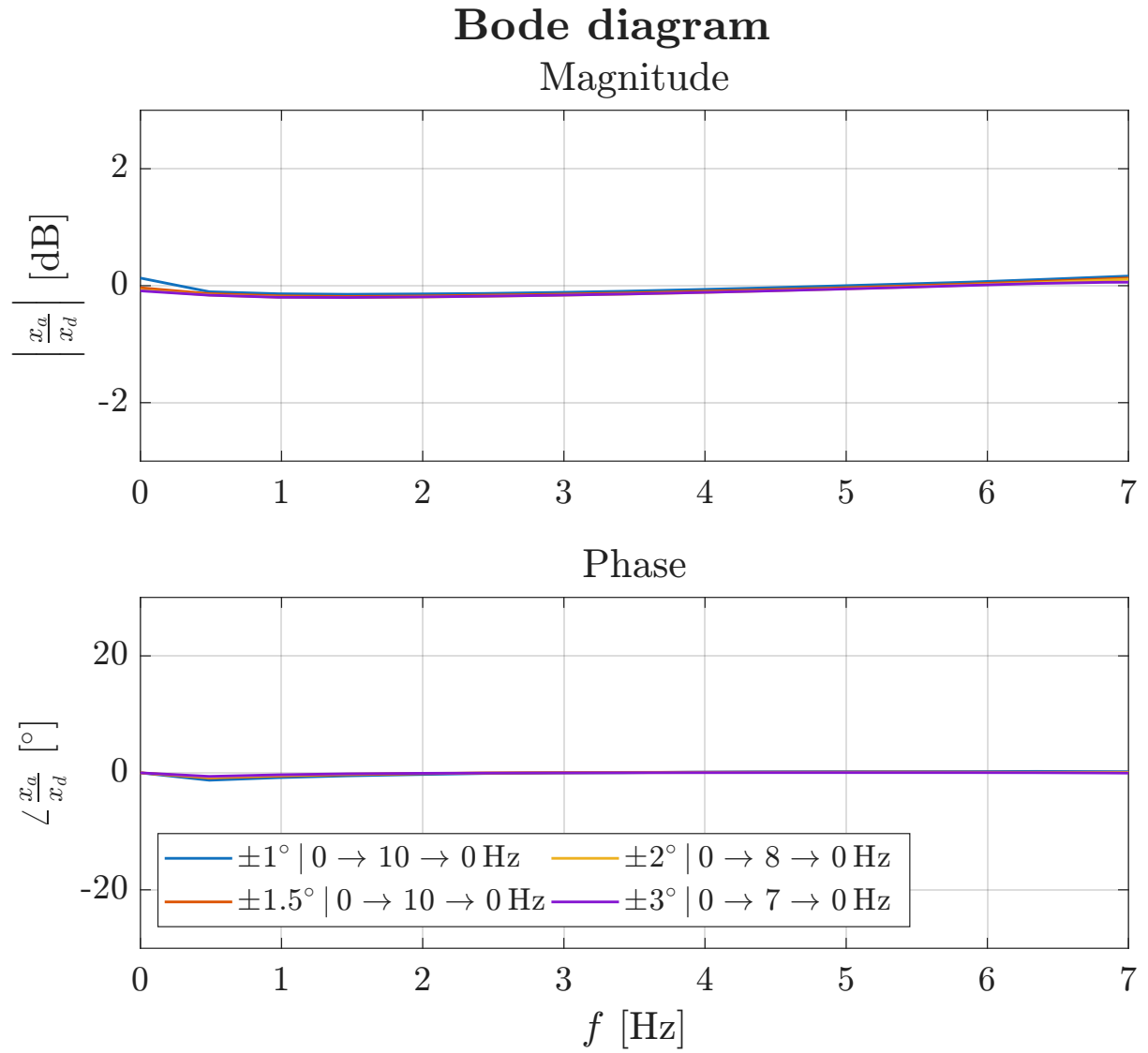


Figure 7.10: Comparison of the actual and desired positions at various input amplitudes up to a maximum frequency of 7 Hz.

Consequently, the observed dynamic behaviour demonstrates the feasibility of employing the proposed morphing aileron not only for low-frequency manoeuvring but also for higher-frequency applications. These capabilities are particularly relevant for active load alleviation strategies, where rapid shape adaptation is required to mitigate transient aerodynamic loads and gust-induced structural responses.

8 | Wind tunnel tests

The wind tunnel test campaign was planned to validate the numerical results obtained during the design phase and to further investigate the experimental findings discussed in Chapter 6 and Chapter 7, in the presence of aerodynamic loads.

This chapter outlines the primary objectives of the test campaign and describes the selected facility. Subsequently, an overview of the experimental setup and the testing procedures is provided. It should be noted that this thesis focuses on the presentation of preliminary results from the static and quasi-static wind tunnel tests, where the aileron deflections were either maintained at fixed positions or varied at a very low actuation speed, whereas the analysis of the dynamic tests is beyond the scope of the present thesis.

8.1. Wind tunnel test objectives

The first objective of the wind tunnel campaign was to characterise the steady aerodynamic performance of the model. The measurements were used to determine the lift coefficient as a function of the angle of attack, the drag coefficient as a function of the angle of attack, and the aerodynamic polars for different aileron deflection angles.

The test matrix also included static and quasi-static tests to evaluate the actuation force required to achieve and maintain prescribed control surface deflections under aerodynamic loading, and to assess whether the structural deformation of the two morphing ailerons under aerodynamic loads remained consistent with the shapes measured in wind-off conditions.

The actuation force was evaluated by commanding a complete deflection cycle from the maximum deflection of 14° to the minimum deflection of -14° , and back to 14° . The motion was performed at a very low actuation speed for the test to be considered quasi-static, thereby ensuring that the flow field could be treated as steady at each instantaneous deflection angle. This procedure provided a further validation of the developed friction model (described in Chapter 5).

On the other hand, the structural deformation of the morphing ailerons was analysed

by holding the control surfaces at prescribed static deflections. The force and shape measurements were conducted across a range of angles of attack and flow velocities to evaluate the influence of varying aerodynamic load magnitudes and distributions on the structural response.

Finally, the campaign proceeded with dynamic testing, where the actuation system was driven using sinusoidal inputs and sine sweep signals (at different oscillation amplitudes and frequencies) to investigate the dynamic response and determine the required dynamic actuation force in the presence of unsteady aerodynamic effects.

8.2. Facility description

The Politecnico di Milano wind tunnel is a closed-loop facility featuring a vertical layout with two distinct test sections.

The lower test room is designed for Low Turbulence tests. It measures 4 m (width) $\times$ 3.84 m (height) $\times$ 6 m (length) and provides a maximum wind speed of 55 m/s. This section can be configured as an open-jet test section, measuring 4 m $\times$ 3.84 m $\times$ 5 m, while maintaining the same maximum velocity [35].

The upper test room, known as the Boundary Layer Test Section, is intended for civil engineering testing. It offers a larger test volume of 13.84 m $\times$ 3.84 m $\times$ 35 m with a maximum wind speed of 16 m/s.

The facility is powered by an array of 14 fans, each 1.8 m in diameter and rated at 100 kW, delivering a total power of 1.4 MW. The fans are organized into two rows of seven 2 $\times$ 2 m independent cells. Each fan is driven by an individual inverter, allowing for precise rotational speed control to achieve the target flow velocity in the test section.

Downstream of the fan array, two corners equipped with turning vanes guide the flow to the upper level. A heat exchanger is placed downstream of the second bend to regulate the flow temperature before it passes through a grid and enters the Boundary Layer Test Section.

A second set of turning vanes redirects the flow to the lower level. Here, the air passes through a 2 m settling chamber, a honeycomb screen, and three wire nets of varying porosity. This conditioning stage reduces axial and lateral turbulence and ensures flow uniformity. A two-dimensional contraction cone with an area ratio of 3.46 : 1 then accelerates the flow into the Low Turbulence test section, before a diffuser expands the duct back to the fan array dimensions.

The current experimental campaign was conducted in the Low Turbulence test section. This section is characterized by a turbulence intensity below 0.1% and mean velocity variations within 0.25%. A key feature of the tunnel design is the availability of two interchangeable test sections, allowing for the assembly of new experiments while the tunnel is operational in the other section.

The test section is equipped with a 2.5 m diameter turntable, which provided the necessary support for the morphing aileron prototype. The turntable enabled automatic control of the angle of attack throughout the tests.

The overall configuration and the internal view of the Low Turbulence test section of the GVPM facility are illustrated in Figure 8.1.

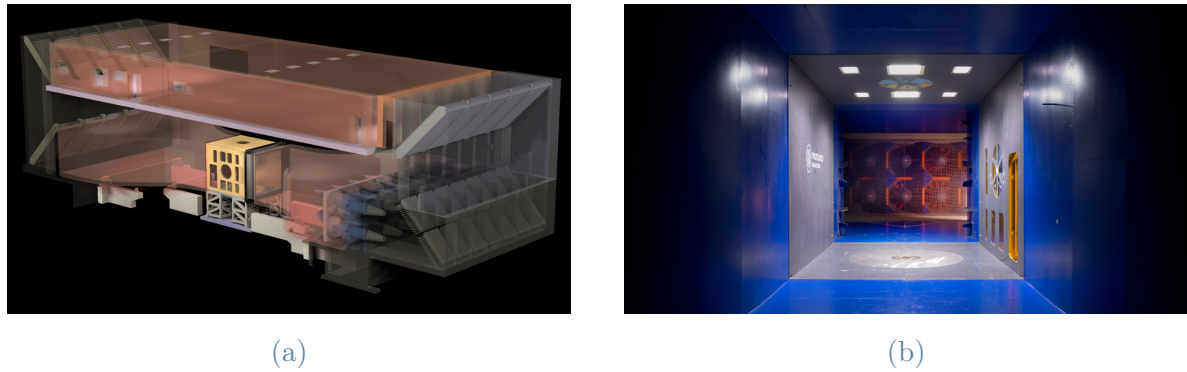


Figure 8.1: Overview of the GVPM wind tunnel vertical layout (a) and internal perspective of the Low Turbulence test section, showing the turntable and the fan array in the background (b) [35].

8.3. Experimental setup

Due to the multiple objectives of the wind tunnel test campaign, encompassing both static and dynamic trials, various measurement systems were implemented. These systems served to record parameters essential for assessing the aerodynamic performance, structural response, and required actuation forces.

The experimental configuration is illustrated in Figure 8.2, showing views from both the pressure and suction sides. The wing section was positioned vertically and mounted onto a RUAG AG Model 777 balance, which was in turn attached to a rotary table. This arrangement allowed for the variation of the angle of attack throughout the testing process.



Figure 8.2: Wind tunnel experimental setup: (a) pressure side view; (b) suction side view.

Specifically, the RUAG AG Model 777 six-component strain gauge balance (Figure 8.3) acquired the force and moment measurements required for the aerodynamic characterisation of the model in both static and dynamic wind tunnel analyses. The instrument was secured to both the test section and the model in accordance with the tightening torque recommended by the manufacturer.

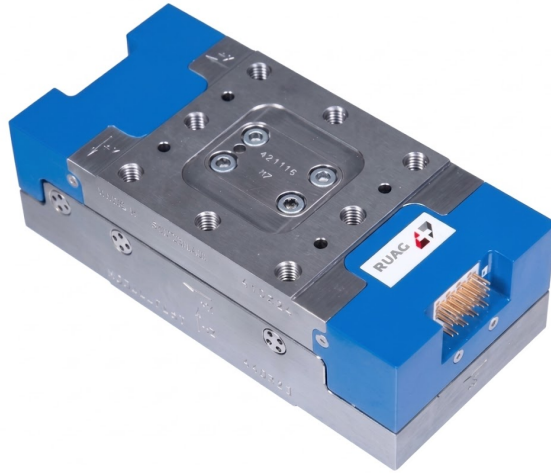


Figure 8.3: RUAG AG Model 777 balance used for aerodynamic force and moment measurements.

The mechanical capacities of the balance are detailed in Table 8.1. It is essential to distinguish between the design loads, which represent the maximum capacities under simultaneous combined loading, and the limit loads, which apply when only a single load component is acting on the balance. The instrument was specifically calibrated to operate accurately within the design load envelope.

Force / Moment	Design load	Limit load
X [N]	3 000	3 000
Y [N]	4 000	16 000
Z [N]	8 500	50 000
L [Nm]	1 100	2 375
M [Nm]	2 500	6 850
N [Nm]	1 300	1 750

Table 8.1: Design and limit loads of the RUAG AG 777 balance [36].

Furthermore, the main electrical and metrological properties are summarised in Table 8.2. The balance guarantees a measurement accuracy better than 0.05% of the design loads, alongside high structural stiffness [36]. Finally, it should be noted that the raw loads

acquired by the balance were measured in the body-fixed reference frame. During the data postprocessing phase, these measurements were converted into the wind axes coordinate system through a rotation transformation, allowing for the extraction of the aerodynamic components.

Parameter	Value
Measurement accuracy	$< 0.05\%$ (of design loads)
Nominal excitation	7 V
Nominal output	14 mV (2 mV/V)
Channels	7 strain gauge bridges, 2 PT100 sensors
Eigenfrequencies	> 900 Hz
Temperature zero shift	$< 0.004\%/K$

Table 8.2: Main electrical and metrological properties of the RUAG AG 777 balance [36].

To evaluate the operational shape, instead, the marker-based Qualisys[®] system used in previous ground tests was replaced by a Siemens Digital Image Correlation (DIC) system. This non-intrusive optical method relies on applying a random speckle pattern of black dots over a white background painted onto the surface.

By tracking the displacement of these speckles across a series of images using a spatial cross-correlation algorithm, the system enables the three-dimensional reconstruction of the aileron surface deformation.

Figure 8.4 illustrates both the high-contrast pattern applied to the ailerons and the camera arrangement. Specifically, four cameras were used in total, with two on each side, mounted directly onto the wind tunnel wall to frame the target area; additionally, LED lights were utilised to ensure the surfaces of interest were well illuminated.

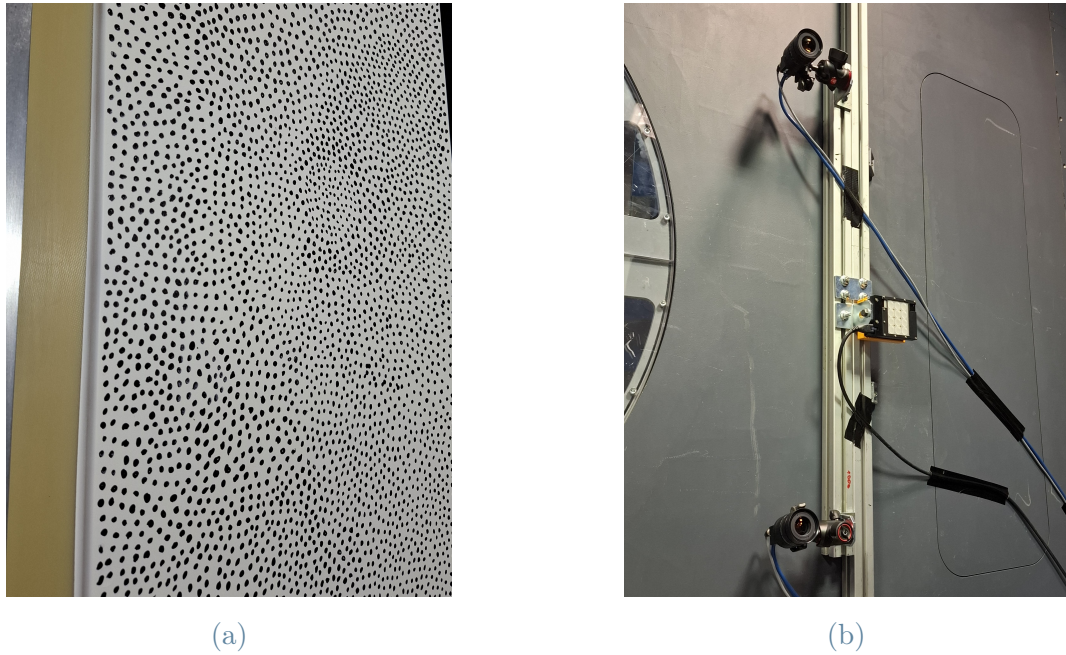


Figure 8.4: DIC system setup: (a) speckle pattern applied to the aileron surface; (b) cameras mounted on the wind tunnel wall.

Prior to the wind tunnel testing phase, an extensive array of ICP[®] triaxial accelerometers (PCB Piezotronics, model 356A22) was utilised to perform Ground Vibration Tests (GVT) to identify the natural frequencies and mode shapes of the structure. Characterising these natural modes was a safety measure to anticipate structural behaviour and prevent resonance or aeroelastic instabilities. However, during the actual wind tunnel campaigns, only six of these sensors were retained within the wing box.

To achieve a comprehensive dynamic characterisation, these six accelerometers were strategically distributed along the main spar, with units positioned at the tip rib. This layout provided a sufficient geometric lever arm to effectively capture both the overall wing torsion, the out-of-plane bending and the in-plane bending. An example of this setup is presented in Figure 8.5, which illustrates one of the accelerometers mounted directly onto the spar. The main technical specifications of these devices are summarised in Table 8.3.



Figure 8.5: A PCB Piezotronics 356A22 triaxial accelerometer mounted on the main spar of the wing-box.

Parameter	Value
Sensitivity ($\pm 10\%$)	100 mV/g
Measurement range	± 50 g pk
Frequency range ($\pm 5\%$)	0.5 to 4000 Hz
Resonant frequency	≥ 25 kHz
Broadband resolution	0.0002 g rms
Weight	5.4 g

Table 8.3: Main technical specifications of the PCB Piezotronics 356A22 triaxial accelerometer [33].

Finally, all parameters available through the HMI were recorded during the tests. These included the actuation force and both the target and actual positions of the motor and rotation of the aileron (the list of the recorded data channels is provided in Section 4.3.1).

8.4. Experimental results

This chapter presents the preliminary results concerning the aerodynamic characterisation of the model and the evaluation of the required actuation forces.

The aerodynamic performance was initially assessed by evaluating the lift and drag coefficients as a function of the angle of attack, alongside the aerodynamic polars for various aileron deflection angles. These analyses were conducted by symmetrically deflecting both ailerons and sweeping the angle of attack from -16° to 26° , a range specifically selected to capture the stall condition for each deflection setting. To remain within the load envelope of the aerodynamic balance across all tested angles, a free-stream velocity of 15 m/s was chosen for this phase.

Subsequently, the investigation shifted to the measurement of the actuation forces under quasi-static conditions. The model was fixed at specific angles of attack while the ailerons were cyclically commanded from the maximum to the minimum deflection angle, and back. The actuation speed was kept sufficiently low ($v_a = 2$ mm/s) to maintain a negligible reduced frequency ($k \approx 0.0044$), thus preventing unsteady phenomena and ensuring that the aerodynamic field could be treated as steady. A further aim of these cycles was to investigate whether the aerodynamic loading introduces different hysteresis effects on the structural friction compared to the wind-off scenario.

To evaluate these actuation forces and the corresponding shape variations under higher aerodynamic loads, it was desirable to increase the wind tunnel velocity to a range of 35 to 40 m/s. However, to prevent exceeding the safety limits of the balance at these higher speeds, the testing procedure was adapted to actuate only one aileron at a time. Concurrently, Siemens shape tracking instrumentation was employed to monitor the structural deformation, assessing whether the aerodynamic loads caused any undesirable shape variations across the different operating conditions.

It should be noted that while the experimental campaign successfully acquired the shape measurements and the dynamic actuation data, the corresponding post-processing phases are still ongoing. Therefore, due to time constraints, the present thesis reports only the findings related to the steady aerodynamic characterisation and the quasi-static actuation forces.

8.4.1. Aerodynamic performance characterisation

To evaluate the steady aerodynamic characteristics, several configurations were tested. Specifically, the measurements were recorded by maintaining a fixed deflection of both ailerons while sweeping the angle of attack until stall conditions were reached. Figure 8.6 illustrates the maximum deflection of both ailerons of 14° viewed from the upper side, alongside the minimum deflection of -14° viewed from the lower side.

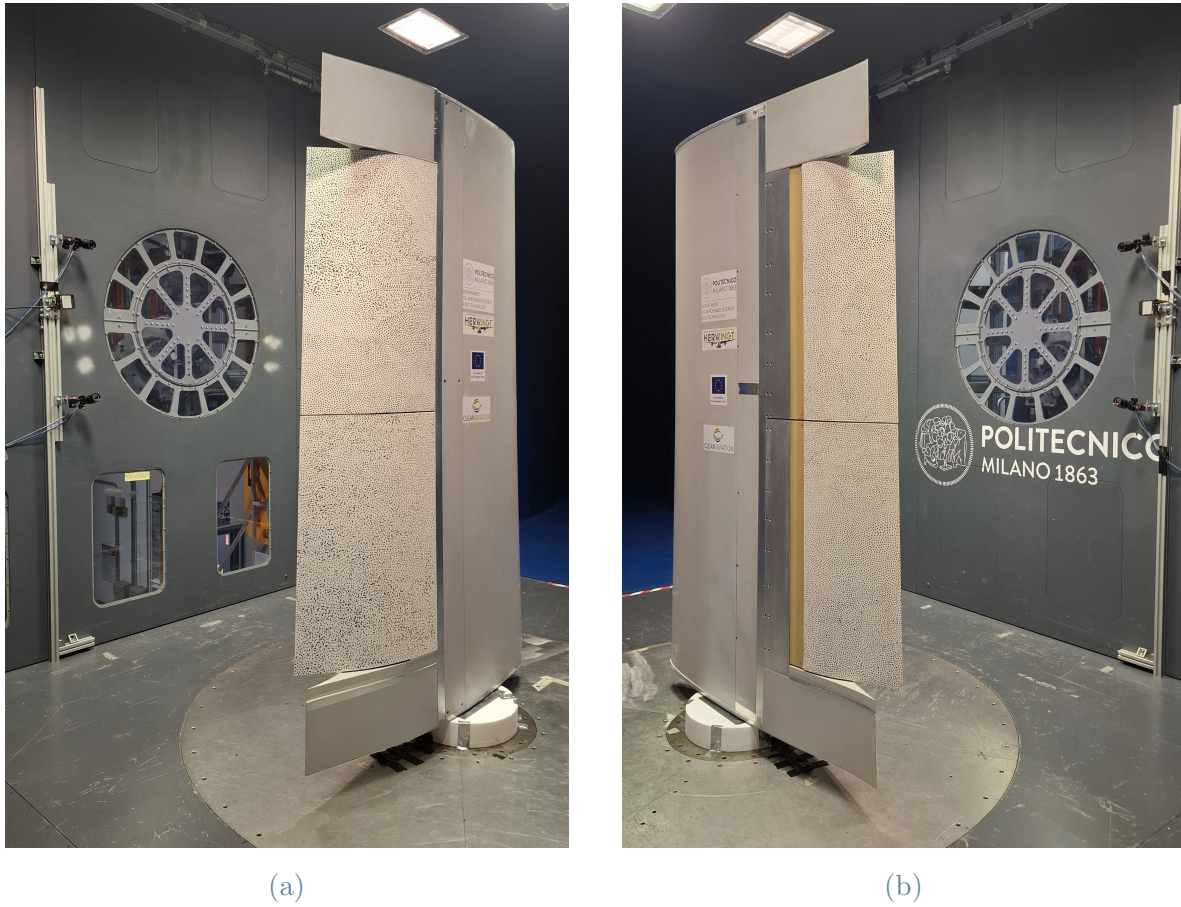


Figure 8.6: (a) maximum deflection viewed from suction side; (b) minimum deflection viewed from the pressure side.

Figure 8.7 and Figure 8.8 present the variations in the lift and drag coefficients, respectively, as a function of the angle of attack for the different deflection settings. While additional analyses were also conducted to evaluate the aerodynamic response when only a single aileron was actuated, the primary focus of this section is on symmetric configurations, where both ailerons were actuated simultaneously. Furthermore, Figure 8.9 reports the corresponding aerodynamic polars derived from these symmetric deflections.

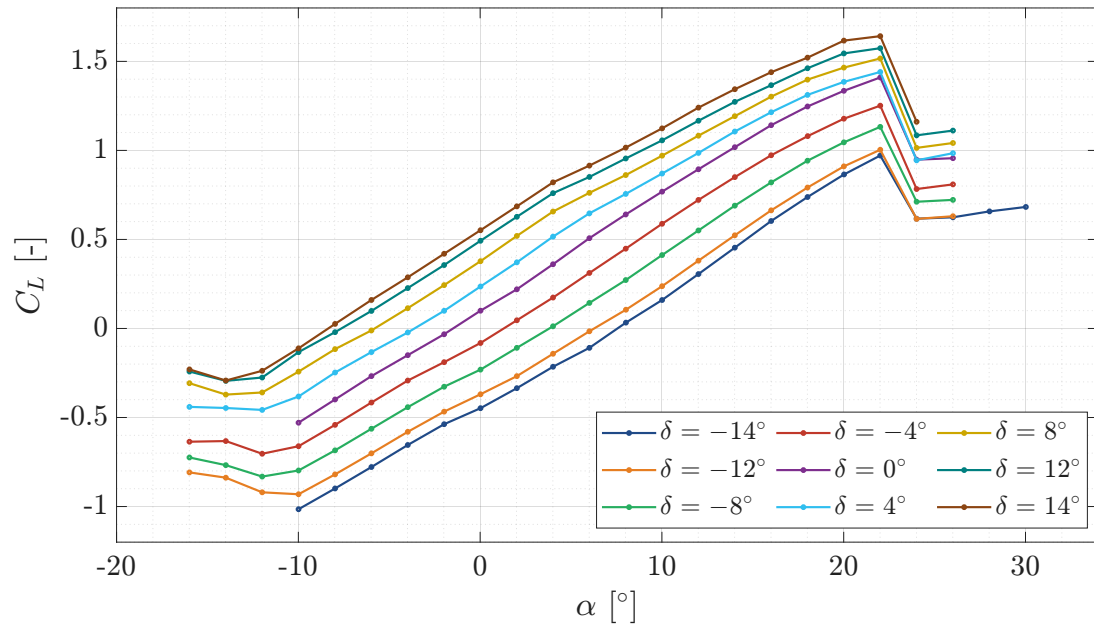


Figure 8.7: Lift coefficient as a function of the angle of attack for various aileron deflection angles.

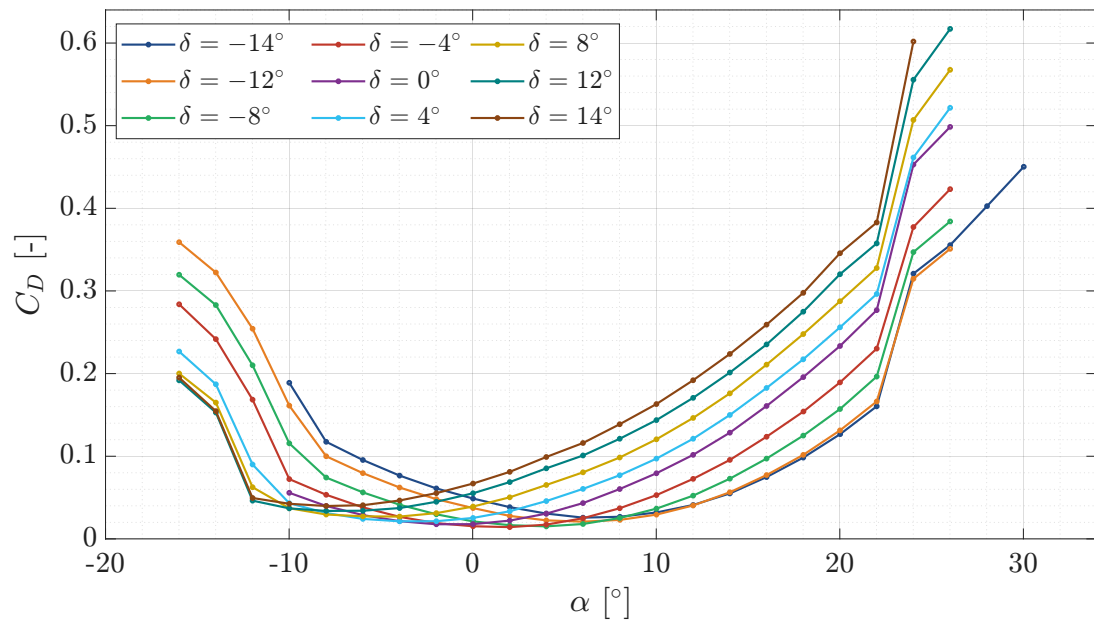


Figure 8.8: Drag coefficient as a function of the angle of attack for various aileron deflection angles.

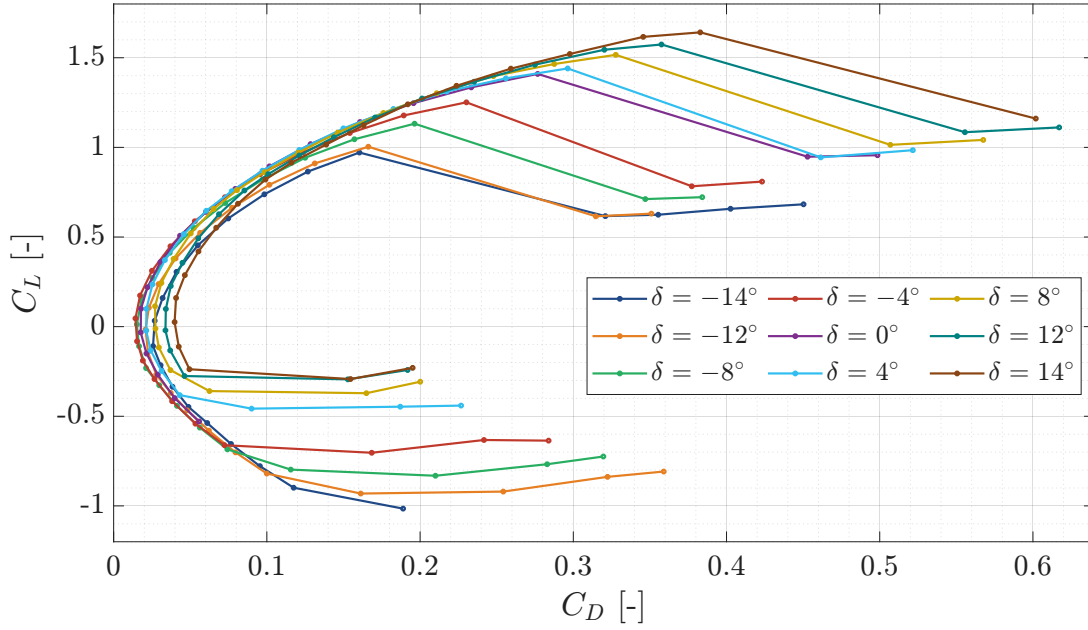


Figure 8.9: Aerodynamic polar for various aileron deflection angles.

The extraction of these aerodynamic results serves three primary purposes. First, it establishes a robust experimental baseline for future numerical-experimental correlations. Second, it allows the required actuation forces (analysed in Section 8.4.2) to be evaluated as a direct function of the applied aerodynamic loads. Finally, it provides a clear understanding of the prototype's steady aerodynamic behaviour with increasing wind speed, ensuring that the tests remain conservative and safely within the load limits of the wind tunnel balance.

8.4.2. Actuation load assessment and friction analysis

Figure 8.10 presents the actuation forces recorded during a complete quasi-static deflection cycle, operating between the extreme limits of $\pm 14^\circ$. These measurements are compared against the baseline wind-off values previously discussed in Section 6.2.3. For clarity, the comparison between wind-on and wind-off conditions is shown for both ailerons at a test section velocity of 35 m/s. This specific velocity represents the maximum allowable wind speed for the aerodynamic balance when reaching the extreme deflection angles and 0° angle of attack.

As expected, the required actuation forces are noticeably higher in the presence of aerodynamic loading. In addition, during positive deflections, particularly for Axis 2, the internal friction increases significantly. This is evident from the broader hysteresis loop,

which in this quasi-static context is entirely driven by friction.

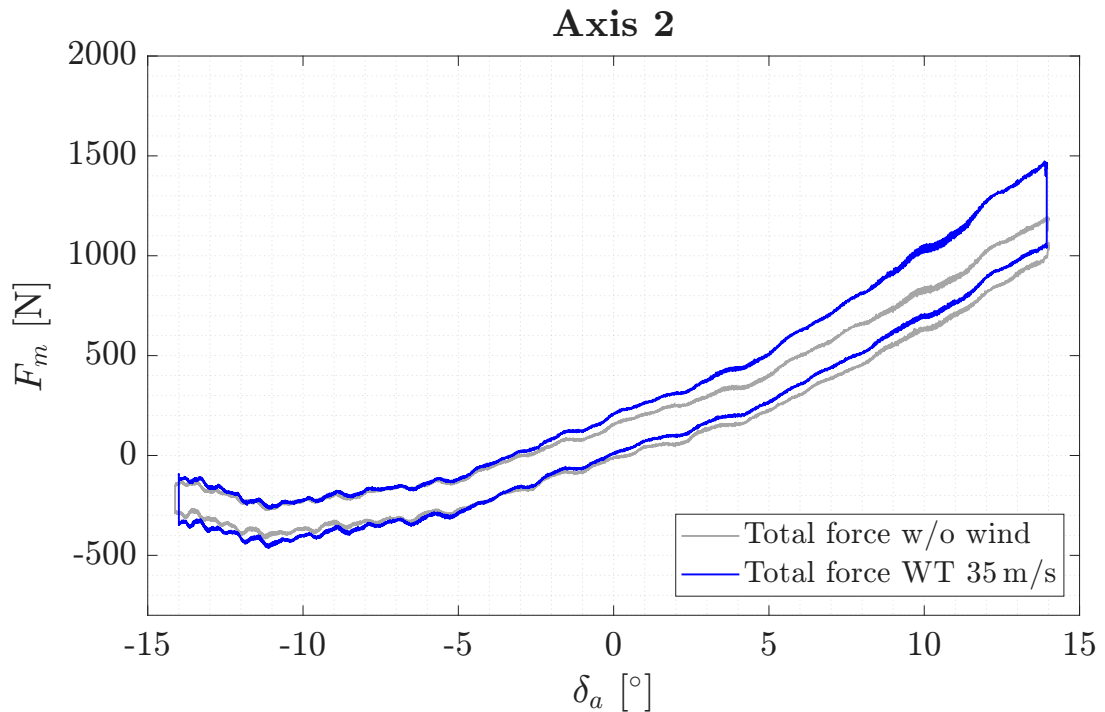
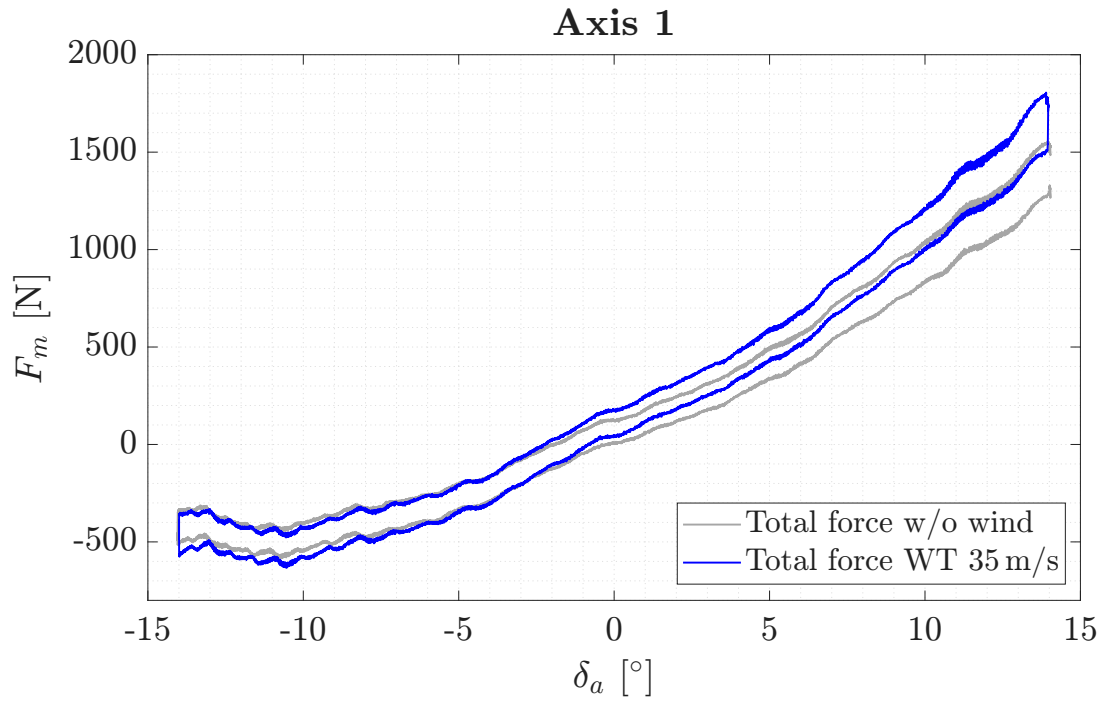


Figure 8.10: Comparison of the required actuation force during a complete quasi-static deflection cycle under wind-on and wind-off conditions for (a) Axis 1 and (b) Axis 2.

When applying the friction models developed in Chapter 5 to both actuation axes, an underestimation of the friction becomes apparent. Ideally, the hysteresis should vanish, with the forward and backward curves perfectly overlapping. However, the curves do not perfectly align, even around the zero-deflection region where the models were originally tuned. Figure 8.11 illustrates the total measured forces under the 35 m/s wind condition, alongside the total actuation force without the friction component.

This underestimation worsens near the kinematic limits, especially at maximum positive deflections. The physical reason behind this discrepancy lies in the formulation of the current friction models, which do not account for the additional external aerodynamic loads. These external loads inevitably increase the normal reaction forces acting on the sliding guides of the mechanism.

Therefore, to improve the accuracy of the friction estimation, the model should be updated to incorporate two key factors. Firstly, it must account for the variation in normal reaction forces based on the instantaneous position of the actuation system, an effect that is valid even in wind-off conditions. Secondly, it needs to include the proportional increase in these normal reactions induced by the external aerodynamic pressure distribution.

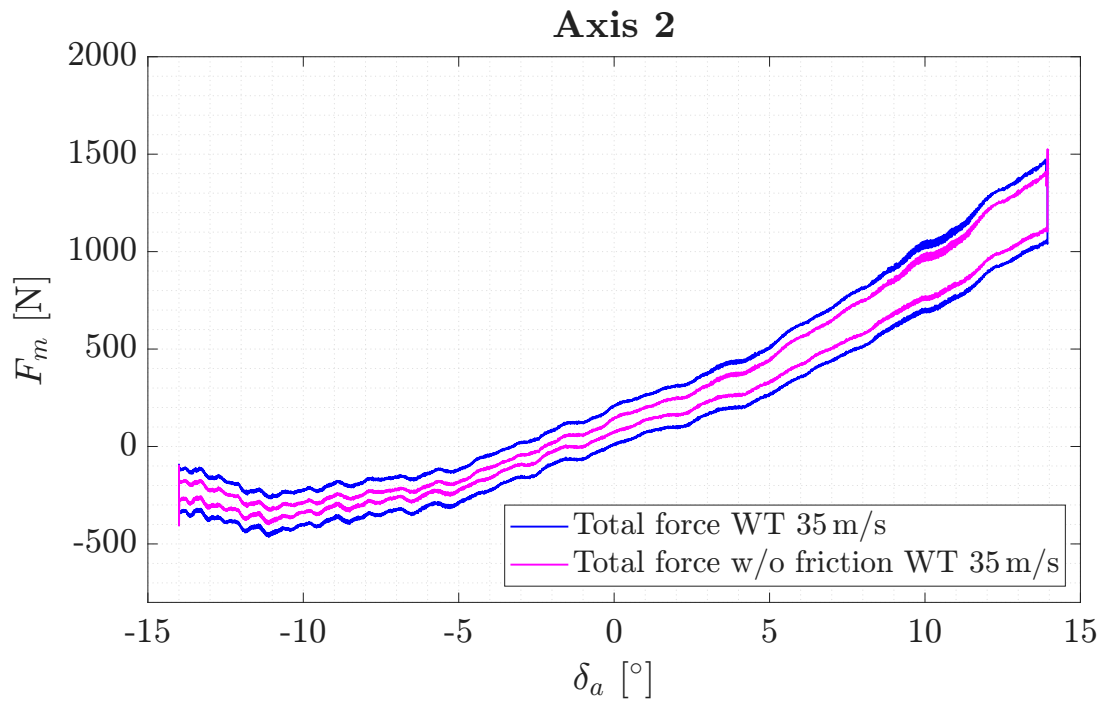
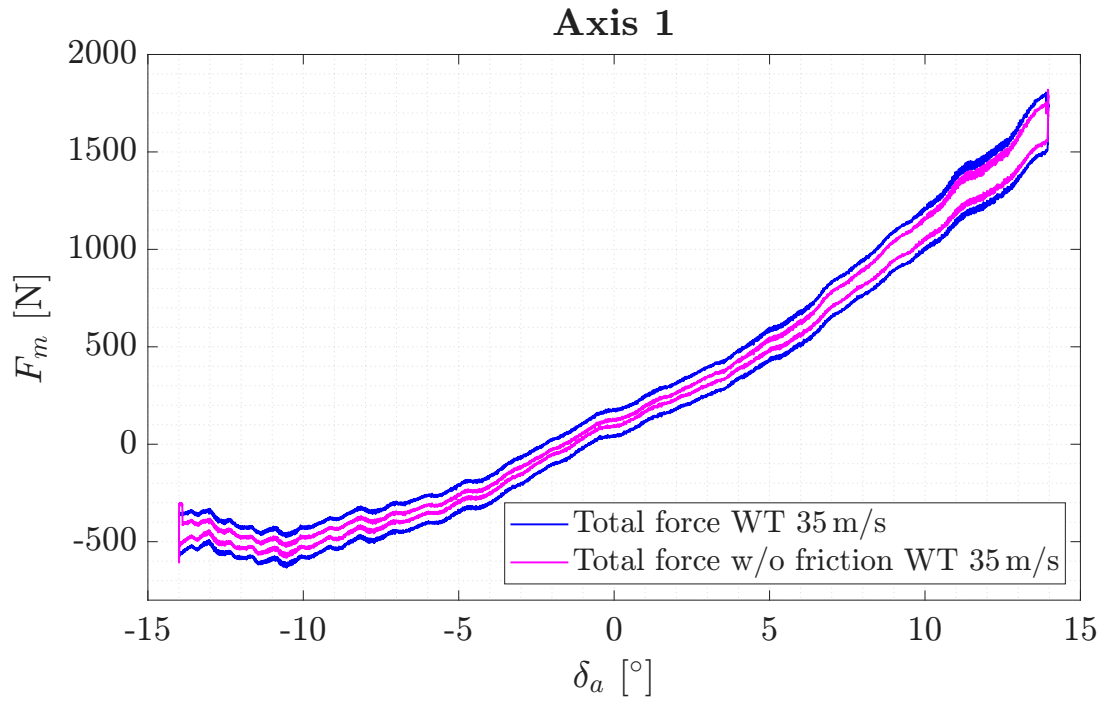


Figure 8.11: Total and friction-deducted actuation force under wind-on conditions, for (a) Axis 1 and (b) Axis 2.

8.5. Shape assessment: static retention and dynamic response

The Siemens DIC system enables a continuous three-dimensional reconstruction of the surface displacement. This specific data collection procedure was repeated for both the suction and pressure sides of the two ailerons across various deflection angles, angles of attack and wind tunnel velocities.

From a static perspective, these optical measurements are essential to evaluate physical shape retention under aerodynamic loads. The primary aim is to determine whether the loaded structural deformation deviates from the baseline wind-off shapes previously analysed in Section 6.2.1. Furthermore, the DIC system can track surface variations over time. This dynamic capability is crucial for wind-on dynamic acquisitions, enabling a detailed evaluation of the actuation bandwidth response under unsteady aerodynamic loading. This subsequently allows for a direct comparison with the wind-off dynamic baseline established in Chapter 7.

As an illustrative example of the shape-tracking capabilities, Figure 8.13 and Figure 8.12 present two data acquisitions performed using the DIC system in the absence of wind. Specifically, these frames capture the movements of the outer aileron (Axis 2).

In particular, Figure 8.12 and Figure 8.13 present contour plots of the out-of-plane displacement w for the upper and lower surfaces, calculated relative to the zero-deflection state. Specifically, the former illustrates the structural response when actuated from 0° to 14° , while the latter displays the deformation up to the minimum deflection of -14° .

During the acquisition phases, certain areas occasionally lacked colour data, although this issue is not present in the specific images shown here. This information loss occurred because surface reflections prevented the optical cameras from capturing the target pattern in those regions. Consequently, suitable interpolation techniques will be required during the data processing stage to mathematically reconstruct the missing surface geometry. Conversely, in instances where extensive areas remained uncaptured, it became necessary to recalibrate the system, reposition the lighting or cameras or adjust the lens apertures.

These images are included to anticipate the methodology that will be applied in the subsequent data processing phases. In particular, during wind tunnel tests, the wing is subjected to deformations. Torsional and out-of-plane bending affect these results. Since these specific displacements disturb the measurements by introducing the overall wing deformation, they must be mathematically removed. To achieve this objective, the

displacement of the rigid part of the ailerons, where they are attached to the wing box, must be evaluated and subtracted to isolate the pure aerodynamic deformation of the control surfaces.

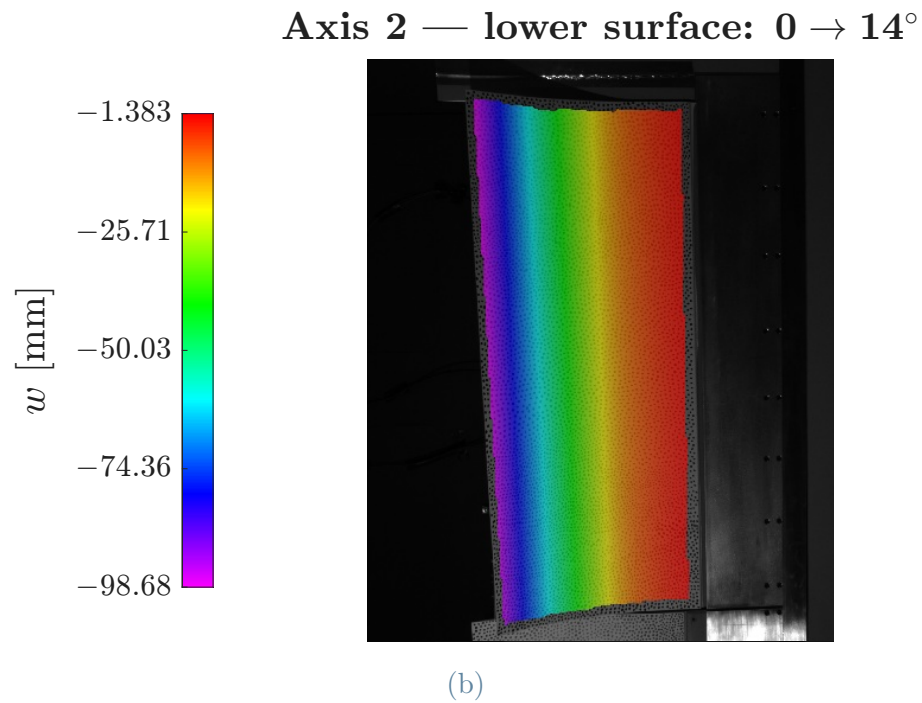
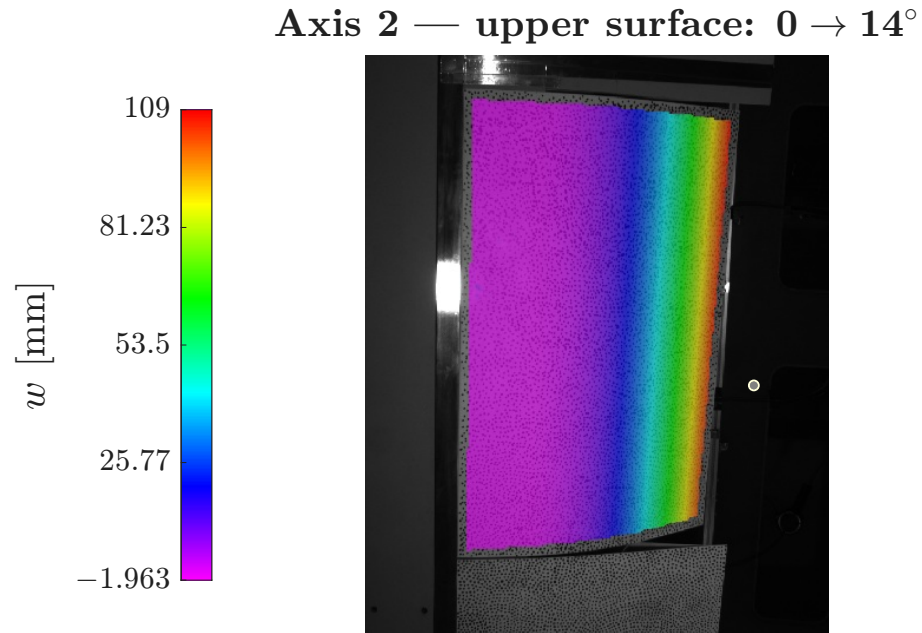
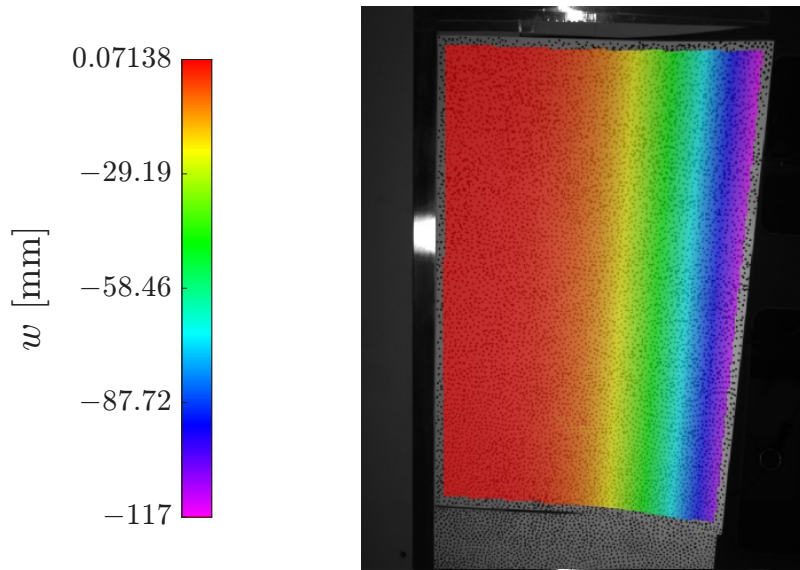


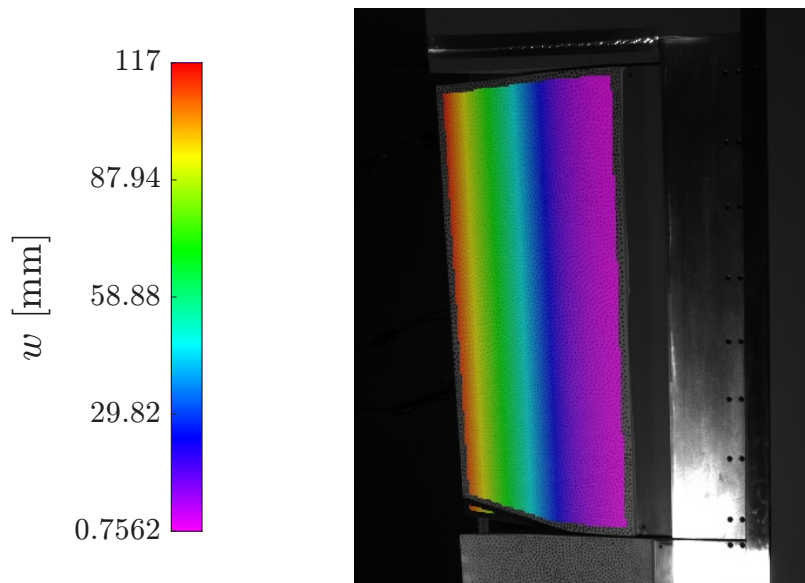
Figure 8.12: Contour plots of the out-of-plane displacement (w) under wind-off conditions at a positive deflection. (a) Upper surface and (b) lower surface.

Axis 2 — upper surface: $0 \rightarrow -14^\circ$



(a)

Axis 2 — lower surface: $0 \rightarrow -14^\circ$



(b)

Figure 8.13: Contour plots of the out-of-plane displacement (w) under wind-off conditions at a negative deflection. (a) Upper surface and (b) lower surface.

9 | Conclusions and future developments

The aerospace industry's transition towards zero-emission aviation demands new technological solutions. Morphing structures, capable of continuous and gapless shape adaptation, represent a promising avenue for reducing aerodynamic drag, improving flight efficiency, and enabling multi-functional control surfaces. Within the framework of the European HERWINGT project, this thesis supported the transition from computational design to physical implementation by validating and testing a full-scale morphing aileron demonstrator.

The comprehensive experimental ground and wind tunnel testing campaigns conducted on the physical prototype yielded several key achievements:

- **Successful hardware integration and control:** the assembly of the compliant S-glass fibre skin with the internal Scott-Russell kinematic mechanisms and the electromagnetic linear actuators proved mechanically robust. The position control architecture allowed for precise regulation of the structure, demonstrating the practical feasibility of the sliding skin concept.
- **Friction characterisation:** friction within the complex kinematic chain was identified as a relevant factor in the actuation effort. By employing a frequency-domain identification procedure, a Coulomb-viscous friction model was extracted. The development of this model served three fundamental purposes: configuring the motor controller's feed-forward pre-filters to improve dynamic tracking accuracy, enabling the validation of numerical predictions by mathematically removing the estimated friction from experimental force measurements, and providing realistic non-linear damping data to update future numerical models.
- **Geometric fidelity and kinematics:** static tests, conducted in wind-off conditions and supported by the Qualisys® motion capture system, confirmed that the manufactured prototype accurately reproduces the theoretical target shapes. The structure achieved deflections up to $\pm 14^\circ$ without geometrical discontinuities. Com-

pared to the initial numerical design, the maximum negative deflection was reduced by one degree, as the physical prototype exhibited a slightly higher stiffness than anticipated. Furthermore, the establishment of precise polynomial laws correlating the motor stroke to the trailing-edge rotation provided a closed-loop feedback mechanism for the user interface. The static actuation forces aligned closely with the numerical predictions, and the applied friction model performed effectively, particularly for small deflections around the neutral position ($\delta = 0^\circ$), whereas it slightly underestimated the actual values at extreme deflections.

- High-bandwidth dynamic performance:** the dynamic experimental campaign, conducted without aerodynamic loads, demonstrated the system's ability to operate effectively within the 0–10 Hz bandwidth required for advanced active control applications such as load alleviation. Over the investigated range of frequencies and amplitudes, the ailerons and the actuation system exhibited neither significant amplifications nor attenuations of the commanded amplitude, and no considerable phase lags were observed. Moreover, the experimental data confirmed the numerical predictions regarding structural resonance: by exciting the system near its first natural frequency, which was experimentally identified for the physical prototype to be approximately 5.4 Hz, the required dynamic actuation forces were lower than the static holding forces. Although the structural anti-resonance was initially masked by internal friction, the application of the developed compensation model successfully isolated the phenomenon, clearly revealing the expected dynamic behaviour. This proves that the strategy of designing a resonant compliant structure effectively reduces the actuation forces in dynamic applications. However, to avoid offsetting the dynamic advantages of the resonant system, it is necessary to account for and mitigate these dissipative forces during the design phase.
- Wind tunnel performance evaluation:** the preliminary wind tunnel tests successfully established the aerodynamic baseline of the prototype, extracting lift, drag and polar curves that will serve as a robust experimental dataset for future numerical-experimental correlations. Furthermore, quasi-static testing revealed that external aerodynamic pressure distributions not only increase the required actuation effort but also amplify the internal structural friction. This finding highlighted the need to update the existing theoretical friction model to account for load-dependent normal reactions on the kinematic guides, which vary based on both the motor position and the applied external aerodynamic forces. Finally, the integration of a Siemens DIC optical system demonstrated its capability to continuously track surface deformations, setting the stage for evaluating not only the shape retention

but also the prototype's ability to accurately follow the target command even under external aerodynamic loads.

In conclusion, the ground tests validated the structural integrity, the kinematic accuracy, and the dynamic capability of the morphing aileron under wind-off conditions. Building upon this foundation, the wind tunnel tests extended the investigation to the aerostuctural domain, quantifying the physical impact of external aerodynamic loads on the required actuation forces.

These achievements confirm the feasibility of manufacturing a robust physical prototype, providing the essential experimental validation to support numerical studies, which predicted the morphing aileron's potential to outperform conventional hinged surfaces. Consequently, future developments will focus on multiple fronts. A primary objective will be the detailed analysis of the shapes acquired both statically and dynamically in the wind tunnel through the DIC system. The wing-box structural deformation must be decoupled and removed from these measurements to accurately isolate and understand the impact of external aerodynamic loads on the aileron. Furthermore, the extensive dataset collected during this experimental campaign will be leveraged to develop a comprehensive numerical-experimental correlation. To accurately support this correlation, the theoretical friction model will be corrected to account for the aforementioned load-dependent normal reactions. Together, these future steps will definitively verify the viability of adopting morphing ailerons as an efficient alternative to conventional control surfaces for next-generation regional aircraft.

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A | Appendix A: HMI data extraction and pre-processing

To streamline the post-processing phase, a custom data extraction routine was developed in MATLAB®. This script is specifically designed to parse the raw text files generated by the HMI of the two actuation systems, extracting critical operational variables such as motor force, encoder-measured position, velocity, and acceleration.

The script incorporates several data-handling features:

- Data cleaning and resampling: the routine automatically identifies and removes duplicate data entries or timestamps caused by potential data acquisition jitter. Furthermore, because raw data may not always be logged at perfectly uniform time intervals, a spline interpolation option is included. This allows the dataset to be accurately resampled to match the theoretical temporal acquisition frequency of the LinMot® system (developed by BLU TECHNOLOGY s.r.l.), which is 250 Hz.
- Time windowing: users can specify exact temporal limits to crop the dataset, isolating specific conditions for targeted analysis.
- Kinematic recalculation: the script provides an option to dynamically recalculate the actual (δ_a) and demand (δ_d) equivalent deflection angles directly from the measured (x_a) and target (x_d) linear motor positions. This feature proved essential during the experimental campaign; when internal components of the actuation mechanism were modified, subsequently altering the kinematic relationship between the motor stroke and the aileron rotation, this function allowed the updated inverse laws to be applied retroactively to the raw positional data.
- Structured output: finally, all extracted and processed variables are compiled into a single, organised MATLAB® **struct**. This architecture facilitates data handling, plotting, and further numerical evaluation.

```

1 function data = exid(base_filepath, axis, options)
2     % Updated version as of 22/05/2026
3     %
4     % exid: extract_interface_data.
5     %
6     % Extracts, cleans, crops, and interpolates interface data.
7     %
8     % Syntax:
9     %     data = exid(base_filepath, axis)
10    %     data = exid(..., 'OptionName', Value)
11    %
12    % Mandatory Inputs:
13    %     base_filepath - String. File path without extension
14    %                     (e.g., 'Data/0_14').
15    %     axis          - Integer (1 or 2). Determines the extension
16    %                     to load (.ax1 or .ax2).
17    %
18    % Options (Name-Value Pairs):
19    %     'time_window' - Vector [t_start, t_end] in seconds to crop
20    %                     the data.
21    %                     Default: [] (No time cropping).
22    %                     To select from the beginning up to x seconds:
23    %                     [0, x].
24    %                     To select from x seconds to the end: [x, inf].
25    %
26    %     'interp'       - Boolean (true/false). If true, interpolates
27    %                     the data to a constant dt of 0.004 s.
28    %                     Default: false.
29    %
30    %     'mm_grad_low'  - Boolean (true/false). If true, ignores the
31    %                     degrees recorded by the interface and
32    %                     recalculates actual_rot and demand_rot
33    %                     from the mm (actual_us, demand_us) using
34    %                     polynomial laws.
35    %                     Default: false.
36    %
37    % Output:
38    %     data           - Structure containing the processed time and
39    %                     kinematic vectors.
40
41    %% 1. Arguments definition
42    arguments
43        base_filepath    % (MANDATORY) Base file path
44        axis              % (MANDATORY) 1 or 2

```

```

45
46     % Optional Name-Value arguments
47     options.time_window = []      % Default: no time cropping
48     options.interp = false        % Default: no interpolation
49     options.mm_grad_law = false   % Default: uses interface degrees
50 end
51
52 %% 2. Axis check
53 if axis == 1
54     ext = '.ax1';
55 elseif axis == 2
56     ext = '.ax2';
57 else
58     error('Error: the specified axis is invalid. Choose 1 or 2.');
```

59 end

```

60
61 filepath = strcat(base_filepath, ext);
62
63 %% 3. Raw data reading
64 Q = readtable(filepath, 'Delimiter', ';', 'DecimalSeparator', ',',
65     'FileType', 'text');
```

65

```

66 t = Q.Date_and_Time / 1000;
67 t = t - t(1);
68
69 demand_us = Q.DemandPosition_mm_;
70 actual_us = Q.ActualPosition_mm_;
71 demand_rot = Q.DemandRotation___;
72 actual_rot = Q.ActualRotation___;
73 demand_v = Q.DemandVelocity_mm_s_;
74 actual_v = Q.ActualVelocity_mm_s_;
75 force = Q.ActualForce_N_;
76
77 %% 4. Duplicate time cleaning (jitter handling)
78 % Saving initial indices
79 idx_all = (1:length(t))';
80
81 % Finding unique time indices, keeping only the first occurrence
82 [~, idx_unique] = unique(t, 'first');
```

83

```

84 % unique discards the repetition indices
85 idx_rep = setdiff(idx_all, idx_unique);
86
87
```

```

88 % Removing duplicates across all fields
89 t = t(idx_unique);
90 demand_us = demand_us(idx_unique);
91 actual_us = actual_us(idx_unique);
92 demand_rot = demand_rot(idx_unique);
93 actual_rot = actual_rot(idx_unique);
94 demand_v = demand_v(idx_unique);
95 actual_v = actual_v(idx_unique);
96 force = force(idx_unique);
97
98 %% 5. Time cropping
99 if ~isempty(options.time_window) && length(options.time_window) ==
100     2
101     t_start = options.time_window(1);
102     t_end = options.time_window(2);
103
104     mask = (t >= t_start) & (t <= t_end);
105
106     t = t(mask) - t_start;
107     demand_us = demand_us(mask);
108     actual_us = actual_us(mask);
109     demand_rot = demand_rot(mask);
110     actual_rot = actual_rot(mask);
111     demand_v = demand_v(mask);
112     actual_v = actual_v(mask);
113     force = force(mask);
114 end
115
116 %% 6. Rotation recalculation via inverse polynomial laws
117 if options.mm_grad_low
118     if axis == 1
119         law_coeffs = [0.000002451148208, 0.001208393866871,
120             0.240808027611052, -0.040771595229629];
121     elseif axis == 2
122         law_coeffs = [0.000002393019881, 0.001251622845691,
123             0.243646794184750, -0.011359071732550];
124     end
125
126     demand_rot = polyval(law_coeffs, demand_us);
127     actual_rot = polyval(law_coeffs, actual_us);
128 end

```

```

129     %% 7. Constant dt interpolation (resampling)
130     if options.interp
131         dt = 0.004;
132         t_new = (t(1) : dt : t(end))';
133
134         demand_us = interp1(t, demand_us, t_new, 'spline');
135         actual_us = interp1(t, actual_us, t_new, 'spline');
136         demand_rot = interp1(t, demand_rot, t_new, 'spline');
137         actual_rot = interp1(t, actual_rot, t_new, 'spline');
138         demand_v = interp1(t, demand_v, t_new, 'spline');
139         actual_v = interp1(t, actual_v, t_new, 'spline');
140         force = interp1(t, force, t_new, 'spline');
141
142         t = t_new;
143     else
144         dt = mean(diff(t));
145     end
146
147     %% 8. Friction coefficients
148     switch axis
149     case 1
150         % Axis 1
151         Fv = 0.0698; % N/(mm/s)
152         Fc = 49.9748; % N
153     case 2
154         % Axis 2
155         Fv = 0.0781; % N/(mm/s)
156         Fc = 63.5011; % N
157     end
158
159     %% 9. Saving to output structure
160     data.t = t;
161     data.demand_us = demand_us;
162     data.actual_us = actual_us;
163     data.demand_rot = demand_rot;
164     data.actual_rot = actual_rot;
165     data.demand_v = demand_v;
166     data.actual_v = actual_v;
167     data.force = force;
168
169     data.dt = dt;
170     data.fs = 1 / dt;
171     data.axis = axis;
172

```

```
173     data.Fv = Fv;  
174     data.Fc = Fc;  
175     data.actual_acc = gradient(data.actual_v, dt);  
176  
177     % Saving the list of all removed duplicate row indices  
178     data.idx_all_repeats = idx_rep;  
179 end
```