

Article

Scaling Effects on Morphing Structures: Preliminary Guidelines for Managing the Effects on a Case Study

Antonio Concilio , Bernardino Galasso  and Salvatore Ameduri

CIRA, Italian Aerospace Research Centre, Via Maiorise, 81043 Capua, Italy; b.galasso@cira.it (B.G.); s.ameduri@cira.it (S.A.)

* Correspondence: a.concilio@cira.it

Abstract: The technique of morphing in aerospace engineering is a relatively new discipline targeting the improvement of aircraft performance, even through dramatic changes to some critical geometrical and mechanical features, to adapt aircrafts' configurations to evolving operation conditions. The development path of morphing systems is complex and shall pass through articulated gates to prove its readiness level due to the concurrence of different disciplines and approaches. The characterization and demonstration of the concepts in a representative environment, such as wind tunnel test facilities, are some of the most relevant steps needed for the maturation of the engineering technique. The practical size limitations of test facilities usually impose the use of scaled models. In the case of morphing systems, whose architecture is strictly dependent on the available room, and whose performance is tightly correlated with the general structural stiffness, changes in dimensions may affect the overall behaviour significantly. Therefore, the adaptive design may change a lot until it arrives to the formation of completely different products. Transportability issues of certain architectural forms, as well as the different classes of vehicles, are also related to that aspect. The scope of this paper is to investigate the impact of some effects of scaling processes on certain features of a morphing system, particularly focusing on the stiffness parameters, for their impact on several features such as the load bearing capability and structural stability in both steady and dynamic conditions. As a case study, a rotorcraft blade segment integrated with torsional shape memory alloy (SMA) actuators was considered. Relevant numerical models were exploited to highlight the different evolution laws of the characteristic structural parameters vs. the referred scale factors. In this investigation, the axial, flap, lag bending, and torsion stiffnesses, as well as normal modes and stress levels, are considered. The achieved results confirm the complexity of attaining an effective reproduction of the targeted morphing architecture, as scaled configurations are considered. In spite of the unavoidable specificity of the analysis herein reported, it is believed that such attainments can have a general validity at least to some extent, and the outcomes may be exported to other morphing systems, at least as guidelines. This study took place within the European project SABRE (Shape Adaptive Blades for Rotorcraft Efficiency, H2020).

Keywords: scaling effects; structural morphing systems; shape memory alloys; adaptive structures; integrated actuators; geometrical layout; mechanical configuration; rotorcraft blade



Citation: Concilio, A.; Galasso, B.; Ameduri, S. Scaling Effects on Morphing Structures: Preliminary Guidelines for Managing the Effects on a Case Study. *Actuators* **2023**, *12*, 366. <https://doi.org/10.3390/act12100366>

Academic Editors: Todor Stoilov Todorov, Rosen Mitrev and Wei Min Huang

Received: 25 July 2023

Revised: 11 September 2023

Accepted: 17 September 2023

Published: 22 September 2023



Copyright: © 2023 by the authors. Licensee MDPI, Basel, Switzerland. This article is an open access article distributed under the terms and conditions of the Creative Commons Attribution (CC BY) license (<https://creativecommons.org/licenses/by/4.0/>).

1. Introduction

Nowadays, the aerospace industry is being asked to face new challenges, strictly related to its environmental impact, the safety of flights, and survival within a competitive market [1]. Such a situation has become even more impellent amid the COVID-19 emergency and its effects [2]. In fact, the significant reduction in passenger traffic during the pandemic, on the one hand, and the need for specific assistance and prevention strategies, on the other hand, had a dramatic impact on the aerospace industry, enforcing more than ever the need to optimise every process, as well as relevant technologies, to better face challenging situations such as this.

The impact of air transportation on the environment and, in general, its contribution to pollution are widely documented [1,3]. The dramatic changes in the climate, on the one side, and the expected return to the usual growth of the aeronautic civil sector in the post-emergency phase, on the other side, require solutions and strategies that are more efficient than ever, involving both the aircraft and the supporting infrastructure systems (airports, connections, anti-terrorist measures and regulations, weather forecast systems, and so on) [4]. This aspect shall be managed by considering the foreseen increase in flown passengers in the next/medium period, related to both the estimated growth of pre-existing market players and the entry of new competitors [1]. Within this context, it is foreseen that these changes will lead to a gradual shift in the market's centre from the US to Eastern Asia (India, China, Japan, Indonesia) [5].

In this competitive scenario, a critical role can be played by technologies with a relevant impact on aircraft efficiency [6]. Among others, the scientific and technological community looks at morphing and laminar flow with increasing interest due to the envisaged benefits in terms of fuel consumption, manoeuvrability, and structural safety [7], and the appealing potentialities for the emerging full-electric architectures. On those topics, many research programs have been funded in recent decades. At the European level, some projects have targeted the enhancement of aircraft and rotorcraft efficiency. Recently, the Clean Sky 1 and 2 programmes focused on adaptive solutions for the enhanced performance of regional vectors [8,9]; in the FP7, SARISTU (Smart Intelligent Aircraft Structures) [10,11] and SADE (Smart High Lift Devices for Next Generation Wings) [12,13] were aimed at developing and testing the morphing and laminar flow concepts for SMR airplanes. SABRE (Adaptive Blades for Rotorcraft Efficiency) [14] and FRIENDCOPTER [15] focused on solutions for adapting blade geometry to extend and improve the helicopter flight envelope. Worldwide, MACW (Mission Adaptive Compliant Wing) [16] tested in-flight a wing integrated with a morphing flap on the NASA experimental aircraft Gulfstream III; the CLEEN project (Continuous Lower Energy, Emissions, and Noise Project) [17] concentrated on the upgrade of conventional systems to enhance aircrafts' environmental impact; MDO-505, a cooperation between Canadian and Italian companies, targeted the development of morphing aerofoils to improve wing efficiency [18]. Despite its short length, this list can allow for a perception of the attention that is deserved to adaptive and, specifically, morphing structures. At the end of the mentioned processes, suitable concepts were selected depending on a number of parameters, referring to macroscopic properties like reliability and manufacturing/integration viability. In the wake of this, the recent Airgreen2 (AG2) project within the Clean Sky 2 program has been recently preparing morphing and adaptive wing component demonstrators for in-flight testing, while full-scale experimental lab campaigns have been ongoing [19–21].

All of the above programs have drafted preliminary maturation paths for their relative technologies, moving on the basis of industrial requirements. This is essential in the view of a well-assessed TRL path [22–24].

To support and enhance the readiness level of morphing technology, more and more experimental demonstrations have been organized, leading to the manufacturing of more and more complex mock-ups, with the final aim of seizing the real behaviour of the proposed system to the maximum extent. In ETRIOLLA (Experimental Transonic Investigations on Laminar Flow and Load Alleviation) [25], a flexible scaled model that was optimised to delay laminar flow degradation was tested in a wind tunnel facility. Similarly, NOVEMOR (Novel Air Vehicles Configurations: From Fluttering Wings to Morphing Flight) [26] investigated the behaviour of novel lifting concepts and morphing wing solutions in significant wind tunnel environments.

In the reported investigations, a critical point was represented by the scalability of the proposed architectures. Former works did investigate this aspect, for instance, demonstrating the difficulties that should have been faced to try to use the same system on different classes of vehicles, or to design representative tests based on scaled prototypes. In [27], the difficulty of exploiting a morphing architecture from a small to a larger UAV

was emphasized, concluding that compliant mechanisms should be tailored for specific vehicles. In [28,29], the authors of these papers preliminarily drafted the effects of the geometrical scale factor on a simple but representative aircraft adaptive structural element.

In morphing systems, scalability issues impact both the structural skeleton, in terms of its capability of absorbing loads while withstanding large deformations, and the other main subsystems, namely the actuation and sensor networks, as well as the control architecture. As discussed in [30], for instance, actuators require specific sizing procedures on the basis of their typology, with the geometry being only one of the aspects needing to be considered. Energy density, weight per volume unit, the transmission of forces and displacements, and even the internal layout and its conceptual definition (f.i., kinematic or solid-state) each have a specific role in the design and may represent constraints to the resizing process. The specific choice of the actuator type then becomes hardly exportable to applications that are similar but different in size. Generally, hydraulic actuators are used for medium–large appliances for high energy density characteristics [31], but their complexity makes extreme miniaturization processes difficult. For the same reason, piezoelectric actuators find their place within small–medium devices; in this latter case, it is worth mentioning that several studies are being carried out to expand their applicability domain [32]. Similarly, high-energy smart actuators like SMA-based ones, which are, in principle, suitable for large-size mechanics, find limitations based on their typical thermal inertia, which is strongly dependent on their size [33].

Shape memory alloys have the unique peculiarity of being able to exhibit a remarkable strain recovery characteristic by changing their intimate molecular structures between two phases, the so called martensite and austenite phases. The two states are, in fact, associated with different volumes, so that one (the austenite phase) is more compact than the other (the martensite phase). Such different volumes are small (i.e., not able to cause significant density variations), but nevertheless enough to produce significant engineering deformations, attaining several percent units. These values, associated with high stiffness coefficients that are characteristic of standard metals, enable the onset of significant forces, which can be used to modify the shape of a target structure. The transformation at the base of this phenomenon is driven by both temperature and stress variations. In synthesis, it can be stated that higher temperatures are needed at higher stress values, as well higher stresses being needed at higher temperatures. This fact may be intuitively accepted, as higher energy levels within the system require equivalent higher levels of input energy to attain modifications. The key aspect of shape memory alloys' working mechanisms is that the two states are complementary: austenite does exist at higher temperatures but low stress levels, while the opposite happens for martensite. Cyclic transformations are possible by varying the temperature or the structural tension, alternatively. For a given SMA, if the external temperature is sufficiently high, the phases change naturally by simply applying and removing an external load, in turn generating the necessary stress levels. In this case, it is spoken of superelasticity (or pseudoelasticity, as the process is non-linear), since the material is able to recover huge strains. If the temperature is not high enough, after a cyclical loading process, some residual strain is found, which can be recovered by increasing the temperature sufficiently. In this case, the occurrence is reported as the shape memory effect, since the material seems to remember its previous state by absorbing a sufficient amount of energy. These few lines try to summarise, without any pretence of a strict engineering rigour, the magnificent property of shape memory alloys. Many details can be found in the literature within many dedicated articles and textbooks [34,35]. In this article, shape recovery is mainly exploited by moving the material to a sufficient stress level, and it in turn being able to reverse autonomously as the thermal load is moved away; in other words, cyclicity is driven by temperature levels and not stress.

In the field of the actuation of morphing through shape memory alloy technology, other relevant projects may be also cited, such as SMyLE (Shape Memory Alloy Leading Edge) [36,37], aimed at developing and testing technologies for advanced droop nose concepts with de-icing capabilities, as well as SMyTE (Shape Memory Alloy Trailing Edge),

which focused on the design, manufacturing, and testing of representative aero-structural components [38,39], targeting the development of camber morphing trailing edge concepts.

As already mentioned, the scalability finds applications for demonstration activities, that is to say, when a system has to be tested in facilities whose restrictions impose the use of scaled models. In line with this, in [40], the authors developed a morphing radiator system based on SMA technology, whose logic of control was tested on a scaled model. Different works can then be cited, addressing the impact of geometrical scaling on the actuation. One recalls the study on bending active structures [41], in which the impact of the main geometric parameters of the proposed hinges is discussed; the work [42] in which the authors discuss the scaling performance of a cell structure in terms of the transmitted displacement, load absorption, and weight impact; and the work [43] in which the authors address the problem of the dependence of a morphing system on the scale factor through a surrogate model. In [44], the authors highlight the impact of scalability on the industrialization and commercialization of morphing systems, implementing a non-dimensional analysis to quantify the impact of the scalability; finally, in [45], the authors focus their attention on the scale of morphing bioinspired wings.

In the same way, integrated sensors present their own scalability issues [46]. In the case of strain contact sensors, their size may alter the measure if the reference structure is small and soft. On the other side, a miniaturization process may be in conflict with wireless or installation requirements. Currently, the problem has been moved to the network level, so as to overcome possible limitations associated with the single sensible element [47]. Pressure and temperature sensors must meet specific requirements to describe the aerothermodynamic field around a typical wing, as the observation moves from full-scale to small WT models. If the selected sensor technology does not allow for direct size reductions, external post-processing approaches are introduced, involving the whole array, for instance, using dedicated interpolation and field reconstruction methods [48]. The algorithm logic aimed at coordinating sensor data and actuators' actions may be also dramatically affected by the scaling process: changes in terms of mechanical and thermal inertia, different ranges of force and displacements, and so on, may require dramatic variations in the software parameters or the architecture itself, with an unavoidable impact on stability and the working domain of the referred system [49].

Withstanding all the considerations above, it is believed that a dedicated study on scaling issues may have a certain relevance to morphing applications; this is the scope of the present article. Since it may be very difficult to address this problem from a general point of view, and to deduce potential consequences without a concrete example to handle, a real adaptive architecture is taken as a reference, a sort of benchmark, to give concreteness to the formulated considerations.

The case study in this paper uses a numerical model of a full-scale morphing rotorcraft blade segment, developed and manufactured within the already mentioned European project SABRE, and validated in lab, wind tunnel, and whirl tower tests [50,51]. The adaptive system consists of a blade structure integrated with a spanwise SMA rod actuator, having the function of altering the twist and contributing to bearing the external and internal loads. The effect of the geometric scale factor is estimated by highlighting its impacts on the stiffness (in terms of axial, lag, flap bending, and torsion), the dynamic response (in terms of normal modes), and the aeroelastic properties (in terms of instability, damping capability, and normal modes of interaction). The adaptive system response is evaluated under the action of the aerodynamic, centrifugal, and internal loads. Non-linear solutions have been implemented to take in proper consideration the complexity of the referred architecture. The geometric scale factor is the sole factor to be considered in this work. Effects of other parameter variations, like the elastic properties, are drafted in the final paragraphs.

In spite of the unavoidable specificity of the analysis, conducted on a particular morphing system, it is believed that it can have a general validity, at least to some extent, and the main outcomes, as guidelines, may be exported to other generic configurations.

2. The Case Study: An Adaptive Twist Blade Model

The proposed adaptive blade model is based on SMA technology. It was developed within SABRE, an H2020 project, as an adapted version of the original architecture that has been experimentally validated [51]. Such a model is herein used as a reference base to evaluate the effects of geometrical scale on different parameters, such as the bending and the torque stiffnesses, the stress level, the actuation performance, and the dynamic stability.

Compared to conventional helicopter blades, the investigated architecture has a high complexity related to adaptivity features and the embodiment of innovative SMA actuators. Geometrical adaptivity poses the well-known paradox of a structure that is rigid enough to withstand the external loads while being adequately flexible to allow for the modification of its shape. In this case, warping is targeted along the entire span of the blade, attained by using pre-twisted shape memory alloy rods mounted along its overall length. By exploiting the possibility of recovering the strain through the heat-induced SMA phase transformation of martensite to austenite, torque is transmitted to the referred system; the return to the original state is ensured through the elastic force system.

Within SABRE, the following criteria were established for designing a full-size demonstrator, driven by the need to respond to wind tunnel and whirl tower specs.

- Structural integrity: a safety factor higher than two with respect to the ultimate material stress should have been guaranteed for the most severe load conditions on all of its parts, except for the SMA elements; the surrounding structure was then imposed to bear the operation loads without the cooperation of the shape memory alloy rods. The herein defined safety factor was computed with respect to the ultimate strength of the adopted materials, excluding the shape memory alloy;
- Morphing twist performance: the SMA torque actuators should have been designed to guarantee at least a 2 deg/m twist under the most severe load conditions;
- Shape preservation: in the same way as requested for the external loads, the actuator-induced twist should have preserved the original airfoil geometry; this requirement was quantified by assuming a maximum absolute shape deviation vs. the nominal one, smaller than the typical roughness of 350 μm , adopted for laminar wind tunnel tests [52].

The selected design criteria that have been exposed were constrained by the test facilities, whose availability was planned in the SABRE project, including a wind tunnel (WT) at the University of Bristol facility, and a whirl tower at the DLR, Braunschweig. The main scope of the WT campaign was to estimate the impact of the additional twist on the aerodynamic performance. In this sense, the capability of the proposed system of actuating the prescribed twist while keeping the external airfoil shape is fundamental. The main target of the whirl tower test campaign was instead to estimate the impact of inertial and aerodynamic forces on the proposed system's functionality. To achieve this target, an architecture whose behaviour was representative of that of a real helicopter blade was required. For this, the stiffness plays a critical role, both in terms of achieving the desired shapes under the simultaneous actions of both the SMA actuator and the external load, and guaranteeing the structural integrity. Additional criteria were also considered, related to safety aspects:

- The maximum SMA temperature was set to never exceed 180 $^{\circ}\text{C}$, to avoid any property degradation or the onset of damage to the surrounding components; this requirement, which is well over the operational temperature considered in the next sections (never surpassing 100 $^{\circ}\text{C}$), was met through a dedicated temperature control logic and, for safety reasons, through the implementation of thermal fuses aimed at interrupting the heating supply circuit if a certain threshold should have been overcome.
- The structural safety factor was set to a minimum value of two for all parts of the demonstrator except for the SMA rods. In the latter case, in fact, to produce enough martensite exploitable for actuation, an adequate pre-load should have been provided. This exception was possible because of the location of the SMA components that, being embedded within the main structure, prevented any projection of possible debris during the tests following the crisis of those specific components.

- The mass layout was designed so that its centre of gravity fell within the first quarter of the chord to ensure aeroelastic stability; even though this requirement was met by the adopted layout, further balancing masses could have been used to shift the barycentre further forward.

In detail, the model represents a segment of the Bo 105 blade, implementing a NACA 23012 airfoil cross-section [53]. In the representation adopted in this paper, it is made of two bays for a 1000 mm approximate total span, and a 270 mm chord (Figure 1). Each bay houses a single SMA rod that is 500 mm long; a two-bay configuration is, in fact, preferred to avoid transversal bending instability during actuation. In the same figure, the reference frame is indicated, highlighting the axes referring to lag and flap bending, and torsion rotations, respectively. The main features of the materials considered in this study, including the SMA, are summarised in Table 1.

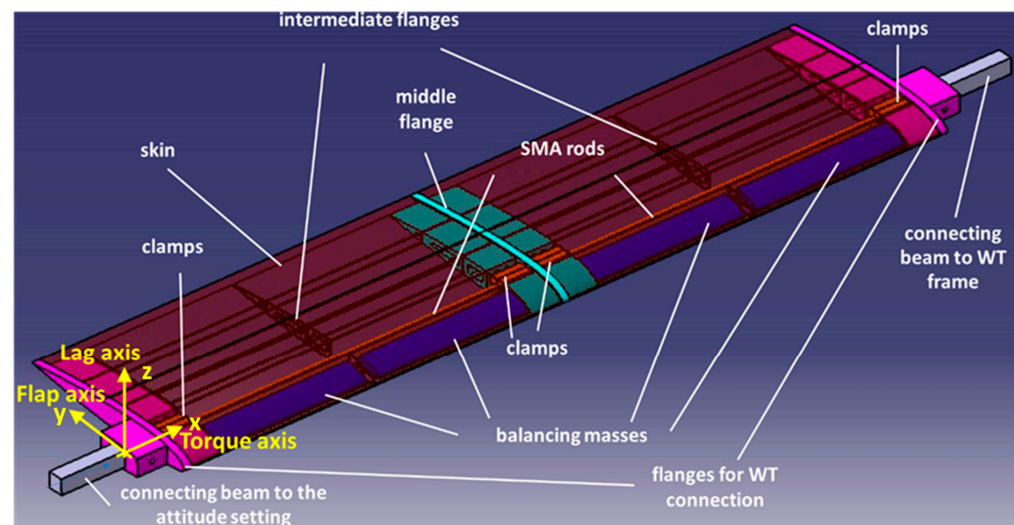


Figure 1. CAD model of the scaled demonstrator and reference frame.

Table 1. Main properties of the used materials.

Alloy Type and Composition	NiTiNi	Ni—ca. 55% Ti—Balance Other Elements *—<0.05% * Fe, C, O, Co, Cr, Ni, H, N
SMA (actuator) (data measured via internal tests)	Austenite Young modulus, E_a , (GPa)	38.8
	Martensite Young modulus, E_m , (GPa)	28.8
	Poisson ratio	0.33
	Martensite start transformation stress, σ_s^{AS} (MPa)	186.0
	Martensite finish transformation stress, σ_f^{AS} (MPa)	348.0
	Austenite start transformation stress, σ_s^{SA} (MPa)	168.0
	Austenite finish transformation stress, σ_f^{SA} (MPa)	6.0
	Martensite start transformation temp., M_s , (°C)	42.0
	Martensite finish transformation temp., M_s , (°C)	15.0
	Austenite start transformation temp., A_s , (°C)	45.0
	Austenite finish transformation temp., M_s , (°C)	72.0
	Stress/temperature ratio (MPa/°K)	6.0
	Density (kg/m ³)	6450.0
	Ultimate stress (MPa)	500.0

Table 1. Cont.

Alloy Type and Composition	NiTiNol	Ni—ca. 55% Ti—Balance Other Elements *—<0.05% * Fe, C, O, Co, Cr, Ni, H, N
7075-T6 Aluminium alloy for middle and side flanges	Young modulus (GPa)	71.0
	Poisson ratio	0.32
	Damping factor (%)	2.0
	Density (kg/m ³)	2750.0
	Ultimate stress (MPa)	572.0
310 AISI Stainless steel alloy for clamps and connecting beam	Young modulus (GPa)	210.0
	Poisson ratio	0.32
	Damping factor (%)	2.0
	Density (kg/m ³)	7900.0
	Ultimate stress (MPa)	655.0
PTFE used for the skin and the middle ribs	Young modulus (MPa)	540.0
	Poisson ratio	0.42
	Damping factor (%)	22.4
	Density (kg/m ³)	2240.0
	Ultimate stress (MPa)	25.0

Lead balancing masses are deployed on the tip (leading edge) to ensure that the centre of gravity is located within the first chord quarter, to prevent basic aeroelastic instability [54]. The SMA rods have a 10 mm diameter circular cross-section, except for at the edges, where they are square-shaped to assure an adequate twist transmission to the blade structure. The relative interfaces are square clamps made of steel (Figure 2a). Eight bolts are implemented to tighten the grips, and to connect the device to the global system. The two demonstrator bays are joined with an aluminium rib (Figure 2b). This component underwent milling operations to keep its weight at the minimum in order to preserve the established centre of gravity location and to create adequate room for hosting the skin, the clamps, and the balancing masses. Similar operations are conducted on the lateral ribs, which have the further role of guaranteeing a proper connection to the wind tunnel facility.

The skin is made of PVDF (polyvinylidene fluoride). The use of this material is a good compromise between the need for securing adequate structural integrity and twist deformability. To this latter scope, each bay is further integrated with an intermediate flange and four webs, with the same thickness as the skin, and placed along the chord at the rib openings, positioned forward and behind the SMA clamp assembly, as shown in the schematic reported in Figure 2b. As illustrated in Figure 3d, the two backwards webs have the same thickness of skin, while the other two in contact with the clamp are thicker to better transmit the twist actions from the SMA rods.

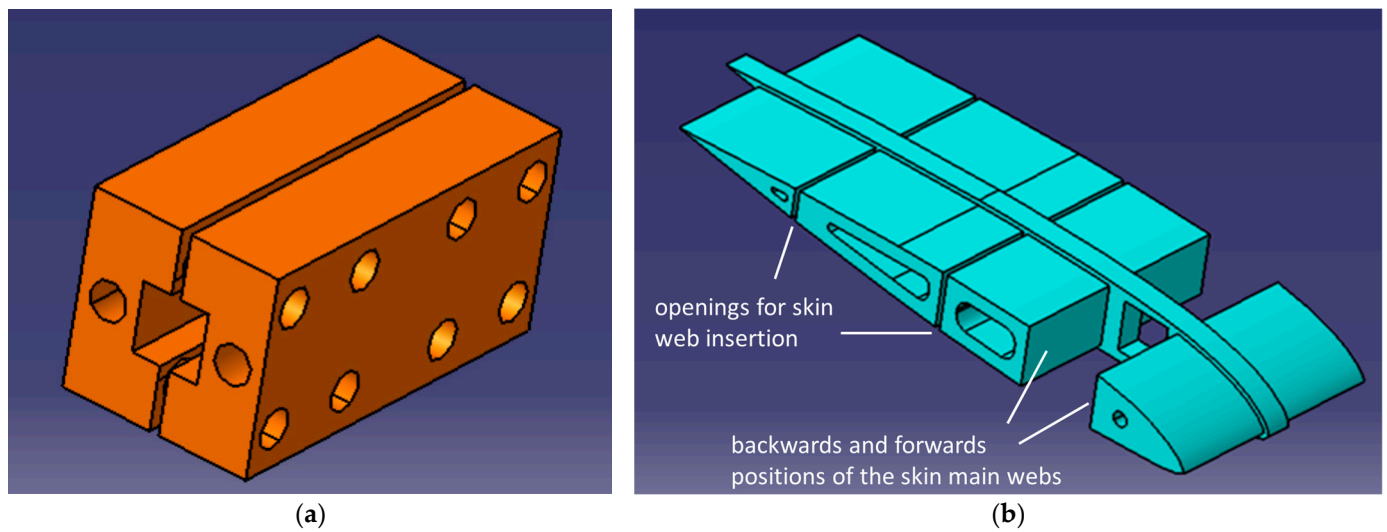


Figure 2. Clamp hosting the SMA (a), and central rib connecting the two bays with locations for web insertion (b).

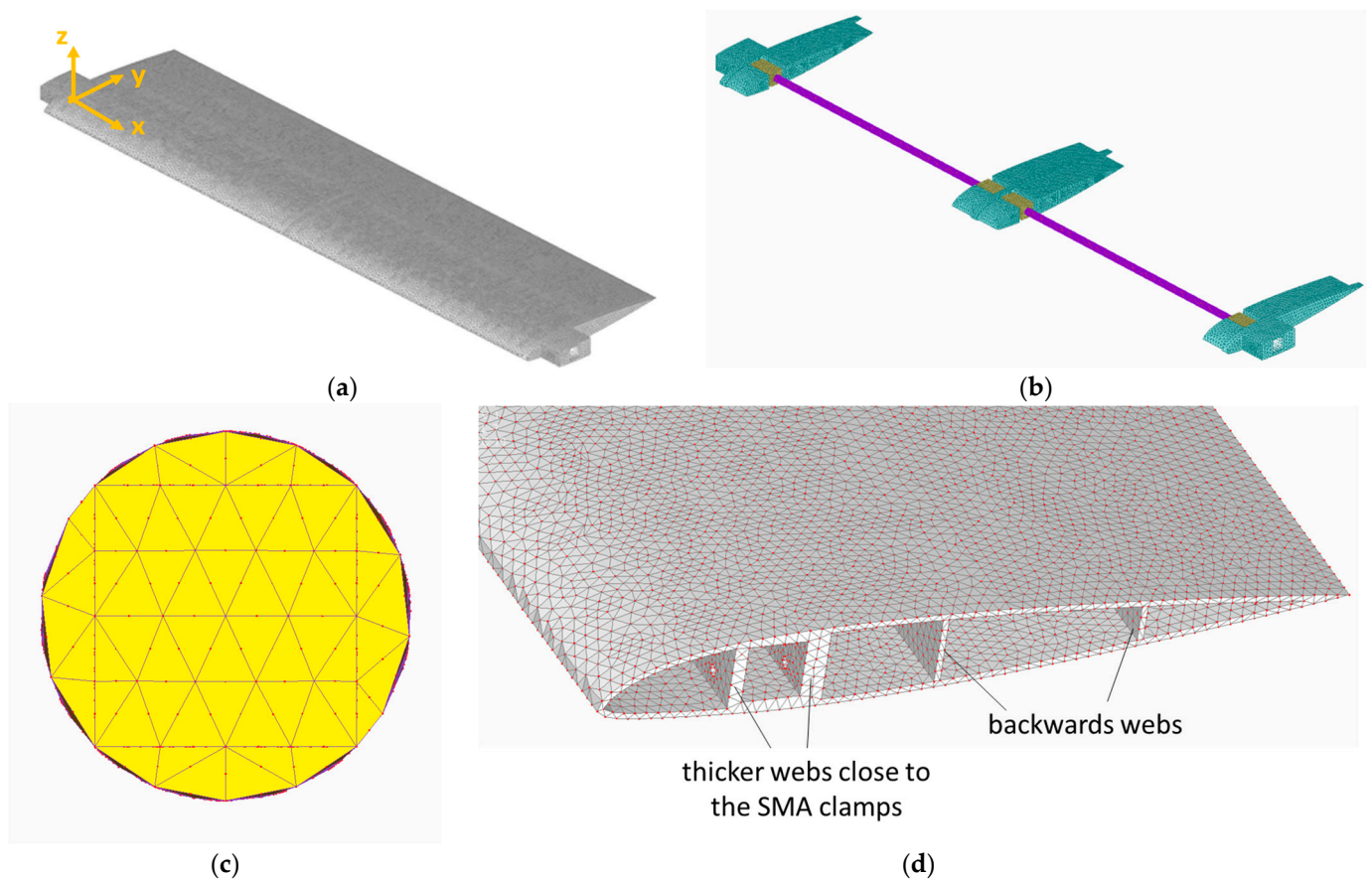


Figure 3. FE model (a), main details from its interior layout (b), SMA cross-section, (c) and skin mesh (d).

3. FE Model Development

As previously stated, the FE models used to represent this two-bay blade configuration are derived from the ones assessed and validated in SABRE. The MSC Nastran solver code [55] was used to address the numerical investigations presented in the next sections, while the FEMAP pre- and post-processor [56] was exploited to build the model and present the numerical outcomes. Four different versions were prepared to reproduce a set of linear geometrical scale factors, sc , namely equal to 0.5, 1, 1.5, and 2. The value of

1 refers to the nominal size. A view of the FE model and of its interior is reported in Figure 3. Three-dimensional elements are used to represent the structural parts, for a total of 426,528 tetrahedrons and 214,516 nodes. The size of the mesh was driven by practical considerations. First, since the phase transformation of the SMA rods was addressed, aimed at torsion generation, the discretization shown in Figure 3c was adopted for the actuator cross-sections. Parabolic tetrahedral elements were used to have an adequate number of nodes along the radius to potentially represent each of the stress distributions depicted in the following paragraphs. Another critical element in such a sizing process was the skin; in fact, it was the more extended component, therefore strongly influencing the computational weight of the model. Since small strain variations could be expected along its thickness, even considering the presence of relevant centrifugal actions, tetrahedral linear elements were chosen to minimise the number of nodes.

The left side of the structure is clamped, while the opposite section is allowed to twist (rotation around the span axis) and translate along the three directions, which is coherently with a typical wind tunnel test room. Such an outline was aimed at investigating the aerodynamic performance variation, consequent of an imposed warping, without the effect of centrifugal actions. Those forces would have altered the overall system stiffness, and would have therefore affected the final geometry achieved. It is worth citing that the device was tested in the whirl tower [50], and the system proved its capability in achieving the prescribed specifications, even in the presence of high-speed rotations along the normal axis.

Non-linear analyses were implemented to simulate the action of the SMA actuators on the hosting structure to the best extent. This choice was necessary for both taking care of the constitutive relations of the shape memory alloy material, the complex functions of the temperature, and the stress; and providing a satisfactory representation of the different phases of the device's installation and operation. Each analysis considered three main steps, schematically reported in Figure 4 [28,57]. There, the first column (a) defines the specific step; the adopted equivalent structural concepts are reported in the middle column (b); and the specific operation simulated at the FEM level is finally summarised in the last column (c). In detail:

- In the pre-twist phase, (a), the actuator is twisted by an imposed torque applied to a node on the centre of the edge of the SMA rod, linked to the other surrounding nodes through a spider rigid element. This generates a pre-existing stress field within the active element that is not yet connected to the main body at this stage. To the zone of the elastic structure that will be connected to the SMA, another spider is applied, connecting all surrounding interface nodes to a central one.
- In the connection phase, (b), the interface sections of the SMA rod and the structure body are connected via a multi-point constraint, or MPC. The MPC is used to define a linear relation among the components of the above-mentioned central nodes' displacement by introducing the coefficients of that function. In that manner, the displacements reported in the MPC cards block are distinguished between dependent and independent degrees of freedom, therefore reducing the effective degrees of freedom to a pre-defined set [58]. The applied twist moment (a) is then removed and the system reaches an equilibrium point, as the increased structural elastic forces balance the residual twisting action of the SMA.
- In the activation phase, (c), the shape memory alloy element is heated, inducing a strain recovery, and therefore transferring a further deformation to the structure. Since it tends to report the SMA deformation as zero, it has the same level of deformation as the one experienced in phase (b), for both the blade and the active rod. In the real operation, the temperature variation is provided by a heater coil deployed around the SMA rod, while, in the simulation process, a uniform temperature is simply imposed to the shape memory alloy.

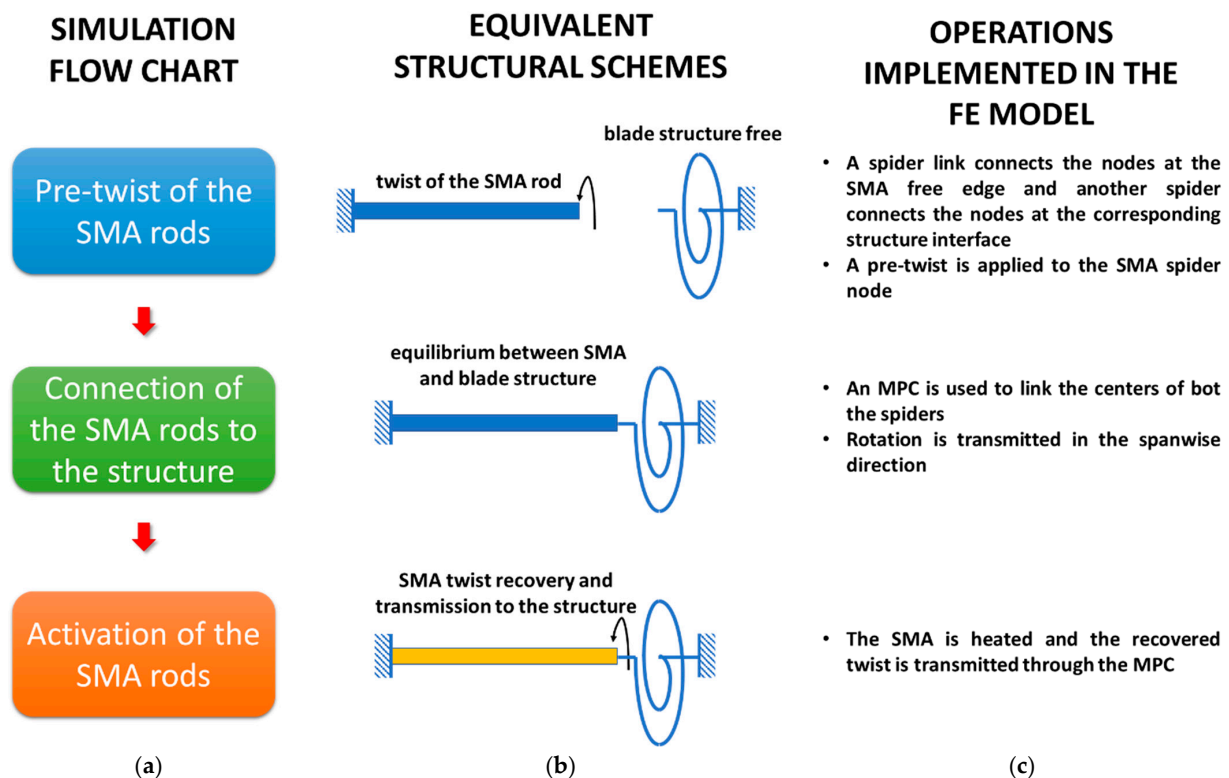


Figure 4. Schematic flow chart of the numerical process (a); equivalent structural schemes (b); operations performed in the FE simulation (c).

4. Parametric Analysis

A non-linear analysis was performed through the SOL 400 of MSC/Nastran; a dedicated card was used for simulating the SMA parts, MATSMA [58], which implements Auricchio's mechanical model [59], and the Saeedvafa-Asaro, thermo-mechanical model, cited in [60]. Among many other parameters, the card specifies the following:

- The Young modulus, the Poisson ratio, and the density in both the martensite and austenite phases;
- The transformation stresses, and the associated transformation temperatures;
- The stress and temperature slopes along the forward ($A \rightarrow M$) and rearward ($M \rightarrow A$) transformation processes.

MATSMA was used jointly with PSLDN1, an MSC Mark proprietary card, which adds non-linear displacement features to the solid elements representing the SMA material segments. A parametric study was carried out, referring to the following parameters:

- Axial, flap, and lag bending stiffness and torque rigidity vs. sc ;
- Actuation performance vs. sc ;
- Stress level vs. Reynolds (Re) and Mach (M) numbers;
- Normal mode characteristics vs. sc ;
- Aeroelastic behaviour (flutter analysis) vs. sc ;
- Rotodynamic features (Campbell diagram) vs. sc .

Propaedeutic to this investigation, the ability to keep the prescribed airfoil shape was verified vs. a torque load action. Operatively, a 2 deg/m twist was imposed on the full-size model ($sc = 1$), and the airfoil shape deviation (or, the cross-section of the blade, measured along its span) vs. the undeformed geometry was computed at intermediate longitudinal stations. Such measures were normalized vs. the chord length (270 mm), and are reported in the form of a bar plot in Figure 5. The maximum absolute variance value was estimated as 110 μm , about 1/3 of the typical roughness requirement for wind tunnel laminar tests [52].

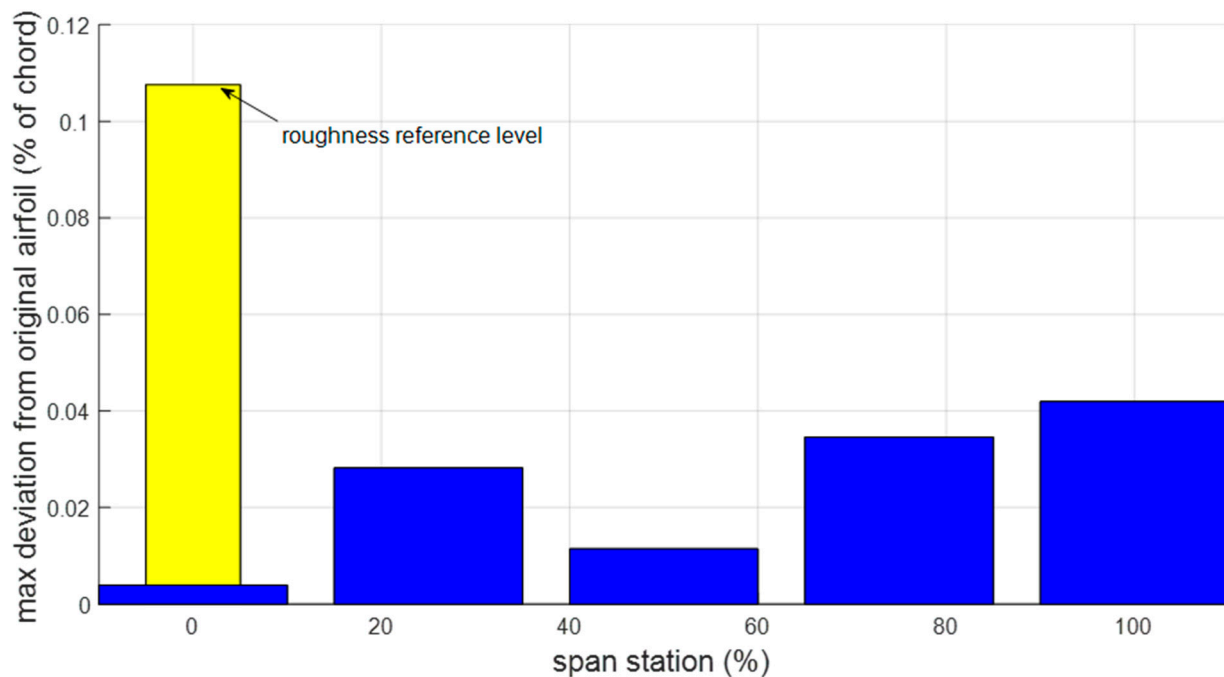


Figure 5. Airfoil maximum shape deviation vs. span station due to a twist of 2 deg/m.

It must be clear that, referring to four values of the scale factor, sc , a polynomial representation of the generic parameter vs. sc may be to the third degree, at most.

5. Stiffness versus sc

The first analysis was aimed at finding a relation between the sc and the blade structural stiffness characteristics, in particular associated with flap and lag bending and torsion stiffness. The SMA actuator, in the form of a bulk rod spanning along the entire blade section length, contributed to the overall stiffness significantly, so that it may be considered as an integral part of the structure (load-bearing actuator). The load transmission among the elements of the considered architecture was intrinsically considered in the FE model, once it was assembled.

Before being integrated into the structure, the shape memory alloy element was supposed to be in a pure austenite phase; then, it was pre-loaded by a torque to achieve a transformation in the martensite phase, and successively connected to the structure. It is worth noting that, since the stress level approaches zero at the centre of rotation, pure austenite is always present, no matter the amplitude of the applied load. In order to properly consider this phenomenon, non-linear analyses have been set.

Static analyses were performed by applying concentrated actions at the free end of the model, clamped on the other side. In spite of the use of a non-linear model, for the reasons described above, the stress and strain fields were kept small enough to approximate the structural response for each sc considered, with its first-order representation in the considered range of action of the imposed loads (linearity).

5.1. Axial Rigidity

The numerical axial rigidity was computed as the ratio between the applied axial force and the corresponding displacement. In this case, the blade model may be assumed as a simple rod, whose linear spanwise displacement is related to the applied force F , the span length L , the cross-section area A , and the material Young modulus E , using the following classical equation [61]:

$$u_{axial} = \frac{F_{axial}}{(EA/L)} = \frac{F_{axial}}{K_{axial}} \quad (1)$$

Holding the following relation between the reference ($_0$), and the scaled geometrical characteristics,

$$A = A_0 sc^2; L = L_0 sc \quad (2)$$

It follows that

$$\frac{EA}{L} = \frac{EA_0 sc^2}{L_0 sc} = sc \frac{EA_0}{L_0} \quad (3)$$

or

$$K_{axial} = sc K_{axial 0} \quad (4)$$

The dependence of the axial stiffness on the scale factor is reported in Figure 6. The FE prediction is in line with the theoretical assumption of a linear relation between the structural stiffness and the scale factor.

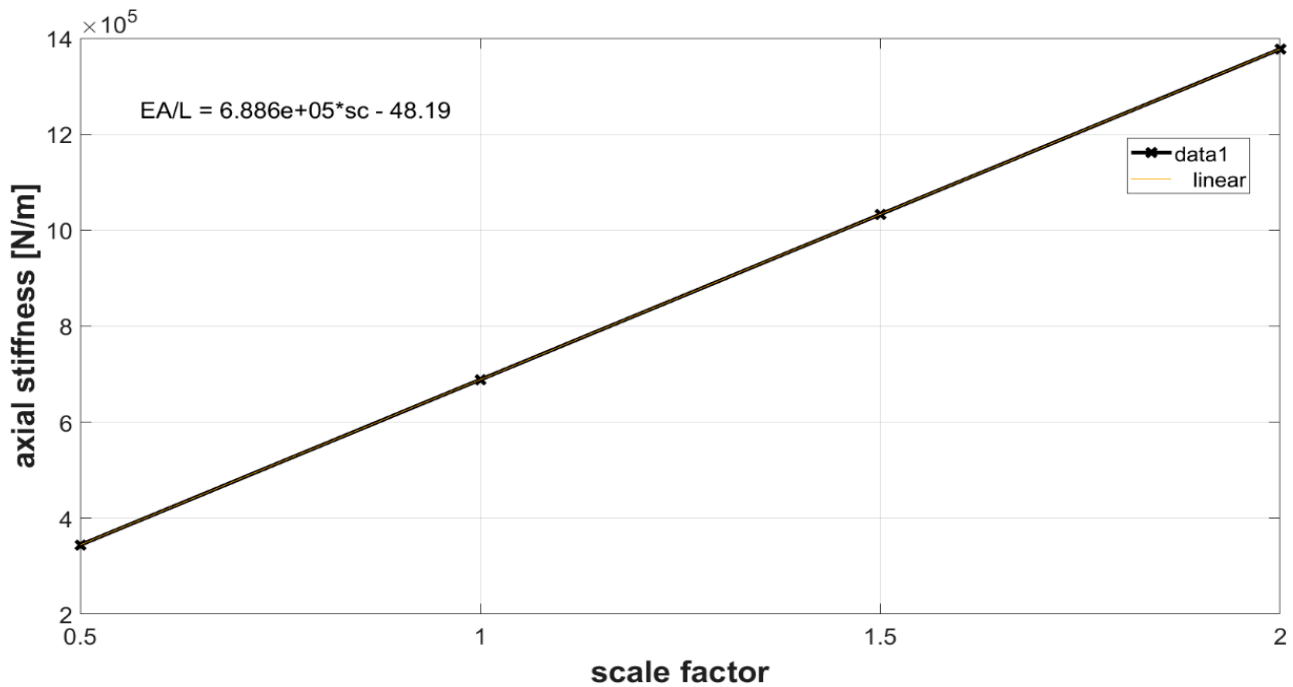


Figure 6. Axial stiffness vs. scale factor and the relevant interpolating polynomial.

5.2. Bending Rigidity

In the same way, the blade bending lag and flap stiffness were numerically estimated by loading the structure with chordwise and normal-to-wing-plane forces, respectively. The nominal lag and bending stiffnesses were computed through the classical equation for a generic cantilever beam, in the following form [61]:

$$w_{flap,lag} = \frac{F_{flap,lag} L^3}{3EI_{flap,lag}} \Rightarrow K_{flap,lag} = \frac{3EI_{flap,lag}}{L^3} \quad (5)$$

The product $EI_{flap,lag}$ was estimated via numerical analysis, using the known applied force, F , and the resulting applicable displacement, $w_{flap,lag}$. Since

$$I = I_0 sc^4; L = L_0 sc \quad (6)$$

it follows that

$$\frac{3EI}{L} = \frac{3EI_0 sc^4}{L_0 sc^3} = sc \frac{3EI_0}{L_0} \Rightarrow K = sc K_0 \quad (7)$$

or

$$K_{flap,lag} = sc K_{flap,lag 0} \quad (8)$$

The plots in Figures 7 and 8 illustrate the dependence of the flap and lag bending stiffnesses on the geometrical scale factors, as computed through the FE computations.

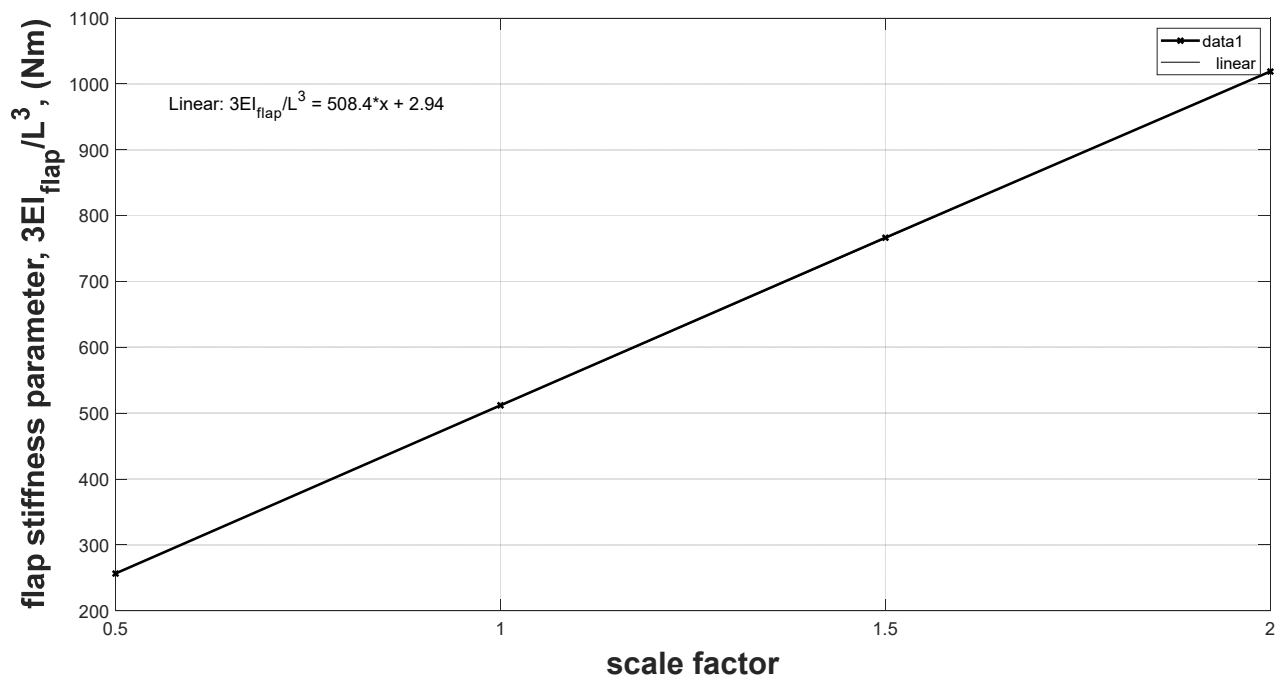


Figure 7. Bending stiffness in flap direction vs. scale factor and the relevant interpolating polynomial.

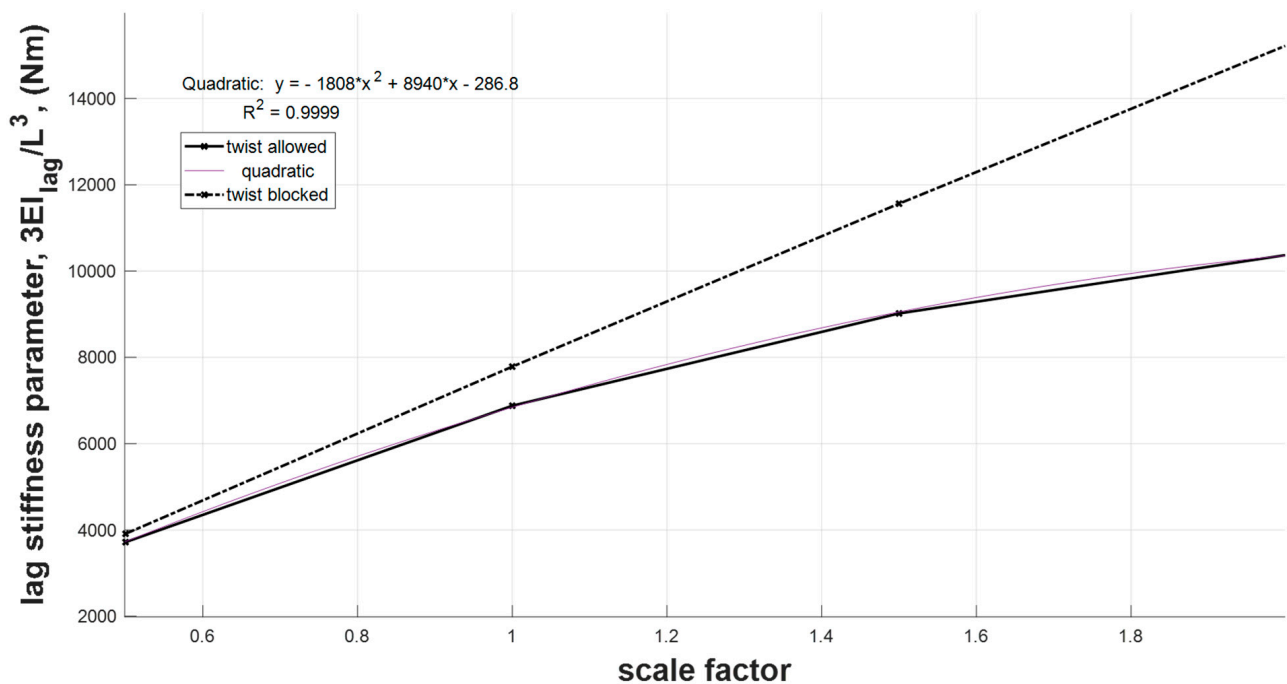


Figure 8. Bending stiffness in lag direction vs. scale factor and the relevant interpolating polynomial, blocking and releasing the torsional DOF.

The computations were initially carried out by letting free the torsional DOF, both for the flap and the lag bending stiffness. In this latter case, however, the influence of the torsion was evident because of the smaller displacement, therefore avoiding the appreciation of the single function relating the external force to the lag bending movement of the blade. It was therefore chosen to perform the computations again by suppressing the torsional

DOF. In fact, flap bending as a function of the sc seems to be well represented by a linear function, as predicted in the basic theory. The latter appears instead to be affected by a strong non-linearity which is well approximated using a quadratic polynomial, as shown by the practically unitary value of the coefficient R^2 . As the twist DOF was locked, a linear trend was found. This can be easily explained for the larger relative sensibility of the lag bending vs. the application of the force along the thickness, easily inducing a rotation and therefore activating the blade's warping. Such an effect vanishes as torsion is inhibited, and the model comes back to its elementary behaviour. An application of the external force at the very exact location of the shear centre does give rise to a diagram equivalent to that of Figure 8.

5.3. Torsional Rigidity

For the torsional stiffness, the product of the shear modulus, G , by the polar moment of inertia, J , over the span length, L , was assumed as a representative parameter. A concentrated moment was applied at the free edge to estimate the produced twist. Under the assumption of a constant twist rate along the span, the equation expresses the link between the applied moment, T , and the attained rotation, $\Delta\theta$.

$\Delta\theta$, can be written as

$$\frac{GJ}{L} \cdot \Delta\theta = T \quad (9)$$

Since

$$J = J_0 sc^4; L = L_0 sc \quad (10)$$

it follows that

$$\frac{GJ}{L} = \frac{GJ_0 sc^4}{L_0 sc} = sc^3 \frac{GJ_0}{L_0} \Rightarrow K = sc^3 K_0 \quad (11)$$

or

$$K_{tors} = sc^3 K_{tors,0} \quad (12)$$

It then follows that the link between the torsional stiffness and the scale factor should be expected to have a cubic characteristic. The plot of Figure 9 shows the torsional stiffness versus the scale factor and the interpolating polynomial, after the numerical simulations. It confirms the basic theory prediction and also provides a confirmation of the effect that was seen before, as the torsion provides a cubic contribution in terms of rotations, and a quadratic contribution in terms of the affected displacements (since the ratio s/r , namely the displacement occurring at the generic radius, may be put in place of $\Delta\theta$, therefore resolving a new stiffness factor, depending on the sc squared).

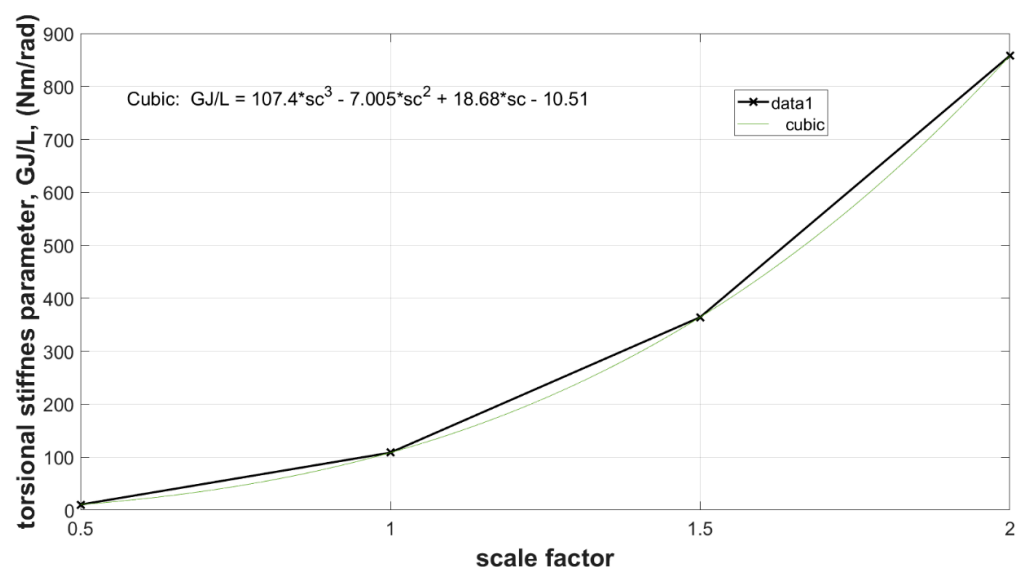


Figure 9. Torsional stiffness vs. scale factor and the relevant interpolating polynomial.

5.4. A Consideration

It may be of some interest to compare the trend of the different curves, for instance, by normalising the axial, lag, and flap bending vs. the torsion stiffness. It should be remarked that those ratios are dimensional (specifically, $1/L^2$), as the stiffnesses have different meanings; furthermore, for the lag bending stiffness, the linear curve was chosen, which is coherent with the former considerations. Such an exercise brings us to the plot reported in Figure 10. All three curves seem parallel to each other because of the implemented logarithmic scale, which is necessary for a compact representation within a unique plot. The curves do appear to strongly diverge in a linear scale as the scale factor approaches zero, highlighting the challenge of obtaining scaled properties by acting on a unique factor. As a latter consideration, it may be said that, since the ratios are dimensional and linked to the inverse of a squared length, the graph below may be seen as the visual representation of sc vs. sc^2 .

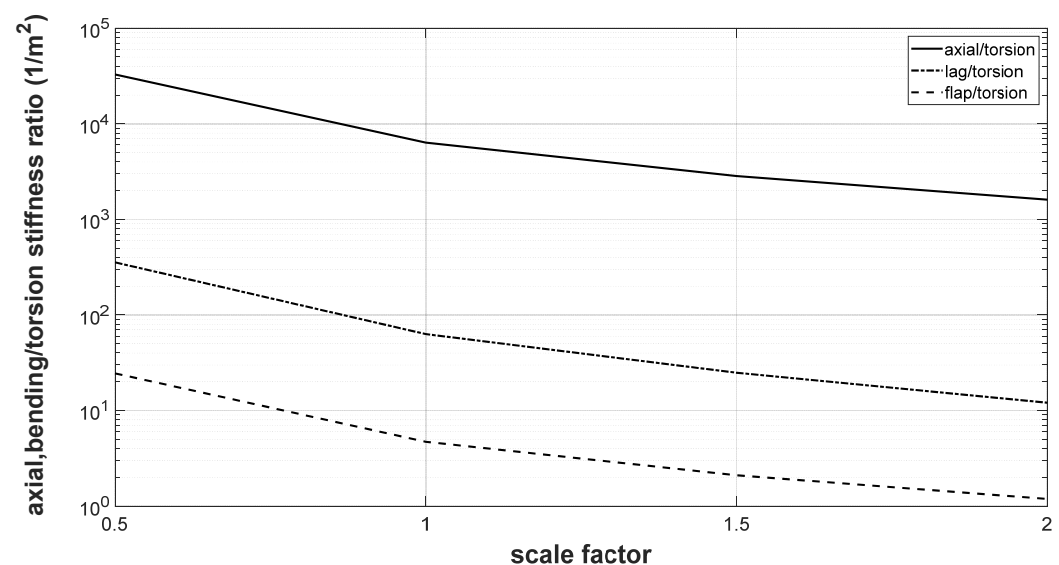


Figure 10. Axial and flap and lag stiffnesses normalized with respect to the torsional rigidity.

6. Stress Level vs. Re and M

The structural system's ability to absorb external loads may be considered its essential characteristic. In this sense, the effect of the Mach (M) and Reynolds (Re) numbers may be significant for an aeronautical element. The former is related to the speed, and then to the magnitude of the forces acting on it, while the latter depends on the characteristic size of the architecture and, therefore, directly on the scale factor. The system chord is taken as the reference length for Re . The 2D Xfoil software [62] was used to determine the aerodynamic pressure distribution around the model. The angle of attack was considered to vary in the range of ± 12 deg, with a 1 deg step, estimating the pressure distribution along the chord at each step. The most severe case resulted in that of -12 deg, which was finally assumed to evaluate the stress field.

At first, the effect of Re was explored by varying the sc and keeping the M constant and equal to 0.07 (about 24 m/s at sea level). This value is equivalent to the linear speed of the middle cross-section of a 1 m span blade rotating at 44.4 rad/s, which is the nominal hover condition of the reference helicopter, the Bo 105. Since the pressure load is mainly driven by the speed and the geometrical shape of the airfoil (inside a certain range of size), with both of them being invariant (the first as a hypothesis, the second as a result, as previously discussed), the same pressure level is expected to act on all the models, irrespectively of the sc value. The resultant force and moment depend on the exposed area (Figure 11), so that a quadratic dependence on the scale factor, and then on the Re (depending in turn on the sc , linearly), was finally observed. However, external force levels were compensated by the clamp surface increase. An example of the resulting stress map is reported in Figure 12

($Re = 4.1 \times 10^5$); it refers to the real-scale model ($sc = 1$), but it is applicable to all other models. A maximum stress level of 3.5 MPa was observed in the skin element, the most critical part, which is well below the PVDF stress limit (14 MPa).

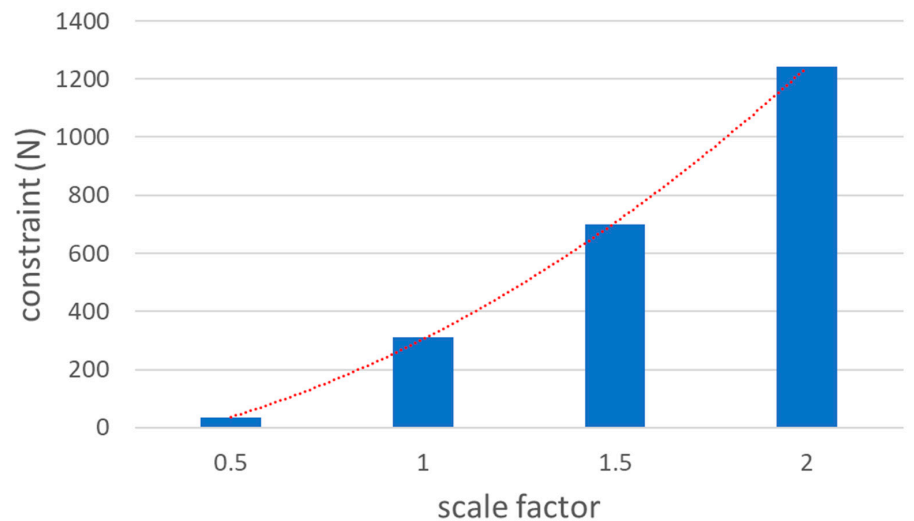


Figure 11. Normal to constraint reaction at the clamped edge vs. scale factor.

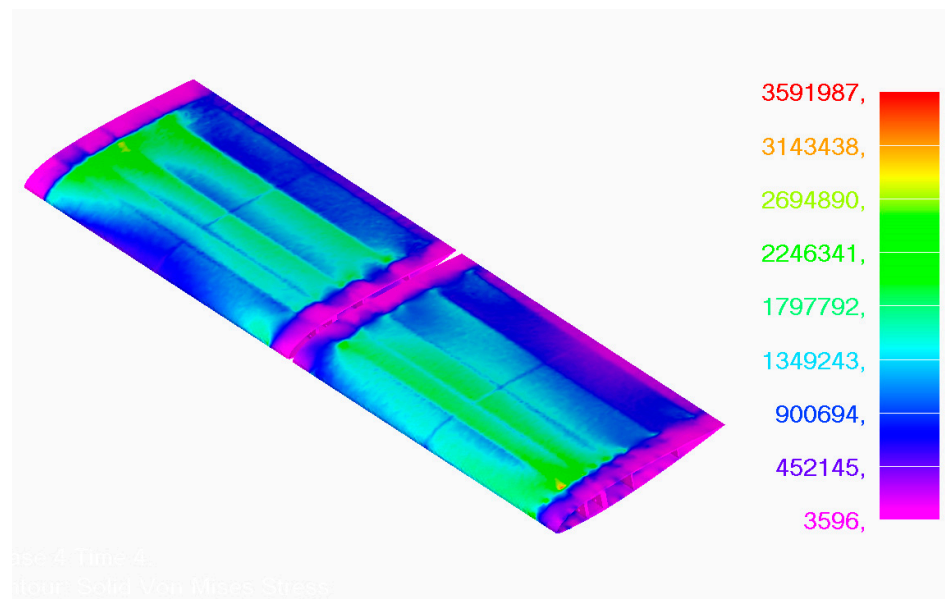


Figure 12. Von Mises stress distribution at $M = 0.07$ and $Re = 4.1 \times 10^5$.

The effect of the M was then investigated by keeping the Re constant at 4.1×10^5 . The chord variation following the application of the sc was compensated using a speed adjustment, in agreement with the following classical relation:

$$Re = \frac{\rho \cdot c \cdot V}{\mu} = \frac{\rho \cdot c_0 \cdot sc \cdot V}{\mu} \rightarrow V = \frac{Re \cdot \mu}{\rho \cdot sc \cdot c_0} \quad (13)$$

where ρ , μ , c , and c_0 are the air density, the dynamic viscosity, and the model's scaled and full-size chord, respectively. The dramatic impact of the Mach number on the stress level is shown in Figure 13. $M = 0.13$ (corresponding to a 0.5 scale factor value) leads to a maximum stress of 13 MPa, almost four times the one attained for the reference model.

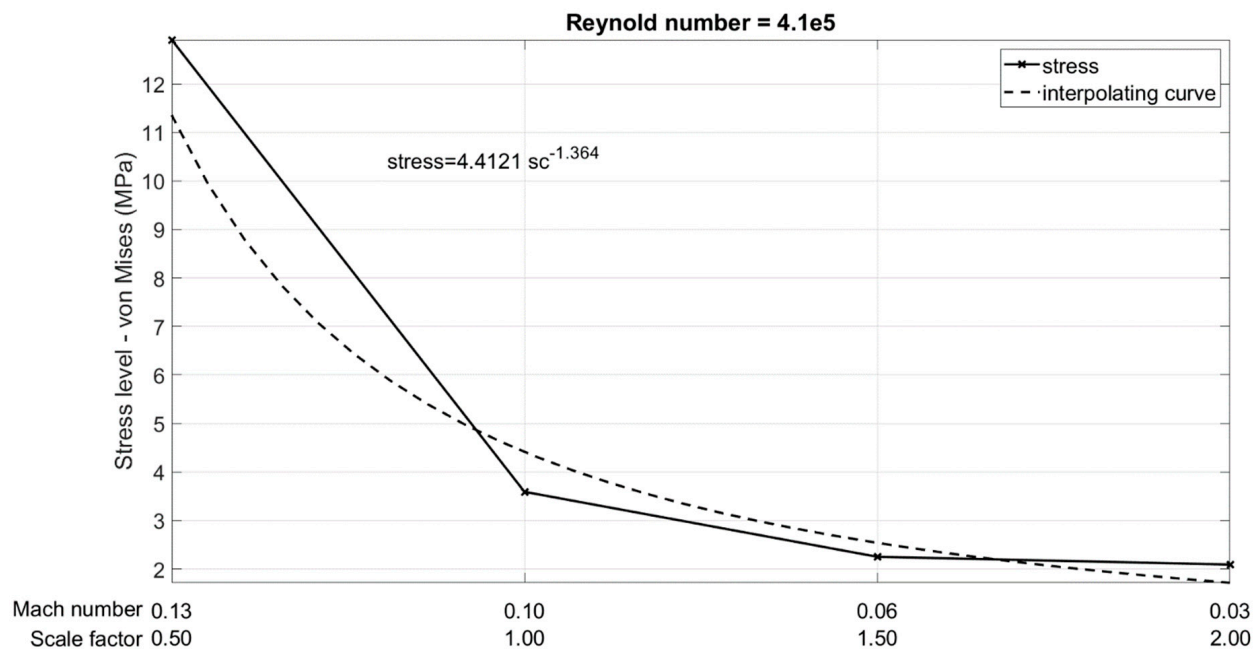


Figure 13. Von Mises stress vs. M and sc at $Re = 4.1 \times 10^5$.

7. SMA Actuator Performance

Widely discussed in the literature, with a huge number of bibliographic references, SMAs are a peculiar material able to change their phase via thermal or stress loads. The most relevant fact is that the two phases (martensite—low T , high stress; and austenite—high T , low stress) have very different physical characteristics, including a different density. Despite being slight, such a property leads to extremely significant effects like the strain recovery capability [63,64].

The shape memory alloy was initially assumed to be in a full-austenite phase. The SMA actuator behaviour was investigated under both axial and torsional loads, which give rise to very a different distribution of phases. In fact, the axial load induced a uniform stress distribution over the cross-section and, thus, a uniform phase map. Contrastingly, the torsional load caused an angular deformation in the plane of the cross-section, with a consequent strain variation ranging from zero (at the section centre) to the maximum value at its border. Therefore, an austenite core is always present, irrespective of the amount of external action; a full martensite transformation may occur at the outer section regions where the stress achieves its larger value, while an intermediate state is formed in between. In Figure 14, three configurations of phase distribution on the cross-section are illustrated. If the stress level produced by the torsional load is lower everywhere than the start transformation threshold, only the uniform pure austenite can be found (scheme on the left). If the stress level values increase to a level higher than the start transformation threshold but lower than the end transformation threshold, the situation depicted by the scheme on the middle occurs; in this case there is a pure austenite core and an external crown. Finally, if stress levels that are higher than the end transformation threshold is achieved, the situation illustrated in the scheme on the right occurs; there are, in practice, three regions: the pure austenite core, an intermediate crown in which austenite and martensite coexist, and an outer crown with pure martensite.

This effect leads to very different trends in the actuator's performance vs. the geometrical scale factor. In the case of the axial load, the ratio between the transmitted force and the structural stiffness depends on the ratio between the SMA rod and the structural cross-section areas, which is independent of the sc . For a torsional actuation, such a uniform distribution cannot hold for the above-mentioned reasons, and for other details that will be given in the next part of this section.

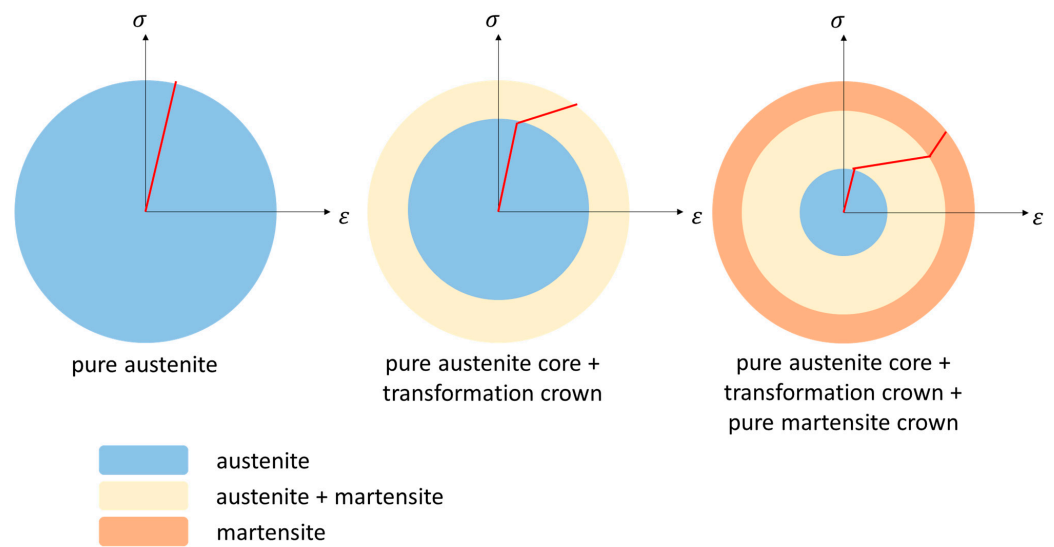


Figure 14. Phase distribution across the cross-section of the rod: pure austenite (**left**) for stress levels below the transformation start; austenite core + transformation region (**middle**) for stress levels higher than the transformation start threshold; austenite core + transformation region + outer pure martensite crown (**right**) for external stress levels higher than the transformation end threshold.

7.1. Axial Load

The plot of Figure 15 illustrates the typical SMA stress–strain cycles for axial loads at four different temperatures: 25 (room temperature), 50, 75, and 100 °C. Even in these cases, the non-linear MSC/Nastran SOL 400 was used. The specific analysis is split into three steps: the first to set the temperature, and the second and third to load and unload the specimen.

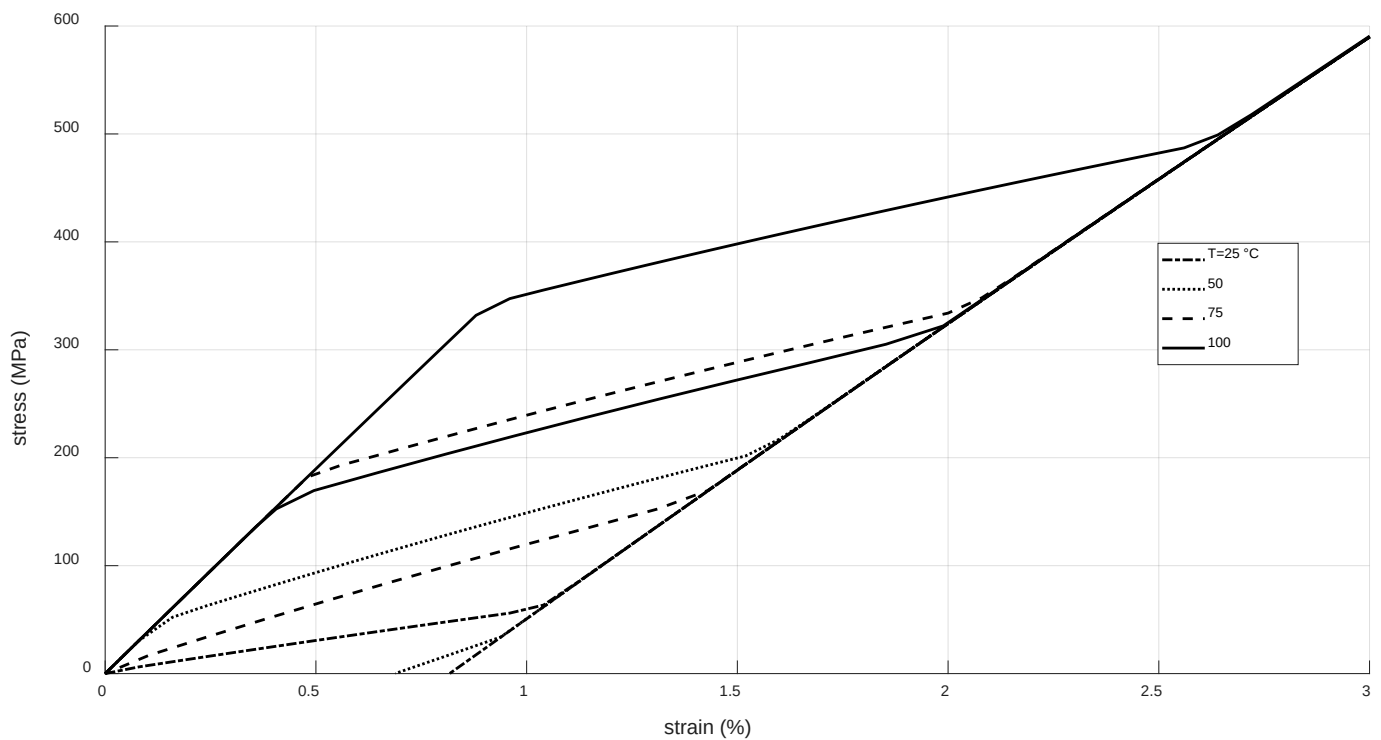


Figure 15. Stress vs. strain vs. temperature for the SMA considered for the model (axial load).

For the load cycles at 75 and 100 °C, the transition segments of the curves are parallel in both directions of the transformation (austenite into martensite and vice versa). At 50 °C, it is possible to see that the reversal transformation (from M to A) gives rise to a residual strain, meaning that a mechanical force is necessary to recover the strain at its initial zero. Finally, the transition segment of the cycle at room temperature shows a different slope; in the adopted model, it is linked to the fact that, at room temperature, the martensite activation stress is nominally below the zero value; physically, it means that the martensite transformation is activated as the stress is imposed. As the martensite fraction at the considered temperature increases as the working temperature decreases, such a line tends to approach the x-axis. However, this apparent behaviour is correlated to a limitation of the herein implemented analytical representation, which could have been overcome by integrating the well-known Brinson's correction [65]. Such a model upgrade would have, however, not modified the global results of this preliminary investigation (even because the way of working of the implemented SMA actuator stands in a continuous transformation between martensite de-twinned and austenite, at considerable temperatures and stress levels), so it was chosen to avoid this further complexity.

7.2. Torsional Load

An analogous three-step approach was implemented for the analysis of the SMA actuator under a torsional load analysis, analysed for the same temperature set (25, 50, 75, and 100 °C). The same angular deformation (0–57 deg) was imposed at the free edge. Different curves were obtained for each sc , as shown in Figure 16.

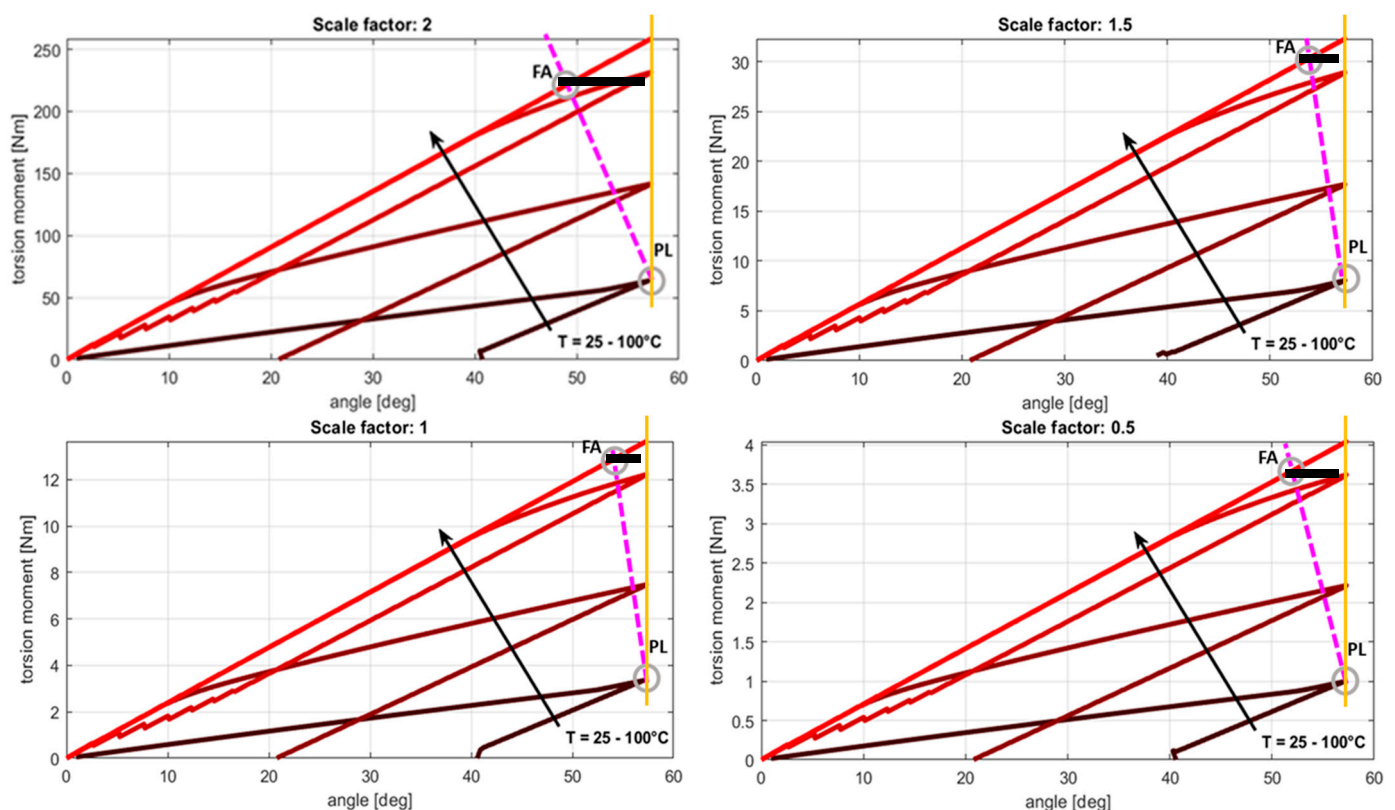


Figure 16. Torque moment vs. angle vs. temperature vs. scale factor.

The impact of the scale factor on the twist recovered was identified against the structural torque stiffness (Figure 9). To this scope, the torque moment vs. angle vs. temperature plots obtained for all scale factor values are reported. In the same plots, the equivalent torsional stiffness of the structure at the different sc values is represented by the pink dashed lines. The PL and FA points, therein reported, represent the pre-load (PL) conditions

(set at the same equilibrium value of 57 deg with the structure) and the full actuation (FA) conditions, achieved for the maximum temperature considered, that is to say, 100 °C, which allows for transformation in pure austenite. It must be clear that any loading process working at that temperature does not produce any martensite fraction, since the martensite activation stress is moved higher than the considered conditions for that temperature. The horizontal distance between these two points, *PL* and *FA* (black thick lines in Figure 16), represents the maximum angular strain, which is potentially recoverable. The observed trend of such a recovery angle, different from the axial actuation, is strongly dependent on the scale factor; this is reported in Figure 17. For an *sc* lower than 1.5, a decreasing trend is noticed, moving from 4 (*sc* = 0.5) to 3 (*sc* = 1.5) deg of potential recovery rotation. Then, the curve strongly rises up to reach 12 deg for a scale factor equal to two, with an approximate parabolic trend. This phenomenon seems to be caused by the different martensite levels produced by the same pre-twist angle in SMA rods of different diameters.

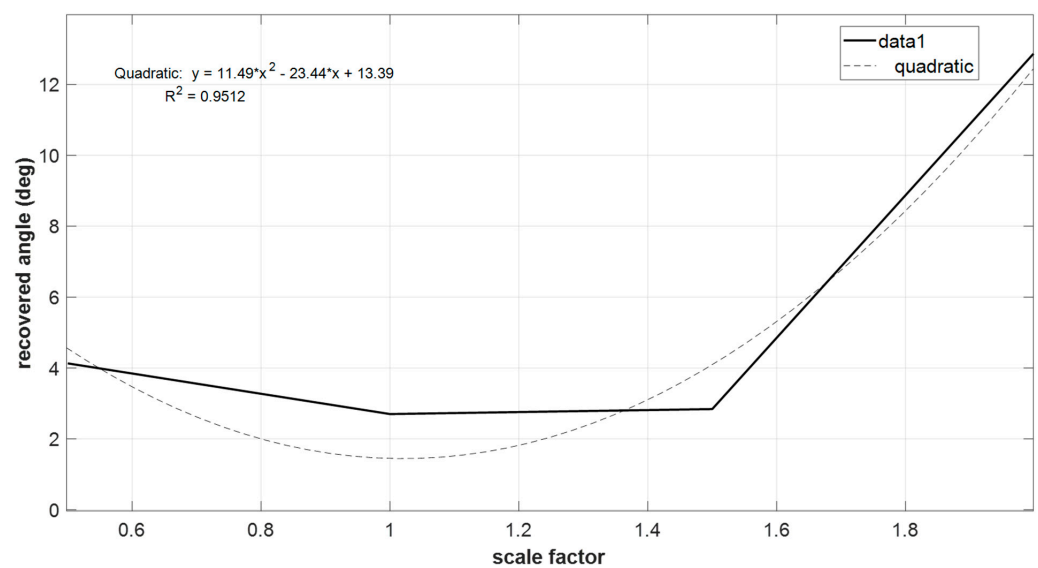


Figure 17. Recovered angle vs. scale factor.

The operational structural torque stiffness was finally computed by intersecting the lines representing the structural stiffness, as reported in Figure 16 (pink lines), with the characteristic curves of the SMA behaviour at the different temperatures and at the different *sc* values (Figure 17). As it can be expected, it dramatically rises with the scale factor, as reported in Figures 18 and 19, and Table 2.

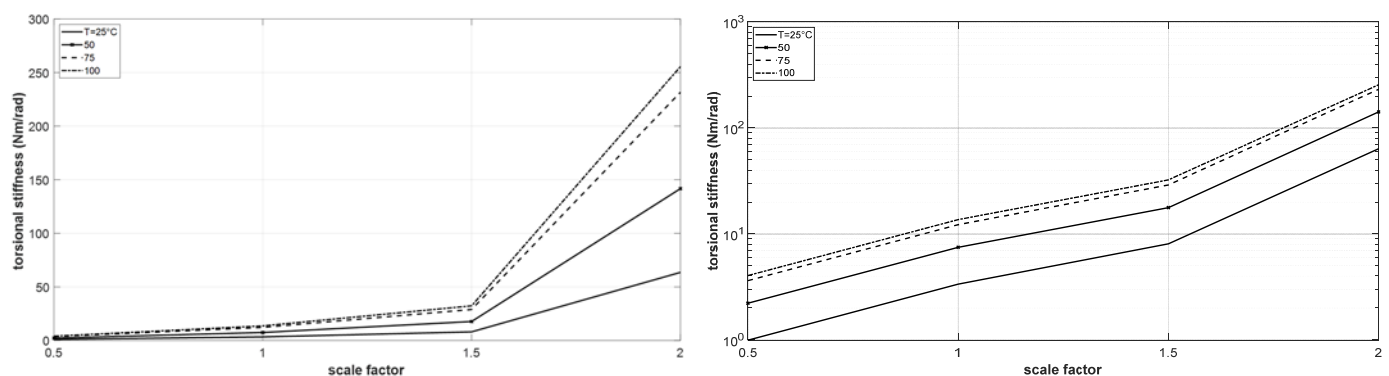


Figure 18. Torque stiffness vs. scale factor, parameterized with reference to linear temperature (left) and logarithmic plot (right).

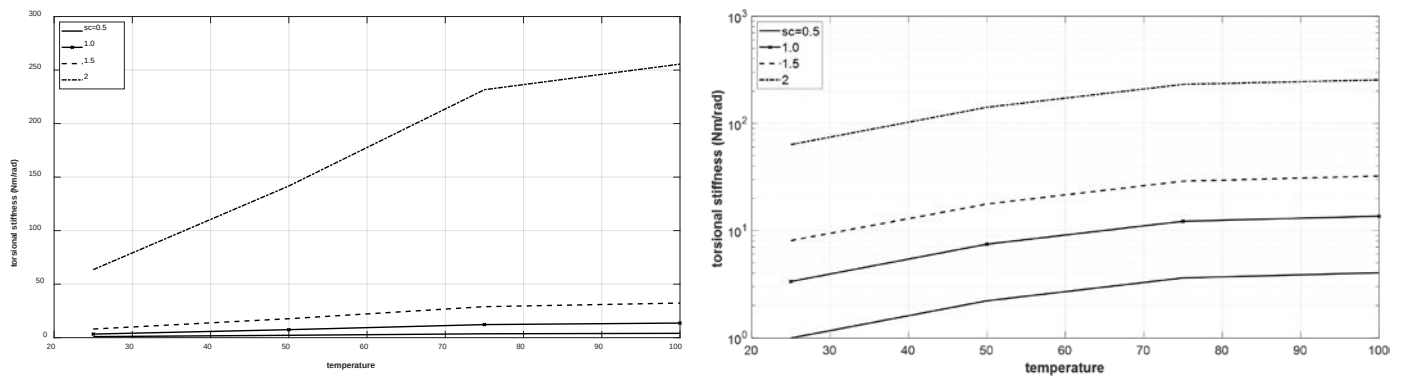


Figure 19. Torque stiffness vs. temperature, parameterized with reference to the scale factor: linear (left) and logarithmic plot (right).

Table 2. Torque stiffness vs. scale factor vs. temperature.

Value of Torque Stiffness vs. sc vs. T (Nm/rad)				
T (°C)	Scale Factor, sc			
	0.5	1.0	1.5	2.0
25	0.99	3.35	8.06	63.56
50	2.21	7.47	17.71	141.70
75	3.62	12.22	28.95	231.63
100	4.04	13.64	32.32	255.57

The plot in Figure 20 (bottom) reports the evolution of the martensite phase during the three-step process, parametrized, vs. the radial coordinate in a non-dimensional representation ranging from 0 to 100% of its max values (0%, centre of rotation; 100%, external radius). The martensite phase depends strongly on the radial coordinate. Starting from full austenite (martensite fraction = 0.0), its concentration linearly increases during the pre-load phase. The larger stress level in the outer annular layers determines a wider production of martensite and, thus, a different slope of the curves during the incremental application of the external torque moment. During the elastic recovery and consequent partial relaxation of the SMA, the produced martensite fraction slightly decreases, in the cases that the austenite activation stress is reached, and is usually considerably lower than the martensite start values (the value according to which the transformation from austenite to martensite begins). Finally, during the activation, the austenite is fully recovered at different temperatures for the different radial stations, as the austenite end temperature is reached for specific stress values, which are larger for the more external layers.

The top part of Figure 20 illustrates instead the process from a macroscopic point of view (the two histories are synchronously represented, as they happen in reality). At first (blue region), there is the application of an external moment aimed at producing a martensite transformation, in turn leading to a potential rotation recovery, commanded through the heating of the actuation rod. Then, the rod is attached to the structural skeleton; then, the two parts are left free to find their elastic equilibrium; in this phase, the SMA rod is partially relaxed, inducing an increase in the structural tension within the blade, until the two stress fields balance. Usually, the stress decrease within the rod is designed so as to produce only a marginal reduction in the martensite fraction, since its presence is to allow a forced rotation recovery of the system. Finally, the heating phase is implemented (red region). It consists of heating the SMA so as to lead to a phase change (namely “activation” in the plot) from martensite to the austenite fraction; the

grey line in the picture shows the temperature increase. As the austenite activation's start temperature is achieved, the shape memory alloy material gradually changes its phase, trying to reconvert to a pure austenite phase. At that point, the crystalline modification will stop, and so too will the rotation recovery. In this architecture, the movement occurs in the same direction as the elastic recovery. From that point on, as the material cools down, it returns to the starting point, indicated by S in the same picture. It is relevant to note that, in the shown simulation, it was chosen to make the activation phase start when the elastic recovery was still ongoing, in order to check the capabilities of the implemented routines.

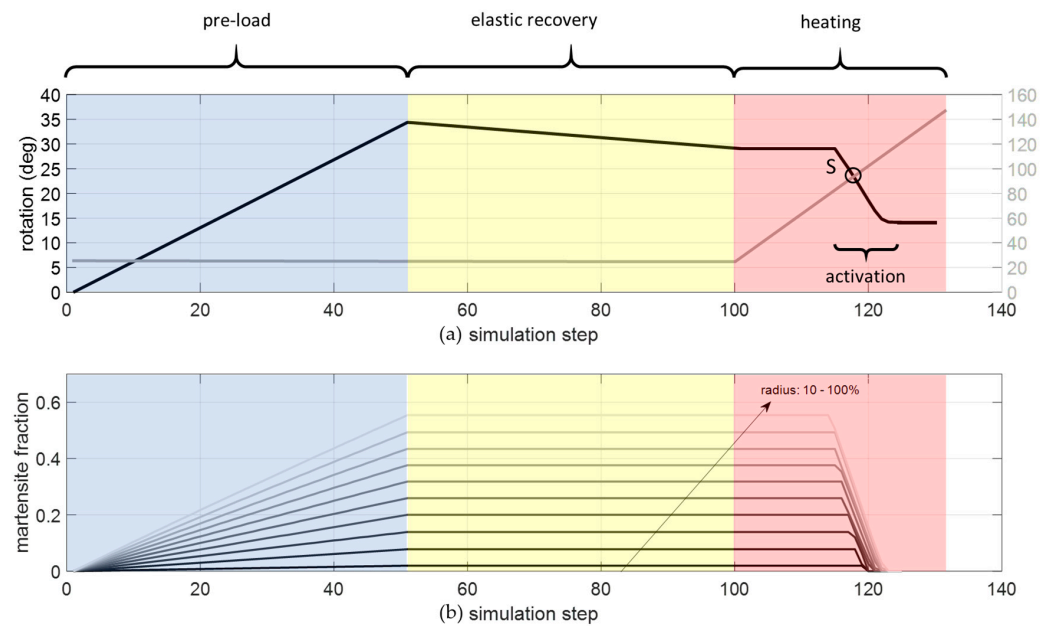


Figure 20. Pre-load, elastic recovery, and thermal recovery, following the adopted 3-step mounting and operation process. Twist rotation and SMA temperature (a); martensite production and austenite recovery (b).

7.3. Heating Aspect

Finally, some considerations can be made with regard to the time of activation. The time required for the full activation of the SMA elements depends on different aspects, some of them strictly related to the heat transfer, others specifically relevant to the SMA material. The rapidity of the activation of the alloy depends on how fast the full transformation temperature is reached. Power supply, its addition, and heat losses play a fundamental role. If it is possible in principle to increase the power supply at will to accelerate the heating and thus the activation, speeding up the de-activation process via cooling is relatively more difficult; in this case, in fact, unless considering additional exchange systems, heat can be dispersed only through the pre-existing heat transfer mechanisms. Figure 21 compares the time history of the temperature computed via a transient heat analysis performed on the FE model of an SMA rod, scaled using sc. Radiation and natural convection heat transfer modes were implemented, assuming a thermal conductivity of 13.3 W/mK, a convection coefficient of 15 W/m²K, and a heat flux of 10 W/m² applied on the lateral surface of the cylinder. A surrounding temperature of 25 °C was assumed. The higher the scale factor, the longer the time needed to achieve the regime's condition was. As shown in the plot of Figure 22, a parabolic dependence can be found between the time needed to achieve the regime conditions and the scale factor.

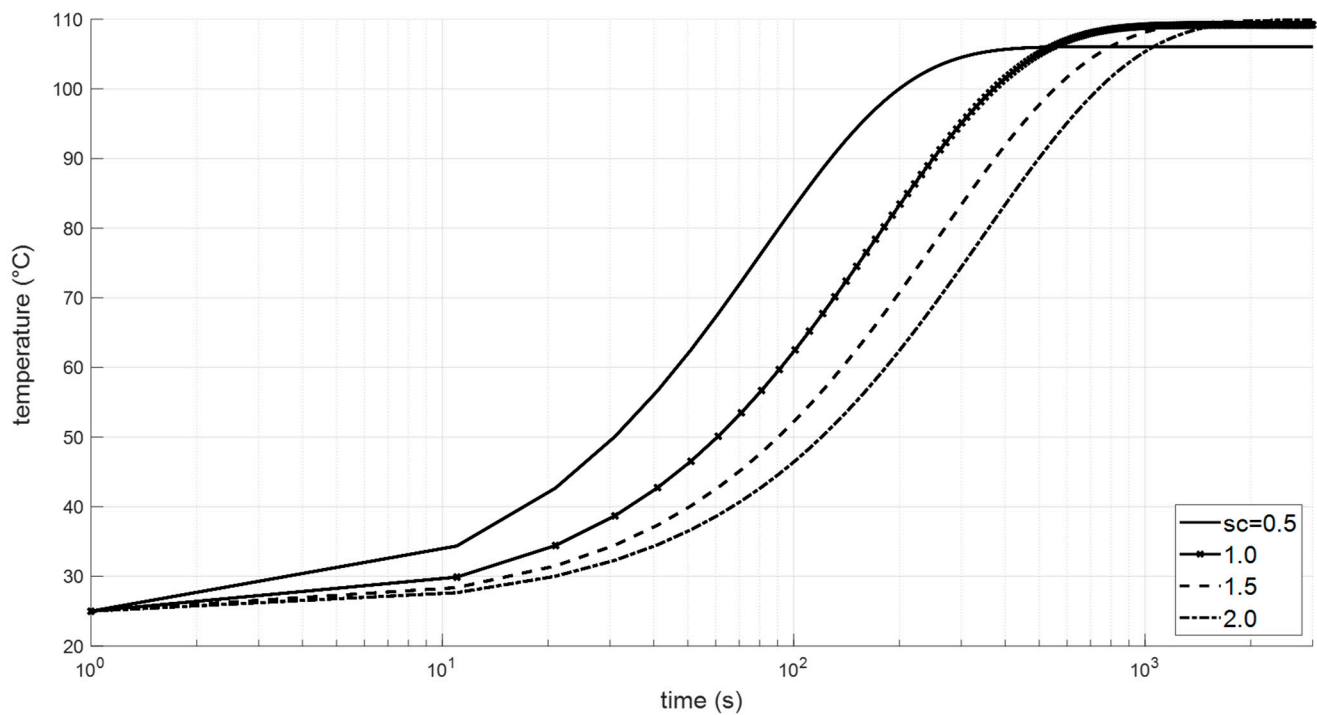


Figure 21. Temperature increase in a SMA rod against the scale factor.

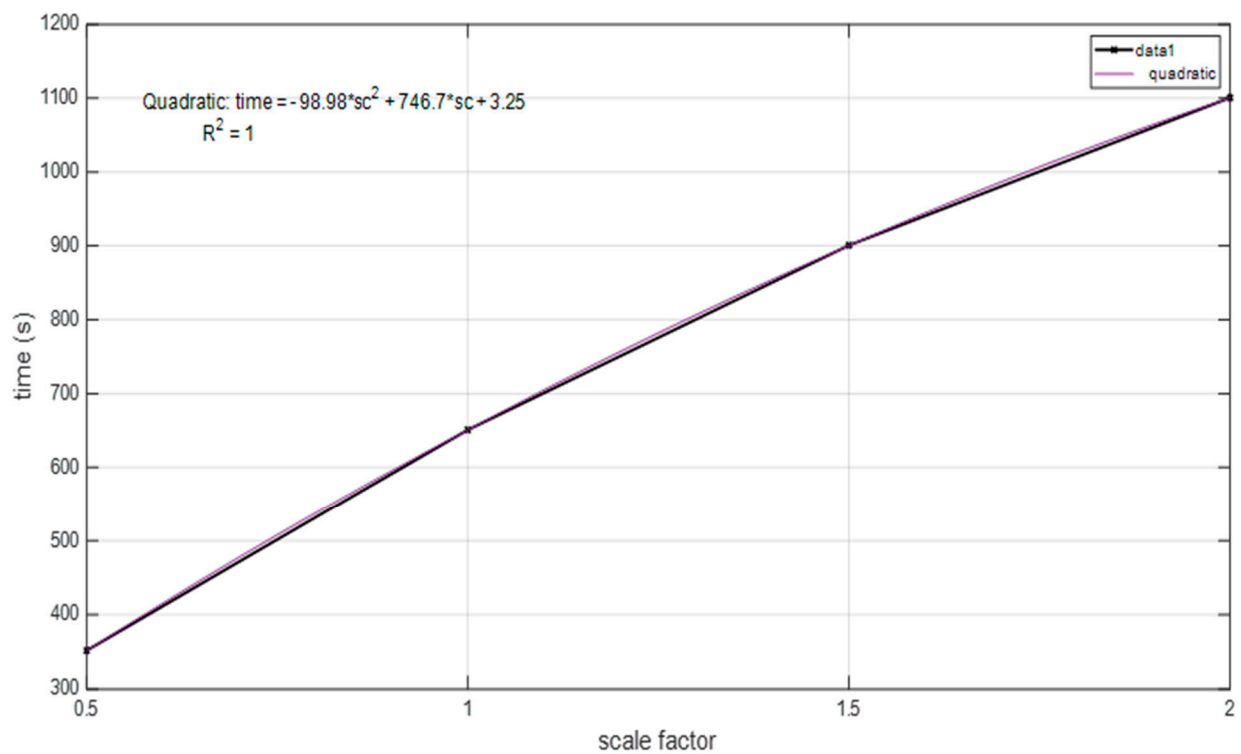


Figure 22. Time needed to achieve regime condition vs. scale factor.

Further to these aspects, the specific nature of the SMA material should also be considered. In fact, for the same power supply and heat transfer characteristics, the activation is a function of the target temperature threshold, and in turn, a function of the stress levels. To mitigate the effect of this parameter, the stress–temperature curve gradient and the zero-stress activation temperatures can be altered, within certain limits, by changing the alloy composition.

8. Dynamic Behaviour

In this section, the impact of the scale factor on the dynamic behaviour of the model is reported, at the levels of both the fixed and rotary wing configurations, analysed through the classical flutter and Campbell diagrams, respectively. MSC/Nastran uses the Galerkin method [66,67], based in turn on an application of Green's function for the dynamic analysis.

8.1. Aeroelastic Analysis

A simplified version of the referred blade segment model was realised to perform the aeroelastic investigations (Figure 23). This numerical representation was made of beam elements, featuring the SMA rods, and plate elements, schematizing the blade structure, for a total of 5534 nodes (about 2.6% of the previous FE model). Two edge constraint arrangements were explored: clamp-pinned and clamp-free. Preliminarily, a modal comparison between the simplified and the refined models was executed by considering the first six eigenvalues and eigenvectors. The percentage deviations proved a good agreement for the two layouts (Figure 24).

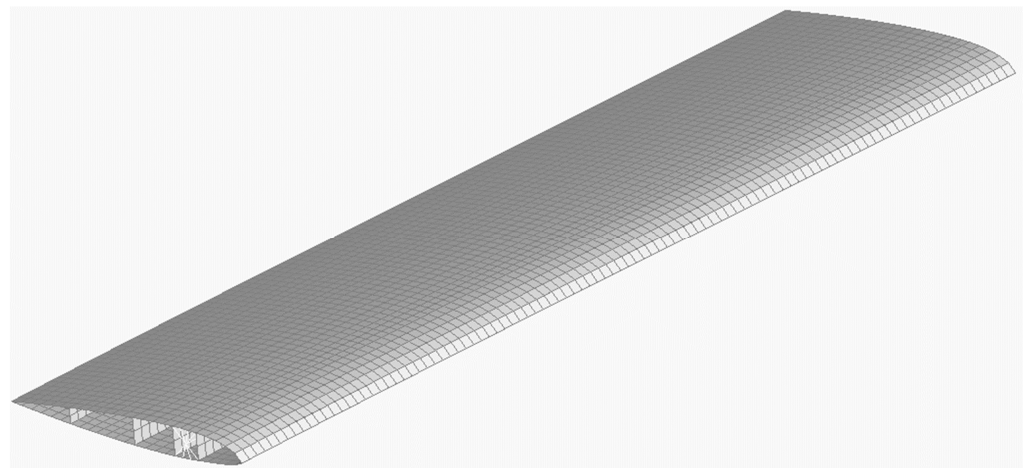


Figure 23. Simplified FE model of the blade segment, used for aeroelastic investigations.

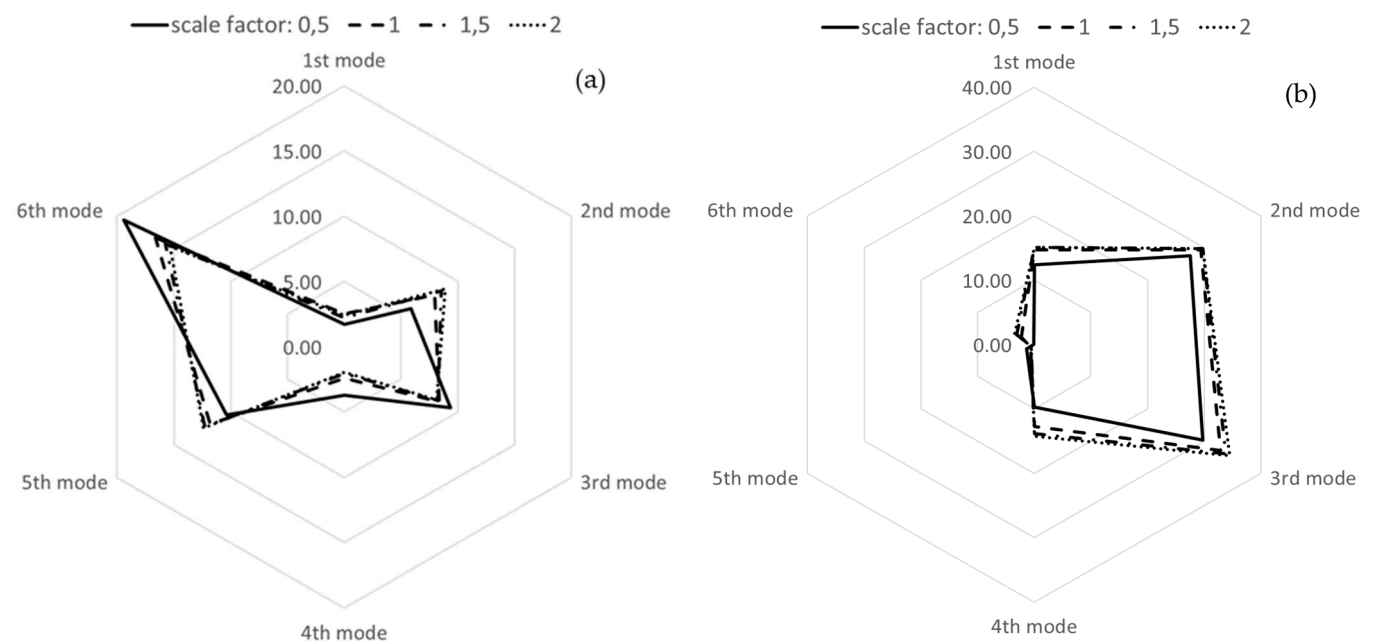


Figure 24. Refined vs. simplified FEM: normal mode comparison (percentage difference, reported along the axis) for clamp-free (a) and clamp-pinned (b) configurations.

The MSC/Nastran SOL 145 aeroelastic flutter was used to carry out the corresponding analysis. An example of the results is reported in Figure 25 ($sc = 1$), using the classical damping/frequency representation vs. speed, parametrized with respect to the different modes, whose characteristic frequencies have already been reported in Figure 24. The data picked from the analogous representations extended to different scale factors (Figure 26c); there, minimal deviations can be appreciated in terms of flutter speed. On the contrary, as expected, the resonance frequencies that resulted were dramatically affected by the sc , as shown in Figure 26a,b. A quasi-exponential relation vs. sc was found, for both the constraint conditions, for the first six considered eigenvalues. It should be remarked that those latter plots are built for more scale factor values than the initially defined set of four (0.5, 1, 1.5, and 2), to give the curves a better regularity; this operation is supported by the relative simplicity of the adopted model.

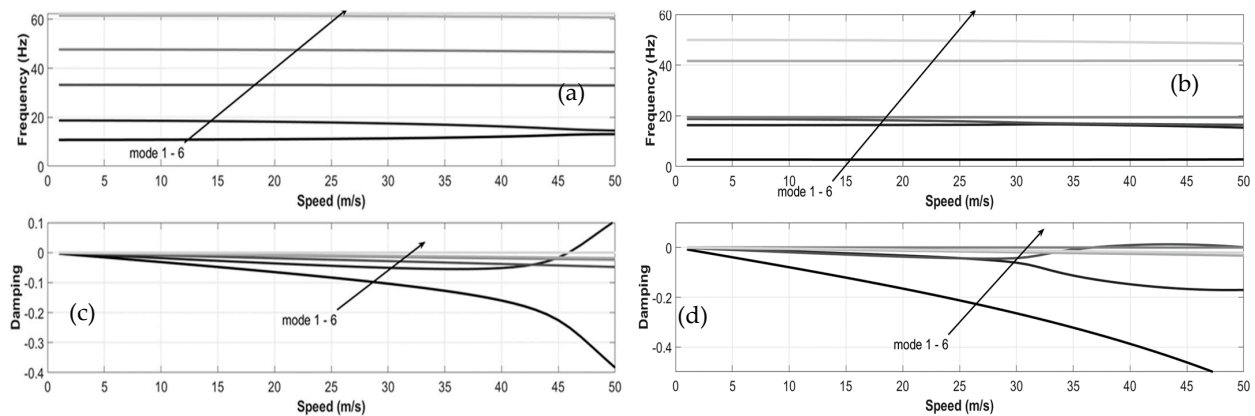


Figure 25. Flutter speed and damping in the clamp-pinned condition (a,c) and clamp-free configurations (b,d).

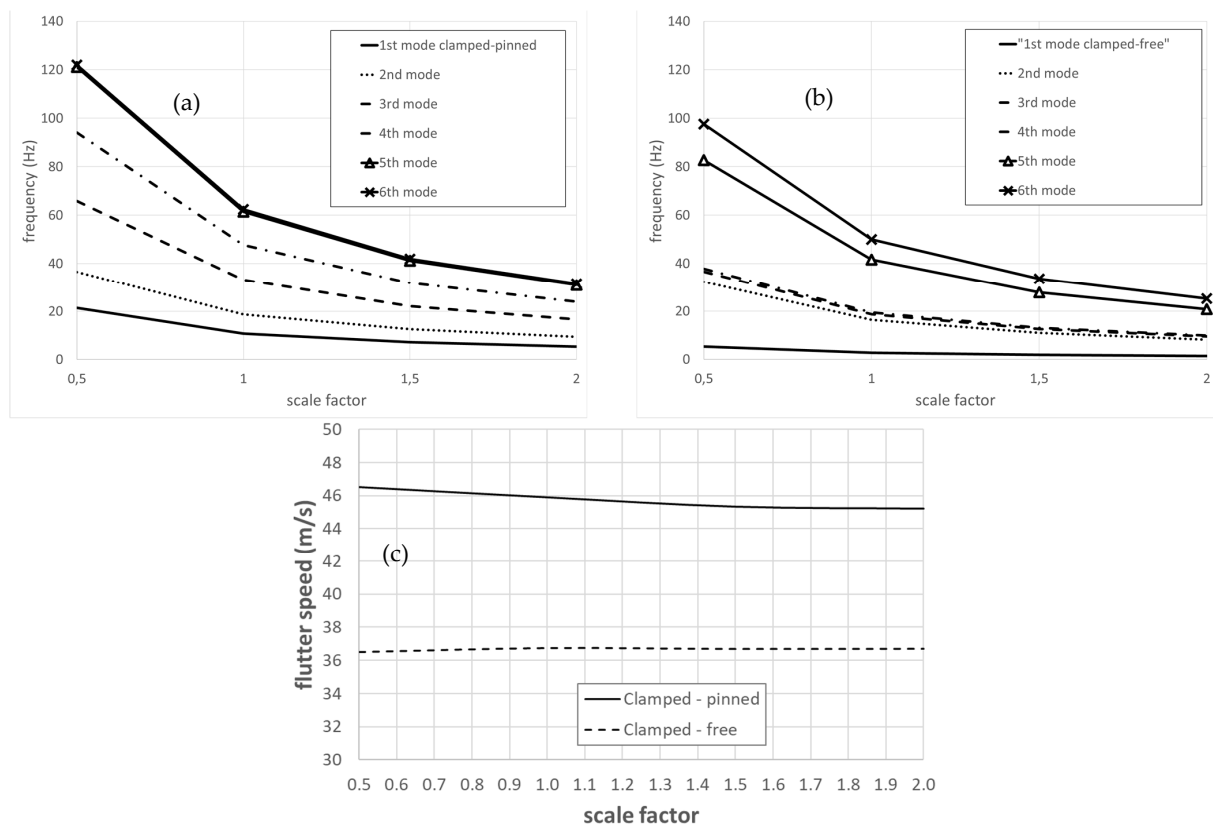


Figure 26. Normal frequencies vs. sc for the two constraint types (clamp-pinned (a) and clamp-free (b)) and flutter speed (c).

Flutter may be seen as the convergence of two different modes, a bending one and a torsional one. As it can be seen in the predictions, it seems that the change in frequency, following a different scale factor, is more or less homogeneous, so that the two modes exhibit the same variations. This is confirmed by the plot in Figure 26 (eigenfrequency as a function of sc), as well as in the Campbell diagram shown in the next section. As it can be seen in Figure 10, the curves are regular enough and, in spite of the different variability (quadratic vs. linear), appear to be in the same range. In other words, their numerical values remain comparable and do not differ that much from one another. This leads to a substantial compensation of such variations that ends up with the expression of flutter speed, which is namely constant along the considered range. This result is surprising enough, but it is extremely positive in terms of the scaling effect, with the aim of reserving the system's characteristics in this new scenario. It seems clear that such an occurrence shall be verified for larger and smaller values of the sc . This would then deserve a further study devoted to this specific point.

8.2. Rotor-Dynamic Behaviour

A further model of the blade segment was realised for the rotor-dynamic analyses. It is significantly different from the former ones, for instance, in terms of span length (Figure 27). In this case, in fact, it is necessary to expand its extension to properly address the rotary-associated phenomena. Their effect would have been very different for squat blades. The section is kept constant and equal to that of the former FE model. The structure is modelled through 50 beam elements along the span (x direction), while the aerofoil chord is oriented along the y -axis. Blade system rotation occurs around the z -axis, at the edge representing the root section. The rotor-dynamic solution of MSC/Nastran, SOL 110, is implemented to estimate the normal frequencies of the main modes affected by rotational speeds ranging from 0 to 50 rps, for each considered scale factor.

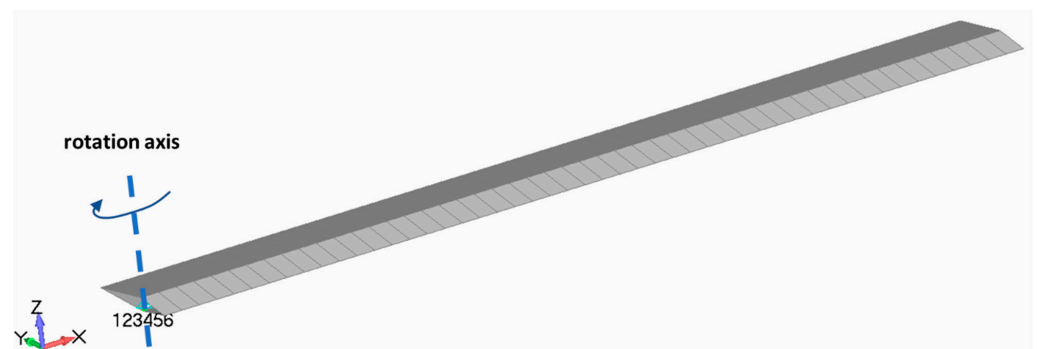


Figure 27. Simplified FE model for the computation of the rotor Campbell diagram.

A comparison among the Campbell diagrams for the different sc values is organised and reported in Figure 28. As it can be seen, the geometrical modification does affect both the eigenfrequencies and the critical points' locations, identified as those where the characteristic curves intersect, which can be associated with potential aeroelastic instability regions. At the nominal rotative speed (5 rps), the curves representing the fourth flap bending (F4) and the third torsional (T3) modes almost intersect (Δ) for $sc = 1.5$; the same situation occurs, for $sc = 2$, for the pair of curves, respectively representing the third flap bending (F3) and the second torsional (T2) modes ($\square$) and the second lag bending (L2) and the third torsional (T3) modes ($\hexagon$). It can be said that, generally, some critical points approach the nominal operation condition as the scale factor increases. This is due to different dependences on the scale factor of the flap bending, lag bending, and torsional rigidities. This happens for the already reported intersections ($\square$) and ($\hexagon$), which dramatically approach the nominal speed as the scale factor increases. A detail concerning the first flapping and torsional modes, F1 and T1, is finally reported in Figure 29.

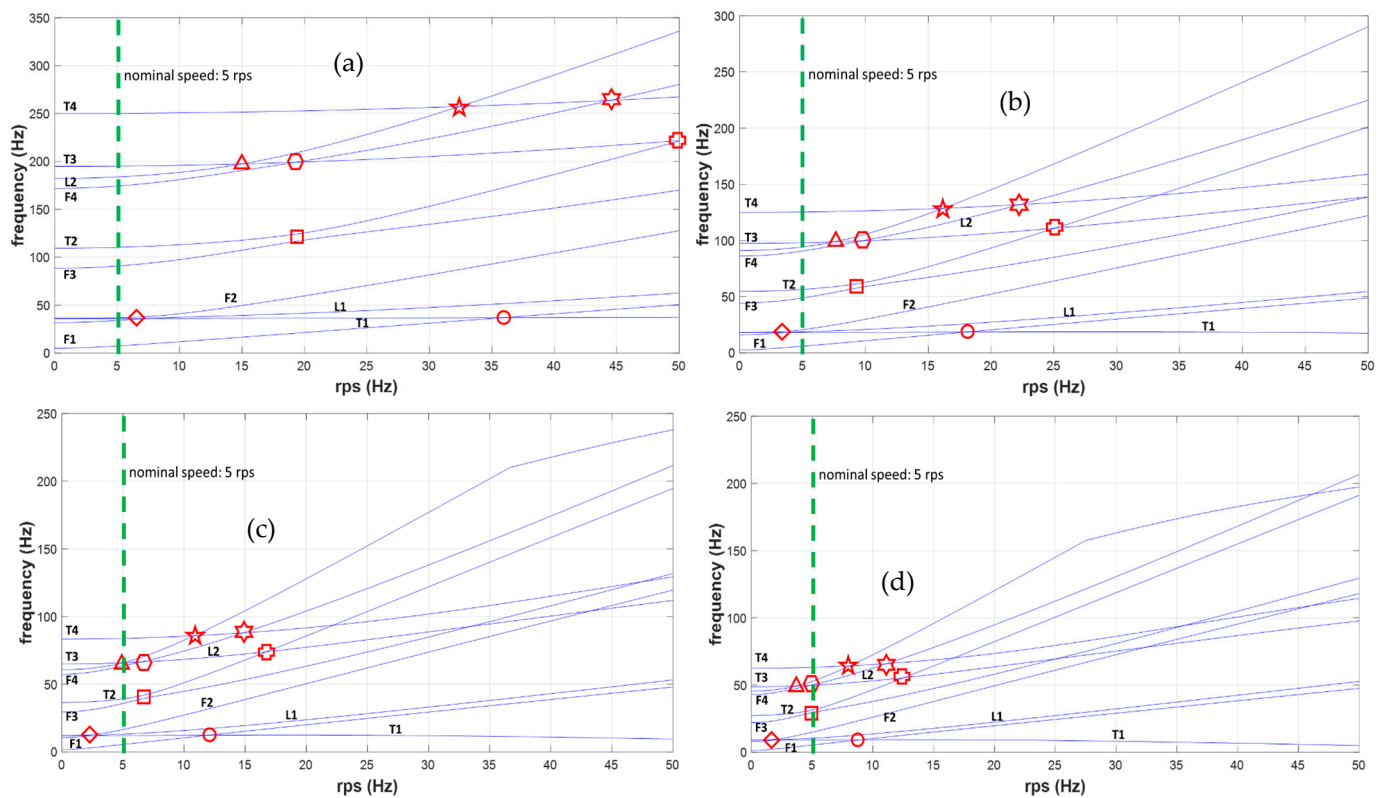


Figure 28. Campbell diagram vs. scale factor 0.5 (a), 1.0 (b), 1.5 (c), and 2.0 (d). Meaning of the red markers have been defined in the text above.

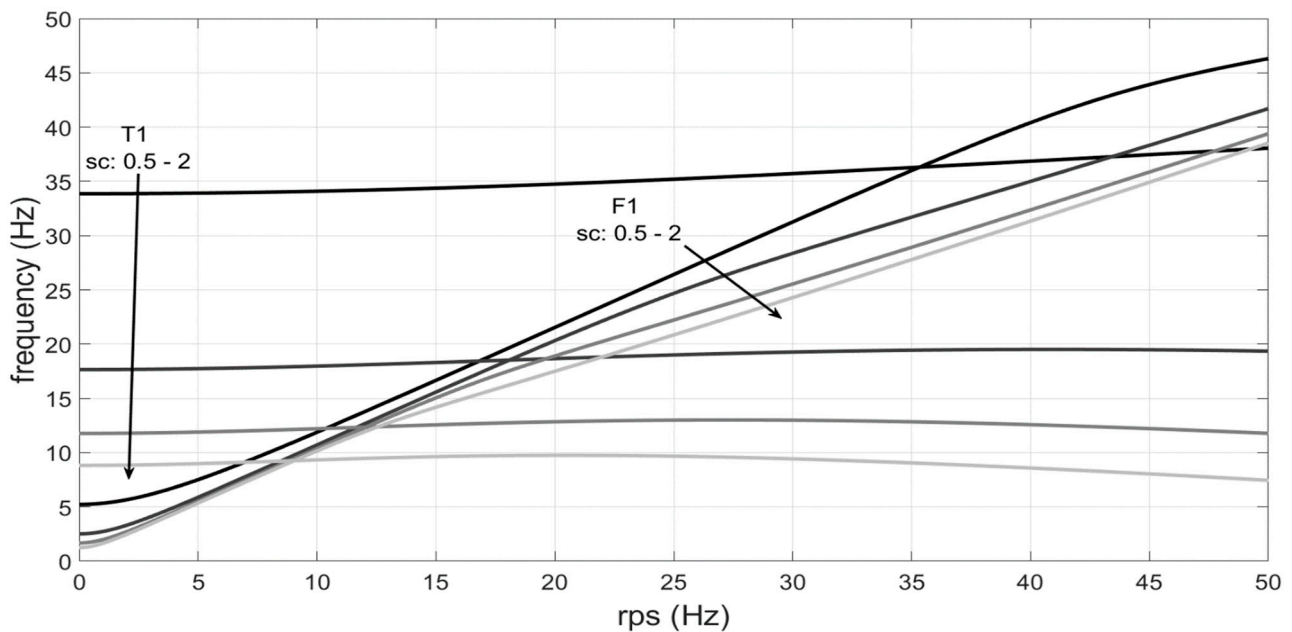


Figure 29. Flapping and torsional modes F1 and T1 vs. scale factor vs. blade speed.

9. Corrective Actions: A Preliminary Approach

The investigations shown in the previous sections highlighted the relevant effects caused by the scale factor on the aero-structural response of the referred adaptive system, with a specific focus on the actuator's behaviour. It is worth mentioning that this is among the most important aspects of this study, since it directly affects the performance of the driving system, which is the core of any smart structure.

The scope of this section is to summarise the outcomes of the performed analysis on the scaling issues, carried out on a set of selected parameters, and explore the benefits and limitations of actions aimed at compensating the observed effects, with the aim of reproducing the features of the reference model (scaled 1:1) to the maximum extent. While it is expected that a single solution would not solve all the possible problems, it may be relevant to understand what level of effectiveness specific modifications may attain in minimizing deviations from the nominal behaviour. In order to proceed, it comes out that it is first necessary to assess the parameters intended to represent the nominal behaviour, which is in itself a first focus of specific structural aspects. In this regard, adjustments of the axial, flap bending, lag bending, and torsional stiffnesses per unit span are herein considered.

Among the many possibilities, the preliminary approach adopted and herein reported is based on the idea of modifying the general stiffness of the tested article. In this case, this purpose is realized by adding or removing internal beam-like elements in the FE model, deployed along the span at different chord stations. Such elementary parts, made of three different possible materials (LPDE, low density polyethylene; PTFE, polytetrafluoroethylene; and steel), have the function of being classical longitudinal stiffeners, affecting all the cited elastic characteristics. Their cross-sections are sized with edges smaller than 5% of the uniform airfoil chord. To increase the manipulation degrees of freedom, it is also considered to modify the constitution of the skin and the ribs by alternatively using the PTFE, the material used for the actual prototype, manufactured within the cited SABRE project, or the LPDE. The latter, characterised by a Young modulus equal to about half of the former, is used for sc values higher than one, since the simple stiffeners' removal is not expected to be enough to reasonably approach the target. The performed modifications are synthetically reported in Table 3.

Table 3. Modifications implemented to obtain similar mechanical features to the reference (full size model). Materials' Young moduli: steel: 210 GPa; PTFE: 540 MPa; LPDE: 250 MPa.

Parameter	Value for $sc = 0.5$	Value for $sc = 1.0$ (Nominal)	Value for $sc = 1.5$	Value for $sc = 2.0$
Scale factor	0.5	1.0 Nominal	1.5	2.0
Material-Model	PTFE	PTFE	LPDE	LPDE
Flap bending stiffness correction				
Additional area (m ²)	8.45×10^3	0	3.80×10^{-4}	6.76×10^{-04}
Distance from the shear centre normal to chord line (m)	3.59×10^{-3}	0	1.90×10^{-2}	7.29×10^{-3}
Material of the additional area	Steel	0	PTFE	PTFE
Added (+) or removed (−)	+	NA	—	—
Lag bending stiffness correction				
Additional area (m ²)	3.95×10^{-5}	0	1.22×10^{-4}	6.76×10^{-4}
Distance from the shear centre in chord direction (m)	5.08×10^{-3}	0	1.39×10^{-2}	2.15×10^{-2}
Material of the additional area	Steel	0	PTFE	PTFE
Added (+) or removed (−)	+	NA	—	—

The achieved results are illustrated in Figure 30. The dark blue bars represent the outcome of the analysis, performed on the original models and achieved via a direct application of the geometric scale factor. The values achieved for $sc = 1$ are, of course, assumed as the reference and the aimed target. In the same picture, the corresponding level is highlighted by a light, dashed, horizontal red line, for the sake of clarity. The bright yellow bars illustrate the effects of the implementation of the cited adjustment strategy, following the data illustrated in Table 3. It results that the torque, axial, lag bending, and flap bending stiffnesses per unit span are clearly improved, although it is not possible to reach a perfect compensation.

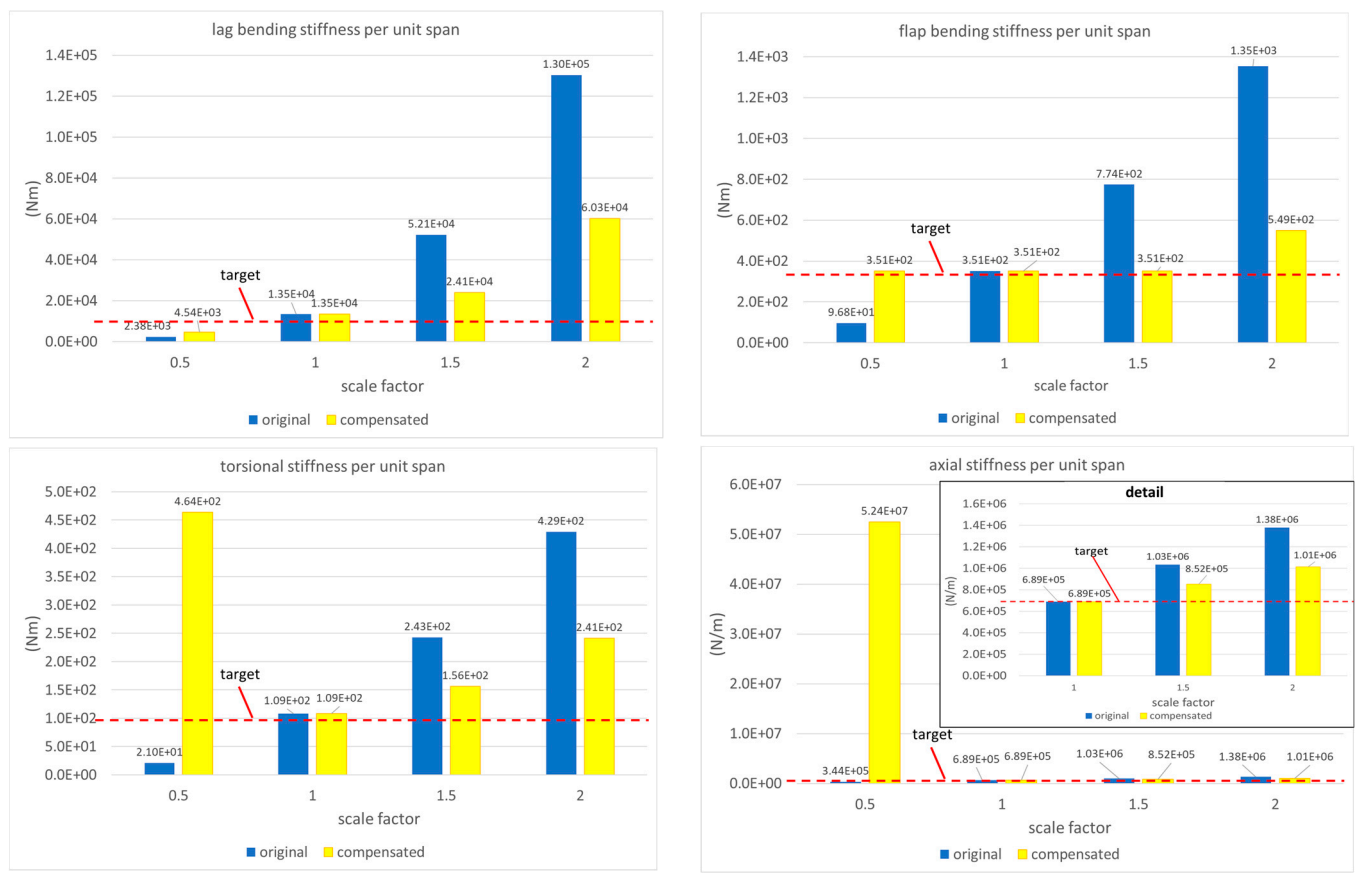


Figure 30. Compensation of the stiffness parameters to approach the full-size values.

The best enhancements were found for the flap bending stiffness, as the target was almost fully matched as $sc = 0.5$ and $sc = 1.5$. Acceptable results were also achieved for $sc = 2$, as a deviation of 56% was reported with respect to the target. The lag bending stiffness exhibited deviations that were similar in trend, but definitely worse than the flap bending stiffness, and the interventions did bring only limited benefits. Relative to that parameter, a factor of six was attained for $sc = 2$, three for $sc = 1.5$, and 1/3 for $sc = 0.5$. For scale factors larger than one, the torsional rigidity appears to be strongly influenced by the lag bending stiffness (predominant with respect to the flap bending stiffness), while at $sc = 0.5$, the additional mass is able to produce relevant effects. A similar behaviour was observed for the axial stiffness; however, the imposed variations led to acceptable results for scale factors larger than one.

The dynamic behaviour of the modified configurations was then explored, with respect to a cantilever arrangement. The comparison among the first six eigenfrequencies is reported in Table 4, together with the flutter speeds, if occurring within the established operational range (0–50 m/s).

The largest deviations were found for the lowest scale factor, with a first eigenfrequency value that was almost an order of magnitude different to that of the reference (22.3 vs. 2.4 Hz). For this sc , no flutter was observed in the observation interval. Smaller deviations were instead obtained for the larger sc values. Generally, if larger frequency values are experienced, further lumped masses can be added to reduce those values. These considerations do have a strong limitation in considering only the eigenvalue, while, for a global comparison process, the modal shapes should also be considered, with a dramatic increase in the complexity of the approach.

Table 4. Dynamic behaviour of the modified configurations vs. reference.

	Scale Factor			
	0.5	1.0 Nominal	1.50	2.0
mode 1 (Hz)	22.27	2.39	1.97	5.85
mode 2 (Hz)	38.21	12.61	11.79	10.21
mode 3 (Hz)	64.95	14.10	13.00	17.62
mode 4 (Hz)	83.35	17.24	13.79	24.80
mode 5 (Hz)	101.75	41.90	29.11	30.91
mode 6 (Hz)	132.15	48.83	34.88	32.04
flutter (m/s)	no flutter	36.40	no flutter	48.48

The proposed preliminary approach herein presented is a first example of what can be done to adjust relevant deviations in the structural behaviour, both static and dynamic, deriving a direct application of geometrical scale factors. The perhaps major constraint is that such modifications shall not affect the basic configuration, i.e., the shape, or even the internal architecture, otherwise bringing further unknowns into the process and moving the scaled model still further from the reference one. Always valid, this statement is exceptionally important for morphing systems, whose inner architecture is intrinsically complex and aimed at satisfying a number of requirements that are non-limited to the structural ones. The proposed methodology may only be partially extended to general cases, but has the merit of showing the feasibility of reducing the effects of the scaling process by paying a partial penalty to the system's re-sizing. In the same way, it can be stated that the use of lumped elements seems adequate for generic rotorcraft blades of usual size and limited scale factors, and could be suitably used for many similar problems.

10. Conclusions and Further Steps

The architecture of an integrated shape adaptation system is deeply affected by size modifications; it can be stated that moving from a certain dimension to others may imply the complete re-design of the envisaged architecture, until reaching the arrival of a completely new device, characterized by very different performance capabilities.

By taking a full-scale adaptive blade model as a reference that is representative of a working prototype manufactured and validated within the SABRE project (H2020), this work quantifies the impact of a geometric scaling process on the main features of a morphing system. This operation may have significant effects on the assessment of scaled demonstrators for laboratory or wind tunnel tests, for instance. The resizing process may affect either the actual exploitability of a specific morphing concept on vehicles of different classes (f.i. moving from a small UAV to a regional aircraft, and vice versa) or nature (ships, boats, automobiles, heavy transport, and so on).

The exportability of the proposed benchmark concept from full-scale models to sizes compatible with laboratory tests, as well as whirl towers or wind tunnel environments, is studied. As expected, the consequences of the resizing process are relevant, and preliminarily quantified for many targeted characteristics, like normal and bending stiffness, modal and aeroelastic parameters, and even aerodynamic behaviour features. Such representing factors are shown to change a lot, and in a wide range.

It seems useful to report in the form of a synthetic table (Table 5) the results that have been found for the different parameters that were explored in this study. For the sake of clarity, it is necessary to state, once again, that such results may be directly applied only to architectures similar to the ones herein exposed, while other investigations are necessary to confirm or expand the evidence that has been herein found and discussed. Trivially, as the studied morphing system is largely dependent on the scale factor, its behaviour is also dependent on this intimate architecture.

Table 5. Synthesis of the dependencies of different representative parameters on the static and dynamic structural behaviour, with respect to the scale factor.

Parameter	Dependency on sc	Beam Theory Prediction
Normal stiffness	Linear, growing	Linear
Bending flap stiffness	Linear, growing	Linear
Bending lag stiffness	Square root, growing	Linear
Torsion	Cubic, growing	Cubic
Reaction force, Reynolds	Quadratic, growing	Quadratic
Stress Levels, $1/M$	Exponential, decreasing	-
Recovered twist angle	Quadratic, parabolic	-
Actuator torque stiffness	Exponential, growing	-
Activation Time	Square root, growing	-
Eigenvalues	Quadratic, decreasing	-
Flutter speed	Constant	-
Eigenmodes density	Increasing	-

As it can be noted, the characteristic parameters exhibit a tremendous variability of behaviour, which makes it difficult to compensate each modification and consequence of the scaling process. This is clearly evidenced in the subsequent study, reported at the very end of this paper, which is aimed at showing the general consequences of certain choices on scaled architectures in terms of characteristic stiffness, based on changes in the used materials, with differences for each scale factor. Again, it seems interesting to summarise those attainments in a table format (Table 6):

Table 6. Qualitative effects of adjustments, carried out in terms of material modification, to make the behaviour of the scaled systems approach the nominal one.

sc	Axial	Flap Bending	Lag Bending	Torsion
0.5	Satisfactory	Excellent	Inadequate	Inadequate
1.0	-	-	-	-
1.5	Satisfactory	Excellent	Satisfactory	Satisfactory
2.0	Poor	Satisfactory	Poor	Satisfactory

In terms of the bar chart in Figure 31, the following values are attributed to the adopted scale:

- Inadequate: 1
- Poor: 2
- Sufficient: 3
- Adequate: 4
- Excellent: 5

It highlights the performances of the adjustments which change dramatically, with regards to both the kind of stiffness analysed and the value of the scale factor (in the picture, $sc = 1$ represents the reference value). A global, immediate view allows for the appreciation that small size increases do not affect the representativeness of the model a lot, and its behaviour may be saved somehow. Instead, for higher or lower values of sc , such variations become significant, moving the behaviour of the system away from that of the nominal one. It is also relevant to observe, as these modifications are different for different stiffness parameters. Since these ones are the bricks and mortar that drive other parameter variations, their behaviour is clearly critical in the trial of making scaled systems that are fully representative of their references.

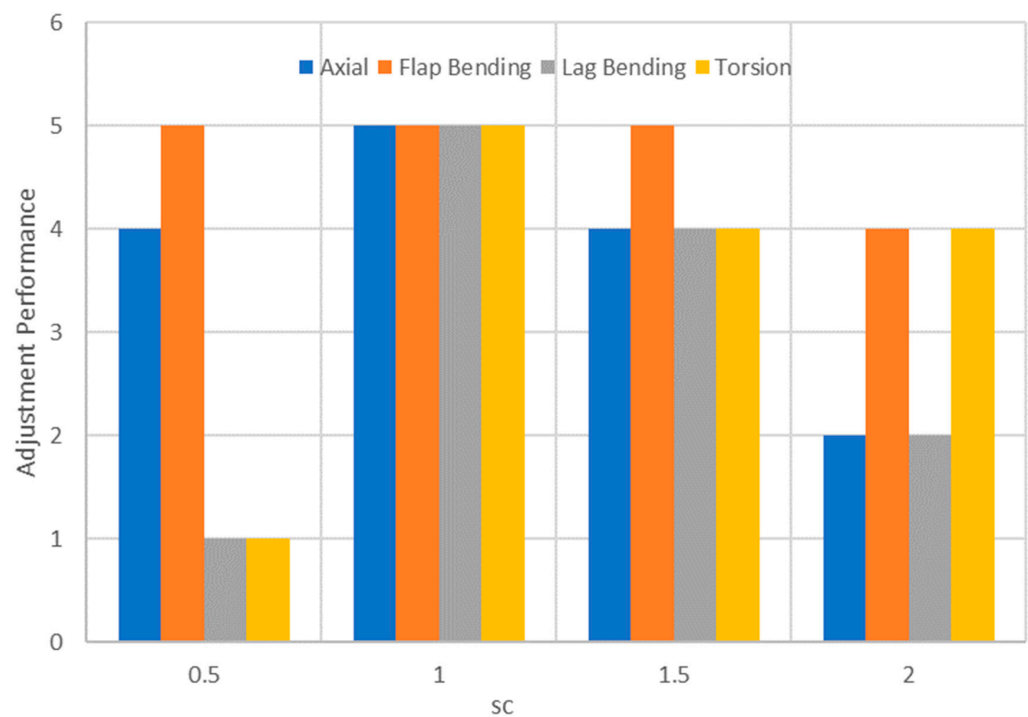


Figure 31. Adjustment effectiveness vs. *sc* for the different stiffness types herein analysed (axial, flap, lag bending, and torsional).

In spite of this specific investigation, carried out on a single type of morphing architecture, it is believed that the outcomes of this study may be somehow generalized, with the aim of creating preliminary guidelines for analogous problems.

One of the consequences of the evidence found in this study (intuitively acceptable) is that a morphing concept should be intimately modified as the architecture is re-sized to match the original characteristics (which should, in turn, be defined; in other words, a selection of those features should be referred to). This is mainly due to the fact that the morphing architecture (layout, outline, elements arrangement, etc.) and characteristics (stiffness, transmitted power, etc.) strictly depend on the overall dimensions (including volumes and external constraints).

As the next steps of this investigation, the authors want to consider further details of the adaptive structures' arrangement, trying to integrate other features in the process, like sensor and driving systems characteristics. As a natural continuation of the work herein presented, they would aim to provide measures to quantify the effectiveness of the selected compensation processes, to classify and identify the most affordable ones. Preliminary criteria to appreciate an objective distance from the target solution would also be introduced. At the same time, it is necessary to export the assessing process towards other kinds of morphing systems, to increase confidence and raise the generality of the outcomes, with the final scope of assessing guidelines to handle the complex process of the scaling. It is expected that the final results should ultimately show how any morphing architecture has its own applicability domain. It is not of such a case that insects', birds', and extinct reptiles' wings are very different in their structures and ways of working.

Manufacturing technologies should be also addressed; they can be dramatically affected by the scaling process and could lead to very different mechanical performances and costs. Actuation is another issue, since it is not possible to scale down or up an actuator and keep its performance within a desired operational range, beyond a certain limit. Related to the enforced motion, load transmission is another relevant matter, involving the re-design of adequate interfaces, with suitable stiffness and load-bearing properties.

In the present work, a compliant structure is investigated; kinematic mechanisms may cause further questions that are related to *sc* to arise. For instance, free-play angles and gaps may have an impact on actuation, depending on the sub-parts' sizes and characteristics; this could require specific mitigation actions in terms of machining and accuracy.

Speaking of adaptive structures, other subsystems such as sensing and logic should be considered. Scaling a sensing network may have relevant effects on both detection performance and layout definition. Size and stiffness changes may suggest the choice of different kinds of sensible elements, and pose further integration problems. Dynamic behaviour variations and the combined use of different materials may require a reinterpretation of the original control logic and data processing.

Author Contributions: Conceptualization, A.C. and S.A.; methodology, S.A. and A.C.; writing—original draft preparation, S.A. and B.G.; writing—final version, all authors; visualization, B.G. and S.A.; supervision, A.C.; funding acquisition, S.A.; resources: A.C. All authors have read and agreed to the published version of the manuscript.

Funding: This work was funded by the European Research Council (ERC) under the European Union's Horizon 2020 research and innovation programme, as part of the Shape Adaptive Blades for Rotorcraft Efficiency (SABRE) project (Grant Agreement No. 723491).

Data Availability Statement: Data used in this article cannot be shared because of confidentiality clauses.

Conflicts of Interest: The authors declare no conflict of interest.

Acronyms and Symbols

0	subscript for full size parameters
A	cross-section
$A \rightarrow M$	austenite into martensite transformation
A_f	austenite end of transformation temperature
A_s	austenite start of transformation temperature
c	scaled chord
γ	shear strain
$\Delta\theta$	twist
E	Young modulus
E_a	Young modulus in austenite phase
E_m	Young modulus in martensite phase
θ_{pt}	pre-twist angle
F	force applied at the free edge of the beam
ϕ	angular coordinate
$F_{1,2,3,4}$	flap modes 1, 2, 3, 4
F_{axial}	spanwise concentrated force applied to the model for axial stiffness estimation
$flap$	flap direction, component
FEM	finite element model, finite element method
G	shear modulus
I	inertia moment of the beam cross-section along the flap and lag axis
J	polar inertia moment of the beam cross-section
K_{axial}	spanwise stiffness
L	span length
$L_{1,2,3,4}$	lag modes 1, 2, 3, 4
lag	lag direction, component
M	Mach number
μ	dynamic viscosity of the air
$M \rightarrow A$	martensite into austenite transformation
M_f	martensite end of transformation temperature
MPC	multi-point constraint
M_s	martensite start of transformation temperature

PVDF	polyvinylidene fluoride
r	radial coordinate
R^2	coefficient of determination
Re	Reynolds number
ρ	air density at sea level
σ_f^{AS}	martensite end of transformation stress
σ_s^{AS}	martensite start of transformation stress
σ_f^{SA}	austenite end of transformation stress
σ_s^{SA}	austenite start of transformation stress
sc	geometric scale factor
SMA	shape memory alloy
SMR	small-medium range (aircraft)
T	torque moment applied at the free edge
$T_{1,2,3,4}$	torsional modes 1, 2, 3, 4
TRL	technology readiness level
u_{axial}	spanwise displacement
UAV	unmanned aerial vehicle
w	transversal displacement of the representative elastic beam
WhT	whirl tower
WT	wind tunnel
x	axial coordinate

References

1. Raconteur. Available online: www.raconteur.net/business-innovation/aviation-industry-soaring-into-the-future (accessed on 9 July 2023).
2. ICAO. Available online: www.icao.int/sustainability/Pages/Economic-Impacts-of-COVID-19.aspx (accessed on 9 July 2023).
3. Green, J.E. Civil aviation and the environmental challenge. *Aeronaut. J.* **2003**, *107*, 281–299. [CrossRef]
4. SNC-Lavalin. Available online: <https://www.snclavalin.com/~media/Files/S/SNC-Lavalin/download-centre/en/whitepaper/aviation-trends-white-paper-digital.pdf> (accessed on 9 July 2023).
5. Allianz-Trade. Available online: https://www.allianz-trade.com/en_global/news-insights/economic-insights/the-world-is-moving-east-fast.html (accessed on 9 July 2023).
6. Concilio, A.; Lecce, L. Historical Background and Current Scenario. In *Morphing Wing Technologies: Large Commercial Aircraft and Civil Helicopters*; Concilio, A., Dimino, I., Lecce, L., Pecora, R., Eds.; Elsevier: Amsterdam, The Netherlands, 2018; pp. 401–428.
7. The Conversation. Available online: <https://theconversation.com/morphing-is-one-way-to-make-aircraft-more-efficient-23683> (accessed on 9 July 2023).
8. Ameduri, S.; Concilio, A.; Dimino, I.; Pecora, R.; Ricci, S. Airgreen2—Clean Sky 2 Programme: Adaptive Wing Technology Maturation, Challenges and Perspectives. In Proceedings of the ASME Conference on Smart Materials, Adaptive Structures and Intelligent Systems, San Antonio, TX, USA, 10–12 September 2018; Paper No. SMASIS2018-8235.
9. Clean Sky 2 Joint Undertaking. Available online: <https://www.clean-aviation.eu/> (accessed on 9 July 2023).
10. Cordis. Available online: <https://cordis.europa.eu/project/id/284562> (accessed on 9 July 2023).
11. TsAGI. Available online: <http://tsagi.com/pressroom/news/2192/> (accessed on 9 July 2023).
12. Cordis. Available online: <https://cordis.europa.eu/project/id/213442/it> (accessed on 9 July 2023).
13. European Commission. Available online: https://ec.europa.eu/research-and-innovation/en/projects/success-stories?id=/research/star/index_en.cfm?p=s1-sade&calledby=infocentre&item=Infocentre&artid=40597 (accessed on 9 July 2023).
14. SABRE. Available online: <https://sabreproject.eu/> (accessed on 9 July 2023).
15. Cordis. Available online: <https://cordis.europa.eu/project/id/502773/reporting> (accessed on 9 July 2023).
16. NASA. Available online: <https://www.nasa.gov/centers/armstrong/news/newsreleases/15-08.html> (accessed on 9 July 2023).
17. Federal Aviation Administration. Available online: <https://www.faa.gov/newsroom/continuous-lower-energy-emissions-and-noise-clean-program?newsId=22534> (accessed on 9 July 2023).
18. Botez, R.M.; Koreanschi, A.; Gabor, O.S.; Tondji, Y.; Guezguez, M.; Kammegne, J.T.; Grigorie, L.T.; Sandu, D.; Mebarki, Y.; Mamou, M.; et al. Numerical and Experimental Transition Results Evaluation for a Morphing Wing and Aileron System. *Aeronaut. J.* **2018**, *122*, 747–784. [CrossRef]
19. Dimino, I.; Ameduri, S.; Concilio, A. Preliminary Failure Analysis and Structural Design of a Morphing Winglet for Green Regional Aircraft. In Proceedings of the ASME 2018 Conference on Smart Materials, Adaptive Structures and Intelligent Systems, San Antonio, TX, USA, 10–12 September 2018; Paper No. SMASIS2018-8236.
20. Ondra, V.; Dibble, R.; Titurus, B. Towards an Application of an Active Tendon in Rotorcraft: A Numerical and Experimental Study of Coupled Bending-Torsion Vibration of a Beam-Tendon System. In Proceedings of the 28th International Conference on Noise and Vibration engineering (ISMA 2018), Leuven, Belgium, 17–19 September 2018; pp. 3645–3659.

21. Zhang, J.; Shaw, A.D.; Amoozgar, M.; Friswell, M.I.; Woods, B.K.S. Spiral Pulley Negative Stiffness Mechanism for Morphing Aircraft Actuation. In Proceedings of the ASME 2018 International Design Engineering Technical Conferences and Computers and Information in Engineering Conference, Quebec City, QC, Canada, 26–29 August 2018; Paper No: DETC2018-85640; ASME: New York, NY, USA, 2018; p. 05.
22. Fournier, S.; Woods, B.K.S. Surrogate Modelling of the Steady Aeroelastic Performance of the Fish Bone Active Camber Concept. In Proceedings of the AIAA Scitech 2019 Forum, San Diego, CA, USA, 7–11 January 2019; AIAA: Reston, VA, USA, 2019; Paper No: AIAA 2019-1859. [\[CrossRef\]](#)
23. NASA. Available online: <https://ntrs.nasa.gov/api/citations/20170005794/downloads/20170005794.pdf?attachment=true> (accessed on 9 July 2023).
24. DAU. Available online: <https://www.dau.edu/cop/stm/lists/tools/allitems.aspx> (accessed on 9 July 2023).
25. Cordis. Available online: <https://cordis.europa.eu/project/id/323460/reporting> (accessed on 9 July 2023).
26. Cordis. Available online: <https://cordis.europa.eu/project/id/285395> (accessed on 9 July 2023).
27. Ajaj, R.M.; Beaverstock, C.S.; Friswell, M.I. Morphing Aircraft: The Need for a New Design Philosophy. *Aerosp. Sci. Technol.* **2016**, *49*, 154–166. [\[CrossRef\]](#)
28. Ameduri, S.; Concilio, A. A Shape Memory Alloy Torsion Actuator for Static Blade Twist. *J. Intell. Mater. Syst. Struct.* **2019**, *30*, 2605–2626. [\[CrossRef\]](#)
29. Ameduri, S.; Concilio, A. Morphing Wings Review: Aims, Challenges, and Current Open Issues of a Technology. *Proc. Inst. Mech. Eng. Part C J. Mech. Eng. Sci.* **2020**, *234*, 3571–3595. [\[CrossRef\]](#)
30. Boyraz, P.; Runge, G.; Raatz, A. An Overview of Novel Actuators for Soft Robotics. *Actuators* **2018**, *7*, 48. [\[CrossRef\]](#)
31. Xia, J.; Durfee, W.K. Analysis of Small-Scale Hydraulic Actuation Systems. *J. Mech. Des.* **2013**, *135*, 091001. [\[CrossRef\]](#)
32. Liu, D.K.-C.; Friend, J.; Yeo, L. A brief review of actuation at the micro-scale using electrostatics, electromagnetics and piezoelectric ultrasonics. *Acoust. Sci. Technol.* **2010**, *31*, 115–123. [\[CrossRef\]](#)
33. Potapov, P.L.; Da Silva, E.P. Time Response of Shape Memory Alloy Actuators. *J. Intell. Mater. Syst. Struct.* **1999**, *10*, 47–54.
34. Funakubo, H. *Shape Memory Alloys*; Taylor & Francis: London, UK, 1987; ISBN 9782881241369.
35. Concilio, A.; Antonucci, V.; Auricchio, F.; Lecce, L.; Sacco, E. (Eds.) *Shape Memory Alloy Engineering: For Aerospace, Structural, and Biomedical Applications*, 2nd ed.; Elsevier: Amsterdam, The Netherlands, 2021; ISBN 9780128192648.
36. Cordis. Available online: <https://cordis.europa.eu/project/id/296617/it> (accessed on 25 August 2023).
37. Karagiannis, D.; Stamatelos, D.; Kappatos, V.; Spathopoulos, T. An investigation of shape memory alloys as actuating elements in aerospace morphing applications. *Mech. Adv. Mater. Struct.* **2017**, *24*, 647–657. [\[CrossRef\]](#)
38. Cordis. Available online: <https://cordis.europa.eu/project/id/267679> (accessed on 25 August 2023).
39. Ameduri, S.; Concilio, A.; Pecora, R.; Karagiannis, D. A Single Slotted Morphing Flap Based on SMA Technology. *Smart Struct. Syst.* **2016**, *17*, 819–835. [\[CrossRef\]](#)
40. Walgren, P.; Bertagne, C.; Wescott, M.; Benafan, O.; Erickson, L.; Whitcomb, J.; Hartl, D. Development and Testing of a Shape Memory Alloy-Driven Composite Morphing Radiator. *Shape Mem. Superelasticity* **2018**, *4*, 232–241. [\[CrossRef\]](#)
41. Lienhard, J.; Knippers, J. Considerations on the Scaling of Bending-Active Structures. *Int. J. Space Struct.* **2013**, *28*, 137–148. [\[CrossRef\]](#)
42. Lesieutre, G.A.; Browne, J.A.; Frecker, M.I. Scaling of performance, weight, and actuation of a 2-D compliant cellular frame structure for a morphing wing. *Smart Mater. Struct.* **2004**, *13*, 785–795.
43. Zhang, J.; Andersson, J.A.E.; Friswell, M.I.; Cooper, J.E. Scalability Assessment of Morphing Aerospace Systems. *J. Aircr.* **2020**, *57*, 1167–1181.
44. Krishnan, R.; Zhang, J.; Cooper, J.E.; Friswell, M.I. Morphing Aerospace Systems: A Roadmap to Commercialization. *J. Aircr.* **2021**, *58*, 1–16. [\[CrossRef\]](#)
45. Gerdes, J.W.; Gupta, S.K.; Wilkerson, S.A. A Review of Bird-Inspired Flapping Wing Miniature Air Vehicle Designs. *J. Mech. Robot.* **2012**, *4*, 021003. [\[CrossRef\]](#)
46. Talmale, R.; Bhat, M.N. Energy Attentive and Pre-Fault Recognize Mechanism for Distributed Wireless Sensor Network Using Fuzzy Logic Approach. *Wirel. Pers. Commun.* **2021**, *124*, 1263–1280. [\[CrossRef\]](#)
47. Hameed, M.S.S.; Suganthi, N.; Balakrishnan, S. Cell-Leach Based Wireless Sensor Network for Optimized Energy Consumption. *Int. J. Adv. Sci. Technol.* **2021**, *30*, 111–120.
48. van Gent, P.L.; Michaelis, D.; van Oudheusden, B.W.; Weiss, P.-É.; de Kat, R.; Laskari, A.; Jeon, Y.J.; David, L.; Schanz, D.; Huhn, F.; et al. Comparative assessment of pressure field reconstructions from particle image velocimetry measurements and Lagrangian particle tracking. *Exp. Fluids* **2017**, *58*, 33. [\[CrossRef\]](#)
49. Gritli, W.; Gharsallaoui, H.; Benrejeb, M. A New Methodology for Tuning PID-Type Fuzzy Logic Controllers Scaling Factors Using Genetic Algorithm of a Discrete-Time System. In *Modern Fuzzy Control Systems and Its Applications*; Ramakrishnan, S., Ed.; In Tech Open: London, UK, 2017. [\[CrossRef\]](#)
50. Ameduri, S.; Ciminello, M.; Concilio, A.; Dimino, I.; Galasso, B.; Guida, M.; Miceli, M.F.; Riemenschneider, J.; Kalow, S.; Luebker, J.; et al. Whirl Tower Demonstration of an SMA Blade Twist System. *Actuators* **2022**, *11*, 141. [\[CrossRef\]](#)
51. Ameduri, S.; Ciminello, M.; Concilio, A.; Dimino, I.; Galasso, B.; Guida, M.; Riemenschneider, J.; Woods, B.K.S. SMA blade twist system: From the requirements to the demonstration in relevant environment. In Proceedings of the SPIE Conference 12483, Long Beach, CA, USA, 12–16 March 2023; Paper No: 124831D. 2023. [\[CrossRef\]](#)

52. Pires, O.; Munduate, X.; Boorsma, K.; Ceyhan Yilmaz, O.; Aa Madsen, H.; Timmer, W.A. Experimental Investigation of Surface Roughness Effects and Transition on Wind Turbine Performance. *J. Phys. Conf. Ser.* **2018**, *1037*, 052018. [CrossRef]
53. Abdelmoula, A.; Rauleder, J. Aerodynamic Performance of Morphed Camber Rotor Airfoils. In Proceedings of the AIAA Scitech 2019 Forum, San Diego, CA, USA, 7–11 January 2019; AIAA: Reston, VA, USA, 2019; Paper No: 109650H. [CrossRef]
54. Academy of Model Aeronautics. Available online: <https://www.modelaviation.com/dealingwithcg> (accessed on 9 July 2023).
55. Hexagon. Available online: <https://simcompanion.hexagon.com/customers/s/article/msc-nastran-2014-1-1-reference-manual-doc11576> (accessed on 9 July 2023).
56. Femap. Available online: <https://plm.sw.siemens.com/en-US/simcenter/mechanical-simulation/femap/> (accessed on 28 August 2023).
57. Ameduri, S.; Concilio, A.; Galasso, B.; Gianvito, A. Development of a Conceptual Demonstrator of a SMA-based Rotorcraft Blade Twist System. In Proceedings of the Bioinspiration, Biomimetics, and Bioreplication IX, Denver, CO, USA, 2–7 March 2019; SPIE: Bellingham, WA, USA, 2019; Volume 10965; Paper No: 109650H. [CrossRef]
58. MSC Software Corporation. *Quick Reference Guide*; MSC Software Corporation: Newport Beach, CA, USA, 2015; pp. 2765–2768.
59. Auricchio, F.; Bonetti, E.; Scalet, G.; Ubertini, F. Theoretical and Numerical Modeling of Shape Memory Alloys Accounting for Multiple Phase Transformations and Martensite Reorientation. *Int. J. Plast.* **2014**, *56*, 19–42. [CrossRef]
60. Wertheimer, T.B.; Harvey, S.; Minaie, R. Recent Advances in Thermal Mechanically Coupled Analysis using MSC. In Proceedings of the Thermal and Fluids Analysis Workshop 2010, League City, TX, USA, 16–20 August 2010.
61. Mechanicalc Inc. Available online: <https://mechanicalc.com/reference/beam-deflection-tables> (accessed on 9 July 2023).
62. MIT. Available online: <https://web.mit.edu/drela/Public/web/xfoil/> (accessed on 9 July 2023).
63. Funakubo, H. *Shape Memory Alloys*; Gordon and Breach Science Publishers: New York, NY, USA, 1987; ISBN 978-2-88124-136-9.
64. Concilio, A.; Antonucci, V.; Auricchio, F.; Lecce, L.; Sacco, E. (Eds.) *Shape Memory Alloy Engineering: For Aerospace, Structural, and Biomedical Applications*, 2nd ed.; Butterworth-Heinemann: Oxford, UK, 2021; ISBN 978-0-12-819264-1.
65. Brinson, L.C. One-Dimensional Constitutive Behavior of Shape Memory Alloys: Thermomechanical Derivation with Non-Constant Material Functions and Redefined Martensite Internal Variable. *J. Intell. Mater. Syst. Struct.* **1993**, *4*, 229–242. [CrossRef]
66. MSC Software. *MSC Nastran 2021.3, Aeroelastic Analysis User's Guide*; MSC Software: Newport Beach, CA, USA, 2021. Available online: https://help-be.hexagonmi.com/bundle/MS_Nastran_2021.3_Aeroelastic_Analysis_User_Guide/raw/ (accessed on 9 September 2023).
67. Wikipedia. Galerkin Method. Available online: https://en.wikipedia.org/wiki/Galerkin_method (accessed on 9 September 2023).

Disclaimer/Publisher's Note: The statements, opinions and data contained in all publications are solely those of the individual author(s) and contributor(s) and not of MDPI and/or the editor(s). MDPI and/or the editor(s) disclaim responsibility for any injury to people or property resulting from any ideas, methods, instructions or products referred to in the content.