Aeroelastic behavior of a morphing wing with adaptive bending-twist coupling based on electrostatic stiffness variation
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Aeroelastic Behavior of a Morphing Wing with Adaptive Bending-Twist Coupling Based on Electrostatic Stiffness Variation

Master Thesis
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Abstract

Shape adaptable structures and especially morphing wings have come under the spotlight for the development of devices with enhanced functionality without additional mechanical complexity. Their peculiar properties allow changes in their geometrical shape by modifying their internal material properties and stiffness behavior.

In this work, electro bonded laminates are implemented to build smart interfaces in order to alternate the warping stiffness of beam structures and, therefore, shift the shear center in a way to obtain torsion moments of different sign. Thereby, the concept implementation in aerodynamic structures to tune the resulting twist angle and lift load was investigated by simulation and experiments. This new semi-active wing concept allows energy-efficient replacement of mechanical devices, such as flaps or ailerons, due to simple voltage variation and the use of aerodynamic flow energy.

The reversibility of structural properties due to alternating interface states and their influence on an airfoil’s mechanical behavior was proven in experiment and simulation. Aeroelastic nonlinear calculations allowed the prediction of the concept’s behavior under dynamic loads and justified the manufacturing of a stable demonstrator wing.

The experimental results show, distinctly, the possibility to tune the transferable interface shear stress, and therefore to change the shear center position depending on the applied voltage. This very first implementation of electro bonded laminates to alter the structural behavior in a complex structure specifies a new, innovative approach to simplify complex mechanisms and opens new material tuning possibilities.
Zusammenfassung


In dieser Arbeit werden elektromechanische Laminate benutzt, um smarte Schnittstellen zu schaffen, die die Verwindungssteifigkeit von Balkenstrukturen verändern und damit deren Schubmittelpunkt so verschieben, dass Torsionsmomente unterschiedlichen Vorzeichens entstehen. Die konzeptionelle Implementierung in aerodynamischen Strukturen wurde in Experimenten und Simulationen untersucht, um deren resultierende Verdrehungswinkel und Auftrieblasten zu verändern. Dieses neue semipassive Flügelkonzept erlaubt, durch einfache Spannungsvariation und Ausnutzen der Strömungsenergie, einen energieeffizienten Ersatz von mechanischen Teilen, wie Klappen und Ruder.


# Glossary

## Symbols

### General Symbols

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\delta$</td>
<td>Small geometrical distance</td>
<td>[m]</td>
</tr>
<tr>
<td>$\omega$</td>
<td>Radial frequency</td>
<td>[rad/s]</td>
</tr>
<tr>
<td>$f$</td>
<td>Frequency</td>
<td>[Hz]</td>
</tr>
<tr>
<td>$\rho$</td>
<td>Density</td>
<td>[kg/m$^3$]</td>
</tr>
</tbody>
</table>

### Mechanical Symbols

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\sigma_{ij}$</td>
<td>Tension in $ij$ direction</td>
<td>[N/m$^2$]</td>
</tr>
<tr>
<td>$\varepsilon_{ij}$</td>
<td>Strain in $ij$ direction</td>
<td>[-]</td>
</tr>
<tr>
<td>$F_{ij}$</td>
<td>Force in $ij$ direction</td>
<td>[N]</td>
</tr>
<tr>
<td>$T_{ij}$</td>
<td>Torsion moment in $ij$ direction</td>
<td>[Nm]</td>
</tr>
<tr>
<td>$K_\theta$</td>
<td>Torsional stiffness</td>
<td>[Nm]</td>
</tr>
<tr>
<td>$K_h$</td>
<td>Bendings stiffness</td>
<td>[Nm]</td>
</tr>
</tbody>
</table>

### Aerodynamic Symbols

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
<th>Unit</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\alpha$</td>
<td>Geometrical angle of attack</td>
<td>[°]</td>
</tr>
<tr>
<td>$\alpha_i$</td>
<td>Induced angle of attack</td>
<td>[°]</td>
</tr>
<tr>
<td>$c_L$</td>
<td>Lift coefficient</td>
<td>[-]</td>
</tr>
<tr>
<td>$c_D$</td>
<td>Drag coefficient</td>
<td>[-]</td>
</tr>
<tr>
<td>$c_M$</td>
<td>Pitching moment coefficient</td>
<td>[-]</td>
</tr>
<tr>
<td>$c_P$</td>
<td>Pressure coefficient</td>
<td>[-]</td>
</tr>
<tr>
<td>$p$</td>
<td>Pressure</td>
<td>[N/m$^2$]</td>
</tr>
<tr>
<td>$q$</td>
<td>Dynamic pressure</td>
<td>[kg/s$^2$m]</td>
</tr>
<tr>
<td>$L$</td>
<td>Lift force</td>
<td>[N]</td>
</tr>
<tr>
<td>$M_L$</td>
<td>Pitching moment</td>
<td>[N]</td>
</tr>
<tr>
<td>$S$</td>
<td>Lift area</td>
<td>[m$^2$]</td>
</tr>
<tr>
<td>$b$</td>
<td>Total wing span</td>
<td>[m]</td>
</tr>
</tbody>
</table>
$c$  Wing chord  \quad \text{[m]}
$a_0$  Lift slope coefficient  \quad \text{[-]}
$v$  Velocity  \quad \text{[m/s]}
$v_\infty$  Flow velocity  \quad \text{[m/s]}
$Ma$  Mach number  \quad \text{[-]}
$Re$  Reynolds number  \quad \text{[-]}
$\eta$  Kinematic viscosity  \quad \text{[m$^2$/s]}
$\nu$  Dynamic viscosity  \quad \text{[N$s$/m$^2$]}

**Electrical Symbols**

$R$  Resistance  \quad \text{[\Omega]}
$L$  Inductance  \quad \text{[H]}
$C$  Capacitance  \quad \text{[F]}
$I$  Current  \quad \text{[A]}
$V$  Voltage  \quad \text{[V]}
$\varepsilon_0$  Vacuum permittivity  \quad \text{[F/m]}
$\varepsilon_r$  Relative electric permittivity coefficients  \quad \text{[F/m]}
$D$  Electrical displacement  \quad \text{[C/m$^2$]}
$E$  Electrical field  \quad \text{[V/m]}

**Indices**

ij  General indices
$x,y,z$  Letter direction in global coordinate system
1,2,3  Index direction in local coordinate system

**Acronyms and Shortcuts**

AC  Aerodynamic Center
CG  Center of Gravity
SC  Shear Center
UD  Uni Directional
EBL  Electro Bonded Laminates
OS  Open State
CS  Closed State
1. Introduction

Shape adaptable structures and especially morphing wings have in the last decades come under the spotlight for the development of devices which enhance functionality without additional mechanical complexity. Morphing, in this context, refers to in-flight shape changes to alter flight performance, e.g., drag reduction or lift manipulation. Concepts of morphing wings have been presented since the beginning of flight and, despite their diversity, none could prove a significant advantage to classical wings.

![Figure 1.1.: Requirement triangle that shows the conflicts between different structural performances](image)

Campanile described the trade-off between lightweight, load carrying properties and shape adaptability [9] in the requirement triangle shown in figure 1.1, where no solution that fulfills all criteria equally, can be obtained. Stiff and loadable structures normally limit compliance due to their lack of deformable parts. On the other hand, shape adaptable structures are far from light and supportive. Hence, a new class of compliant structures based on semi-active systems offers advantageous properties with low mechanical complexity and, regardless of their lightweight structure, selective stiffness which enables load carrying properties.
1. Introduction

1.1. Different Approaches to Morphing Wings

Since the beginning of flight, people have dreamed of smooth and yet flexible wings, enabling bird-like flight behavior. As history showed, human failure in doing so started in the Greek mythology, where Daidalos copied bird wings and failed. In the renaissance, Leonardo da Vinci anticipated our modern hang gliders, but failed to overcome the weight penalties. Centuries later, Otto Lilienthal, the great flight pioneer, finally managed to handle, simple gliding flight [28] and marked the first ever proven manned flight. The Wright brothers, first in a long list, initially planned to implement a roll control by using wing warping, but failed to reach sufficient stiffness to weight ratio for proper steering mechanisms. In 1903, [53] Orville Wright stated that shape adaptable but stiff structures involve significant weight penalties and the ongoing performance losses.

![Variable camber concept introduced by Holle and Judge](image)

Figure 1.2.: Variable camber concept introduced by Holle and Judge

Approaches somewhat similar to morphing, but focused on mechanical performance rather than being smart, dominated the following decades. Starting from the pioneering work of Holle and Jugde [24] in 1916, who were the first to implement varying camber concepts, the mechanical complexity and, therefore, the additional weight eliminated any gain considering flight performance. Despite the huge profile morphing realized by camber variations the concept in figure 1.2 was condemned to stay a concept. Several similar research activities were conducted in the following decades; among others Rocheville (1932) [41], Antoni (1928) [4], Grant (1935) [20], Parmele (1931) [36], Lyon (1965) [33] published patents showing the different working principles of morphing wings.

A newer approach presented by J. Cooper et al. [11] focused on internal changes to tune shear center positions by wing box shift or spar rotation. The mechanical com-
1. Introduction

Complexity could be reduced compared to earlier concepts, but drawbacks in aerodynamic stability and severe weight penalties limited the feasibility.

Recently, research drifted towards flapless wings with complex rib structures to ensure a predefined deflection by applying a simple actuator force. These so called compliant structures, which are essentially structures with selective stiffness components open ways to huge mechanical simplification and promote enormous weight gain. Hasse et al. [21] showed, distinctly, that optimization of the internal rib geometry allows shifts in deformation energy for different virtual degrees of freedom in a way to lower the mode energy for a preferred deflected rib shape. The required deformation energy is though not only dependent on the optimized rib, but also, substantially, on the eigenmodes of the stiff wing skin. This simple boundary condition limits the achievable shapes and therefore, the achievable performance gains significantly.

In 1989 Crawley et al. [12] investigated for the first time 3D warping with built-in piezo elements for actuation reasons. This new promising approach allows flexible camber modifications with high accuracy and high operation frequencies limited primarily by the structural stability. Hence, these switches in topology are only realizable by using large amounts of actuator energy for deformation and are substantially limited by the low energy modes defined by the wing skin [38].

1.2. The Novelty of EBL

Most adaptive materials and concepts, e.g. compliant structures [21], artificial muscles [5], or thermo activated elements [23] are either predefined in their way of behavior or do need significant amounts of energy input to change their operation state. Bergamini et al. [6, 30, 32] conducted research on electro bonded laminates which, on the contrary, allow fast electrostatic changes to change their overall stiffness behavior. Different to other shape adaptable materials, electro bonded laminates are not suitable for active shape adaption, but keep their respective shape by means of electrostatic attraction forces.

We plan to use a semi-passive approach in order to change the wing torsion depending on internal stiffness variations due to electrostatic state changes. In this concept, the initial idea of the flight pioneers to change the wing torsion instead of changing the profile shape is realized. Considering this, the approach lead to the formulation of the thesis.

1.3. Objectives of the Thesis

Introducing controlled torsional deformations in a wing structure represents an effective way of varying the lift forces generated by the wing. The implementation of constant twist, for example by using bending-twist coupling in anisotropic structures, is well known as aeroelastic tailoring. The novel concept of adaptive aeroelastic tailoring, which aims at variable coupling stiffness and thus controlled morphing is currently under development in the frame of the research project 'Morphing Airfoil...
1. Introduction

with Adaptive Stiffness' at the Center of Structure Technologies.

One of the major challenges in the implementation of shape-adaptable aeroelastic structures consists in the realization of airfoils that, on the one hand, are able to carry aerodynamic loads with a sufficiently high stiffness and on the other hand show enough compliance to permit morphing without requiring a large actuation energy. Controlling the structural stiffness of a morphing wing allows, in principle, to escape this conflict of requirements and, consequently, offers pronounced advantages of the mentioned variable-stiffness approach over other morphing concepts, namely its promising energy efficiency and lightweight potential.

An experimental wing structure with controllable polymer temperature and thus shiftable shear center location and torsional stiffness, has already been manufactured at the institute [23] and showed the effectiveness of adaptive bending-twist coupling.

While the change in rigidity of the afore-mentioned demonstrator structure is based on temperature variation of its polymer layers, this master thesis project aims at the implementation of electro-bonded laminates in a morphing wing with adaptive coupling stiffness, which promises big advances in terms of energy efficiency and dynamics.

This leads to the formulation of the objectives the thesis is dealing with.

- Modeling of the geometry: A fully parametric model of the desired wing configuration is created and validated in ANSYS. The model shall be adaptable to different geometry configurations and interface positions to allow various parameter studies.
- Proof of Concept: Shifts of the shear center and the resulting changes in structural deflection as well as the proof of reversibility are shown.
- Aeroelastic modeling by coupling ANSYS, MATLAB and Xfoil: The geometrical model is charged with aerodynamic pressure distributions, calculated in Xfoil, and the occurring deflection is validated in MATLAB.
- Design fixation: Different design iterations are conducted to find an appropriate geometry with realizable EBL interfaces; Reversibility under aerodynamic loads and robust behavior, e.g. exchangeable interfaces, are implemented.
- Demonstrator manufacturing and wind tunnel test preparations: Manufacturing of a scaled demonstrator wing according to the designed airfoil data obtained in the simulation. Implementation of functional and exchangeable electro bonded laminate interfaces. Statical tests proof the already simulated working principles and allow preparations of further tests under dynamic conditions, i.e. wind tunnel tests.
2. Theoretical Foundations

In this chapter, the theoretical concepts and the main conventions the thesis is based on are shortly introduced. The different working principles, starting with the shift of shear center location and the electrostatic stiffness variation, and ending with the aeroelastic coupling to predict the aerodynamic behavior, are outlined. All definitions are consistent through the following chapters and referenced in the sections below. Unless mentioned differently, all relevant parameters and results are listed in SI units [45].

2.1. General Remarks

2.1.1. Coordinate Systems

![Global coordinate system for all simulations and experimental wings](image)

Figure 2.1.: Global coordinate system for all simulations and experimental wings

The global coordinate system described in figure 2.1 is used for all investigated wing configurations. The origin is placed at the wing root leading edge with right handed direction of rotation.

A second, local coordinate system is used for describing FE element surface orientations, laminate layups and laminae main directions, where indices 1, 2, 3 refer to the fiber direction (index 1) and orthogonal directions of the fiber respectively (indices 2, 3). Out-of-plane directions on finite element surfaces are always described by the 3 direction.
2. Theoretical Foundations

X, 1  Chord direction, primary fiber direction
Y, 2  Vertical direction in-plane, transverse fiber direction
Z, 3  Wingspan coordinate, out of plane orthogonal fiber direction

2.1.2. Laminate Layup

In order to describe the different laminate layups properly, some important conventions have to be introduced. Figure 2.2 shows a unidirectional (UD), transversely isotropic laminate with primary direction 1 and counterclockwise positive rotation. A $[+60^\circ]$ layer corresponds to a rotation around 3 axis in counterclockwise direction.

![Transversely isotropic UD laminate with fibers oriented in 1 direction](image)

For all analytical and numerical calculations, the theory of thin plates according to mechanics of composites as described in [27] is used. Quasi-isotropic laminates are realized by a $[0, -60, +60]$ laminate due to small desired plate thicknesses and favorable local bending stiffness.

2.2. Shift in Shear Center Location

Given the possibility to change the shear center location of simple beam structures [30], one can imagine to tune the bending and twist behavior of structures under static or dynamic loads.

The shear center position of profiles without warp hindering is defined as the point where an applied force does not induce any additional torsion, but only bending to the profile. It is determined by the shear moduli $G_i$ and the thicknesses $t$ of the different sections of the thin walled. Thereby, the shear center position is most sensitive to opening and closing (set section stiffness to ‘zero’) of single profile sections.

By changes in sectional warp and the related torsion stiffness shifts in shear center position along the x axis are obtained. For forces attacking at a defined point regarding the x axis, depending on the shifted shear center position, positive or negative moments distort the structure. In figure 2.3, the working principle is explained; for a thin walled beam with rectangular cross section with identical wall thicknesses $t$ and shear moduli $G$, no torsion is obtained for a symmetrical force position in the initial ‘closed
2. Theoretical Foundations

configuration’ state (left), a negative twist around the z-axis in the ’LEFT open configuration’ and a positive twist around the z-axis in the ’RIGHT open configuration’. These changes in the section’s shear stiffness involved energy and time-consuming active processes, such as mechanical actuators or thermo elements [23]. In contrast, the concept using electro bonded laminates to open and close the profile sections is semi-passive, which means no work is performed to change states.

Lift forces applied not in the shear center, cause deflection due to torsion \( w_{\text{torax}} \), bending \( w_{\text{bend}} \) and transverse shear deflection \( w_{\text{shear}} \). It is, therefore, only possible to produce changes in torsion by shear center shift for cases where the open state equilibrium of maximal deflection is not reached [39].

![Figure 2.3: Shear center shift on a box section (beam of length \( L \)) shown in front view is fully clamped at \( z = 0 \). The shear center shift is realized by changes in section shear moduli \( G \)](image_url)

with

| \( t \) | Section thickness | \([\text{mm}]\) |
| \( G_i \) | Shear modulus | \([N/\text{mm}^2]\) |
| \( F \) | Applied force | \([\text{N}]\) |
| \( L \) | Beam length | \([\text{m}]\) |

**Warp deflection** Stiff profiles, in terms of torsion, are obtained most easily with closed profiles which are likely to have a high torsional moment of inertia \( I_t \). For finite structures that are clamped at their roots, warping is increasingly hindered with decreasing distance to the clamping. Thereby open profiles are soft against warping and show lower torsion stiffness, whereas closed profiles hinder warping deflection by magnitudes stronger (for an analytical derivation see [18]).

**Torsional stiffness** \( K_\theta \) The torsional stiffness \( K_\theta \) is defined as an applied moment of torsion \( M_t \) divided by the angle of torsion \( \vartheta \), where \( M_t = F(x_{\text{force}} - x_{\text{sc}}) \) and \( x \) describes the location of force application. The amount of torsion is defined by the St. Venant torsion lowered by the effects of warp hindering.
2. Theoretical Foundations

**Bending Stiffness** $K_h$. Bending stiffness is defined as Young’s modulus $E$ times second moment of inertia $I$ or force divided by profile deflection. Keeping the material and the width of a profile constant, the bending stiffness is only dependent of the profile height and wall thicknesses. An open and a closed thin walled profile with identical geometrical and material properties resist a bending force that attacks in their respective shear center identically. The profile deflection of a distortion free profile is independent of its torsion and warp stiffness.

2.3. Electrostatic Stiffness Variation Concept

![Image of working principle of EBL](image)

Figure 2.4: Working principle of EBL shown for a double layer interface with one intersection; the left configuration shows the OFF state, whereas in the right configuration a voltage is applied to induce normal forces and allow shear stress transfer.

First described by Bergamini [6], several approaches to implement the novelty and simplicity of these multi-state materials were conducted. One advantage is that electro bonded laminates (EBL) offer a new way to overcome the weight penalty of shape-adaptive systems. Another, the very simple working principle of EBL that is based on electrostatic attraction forces, similar to the ones in a plate condenser, where normal forces depending on the electrostatic load density $q$ occur. The occurring Maxwell stress $\sigma_{\text{maxwell}}$ in equation 2.1 between the single interface parts is inversely proportional to the square of the distance given by the thickness $\delta$ of the dielectric. Friction forces due to electrostatic normal forces allow shear stress transfer $\tau_{\text{max}}$ between single attracted layers for friction coefficients $\mu > 0$. As an example, thin laminates in a lose ply behave as one bonded laminate when pressed together and exhibit much higher bending stiffness due to shear transfer between the individual layers. In figure 2.4 the differences between open (OFF) and closed (ON) EBL state are outlined. In case of an applied voltage $V$, shear forces $Q$ lead to shear stresses (green) in the attracted layers.

\[
\sigma_{\text{maxwell}} = \frac{1}{2}\varepsilon\varepsilon_0 E^2 \tag{2.1}
\]

\[
\tau_{\text{max}} = \frac{1}{2}\mu\varepsilon\varepsilon_0 E^2 \tag{2.2}
\]
with static friction coefficient $\mu$, electric field strength $E = \frac{V}{\delta}$ (voltage divided by distance), relative permittivity $\varepsilon$ and vacuum permittivity $\varepsilon_0$. Equation 2.1 can be rewritten as

$$C = 2 \frac{A \sigma_{\text{max}} d}{\mu V^2} = 2 \frac{Q_{\text{max}} d}{\mu V^2}$$  \hspace{1cm} (2.3)$$

where $C$ is the electric capacitance and $A$ the interface area.

Depending on the dielectric breakdown voltages and thicknesses published in [30], Maxwell stresses $\sigma_{\text{maxwell}}$ of $1 - 3 \, MPa$ can be realized (see among others [54, 32]). For an area of 200 $mm^2$ and a friction coefficient of $\mu = 0.3$, between 60 - 180 N of shear force are theoretically transferable. All formula assume a constant maximal transferable $\tau_{\text{max}}$ over the whole contact area.

2.4. Aeroelastic Coupling Principle

The concept of shear center shift and its reversibility shown in section 2.2 shall not only be proved statically but also under conditions close to real flight conditions, e.g. wind channel test. Therefore, stability analysis can only be performed for aerodynamically coupled wing geometries with suitably chosen convergence criteria. The mechanical 2D model in figure 2.5 contains all definitions for a rigid wing section description. The aerodynamic center ($AC$) is defined as the point of constant aerodynamic moment coefficient $c_m$, independently of different angles of attack $\alpha$. Furthermore, the AC and the point of pressure, where no aerodynamic moment attacks, are coincident for symmetrical rigid wing profiles e.g. of the NACA class.

![Figure 2.5: Simplified rigid wing model for analytical stability calculations on 2D infinite wings](image)

The pressure distribution in flight condition results in a aerodynamic lift force $L$ attacking at the AC and causing a moment depending on the distance $e$ between shear center ($SC$) and aerodynamic center. Another important wing property is the center of gravity ($CG$) which mainly sets the structure’s dynamic stability.
2. Theoretical Foundations

**Divergence**  Divergence is a static aerostatic phenomenon, where the structure under aerodynamic deflection, and its resulting back driving reaction due to structural stiffness, can not withstand the changes due to aerodynamic forces and moments. The phenomenon of divergence causes instantaneous collapse for velocities exceeding the divergence velocity, thereby either torsion or bending stiffness does not equal the aerodynamic forces. For the investigated warp soft structures, divergence due to imbalance in the torsional moments is the more critical case. Appropriate analyses are performed in chapter 5 to ensure aeroelastic stability. The implemented calculation routines for divergence are explained in section 3.3.4.

**Flutter**  Flutter is a dynamic aeroelastic phenomenon and represents the inverse of bird flight. Flow energy is, by phase shifts between wing torsion and deflection, dissipated in the wing structure. At the velocity where the additional flow energy exceeds the structural damping capacities, the vibration amplitude increases until the wing collapses. Implemented calculation routines for flutter are explained in section 3.3.5.

**Standard Atmosphere**  Defined at sea level, temperature $T = 288.15 \, K$, air density $\rho = 1.225 \, \text{kg/m}^3$, kinematic viscosity $\nu = 15.11 \times 10^{-6} \, \text{m}^2/\text{s}$ and pressure of $p = 101325 \, \text{Pa}$. 
3. Simulation of Structural and Aerodynamic Phenomena

3.1. Proof of Concept

In a previous master thesis [23], the concept of variable shear center location (see 2.2) was numerically and experimentally validated for an optimized demonstrator. The investigated demonstrator wing, with active thermo elements to alter shear stiffness, was tuned to show high differences in torsional rigidity between closed and opened state to induce an optimized lift change $\Delta L$. Hence, reversibility under static and aerodynamic loads has to be proven. Full reversibility shall be defined as the ability to reset a modified system to its initial state, where partial reversibility does not include reinitialization of all system parameters, but is fulfilled when at least one reversible parameter is present.

3.1.1. Reversibility Under Simple Aerodynamic Loads

For simple beam configurations with coincidence between center of gravity and shear center, the proposed shift of the shear center from section 2.2 was validated under single static loads. In the proposed application, shear center dislocation in both, leading and trailing edge direction and its reversibility under aerodynamic loads have to be proven. The fully coupled aeroelastic model with contact elements is not suitable for first concept evaluations, therefore simplified, coarsely meshed beam structures were used as wing approximations. Different methods to preserve element states in between subsequent load steps for changing EBL states were tested. An appropriate method was found in killing $\text{EKILL}$ and reactivating $\text{EALIVE}$ [3] the interface elements. By doing this, the warp stresses and deflections in the beam could be preserved. However, reactivated elements face a strain free state and can not be evaluated in terms of inner stress state. The simplification in modeling the EBL elements neglects interlace slip due to overload and assumes a 'zero' stiffness for open interfaces.

Another phenomenon concerning reversibility was observed; the achievable magnitude in torsion angle change, achieved by shifts in shear center, decreased with increasing number of cycles (see figure 3.1), while the tip deflection increased. For a closed beam configuration with applied force at the shear center, the beam deflection is lowest. In case of a suddenly opened profile, the shear center is shifted and shear stiffness and torsional stiffness are lowered; the profile deflects more and gets distorted. Closing the profile section again means freezing the occurred warp deflections. The opening of the other beam side, resulting in a back shift of shear center, does not invert
all frozen warp deflection of the previously opened side due to frozen deflections. This leads to losses in achievable torsion; in on the contrary the deflection increases with every interface change until the open state equilibrium is reached.

### 3.1.2. Parameter Iteration

The two main requirements to obtain a reversible and, in terms of mechanical complexity simple, structure are defined below.

- The parameter which is mainly varied is the warp stiffness. Therefore, the structure in its open state has to be soft against warping and the resulting torsion. This ability to change torsion under loads is quantified by the ratio 'Measured Torsion per Applied Force'.

- Previous shape adaptable structures featured a high mechanical complexity, therefore reversibility with the least possible complexity (EBL interfaces) constitutes the second goal.

Different parameter iterations with variable number of interfaces, openings and rib configurations were conducted. In figure 3.2, the finally chosen beam concept with two EBL interfaces (marked in red) and two bottom openings in the wing is shown. Nose and back parts are modeled as simple beam profiles with a coated profile section area similar to an appropriate nose or back part respectively. Clamping, skin thickness and wing box position are chosen to get a shear center position close to the attacking lift force at \( x_{AE} = 0.25 \ c \). In order to minimize the wing’s torsion rigidity, only the wing box part was clamped at \( z = 0 \), whereas all other nodes were not restricted in their motion.
3. Simulation of Structural and Aerodynamic Phenomena

Figure 3.2: Wing box concept with tailored rib structure and two EBL interfaces (marked in red) to change torsion stiffness

3.2. Implementation of EBL Behavior

Simulations of nonlinear effects in both geometrical nonlinearities and friction effects coupled with electrostatic behavior, necessitate simplifications for large numerical models. Since primarily the aeroelastic coupling effect was investigated, a bottom-up approach of increasing model complexity was chosen. The figures presented in this section feature a configuration similar to [23] with three interfaces: a front interface, a front spar interface and a rear spar interface. Single lift forces at the wing tip’s aerodynamic center $x_{AE}$ are applied to compare the different EBL models.

3.2.1. Approximation with Orthotropic Materials

As a first approach, an isotropic material with approximately the dielectric’s structural properties filled the gap between the different spar parts (see figure 3.3). Simple changes in the material properties with the ANSYS command $MPCHNG$ between two load steps softened the ‘dielectric’ and allowed larger deflections. Better results were obtained with orthotropic materials that offered selective stiffness variation, for example in the expanded sliding directions.

Hence, large element deflections in nonlinear calculations caused by the lack of slip possibilities lead to severe convergence problems. Especially the open state configuration, where low shear moduli allowed larger element distortions, often resulted in poor element shapes. In addition to that, the physical EBL properties, especially in the open state, could not be modeled without extensive material tests.

3.2.2. Advanced Approximation with Contact Elements

Contact elements offer a variety of tuning parameters and were the next, more realistic, EBL approximation. For all calculations ‘Augmented Lagrangian’ method and stepwise stiffness update based on the current mean stress and the allowable penetration were used. Since the problem complexity was 3D, four node $target 170$ element and four node $contact 173$ element [2] were used.

Where for first calculations a maximal sticking stress $\tau_{max} = 0.15 \text{ MPa}$ was used to simulate the closed state interface, normal pressure $\sigma_{maxwell}$ based on the applied
3. Simulation of Structural and Aerodynamic Phenomena

Figure 3.3.: Summarized element deflection on a scale from zero at the clamping (blue) to maximal at the wing tip (red) for front interfaces and front spar interface.

Figure 3.4.: Element contact state; red: sticking, orange: sliding, yellow: near contact, blue: no contact for front and front spar interface.
3. Simulation of Structural and Aerodynamic Phenomena

voltage (see section 2.3) and a friction coefficient of $\mu = 0.3$ were assumed for the later simulations. For an applied voltage of 5000 V, a relative permittivity of $\varepsilon_r = 3.57$ and a dielectric thickness of $\delta = 0.025 \text{ mm}$, 0.19 MPa of transferable shear stress results. Assuming a small safety factor of 1.25 this leads to the stated $\tau_{\text{max}}$. The two different approximations resulted in very similar deflections for load cases, where no slip in the closed interfaces occurred. Where in the first approach a constant maximal shear stress in the interface is assumed, the second, more realistic model acts on the assumption of constant normal stress. Depending on the dielectric’s permittivity and the applied voltage a constant normal stress compresses the interface parts in the simulation. It has to be mentioned that varying dielectric thicknesses and air gaps are not modeled in the smeared permittivity.

In figure 3.5 the applied Maxwell stress normal to the EBL interface allows the transfer of shear stress. Therefore, the Maxwell stress is easily adaptable for different permittivities $\varepsilon_r$ and voltages $V$.

![Figure 3.5: Applied Maxwell stress at the front interface to transfer shear stresses](image)

3.3. Full Aeroelastic Coupling Concept

3.3.1. Coupling Scheme

The coupling scheme presented in figure 3.6 illustrates the connective interfaces between the three programs ANSYS Classic, MATLAB and Xfoil. MATLAB is first used to define the model parameters and the associative key points handed over to ANSYS. All structural calculations including buckling analysis, shear center evaluation and aerodynamic stability analyses are performed in ANSYS based on the particular MATLAB commands. The calculated results, such as section deformation, torsion at specific points and reaction forces are exported to MATLAB, where they define another iteration loop or lead to convergence. For aeroelastic coupled analyses, deformed profile sections calculated with ANSYS are handed over to Xfoil. The profile information from Xfoil are applied to the 3D wing and the resulting pressure distribution is exported to ANSYS. As explained in the previous sections, the wing is not fully reversible under loads, therefore the step-by-step torsion variation for nonlinear analyses is also realized in the scheme. With this network of calculation routines, even parameter studies for different wing concepts are easily and fastly manageable.
3. Simulation of Structural and Aerodynamic Phenomena

3.3.2. Pressure Application with Xfoil

The freeware Xfoil allows laminar (low Reynolds and Mach numbers) flow calculations including viscoelastic effects for arbitrary 2D wing profiles. In our case, both the demonstrator and the up-scaled wing are operated at low Mach numbers ($Ma = 0.1 - 0.3$), where the assumptions for laminar and incompressible flows hold true. In order to transform the 2D $c_p$ values to 3D, deflections from ANSYS results are subtracted from the undeformed NACA 0012 reference file profile and handed over to Xfoil. The selected sections are defined by the number of ribs in the model, since the profile shape is less distorted at the rib positions. With the calculated $c_p$ values from Xfoil along the profile contour, 2D pressures can be calculated applying equation 3.1. This resulting pressure distribution shown in figure 3.7 is, on demand, scaled with a lift line correction and linearly interpolated between explicitly evaluated sections.

$$ p = \frac{1}{2} \rho c_p v_\infty^2 $$  \hspace{1cm} (3.1)

All calculations are performed for standard atmospheric values as defined in section 2.4 and log amplification $N_{crit} = 12$ in viscous mode [16]. The demonstrator flight conditions are set to $v_\infty = 35 \text{ m/s}$ and an initial angle of attack $\alpha = 2^\circ$.

3.3.3. Lift Line Corrections

The aerodynamic software [16] used for calculating aerodynamic characteristic pressure coefficient $c_p$ distributions and lift coefficients $c_l$ for profile sections does not hold true...
3. Simulation of Structural and Aerodynamic Phenomena

![Diagram showing pressure distribution on deformed wing section for angle of attack $\alpha = 2^{\circ}$ and velocity $v_{\infty} = 35$ m/s](image)

Figure 3.7: Pressure distribution on deformed wing section for angle of attack $\alpha = 2^{\circ}$ and velocity $v_{\infty} = 35$ m/s

for finite 3D wings The results have, therefore, been scaled by a lift line method.

First, the still popular analytical Prandtl lift line correction method, first published in 1927 by Prandtl in [37], was implemented. Starting with the Fourier series to build the circulation approximation for an arbitrary finite wing,

$$\Gamma_\theta = 2bv_{\infty}\sum_{n=1}^{N} A_n \sin(n\theta) \quad (3.2)$$

considering the span-wise transformation from space to complex $\theta \in [-\pi, \pi]$ in equation 3.3,

$$z = -\frac{b}{2}\cos(\theta) \quad (3.3)$$

the lift coefficient $c_l(z_0)$ at span wise position $z_0$ is given by

$$c_l(z_0) = \frac{2\Gamma(z_0)}{v_{\infty} c(z_0)} \quad (3.4)$$

with flight velocity $v_{\infty}$, local chord $c(z_0)$ and local circulation $\Gamma(z_0)$. For a full derivation of the theory we refer to [25].

Another, but simplified approximation method was initially proposed by O. Schrenk [43, 44] in 1940 and is known as the 'Schrenk’s method'.

$$c_L = a_0 \frac{1}{1 + \frac{a_0}{\pi A}} \alpha \quad (3.5)$$
3. Simulation of Structural and Aerodynamic Phenomena

![Figure 3.8: The influence of Prandtl lift line correction along the wing span of a low aspect ratio wing (AR = 6.7)](image)

\[ c_l(z) = \frac{c_L}{2} \left[ 1 + \frac{4S}{\pi c(z)b} \left( 1 - \left( \frac{2z}{b} \right)^2 \right) \right] + c_{L\text{base}} \]  

(3.6)

with

- \( z_0 \): Spanwise coordinate [m]
- \( \Gamma \): Circulation [1/s]
- \( c_l \): Local lift coefficient [-]
- \( c_L \): Global lift coefficient [-]

The base lift coefficient \( c_{L\text{base}} \) is always assumed to be zero, which postulates a rigid, torsion free wing with zero lift angle \( \alpha_0 = 0 \) as defined in 2.4. A complete derivation of the theory can be found in [52].

Taking one of the two methods, the 2D lift coefficients calculated in Xfoil are scaled with the respective lift line coefficients. These scale factors shown in figure 3.8 multiplied by the pressure values along the wing span result in new lift distributions along the finite wing. It shall be noted that all aeroelastic calculations were performed with the more accurate Prandtl lift line correction (for comparison reasons see figure 3.9).

For the purpose of verification, both methods were compared with the Java-based freeware NURFLUGEL [8], which showed good agreement for all angles of attack applied to a NACA 0012 profile. Since in Xfoil no induced drag \( c_{D\text{i}} \), but only friction and pressure drag coefficients are calculated, no drag coefficient correction was performed.
3. Simulation of Structural and Aerodynamic Phenomena

3.3.4. Divergence Routines

For the in terms of torsion soft wing, divergence shall be defined as non-convergent torsion angle increments $\Delta \alpha$ for coupled non-linear aeroelastic analyses. Thereby, the structural moment increment $\Delta M_t$ does not equal the aerodynamic moment increment $\Delta M_A$ for small disturbances, ergo the system becomes unstable. For a simplified, rigid but pivot-mounted thin wing, the equilibrium equation

$$\Delta M_t + \Delta M_a = 0 \quad (3.7)$$

with torsional moment increment $\Delta M_t = -K \Delta \alpha$ and aerodynamic moment increment $\Delta M_a = qS \epsilon \Delta \alpha$ describes the neutral equilibrium, where $\epsilon = x_{sc} - x_{AC}$ is the difference between center of rotation and aerodynamic center. Small perturbations, like the updated aerodynamic lift distributions in the coupled simulation, lead to increasing, non-convergent torsion. The lowest velocity where no stable equilibrium between mechanical and aerodynamic moment is present, characterizes the divergence speed $v_{div}$. For a full derivation of the perturbation analysis, we refer to [10].

3.3.5. Flutter Routines

The most widely used flutter analysis connects modal deflections with aerodynamic force distributions to predict the flutter velocity. In this thesis, a modal flutter analysis with a static aerodynamic operator and p-method for solution was implemented. Starting with the equation of a dynamic system without damping in modal form with $N$ eigenmodes.

$$M \ddot{\mathbf{a}}(t) + K \mathbf{a}(t) = \mathbf{q}(t) \quad (3.8)$$
3. Simulation of Structural and Aerodynamic Phenomena

The generalized mass matrix $M$ is the unity matrix scaled by a factor $f^2$. Depending on the scaling of the eigenmodes, the stiffness matrix $K$ is a diagonal matrix with the scaled square of the radial eigenfrequencies $f^2 * \omega_i^2$ as entries. For the generalized forces $q(t)$, the discrete physical forces $q(t)$ are transformed by the eigenvector matrix $\Phi^T$.

$$q(t) = \Phi^T q(t)$$

(3.9)

The solution approach in equation 3.10 fulfills the characteristic equation 3.11 for all velocities $\nu$.

$$a(t) = a_0 e^{\nu t}$$

(3.10)

$$det[M \nu^2 + K - A] = 0$$

(3.11)

In this case without damping and for a stationary aerodynamic operator, the smallest velocity where at least one real part $Re[\nu]$ becomes positive is the flutter speed $v_{flut}$.

Concerning the implementation of this method, ANSYS was used to obtain the eigenfrequencies $\omega_i$ and the 'normalized by mass' eigendeflections $1/f * \Phi$. By defining a scale factor $f$, the scaled mass $M$ (by $f^2$), stiffness $K$ (by $f^2$) and eigendeflections $\Phi$ (by $f$) were calculated. With the help of Xfoil and the application of Prandtl's lift line method, a pressure distribution along the wing for the scaled eigendeflections $\phi_i$ was computed. Applying the pressure distribution resulting from each eigendeflection on the model, one gets the discrete physical forces $p_i$.

Since the physical forces $p_i$ result from deflections of the scaled eigenmode $\phi_i$, the modal deflection $a_i$ is one at the $i$-th entry and the $i$-th column of the aerodynamic stiffness matrix $A$ is equal to the modal force vector $q_i$, where for general cases the relation in equation 3.12 holds true.

$$column(A) = q_i/a_i$$

(3.12)

For a full derivation and the methodology of another, simplified flutter analysis for a pivot-mounted rigid wing, we refer to [10].
3. Simulation of Structural and Aerodynamic Phenomena

3.4. Model Convergence

Complex and multi-material parts with large and abrupt stiffness and thickness changes, e.g. adhesive film vs. aluminum guides, are likely to produce severe convergence problems. Therefore, the model convergence, especially for linear calculations where no convergence criterion for force and moment tolerances is defined, is evaluated for different element aspect ratios ($AR$) $AR = \frac{l_{elem,span}}{l_{elem,chord}}$. As reference model, the demonstrator geometry described in chapter 5 without interfaces was evaluated.

![Convergence plot for different aspect ratios over decreasing element sizes](image)

Convergence in terms of changes in wing deflection in figure 3.10 was analyzed for aspect ratios 1 and 0.5. Aspect ratio 1 is preferable and was taken as reference for all analyses. The very good deflection and rotation agreement between simulation and experiment shown in chapter 6 is another strong indication for good model convergence. Concerning the results all demonstrator simulations were performed with 240 elements per meter and an aspect ratio equal to 1.
4. Material Test and Experimental Proof of Concept

The manufacturing complexity and additional mechanical flexibility introduced due to shape adaptable structures and the described semi passive model, lead to several pre-investigations. Not only were material parameters of the different wing components evaluated and implemented in the numerical model but also different tests on EBL parts and spar concepts compared.

4.1. Composite Materials

Most parts of the wing demonstrator (described in chapter 5) are hand laminated and their material parameters may vary significantly from given values. For all potential material choices, three point bending tests according to [14] with a sample size of five allowed the evaluation of the Young’s moduli. Only the UD prepreg material allowed the Young’s modulus evaluation in single directions. Yet, for [0,90] fabrics the missing parameters are extracted from a thin layered cross ply stacked out of similar UD materials in composite calculation program CAP [26]. All out of plane parameters, $E_{23}$, $E_{33}$, $G_{23}$, $\nu_{23}$, can be approximated by using similar materials defined in CAP.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>UD MTM 44-1</th>
<th>CFRP web 200 $g/m^2$</th>
<th>GFRP web 290 $g/m^2$</th>
</tr>
</thead>
<tbody>
<tr>
<td>$E_{xx}$ [GPa]</td>
<td>98.133</td>
<td>48.9</td>
<td>19.86</td>
</tr>
<tr>
<td>$E_{yy}$ [GPa]</td>
<td>6.79</td>
<td>47.8</td>
<td>16.92</td>
</tr>
<tr>
<td>$E_{zz}$ [GPa]</td>
<td>6.79</td>
<td>6</td>
<td>3.5</td>
</tr>
<tr>
<td>$G_{xy}$ [GPa]</td>
<td>5</td>
<td>5</td>
<td>5</td>
</tr>
<tr>
<td>$G_{xz}$ [GPa]</td>
<td>3.12</td>
<td>4.0</td>
<td>3.5</td>
</tr>
<tr>
<td>$G_{yz}$ [GPa]</td>
<td>3.12</td>
<td>4.0</td>
<td>4.0</td>
</tr>
<tr>
<td>$\nu_{xy}$ []</td>
<td>0.28</td>
<td>0.15</td>
<td>0.139</td>
</tr>
<tr>
<td>$\nu_{xz}$ []</td>
<td>0.28</td>
<td>0.28</td>
<td>0.31</td>
</tr>
<tr>
<td>$\nu_{yz}$ []</td>
<td>0.15</td>
<td>0.3</td>
<td>0.31</td>
</tr>
<tr>
<td>$\rho$ [kg/m$^3$]</td>
<td>1580</td>
<td>1550</td>
<td>1800</td>
</tr>
</tbody>
</table>

Table 4.1.: Relevant material parameters to fully describe 3D material behavior of orthotropic and transversely isotropic materials
4. Material Test and Experimental Proof of Concept

4.2. Maximal Shear Transfer over Voltage

Similar shear tests on EBL were already conducted (see among others [32, 54]) and showed reasonable accordance with the theoretical values stated in section 2.3. The tested multilayer dielectric of thickness $\delta = 0.025 \text{ mm}$ and the mechanical test setup, including the electrodes, is build according to [54]. The performed double lap joint shear tests for different electrode overlapping lengths (shown in figure 4.1) matched the theoretical shear stress transfer $\tau_{m,\text{max}}$ (maximal mean shear stress) sufficiently. Hence, for increasing overlapping lengths, the discrepancy between simplified theory and experiment increased due to higher stress gradients (see [32]) increased. Another outlier marked the test with 10 mm overlapping length where the maximal shear stress was around 80 % lower than expected.

![Figure 4.1: Maximal mean shear stress transfer $\tau_{m,\text{max}}$ over increasing voltage for double lap shear joints with different overlapping length](image)

4.3. Evaluation of Exchangeable Spar Principles

Since the dielectric’s reliability and durability is still one of the main limiting points, a fixed, non-removable solution for the spars containing the interfaces could not be considered. Besides the most important characteristic of free-of-play shear stress transfer between the wing’s skin and the spar parts, the following requirement list was considered:

- The spars contribute substantially to the wing’s structural stability and have, therefore, to prevent structural instabilities such as buckling and large deformations.

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4. Material Test and Experimental Proof of Concept

- Flight conditions as defined in chapter 3 must not lead to interface slipping for all smart interfaces in the closed state.

- Symmetrical overlapping joints, and therefore interface placements, are required to equilibrate bending moments. Additionally, the manufacturing capabilities strongly limit the number of realizable interfaces.

- The spars, including EBL interfaces and exchange mechanisms, may not hinder bending and torsion in the OFF state, where no significant shear stress is transferred.

- Weight penalties must not significantly reduce flight performance, which results in limitations of material selection and geometrical size.

- The design has to facilitate the shear stress transfer in the interfaces and allow play-free force transmission.

- Aerodynamic surface has to be preserved, therefore, no interface fixations, that disrupt the smooth wing skin, are allowed.

In the design shown in figure 4.2, the aforementioned restrictions are implemented. The interfaces are symmetrically located to avoid superposed bending influences, where thinner spar parts in the region of the electrodes allow better adaption in regions of manufacturing inaccuracies or unwanted electrode distances. Break-downs between the electrodes are unlikely due to the dielectric layer. In contrary to that, break-downs between electrodes and guides have to be prevented by using electric insulation layers (blue layers in figure).

The linear guides and bearing were manufactured by RUAG Aviation, Emmen, out of aluminum 2024-T351 (see table 4.2) after drawings given in [50]. For a detailed concept evaluation one can consider the presented results in this source.

<table>
<thead>
<tr>
<th>Young's Modulus $E$ [GPa]</th>
<th>Shear Modulus $G$ [GPa]</th>
<th>Density $[\text{kg/m}^3]$</th>
</tr>
</thead>
<tbody>
<tr>
<td>73.1</td>
<td>27.48</td>
<td>2780</td>
</tr>
</tbody>
</table>

Table 4.2: Material properties of linear guide made from aircraft aluminum 2024-T351
4. Material Test and Experimental Proof of Concept

4.4. Shear Tests of Spar Parts

The chosen exchangeable spar solution from previous section 4.3 was tested under simple shear loading. Since the section airfoil geometry is assumed to keep its pre-defined shape, the spars mainly absorb bending and shear stresses, where bending stresses are taken by the thicker spar parts and the linear guides. In contrast to that, warping stresses resulting from shear forces $Q$ are either leading to structural stress concentrations or get dissipated in the closed interface as shear stresses.

This test was performed to evaluate changes in shear force transfer in order to estimate maximal shear forces. For that purpose, the 400 mm long interface was shifted into short, fixed to the ground, guides. In order to allow slipping only one side, the other linear rack was fixed to the guide with a pin. In terms of geometry and material, the whole interface was build according to the front spar described in section 5.2. This means 12.7 mm wide steel electrodes and the same multilayer dielectric used in the previous EBL shear tests built the electric interfaces. Evaluating, the tests an approximate multiplication of mean shear force transfer of seven for 6000 V was reached for a not primarily polarized dielectric. The variation in force transfer, however, turned out to reduce the result quality heavily. Better in means of shear force, but less predictable results were obtained with previously polarized multilayer dielectric, where the testing machine's force limit was reached for voltages above 3000 V. Bending tests at the spar tip, with force application on one of the spars, resulted in very low transferable loads for tearing and compression and are not presented.
4. Material Test and Experimental Proof of Concept

Figure 4.3.: Transferable shear force $Q$ for different applied voltages $V$ at the single interfaces
5. Demonstrator Design and Manufacturing

Previous tests (see chapter 4 and evaluations of different concepts in section 3.1), allowed to start the last design iteration. Upcoming wind channel test required not only structural dimensioning, but also coupled aeroelastic investigations. As reference load, a wind speed of $v = 35 \text{ m/s}$ and an initial angle of attack of $\alpha = 2^\circ$, which resulted in a lift force of $L = 50 \text{ N}$ for standard atmosphere and wing area $S = 0.3 \text{ m}^2$, were defined. The most important wing parameters are summarized in figure 5.1, where a full parameter list can be found in the addendum A.

![Diagram of wing structure with parameters](image)

Figure 5.1.: Important design parameters for the experimental wing structure

5.1. Design Fixation

5.1.1. Structural Properties

The structural investigations include not only buckling and mechanical strength, but also structural stiffness analysis and shear force limits in the closed interfaces. Concerning the structural strength a full investigation is presented in [50].

Structural Stability

Buckling analyses were performed for the clamped structure in the 'open', 'front open' and 'back open' states and are summarized in table 5.1. To avoid structurally irrelevant buckling of the open interface, the thin overlapping parts were not meshed.
Furthermore, the closed interface was coupled with constraint equations to avoid long calculation times. The presented buckling load factors show that structural stability is not critical under flight conditions. Firstly, because the electrical interfaces limit flight speed and therefore the applied forces. Additionally the high factors help to avoid complete wing failure for damaged, meaning open, interfaces.

Figure 5.2.: First structurally relevant buckling mode for the 'open' state configuration

<table>
<thead>
<tr>
<th>Wing State</th>
<th>Load multiply</th>
<th>Location</th>
</tr>
</thead>
<tbody>
<tr>
<td>All open</td>
<td>3.11</td>
<td>1st front rib</td>
</tr>
<tr>
<td>Front open</td>
<td>6.21</td>
<td>3rd center rib</td>
</tr>
<tr>
<td>Back open</td>
<td>5.38</td>
<td>3rd center rib</td>
</tr>
</tbody>
</table>

Table 5.1.: Buckling load factors for different relevant interfaces states and a wing tip force of 50 N applied at the aerodynamic center

Moreover, the buckled rib in the 'open' configuration (see figure 5.2) is not likely to lead to global instabilities and could, if needed, easily be reinforced.

**Structural Stiffness and Strength**

Taking the high buckling load factors, in this buckling critical structure, as indication, structural strength limits are supposed to be not critical. An analysis concerning the material strength, showed failure coefficients below 0.1 using the Tsai-Wu criterion.
5. Demonstrator Design and Manufacturing

described in [27]. For a more detailed study on structural strength with the Tsai-Wu criterion we refer to [50]. Nevertheless, the wing assembly with epoxy resin assumably lowers the strength at the connection interfaces. Regarding structural stiffness, the wing is designed to twist under loads, where the resulting lowered bending stiffness does, as presented, do not lead to structural weakening.

Breakdown Strength

For aerodynamic pressure distributions with Prandtl lift line correction resulting in a lift force of around $50 \, \text{N}$. The assumed maximal transferable shear stress of $\tau_{\text{max}} = 0.15 \, \text{MPa}$ is not exceeded. Furthermore, all nonlinear analyses, including friction simulations in the interfaces, converged without issues for ANSYS standard convergence tolerances [2].

5.1.2. Aeroelastic Properties

Aerodynamic loads, including dynamic moments and force changes, must not cause structural instabilities. The wing is assumed to have at least one electric interface closed at a time and is therefore critical for either front or back interface opened.

Divergence Analysis

Divergence, as described in section 2.4, would lead to increasing torsion angles for a coupled analysis; this means the structural stiffness does not equilibrate the aerodynamic moments. In figure 5.3, mean torsion angles $\alpha_{\text{mean}}$ (average rotation at the aerodynamic center along the wingspan) along the wing span converge for all tested velocities equal or above flight speed of $35 \, \text{m/s}$.

![Figure 5.3: Mean torsion angle for increasing velocities above cruise speed](image_url)
Starting with an initial angle of attack $\alpha = 2^\circ$, the resulting torsion angles converge for the critical state, where the front interface is opened, for all tested velocities up to $45 \, \text{m/s}$. Since other wing components are likely to fail at lower velocities, the wing is stated as not divergent.

**Simple Flutter Analysis**

In a first approach to demonstrate flutter stability, a simplified rigid wing model as presented in section 2.4 was used to get flutter velocity approximations. Therefore, assumptions on a global wing stiffness with respect to torsion $K_\theta$ and with respect to bending $K_h$ according to section 2.2 were made. As for the divergence analysis, the wing was assumed to have either a closed front or a closed back interface. Flutter results, by reasons of approximations for the substitute wing stiffness and the definition of a single shear center for the rigid structure and the obvious inaccuracies, are not presented.

**Modal Flutter Analysis**

The modal flutter analysis described in subsection 3.3.5 was performed for the most critical interface states. Since the lowest eigenmodes determine the wing’s flutter behavior, the first three eigenmodes (first twist, first bending and a mixed mode) were embraced in the calculation. Like for the divergence analysis, the nodes forming the closed interface spars were coupled with constraint equations and the thin electrode parts of the open interface were not meshed to avoid low frequency eigenmodes.

In figure 5.4, the deviation of imaginary and real parts for the ‘closed front’ interface are shown, where the flutter speed is around $v_{\text{flutter}} = 100 \, \text{m/s}$. Despite the higher overall stiffness compared to the ‘all-opened’ state, the flutter velocity is around $20 \, \text{m/s}$ lower. Depending on the difference $d$ between shear center position $x_{SC}$ and the location of center of gravity $x_{CG}$, a structure abets or suppresses flutter.

![Figure 5.4: Solution variation over velocity for closed front interface](image)
5. Demonstrator Design and Manufacturing

In the case of the demonstrator, the center of gravity is, due to the heavy aluminum guides and the high spar weight compared to the rest of the wing, quite close to the aerodynamic center at \( x_{AC} = 0.25 \). Therefore, the 'all-opened' configuration with a shear center closer to the wing's trailing edge is less likely to flutter than the 'front-closed' configuration with a shear center closer to the wing tip. It should be noted, that for all configurations the first and, therefore, most critical mode is a torsion only mode.

Analyses for the 'all-closed' and 'back closed' configurations showed no flutter for velocities up to \( v_{\infty} = 200 \, \text{m/s} \). The 'all-closed' state exhibits a much higher wing stiffness and a shear center close to the aerodynamic center, where the 'back closed' state shifts the shear center behind the center of gravity. Both of these characteristics suppress the likeliness to flutter.

For a better understanding parameter studies with additional masses at the wing tip or at the wing's trailing edge could and have for one case been conducted. It is assumed that the implementation of more realistic loads, e.g. an additional quasi-stationary or in-stationary aerodynamic operator would lead to reduced flutter speed predictions and match reality significantly better.

5.2. Wing Components Manufacturing

In a next step, the described design fixation allowed the fabrication of the wing components. If not mentioned differently, resin L 235 and hardener 235 [22] were used for all hand laminated parts. All parts were treated with at least twelve hours curing under vacuum and post curing for four hours at 60° Celsius.

5.2.1. Skin Manufacturing

Counter Mould for Surface Smoothing

Aerodynamic behavior is very sensitive to small disturbances along the wing surface, therefore the outer skin surface must not abet flow separation or turbulent flow. For this reason, a counter mould was manufactured on the design basis of [51] and [23] out of one layer of 290 \( \text{g/m}^2 \) glass fabric [46] around the nose and two layers for the rest of the mould. In order to simulate the skin thickness, a 1 \( \text{mm} \) thick PE foil created an offset between aluminum mould and layup. DOW D.E.R. 330 resin [15] and hardener Ancamine 2167 [1] were used to ensure high glass temperatures after curing. The counter mould was cured for 24 hours under vacuum and room temperature and consecutively post-cured for two hours at 80° Celsius and three hours at 150° Celsius.

Skin Parts

Prior experiences stated in [23] revealed that residual stresses in the wing skin lead to non-negligible deformation in case of wing opening after curing. On these grounds, the different skin parts were individually cured and afterwards cut in shape and assembled.

For all skin parts, UD carbon prepreg with \( MTM 44 - 1 \) [49] resin was stacked in
5. Demonstrator Design and Manufacturing

Figure 5.5.: Counter mould curing under vacuum

six layers forming a symmetrical laminate \([0^\circ, +60^\circ, -60^\circ]\), with high local bending stiffness. For higher surface quality, the counter mould was applied during the four hour curing process in the autoclave. Since the samples used for the material tests and previous wings were cured under vacuum conditions, the wing skin was tempered at 130\(^\circ\) Celsius without additional pressure. Polishing the wing surface with wet micro grain size sand paper resulted in a good aerodynamic surface. The three skin parts were cut in shape with a highly precise electronic saw, which allowed tolerances below 1 mm.

5.2.2. Ribs Manufacturing

A two layer GFRP plate hand laminated of fabric 290 g/m\(^2\) with a thickness of only 0.45 mm was used as basis for the ribs. Since low warping stiffness is one main goal, the rib thickness was chosen very small, because the laminate layup was already fixed by the fabric and did not allow further tuning. In the open configuration, the center skin part is kept in position only by the small center ribs’ webs and the front and back ribs’ overlapping parts, therefore a quite complex rib structure resulted. The rib shape was exported from ANSYS as vector graphic, then slightly optimized at the edges to fit the machine capabilities for automatic cut out.

5.2.3. Spar and Interface Manufacturing

The highly complex smart interfaces were manufactured according to design decisions in section 4.3. In a first step, the 12.7 mm wide and 0.050 mm thick electrodes were pasted onto a 0.1 mm thick glass foil of 20 mm and 23 mm width, respectively. Thicker load carrying spar parts were cut out from a 1 mm thick GFRP plate [48], with heights of 18 mm for front upper part and 23 mm for the back upper part. The lower spar parts that provide additional bending stiffness were cut out of the same GFRP plate and have a height of 7 mm each for front and back spar. In a last step, the electrode assembly was glued with epoxy L 235 into the aluminum rails’ notches and cured at
5. Demonstrator Design and Manufacturing

Figure 5.6.: Automatic cut out ribs parts

room temperature.

Figure 5.7.: Test spar interface (short) and assembled front and back spar interfaces

5.3. Wing Assembly

The single wing parts described above were assembled to fit the prescribed design parameters. First, front, center and back rib parts were positioned with the help of small GFRP brackets and fixed with epoxy. Secondly, the assembled smart interfaces which were held in position with distance holders were glued into the wing and covered with the burdened center skin part. In a third step, the still not fixed upper parts of the back ribs were straightened with magnetic fixations to ensure better alignment with the lower rib parts. The additional brackets at the back ribs' tips ensured better contact to the center skin part and were aligned and fixed to the center skin part in a fourth step. After curing, the most sensitive back skin part with already mounted brackets to hold the back ribs in position was glued. The design parameters were sufficiently matched for rib positions and wing length, where opening values $l_{\text{open}}$ and spar alignment with respect to the wing tip had to be adapted in the numerical model.

Since different clamping approaches were implemented and tested, issues about the wing fixation are discussed in chapter 6.
Figure 5.8.: Wing assembly with already fixed ribs and spar interfaces
6. Mechanical Testing

6.1. Test Setup

In Figure 6.1, the test rig with clamping, the positioned laser devices, the load introduction at the wing tip and the electric cables attached to the voltage source are shown. The demonstrator wing was attached to a massive, fixed to the ground steel angle, with its respective clamping. Whereas the laser devices measured the deflection due to load introduction at the wing tip. The EBL interfaces were attached to a voltage source to modify their closing behavior.

![Figure 6.1.: Full test setup of the clamped demonstrator wing](image)

6.1.1. Test Rig and Clamping

As a first approach, a 300 mm long sandwich clamping out of two 3.5 mm thick carbon plates made from $[0^\circ, 90^\circ]$ fabric [47] with a soft ROHACELL-31 foam [42] in between, was glued into the wing. To do so, the center rib at the wing’s root was removed and the clamping fixed with the standard epoxy L 235, 90 mm deep into the wing, in equal distance from the front and back guides.

Several test results with the previously described sandwich clamping lacked repeatability and the soft core showed significant compression damages. Aluminum reinforcements minimized compression, but did not lead to sufficient results. As a first consequence, the damaged sandwich clamping was removed and replaced by a solid steel $St - 37$ clamping shown in Figure 6.2. Again, a length of 90 mm of trapezoidal shaped steel was inserted in the wing and fixed with epoxy. The jutting rest of the clamping was fixed with M8 screws to a solid aluminum block, which is in turn fixed to the mentioned steel angle.

Concerning the simulation, the sandwich clamping could not be assumed as rigid due to the very soft core and was therefore simulated in the numerical model to fit...
6. Mechanical Testing

![Image](image1.png)  
(a) CFRP sandwich clamping with additional steel plates

![Image](image2.png)  
(b) Massive steel clamping

Figure 6.2.: Different tested clamping configurations

the experimental results. In contrary to that, the new steel clamping out of St – 37 could, as model deflections showed, easily be neglected in the simulation and modeled as fully clamped part. All results presented in the next sub chapters are based on tests with the new steel clamping.

![Image](image3.png)

Figure 6.3.: Additional guide fixations to hinder relative movements between guide and linear rail

Yet another nonlinearity showed significant influences on the test results; depending on the applied forces, the measured deflection per force varied heavily and hindered proper comparison with the FE model. One main reason for this phenomenon was found in the relative movements between linear guide and rack. At the fixation bar, clamps and clamping plates were attached to avoid slip. At the wing tip however, the lack of space claimed another solution. An attachment of the shape of the linear guide with threaded holes was bonded onto the guides’ tip ends. Another threaded hole was cut into the rails’ end surfaces and allowed the fixation of end plates with screws which eliminate slip between rails and guides (see figure 6.3).

The fixation of the clamping was assumed to have a completely rigid steel ankle and ground fixation, therefore no investigations on their relative movements were performed.
6. Mechanical Testing

6.1.2. Load Application

The demonstrator parameters were tuned to result in a shear center in the 'closed state' near the aerodynamic center. Assuming a good match between experiment and simulation, the load application was placed at 75 mm from the leading edge. In order to minimize profile deformation, the center rib was reinforced with an additional glued-on rib. A fixed aluminum piece with an threaded M5 hole allowed the implementation of short screws to transfer the load between wing and test machine.

![Wing tip with force application point 75 mm from the leading edge](image)

6.1.3. Laser Positions

The available laser detectors have a limitation in their measurement range of 10 mm, which necessitated adjustments in force and measurement position. In figure 6.5, different laser positions are presented. Though, all meaningful measurements were performed with 'upmid' laser position. This decision was justified for two reasons: firstly, the tip deflection for 20 N exceeded the measuring range and secondly in order to have the deflection in parallel with the laser beam (no unwanted deflections due to wing movements along the x direction). Signals were transmitted to an analog acquisition module (NI 9219 from National Instruments) and evaluated in Lab View.

![Laser position pairs for data acquisition](image)

The comparable rotation was not available at the wing tip position due to deflections that exceeded the measuring range, but only in the middle of the demonstrator length.
6. Mechanical Testing

The shear center is therefore defined as position in the half span wise $L/2$ of the demonstrator where no torsion occurs for a single lift force at the wing tip. Generally, the measured torsion angle $\alpha_{\text{meas}}$ is defined as arctangent of the measured front deflection $y_{\text{front}}$ minus the measured back deflection $y_{\text{back}}$ divided by the measuring point distance $d_{\text{meas}}$ as characterized in equation 6.1.

$$\alpha_{\text{tors}} = \tan^{-1}\left(\frac{y_{\text{front}} - y_{\text{back}}}{d_{\text{meas}}}\right) \times 180/\pi$$

(6.1)

6.2. Data Acquisition and Validation

6.2.1. Measurement Process

Calibration tests showed, depending on the clamping and other nonlinearities, creeping of the wing structure. Therefore, it was evident to use a universal testing machine for reproducible test conditions. All measurements for 5 N, 10 N and 20 N were performed at test velocities of 100 mm/min, 200 mm/min and 300 mm/min respectively. Once the test force was reached it was held for three seconds by force control. For voltage application at the interfaces a single voltage source of type 'SRS PS350 5kV' with the possibility to switch between positive and negative polarization was used. As previous measurements with electro bonded laminates showed [54], polarized dielectric layers store non-negligible amounts of charge that produces residual strength under loads. The discharging process takes several hours and hinders measurements where interface states are switched to proof reversibility. Taking this into account, only static tests with one or both interfaces step wise closed were conducted.

6.2.2. Comparison with Adapted FE Model

The numerical model parameters were fitted to match the measured geometrical data and material properties of the demonstrator wing. The already performed convergence analysis was repeated for this adapted FE model and showed a similar convergence behavior. First, comparisons between model and experiment without spars allowed the validation of the numerical model. For all forces between 5 and 20 N, differences in deflection of less than 5 % could be achieved. The extremely soft structure (in terms of warping and torsion) inhibited a proper evaluation of the torsion angle without spars. In this case, the measured twist response is very sensitive to the position of the laser sensors.

6.2.3. Wing Structure with Interfaces

Already mentioned difficulties lowered the testable number of valid configurations. In this section, measurements for the all-open state and stepwise closed front or back interface are presented. Measurements to validate reversibility are, because of the aforementioned residual strengths in a polarized EBL, not presented.
6. Mechanical Testing

<table>
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<tr>
<th>EBL Interface</th>
<th>Capacitance [nF]</th>
<th>Calculated ( \varepsilon_r )</th>
<th>( \sigma_{\text{maxwell}} ) (3000 V) [Pa]</th>
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<td>6.375e4</td>
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<tr>
<td>Back</td>
<td>17.4</td>
<td>1.9</td>
<td>1.211e5</td>
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</tbody>
</table>

Table 6.1: Measured capacities for different EBL states and the resulting relative permittivities \( \varepsilon_r \) to calculate the Maxwell stress.

Given the dielectric thickness \( \delta = 0.025 \text{ mm} \), the overlapping area of the electrodes \( A_{\text{tot}} = 2 \times 1 \times 0.0127 \text{ m}^2 \) and the relative permittivity \( \varepsilon_r = 3.57 \) [29], a theoretical capacitance of \( C = 32.2 \text{ nF} \) was calculated. However, surface irregularities and air gaps between dielectric and electrode reduced the measured capacitances. In Table 6.1, the measured capacitances for front and back interface and the resulting relative permittivities are listed. In order to fit the measurement conditions best, the physical assumptions of a uniformly distributed Maxwell stress shown in section 2.3, depending on the relative permittivity and the applied voltage were implemented in the simulation. A friction coefficient \( \mu = 0.3 \) is assumed (see [6, 31]) for the utilized multilayer described in [54].

![Simulation results compared with experimental results for 10 N and relative permittivity in the numerical model of \( \varepsilon_r = 1.0 \)](image)

Figure 6.6: Simulation results compared with experimental results for 10 N and relative permittivity in the numerical model of \( \varepsilon_r = 1.0 \).

In figure 6.6, the deflection and torsion results for the stepwise closed front interface over voltage are plotted. The difference in initial torsion angle between experiment and simulation was very small with respect to the absolute value. In case of the displacement, an initial discrepancy of around 15% was measured. For an applied voltage of 1000 V, both the changes in torsion and the changes in deflection were nearly identical. This showed the very good predictability for low voltages and low forces. Hence, with increasing voltage, the interfaces in the simulation close much closer to the experimental results.
6. Mechanical Testing

stronger than the real world interfaces. A discrepancy between model and experiment of nearly 30% for the torsion angle and around 70% for the deflection at 3000 volt resulted.

Figure 6.7 presents the same results, but for the stepwise closure of the back interface. Similar to the stepwise closed front interface the back interface showed very good agreement for low voltages and a single lift force of 10 N. Again, the simulation performed for higher voltages much better than the experiment.

![Simulation results compared with experimental results for 10 N and relative permittivity in the numerical model of $\varepsilon_r = 1.9$.](image)

The initial differences in deflection for the open state are assumed to come from slip between rail and guides, because the relative movement between these two parts was only eliminated at the tip and at the end. Some inevitable clearance between rail and guides certainly allows a difference in bending deflection for low forces. Higher forces would most probably lead to canting and therefore eliminate this nonlinearity, but would also complicate the interface closure.

The predictability of the EBL behavior decreases, despite the adapted permittivity, for voltages above 1000 V. Previous tests on spar parts similar to the implemented interfaces showed very good behavior in shear direction (see section 4.4), but bad behavior for forces orthogonal to shear (tension, compression). The applied force at the wing tip certainly changes the profile shape, which means the interface is spread and locally inactive. Where interface parts close to the root stick for low voltages, the regions close to the tip need much more attraction force to overcome the tension force. Secondly, the permittivity was corrected under the assumption of equally distributed capacitance. In doing so, air gaps with zero permittivity and local changes in the distance between the electrodes were not simulated and lead to growing inaccuracies for higher voltages. In the simulation, the electrodes, whether in closed state or not, feature no additional distance in between them. Hence, in the experimental configura-
6. Mechanical Testing

...tion, depending on the distance between the electrodes, additional forces to overcome the spar stiffness are needed in order to close the interface (a reduced normal force can be applied).

Another interesting aspect is the fact that the increments in torsion for increasing voltages are decreasing and, in case of the simulation, even become negative. For higher forces or measuring positions further away from the clamping, this effect could not be reproduced in the numerical evaluation. One explanation can be found in the decreasing influence of the clamping for a gradually stiffened structure. This means that warp hindering effects get smaller due to the closure of profile sections. Other explanations may be found in numerical simulation inaccuracies and the fact that with increasing Maxwell stresses, the closed interface is compressed and the lowered section stiffness leads to smaller shear center shifts.

6.2.4. Aeroelastic Investigations

In this subsection, the expected wing behavior under aerodynamic loads (Prandtl lift line correction, angle of attack $\alpha = 2^\circ$, flow velocity $v = 35 \text{ m/s}$) in a wind tunnel is presented as a first outlook for upcoming tests on the wing. Since good agreement with the simulation was reached for a lowered relative permittivity of $\varepsilon_r = 1.9$ and a voltage not higher than 1000 V, only these simulations are presented. Under flight conditions, the switch from 'open front' interface to 'open back' and to 'all-closed' represents the principal operation mode. The behavior for the 'all-opened' state is the most critical one and is needed for relaxation reasons.

![Simulated behavior under aerodynamic loads for an applied voltage of 1000 V and relative permittivity $\varepsilon_r = 1.9$](image)

Figure 6.8. Simulated behavior under aerodynamic loads for an applied voltage of 1000 V and relative permittivity $\varepsilon_r = 1.9$

Figure 6.8 shows the varying structural deflections for the load case array 'all-closed', 'front-open', 'back-open' and 'all-opened'. As expected, compared to a simulation with maximal transferable shear stress $\tau_{max} = 0.15 \text{ MPa}$, where a maximal tip twist...
of 0.009°/N was reached, the ‘experimental’ model showed only 0.003°/N twist. Nevertheless, the reversibility as well as divergence stability for this state of the art simulation, were shown distinctly.

In wind channel tests primarily the changes in lift and drag coefficient are measured; considering the small changes in twist, the lift changes of around 2% shown in figure 6.9 are justified.

![Figure 6.9: Change in lift coefficient $c_L$ for different interface states](image-url)
7. Concept Application on a Glider Wing

In a last step, the semi passive concept is implemented numerically in a more realistic structure to demonstrate functionality in full scale applications. Glider provide, due to the high aspect ratio and low cruise speed, optimal conditions for the implementation of simple flap substitutions. As a well known example, the mid range glider 'Schleicher ASW 27b' [19] with a cruise speed of around $v = 40 \text{ m/s}$, a typical operation weight of $m = 300 \text{ kg}$ and an overall aerodynamic lift area $S = 9 \text{ m}^2$ with wing span $b = 15 \text{ m}$ has been used for this purpose.

Figure 7.1.: Mid range glider AWS 27b where the semi active concept of shear center shift is applied

The following assumptions regarding structural simplifications and flap performance are made:

- The wing shape is, for reasons of simplification, chosen rectangular with an equal lift area of $S/2 = 4.5 \text{ m}^2$ as presented in 7.2.
- Buckling load factors are not evaluated for distorted lift configurations but for lift loads on distortion free wings.
- The internal wing geometry is optimized in a way to ensure reversibility and sufficient buckling load, but not to optimize flight performance.
- Changes in lift coefficients $c_L$ between 'front open' and 'back open' state are assumed to behave the same for different moderate angles of attack $\alpha = 0 - 5^\circ$.

The final wing weight of $m_{\text{wing}} = 42.5 \text{ kg}$ was reached for the parameter values presented in table A.3 in addendum A. Compared to a structurally optimized lightweight
structure as described in [19] without shape adaptability, a 25 % weight penalty resulted. It shall be mentioned that, besides the lower skin thickness compared to the upper skin thickness, all other structural conditions, such as clamping at the wing box and rib distance were exactly scaled from the afore presented demonstrator. Thickness variations and a change in spar and rib material to CFRP [47] allowed a more lightweight structure with good flight performance.

The buckling factor for an assumed angle of attack $\alpha = 2^\circ$ and $v = 40 \text{ m/s}$ amounts under aerodynamic loads for the 'opened back' 1.97 interface and 1.88 for 'opened front' interface. As simplifications to calculate the buckling load, the closed interface was not modeled with contact elements but coupled with constraint equations (no slip can occur) and the buckling-critical, but not stability-relevant, electrodes were not meshed.

7.1. Parameter Studies

It shall be mentioned that due to long calculation times, as a result of large models and friction effects, and limited computational performance only one dimensional parameter studies are presented. Nevertheless, the four most important varied parameters, the wing box position (defined over the front EBL position), the aspect ratio, the lower skin thickness and the rib thickness, picture the structural behavior sufficiently.

7.1.1. Optimal Wing Behavior

In this section, parameter influences due to variations in wing box shift and variations in the wing's aspect ratio are presented, where studies on other structural changes are listed in the addendum A. Concerning reversibility effects, the back interface was closed first to obtain a higher twist angle, followed by a closed front interface to lower the twist angle. As mentioned in previous parts, the interface behavior is assumed
to allow shear stresses of $\tau_{\text{max}} = 0.15 \, MPa$ (see section 3.2) to be transferred in its closed state.

**Shift in Wing Box Position**

For structures with low warping stiffness, one of the most sensitive parameter concerning reversibility and divergence speed is the wing box position. By varying this parameter, the plane’s maximal lift force as well as twist reversibility is defined.

In figure 7.3, the variation in mean torsion angle (average rotation at the aerodynamic center along the wingspan) and the maximal tip torsion angle are plotted. Contrary to the expectations, for configurations with shear center in the open state behind the aerodynamic center, the mean torsion angle recoiled below zero. Hence, the maximal torsion angle never passed the ordinate after an initial high lift configuration. Reasons for this behavior are the lack of back driving forces, because of zero lift at the wing tip and friction forces in the possibly slipped back interface.

Figure 7.3: Torsion angle variation for changes in wing box position with equal box width for aspect ratio $AR = 25$

For the first four wing box positions, the lift coefficients for ‘back interface closed’ and forces in figure 7.4 increased as expected, but in the most backward position a kink appeared. This nonlinearity is explained with slip in the closed interface that lowers the increase in lift coefficient. Further back shifting of the wing box would, therefore, imply higher transferable shear stresses or multi-layer interfaces.

Wing box shifts, starting from the initial parameter values equal or greater than 0.05 the chord width in positive x direction lead to divergence of the wing and are, therefore, not presented.

**Shift in Aspect Ratio**

Another important parameter that allows tuning of the flight performance, is the wing’s aspect ratio, described by chord and span wise length. It is expected to see higher
7. Concept Application on a Glider Wing

Figure 7.4: Lift force variation for changes in wing box position with equal box width for aspect ratio $AR = 25$

global lift coefficients $c_L$ for increasing aspect ratios, as it is the case for conventional wings.

Figure 7.5 (a) shows for the ‘back interface closed’, increasing mean torsion angles for increasing aspect ratios up to $AR = 22$. The kink in both mean and maximal torsion angle indicates nonlinear behavior in the interfaces. As for the wing box shift, the higher lift forces (in this case for increasing lift areas $S$) lead to slip in the closed interface. This nonlinear behavior influences the shear center position and therefore changes the reachable twist.

Figure 7.5: Torsion angle variation for changes in the wing’s aspect ratio for EBL front position 0.15

As before, the maximal torsion angle is not reversible for higher aspect ratios, but
only the mean torsion angle. In cases where a realistic pressure distribution is applied, no aerodynamic back driving forces at the tip support the recoiling of the wing tip twist.

![Graphs showing lift force and coefficient variation with aspect ratio](image)

Figure 7.6: Lift variation for changes in the wing's aspect ratio for EBL front position

To summarize, depending on the requirements, an optimal aspect ratio to fulfill the required twist changes and, more importantly, to ensure complete sticking for closed interfaces could be defined.

### 7.1.2. Comparison with Conventional Flaps

The conventional glider wing is optimized to maximize lift and features an asymmetric wing profile with positive lift for a zero angle of attack \( \text{AOA} \). In figure 7.7, the idea was not to compare absolute flow angles or lifts, but changes in lift.

The smart glider wing configuration exhibited an aspect ratio of \( AR = 25 \) to match the conventional glider dimensions and parameters as defined in table A.3. Comparing the results, the achievable changes for the compliant wing add up to around \( 1^\circ \) in changes of AOA and nearly \( 2^\circ \) in changes of flap angle. Percentaged, the maximal difference between high lift and low lift configuration peaks at 25 % which is sufficient for moderate maneuvers with conventional flap amplitude below 2°.

Without controlling, the amount of change in lift for the smart interfaces is not steerable, but only switchable from maximal to minimal lift. Furthermore, the allowable changes in lift coefficient \( \Delta c_L \) have to be enlarged, for example by reducing the lower wing thickness (see figure A.3 in the addendum).
7. Concept Application on a Glider Wing

![Graph showing lift coefficients comparison](image)

Figure 7.7: Comparison between lift coefficients of the conventional glider with flaps and the glider wing with smart interfaces.
8. Conclusion and Outlook

8.1. Summary

The main concept was to use electro bonded laminates as smart interfaces to alternate the warping stiffness of the wing box and, therefore, shift the shear center in a way to obtain moments of different sign which result in changes of the twist. This new semi-active wing concept allows energy efficient replacement of mechanical devices, such as flaps or ailerons, due to simple voltage variation and the use of aerodynamic flow energy. It could be shown that simple beams and more advanced structures, such as wings, are reversible in terms of torsion for consecutively opened and closed spar interfaces. This has been shown true for single loads and fully coupled aerelastic analyses. Secondly, a demonstrator wing was designed to be statically and dynamically stable at simultaneously low warp stiffness to reach sufficient changes in torsion. In a next step, the demonstrator was manufactured and tested under static tip loads. For the investigated interface states of front interface closed and back interface opened and vice versa, reproductive results that matched the simulation, quantitatively, were obtained. In a last step, the concept was applied to a mid-range glider wing and could demonstrate the working principle for a wing just slightly heavier than conventional glider airfoils.

8.2. Meaningfulness of the Results

8.2.1. Difficulties During the Measurements

Several difficulties during the measurement process, that lead to intense adaption of different main components of the test setup, are presented in this sub chapter. First of all, the first sandwich clamping was neither predictable nor of sufficient rigidity and therefore exchanged. Secondly, the linear guides exhibited significant slip and required additional end plates and clamping to avoid these relative movements. The EBL interfaces performed, despite different optimizations, such as bending soft electrode and depolarization, below the expectations.

8.2.2. Further Setup Improvements

Many changes to optimize the measured deflection were already implemented before the report was handed in. Regardless of this fact, known error sources that were too time-consuming or beyond our resources have to be fixed to ensure reliable results in the presented test configuration.
8. Conclusion and Outlook

- On the simulation side only small improvements are realizable because 3D coupled electromechanical modeling requires a lot of computation power.

- The mechanical performance of the EBL interfaces is around 1/3 of the theoretical one. Electrode surfaces and manufacturing accuracies of future interfaces should allow for better smoothness and lower tolerances in span wise direction.

- As mentioned before, polarized dielectric store residual charges over several hours, what limits interface switch speeds to a quasi static level. Smart de-polarization processes with inversely polarized voltage sources could overcome this problem.

- Measurements at the 'upmid' position showed to be very sensitive to small changes in laser position. It is therefore recommended to repeat the measurements for laser positions at the wing tip.

- The demonstrator was initially designed to be tested in a wind channel. As of now, the openings in the lower wing skin would lead to local turbulences and must be closed with a, in terms of shear, soft material.

8.3. Consolidated Findings and Next Research Steps

The development process and the subsequent tests have shown that despite already implemented corrections, several improvements especially on the manufacturing side are necessary. Nevertheless, this very first application of electro bonded laminates in complex structures showed the feasibility and predictability of such extremely smart materials.

- Electro bonded laminates were sufficiently modeled in their non linear behavior, with the help of simple electrostatic physics and 3D contact elements.

- The possibility to invert torsion by opening and closing the different interfaces was shown in simulation for simple beam structures, the manufactured demonstrator and a scaled-up glider wing. Thereby realistic span wise distributed lift forces were applied and iteratively adapted.

- Adaptively warping structures with sufficient static and dynamic properties that prevent flutter and divergence for realistic flight conditions were found.

- Although different nonlinearities in the material behavior and especially in the interfaces occurred, the final results were reproductive and stood in good agreement with the simulation results for low voltages.

- In structures, e.g. applications in industry, where the dielectric properties are not likely to suffer break downs, the nonlinear effects due to rail and guide movements are eliminated.
8. Conclusion and Outlook

In the future, ongoing and new projects to improve reliability and mechanical performance have to set new aspects on this very young concept. First of all, with airplanes in mind, fast switching between open and closed state to allow maneuvers is inevitable. Therefore, dielectric polarization and depolarization time must be steerable and predictable. Secondly, for the time being, the mechanical performance, e.g., shear stress transfer, allows only low force transfers and is not reliable for high loads.

Instead of interfaces where the electrodes attract each other to produce normal stresses, one could think about rejecting electrodes. Flexible supports would produce equally distributed normal forces depending on their spring constant to close the interfaces. The rejecting electrodes would, in this case, be charged to compensate the spring forces and obtain an opening of the interfaces.

Another problem is the decreasing amplitude of changes in torsion angle for subsequent interface switches without wing relaxation. An active controller could easily use the wing's inertia in case of interface switch to get from positive lift to negative lift; thereby, additional voltage switches to maintain the driving moment for lift changes from positive to negative would be required. This predictive control principle could only be realized for smart interfaces reduced to the wing tip and accurate sensors to measure actual static and dynamic properties.
A. Addendum
A. Addendum

Figure A.1.: Torsion angle variation for changes in rib thickness

Figure A.2.: Lift force variation for changes in rib thickness
A. Addendum

Figure A.3.: Torsion angle variation for changes in lower hull thickness

Figure A.4.: Lift force variation for changes in lower hull thickness
### Ansys definition

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A. Addendum
### A. Addendum

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Table A.2.: Chosen parameter to numerically rebuild the demonstrator
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