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Exploiting control surface reversal for active and passive manoeuvre load alleviation

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ABSTRACT

High-aspect-ratio wings offer significant aerodynamic efficiency gains but face substantial structural mass penalties because wing sizing is strongly driven by manoeuvre loads. Effective manoeuvre load alleviation is therefore a key enabler of such configurations. However, current manoeuvre load alleviation strategies still require trade-offs: active systems demand high torsional stiffness and therefore increase structural mass, whereas passive manoeuvre load alleviation through composite tailoring reduces roll control authority. This paper shows that intentionally operating control surfaces beyond their reversal point can combine key elements of both approaches within a single aeroelastic design framework, enabling simultaneous active and passive manoeuvre load alleviation while satisfying a prescribed steady-roll authority requirement. An aeroelastic optimisation framework incorporating analytical control-deflection sensitivities is developed and applied to a representative mid-range transport wing. Detailed aeroelastic analysis reveals that, in swept composite wings, control surface reversal is governed by wing-level spanwise load redistribution arising from bending-torsion coupling and misalignment between the elastic and flexural axes, rather than by a purely local sectional effect. Load redistribution extends far inboard of the control-surface region, providing an effective mechanism for shifting manoeuvre loads away from the outboard wing. The reversal-enabled design achieves 13.3 % structural mass reduction compared to a conventionally optimised baseline while meeting the prescribed steady-roll requirement in nominal operation and in a representative single-surface loss-of-function case with the midboard control surface disabled. These results indicate that, within the present modelling scope, control surface reversal can be purposefully exploited for manoeuvre load alleviation and structural-mass reduction in high-aspect-ratio wings.

1. Introduction

Aviation presents a significant challenge for global decarbonisation. It currently contributes roughly 2.5 % of anthropogenic CO₂ emissions, but about 4 % of observed human-induced warming when non-CO₂ effects are included; under resumed traffic growth, aviation could consume 6 % to 17 % of the remaining warming budget consistent with the 1.5 °C–2 °C targets by 2050 [1,2]. As shown by the Breguet range equation in Eq. (1), mission fuel burn is governed by propulsive efficiency through the specific fuel consumption, aerodynamic efficiency through C_L/C_D , and structural efficiency through the required fuel-mass fraction:

$$R = V \frac{1}{\text{TSFC}} \frac{C_L}{C_D} \ln \left(1 + \frac{M_{\text{fuel}}}{M_{\text{final}}} \right). \quad (1)$$

In the present work, the propulsion term is not addressed; instead, the focus is on the coupled aerodynamic and structural terms. High-aspect-ratio wings can improve aerodynamic efficiency by reducing in-

duced drag, but they also increase bending moments and manoeuvre-load-driven structural mass, creating a fundamental trade-off between aerodynamic and structural efficiency. Reducing structural mass lowers the required fuel fraction for a given range, improving overall energy efficiency and lowering emissions. Load alleviation techniques are therefore central to resolving this trade-off and enabling lighter high-aspect-ratio wings whose aerodynamic benefits would otherwise be offset by increased structural loads.

Two established strategies exist, each with its inherent limitations. *Active load alleviation* uses control surface deflections to modify aerodynamic loads during flight [3]. While widely applied in operational aircraft and extensively studied through conventional scheduling [4], morphing surfaces [5], and distributed control concepts [6,7], active aeroelastic control also includes related strategies for dynamic response and flutter suppression, for example using flapped airfoil models [8] and piezoelectric actuation [9]. In the MLA context considered here, however, active systems require high torsional stiffness to maintain control effectiveness, incurring a mass penalty that can offset load-reduction

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Nomenclature

α	Angle of attack
δ	Control surface deflection angle
η_{eff}	Roll control effectiveness
ρ	Air density
Γ	Vortex strength vector
C_L	Lift coefficient
C_D	Drag coefficient
p_{roll}	Steady roll rate
V_∞	Free-stream velocity magnitude
$\mathbf{V}_\infty$	Free-stream velocity vector
s	Wing semispan
c	Local chord length
$\mathbf{K}_{\text{st}}$	Static aerodynamic influence coefficient matrix
$\mathbf{K}_a$	Aerodynamic stiffness matrix
$\mathbf{K}_s$	Structural stiffness matrix
$\mathbf{F}_{\text{st}}$	Steady aerodynamic force vector
$\mathbf{F}_{\text{aero}}^s$	Aerodynamic loads in structural coordinates
$\mathbf{F}_s$	Internal structural forces and moments
$\mathbf{M}_{\text{st}}$	Steady aerodynamic moment vector
$\mathbf{n}_p$	Panel normal vector
$\mathbf{u}$	Structural displacement vector
$l(y)$	Spanwise lift distribution (lift per unit span)
ΔL	Change in lift carried by a spanwise strip
x_{FA}	Chordwise position of flexural axis
r_{cr}	Buckling factor
$\epsilon_x^{\text{max}}, \epsilon_x^{\text{min}}$	Maximum and minimum principal strains
γ^{max}	Maximum shear strain

benefits [10]. *Passive MLA through composite tailoring* exploits bend-twist coupling in composite laminates to redistribute loads toward the wing root through washout deformation [11]. While inherently reliable, this approach reduces roll control authority, a dominant constraint in optimisation studies [12], and may limit achievable mass savings.

For MLA design, the opposing requirements of active control and passive tailoring, high torsional stiffness for control effectiveness versus structural tailoring that promotes passive washout through torsional compliance and bend-twist coupling, have motivated renewed interest in operating beyond the control-reversal regime. In this context, control surface reversal refers to the aeroelastic condition in which structural deformation changes the sign of the roll response to a prescribed antisymmetric control-surface deflection, so that the same control input produces a rolling moment in the opposite direction. Theoretically, operating a control surface beyond the reversal onset can regain or even enhance control authority [13]. Experimental data from the F/A-18 X-53 Active Aeroelastic Wing programme demonstrated this effect in practice [14]. Subsequent studies explored ways to exploit or shift reversal through variable-sweep wingtips [15], post-reversal behaviour analysis [16], and adaptive stiffness concepts such as variable-stiffness spars. More recently, Sharpe et al. [17] introduced “tailerons”, control surfaces capable of reversal at very low airspeeds for divergence and flutter suppression, demonstrated on the Dawn One solar aircraft.

Despite substantial progress in both active and passive load alleviation methods, these methods are still developed independently or applied sequentially, without fully accounting for the strong coupling between aeroelastic stiffness, control effectiveness, and spanwise load redistribution. In classical textbook methods, control surface reversal is commonly introduced using typical-section models, in which the phenomenon is interpreted primarily through the local sectional response to control-surface deflection [13]. Sharpe et al. [17] likewise build on this classical 2D framework, while validating key trends with aircraft-level analysis. The present study supports a different interpretation for flexible, swept composite wings: the reversal relevant to MLA is governed not merely by a local sign change in sectional lift near the control surface,

but by wing-level spanwise lift redistribution driven by bending-torsion coupling and misalignment between the elastic and flexural axes. This redistribution extends far inboard of the control-surface region.

Within this context, the present study introduces a unified design approach in which operation in the post-reversal regime is not treated as a limitation, but as an intentional mechanism for combining active control surface load alleviation with passive composite tailoring. In this view, control surface reversal is used to remove the conventional conflict between active and passive MLA: the aeroelastic flexibility that would normally reduce control authority is instead treated as a design mechanism that enables load redistribution, while the optimisation still enforces the required steady-roll authority.

To investigate this concept, an aeroelastic optimisation framework is developed that combines active MLA with passive MLA through composite tailoring by intentionally exploiting control surface reversal. The framework is implemented in the in-house tool PROTEUS, extended with analytical sensitivities for control-surface deflections, verified via the complex-step method, and embedded within the Convex Conservative Separable Approximations (CCSA) class of optimisation algorithms [18]. The framework is applied to a representative mid-range transport wing to assess whether intentional use of post-reversal control-surface behaviour can reduce structural mass while maintaining adequate roll authority.

The remainder of the paper is organised as follows: Section 2 presents the coupled aeroelastic model, Section 3 details the sensitivity formulation and optimisation framework, and Section 4 discusses the optimisation results and the aeroelastic mechanisms governing control surface reversal. The conclusions are summarised in Section 5.

2. Aeroelastic model

This section presents the formulation of the coupled aeroelastic framework used in this study. The model couples a linear aerodynamic model based on the Vortex Lattice Method (VLM) with a geometrically nonlinear Timoshenko beam structural model. Furthermore, the model incorporates control surface deflections to account for MLA effects. The following subsections detail the aerodynamic and structural formulations, control surface modelling approach, and resulting aeroelastic responses, which form the foundation for the sensitivity analysis presented in Section 3.

2.1. Structural model

The wing structure is initially divided into discrete laminate regions, each characterised by distinct laminate properties corresponding to the various components of the wing box, such as the upper and lower skins, and the front and rear spars. Each laminate region is parameterised by its thickness and by Lamination Parameters (LPs), which provide a continuous description of laminate stiffness properties suitable for efficient gradient-based optimisation. The cross-sectional modeller developed by Ferde and Abdalla [19] is then used to condense the wing box geometry and laminate properties into equivalent Timoshenko stiffness matrices for each finite element. In this beam representation, ribs are not modelled as independent elements contributing to stiffness; they are used to define the spanwise panel length for local buckling. Stringers are included in the cross-sectional model, where their axial stiffness and mass contribute to the equivalent sectional properties, and their spacing defines the chordwise buckling panel width. A co-rotational framework, as described by Battini and Pacoste [20], is adopted to formulate a geometrically nonlinear Timoshenko beam model that employs a load-control approach in combination with the Newton-Raphson root-finding method to find a nonlinear aeroelastic equilibrium. Further details on the development of the structural formulation can be found in [21].

With the structural formulation established, the next step is to introduce the aerodynamic model based on VLM which will be later in-

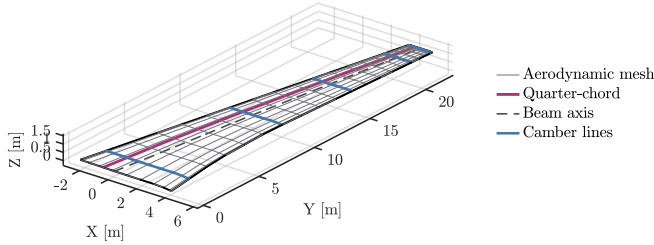


Fig. 1. Visualisation of the aerodynamic model used in the analysis.

tegrated with the beam formulation to develop the coupled aeroelastic model.

2.2. Aerodynamic model

The aerodynamic model is based on the VLM, which is a linear approach based on potential flow theory [22]. This method assumes a thin wing and discretises the camber surface into vortex ring elements in both the chordwise and spanwise directions. The aerodynamic mesh represents the wing by a set of rigid airfoil sections distributed along the span, each described by a camber line that is translated and rotated according to the local beam deformation. The flow is assumed to be inviscid and irrotational, while compressibility effects are accounted for using the Prandtl-Glauert transformation, extending the validity of the model to the high-subsonic regime. Because the VLM solves directly for the bound-circulation distribution over the lifting surface, it provides spanwise loading and the incremental aerodynamic response to control-surface deflections in a form well suited to static aeroelastic and control-effectiveness calculations for attached-flow wings [22]. In addition, it remains computationally efficient for design exploration. A visualisation of the aerodynamic model is provided in Fig. 1. The figure shows the half-wing aerodynamic mesh, the quarter-chord line, the structural beam axis, and representative camber line sections. It also illustrates that the aerodynamic discretisation follows the prescribed wing geometry, including the twist distribution and the section camber representation.

The steady-state vortex strength distribution on the wing surface is obtained from

$$\mathbf{K}_{st} \mathbf{\Gamma}_b = -V_\infty \cdot \mathbf{n}_p, \quad (2)$$

where $\mathbf{K}_{st}$ denotes the static Aerodynamic Influence Coefficients (AIC) matrix, $\mathbf{\Gamma}_b$ is the bound-vortex strength vector, V_∞ the free-stream velocity vector, and $\mathbf{n}_p$ the normal vector of the panel.

After solving Eq. (2), the steady aerodynamic lift per panel is computed as

$$\mathbf{F}_{st} = \rho V_\infty \mathbf{\Gamma}_b, \quad (3)$$

and the corresponding aerodynamic moment about the reference axis as

$$\mathbf{M}_{st} = \mathbf{r} \times \mathbf{F}_{st}, \quad (4)$$

where ρ is the air density, V_∞ the magnitude of the free-stream velocity, and $\mathbf{r}$ the position vector from the reference axis to the aerodynamic force application point.

2.3. Control surface modelling

MLA is achieved by deflecting the wing's control surfaces, thereby modifying the aerodynamic loads acting on the wing. To account for these effects, the aerodynamic solver of PROTEUS was extended to include control surface deflections in the symmetric aeroelastic solution.

Within the VLM formulation, control surface deflections can be represented either by explicitly deflecting the aerodynamic mesh - requiring recomputation of the AIC matrix for each configuration - or by keeping the aerodynamic mesh and AIC matrix fixed while implicitly incorporating the deflection through local modification of the panel normals,

as illustrated in Fig. 2. In the present work, the latter, more computationally efficient approach is adopted.

The total panel normal vector, including control surface deflection effects, is expressed as

$$\mathbf{n}_p^{MLA} = \mathbf{R}_\delta \mathbf{n}_p, \quad (5)$$

where $\mathbf{R}_\delta$ is the Rodrigues' rotation matrix about the hinge axis associated with a control surface deflection angle δ . This formulation avoids repeated computation of $\mathbf{K}_{st}$ and requires only the system in Eq. (2) to be solved for each deflection case, resulting in a significant reduction in computational cost.

2.4. Aeroelastic responses

This subsection presents the evaluation of the responses obtained from the coupled aeroelastic model. The responses include the static aeroelastic equilibrium, the roll control effectiveness under manoeuvre conditions, and the principal-strain and buckling analyses.

2.4.1. Static aeroelastic response

The aerodynamic loads obtained from the VLM are initially expressed in the aerodynamic coordinate system and are transferred to the structural domain through

$$\mathbf{F}_{aero}^s = \mathbf{T}_{AS} \mathbf{R}_\alpha \mathbf{F}_{aero}^a = \mathbf{T}_{AS} \mathbf{R}_\alpha \begin{bmatrix} \mathbf{F}_{st} \\ \mathbf{M}_{st} \end{bmatrix}, \quad (6)$$

where the rotation matrix $\mathbf{R}_\alpha$ transforms the aerodynamic loads $\mathbf{F}_{aero}^a$ from the aerodynamic frame to the body-fixed coordinate system through the aircraft angle of attack, α . The transformation matrix $\mathbf{T}_{AS}$ then maps the resulting loads onto the structural beam nodes.

As formulated by Werter [12], the static aeroelastic equilibrium is governed by the balance of the internal structural forces and moments, $\mathbf{F}_s$, and the externally applied loads, $\mathbf{F}_{ext}$,

$$\mathbf{F}_s - \mathbf{F}_{ext} = \mathbf{F}_s - \mathbf{F}_{aero}^s - \mathbf{F}_e = \mathbf{0}, \quad (7)$$

where $\mathbf{F}_{aero}^s$ denotes the aerodynamic loads mapped to the structural degrees of freedom, such as Eq. (6), and $\mathbf{F}_e$ represents other external forces and moments.

Linearising Eq. (7) around an equilibrium configuration $\mathbf{u}_0$ yields

$$(\mathbf{K}_a + \mathbf{K}_e - \mathbf{K}_s) \Delta \mathbf{u} = \mathbf{F}_s(\mathbf{u}_0) - \mathbf{F}_{aero}^s(\mathbf{u}_0) - \mathbf{F}_e(\mathbf{u}_0) = \mathbf{R}_0, \quad (8)$$

where $\mathbf{K}_a$ is the aerodynamic stiffness matrix, $\mathbf{K}_e$ is the stiffness matrix of the externally applied loads, $\mathbf{K}_s$ is the structural stiffness matrix, and $\mathbf{R}_0$ the residual force vector. The system in Eq. (8) is solved using the Newton-Raphson root-finding method until the norm of the residual force vector, $\mathbf{R}_0$, falls below a prescribed convergence tolerance, resulting in the closely coupled static aeroelastic equilibrium solution.

2.4.2. Roll control effectiveness

Control effectiveness is evaluated to quantify the authority of the control surfaces for the selected design Load Cases (LCs). For the anti-symmetric analyses used here, a positive deflection δ denotes a tip-down rotation of the control surface on the reference half-wing, with an equal and opposite deflection applied on the opposite wing. The roll control effectiveness, η_{eff} , is defined as the negative ratio between the rolling moment coefficient induced by a control surface deflection, C_{L_δ} , and the rolling moment coefficient due to roll damping, C_{L_p} ,

$$\eta_{eff} = -\frac{C_{L_\delta}}{C_{L_p}} = \frac{p_{roll} s}{\delta V_\infty}, \quad (9)$$

where p_{roll} is the steady roll rate resulting from a control surface deflection δ , s is the wing semispan, and V_∞ is the free-stream velocity.

To compute p_{roll} , an antisymmetric aeroelastic analysis is performed around the steady symmetric aeroelastic solution. In this analysis, each vortex ring element on one wing has a corresponding image element with equal magnitude and opposite circulation on the opposite wing.

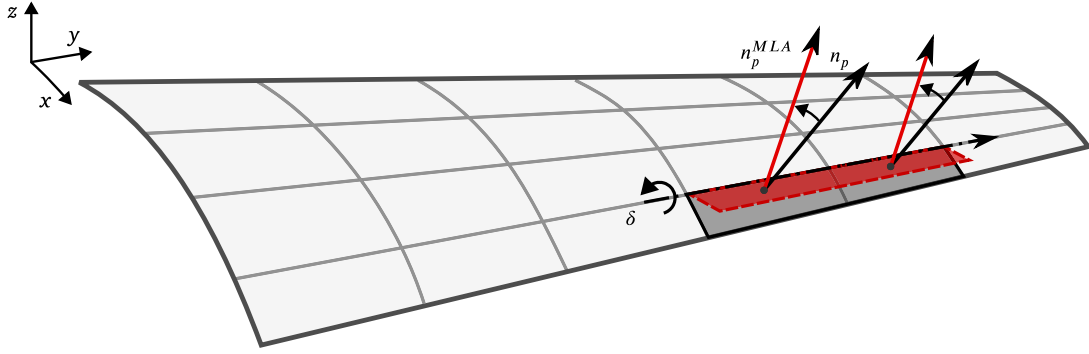


Fig. 2. Schematic representation of the aerodynamic mesh with control surface deflection.

The flow tangency condition is augmented to account for the local velocity induced by roll motion. This introduces the steady roll rate, p_{roll} , as an additional unknown, which is determined by appending the roll-moment equilibrium condition to the system in Eq. (8).

2.4.3. Principal-strain and buckling analysis

To evaluate the principal-strain and buckling responses used as structural constraints, the cross-sectional modeller developed by Ferde and Abdalla [19] is employed to convert the beam force resultants into the corresponding shell strains within each cross-section. The required cross-sectional forces, $\mathbf{f}_\ell$, are obtained directly from the local element deformations resulting from the static or dynamic structural response,

$$\mathbf{K}_\ell \mathbf{p}_\ell = \mathbf{f}_\ell, \quad (10)$$

where $\mathbf{K}_\ell$ and $\mathbf{p}_\ell$ denote the local element stiffness matrix and deformation vector, respectively. Further details on the formulation of the elemental stiffness matrix can be found in [21].

Within the cross-sectional modeller, the corresponding shell strains are expressed as

$$\boldsymbol{\epsilon} = \mathbf{H} \mathbf{f}_\ell \quad (11)$$

where $\mathbf{H}$ represents the transformation matrix that relates the force resultants to the shell strain distribution. The derivation of this matrix is an integral part of the cross-sectional analysis and is detailed in [19] (more specifically, Eq. 40). From the resulting shell strain field, the maximum and minimum principal strains together with the maximum shear strain are extracted for each structural region and compared with the corresponding strain allowables. These quantities constitute the strain-based structural constraints used in the optimisation.

Buckling analysis is performed following the methodology developed by Dillinger [23], in which each panel between two ribs and two stringers is analysed as a simply supported flat plate subjected to in-plane loads obtained from the aeroelastic loads via the cross-sectional modeller. In the present model, the rib spacing defines the spanwise buckling panel length, while the stringer pitch defines the chordwise buckling panel width. The simply supported boundary condition holds under the assumption that the stiffeners neither buckle nor separate from the panel, and is intended as a conservative approximation since the actual panel support is generally closer to an elastically restrained or partially clamped condition. The inverse buckling factor is obtained by solving the following eigenvalue problem,

$$(\mathbf{K}_g - r\mathbf{K})\mathbf{a} = \mathbf{0}, \quad (12)$$

where $\mathbf{K}_g$ is the geometric stiffness matrix, $\mathbf{K}$ is the panel stiffness matrix, r is the inverse buckling factor and $\mathbf{a}$ is the mode shape. Further details can be found in [23].

3. Control-surface sensitivity analysis and optimisation

This section presents the analytical formulation of sensitivities with respect to control-surface deflections, their verification, and their appli-

cation within a gradient-based optimisation framework. The sensitivities of aeroelastic responses with respect to control surface deflections are derived using the direct method, verified using the complex-step method, and employed within algorithms based on the CCSA framework.

3.1. Direct sensitivity formulation

In gradient-based optimisation, it is essential to compute the total derivative $df/d\mathbf{x}$, where f is a function of interest and $\mathbf{x}$ is the vector of the design variables. Such a function of interest is usually dependent on both the design variables, $\mathbf{x}$, and state variables $\mathbf{w}$,

$$f = f(\mathbf{x}, \mathbf{w}). \quad (13)$$

By differentiating Eq. (13) with respect to $\mathbf{x}$, the total derivative $df/d\mathbf{x}$ is calculated via the chain rule,

$$\frac{df}{d\mathbf{x}} = \frac{\partial f}{\partial \mathbf{x}} + \frac{\partial f}{\partial \mathbf{w}} \frac{d\mathbf{w}}{d\mathbf{x}}, \quad (14)$$

where the partial derivatives $\partial f/\partial \mathbf{x}$ and $\partial f/\partial \mathbf{w}$ account only for the explicit dependence of f on the design variables and state variables, whereas the more expensive total derivative $d\mathbf{w}/d\mathbf{x}$ captures the implicit dependence of the state variables on the design variables and is computed from the residual equations, $\mathbf{R}(\mathbf{x}, \mathbf{w}) = \mathbf{0}$, by applying the chain rule,

$$\frac{d\mathbf{R}}{d\mathbf{x}} = \frac{\partial \mathbf{R}}{\partial \mathbf{x}} + \frac{\partial \mathbf{R}}{\partial \mathbf{w}} \frac{d\mathbf{w}}{d\mathbf{x}} = \mathbf{0}, \quad (15)$$

and by rearranging,

$$\frac{\partial \mathbf{R}}{\partial \mathbf{w}} \frac{d\mathbf{w}}{d\mathbf{x}} = -\frac{\partial \mathbf{R}}{\partial \mathbf{x}}, \quad (16)$$

Moreover, by solving this linear system n_x times, once for each design variable, one obtains the matrix $d\mathbf{w}/d\mathbf{x}$.

This approach is commonly referred to as the *direct method* for computing derivatives. An alternative frequently employed in Computational Fluid Dynamics (CFD) is the *adjoint method*, in which the number of linear systems to be solved scales with the number of functions of interest, n_f [24]. In realistic aeroelastic optimisation problems, however, the number of failure constraints per LC is typically much larger than the number of design variables, making the direct method computationally preferable [12].

An independent numerical reference is nevertheless required to verify the correctness of the implemented analytical sensitivities. For this purpose, the complex-step method is attractive because it provides near-machine-precision derivative estimates without the subtractive-cancellation errors associated with finite differences. However, it requires complex-arithmetic support throughout the solver and one additional complex-valued analysis per design variable. Analytical derivatives are therefore retained for the optimisation loop, while complex-step results are used only for verification.

The generic direct-sensitivity relations in Eqs. (14) and (16) are now specialised to the present aeroelastic equilibrium problem. The state variables are the structural deformations, $\mathbf{u}$, and the residual is defined from the static aeroelastic equilibrium in Eq. (7) as $\mathbf{R}(\mathbf{x}, \mathbf{u}) = \mathbf{F}_s - \mathbf{F}_{\text{aero}}^s - \mathbf{F}_e = \mathbf{0}$. Substituting this residual definition into Eq. (16) yields

$$\frac{\partial(\mathbf{F}_s - \mathbf{F}_{\text{aero}}^s - \mathbf{F}_e)}{\partial \mathbf{u}} \frac{d\mathbf{u}}{d\mathbf{x}} = -\frac{\partial(\mathbf{F}_s - \mathbf{F}_{\text{aero}}^s - \mathbf{F}_e)}{\partial \mathbf{x}}, \quad (17)$$

With $\mathbf{w} = \mathbf{u}$, the total-derivative relation in Eq. (14) becomes

$$\frac{df}{d\mathbf{x}} = \frac{\partial f}{\partial \mathbf{x}} + \frac{\partial f}{\partial \mathbf{u}} \frac{d\mathbf{u}}{d\mathbf{x}}, \quad (18)$$

The derivative of the residual with respect to the state variables defines the aeroelastic tangent stiffness matrix, $\mathbf{K}_{AE}$,

$$\mathbf{K}_{AE} = \frac{\partial(\mathbf{F}_s - \mathbf{F}_{\text{aero}}^s - \mathbf{F}_e)}{\partial \mathbf{u}}, \quad (19)$$

which is already implemented in PROTEUS.

Using this definition, Eq. (17) can be written as

$$\mathbf{K}_{AE} \frac{d\mathbf{u}}{d\mathbf{x}} = -\frac{\partial(\mathbf{F}_s - \mathbf{F}_{\text{aero}}^s - \mathbf{F}_e)}{\partial \mathbf{x}}. \quad (20)$$

In the present implementation, the additional design variables are the control-surface deflections, δ . Since the internal structural forces and the other external loads do not explicitly depend on δ , Eq. (20) further simplifies to

$$\mathbf{K}_{AE} \frac{d\mathbf{u}}{d\delta} = \frac{\partial \mathbf{F}_{\text{aero}}^s}{\partial \delta}, \quad (21)$$

where the sensitivity of aerodynamic forces with respect to control surface deflections, $\partial \mathbf{F}_{\text{aero}}^s / \partial \delta$, is still unknown.

3.2. Computation of aerodynamic and structural sensitivities

With the direct sensitivity framework established, the required partial derivatives must be evaluated for the aerodynamic and structural components of the coupled system.

3.2.1. Aerodynamic sensitivities

From Eq. (3), the sensitivity of the aerodynamic load per aerodynamic panel with respect to a control surface deflection δ is given by,

$$\frac{\partial \mathbf{F}_{\text{st}}}{\partial \delta} = \rho V_\infty \frac{\partial \Gamma}{\partial \delta}. \quad (22)$$

Differentiating the VLM system in Eq. (2) with respect to δ gives

$$\mathbf{K}_{\text{st}} \frac{\partial \Gamma}{\partial \delta} + \frac{\partial \mathbf{K}_{\text{st}}}{\partial \delta} \Gamma = \frac{\partial \mathbf{B}_{\text{st}}}{\partial \delta}, \quad (23)$$

where $\mathbf{B}_{\text{st}} = -\mathbf{V}_\infty \cdot \mathbf{n}_p^{\text{MLA}} = -\mathbf{V}_\infty \cdot \mathbf{R}_\delta \mathbf{n}_p$, according to Eq. (5). Since the aerodynamic mesh remains fixed, $\partial \mathbf{K}_{\text{st}} / \partial \delta$ is neglected, and the vortex-strength sensitivity follows from

$$\mathbf{K}_{\text{st}} \frac{\partial \Gamma}{\partial \delta} = \frac{\partial \mathbf{B}_{\text{st}}}{\partial \delta}. \quad (24)$$

Using Eq. (5), the right-hand side becomes

$$\frac{\partial \mathbf{B}_{\text{st}}}{\partial \delta} = -\mathbf{V}_\infty \frac{\partial \mathbf{R}_\delta}{\partial \delta} \mathbf{n}_p. \quad (25)$$

Once the Eq. (24) is solved, the sensitivity of the steady aerodynamic load per panel with respect to control surface deflection, $\partial \mathbf{F}_{\text{st}} / \partial \delta$ can be computed from Eq. (22). A similar method is applied to the calculation of the derivative of the steady aerodynamic moment around the reference axis, $\partial \mathbf{M}_{\text{st}} / \partial \delta$. Finally, the derivatives of the aerodynamic loads in the structural domain are obtained as,

$$\frac{\partial \mathbf{F}_{\text{aero}}^s}{\partial \delta} = \mathbf{T}_{AS} \mathbf{R}_\alpha \left[\frac{\partial \mathbf{F}_{\text{st}}}{\partial \delta}, \frac{\partial \mathbf{M}_{\text{st}}}{\partial \delta} \right]. \quad (26)$$

By utilising Eq. (26), it is now possible to solve the linear system of equations Eq. (21) to obtain $d\mathbf{u}/d\delta$.

Eq. (18) can now be utilised to calculate the derivatives of any function of interest with respect to control surface deflections,

$$\frac{df}{d\delta} = \frac{\partial f}{\partial \delta} + \frac{\partial f}{\partial \mathbf{u}} \frac{d\mathbf{u}}{d\delta}. \quad (27)$$

3.2.2. Principal-strain and buckling sensitivities

The sensitivities of structural responses, such as strain and buckling, are obtained using Eq. (27), where the aerodynamic sensitivities provide the right-hand side of Eq. (21).

Strain sensitivities. By utilising Eq. (27), and noting that ϵ does not explicitly depend on δ and that the matrix $\mathbf{H}$ is constant,

$$\frac{d\epsilon}{d\delta} = \frac{\partial \epsilon}{\partial \mathbf{u}} \frac{d\mathbf{u}}{d\delta} = \mathbf{H} \frac{\partial \mathbf{f}_\epsilon}{\partial \mathbf{u}} \frac{d\mathbf{u}}{d\delta}. \quad (28)$$

where the sensitivity of elemental forces with respect to elemental deformations, $\partial \mathbf{f}_\epsilon / \partial \mathbf{u}$, is already implemented in PROTEUS by De Breuker [21] and $d\mathbf{u}/d\delta$ is obtained by solving Eq. (21).

Buckling sensitivities. In a similar way, the total derivative of the inverse buckling factor, r , with respect to the control surface deflection, δ , can be obtained, noting that r does not explicitly depend on δ ,

$$\frac{dr}{d\delta} = \frac{\partial r}{\partial \delta} + \frac{\partial r}{\partial \mathbf{u}} \frac{d\mathbf{u}}{d\delta} = \frac{\partial r}{\partial \mathbf{u}} \frac{d\mathbf{u}}{d\delta}, \quad (29)$$

by applying the chain rule, $\partial r / \partial \mathbf{u}$ is computed as,

$$\begin{aligned} \frac{\partial r}{\partial \mathbf{u}} &= \frac{\partial r}{\partial \mathbf{N}} \frac{\partial \mathbf{N}}{\partial \epsilon} \frac{\partial \epsilon}{\partial \mathbf{u}} = \frac{\partial r}{\partial \mathbf{N}} \frac{\partial(\mathbf{A}\epsilon)}{\partial \epsilon} \frac{\partial(\mathbf{H}\mathbf{f}_\epsilon)}{\partial \mathbf{u}} \rightarrow \\ \frac{\partial r}{\partial \mathbf{u}} &= \frac{\partial r}{\partial \mathbf{N}} \mathbf{A} \mathbf{H} \frac{\partial \mathbf{f}_\epsilon}{\partial \mathbf{u}}, \end{aligned} \quad (30)$$

where $\mathbf{N} = \mathbf{A}\epsilon$ is the vector of force resultants and $\mathbf{A}$ is the membrane stiffness of the analysed panel. The expression of the derivative of the inverse buckling factor with respect to the force resultants can be found in [23]. Then, the total derivative can be obtained as,

$$\frac{dr}{d\delta} = \frac{\partial r}{\partial \mathbf{N}} \mathbf{A} \mathbf{H} \frac{\partial \mathbf{f}_\epsilon}{\partial \mathbf{u}} \frac{d\mathbf{u}}{d\delta}, \quad (31)$$

3.3. Verification of sensitivities

This subsection presents the verification of the sensitivities computed using the direct method by comparison with the complex-step method [25]. The complex-step method provides an accurate approximation of derivatives by perturbing a real function with a small imaginary step and taking the imaginary part, avoiding subtractive cancellation errors inherent to finite-difference approximations. The derivative is computed as

$$\frac{\partial f}{\partial x} \approx \frac{\text{Im}[f(x + ih)]}{h}, \quad (32)$$

where h is a very small imaginary step size; in the present verification, $h = 10^{-50}$ was used. Comparison against the complex-step method verifies the correctness of the direct sensitivity implementation and supports the use of these analytical sensitivities in the subsequent analyses and optimisation.

3.3.1. Single-point verification on a rectangular wing

A simple rectangular wing equipped with two control surfaces, as introduced in Fig. 3, was analysed to verify the implementation of the direct sensitivity method at a single design point. The LC details are given in Table 1. It is important to note that this configuration serves solely for verification purposes; therefore, it does not represent a fully realistic load case scenario but provides a sufficient basis to verify the accuracy of the developed analytical sensitivities.

First, a component-level verification was performed for the computation of $\partial \mathbf{F}_{\text{st}} / \partial \delta$. The resulting relative error of the panel aerodynamic

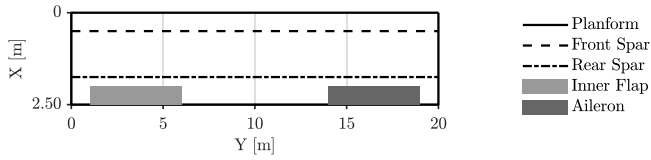


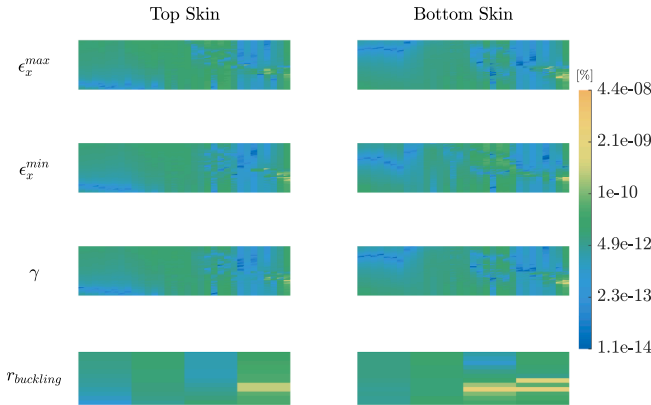
Fig. 3. Geometry of the simple rectangular wing showing planform, structural layout and movable surface arrangement.

Table 1
Load case considered in the verification study.

LC	EAS (m/s)	M	Altitude (m)	n_z
1	100	0.3	100	1.0



(a) Aerodynamic sensitivity errors: inboard and outboard control-surface deflections.



(b) Structural sensitivity errors: strain and buckling responses.

Fig. 4. Verification of analytical sensitivities against the complex-step method, presented on the wing level: (a) component-level aerodynamic sensitivities; (b) structural response sensitivities.

forces sensitivity is shown in Fig. 4a, where the upper plot corresponds to the sensitivities with respect to the inboard control surface and the lower plot to the outboard control surface. The maximum error remains below 10^{-10} , indicating excellent agreement between the two methods and confirming the correctness of the analytical implementation.

Subsequently, the relative error between the two methods was evaluated for the derivatives of key structural response quantities: the maximum and minimum principal strains (ϵ_x^{max} , ϵ_x^{min}), the maximum shear strain γ^{max} , and the inverse buckling factor r . The error distribution is shown in Fig. 4b for the inboard control surface only, as the corresponding results for the other control surfaces exhibit the same behaviour and are omitted for brevity. The maximum error remains below 10^{-7} , demonstrating excellent agreement between the two methods for all structural response quantities and further confirming the accuracy of the direct sensitivity implementation.

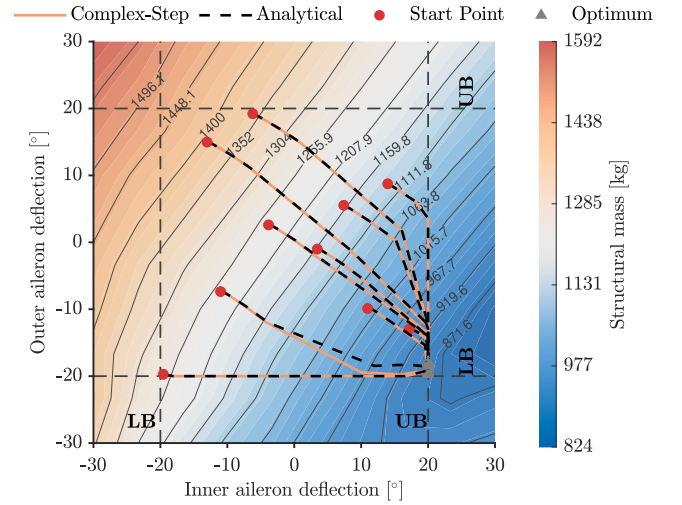


Fig. 5. Structural mass contours obtained from prescribed inboard and outboard control surface deflections within $\pm 20^\circ$, with optimisation trajectories from runs in which both deflections are design variables. The plot axes are extended to $\pm 30^\circ$ for visual context.

3.3.2. Sensitivity verification across the design space using latin hypercube sampling

To evaluate the robustness of the computed sensitivities across the design space, two complementary studies were performed using the same rectangular wing configuration. First, the inboard and outboard control surface deflections were prescribed inputs rather than optimisation variables, thereby defining a two-dimensional design space. Each deflection was varied in ten uniform increments between -20° and 20° , resulting in a total of 100 combinations. For each prescribed deflection pair, a structural optimisation was carried out to minimise the structural mass subject to strain and buckling constraints, while optimising only the structural design variables. The resulting structural-mass values define the contour map shown in Fig. 5. The axes of this plot are extended to $\pm 30^\circ$ only for visual context; no prescribed cases were analysed outside the $\pm 20^\circ$ range.

Second, the control surface deflections were also included as design variables in additional optimisation runs, again with bounds of $\pm 20^\circ$. To ensure a broad exploration of the admissible region, the initial design points were generated using Latin Hypercube Sampling (LHS). The corresponding convergence histories are overlaid in Fig. 5. Both the direct and complex-step sensitivity methods produced nearly identical convergence paths, confirming the accuracy and consistency of the computed sensitivities throughout the admissible design space. Furthermore, both approaches converged to the same region, corresponding to the global minimum identified from the prescribed-deflection contour map.

The contour distribution in Fig. 5 also highlights several aeroelastic trends. The diagonally oriented contour lines indicate coupling between the inboard and outboard control surfaces, meaning that their influence on structural mass is not independent. All optimisation trajectories converge toward the same minimum region, corresponding to a configuration with the inboard control surface deflected downward (positive deflection) and the outboard control surface deflected upward (negative deflection). This configuration effectively redistributes the aerodynamic loads toward the wing root, thereby reducing bending moments and overall structural mass.

3.4. MMA and GCMMA optimisation algorithms

Due to the highly constrained design space of the problems presented in this study, the CCSA framework proposed by Svanberg [18] was utilised, as it is well-suited to handle heavily constrained design problems, such as aeroelastic optimisation. Within this framework, the

nonlinear optimisation problem is replaced at each iteration by a convex, separable subproblem constructed from local first-order approximations of the objective and constraints. Two implementations of this approach are employed: the Method of Moving Asymptotes (MMA) and its globally convergent variant, the Globally Convergent Method of Moving Asymptotes (GCMMA).

The MMA [26] constructs convex, separable approximations using moving asymptotes that adaptively control the curvature of each variable's local model. While efficient and fast-converging for well-behaved problems, MMA lacks an explicit damping mechanism, which can lead to non-conservative updates in highly nonlinear regions. The GCMMA [18] extends this framework by introducing adaptive damping factors, ρ_j , that ensure conservative approximations and monotonic convergence.

The damping factor plays a central role in balancing convergence robustness and efficiency [27]. High damping values promote conservativeness and guarantee feasibility but can overly restrict design updates, leading to slow convergence or premature stagnation of the optimisation process. Conversely, low damping values enable faster convergence but may compromise stability when constraint gradients are inaccurate. In the present study, both GCMMA and MMA were employed to mitigate the influence of the damping factor, depending on the optimisation behaviour. The development of a more sophisticated initialisation and update strategy for the damping parameters, as proposed by IJsselmuide [27], will be considered in future work.

4. Analysis and results

This section presents the application of the developed modelling and optimisation framework to the structural design of a representative mid-range transport aircraft wing. Following a brief description of the reference model and its parameterisation, two optimisation studies are conducted to explore different control-effectiveness objectives. One is constrained to maintain conventional control authority, while the other allows operation beyond the control-reversal regime of the outboard control surface. The results are then analysed to assess the influence of control surface reversal on active MLA and passive MLA through composite tailoring. Finally, a detailed aeroelastic analysis of the reversal-enabled configuration is performed to clarify the wing-level aeroelastic mechanisms governing control surface reversal.

4.1. Model set-up

The reference configuration is the SE²A Mid-Range aircraft [28], representative of a 150-passenger single-aisle transport comparable to the Airbus A320. The aircraft aims to balance operating costs and CO₂-equivalent emissions, including contrail effects, resulting in a cruise altitude of 7,650 m at a cruise speed of Mach 0.71. It features over-wing engines, an increased aspect ratio, and a moderately swept wing. The main geometric and performance parameters are listed in Table 2.

Fig. 6a illustrates the planform geometry, structural layout, movable surface configuration, and non-structural mass distribution. The wing is discretised into five spanwise design sections of equal length, each divided into four regions: upper and lower skins, and front and rear spars¹. Each laminate is defined by eight LPs (four for the in-plane A matrix and four for the bending D matrix), as well as its total thickness t . For clarity, Fig. 6b illustrates the structural design discretisation used throughout the paper, including the 20 component-wise design regions for skins and spars. The unidirectional CFRP material properties are listed in Table 5.

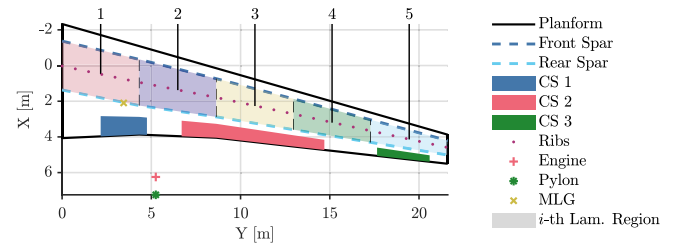
Three control surfaces are present on each half-wing, as shown in Fig. 6a. These are denoted as the inboard control surface (CS 1), the midboard control surface (CS 2), and the outboard control surface (CS 3).

¹ This structural convention and ordering are used consistently across all figures showing thickness, stiffness, and constraint distributions.

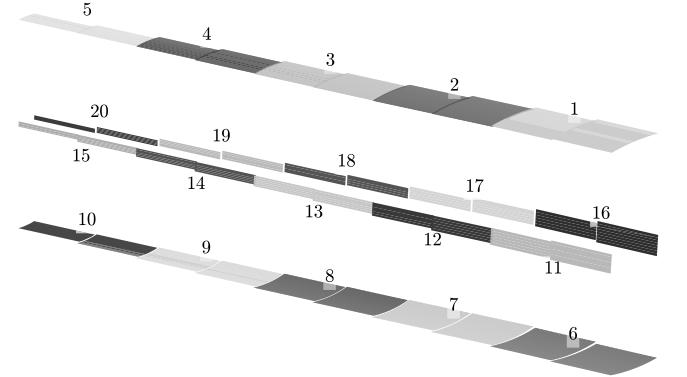
Table 2

Main geometric, mass, and performance parameters of the SE²A MR configuration, as introduced by Karpuk et al. [28].

Parameter	Symbol	Value	Unit
Wing reference area	A_{ref}	155.5	m ²
Wingspan	b	43.4	m
Mean aerodynamic chord	c_{MAC}	4.1	m
Aspect ratio	AR	12.11	–
Taper ratio	λ	0.26	–
Sweep angle (mid-chord)	Λ	12°	–
Maximum take-off mass	MTOM	65 334	kg
Maximum zero-fuel mass	MZFM	54 964	kg
Operating empty mass	OEM	35 313	kg
Design cruise Mach number	M_c	0.71	–
Maximum operating Mach number	M_{MO}	0.76	–
Maximum operating EAS	v_{MO}	176.5	m/s
Design cruise altitude	H_c	7 650	m
Ceiling altitude	H_{max}	11 200	m



(a) Reference configuration of the SE²A MR aircraft showing planform and structural layout, movable surface arrangement, non-structural mass distribution, and laminate regions.



(b) Structural design-region discretisation into the 20 regions used for stiffness and thickness optimisation.

Fig. 6. SE²A MR aircraft: (a) geometry, structural layout and movable surfaces; (b) structural design-region discretisation used for stiffness and thickness optimisation.

3). Control surface scheduling is incorporated through deflection variables $\delta_{i,j}$, which represent the deflection of surface i under LC j and form part of the global design vector, in addition to the laminate design variables. The geometric specifications of the three control surfaces are provided in Table 3.

The critical LCs considered in this study are summarised in Table 4, selected based on a comprehensive load analysis across the flight envelope. Both the Maximum Take-Off Mass (MTOM) and Maximum Zero-Fuel Mass (MZFM) conditions are included to capture the influence of fuel inertia relief effects.

The wing box layout used to define the local buckling panels is shown in Fig. 7. The half wing box contains 30 rib stations, forming 29 rib bays with a uniform rib spacing of 0.742 m. This spacing is used as the spanwise buckling panel length. The stringers define the chordwise panel width and are not assigned a constant absolute pitch along

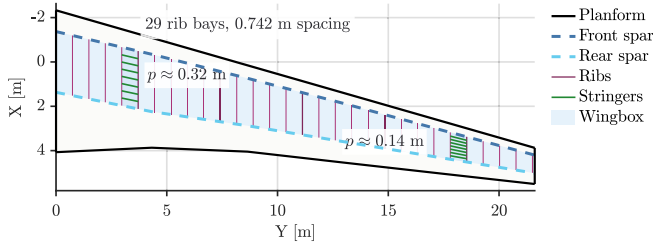


Fig. 7. Schematic of the wing box layout used to define rib bays and representative stringer pitch for the local buckling analysis.

Table 3

Control surface specifications for the SE²A MR configuration.

Control Surface	Span (m)	Chord (%)	Deflection
Inboard (CS 1)	2.16–4.75	80	$\pm 20^\circ$
Midboard (CS 2)	6.70–14.70	80	$\pm 20^\circ$
Outboard (CS 3)	17.65–20.60	80	$\pm 20^\circ$

Table 4

Load cases considered in the present study.

LC	EAS (m/s)	M	Altitude (m)	Mass case	n_z
1	146	0.71	7 650	MTOM	1.0
2	180	0.53	0	MTOM	2.5
3	121	0.35	0	MTOM	−1.0
4	176	0.76	6 010	MZFM	2.5
5	125	0.54	6 010	MTOM	−1.0

Table 5

Mechanical properties of the unidirectional CFRP ply.

Property (Unit)	E_{11} (GPa)	E_{22} (GPa)	G_{12} (GPa)	ν_{12} (–)	ρ (kg/m ³)	t_{ply} (mm)
Value	170	9.9	5.1	0.35	1452	0.15

the span. Instead, their layout scales with the local wing box width, so the stringers converge with the tapered wing box. In the representative sections shown in Fig. 7, seven stringer lines are used and the local pitch decreases from approximately 0.323 m inboard to 0.143 m outboard, corresponding to approximately 12 % of the local wing box width.

These manoeuvre load cases capture the critical flight conditions for the present structural-sizing study. Prior work by Binder et al. [10] indicates that, when gust load alleviation is available and effective, manoeuvre loads typically remain the dominant sizing cases even when active and passive MLA are combined. The present study therefore restricts the optimisation to manoeuvre load cases under that assumption. Gust loads would still need to be assessed across the flight envelope for a comprehensive design evaluation.

The following subsection applies this configuration within the aeroelastic optimisation framework to examine how exploiting control surface reversal can contribute to structural mass reduction through load alleviation.

4.2. Aeroelastic optimisation studies

The aeroelastic optimisation problems were formulated to minimise the structural mass of the SE²A MR wing while satisfying the constraints listed in Table 7.

Although a combined aerostructural metric, such as the Breguet equation Eq. (1), would ultimately provide a more complete measure of aircraft-level efficiency, the use of an inviscid VLM model does not permit accurate prediction of drag. For this reason, structural mass is adopted as the optimisation objective. By enforcing preservation of the cruise-twist distribution, the aerodynamic performance of the base-

line configuration is retained, allowing reductions in structural mass to be interpreted as meaningful indicators of improved efficiency within the fidelity limits of the present model. Incorporating a higher-fidelity drag model to enable full aerostructural optimisation is left for future work.

The cruise shape constraint introduces coupling between the jig shape (unloaded wing geometry) and wing stiffness: fixing both would eliminate the optimiser's freedom to exploit composite tailoring for passive MLA. Therefore, the jig-twist distribution, defined by elemental twist angles ϕ_i at each beam node, is included as a design variable, allowing the optimiser to adjust the unloaded geometry to satisfy the cruise shape while simultaneously tailoring the composite layout to minimise structural mass. The complete set of design variables thus comprises the LPs, ply thicknesses, jig-twist distribution, and control surface deflections for each LC. The optimisation constraints, grouped by engineering domain (structural, aerodynamic, and manufacturing), are summarised in Table 7 and cover material allowables, buckling, aeroelastic stability, local angle of attack limits, outboard control effectiveness, lamination feasibility, and blending constraints. Following the aeroelastic stability formulation used by Werter [12], the stability constraint is imposed by requiring the real parts of the eigenvalues of the coupled aeroelastic state matrix to remain negative for the analysed conditions. This provides a linear dynamic stability constraint within the present optimisation framework, but does not replace a dedicated flutter margin assessment over an extended flight envelope.

With the current implementation of PROTEUS, only steady roll manoeuvres can be computed. According to Holleman [29], a steady-state roll rate between 15°s^{-1} to 20°s^{-1} is required to achieve acceptable and satisfactory pilot ratings. Using the definition in Eq. (9), $p_{roll} = \eta_{eff} \delta V_\infty / s$, and the maximum outboard control-surface deflection of 20° , the threshold $|\eta_{eff}| = 0.15$ corresponds to a steady roll rate of about 17°s^{-1} at the lowest analysed speed, while larger roll rates are obtained at the remaining load cases. The bound $|\eta_{eff}| \geq 0.15$ is therefore adopted here as a practical steady-roll authority requirement for the outboard control surface, which is also the surface most susceptible to reversal. Two optimisation cases were investigated to assess the influence of control surface reversal on structural efficiency:

- **Case A** (conventional control effectiveness), in which outboard control effectiveness is constrained to remain positive ($\eta_{eff} \geq 0.15$), and
- **Case B** (reversal-enabled), in which the constraint is inverted ($\eta_{eff} \leq -0.15$) to allow operation of the outboard control surface within the post-reversal regime while retaining sufficient steady-roll control effectiveness

During the optimisation studies, the GCMMA algorithm proved overly conservative when applied from the initial design due to the influence of the damping factors. Following the discussion in Section 3.4, the standard MMA was therefore employed during the initial iterations, while the design moved away from the initial point, before gradually transitioning to GCMMA. This adaptive strategy required more outer iterations but reduced the cost associated with repeated inner damping updates, yielding comparable overall computational expense. In the GCMMA phase, each candidate design was analysed again and the conservativeness of the local approximation was checked before accepting the design update. If the approximation was non-conservative, the damping parameters were updated and the subproblem was resolved. The main optimiser settings are summarised in Table 6.

The optimisation histories stored the normalised objective, constraint values, gradients, number of violated constraints, and number of inner iterations. The reported designs correspond to feasible, converged points from the respective optimisation histories. The complex-step calculations reported in Section 3.3 were used only for sensitivity verification and were not part of the production optimisation runs.

Table 6
Optimiser settings used in the production optimisation runs.

Setting	Value
Minimum relaxation parameter, $\epsilon_{\min}$	10^{-8}
Maximum number of outer iterations	100
Linear objective coefficient, a_0	1
Constraint linear coefficients, a_j	0
Constraint penalty coefficients, c_j	1000
Constraint regularisation coefficients, d_j	1
Initial damping parameters, ρ_0, ρ_j	0.01
Minimum damping parameters, $\rho_{0,e}, \rho_{j,e}$	10^{-6}

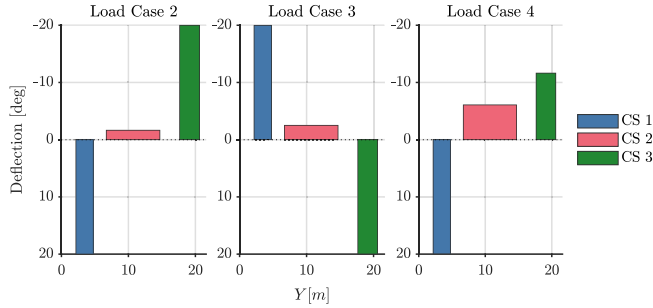


Fig. 8. Optimised control surface scheduling patterns for Case A (conventional control effectiveness). Positive deflection denotes tip-down rotation of the control surface.

4.2.1. Conventional control effectiveness design (Case A)

The optimal control surface scheduling for Case A is shown in Fig. 8, where a positive deflection denotes a downwards rotation of the control surface. For clarity, only LCs 2, 3, and 4 are presented. The control-surface schedules discussed here and in the following subsection are obtained directly from the simultaneous aeroelastic optimisation, with the deflections included as design variables in each load-case equilibrium rather than obtained through a sequential trim procedure. References to lift balance therefore describe the physical role of the optimal allocation, not an additional trimming step. In this conventional case, one would expect the positive-g manoeuvres to use the inboard control surface to add lift, while the outboard control surface unloads the wing tip to reduce the root bending moment; the sign of this pattern should then reverse in the negative-g case. In LCs 2 and 4 (2.5 g pull-up), additional lift is generated by deflecting the inboard control surface downwards to 20° to satisfy the required manoeuvre lift, while the outboard control surface is deflected upwards to -20° for the LC 2 and around -12° for the LC 4 to reduce the local lift near the tip, thereby decreasing the root bending moment. The midboard control surface adopts a small deflection at around -2° for the LC 2 and -6° for LC 4 to compensate the lift redistribution associated with tip unloading, as the inboard control surface is already set to its maximum downward deflection. This mixed-sign schedule reflects the need to unload the tip while preserving the total load required by the load case; a uniform tip-up deflection of all surfaces would reduce lift over the full span and would therefore require compensation elsewhere in the allocation rather than producing the targeted redistribution achieved here. For LC 3 (-1 g push-over), the deflection pattern reverses as expected; the inboard control surface deflects fully upward, the outboard control surface fully downward, and the midboard control surface stays moderately upward at about 3°.

Fig. 9 presents the optimised structural design variables for Case A, showing the distribution of laminate thickness and corresponding normalised in-plane and out-of-plane stiffness. The polar plots of stiffness were generated following the formulation of Dillinger [23].

Both upper and lower skins exhibit similar spanwise trends in thickness and stiffness. The thickness decreases progressively from root to

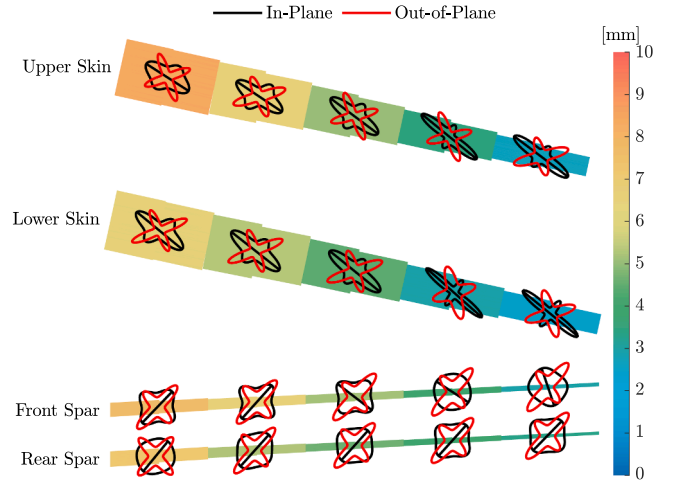


Fig. 9. Thickness and stiffness distribution of the optimised wing skins and spars for Case A.

tip, reflecting bending-moment-driven sizing. The inboard region is considerably thicker to sustain the dominant bending loads, whereas the progressive thinning toward the tip indicates effective MLA that shifts loads inboard. The upper skin is thicker than the lower at the root due to compression under positive 2.5 g manoeuvres, consistent with the later observation that the upper-skin sizing is primarily governed by LC 2. At the same time, the front spar is about 1 mm thicker than the rear spar in the inboard part of the wing, while for the rest of the wing, the spars have the same thickness to keep the shear centre near the mid-chord and prevent loss of control effectiveness due to excessive washout deformation.

The in-plane stiffness distribution is anisotropic, with principal stiffness directions oriented diagonally rearward relative to the wing reference axis. This orientation promotes wash-in deformation (nose-up twist under upward bending), which enhances roll control effectiveness. The out-of-plane stiffness exhibits strong bi-axial tailoring, evidenced by star-shaped bending-stiffness polar plots typical of laminates with significant D_{66} (bending-shear coupling). This coupling increases resistance to twisting curvature (κ_{xy}) and suppresses shear-dominated and diagonal buckling modes, thereby improving local flat-plate buckling performance under combined compressive and shear loading representative of wing skins.

In summary, the conventional configuration combines wash-in tailoring and differential control surface deflections throughout the span to balance passive MLA through composite tailoring with positive roll control authority.

4.2.2. Reversal-enabled design (Case B)

The optimal control-surface scheduling for Case B is shown in Fig. 10. The inboard control surface behaves similarly to Case A across all LCs, providing additional lift as part of the same allocation that balances the total load. The midboard control surface follows the same qualitative pattern in the 2.5 g manoeuvres (LC 2 and LC 4), but with larger upward deflections of approximately 10°. In contrast, for the -1 g manoeuvre (LC 3), the midboard control surface switches to a downward deflection of approximately 7°.

The most significant change occurs for the outboard control surface. In LC 2, it deflects downward by approximately 9°. Before reversal onset, this would increase outboard lift; however, since the control surface operates within the post-reversal regime, the downward deflection instead reduces the local aerodynamic load, demonstrating the intentional use of the reversed-control effect. Interestingly, the optimiser does not select symmetric reversal behaviour for all manoeuvres. This indicates that operating in the post-reversal regime does not by itself determine

Table 7

Constraint sets applied in the structural mass-minimisation problem. Columns “Limit / Case A” and “Limit / Case B” correspond to the conventional and reversal-enabled cases, respectively. Binary entries use 0/1 (inactive/active).

Set	Constraint type	Formulation	Limit / A	Limit / B	Description
1	Laminate thickness	$t_{\min} \leq t_i \leq t_{\max}$	1–50 mm	1–50 mm	Min/max ply thickness per panel.
2	Control surfaces deflections	$\delta_{\min} \leq \delta_i^j \leq \delta_{\max}$	$-20^\circ/20^\circ$	$-20^\circ/20^\circ$	Min/max deflections per control surface and load case.
3	Laminate feasibility	Binary	1	1	Laminate-parameter feasibility region [30].
4	Strain failure	$\epsilon_i^j \leq \epsilon_{\max}^j$	1	1	Strain allowables per element and load case.
5	Aeroelastic stability	$\Re(\lambda_i) < 0$	1	1	Positive damping of coupled aeroelastic modes.
6	Buckling	$r_{cr} \leq 1.0$	1	1	Upper bound on local panel buckling factor.
7	Cruise twist	Binary	1	1	Enforce target cruise twist.
8	Local angle of attack	$\alpha_i^{\min} \leq \alpha_i \leq \alpha_i^{\max}$	1	1	Keep local AoA within linear aerodynamic regime.
9	Blending / continuity	Binary	1	1	Manufacturing continuity between adjacent panels [31].
10	Outboard control effectiveness	η_{eff}	≥ 0.15	≤ -0.15	Conventional minimum (A) vs reversal-enabled bound (B) [23].

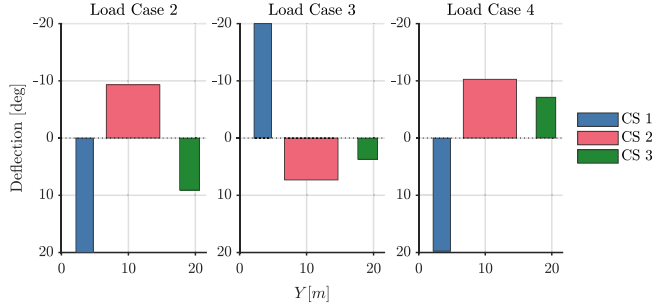


Fig. 10. Optimised control surface scheduling for Case B (reversal-enabled design).

the preferred deflection direction: the optimal sign remains load-case dependent because the structural benefit is governed by the resulting spanwise load redistribution and load-case balance, rather than by reversal alone. In LC 3, only a moderate downward deflection is applied, while in LC 4 a small upward deflection is used. These patterns will be discussed further in Section 4.5.

Fig. 11 shows the optimised structural design variables for Case B. The upper and lower skins display markedly different spanwise patterns. The upper skin thickness decreases sharply between design Sections 2 and 3, reaching the minimum allowed 1 mm, indicating effective load alleviation during high-g manoeuvres. The inboard regions remain thicker to sustain the increased bending loads transferred toward the root. The lower skin thickness decreases more gradually, with moderate variation along the span. The front spar is significantly thicker than the rear, shifting the shear centre forward and promoting washout deformation. The upper-skin laminates exhibit highly anisotropic in-plane stiffness tailored to promote the washout coupling that enables the reversal-based scheduling of the outboard control surface; the associated passive MLA through composite tailoring is therefore aligned with, rather than separate from, the control-reversal mechanism. The lower skin shows milder tailoring, with out-of-plane stiffness again driven by buckling performance.

In summary, Case B achieves structural efficiency through enhanced washout coupling and load redistribution produced jointly by the tailored structure and the reversal-enabled control-surface schedule.

4.2.3. Design comparison

Table 8 compares the total structural masses. The reversal-enabled configuration, Case B, achieves a 13.3 % reduction (180 kg) relative to Case A, showing that intentional use of the post-reversal regime can yield a lighter yet constraint-compliant structure.

To place the structural differences in aerodynamic context, Fig. 12 compares the spanwise lift distributions, $l(y)$, of Cases A and B for the optimised manoeuvre cases. Here, $l(y)$ denotes the total lift per unit span. In LCs 2 and 4, both designs retain the expected bell-shaped positive-g loading, but Case B carries more load inboard and less load over most of

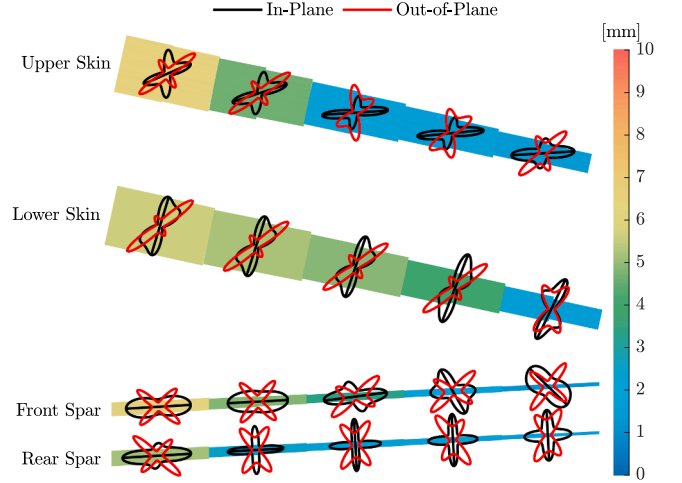


Fig. 11. Thickness and stiffness distribution of the optimised wing skins and spars for Case B.

Table 8

Total structural mass comparison between the conventional (Case A) and reversal-enabled (Case B) designs.

Case	Structural mass [kg]	Δm [kg]	Change [%]
A	1348	–	–
B	1168	180	–13.3

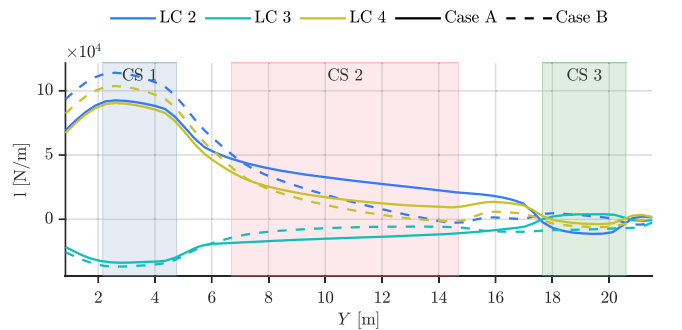


Fig. 12. Comparison of the spanwise lift distributions, $l(y)$, of Cases A and B at the optimised aeroelastic equilibrium for LCs 2–4. Solid lines denote Case A and dashed lines denote Case B. Coloured shaded strips indicate the spanwise extents of the control surfaces.

the mid- and outboard wing. This redistribution reduces the outboard contribution to bending moment and is consistent with the thinner outboard skins and spars discussed below. In LC 3, the difference is smaller and changes sign locally along the span, which is consistent with the more mixed structural changes observed under the negative-g case.

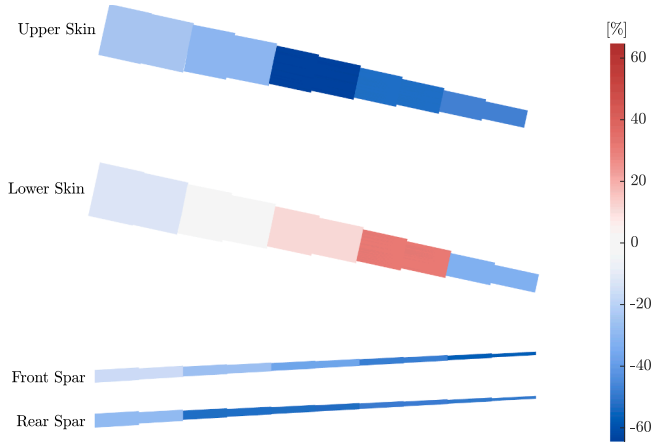


Fig. 13. Percentage difference in laminate thickness between Case B and Case A.

Table 9
Reduced frequency, k_{crit} , of the critical coupled aeroelastic mode for the final designs.

Design	LC 1	LC 2	LC 3	LC 4	LC 5
Case A	1.08	0.88	0.49	0.00	0.47
Case B	0.34	0.19	0.41	0.21	0.40

Fig. 13 presents the percentage variation in laminate thickness between Case A and B. The reversal-enabled design exhibits thickness reductions of up to 60 % in several outboard areas, particularly along the upper skin, consistent with the reduced outer-wing loading in LCs 2 and 4. Both spars experience reductions of 30 %-60 %; however, a noticeable thickening of approximately 30 % in the midboard lower skin is consistent with the smaller and locally mixed redistribution observed in the negative-g case.

The aeroelastic stability constraint was also inspected in terms of the reduced frequency of the critical coupled mode for the final designs. The critical mode is defined here as the eigenmode with the largest real part, and its reduced frequency is evaluated as

$$k_{crit} = \frac{|\Im(\lambda_{crit})| c_{MAC}}{2V_{\infty}}. \quad (33)$$

Here, $k_{crit} = 0$ corresponds to a real, non-oscillatory critical eigenvalue. Werter [12] compared the underlying dynamic aerodynamic formulation with Possio-equation reference solutions for oscillatory subsonic compressible flow, reporting good amplitude agreement up to approximately $k = 1$ and phase errors below about 10° at $M = 0.8$ and $k = 1$. The representative values in Table 9 show that the critical modes of the reversal-enabled design remain in the low-to-moderate range. The lower values obtained for Case B are consistent with its more flexible tailored structure, which lowers the frequencies of the governing coupled modes. These values support the interpretation of the imposed eigenvalue constraint as a design-level local dynamic stability check, but they do not replace a dedicated flutter margin assessment over the flight envelope.

The structural sizing consequences of the two optimisation strategies are examined next through the critical constraint type and governing LCs of each laminate.

4.2.4. Critical constraints and governing failure modes

Fig. 14 presents the most critical constraint and the governing LCs for each structural panel in both optimisation cases.

In Case A, buckling constraints dominate most of the inboard upper and lower skin panels, while shear strain constraints appear only locally near the tip. Relative to Case B, Case A retains a larger share of manoeuvre loading over the mid- and outboard wing, so those panels continue to make a stronger contribution to bending moment and buck-

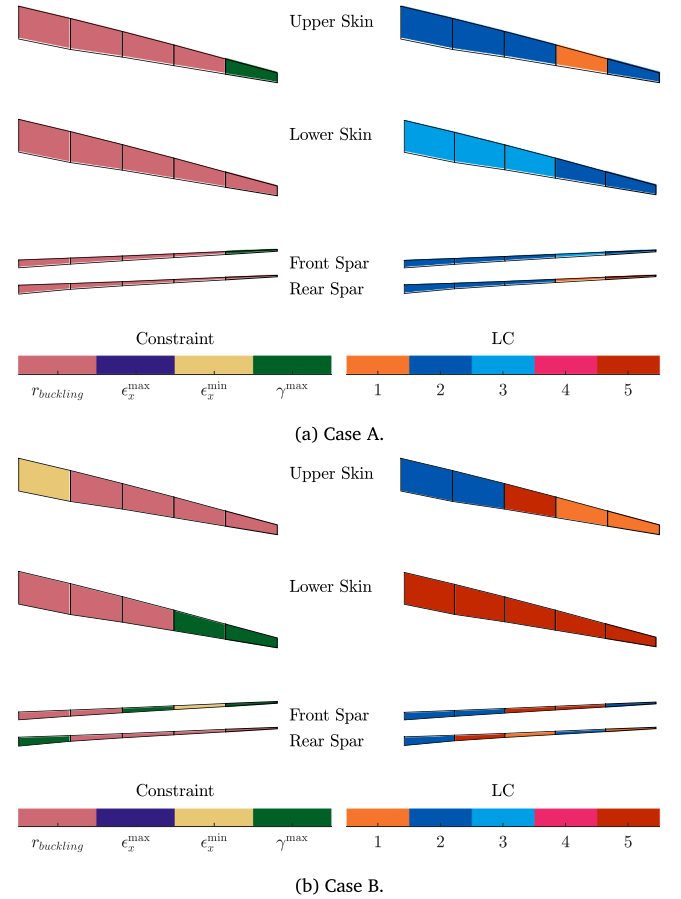


Fig. 14. Constraint criticality per panel, showing the most critical constraint type (left) and governing load case (right) for both Case A and Case B.

ling remains the primary driver of laminate thickness there. In Case B, the pattern changes substantially. Buckling remains dominant over most of the upper skin, the inboard portion of the lower skin, and large parts of both spars. Compressive strain constraints appear only locally, in the innermost upper-skin panel and in one outboard panel of the front spar, while shear strain constraints govern the outboard lower skin and appear locally in both spars. This redistribution indicates that the optimiser successfully shifts aerodynamic loading inboard, relieving the outboard panels and enabling a thinner, more buckling-tolerant design.

The governing LCs shown in Fig. 14a and 14b further highlight this redistribution of structural design drivers across the wing. In Case A, the upper skin is primarily sized by LC 2 (2.5 g pull-up), whereas the lower skin is predominantly governed by LC 3 (−1 g push-over). Only a few panels, one on the upper skin and the outboard rear spar, are sized by the cruise condition (LC 1). Case B exhibits a more distributed pattern of governing drivers. LC 5 (−1 g manoeuvre) dominates large portions of the lower skin and several spar panels, while LC 1 governs the outboard upper skin due to the substantial outboard load reduction produced by reversal-based scheduling. The inboard upper skin remains governed by LC 2, reflecting the shift of aerodynamic loads toward the root.

Overall, these maps show that reversal-enabled optimisation leads to a broader and more balanced distribution of structural design drivers, rather than a design dominated solely by the highest-g manoeuvres. The underlying aeroelastic mechanisms responsible for this redistribution are examined in the following subsection.

4.3. Aeroelastic coupling and roll-control performance

This subsection interprets how the structural differences between Cases A and B translate into aeroelastic behaviour. It first examines the

positions of the elastic and flexural axes to explain the coupling mechanisms underlying the observed roll-control effectiveness, and then assesses the resulting operational implications through a roll-rate allocation analysis, including a representative single-surface loss-of-function case.

4.3.1. Flexural-axis shift and roll-control effectiveness

To interpret the observed aeroelastic behaviour, the concept of the *local flexural axis* is introduced. The local flexural axis defines the chordwise position at which an applied vertical load produces zero twist of the corresponding wing section. Here, it is computed using the flexibility matrix-based method proposed by Stodieck et al. [32]. The flexibility matrix is obtained by applying unit loads separately at the leading and trailing edges of each wing section and computing the resulting vertical and torsional deflections from the structural model. The flexibility relationship is expressed as

$$\begin{bmatrix} r_1 \\ r_2 \end{bmatrix} = \begin{bmatrix} f_{11} & f_{12} \\ f_{21} & f_{22} \end{bmatrix} \begin{bmatrix} R_1 \\ R_2 \end{bmatrix}, \quad (34)$$

where r_1 and r_2 are the vertical and torsional deflections, R_1 and R_2 are the applied unit loads at the leading and trailing edges, and f_{ij} are the flexibility coefficients. The chordwise position of the local flexural axis, x_{FA} , expressed as a fraction of the local chord c , is given by

$$\frac{x_{FA}}{c} = \frac{f_{11} - f_{12}}{f_{11} - f_{12} + f_{22} - f_{21}}, \quad (35)$$

which corresponds to the point where equal vertical deflection occurs at both loading points, i.e., where no twist is induced. Values of $x_{FA}/c < 0.25$ indicate that the zero-twist loading point lies forward of the quarter-chord and therefore favour washout coupling, while larger values indicate weaker washout or wash-in behaviour.

The elastic axis is introduced in parallel as the locus of shear centres along the span, i.e., the chordwise location at which a transverse shear load produces no twist of the section. In the present study, the shear-centre offset of each wing section relative to the beam reference line is extracted from the equivalent Timoshenko sectional stiffness matrices returned by the cross-sectional modeller and then expressed relative to the local chord to obtain the elastic-axis distribution.

Fig. 15 compares the resulting flexural-axis and elastic-axis distributions for Cases A and B. In Case A, the elastic axis remains close to the beam reference line and generally aft of the quarter-chord, consistent with a conventional design. This follows from the relatively balanced front- and rear-spar layout of Case A, which was retained specifically to keep the shear centre near the mid-chord and avoid excessive washout. In Case B, however, the elastic axis exhibits a more complex pattern: near the root, it lies close to the quarter-chord, then shifts abruptly forward, locally extending ahead of the leading edge, before moving aft again near the 13 m span station. This behaviour is driven by the reversal-enabled structural tailoring: the locally thicker front spar and strongly anisotropic skin stiffness move the shear centre markedly forward over the inner-midspan region, while farther outboard the section becomes thinner and more balanced again, causing the elastic axis to return aft.

The flexural-axis distributions are smoother than the elastic-axis distributions and should therefore be interpreted separately. In both designs, the flexural axis lies aft of the quarter-chord over the inboard wing, but in Case B it is generally more forward than in Case A, indicating a stronger tendency towards washout under vertical loading. The key difference, however, lies in the relative placement of the two axes. Around the inner-midspan region of Case B, the elastic axis shifts sharply forward while the flexural axis varies much more gradually, creating a substantially larger separation between them than in Case A. This larger offset between the elastic and flexural axes is consistent with the stronger bend-twist coupling observed in Case B and helps explain why washout coupling, control surface reversal, and passive MLA through composite tailoring are all enhanced there. In Case A, by con-

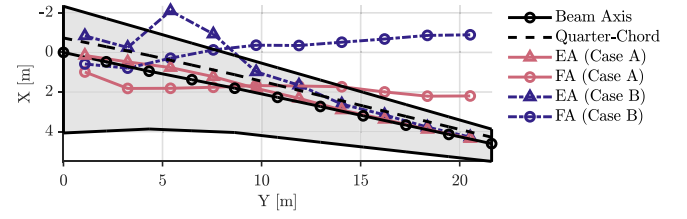


Fig. 15. Elastic (EA) and flexural (FA) axes of the optimised structures for Case A and Case B.

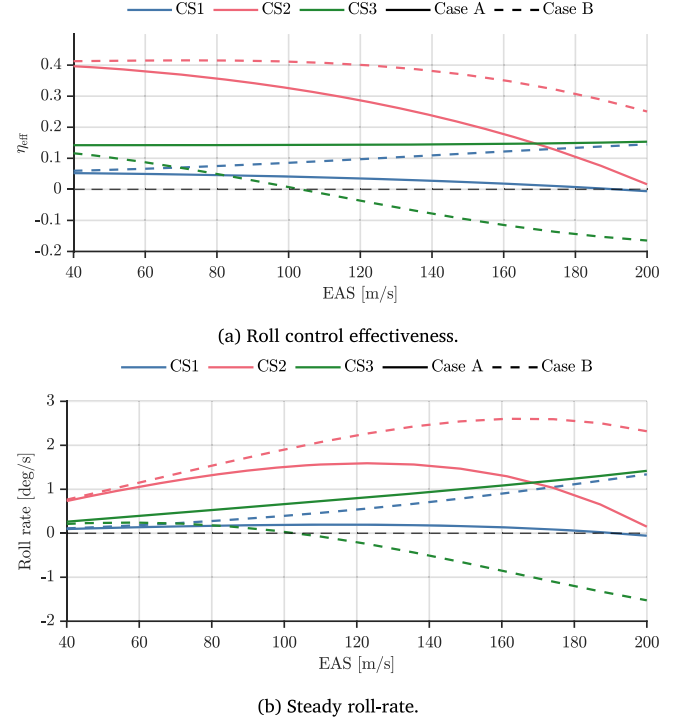


Fig. 16. Roll control effectiveness (left) and steady roll-rate (right) produced by a positive 1° control-surface deflection for both Case A and Case B.

trast, the two axes remain closer and vary more smoothly, so the coupling is weaker and the design retains more conventional roll-control behaviour.

Fig. 16 compares the roll control effectiveness and steady roll-rate response to a positive 1° control-surface deflection for each control surface and optimisation case. In Case A, the effectiveness remains positive for all the control surfaces up to 180 m/s, with the midboard control surface providing the dominant roll authority at low speeds and the outboard control surface dominating from 160 m/s onwards. The outboard control surface exhibits nearly linear roll-rate growth with airspeed, while flexibility reduces the midboard control surface's effectiveness and roll rate. In Case B, the outboard control surface shows negative effectiveness beyond approximately 100 m/s, confirming post-reversal operation, and produces a corresponding negative roll rate. The inboard control surface gains roll authority at high speeds, consistent with the stronger wing-level aeroelastic coupling and inboard lift redistribution discussed later, while the midboard control surface shows higher roll-rate response and delayed loss of control effectiveness. Since η_{eff} combines both the control-surface rolling moment derivative and the effective roll damping derivative, these trends are decomposed below before drawing conclusions on the underlying mechanism.

To further interpret the roll control effectiveness trends, the two terms entering Eq. (9) were evaluated separately at the LC 1 equivalent airspeed, $V_\infty = 146.9 \text{ m s}^{-1}$. The resulting derivatives are reported

Table 10

Decomposition of roll control effectiveness at the LC 1 equivalent airspeed, $V_\infty = 146.9 \text{ m s}^{-1}$, for a positive 1° control-surface deflection.

CS	$C_{L_{\delta,A}}$	$C_{L_{\delta,B}}$	$C_{L_p,A}$	$C_{L_p,B}$	$ C_{L_{\delta,B}} / C_{L_{\delta,A}} $
1	-1.87×10^{-2}	-1.05×10^{-2}	7.61×10^{-1}	9.21×10^{-2}	0.121
2	-1.66×10^{-1}	-3.43×10^{-2}	7.61×10^{-1}	9.21×10^{-2}	0.121
3	-1.10×10^{-1}	8.43×10^{-3}	7.60×10^{-1}	9.21×10^{-2}	0.121

in Table 10. The decomposition shows that the higher roll control effectiveness of CS1 and CS2 in Case B is primarily caused by a reduction in the magnitude of the effective aeroelastic roll damping derivative, rather than by an increase in the control-surface rolling moment derivative. For CS2, for example, $C_{L_{\delta}}$ is smaller in magnitude in Case B than in Case A, while the magnitude of C_{L_p} is reduced to approximately 12% of the Case A value. This reduction can be interpreted from the elastic-axis shift shown in Fig. 15. In the inner-midspan region of Case B, the elastic axis moves forward of the aerodynamic loading region associated with roll damping, which acts close to the quarter-chord. For the roll direction considered, the resulting moment arm promotes elastic twist that reduces the local angle-of-attack increment associated with the rolling motion, thereby weakening the effective roll damping. The outboard control surface behaves differently: in Case B, CS3 has the opposite $C_{L_{\delta}}$ sign to CS1 and CS2, producing the negative roll effectiveness shown in Fig. 16. The similar C_{L_p} values obtained for the three control surfaces indicate that this is mainly an aeroelastic roll damping effect of the complete configuration, while the differences between the individual control surfaces are governed by their respective $C_{L_{\delta}}$ values.

Together, these results demonstrate fundamental differences in how the two designs achieve roll control. The forward shift of the elastic axis and enhanced washout coupling in Case B not only enables operation in the post-reversal regime, but also reduces the effective roll damping and redistributes roll authority between the control surfaces. This distributed approach translates to different operational strategies, which are examined in the following subsection.

4.3.2. Roll-rate allocation and loss-of-function robustness

To assess the practical implications of these control effectiveness characteristics, an optimisation framework was developed to determine the minimal control surface deflections required to achieve a target roll rate across the flight envelope. Given the roll-rate response in $^\circ \text{s}^{-1}$ for each control surface, derived from the positive 1° deflection sweeps, the problem is formulated as a minimum-norm allocation:

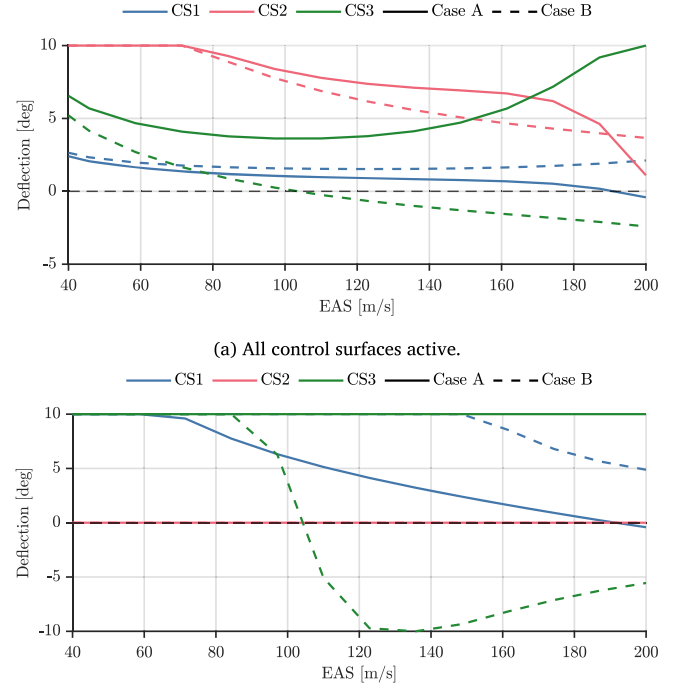
$$\begin{aligned} &\text{minimise} \quad \|\delta\|_2 \\ &\text{subject to} \quad \mathbf{g}(V_\infty) \cdot \delta = \dot{p}_{\text{target}}, \\ &\quad \delta_{\min} \leq \delta_i \leq \delta_{\max}, \end{aligned} \quad (36)$$

where $\mathbf{g}(V_\infty)$ is the speed-dependent effectiveness vector in $^\circ \text{s}^{-1}$ and δ is the deflection vector. The minimum-norm objective minimises overall control-surface deflection while meeting the roll-rate requirement, and is therefore used here as a minimum-deflection control-allocation strategy for achieving the target 15°s^{-1} roll rate [29]. The unconstrained minimum is given by

$$\delta^* = (\mathbf{g}^T \mathbf{g})^{-1} \mathbf{g}^T \dot{p}_{\text{target}}, \quad (37)$$

with subsequent projection onto the feasible deflection range [33]. This approach assumes linear superposition of control surface effects, which is valid for small deflections where aerodynamic and structural nonlinearities remain negligible. The framework identifies the most efficient deflection combination at each airspeed and permits selective activation of control surfaces, enabling analysis of redundancy and loss-of-function cases.

For this study, a target roll rate of 15°s^{-1} is specified with deflection limits of $\pm 10^\circ$, used here as a representative steady-roll requirement within the limits of the present static allocation model. The resulting



(b) Midboard control surface (CS 2) disabled (single-surface loss-of-function case).

Fig. 17. Optimal control surface deflections required to achieve a target roll rate of 15°s^{-1} for both Case A (solid) and Case B (dashed). Positive deflection denotes tip-down rotation; negative values therefore indicate scheduling with the reversed sign relative to the positive deflection convention.

deflection schedules are presented in Fig. 17 for two configurations: all three control surfaces active (top) and the midboard control surface (CS 2) disabled to represent a single control-surface loss-of-function case (bottom). The midboard control surface (CS 2) is chosen because it provides the largest share of the required roll-control input at cruise speeds in both designs; disabling it therefore provides a representative assessment of the design's robustness.

With all control surfaces active (Fig. 17a), both designs successfully meet the target roll rate across the flight envelope. Case A exhibits a conventional strategy: the inboard control surface (CS 1) remains inactive, the midboard control surface (CS 2) dominates at low speeds (approximately 10°), and the outboard control surface (CS 3) progressively takes over as airspeed increases. Case B demonstrates a fundamentally different approach, deliberately exploiting the reversal behaviour. Here, the inboard control surface (CS 1) contributes modest roll authority (approximately 2°), the midboard control surface (CS 2) remains primary in the mid-speed range (approximately 8°), and the outboard control surface (CS 3) operates with reversed deflections (approximately -3°), treating the reversed control effect as an integral part of the normal operating strategy. The comparable deflection magnitudes between both cases show that the reversal-enabled design can satisfy the same prescribed steady-roll requirement without increased control activity.

The single-surface loss-of-function case with the midboard control surface (CS 2) disabled (Fig. 17b) reveals the load redistribution strategies of both designs. Case A demonstrates a clean handoff from the inboard control surface (CS 1) to the outboard control surface (CS 3): the inboard control surface decreases from approximately 10° to zero, while the outboard control surface increases to the maximum of 10° . Case B exhibits more nuanced behaviour: the outboard control surface (CS 3) saturates at its maximum negative deflection (approximately -10°) around 120 m/s, beyond which its increasing effectiveness reduces the required deflection magnitude. The inboard control surface (CS 1) then compensates to maintain the target roll rate, indicating that the reversal-enabled

Table 11

Comparison of individual and combined load alleviation strategies and their relation to the present optimisation cases.

Strategy	Main mechanism	Typical advantage	Main limitation	Relation to present work
Individual strategies				
Passive MLA	Bend-twist coupling shifts loads inboard	No active command; structurally integrated	Can reduce roll authority and limits adaptability during flight	Conceptual baseline
Active MLA	Control-surface deflections redistribute manoeuvre loads	Load reduction adapted to the flight condition	Requires sufficient control effectiveness and may favour higher torsional stiffness	Conceptual baseline
Combined strategies				
Combined MLA, conventional control	Passive tailoring and active scheduling are combined while preserving positive outboard control effectiveness	Combines tailoring with conventional control scheduling	Tailoring and control effectiveness remain in tension, which can restrict mass savings	Case A
Combined MLA, reversal-enabled control	Passive tailoring and active scheduling are combined through post-reversal load redistribution	Relaxes the conventional conflict between tailoring and control, enabling lower mass with retained steady-roll authority	Requires scheduling with flight condition and robustness checks	Case B; proposed strategy

design remains capable of meeting the prescribed steady-roll requirement in this representative loss-of-function case, albeit with greater load redistribution, particularly at low speeds.

These results show that both configurations can meet the prescribed steady-roll requirement within the present static allocation model. The reversal-enabled design achieves this through a distributed control strategy that systematically exploits the negative effectiveness of the outboard control surface (CS 3), rather than avoiding reversal as a limiting constraint. This supports the feasibility of intentional reversal operation for achieving the structural mass reductions demonstrated in Section 4.2.

4.4. On the mechanisms of control surface reversal

During the optimisation of Case B (reversal-enabled design), it was observed that in certain LCs (e.g., LC 4), the optimisation yielded an upward deflection of the outboard control surface even though that surface operated within the control-reversal regime. Under such conditions, an upward deflection of the outboard control surface generates a local lift increase over the outboard wing, which in isolation would increase the root bending moment. To clarify this counterintuitive behaviour, a separate parametric study was conducted to examine the spanwise redistribution of lift with increasing Equivalent Airspeed (EAS).

Fig. 18 illustrates the change in lift carried by each spanwise strip, ΔL , for a 10° deflection of the outboard control surface at EAS values from 40 m/s to 240 m/s, with the angle of attack fixed at $\alpha = 5^\circ$. Here, ΔL denotes the lift change of each spanwise strip relative to the aeroelastic equilibrium at zero deflection of the outboard control surface. The results show that, as EAS increases, the deflection induces progressively stronger redistribution of lift along the span. This behaviour indicates that, in flexible swept wings, control surface reversal is governed by wing-level lift redistribution arising from bending-torsion coupling and the associated twist response, rather than by a purely local change in lift beneath the control surface.

At 40 m/s, the outboard region beneath the deflected outboard control surface shows the expected positive strip-wise lift change, ΔL , while the mid- and inboard regions remain largely unaffected. With increasing airspeed, however, a pronounced spanwise lift redistribution emerges: the outboard section maintains a positive ΔL , but the mid-span region develops an increasingly negative lift response, reaching approximately -5 kN at 240 m/s. This behaviour indicates that the structural response is no longer confined to the outboard section: the control-surface deflection drives a pronounced mid-span bending and nose-down twist response, consistent with washout in swept, anisotropic composite wings.

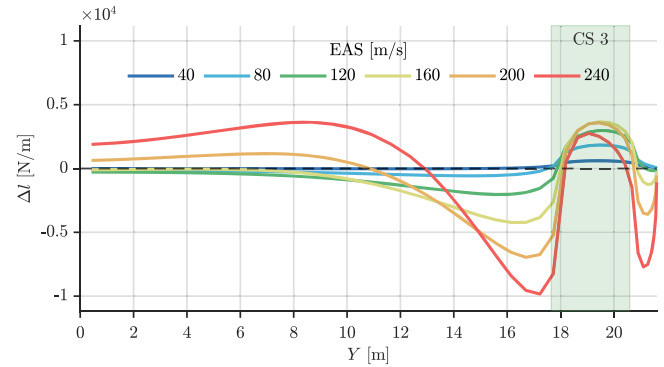


Fig. 18. Spanwise distribution of the strip-wise lift change, ΔL , produced by a 10° deflection of the outboard control surface at $\alpha = 5^\circ$ for several EAS. Curves are relative to the aeroelastic equilibrium with the outboard control surface at zero deflection. The coloured shaded strip indicates the spanwise extent of the deflected outboard control surface.

The origin of control surface reversal in this configuration therefore lies not in the strip-wise lift increment directly beneath the outboard control surface changing sign, but in the development of negative lift over the mid-span region. This departs from the classical two-dimensional interpretation and supports the view that, in flexible swept wings, reversal is governed by wing-level lift redistribution rather than by a purely local sectional effect.

The inboard strip-wise lift response is more complex. At low speeds, ΔL remains close to zero, but as the airspeed increases, the lift initially decreases before recovering near the root, becoming positive and reaching approximately 2.5 kN at 240 m/s. This recovery is consistent with nose-up twist developing in the inboard panels, which increases the local effective angle of attack at high flight speeds.

The flexural-axis and elastic-axis distributions of Case B, shown in Fig. 15, help explain this behaviour. Relative to Case A, Case B exhibits a larger and more rapidly varying separation between these axes over the inner- and mid-span wing. This is consistent with stronger bend-twist coupling in those regions. Consequently, the load change introduced by the outboard control surface is not confined to the tip region, but drives twist and lift redistribution over a broad spanwise extent, consistent with the mid-span unloading and inboard lift recovery observed in Fig. 18.

Fig. 19 decomposes the total strip-wise lift response, ΔL , at 240 m/s into the rigid aerodynamic contribution and the additional load re-

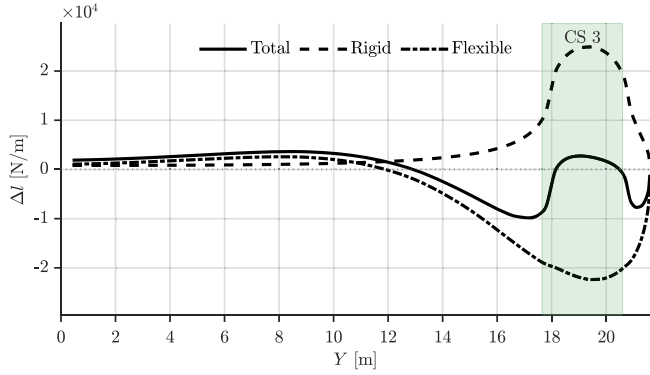


Fig. 19. Decomposition of the strip-wise lift response, ΔL , at 240 m/s into rigid aerodynamic and flexibility-induced components for Case B. The coloured shaded strip indicates the spanwise extent of the deflected outboard control surface.

distribution due to structural deformation. In the rigid part, deflecting the outboard control surface changes the bound-circulation distribution and therefore the induced-velocity field over the whole lifting surface, not just near the control surface itself. The inboard panels must then adjust their circulation accordingly, which explains why the inboard lift responds even in the absence of structural deformation.

The flexible contribution further amplifies this redistribution. At high flight speeds, the flexibility-induced component indicates that structural deformation reduces the effective angle of attack over the mid-span region while partially restoring it inboard. This strengthens the negative mid-span lift response and the inboard lift recovery on top of the rigid aerodynamic redistribution. The combined aerodynamic and structural effects account for the high-EAS behaviour observed in Fig. 18.

In summary, control surface reversal in flexible, swept wings is not a purely local phenomenon. It results from spanwise bending-torsion coupling driven by misalignment between the elastic and flexural axes, producing lift redistribution that extends far inboard of the control surface itself.

4.5. Exploiting control surface reversal for load alleviation

As shown in Section 4.2, the optimiser exploits reversal effects differently depending on the airspeed and LC, using them as a mechanism for load redistribution and structural mass reduction.

Two distinct reversal-based load alleviation mechanisms can be identified:

1. **Reversed deflection:** the control surface deflects opposite to its pre-reversal sign. In the reversal regime, this reduces midboard wing loads while locally increasing outboard loading. The overall effect is often structurally beneficial because the outboard contribution to bending moment is reduced.
2. **Unreversed sign deflection in the reversal regime:** the control surface retains its pre-reversal sign, but because the surface already operates in the reversal regime, the outboard lift is reduced while the midboard lift increases, again contributing to overall load alleviation.

The relative benefit of each mechanism depends on the concurrent deflection patterns of the other control surfaces, particularly when the outboard control surface does not act in isolation.

Fig. 20 demonstrates how both mechanisms are used simultaneously by the optimiser in Case B. The figure presents the strip-wise lift deviation, ΔL , of Case B relative to Case A for LCs 2–4. In LC 4, despite operating within the reversal regime, the outboard control surface is

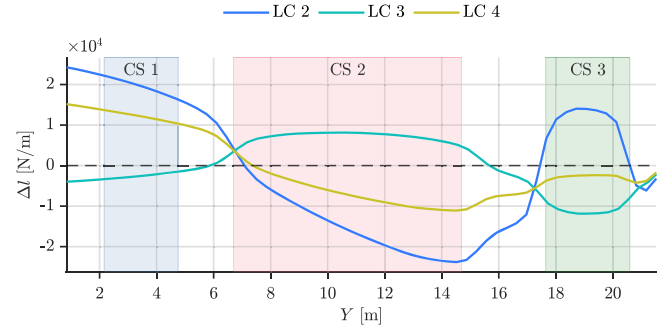


Fig. 20. Difference in strip-wise lift, ΔL , between Case B and Case A for LCs 2–4. Positive values indicate a local load increase in Case B, while negative values indicate unloading relative to Case A. Coloured shaded strips indicate the spanwise extents of the control surfaces. The optimiser uses both reversed and unreversed sign deflections to achieve spanwise load redistribution.

deflected upward, reducing the outboard load. In LC 2, by contrast, the surface deflects downward, increasing the outboard load but producing a larger reduction mid-span, which dominates the overall effect.

These results demonstrate that outboard control-surface deflection alone can trigger strong spanwise aeroelastic coupling. With increasing EAS, the wing transitions from a locally controlled system to a globally interactive one, in which the control surfaces act as distributed load-control devices rather than simple roll effectors.

To correctly interpret this behaviour, it is essential to distinguish between reversal as traditionally defined and reversal as it is displayed in the context of MLA. Neither the classical roll-reversal criterion nor a sign change in the root bending-moment sensitivity uniquely identifies the onset of structurally meaningful reversal. Both metrics are low-dimensional aggregates of the spanwise lift distribution, whereas structural sizing is governed by local panel-level buckling and strain constraints. Consequently, configurations with the same sign of bending-moment sensitivity may still exhibit markedly different active constraints and thickness patterns, and vice versa.

This distinction highlights that roll reversal and reversal for load alleviation are fundamentally different phenomena. The deflection that reverses the rolling moment does not, in general, coincide with the deflection at which the spanwise load redistribution switches from structurally beneficial to structurally detrimental. In flexible, swept composite wings, the load alleviation behaviour is governed by the wing-level redistribution mechanisms described earlier, most notably mid-span lift reduction driven by spanwise bending-torsion coupling, rather than by the classical two-dimensional roll response definition.

Table 11 distinguishes between individual passive and individual active load alleviation, and the two combined strategies examined in the present study. Within this framing, the proposed approach differs from the conventional combined strategy by treating post-reversal behaviour as an admissible design mechanism rather than as a constraint to be avoided.

5. Conclusions

This work introduced an aeroelastic optimisation framework that combines active and passive MLA through the intentional use of control surface reversal. Analytical sensitivities with respect to control surface deflections were implemented and verified using the complex-step method within the PROTEUS in-house tool, enabling accurate and efficient gradient-based optimisation.

Beyond establishing feasibility, the study helps explain the mechanisms by which reversal can be purposefully exploited. Two complementary reversal-based modes are activated: (i) reversed deflections that redistribute bending loads inboard by inducing mid-span lift re-

duction while tolerating local outboard increases; and (ii) unreversed sign deflections executed within the post-reversal regime that suppress outboard lift yet enhance midboard unloading. Their effectiveness depends on tailored washout and wash-in stiffness patterns and deliberate misalignment between the elastic and flexural axes, including forward excursions of the elastic axis that amplify bending-torsion coupling.

Importantly, reversal for roll control and reversal for MLA are not equivalent. Classical roll reversal is identified through a sign change in global roll response metrics, whereas the structurally beneficial reversal observed here is governed by spanwise lift redistribution driven by bending-torsion coupling. Because structural sizing depends on local panel-level strain and buckling constraints, the deflection at which rolling response reverses does not in general coincide with the deflection at which load redistribution becomes structurally beneficial. This distinction shifts the active constraint pattern from predominantly outboard buckling to a more distributed mixture of inboard compressive strain, shear, and selective buckling, and enables a 13.3% structural-mass reduction while maintaining adequate roll authority. The roll efficiency decomposition further indicates that the larger inboard and midboard roll responses of the reversal-enabled design are driven primarily by a strong reduction in effective aeroelastic roll damping, while the outboard surface exhibits a reversed rolling moment derivative at the LC 1 speed. A minimum-norm deflection-allocation framework further supports the feasibility of meeting the prescribed steady-roll requirement across the analysed flight envelope, with the reversal-enabled design meeting the target roll performance through distributed control strategies and remaining effective in the representative simulated loss-of-function case.

These results show that the practical benefit of the reversal-enabled strategy is not obtained by active or passive MLA in isolation, but by allowing both mechanisms to act through the same aeroelastic load allocation process. Relative to the conventional design, the same flexibility that would normally be restricted to preserve positive outboard control effectiveness is instead used to reduce structural mass while satisfying the prescribed steady-roll authority requirement. A direct consequence is that practical reversal-enabled MLA scheduling cannot rely on the nominal rigid wing deflection sign alone, but would require a model of control effectiveness and aeroelastic load redistribution that is scheduled with flight condition. From a practical perspective, control surface reversal evolves progressively with dynamic pressure rather than appearing as an abrupt discontinuity. This behaviour could support incremental flight testing and system identification of the control-effectiveness trends. Since manoeuvre-load prediction and control-effectiveness prediction depend on the same coupled aeroelastic model components, loads-model validation provides relevant supporting evidence for the predicted effectiveness trends, although dedicated robustness assessment and flight-test validation remain necessary before operational implementation.

The computational framework employs a steady VLM with Prandtl-Glauert correction and angle-of-attack constraints, enabling efficient gradient-based optimisation with exact analytical sensitivities while capturing the essential load-redistribution physics. Within this modelling scope, the approach is appropriate for establishing the fundamental viability of reversal-based load alleviation, though extension to higher-fidelity aerodynamics and additional gust load cases will be necessary for more comprehensive validation. Building on the present results, future work could incorporate drag prediction and aerostructural optimisation, optimise control surface geometry and spanwise positioning, extend analyses to include flutter margins and gust loads, and integrate ply-recovery methods for manufacturing specifications. These results indicate that, within the present modelling scope, control surface reversal can be purposefully exploited for MLA and structural-mass reduction in high-aspect-ratio wings.

CRediT authorship contribution statement

Evangelos Filippou: Writing – original draft, Visualization, Validation, Software, Methodology, Investigation, Formal analysis, Conceptualization; **Jurij Sodja:** Writing – review & editing, Supervision, Methodology, Funding acquisition; **Roeland De Breuker:** Writing – review & editing, Supervision, Project administration, Methodology, Funding acquisition, Conceptualization.

Declaration of competing interest

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

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AI-based language tools were employed to improve readability and clarity. All generated content was critically reviewed and verified by the authors.

Data availability

Data will be made available on request.

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