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Aeroelastic Analysis of a Large Airborne Wind Turbine

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This paper presents an extension of the simulation code ASWING to aeroelastic analysis of an airborne wind turbine. The device considered in this study consists of a tethered rigid wing with onboard-mounted wind turbines designed for wind energy harvesting in crosswind flight operation. The electrically conducting tether is deployed from a ground station and represented as a linear elastic spring with stiffness, mass, and frontal area emulating the properties of the real tether. The tether splits into several bridle lines to distribute the load transfer from the wing and to some degree also constrain its roll motion. The comparatively short bridle lines are considered to be inelastic with insignificant mass and aerodynamic drag contributions. The simulation model is validated by wind tunnel tests of a simplified scale model of the bridled wing. The comparison of computed and measured dynamic aeroelastic response shows that the tether force and the geometry of the bridle line system can strongly influence the flutter speed of the wing. In a final step, the simulation model is used to analyze the divergence, control reversal and effectiveness, and flutter behavior of a next-generation large-scale airborne wind turbine. The results confirm the significant influence of the geometry of the bridle line system on static and dynamic aeroelastic phenomena. It is concluded that classical methods used for suppression of aeroelastic instabilities can be applied to bridled wings only if this influence is taken into account.

I. Introduction

IN MOST parts of the world, wind at higher altitude is stronger and generally more persistent [1–4]. Airborne wind energy (AWE) systems aim to harvest this energy potential, which is inaccessible to conventional, ground-based wind turbines. The characteristic features of AWE systems are the flying vehicle that substitutes the rotor blades of a wind turbine and the tether that substitutes its tower. This comparison is schematically illustrated in Fig. 1 for the example of the airborne wind turbine (AWT) concept considered in this study [5]. The depicted flying vehicle has similarities with an aircraft: it consists of a main wing with aerodynamic surfaces for lateral control, a fuselage, and an elevator and rudder for longitudinal and vertical control. The wing is connected to the tether by two bridle lines. Small wind turbines are mounted on pylons that extend above and below the main wing. To harvest wind energy, the vehicle is flown on a circular crosswind trajectory, which exposes these onboard turbines to a high relative flow velocity. A conducting tether transmits the generated electricity to the ground. The specific concept was first proposed by Loyd [6] and analyzed in more detail in [7,8], for example.

The higher capacity factor, in combination with a significantly lower material effort, bears the potential of a substantially reduced energy cost. Triggered by this potential, various concepts are currently being explored, most of them using flexible membrane or rigid wings for aerodynamic lift generation [9,10]. Compared with contemporary wind turbines, these technology demonstrators are still relatively small, generate up to tens of kilowatts, and the flying vehicles with a wing span of up to several meters can be handled by a single person or a small ground crew [11]. Aiming at utility-scale

electricity generation in park configurations, several companies are advancing toward rigid-wing AWE systems of about 4–5 times the wing span of the technology demonstrators [12]. Makani Power has developed a technology demonstrator of 7 m wing span and 20 kW nominal electrical power divided over four onboard generators. This platform has been used to demonstrate autonomous launching, landing, and 10 h endurance flight [5]. After its acquisition by Google in 2013, the company was integrated into Alphabet's moonshot factory X and has since built a prototype of 28 m wing span and 600 kW electrical power, which was operated successfully in crosswind flight in December 2016 [13,14].

To maximize the energy output of such a system, the aerodynamic design needs to be optimized while the airborne structure needs to be lightweight. The continuous average wing loading during nominal operation is much higher than for conventional aircraft, because the tensile force transferred by the tether is more than an order of magnitude larger than the gravitational force acting on the aircraft and because the required narrow crosswind loop maneuvers induce large transverse acceleration forces. Another substantial difference to conventional aircraft is the use of bridle lines to reduce the bending load and thus the mass of the structure. However, the additional lines also increase the aerodynamic drag of the system, which negatively affects its performance. Depending on its layout, the bridle line system affects the flight dynamic and steering behavior of the aircraft [15]. For these reasons, the bridle line system is an important component of the system design.

For small-scale rigid-wing systems, aeroelastic phenomena are generally not a key consideration. However, large-scale lightweight wings with high aspect ratio will deform considerably under aerodynamic load and aeroelastic phenomena have to be taken into consideration. Next to static deformation of the structure, it is particularly also the dynamic response and aeroelastic coupling effects that can cause dangerous resonance phenomena [16]. For example, wing flutter caused the crash of the blended wing Lockheed F-117 Night Hawk in 1997 [17] and tail flutter caused the North American P-51D Mustang to crash into the spectators during the Reno air race in 2011 [18]. These accidents clearly show that aeroelasticity of flying structures requires careful attention in the design process.

Because bridled parafoils have been used extensively for airborne payload delivery, the flight dynamics and deformation of this specific system configuration have been studied extensively in the past

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Fig. 1 Conventional wind turbine (left) and tethered rigid-wing AWT in crosswind flight with onboard-mounted wind turbines (right).

[19,20]. Aerodynamic properties of large bridled ram air wings for ship traction have been computed by coupled finite element and fluid dynamic analysis [21]. Dynamic aeroelastic models of leading edge inflatable kites for AWE applications have been developed on the basis of multibody [22] and finite element frameworks [23]. In both approaches, the inflatable tubular frame and the attached canopy membrane was modeled. A simulation tool for bridled rigid wings does not exist to our knowledge.

The objective of this study is to develop a validated aeroelastic model of a bridled rigid-wing AWT for use in the preliminary design phase and to use this model for the aeroelastic analysis of a next-generation AWT. In Sec. II we outline the basic aeroelastic phenomena governing bridled wings in crosswind operation. In Sec. III we develop the simulation model by extending the widely used simulation code ASWING with a tether and bridle line system. In Sec. IV this model is validated with wind tunnel data for a small-scale setup and in Sec. V it is applied to a next-generation AWT.

II. Aeroelastic Bending and Torsion of Bridled Wings

Aeroelasticity of aircraft wings is generally investigated as an interaction phenomenon involving inertial, elastic, and aerodynamic forces ([24,25], [26] Chap. 1). Specific to the application of airborne

wind energy generation is the use of a tensile support system to transfer the generated aerodynamic force to the ground. In this study, we consider an AWT in nominal crosswind operation with a typical lift-to-weight ratio of 15 flying at an altitude of a few hundred meters from the ground station. For these conditions, it can be assumed that the tensile support system is fully tensioned and that tether and bridle lines are straight. Because the tensile forces are of the same order of magnitude as the aerodynamic forces, the bridling of the wing is significantly affecting the static and dynamic aeroelastic behavior of the flying vehicle. In this section, we identify and describe the effect of the bridling qualitatively.

The out-of-plane bending of the main wing depends mainly on the spanwise distribution of the aerodynamic load, the spanwise attachment location of the bridle line, and the variation of the bending stiffness along the wing span. The superimposed torsion depends on the locations of the aerodynamic center and the bridle line attachments relative to the elastic axis, because these two chordwise distances determine the contributions of the resultant aerodynamic force and the bridle line forces to the twisting moment. The mechanism is illustrated schematically in Fig. 2 for the example of a bridle attachment roughly halfway between root and tip, relatively close to the leading edge, at $x_b < 0$. The elastic axis of the wing is downstream of the quarter-chord line defining the aerodynamic center of the airfoil, at $c_{ea} > 0$. This specific layout will also be used in Sec. V as baseline configuration for the wing of a next-generation AWT. The illustration includes the apparent wind velocity $\mathbf{v}_a$, the body-fixed reference frame (xyz) of the aircraft, with its origin located at quarter-chord midspan of the undeformed straight wing, and, for the wing section to which the bridle line attaches, the local reference frame (csn) with its origin located at quarter chord. For static aeroelastic deformations, the resultant aerodynamic force $\mathbf{F}_a$ acting on a wing section and the bridle line force $\mathbf{F}_b$ have to be in equilibrium with the stress resultant force $\mathbf{F}_e$ acting in the wing cross section. Similarly, the moments of the external forces $\mathbf{F}_a$ and $\mathbf{F}_b$ about the elastic axis and the aerodynamic moment $\mathbf{M}_a$ have to be in equilibrium with the stress resultant moment $\mathbf{M}_e$ acting in the wing cross section.

The characteristic static bending mode is caused by the aerodynamic load pulling the root and the tip of the wing away from

Fig. 2 Aeroelastic bending and torsion of a half wing supported by a bridle line. The inset shows forces F_e , F_b and moment M_e acting in the wing cross section to which the bridle line attaches, as well as force F_a and moment M_a acting on the surface of the corresponding free end of the wing.

the ground anchor, while the bridle line locally constrains this deformation. Because the aerodynamic center is located upstream of the elastic axis, the aerodynamic load generates a positive (nose up) twisting moment. Without bridle, the wing would twist along the span with a continuously increasing positive angle θ . With attached bridle, a negative (nose down) twisting moment is introduced locally, at the spanwise location y_b . Because the total aerodynamic force acting on the wing and the tether force are about equal in magnitude but opposite in direction and because the bridle attachment is upstream of the aerodynamic center, the joint moment contribution of these external forces is now negative. Starting from the root, the twist angle θ is thus first decreasing along the span and then increasing again toward the tip. Moving the bridle attachment downstream while keeping all forces constant increases the joint moment contribution of the external forces, until it gets positive for $x_b > 0$. Accordingly, also the wing twist increases.

For conventional untethered aircraft the wing twist angle strongly depends on the position of the elastic axis relative to the aerodynamic center [16,27]. This is, however, not the case for a tethered airborne wind energy system in nominal crosswind operation. A shift of the elastic axis, either upstream or downstream, does not influence the twist angle significantly. This can be explained by the equal increase or decrease of the torsion moment arms c_{ea} and $c_{ea} - x_b$ of the aerodynamic force and bridle line force, respectively. Because these dominant forces are about equal in magnitude but opposite in direction, the effect of the elastic axis position c_{ea} on their resulting moment cancels out.

The interaction between aerodynamic load distribution and bridle line forces also affects the critical aeroelastic phenomena that can lead to structural failure or impaired controllability of the flying vehicle. The relevant static phenomena considered in this study are torsional divergence and aileron control effectiveness and control reversal. Divergence can occur when the deflection of the aerodynamically loaded wing increases the load or moves the load distribution such that the twisting effect continuously increases, which eventually leads to the failure of the structure. Aileron control reversal, on the other hand, denotes the loss and reversal of the expected flight dynamic response as a result of wing deformation [16,27].

The relevant dynamic aeroelastic phenomenon is flutter that is a self-excited oscillatory instability in which the aerodynamic forces couple with the natural vibration modes of the wing. The generated oscillatory deformations can increase in amplitude and lead to structural failure ([25] Chap. 5). The flow velocity at which the first signs of dynamic instabilities occur is denoted as flutter speed v_f . We can distinguish two different types of flutter. For hard flutter, which occurs very close to the flutter speed, the sum of the natural positive damping of the structure and the negative damping of the aerodynamic forces decreases very suddenly. For soft flutter the net damping decreases gradually [24].

III. Simulation Model

The analysis tool ASWING has been developed for coupled aerodynamic, structural, and flight dynamic simulation of deformable aircraft [28]. The integrated physical model is based on a nonlinear Bernoulli-Euler beam representation of fuselage and surface structures, allowing for arbitrary large deformations. Aerodynamic loads are determined by a lifting line model based on wing-aligned trailing vorticity, Prandtl-Glauert compressibility transformations, and local-stall lift coefficients. The sparse eigenvalue package ARPACK is used to determine flight and structural instabilities. This simplified representation is suitable for quick explorations of the design space with a reasonable accuracy and is therefore most useful in the preliminary design of a conventional aircraft [28–30]. The functionality of ASWING does not include a tethering of the aircraft to the ground. Extending the simulation code by a tensile support system is a key contribution of the present study. The bridle line forces are included in the program flow of ASWING similar to the strut, engine, inertial, and aerodynamic loads, as indicated in Fig. 3.

In the following, we first outline the general approach for including tensile structural elements that connect to the ground in the

Fig. 3 Flowchart of ASWING for the computation of steady equilibrium flight including the additional forces from the tensile support system.

simulation model and then detail the additional model components. For the purpose of brevity we omit modeling and numerical solution aspects that are described in detail in the ASWING manuals [29,30].

A. Geometry and Kinematics of the Tensile Support System

The physical model of the AWT, including tether and bridle line system, is illustrated schematically in Fig. 4. Following the ASWING methodology, the main wing is represented as a beam that bends and twists as a result of the aerodynamic loading, the inertial forces, and the constraints imposed by the tensile support system. For the numerical description of the aeroelastic problem the beam framework is discretized by beam nodes P_i . The aerodynamic load is transferred from the deformed main wing beam to the two bridle lines at points P_{b1} and P_{b2} , respectively. The bridle attachment points are generally not located on the beam axis but are offset from two specific beam nodes, P_{i1} and P_{i2} , respectively. At point P_{tb} , the bridle lines join and connect to the tether that is anchored at point P_{tg} to the ground. Subscripts b , t , and g refer to bridle, tether, and ground, respectively.

A hierarchy of three different reference frames is used to describe the motion and deformation of the AWT [30]. Positions in the inertial reference frame (XYZ) are denoted by vectors $\mathbf{R}_i$, and positions in the body-fixed reference frame (xyz) of the aircraft by vectors $\mathbf{r}$. In general, bold upper case letters are used to denote kinematic vectors resolved in the inertial reference frame, and bold lower case letters for kinematic vectors in the body-fixed reference frame. The body-fixed reference frame is attached to the aircraft at point P_k , with position vector $\mathbf{R}_k$, while the orientation with respect to the inertial frame is defined by the Euler angles Φ , Θ , and Ψ , which are combined in the angle triplet $\mathbf{\Theta} = [\Phi, \Theta, \Psi]^T$. The subscript k refers to kite, to stress the fact that the aircraft is tethered to the ground. As mentioned above, the structure of the aircraft is represented by beams that are connected at joints. For each beam node we define a local reference frame (csn) _{i} with its c axis pointing along the chord and its s axis aligned with the local beam axis. The positions of these reference frames relative to the body-fixed reference frame of the aircraft are described by vectors $\mathbf{r}_i$, and their relative orientation by the Euler angles ϕ_i , θ_i , and ψ_i , which are combined in the angle triplet $\mathbf{\theta}_i = [\phi_i, \theta_i, \psi_i]^T$ [30].

Fig. 4 Aeroelastic model of the AWT and the tensile support system connecting it to the ground.

Starting from the position vectors $\mathbf{r}_{i1}$ and $\mathbf{r}_{i2}$ of the beam nodes P_{i1} and P_{i2} , we add the offset vectors $\Delta\mathbf{r}_{b1}$ and $\Delta\mathbf{r}_{b2}$ to determine the position vectors of the bridle attachment points as

$$\mathbf{r}_{b1} = \mathbf{r}_{i1} + \Delta\mathbf{r}_{b1} \quad \text{and} \quad \mathbf{r}_{b2} = \mathbf{r}_{i2} + \Delta\mathbf{r}_{b2} \quad (1)$$

We assume that the local deformation of the wing structure between beam axis and bridle line attachment is negligible. Accordingly, the offsets of the bridle attachment points are constant when expressed in the local reference frames, which leads to

$$[c_{b1} \quad s_{b1} \quad n_{b1}]^T = \mathbf{T}_{i1,0} \Delta\mathbf{r}_{b1,0} \quad (2)$$

$$[c_{b2} \quad s_{b2} \quad n_{b2}]^T = \mathbf{T}_{i2,0} \Delta\mathbf{r}_{b2,0} \quad (3)$$

where $\mathbf{T}_{i1}$ and $\mathbf{T}_{i2}$ are the matrices for transforming vector components from the body-fixed reference frame of the aircraft to the local reference frames. The two matrices are functions of the Euler angle triplets θ_{i1} and θ_{i2} . The subscript 0 in Eqs. (2) and (3) denotes the undeformed state, which is known a priori. The instantaneous offset vectors in the body-fixed reference frame can thus be evaluated as

$$\Delta\mathbf{r}_{b1} = \mathbf{T}_{i1}^T [c_{b1} \quad s_{b1} \quad n_{b1}]^T = \mathbf{T}_{i1}^T \mathbf{T}_{i1,0} \Delta\mathbf{r}_{b1,0} \quad (4)$$

$$\Delta\mathbf{r}_{b2} = \mathbf{T}_{i2}^T [c_{b2} \quad s_{b2} \quad n_{b2}]^T = \mathbf{T}_{i2}^T \mathbf{T}_{i2,0} \Delta\mathbf{r}_{b2,0} \quad (5)$$

where $\mathbf{T}_{i1}^T$ and $\mathbf{T}_{i2}^T$ are the matrices for the reverse transformations, that is, from the local reference frames to the body-fixed reference frame of the aircraft. The only variable contributions on the right-hand sides of Eqs. (4) and (5) are the transformation matrices $\mathbf{T}_{i1}$ and $\mathbf{T}_{i2}$. The bridle line vectors can be formulated as

$$\mathbf{b}_1 = \mathbf{r}_{tb} - \mathbf{r}_{b1} \quad \text{and} \quad \mathbf{b}_2 = \mathbf{r}_{tb} - \mathbf{r}_{b2} \quad (6)$$

where $\mathbf{r}_{tb}$ is the position vector of the tether-bridle connection point. Accordingly, the tether vector can be formulated as

$$\mathbf{t} = \mathbf{r}_{tg} - \mathbf{r}_{tb} = \mathbf{T}_E^T (\mathbf{R}_{tg} - \mathbf{R}_k) - \mathbf{r}_{tb} \quad (7)$$

where $\mathbf{T}_E$ is the matrix for transforming vector components from the body-fixed to the inertial reference frame, and $\mathbf{T}_E^T$ accordingly for the reverse transformation. The matrix is a function of the Euler angles Φ ,

Θ , and Ψ . The vector $\mathbf{r}_{tg}$ describes the translation of the entire aircraft relative to the fixed point P_{tg} on the ground.

B. Approximate Solution of Tether-Bridle Connection Point

Similar to the position vectors $\mathbf{r}_i$ of the beam nodes, the vector $\mathbf{r}_{tb}$ is an unknown of the aeroelastic problem, depending on the tensile forces in the lines and the aerodynamic drag and weight forces acting on the lines. However, an approximate geometric solution for the vector $\mathbf{r}_{tb}$ can be formulated on the basis of two simplifying assumptions. First, because the bridle lines are very short compared with the tether, they can be represented as inelastic line segments with constant lengths l_{b1} and l_{b2} . As consequence, the point P_{tb} is constrained geometrically to a circle around the axis connecting the bridle attachment points P_{b1} and P_{b2} . Second, our analysis showed that for typical rigid-wing AWT operating conditions, the aerodynamic drag and weight force of the tether account for about 2–3% of the tether force. For this reason, we assume that tether drag and weight do not affect the position of the tether-bridle connection point significantly and that the vectors $\mathbf{t}$, $\mathbf{b}_1$, and $\mathbf{b}_2$ are approximately coplanar during nominal crosswind operation. Based on these assumptions, the position vector $\mathbf{r}_{tb}$ can be determined geometrically.

The tensile support system is illustrated schematically in Fig. 5. Starting from the known positions of points P_{b1} , P_{b2} , and P_{tg} , we define the vectors

$$\mathbf{d} = \mathbf{r}_{b2} - \mathbf{r}_{b1} \quad (8)$$

$$\mathbf{f} = \mathbf{r}_{tg} - \mathbf{r}_{b1} = \mathbf{T}_E^T (\mathbf{R}_{tg} - \mathbf{R}_k) - \mathbf{r}_{b1} \quad (9)$$

$$\mathbf{a} = \mathbf{f} - (\mathbf{f} \cdot \mathbf{d}) \frac{\mathbf{d}}{d^2} \quad (10)$$

to define a bridle reference frame with orthogonal base vectors

$$\mathbf{e}_{b,x} = \frac{\mathbf{d}}{d}, \quad \text{and} \quad \mathbf{e}_{b,y} = \frac{\mathbf{a}}{a} \quad (11)$$

We then consider the triangle defined by the side lengths l_{b1} , l_{b2} , and $d = d_1 + d_2$ to determine the height h and the partial side length d_1 as

$$d_1 = \frac{d}{2} \left(1 + \frac{l_{b1}^2 - l_{b2}^2}{d^2} \right) \quad (13)$$

$$\mathbf{r}_{\text{tb}} = \mathbf{r}_{b1} + d_1 \mathbf{e}_{b,x} + h \mathbf{e}_{b,y} \quad (14)$$
$$\mathbf{F}_t + \mathbf{F}_{b1} + \mathbf{F}_{b2} = 0 \quad (15)$$
$$\mathbf{F}_{b_1} = F_{b_1} \frac{\mathbf{b}_1}{l_{b_1}} \quad \text{and} \quad \mathbf{F}_{b_2} = F_{b_2} \frac{\mathbf{b}_2}{l_{b_2}} \quad (16)$$
$$\mathbf{F}_t = \mathbf{F}_{e,t} + \mathbf{D}_t + \mathbf{g}m_t \quad (17)$$

To calculate the forces $\mathbf{F}_{b1}$, $\mathbf{F}_{b2}$, and $\mathbf{F}_t$ we start from the geometry of the tensile support system, described by vectors $\mathbf{b}_1$, $\mathbf{b}_2$, and $\mathbf{t}$, as defined by Eqs. (6) and (7), respectively. In a first step, we evaluate the tether force $\mathbf{F}_t$ from Eqs. (17–23). While the dominant elastic force contribution in Eq. (17) is by definition aligned with the tether, the aerodynamic drag and weight force contributions are not. Because the approximate solution of the tether-bridle connection point $\mathbf{r}_{tb}$ is based on the assumption of a planar force equilibrium, we remove the relatively small out-of-plane component $\mathbf{F}_{t,n}$ of the tether force, pointing in direction $\mathbf{e}_{b,x} \times \mathbf{e}_{b,y}$. With the remaining in-plane component we use two dimensions of Eq. (15) together with Eq. (16) to solve for F_{b1} and F_{b2} . For example, using the x and y dimensions of Eq. (15) we can work out the following solutions:

$$F_{b1} = \frac{[\mathbf{b}_2 \times \mathbf{F}_t]_z}{[\mathbf{b}_1 \times \mathbf{b}_2]_z} = l_{b1} \frac{b_{2,x}F_{t,y} - b_{2,y}F_{t,x}}{b_{1,x}b_{2,y} - b_{1,y}b_{2,x}} \quad (25)$$

$$F_{b2} = \frac{[\mathbf{b}_1 \times \mathbf{F}_t]_z}{[\mathbf{b}_1 \times \mathbf{b}_2]_z} = l_{b2} \frac{b_{1,y}F_{t,x} - b_{1,x}F_{t,y}}{b_{1,x}b_{2,y} - b_{1,y}b_{2,x}} \quad (26)$$

In case that the denominator in these expressions—the z component of the cross product of both bridle line vectors—approaches zero, a different pair of dimensions is used to calculate the bridle line forces. In a last step, we distribute the out-of-plane component $\mathbf{F}_{t,n}$ to the two bridle line forces $\mathbf{F}_{b1}$ and $\mathbf{F}_{b2}$, such that the resulting moment of these additions around the tether axis vanishes. This separate treatment of in-plane and out-of-plane force components is necessary because the force equilibrium described by Eq. (17) with coplanar bridle and tether vectors does not allow transferring the out-of-plane force contributions of the tether drag and weight force.

D. Aeroelastic Stability Analysis

In ASWING the variables of the aeroelastic free-flying aircraft are arranged in a state vector $\mathbf{x}$ and a corresponding time rate of change vector $\dot{\mathbf{x}}$ [30]. To include the ground-attached tensile support system, these vectors are expanded by the position vector $\mathbf{R}_k$ and orientation Θ of the body-fixed reference frame relative to the inertial reference frame, resulting in

$$\mathbf{x} = [\mathbf{R}_k \quad \Theta \quad \mathbf{r}_i \quad \theta_i \quad \mathbf{F}_i \quad \mathbf{M}_i \quad \dots] \quad (27)$$

$$\dot{\mathbf{x}} = [\dot{\mathbf{R}}_k \quad \dot{\Theta} \quad \dot{\mathbf{r}}_i \quad \dot{\theta}_i \quad \dots] \quad (28)$$

where the $\mathbf{F}_i$ and $\mathbf{M}_i$ are the internal structural forces and moments at the beam nodes $i = 1, \dots, N$. Commanded variables are arranged in a vector $\mathbf{u}$. Defining $\mathbf{s}$ as the residual vector, all beam, global, and control equations can be written in residual form

$$\mathbf{s}(\mathbf{x}, \dot{\mathbf{x}}, \mathbf{u}) = 0 \quad (29)$$

The residual equations can be used for a time-marching calculation of the aeroelastic behavior of the tethered aircraft, as described in [30].

The flight and structural instabilities are identified by means of an eigenmode analysis in the frequency domain. Eigenvalues λ_k and eigenvectors $\mathbf{v}_k$ are nontrivial solutions of the unforced perturbed system

$$\mathbf{A}\mathbf{v}_k = \mathbf{M}\mathbf{v}_k\lambda_k \quad (30)$$

where $k = 1, 2, \dots$ is the solution mode index, and $\mathbf{A}$ and $\mathbf{M}$ are the stiffness and mass matrices, respectively [30]. The two Jacobian matrices are defined as partial derivatives of the residual vector with respect to the state vector and its time rate of change vector

$$\mathbf{A} = \left[\frac{\partial \mathbf{s}}{\partial \mathbf{x}} \right], \quad \mathbf{M} = - \left[\frac{\partial \mathbf{s}}{\partial \dot{\mathbf{x}}} \right] \quad (31)$$

The commanded variables $\mathbf{u}$ are constant in the eigenmode analysis and are hence not included in Eq. (30). Because the aerodynamic load is transferred from the aircraft to the tether via the bridle line system, Eq. (29) additionally includes the bridle line forces specified by Eq. (16). As a consequence, the stiffness matrix $\mathbf{A}$ contains the partial derivatives of the bridle line forces. In the following, we outline the analytical calculation of the partial derivative of the bridle line force $\mathbf{F}_{b1}$.

Applying the product rule to Eq. (16) we get

$$\frac{\partial \mathbf{F}_{b1}}{\partial \mathbf{x}} = \frac{\partial \mathbf{F}_{b1}}{\partial \mathbf{x}} \frac{\mathbf{b}_1}{l_{b1}} + \frac{\mathbf{F}_{b1}}{l_{b1}} \frac{\partial \mathbf{b}_1}{\partial \mathbf{x}} \quad (32)$$

The partial derivative of the force magnitude can be further broken down by applying the chain rule to Eq. (25):

$$\frac{\partial F_{b1}}{\partial \mathbf{x}} = \frac{\partial F_{b1}}{\partial F_t} \cdot \frac{\partial F_t}{\partial \mathbf{x}} + \frac{\partial F_{b1}}{\partial \mathbf{b}_1} \cdot \frac{\partial \mathbf{b}_1}{\partial \mathbf{x}} + \frac{\partial F_{b1}}{\partial \mathbf{b}_2} \cdot \frac{\partial \mathbf{b}_2}{\partial \mathbf{x}} \quad (33)$$

with component derivatives of F_{b1} with respect to F_t , $\mathbf{b}_1$, and $\mathbf{b}_2$ readily determined from Eq. (25).

In a similar way, we apply the chain rule to Eq. (18) to further break down the partial derivative of the elastic tether force

$$\frac{\partial \mathbf{F}_{e,t}}{\partial \mathbf{x}} = \frac{\partial \mathbf{F}_{e,t}}{\partial t} \cdot \frac{\partial t}{\partial \mathbf{x}} + \frac{\partial \mathbf{F}_{e,t}}{\partial l_t} \frac{\partial l_t}{\partial \mathbf{x}} + \frac{\partial \mathbf{F}_{e,t}}{\partial F_{e,t}} \frac{\partial F_{e,t}}{\partial \mathbf{x}} \quad (34)$$

with component derivatives

$$\frac{\partial \mathbf{F}_{e,t}}{\partial t} = \frac{F_{e,t}}{l_t} \mathbf{I}, \quad \frac{\partial \mathbf{F}_{e,t}}{\partial l_t} = -\frac{t}{l_t^2} F_{e,t}, \quad \frac{\partial \mathbf{F}_{e,t}}{\partial F_{e,t}} = \frac{t}{l_t} \quad (35)$$

where $\mathbf{I}$ represents the unity matrix. Expressing the tether length as $l_t = \sqrt{\mathbf{t} \cdot \mathbf{t}}$ the partial derivatives of the tether length and elastic force magnitude can be reformulated as

$$\frac{\partial l_t}{\partial \mathbf{x}} = \frac{\mathbf{t}}{l_t} \cdot \frac{\partial \mathbf{t}}{\partial \mathbf{x}}, \quad \frac{\partial F_{e,t}}{\partial \mathbf{x}} = k \frac{\mathbf{t}}{l_t} \cdot \frac{\partial \mathbf{t}}{\partial \mathbf{x}} \quad (36)$$

The remaining unresolved partial derivatives are $\partial \mathbf{b}_1 / \partial \mathbf{x}$, $\partial \mathbf{b}_2 / \partial \mathbf{x}$, and $\partial \mathbf{t} / \partial \mathbf{x}$. To calculate these, we combine Eqs. (6) and (7) with Eq. (14) to get

$$\mathbf{b}_1 = d_1 \mathbf{e}_{b,x} + h \mathbf{e}_{b,y} \quad (37)$$

$$\mathbf{b}_2 = (d_1 - d) \mathbf{e}_{b,x} + h \mathbf{e}_{b,y} \quad (38)$$

$$\mathbf{t} = \mathbf{T}_E^T (\mathbf{R}_{ig} - \mathbf{R}_k) - \mathbf{r}_{b1} - d_1 \mathbf{e}_{b,x} - h \mathbf{e}_{b,y} \quad (39)$$

We further note that the partial derivatives with respect to the state vector can have nonzero contributions only for the state variables $\mathbf{R}_k$, Θ , $\mathbf{r}_{i1}$, $\mathbf{r}_{i2}$, θ_{i1} , and θ_{i2} because the forces occurring in the tensile support system depend only on the positions and velocities of the suspension points P_{b1} , P_{b2} , and P_{ig} . Taking this into account, we can derive from Eqs. (37–39) closed analytical contributions to $\partial \mathbf{b}_1 / \partial \mathbf{x}$, $\partial \mathbf{b}_2 / \partial \mathbf{x}$, and $\partial \mathbf{t} / \partial \mathbf{x}$. The contributions due to aerodynamic drag and gravitational force acting on the tether are derived in [28] and added to the stiffness matrix $\mathbf{M}$. In a similar way, the additional Jacobian contributions to the mass matrix $\mathbf{M}$ are evaluated.

IV. Wind Tunnel Validation

The extended simulation model is validated with the measured aeroelastic behavior of a bridled wing in a wind tunnel experiment. The measurements were performed in the low-speed low-turbulence wind tunnel (LLT) of Delft University of Technology in a test section 1.80 m wide, 1.25 m high, and 2.60 m long, which can achieve a maximum flow speed of 100 m/s. Under these conditions a small-scale bridled wing model can be designed to exhibit dynamic aeroelastic instabilities.

A. Model Description and Test Setup

As a starting point we have chosen a long and slender wing based on a relatively thin NACA0012 airfoil. As shown in Fig. 6 the wing center ($y = 0$) is rigidly supported from the ceiling by a steel strut, constraining translation and rotation. The wing tips ($y = \pm y_{\max}$) extend into airfoil-shaped steel sleeves that limit transverse movements to a few millimeters, allowing the wind tunnel model to flutter, but preventing excessive deformation that would lead to a destruction of the model and possible damage to the tunnel. The key parameters of the wind tunnel model are summarized in Table 1.

Fig. 6 Rear view (top) and perspective view (bottom, with removed flutter constraints) of the test setup in the wind tunnel test section.

These values are scaled to fit the wind tunnel while representing a realistic AWT in nominal crosswind operation. For example, the aspect ratio of the wind tunnel model is similar to the value of the next-generation large-scale AWT.

Rigid levers made of carbon fiber are glued in chordwise direction to the wing at $y = \pm 0.5y_{\max}$ and close to the tips. The four levers are used to attach the bridle lines and to make the wing susceptible to flutter by adding mass far downstream of the elastic axis. The point of attachment is specified by coordinate x_b , which is measured in downstream direction from the quarter chord of the airfoil, with discrete values ranging from $x_b = 1.0$ up to 22.5 cm (see Fig. 6 bottom). The two bridle lines are attached to either the inner or the outer set of levers. They connect to a single spring that is attached to the ground and has the function to simulate the elastic behavior of the tether. The spring is pretensioned to a force level of $F_{t,0} = 25$ to 125 N for which the initial wing bending is acceptable.

B. Simulation Results

We first analyze the behavior of the wing model without tensile support system, using the original ASWING code. For this baseline configuration the first signs of dynamic instabilities occur at a flutter speed of $v_f = 48$ m/s. Next, we consider the test configuration with the bridle lines attached close to the wing tips ($y_b \approx \pm y_{\max}$). The computed flutter speed is shown in Fig. 7, indicating that for configurations with small x_b or low F_t flutter is practically unaffected by the tensile support system. However, toward combinations of larger x_b and F_t , the flutter speed first rises and then rapidly drops to a low value. Combinations of x_b and F_t on this slope are denoted as *critical*. According to [27] the sudden drop of flutter speed is related

Fig. 7 Computed flutter speed as function of the chordwise bridle attachment and the tether force (bridle lines attached to outer levers).

Fig. 8 Computed flutter speed as function of the chordwise bridle attachment and the tether force (bridle lines attached to inner levers). Markers 1, 2, and 3 indicate parameter combinations referring to Figs. 9, 10, and 11, respectively.

to the transition from normal flutter to stall flutter. This transition is caused by the increasing twist of the wing tips and correspondingly higher flow incidence angles, leading to local separation of the flow. The following drop of the aerodynamic forces and decreasing bending and twisting back leads to reattachment of the flow, triggering the next flutter cycle ([37] Chap. 5).

We then consider the test configuration with the bridle lines attached halfway between root and tip of the wing ($y = \pm 0.5y_{\max}$). The computed flutter speed is shown in Fig. 8. Comparing this surface plot with the plot shown in Fig. 7 it is obvious that the sensitivity of the stall flutter behavior is similar, except that the critical condition occurs at lower values of x_b and F_t . This is because a certain combination of x_b and F_t induces a stronger wing twist when the bridle lines attach to the outer levers.

In a next step we examine flutter by means of a root locus analysis. In Figs. 9–11 the four most interesting flutter modes are plotted for the representative parameter combinations indicated in Fig. 8 as functions of their growth rate $\sigma = \text{Re}(\lambda_k)$ and their angular frequency $\omega = \text{Im}(\lambda_k)$. The analysis reveals that when approaching critical conditions, the unstable flutter mechanism shifts from lower-frequency modes 1 and 2 to higher-frequency modes 3 and 4. For the case of critical conditions the flutter speed has dropped by 40% of the value of the unbridled wing.

C. Experimental Results

For the baseline configuration without tensile support system a flutter speed of $v_f = 58$ m/s was measured in the wind tunnel.

Table 1 Wind tunnel model parameters

Description	Value
Wing span	1250 mm
Wing chord	76.2 mm
Thickness Kevlar 0/90	0.36 mm
Thickness carbon unidirectional	
for $0 < y/y_{\max} < 0.6$	0.40 mm
for $0.6 < y/y_{\max} < 1$	0.20 mm
Spring stiffness	1000 N/m
Spring max. elongation	20 cm

Fig. 9 Computed root locus plot for $x_b = 0$ and $F_t = 100$ N (bridle lines attached to inner levers).

Fig. 10 Computed root locus plot for $x_b = 10$ cm and $F_t = 50$ N approaching critical conditions (bridle lines attached to inner levers).

Fig. 11 Computed root locus plot for a critical combination $x_b = 25$ cm and $F_t = 75$ N (bridle lines attached to inner levers).

This differs significantly from the computed value of $v_f = 48$ m/s because the simulation does not account for imperfections in the airfoil shape, aerodynamic effects of the tunnel walls and the tip

constraints, the nonperfect linear mass and stiffness distribution of the manufactured model, and the nonrigid bridles. However, the main interest of this paper is to validate the additions of the tether-bridle module and hence these effects on the flutter characteristics are considered in more detail.

In the experiment we observe two distinctly different phenomena at the onset of flutter. For combinations of low x_b and F_t , flutter occurs abruptly, with only a slight increase in wind speed (hard flutter), while for combinations of higher x_b and F_t , flutter develops gradually (soft flutter). We observe that at relatively low wind speeds, the wing tips start to oscillate a few times between the constraints and then stop again. Further increasing the wind speed results in continuous oscillations of the wing tips between the constraints. Limit-cycle oscillations (LCO) are characteristic for stall flutter [38–41] and can also be observed in the simulations.

Experiments have shown that with the transition to stall flutter the flutter speed drops to a minimum, but then rises again as the wing is completely stalled [27]. Because ASWING is based on inviscid aerodynamics, it is unreliable in the stall regime and continues to predict very low flutter speeds after transition through the critical regime. This is clearly shown in Figs. 7 and 8. To compare computed and measured results we distinguish between a significant and an insignificant decrease of flutter speed compared with the unbridled wing, using a 20% decrease as threshold. The comparison is shown in Fig. 12 for the test setup with bridle lines attached at $y_b = \pm y_{\max}$. The diagram includes a contour plot of the computed flutter speed shown in Fig. 7. For bridle attachment at $x_b = 1$ cm, the measured flutter speeds v_f are within the 20% range of the unbridled flutter speed $v_{f,0}$ for all investigated values of the tether force. For $x_b = 8$ cm, we observe a significant decrease of v_f for tether forces above $F_{t,\text{crit}} = 75$ N. For $x_b = 12.5$ cm, the critical tether force is further reduced to $F_{t,\text{crit}} = 47$ N.

In Fig. 13 we compare measured and computed results for the test setup with bridle lines attached at $y_b = \pm 0.5 y_{\max}$. For $x_b = 1$ and 8 cm the flutter speed was not significantly decreased by the bridling for all investigated values of the tether force. For $x_b = 17$ cm, the flutter speed drops to significantly lower values somewhere between a tether force of $F_t = 40$ and 60 N. For $x_b = 22$ cm, the critical tether force was at $F_{t,\text{crit}} = 30$ N.

To summarize, we can conclude that although the absolute values of computed and measured flutter speeds differ significantly, the sensitivity with respect to bridle attachment position and tether force is captured well by the simulation model.

V. Application Case

In this section the aeroelastic effects of the main wing of a next-generation large-scale AWT are analyzed computationally.

Fig. 12 Flutter speed as function of the bridle attachment and the tether force. Validation of the simulation model by measured data (bridle lines attached to outer levers).

Fig. 13 Flutter speed as function of the bridle attachment and the tether force. Validation of the simulation model by measured data (bridle lines attached to inner levers).

The geometrical, structural, and aerodynamic properties of the generic but representative design, based on a straight, unswept main wing, are listed in Tables 2–4. The inner parts of the main wing, between the bridle attachment points ($-0.5y_{\max} < y < 0.5y_{\max}$), have

Table 2 Geometric, structural, and aerodynamic properties of a typical next-generation large-scale AWT (General)

Parameter	Value	Description
$\mathcal{R}$	18.7	Aspect ratio
l_f^*	25.5	Fuselage length, % wing span
A_{int}^*	11.9	Elevator area, % wing area
A_{rt}^*	10.33	Rudder area, % wing area
l_{b1}^*, l_{b2}^*	28.2	Bridle line lengths, % wing span
$[x_b^*, y_b^*, z_b^*]$	$[-0.25, \pm 22.93, -1.07]$	Bridle attachment, ^a % wing span
$v_{a,\max}$	95	Max. apparent wind speed, m/s

^aNormalized by main wing span or area.

^aOrigin of the body-fixed reference frame at quarter-chord midspan of the wing.

Table 3 Geometric, structural, and aerodynamic properties of a typical next-generation large-scale AWT (Tether)

Parameter	Value	Description
k	60.0	Stiffness, kN/m
l_t	293	Length, m
$(C_D A)_t$	24.3	Effective drag area, % wing area
m_t	43.0	Mass, % total wing mass
$F_{t,\max}$	39.59	Max. allowable force, ^b %

^bNormalized by the ratio of total rotor power to max. apparent wind speed.

Table 4 Geometric, structural, and aerodynamic properties of a typical next-generation large-scale AWT (Main wing)

Parameter	Value, root	Value, wing tip	Description
c	5.18	2.75	Chord, % wing span
$C_{L,\max}$	3.1	3.1	Max. lift coefficient
α_{stall}	23	23	Critical angle of attack, deg
GJ	1.37×10^6	0.07×10^6	Torsional stiffness, $\text{N} \cdot \text{m}^2$
$[c_{e\alpha}, n_{e\alpha}]$	[2.76, 2.28]	[3.31, 1.79]	Elastic axis, ^c % root chord
$[c_{cg}, n_{cg}]$	[27.38, 2.41]	[15.93, 1.93]	Center of gravity, ^c % root chord
$C_{L,\delta}$	0.59	0.59	C_L increment with δ flap, deg^{-1}
$C_{M,\delta}$	-0.10	0.10	C_M increment with δ flap, deg^{-1}

^cThe elastic axis and center of gravity are relative to the quarter chord line of the airfoil.

a constant chord and structural design, while the outer parts are tapered. The vehicle is equipped with onboard wind turbines that are mounted symmetrically above and below the main wing, between the bridle attachments.

For this specific design torsional divergence, aileron reversal, aileron effectiveness, and flutter are expected to be the most critical aeroelastic phenomena [16,27]. At the moment, the AWE industry is lacking an overarching regulatory framework. The technological similarities between an aircraft and an AWT may justify that, as a starting point, the regulations for “Normal, Utility, Acrobatic and Commuter Category Airplanes” of the FAA and the EASA are used as a guideline [42,43]. These require a safety factor of about 15–20% to protect against potentially dangerous aeroelastic instabilities.

In the study we explored the effect of the following aerodynamic, structural, tether, and bridle parameters on the aeroelastic behavior:

1. rotor power output,
2. main wing center of gravity position,
3. main wing elastic axis position,
4. main wing in-plane and out-of-plane bending stiffnesses,
5. main wing torsional stiffness,
6. main wing mass inertia,
7. fuselage stiffness,
8. amount of flap deflection,
9. flap aerodynamic properties,
10. bridle attachment locations,
11. tether stiffness, and
12. tether effective drag area.

In the next sections we describe the most interesting results of this computational sensitivity analysis.

A. Flight Speed Regimes

At all operating points the aircraft is trimmed for straight, horizontal, and steady flight, using eight independently actuated flaps that are arranged along the main wing. The flaps at the wing tips—the ailerons—trim the aircraft in roll motion. We distinguish three flight speed regimes in which different control strategies are used:

Flight regime 1: $v_a \leq 65 \text{ m/s}$, $F_t \sim v_a^2$. Flap deflections and main wing angle of attack are zero, and aerodynamic loading and hence also tether force increase quadratically with airspeed.

Flight regime 2: $65 \text{ m/s} < v_a \leq 95 \text{ m/s}$, $F_t = F_{t,\max}$. Flaps are deflected upward, linearly increasing with flight speed to avoid that the tether force exceeds the maximum value $F_{t,\max}$.

Flight regime 3: $95 \text{ m/s} < v_a$, $F_t = F_{t,\max}$. Upward flap deflections are maximum and main wing angle of attack is decreased with flight speed.

The increasing upward deflections of the flaps in flight regime 2 and the decreasing angle of attack in flight regime 3 essentially de-power the main wing.

B. Static Aeroelastic Bending and Torsion

The computed wing tip deflection is shown in Fig. 14 for varying bridle attachment along the chord. The corresponding wing twist angle $\theta_{\max}$ is shown in Fig. 15.

In flight regime 1, the aerodynamic loading is concentrated on the inner part of the wing, between the bridle attachments, because the outer parts, from the bridle attachments to the tips, are tapered. The outer parts of the wing thus have a smaller surface and significantly reduced torsional stiffness. Because the wing is supported at the attachment points and the aerodynamic loading pulls the inner part of the wing away from the ground attachment, in positive z direction, the outer parts are deflected in opposite direction. With increasing aerodynamic loading, this tip deflection in negative z direction becomes more pronounced, while the twist angle of the tip continuously increases.

Flight regime 2 is governed by constant force control, using the flaps of the main wing. Because the total aerodynamic loading is not increasing anymore, the global bending mechanism described above, against the two bridle attachments, is not active in this flight regime. As a secondary effect of the upward (negative) flap deflections, the aerodynamic center is shifting upstream, resulting in a nose-up

Fig. 14 Tip deflection as a function of apparent wind speed for varying bridle attachment positions. The nominal value $x_{b,0}$ is listed in Table 2.

Fig. 15 Maximum wing twist angle (positive in case of wash in) as a function of apparent wind speed for varying bridle attachment positions. The nominal value $x_{b,0}$ is listed in Table 2.

pitching moment. Also the aerodynamic loading shifts toward the tips because of the increasing twist and local angle of attack at the tips. As a consequence, the outer parts of the wing bend in positive z direction and the twist angle continues to increase with the flight speed. In flight regime 3 the entire wing is increasingly pitched nose-down, to limit the tether force to the maximum value. The deformation trends observed in flight regime 2 are continued in this regime.

A sudden increase in twist angle for a small increment of airspeed indicates that the flight speed is close to the divergence speed. None of the evaluated flight cases show such behavior and for this reason we can conclude from Fig. 15 that for the considered range of chordwise bridle attachment the wing is safe against torsional divergence up to a flight speed of 130 m/s.

C. Aileron Control Reversal and Control Effectiveness

The effectiveness of a control surface is defined as the ratio of the lift force contribution of the control surface to the lift force contribution of a hypothetical rigid control surface [16]. In the present study, we analyze the control effectiveness of the ailerons, assuming that the aeroelastic effects of rudder and elevator can be neglected.

The computed aileron effectiveness is shown in Fig. 16 as a function of the apparent wind speed for varying torsional stiffness of

Fig. 16 Aileron effectiveness as a function of apparent wind speed for varying torsional stiffness. The nominal value GJ_0 is listed in Table 4.

the wing. A downward deflection of the aileron increases the effective camber of the airfoil and thus also the lift of the wing. This increase is partially compensated by the twisting of the wing in negative θ -direction, which is caused by the nose-down pitching moment resulting from the downstream shift of the aerodynamic center. Because this pitching moment increases with the square of the airspeed, while the elastic restoring moment remains constant, the aileron effectiveness decreases with increasing speed. This is clearly indicated by Fig. 16. It is evident that there will be a critical airspeed at which the actuation effect of the aileron will be completely canceled by the twisting of the wing. This airspeed is denoted as aileron reversal speed ([25] Chap. 4). Beyond this airspeed a deflection of ailerons results in a rolling moment opposite than that of a rigid wing. To avoid this effect, the torsional stiffness of the wing should be increased in case the aileron reversal speed is lower than the operational flight speed [16].

The aileron requirement is defined in [42,43] as a required roll rate, which is a function of the aileron effectiveness, the aileron area, and the deflection angle. Therefore, the requirement on the aileron effectiveness cannot be derived from the aircraft regulations. A minimum control effectiveness of $\eta_{cs} = 70\%$ is assumed in this example. From Fig. 16 we can conclude that the control effectiveness requirement is satisfied up to an apparent wind speed of 100 m/s. The aileron control reversal requirement is satisfied up to at least $v_a = 130$ m/s, even if the torsional stiffness GJ is at 50% of the nominal value GJ_0 .

D. Flutter

The flutter characteristics of the main wing are investigated by means of a root locus analysis. We found that a critical mode with positive growth rate ($\sigma > 0$) occurred at a frequency of around $\omega = 44$ rad/s. This aeroelastic instability can be suppressed by classical approaches such as an increase of torsional stiffness, bending stiffness, or a shift of center of gravity. Figure 17 illustrates how the chordwise position of the center of gravity influences this critical mode. An upstream shift from the nominal value $c_{cg,0}$ by 10 cm is sufficient to suppress the instability. Alternatively, we found that the susceptibility to flutter up to at least 130 m/s can be eliminated by a 50% increase of torsional or out-of-plane bending stiffness.

Next to these classical methods, we know from the wind tunnel validation presented in Sec. IV that adjusting the position of the bridle attachments can also be an effective means to influence the flutter characteristics. The root locus plot for different attachment positions is shown in Fig. 18, indicating that flutter at around $\omega = 44$ rad/s can be suppressed up to apparent wind speeds of 110 and more than 130 m/s by upstream shifts of 50 and 70 cm, respectively.

Fig. 17 Computed root locus plot showing the effect of the center of gravity position on the flutter characteristics. The nominal value $c_{cg,0}$ is listed in Table 4.

Fig. 18 Computed root locus plot showing the effect of bridle attachment positions on the flutter characteristics in the frequency range close to $\omega \approx 44$ rad/s. The nominal value $x_{b,0}$ is listed in Table 2.

Fig. 19 Computed root locus plot showing the effect of bridle attachment position on the flutter characteristics in the frequency range from $\omega \approx 3$ to 9 rad/s. The nominal value $x_{b,0}$ is listed in Tables 2.

A side effect of the upstream shift of the bridle attachment is the excitation of a flutter mode in the frequency range from $\omega \approx 3$ to 9 rad/s. The corresponding root locus plot is shown in Fig. 19. In contrast to the aeroelastic instability at $\omega = 44$ rad/s the flutter in

this frequency range is a flight dynamic mode. For upstream shifts of the bridle attachment of less than 50 cm, this mode is stable up to an apparent wind speed of at least 130 m/s. For an upstream shift of 70 cm, the oscillations become unstable if the apparent wind speed exceeds 90 m/s.

We can conclude from this analysis that moving the bridle position upstream suppresses the elastic flutter mode, while it excites flight dynamic modes.

VI. Conclusions

This paper presents an extension of the simulation code ASWING to static and dynamic aeroelastic analyses of airborne wind turbines or other types of tethered aircraft. The simulation model is validated by wind tunnel tests of a small-scale wing setup exhibiting aeroelastic instabilities. Experiment and simulation consistently indicate that the location of the bridle line attachments and the magnitude of the tether force strongly influence the dynamic aeroelastic behavior. Measured and computed data are in satisfying agreement.

The validated simulation model is subsequently used for the analysis of a generic, but representative next-generation airborne wind turbine. The results show that the classical aeroelastic stabilization methods of the aircraft industry can also be used for tethered aircraft. These are an increase in torsional stiffness to suppress the divergence instability and to increase the aileron reversal speed and an upstream shift of the center of gravity to reduce the susceptibility to flutter modes. However, the simulations also indicate that the effects of the tether and bridle lines are significant. Most notably, the tensile support of the wing decreases the effect of the elastic axis on the wing twist. Also, the wing twist and the flutter behavior are highly dependent on the locations of the bridle line attachments.

Fundamental scaling laws corroborate that the size increase of future AWTs increases the susceptibility to static and dynamic aeroelastic effects. This will increase the need for simulation models, such as the one presented in this paper. A main improvement can be made by increasing the generalizability and usability of this program by modifying the system for three and/or more bridle lines.

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