Aeroelastic Control Using Distributed Floating Flaps
Activated by Piezoelectric Tabs

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In this paper, a novel aeroservoelastic effector configuration that is actuated by piezoelectric tabs is presented. The effector exploits trailing-edge tabs installed on free-floating flaps (FFFs). These flaps are used to prevent flutter from occurring and to alleviate loads originating from external excitations such as gusts. A vertical tailplane wind-tunnel model with two free-floating rudders and a flutter control mechanism were designed, and the aeroelastic stability and response characteristics have been modeled numerically. The controller uses the tailplane tip acceleration as a sensor and sends control signals to the piezoelectrically actuated tabs. Wind-tunnel experiments were performed to demonstrate the feasibility of the technology. It was demonstrated experimentally that the flutter speed associated with the free rudders could be increased by 80%. The same controller, applied to the external rudder, was used to alleviate the aeroelastic response of the tailplane to the excitation of the other rudder, which resulted in a significant decrease in the root bending moment of the tailplane. The results indicate that the FFFs can be very effective in alleviating gust responses and also can be used to prevent freeplay-related limit-cycle oscillations, which are typical for tailplane–rudder combinations.

I. Introduction

In its initial year in service, the Airbus A310 suffered from excessive vibrations of its rudder associated with deviations of the mass distribution and changes in the tailplane stiffness. The Transportation Safety Board of Canada showed that these occurrences were related to aeroelastic instability and could lead to the failure and rupture of the complete rudder surface [1], as in the case of Air Transat Flight TS 961. One of the issues that could lead to aeroelastic instability was freeplay in the rudder due to unforeseen wear of the lateral control system. Other reasons considered were a decrease in the actuator stiffness and a reduction in the rudder torsional stiffness. Although freeplay was not critical for the given case, it has served as inspiration to develop a flutter suppression system that can overcome freeplay in the rudder. To prevent the structure from failing, it may be desirable to install active flutter suppression and load alleviation systems on the rudder. A way to provide the control system with sufficient control authority and frequency bandwidth, with a minimum impact on weight and mechanical complexity, could be to use free-floating flaps (FFFs) actuated by piezoelectrically driven trailing-edge tabs.

Recent research work on FFFs has been carried out by Heinze and Karpel [2], in which a single FFF was mounted near the tip of a high-aspect-ratio flexible wing. The potential of the FFF concept for gust load alleviation was demonstrated with the tab actuator mounted such that the flap was mass balanced to avoid control-surface flutter. A similar setup was used for drag reduction investigations [3,4], albeit without piezoelectric actuation.

Barlas and van Kuik [5] identified trailing-edge devices such as flaps as potential candidates for load alleviation for wind turbines. Van Wingerden et al. [6], Basualdo [7], and Rice and Verhaegen [8] have demonstrated load alleviation for gust loading in wind-tunnel experiments.

The purpose of the research reported in this paper was to advance the FFF concept by using several FFFs on the same wing and designing them with the entire tab actuators located inside the flap. The advantage of this design is that it is simpler, lighter, and faster. The disadvantage is that the underbalanced FFFs tend to produce low-speed flutter that should be suppressed. A convenient setup for demonstrating FFFs is a vertical tail with the FFFs serving as rudders. This will naturally expand the research toward aerostatic control of vertical tailplanes with rudders.

The schematic setup of the FFF model is given in Fig. 1. The piezoelectric bender actuates a lever that deflects the trailing-edge tab. In contrary to the actuator assembly of Heinze and Karpel [2] that shifted its center of mass forward of the hinge line, the mechanism in our case is fully mounted in the rudder. Being inertially underbalanced, the free rudder causes flutter at a low velocity in interaction with the first wing bending mode. A control law that suppresses this flutter by activating the tab can also be used to alleviate other excessive aeroelastic response phenomena.

II. Numerical Model

A numerical model has been used for flutter analysis based on structural eigenmodes. The centerpiece of the model is a main wing box. Two free-floating rudders are attached to it. The model is clamped at the root. The dimensions are given in Table 1.

The structural model has been created within Nastran. The tailplane has been idealized using 914 CQUAD4 shell elements. Lumped masses have been allocated at the tip of the tailplane to reduce the bending frequency of the wind-tunnel model to ensure that
flutter occurs within the wind-tunnel velocity range due to interaction between the first bending mode and flap deflection modes. This enables a single-input-single-output controller design, as other modes will not interact with each other, as their frequencies are too far apart. Consequently, these modes will not flutter within the evaluated regime up to velocities of 90 m/s.

Flutter analysis was performed using the P-K method of MSC/Nastran [9] and confirmed with the g method of ZAERO [10], with the first five modes taken into account. The mode descriptions and the natural frequencies are given in Table 2 and shown in Fig. 2. Figures 3 and 4 show the resulting frequency and damping plots versus the air velocity, respectively. Flutter occurs at 14 m/s, when a damping branch crosses the zero line, due to the interaction of a rudder deflection mode with the bending mode of the tailplane.

Five frequencies can be identified in Fig. 3. Two modes start at zero frequency. These are the rudder deflection modes, for which the deflections are strongly resisted by the resulting aerodynamic hinge moments, as their aerodynamic center lies behind the rotational axis. Because of these forces, the frequencies of the rudder modes increase with the air velocity and approach the frequency of the bending mode. The difference in frequency is less than 2 Hz at 14 m/s, in which the first interaction of the rudder deflection modes with the first bending mode causes control-surface induced flutter, as can be observed in Fig. 4. At higher speeds of about 60 m/s, the second interaction with the first bending mode develops into a second flutter mode. In both cases, the flap movement is opposed to the bending deformation, as shown in Fig. 2a, due to the underbalance of the rudders. This results in energy fed to the structure, which becomes undamped due to the unsteady aerodynamics.

### III. Controller Design

The controller design was kept as simple as possible as the focus was on the technology demonstration.

The aerodynamics of the numerical model have been restated in terms of a minimum-state rational-function approximation [11], which lead to a time-domain state-space aeroelastic model. This system, with the flap deflections as the inputs and an accelerometer at the tailplane tip as the output, established the plant model for control design.

The open-loop system, excited by gust and control inputs, was defined as [12].

\[
\begin{align*}
\{x_{ae}\} &= \{A_{ae}\}\{x_{ae}\} + \{B_{ae}\}\{u_{ae}\} + \{B_{wg}\}\{w_g\} \\
y_{ae} &= \{C_{ae}\}\{x_{ae}\} + \{D_{ae}\}\{u_{ae}\} + \{D_{aw}\}\{w_g\}
\end{align*}
\]

(1)

where \(x_{ae}\) is the state vector, \(A_{ae}\) is the dynamic matrix, \(B_{ae}\) is the input matrix, and \(u_{ae}\) the input vector. \(w_g\) represents the gust velocity input. \(B_{wg}\) is the input matrix to the gust vector, and the state and control-input vectors are defined as

\[
\begin{align*}
\{x_{ae}\} &= \begin{bmatrix} \xi_1 \\ \xi_2 \\ x_u \end{bmatrix} \\
\{u_{ae}\} &= \begin{bmatrix} \delta_1 \\ \delta_2 \end{bmatrix}
\end{align*}
\]

(2)

where \(\xi\) is the modal displacement, \(x_u\) is the aerodynamic lag vector, and \(A\) is the vector of commanded tab deflections. The definition of the other matrices can be found in [12]. \(y_{ae}\) of Eq. (1) is the single measured acceleration that depends on our case on the modal accelerations and the gust input. The aeroelastic system is combined with a state-space representation of the control system such that the open-loop system becomes

\[
\begin{align*}
\{\dot{x}\} &= [A]\{x\} + [B]\{u\} + [B_{aw}]\{w\} \\
\{y\} &= [C]\{x\}
\end{align*}
\]

(3)

where \(y\) contains the presented case a single accelerometer output and \(u\) contains the input to the control system. The system matrices are expanded in Eq. (3) to include the control system and actuator models. Figure 5 displays the control system layout, with the state-space box reflecting Eq. (1), expanded to include the actuator models shown next. The two tabs are controlled by identical commands. The accelerometer output \(y_a\) is connected to the notch filter (NF) input \(u\) by a gain factor \(K\) of 50, which closes the aeroservoelastic loop. The control system is based on a NF for which the input is connected to the accelerometer output via a gain factor \(K\). The NF transfer function (TF) is

\[
NF = \frac{s^2 + 2\zeta_1\omega + \omega^2}{s^2 + 2\zeta_2\omega + \omega^2}
\]

(4)

with \(\omega = 2\pi \times 35.4, \zeta_1 = 0.1,\) and \(\zeta_2 = 0.2.\) The signal is split to be the same for both actuators of the aeroservoelastic plant. ATF models the finite bandwidth (150 Hz) of the accelerometer as a low-pass filter. The TF is given by

\[
TF_{acc} = \frac{\omega^2}{s^2 + 2\zeta\omega + \omega^2}
\]

(5)

with \(\omega = 2\pi \times 150\) and \(\zeta = \frac{1}{\sqrt{2}}.\) The control TFs were converted to a state-space form and integrated with the plant to form Eq. (3) using ZAERO, which requires a third-order actuator model. It was chosen to be of a relatively large bandwidth to reflect the performance of piezoelectric actuators. The actuator TF is given by

\[
\delta = \frac{4.96 \times 10^9}{s^3 + 5403s^2 + 8.68 \times 10^9s + 4.96 \times 10^7}
\]

(6)

This simple controller has been implemented in ZAERO to obtain a prediction of the flutter properties with the controller switched on. Figures 6 and 7 give the damping and the frequency plots, respectively. The control system modified the interaction between the first rudder mode and the first bending mode in a way that eliminated the low-speed flutter mechanism.

The flutter speed increases to 48 m/s at 34 Hz. This clearly shows the potential of the piezoelectric actuator usage in controller design of tailplanes, wings, or blades. When interacting, the frequency branches that relate at low velocities to the first tail bending and the
outboard rudder deflection modes exchange shapes. As one can see from the frequency curves, the rudder deflection frequency increases strongly up to 30 Hz. At the same time, the second bending frequency decreases from 50 to 35 Hz due to the interaction with the rudder mode, such that it starts to interact with the torsion mode. The consequence is that this mode combination becomes the first flutter mode instead of the original bending-rudder type. Additionally, it can be seen that the frequency plot of the bending mode strongly decreases from 20 m/s onward due to the control effects, but the interaction with the inboard rudder mode does not produce flutter either. The change of the frequency plot of the inboard control surface is very small.

IV. Piezoelectric Actuator Design

The trailing-edge tabs are driven by piezoelectric actuators, which are mounted within the rudder. The bender used is constructed from T219-H4CL lead-zirconium-titanate (PZT) sheets manufactured by Piezo Systems, Inc. Its properties are given in Table 3. Further specifications can be found online.\(^*\) The maximum voltage that this material can handle is 110 V. At higher voltages, domain switching would occur. Therefore, a saturation limit of 100 V has been included in the controller design.

As shown in Fig. 1, the bender is clamped to the rotation axis of the rudder. At the other end, it is hinged to a quasi-rigid bar that drives the control tab.

Dunsch and Breguet\(^{[13]}\) give the deflection of a bimorph bender as

\[ \delta(L) = \frac{3d_{31}L^2}{8h^2} U \]  \hspace{1cm} (7)

with \(L\) as length of the bender, \(h\) the thickness, \(U\) the applied voltage, and \(d_{31}\) as the piezoelectric coefficient. Using flat plate aerodynamics, the force on the trailing-edge tab can be approximated by

\[ F = \frac{1}{2} \rho V^2 S_{\text{tab}} 2\pi \delta \]  \hspace{1cm} (8)

Combining with the analytical deflection of the bender, the tab deflection can be evaluated to

\[ \delta = \frac{3d_{31}L^2/8h^2 U}{d + \rho V^2 S_{\text{tab}} \pi L^2/3EIb/d} \]  \hspace{1cm} (9)

Data available online at http://www.piezo.com/prodbm2highperf.html [retrieved 2 January 2011].
where $b$ is the distance from the aerodynamic center of the tab to its hinge line and $d$ the length of the bar that connects the tab to the tip of the bender. Figure 8 shows the tab rotation as a function of the voltage (isofield lines from 10 to 70 V) and the velocity. It can be concluded that, over the entire operational range of the wind tunnel (WT) up to 30 m/s, at least a tab deflection of 1 deg can be reached with 70 V.

These results have been confirmed with a simulation of the bender-bar system using ABAQUS. The deflection is shown in Fig. 9. As can be seen, the quasi-rigid beam element does not show any visible deflections. For the input voltage of 60 V in the displayed case, a tab rotation of 1.26 deg can be observed.

V. Wind-Tunnel Experiment

A. Structural Model Verification

The first step in the wind-tunnel experiments was the identification of the actual model (Fig. 10) using the method developed by van der Veen et al. [14]. During the construction, some adjustments to the model needed to be made. The most notable is that the attachment to the balance needed to be changed. In the wind-tunnel model, the tailplane was clamped to a bar that ran through an aerodynamic panel. This bar was attached to the balance. Because of the addition in length, the stiffness of the model decreased; thus, the eigenfrequency decreased as well.

Figure 11 shows the acceleration of the tip as a function of the frequency of the trailing-edge tab motion, extracted from stochastic excitation of the tab with maximum of 100 V for both the numerical and the wind-tunnel models. The peaks correspond to the eigenfrequency of the structure. For 16 m/s, the identification was performed in closed-loop operation due to instability of the uncontrolled system at this wind speed, which is beyond the flutter speed. A deterministic perturbation signal was superimposed on the control signal, allowing the open-loop dynamics to be identified using an identification technique suitable for use with closed-loop measurement data [14]. The curves for 10 m/s correspond to open-loop responses in the preflutter case. The graphs of the numerical methods are based on the state-space matrices obtained in the open-loop analysis in ZAERO.

The first experimental peak is clearly just below 4 Hz for the real model. As expected, this is a lower frequency compared to the numerical model because the additional beam decreases the stiffness of the structure, thereby reducing the eigenfrequency. However, the peaks display similar magnitudes. The first torsional mode can be identified at 29 Hz. Its frequency is lower than the predicted value of 35 Hz due to the structural modifications. At frequencies of more than 40 Hz, the identification of the wind-tunnel model is strongly polluted with environmental noise. Therefore, higher frequencies are filtered out in the controller.

\[ x' = Ax + Bu \]
\[ y = Cx + Du \]

where $A$, $B$, $C$, and $D$ are matrices, $x$ is the state vector, $u$ is the control input, and $y$ is the output.
Table 3 Properties of the lead-zirconium-titanate piezoelectric material

<table>
<thead>
<tr>
<th>Property</th>
<th>Definition</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>(l_{\text{piezo}})</td>
<td>Thickness of piezoelectric sheet</td>
<td>0.1905 mm</td>
</tr>
<tr>
<td>(l_{\text{substrate}})</td>
<td>Thickness of substrate</td>
<td>0.06 mm</td>
</tr>
<tr>
<td>(E_{\text{piezo}})</td>
<td>Young's modulus of piezoelectric sheet</td>
<td>62 MPa</td>
</tr>
<tr>
<td>(E_{\text{substrate}})</td>
<td>Young's modulus of substrate</td>
<td>70 MPa</td>
</tr>
<tr>
<td>(\varepsilon_{11})</td>
<td>Piezoelectric coefficient</td>
<td>(-320 \times 10^{-9}) mm/V</td>
</tr>
<tr>
<td>(L)</td>
<td>Length of bender</td>
<td>78 mm</td>
</tr>
<tr>
<td>(w)</td>
<td>Width of bender</td>
<td>36 mm</td>
</tr>
<tr>
<td>(d)</td>
<td>Length of beam</td>
<td>52 mm</td>
</tr>
<tr>
<td>(b)</td>
<td>Distance of hinge to aerodynamic center</td>
<td>7.5 mm</td>
</tr>
<tr>
<td>(\rho)</td>
<td>Density</td>
<td>7800 kg/m³</td>
</tr>
</tbody>
</table>

B. Control System Changes

The control system has been updated with respect to the numerical case, as can be seen in Fig. 12. Two control blocks have been added, namely, a low-pass filter and a lead compensator. The controller consists of a series interconnection of several elements. There are numerous natural modes of the structure at frequencies beyond the frequency of interest, i.e., the flutter mode at a frequency of 3.7 Hz. Because the acceleration is measured, these modes contribute significantly to the measurement signal. It is not a desire to control these modes, however, and their associated phase lag could easily introduce instability in feedback. For this reason, two low-pass filters are added to filter off most of the signal beyond 10 Hz. Because the first torsional mode at 35 Hz is fairly close to the controlled mode and the low-pass filters did not provide sufficient attenuation at this frequency, it was filtered out using an additional NF. Finally, a lead compensator was added and tuned to obtain the desired phase of close to 0 deg at the controlled frequency and to obtain a crossover frequency of 4 Hz with a gain margin of 10 dB and a phase margin of 45 deg. The overall transfer of the input to the state-space model is given in Eq. (10). The Bode plot of the TF of the controller is shown in Fig. 13. The peaks TF at 50 rad/s, which is slightly above the first bending eigenfrequency of the model.

\[
\text{TF} = G \cdot LP_1 \cdot LP_2 \cdot LL \cdot NF
\]  

(10)

with the gain \(G\) equal to 70. The low-pass filter is given in Eq. (11),

\[
\text{LP} = \frac{\omega^2}{s^2 + 2\zeta\omega s + \omega^2}
\]

(11)

with the coefficients for the first low-pass filter being \(\zeta = 0.5\) and \(\omega = 2\pi \times 15\), whereas the coefficients for the second low-pass filter are \(\zeta = 0.7\) and \(\omega = 2\pi \times 20\). The TF of the lead compensator is shown in Eq. (12),

\[
\text{LL} = \frac{\tau_1 s + 1}{\tau_2 s + 1}
\]

(12)

The coefficients in Eq. (12) are \(\tau_1 = \frac{1}{0.016}\) and \(\tau_2 = \frac{1}{0.016}\). The last component of the controller is the NF as given in Eq. (4). The parameters were changed to \(\omega = 2\pi \times 29\), \(\zeta_1 = 0.016\), and \(\zeta_2 = 0.08\).

C. Control Authority

Figure 14 shows the deflection of the rudder in response to a sinusoidal deflection of the associated piezoelectric tab of 1 deg amplitude at an air velocity of 11 m/s. Especially the outboard (second) control surfaces perform very well. For very low frequencies, the numerical performance could not be met. However, in the frequency range containing the first flutter frequency, the predictions match the measured displacements closely, and amplitudes of up to 8 dB can be reached. At higher frequencies, the amplitude reduces. This reduction is associated with the incomplete buildup of the unsteady aerodynamic forces. Although the numerical prediction shows resonance of the control-surface authority at the structural eigenfrequency, the wind-tunnel measurements failed to capture this phenomenon as only a limited number of frequency samples was taken. The differences between the numerical prediction and the experimental behavior of the inboard control surface can be attributed to friction in the hinge due to production inaccuracies. This limited the maximum deflections the inboard tab could reach.
The gear ratio between tab rotation and rudder deflection increases when approaching the open-loop flutter speed, as shown in Fig. 15. This means that the control system using FFFs driven by a trailing-edge tab becomes more efficient when approaching the flutter speed.

D. Open-Loop Flutter

The first test series was the reproduction of the results of the numerical prediction concerning the flutter speed. In Fig. 16, the wind-tunnel measurements of the flutter mode are plotted against the prediction according to the numerical model. As can be seen, the experimental results are similar to the numerical prediction by about an offset of \(2 \text{ m/s}\). It is believed, that the numerical model deviates from the test results because of the alterations that needed to be made when attaching the structure to the balance that lowered the first bending frequency.

E. Dynamic Load Alleviation

The second test was conducted to demonstrate dynamic loads alleviation due to external excitation. Because the tunnel is not equipped with gust generators, the inboard control surface has been used to excite the model in a sinusoidal fashion, whereas the outboard surface has been used with the control law that was designed for
flutter suppression. Figure 17 shows the root-bending-moment amplitude as a function of the wind speed up to just below the open-loop flutter speed. Although the effect is small for low velocities, it becomes more pronounced when approaching the open-loop flutter speed. At low speeds, the forces generated are not high enough to overcome the friction in the system, and therefore, not enough control authority can be obtained. At higher speeds, the root bending moment was decreased by about 40%. The large load reduction is the result of the significant damping added to the first bending mode by closing the control loop.

Similar observations were made when considering the second load case when the structure was excited by force impulses, and damping was evaluated from the decay rate of the resulting tip accelerations. For values below 8 m/s, the difference between the controlled and uncontrolled cases are small. As the wind speeds increase, the damping coefficient is increased significantly by the designed control mechanism, due to delaying the flutter speed, as shown in Fig. 18.

F. Flutter Suppression

The most important challenge of the wind-tunnel tests was to demonstrate the ability of the control system to increase the flutter speed. For that purpose, the flutter tests have been repeated with the controller switched on. Figure 19 illustrates the pole trajectories of the controlled model compared to the numerical solution. As already pointed out, the eigenfrequencies of the wind-tunnel model are lower than the numerical model. Damping was extracted in the tunnel by introducing a force impulse using a rope connected to the tailplane tip and measuring the rate of tip-acceleration decay.
tests was based on the system identification at 11 m/s. A reidentification of the dynamics at higher speeds is likely to result in an improved model that can further suppress flutter than it was demonstrated in the wind-tunnel tests.

A second experiment was conducted to prove that the flutter speed can not only be increased but that the system can be stabilized by the control system. Once flutter has been started. For that purpose, the structure was excited by an impulse while the controller was switched off. For this experiment, an airspeed 11.6 m/s was chosen, which is slightly above the open-loop flutter speed. Figure 21 shows the tip acceleration as a function of time. Right after the initial impulse, at \( t = 0 \), the acceleration amplitude was about 1 g. Within 4 s, this acceleration tripled. At that point, the controller was switched on, and the system immediately became stable again within considerable damping. The amplitude of the accelerations decreased to less than 0.4 g within 2 s. This damping corresponds to the closed-loop damping as displayed by the pole trajectories in Fig. 19.

VI. Conclusions

The technology demonstration of flutter suppression and load alleviation using free-floating flaps driven by trailing-edge tabs has been successful. An increase in the flutter speed of more than 80% from 11.6 to 21.5 m/s has been reached. Numerical studies show that the flutter speed could be increased even further with the given model, but saturation of the actuators was a limiting factor in controlling flutter onset. Already at 15 m/s, the controller started limiting the voltage that is sent to the piezoelectric actuator to avoid saturation. At wind speeds of 18 m/s, the knock-down factor was about 1/8. Still, the controller was able to stabilize the model until 21 m/s. It is assumed that an update in the actuator design toward multiple actuators combined with higher deflections of the lead-zirconium-titanate benders can yield a substantial increase in control-surface deflection and thus in control moment. Besides the flutter prevention, the controller has shown to be able to reduce the dynamic response due to external excitation significantly by a factor of more than 2. The controller can work throughout a wide range of wind speeds and yaw angles, making it suitable for applications on real aircraft. A scheduled controller that includes the measurement of the wind speed can increase the controller performance.

In a next step, the numerical side of the problem should be further investigated. Limit-cycle oscillations can be the subject of follow-up studies. Another highly interesting case is the numerical simulation of the controller being switched on as a comparison to the experimental results.

Acknowledgments

The system identification was technically supported by Jan Willem van Wingerden of Delft University of Technology. Credit

The experimental damping plot shows only the damping of the dominant mode. One can see a very strong increase in damping starting at 11 m/s with a peak at 14 m/s. This increase is similar to the one shown in Fig. 7, starting at about 20 m/s, when the closed-loop outboard rudder starts to interact with the first bending mode. The measured damping in Fig. 19 is strongly reduced after a wind speed of 15 m/s but does not cross the zero line. The strong reduction results from the saturation limit of the controller during the damping measurements and because the dominant bending mode starts to interact with the inboard rudder that has a smaller effect on the damping. Figure 20 shows the tip accelerations and the input sent to the piezoelectric actuators sent by the controller. The controller output alternates between the saturation limit in both directions. Still, the controller is able to suppress flutter, and the system is stabilized by driving the rudders to oscillate out of phase with the motion of the vertical tailplane, thereby reducing the energy of the system.

Significant flutter suppression until a speed of 21.5 m/s was demonstrated, which is an increase of 80% with respect to the open-loop flutter. The limiting factor was the voltage saturation of the piezoelectric actuator, in which peak overshoots of a factor of 8 were observed for an impulse at the tip of the vertical tailplane. Presumably, the control is able to prevent flutter at higher wind speeds if the amplitude of the excitation is not too big. Even more so when considering that the linear model that was used throughout the
also goes to Mark Pustelnik of Technion for his work on the controller design of the numerical model.

References