Comparisons of Flutter Analyses for an Experimental Fan

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Abstract

Two propulsion aeroelasticity codes were used to model the aeroelastic characteristics of an experimental forward-swept fan that encountered flutter during wind tunnel testing. Both of these three-dimensional codes model the unsteady flowfield due to blade vibrations using the Navier-Stokes equations. In the first approach, the unsteady flow equations are solved using an implicit time-marching approach. In the second approach, the unsteady flow equations are converted to a harmonic balance form and solved using a pseudo-time marching method. This paper describes the flutter calculations and compares the results to experimental measurements.

Introduction

Aircraft engine turbomachinery blades are susceptible to aeroelastic vibration problems and high-cycle fatigue failures. Flutter stability is likely to become a major challenge as aggressive new designs of fan, compressor and turbine blades are developed to reduce noise, improve performance, and reduce weight. Hence, it is important to develop and validate numerical tools that can be used to verify aeroelastic stability using high-fidelity physics-based models. Such numerical tools will enable gas turbine designers to develop new turbomachinery blading that will not flutter, thus improving safety, reducing development cycle time and cost, and enabling the targeted improvements listed earlier.

Research has been on-going in the development, validation and application of high-fidelity models for aeroelastic vibrations in aircraft engine fan, compressor, and turbine blades (Ref. 1). Recent work has included time-domain solution of the Reynolds-averaged Navier-Stokes (RANS) equations to provide the unsteady flowfield and unsteady aerodynamic forces on the blades. An example of such work is the TURBO aeroelastic analysis code (Refs. 2 and 3). Such high-fidelity time-domain models require large numbers of computations and a long time for startup transients to decay before the final periodic solution is obtained. A second approach is to use the periodicity in time of typical turbomachinery flows to represent each flow variable by a Fourier series in time, leading to a harmonic balance form of the Navier-Stokes equations (Ref. 4). Solutions to these equations can be obtained using methods that are typically used for steady flow problems such as pseudo-time marching and local time stepping. Thus, the harmonic balance approach leads to a method that can be significantly faster than the typical time-domain solution method (Ref. 4).

In the present study, the configuration selected is an experimental fan (Ref. 5) for which wind-tunnel measurements of performance and flutter vibrations are available. This fan was designed with aggressive goals for performance and noise reduction. During wind-tunnel testing, the fan performed well at design speed, and was successfully throttled to the stall line. However, flutter was encountered just above the operating line at part-speed conditions. The flutter mode was identified as the first bending mode of the airfoil, in a two nodal diameter forward-traveling wave pattern. In this paper, a comparison is presented between two flutter analyses applied to this fan: a time-domain RANS aeroelastic code (Ref. 2) (TURBO) and a harmonic balance (HB) RANS aeroelastic analysis and computer code developed by Hall et al. (Refs. 4 and 6). Both analyses solve the three-dimensional unsteady, Reynolds-Averaged Navier-Stokes equations with the ability to model a rotating blade row with harmonic blade vibrations or incoming periodic distortions. For flutter calculations, the blade vibration is prescribed to be the modal deflection and a frequency, both of which are calculated from a separate structural dynamics analysis. Computations are performed in a single blade passage for both steady and unsteady analyses. In the time-domain analysis (Ref. 2), the unsteady pressures are used to calculate a work-per-cycle that is then used to calculate the aerodynamic damping, which is used to determine aeroelastic stability in the selected vibration mode and traveling wave pattern. In the harmonic balance analysis, the resulting unsteady pressures are used to calculate a generalized aerodynamic force and then an eigenvalue problem is solved to calculate the aerodynamic
damping, which is used to determine stability of the blade in a specific vibration mode (Ref. 7).

Note that both the aeroelastic codes were run with small amplitude vibrations of the fan blade to calculate linearized unsteady aerodynamics for a conventional linear flutter analysis. However, both the analysis procedures can also be used for larger amplitudes of vibration.

Analysis

Aeroelastic Model

The equations of motion for a fan blade (with all blades assumed to be identical) can be written as

\[ [M] \ddot{q} + [K] q = [A] q \]  \hspace{1cm} (1)

where \([M]\) and \([K]\) are generalized mass and stiffness matrices, \([q]\) is the generalized displacement vector, and \([A]\) is the blade vibration-dependent generalized aerodynamic force matrix. The matrices \([M]\), \([K]\), and \([A]\) are of size \(NM \times NM\); \([q]\) is of size \(NM \times 1\); \(NM\) is the number of modes.

The elements of \([M]\) and \([K]\) are obtained from a free-vibration analysis using a commercial structural dynamics analysis software. The matrices \([M]\) and \([K]\) are diagonal and their non-zero elements are related as

\[ K_i = M_i \omega_i^2 (1 + 2i\zeta) \]  \hspace{1cm} (2)

where \(\omega_i\) is the natural frequency of the \(i\)th mode, and \(\zeta\) is the structural damping ratio; usually the mode shapes are mass-normalized and therefore \(M_i = 1\).

Since all the blades are identical (that is, a tuned rotor), the aeroelastic modes consist of individual blades vibrating with equal amplitudes at a fixed interblade phase angle between adjacent blades. Hence, the motion of the \(s\)th blade in \(r\)th interblade phase angle mode can be written as

\[ \{q_s\} = \{q_r\} e^{i\sigma_r t} e^{i\omega r t} \]  \hspace{1cm} (3)

where \(\omega\) is the vibration frequency, \(\sigma\) is the interblade phase angle related to nodal diameter (ND) pattern of the traveling wave and number of blades \(N_{\text{blades}}\) as

\[ \sigma = 2\pi ND / N_{\text{blades}} \]  \hspace{1cm} (4)

Thus, the equations of motion for a blade become

\[ -\omega^2[M]q_r + [K]q_r = [A]q_r \]  \hspace{1cm} (5)

The following subsections describe the flutter calculation method used with the harmonic balance code and separately with the time-domain TURBO code.

HB Flutter Analysis

The HB flutter analysis requires calculation of elements of the generalized aerodynamic force matrix \([A]\). Unsteady flowfield computations are carried out for each vibration mode and assumed frequency. For a selected value of interblade phase angle, the harmonic balance code is used to calculate the unsteady pressure distribution on the blade surface, which is further used to calculate the (complex-valued) elements of the generalized aerodynamic force matrix \([A]\). This calculation is repeated for \(N_{\text{blades}}\) interblade phase angles given by Equation (4).

To calculate flutter stability, Equation (5) is written in a standard eigenvalue form as:

\[ [P] - \gamma [Q] q_r = 0 \]  \hspace{1cm} (6)

where

\[ [P] = ([K] - [A]) / \omega^2; \quad [Q] = [M]; \quad \gamma = (\omega / \omega_0)^2 \]  \hspace{1cm} (7)

The real part of the eigenvalue (\(\mu\)) represents the damping ratio, and the imaginary part (\(\nu\)) represents the damped frequency; flutter occurs if \(\mu > 0\) for any eigenvalue.

In the present work, structural damping is set to zero and therefore damping is referred to as aerodynamic damping. Also, note that \(\mu\) is opposite in sign to damping.

TURBO Work-Per-Cycle Method

The work-per-cycle approach is used to determine flutter stability. First, the flowfield through the blade row is calculated with no prescribed blade vibration. Starting with this converged steady flowfield, blade vibrations are prescribed in a selected mode, frequency, and nodal diameter pattern or phase angle. After the transients in the flowfield decay, and a periodic flowfield is obtained, the work done on the vibrating blade is calculated for a cycle of blade vibration as follows:

\[ W = \oint_{\text{surface}} -p dA \cdot (\partial X / \partial t) dt \]  \hspace{1cm} (8)

where, \(p\) is the blade surface pressure and \(A\) is the surface area vector. For harmonic vibration, the work-per-cycle of oscillation, can be rewritten as
The aerodynamic damping ratio \( \zeta \) associated with blade vibration is related to the work-per-cycle \( W \) and the average kinetic energy \( K_E \) of the blade over one cycle of vibration through the following equation (Ref. 8)

\[
W = \oint -p dA \cdot \partial q_0 \omega \cos(\omega t) dt
\]

(9)

\[
\frac{W}{K_E} = \frac{8\pi \zeta}{\sqrt{1-\zeta^2}}
\]

(10)

where

\[
K_E = \frac{1}{T} \int_0^T \frac{1}{2} mV^2 dt
\]

(11)

\[
\zeta = C/C_{cr} ; \quad C_{cr} = 2m\omega
\]

(12)

In the preceding equations, \( C \) is the damping, \( C_{cr} \) is the critical damping, \( m \) is the mass of the blade, \( V \) is the surface velocity due to blade vibration, and \( T \) is the time period.

For small values of damping ratio which typically occur in aerelastic calculations of interest, \( \zeta \ll 1 \), the aerodynamic damping ratio can be approximated as

\[
\zeta \approx -W/8\pi K_E
\]

(13)

If aerodynamic damping is negative, flutter can occur. Note that the structural damping (material and mechanical damping) has not been considered. Also, note that aerodynamic damping \( \zeta \) is opposite in sign to \( \mu \).

Results

In this study, the airfoil geometry used was calculated based on static deflections obtained from structural analysis for 85 percent speed. The static deflections included the effects of rotational speed, applied pressures, and blade temperatures; nonlinear geometry effects were also included in the analyses. The blade geometry at 85 percent speed and nominal operating conditions was used for all computations. Previous calculations (Ref. 2) for this case have shown that changes from the nominal blade geometry due to changes in rotational speed were not significant and therefore a single geometry was used for computations at all speeds and operating conditions in the present work. The computational grid used was generated using commercial software. The grid for the harmonic balance computations is shown in Figure 1(a); the grid size is 193x33x49 for the O-grid block that wraps around the blade airfoil with 193 grid points around the airfoil, 33 grid points in the circumferential direction, and 49 grid points in the spanwise direction. The H-grid blocks in the inlet and exit sections are each 17x33x49 with 17 points in the streamwise direction, 33 grid points in the circumferential direction, and 49 grid points in the spanwise direction. The tip clearance was based on rig test measurements and is modeled using 9 points between the blade tip and casing. For the TURBO time-domain computations, the H-grid, shown in Figure 1(b), has a size of 121x51x39; 4 cells are used in the tip clearance region.

The inlet flow conditions used in the computations consisted of circumferentially-averaged radial profiles of total pressure, total temperature, and flow angles. These prescribed profiles were based on rig measurements, supplemented by previous steady computations. The exit flow conditions consisted of a circumferentially-averaged radial profile of static pressure. This profile, which was based on pressure measurements at design speed, was used with uniform scaling for the computations at all speeds and at all conditions.

Steady Computational Results

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Steady computational results were presented for three fixed rotational speeds (100, 85, and 75 percent). For each speed, the exit pressure profile was uniformly scaled to vary the mass flow rate and the operating point on the fan map. Thus, three speedlines were generated using each analysis code. Figure 2 shows the convergence of the steady harmonic balance computations as a plot of non-dimensional torque with iteration counter (100 iterations per counter). The computations were carried out at 85 percent speed for various values of imposed backpressure at the exit boundary. As can be noted from Figure 2, excellent convergence was obtained for all operating conditions. Similar convergence was obtained for the computations at the other speeds as well as for the computations with the TURBO code at all speeds. All steady results presented here are from well-converged solutions.
The fan map is shown in Figure 3 with the results of harmonic balance steady computations denoted as HB and the results of the TURBO computations. Several computations were done for 75, 85, and 100 percent speeds. All computed results correlate well with measurements. Of particular interest is the last condition computed on the stall side, beyond which no converged solutions were obtained. For the HB results, the predicted pressure ratio is approximately 3 percent lower at 75 and 85 percent speeds, and approximately 4.5 percent lower at 100 percent speed. For the TURBO results, the predicted pressure ratio is approximately 1.5 percent higher at 100 percent speed and nearly identical at the other two speeds. For the HB results, the mass flow rate at the near-stall point is lower than measurements at 75 percent speed, in excellent agreement at the 85 percent speed, and significantly higher at the 100 percent speed. For the TURBO results, the mass flow rate is higher at the part speeds, and nearly the same at 100 percent speed. Note that the stall line shown in Figure 3 was estimated based on prior experience and was not confirmed during rig testing because of the occurrence of
flutter. Also, since the stall side was of primary interest, no attempt was made to determine the upper bound on choke flow.

Overall, the HB results show good correlation with the TURBO results, except for a slight shift in the speed lines towards lower mass flow rate and lower pressure ratio for both 75 and 85 percent speeds. At 100 percent speed, the HB results are shifted towards lower pressure ratio, and slightly higher mass flow rate. At 75 and 85 percent speeds, the HB calculations provide converged results for lower mass flow rates than were obtained with TURBO. Also, the HB results consistently show a slightly lower pressure ratio as compared to TURBO and the test data. These differences need to be investigated further. It should be noted that significant differences exist between the numerical method used in the TURBO computations and the HB computations. These differences include time-domain versus frequency-domain, numerical discretization, grid, and algorithm used to solve the RANS equations, turbulence modeling, and others. Therefore, the differences in results noted in Figure 3 are not entirely surprising.

To examine the sensitivity of the HB computed results to numerical parameters, the second and fourth order smoothing parameters, which are necessary to ensure numerical stability, were varied, respectively, between 0.25 and 1, and between 0.002 and 0.006, as listed in Table 1. The results shown in Figure 4 indicate that no significant change in pressure ratio resulted from the stated variation in these numerical parameters. The mass flow variation was less than 0.5 percent.

### TABLE 1.—RANGE OF NUMERICAL SMOOTHING PARAMETERS USED TO STUDY VARIABILITY OF HB COMPUTED PERFORMANCE

<table>
<thead>
<tr>
<th>Case</th>
<th>2nd order</th>
<th>4th order</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1.0 (nominal)</td>
<td>0.006 (nominal)</td>
</tr>
<tr>
<td>2</td>
<td>1.0</td>
<td>0.002 to 0.005</td>
</tr>
<tr>
<td>3</td>
<td>0.25</td>
<td>0.002</td>
</tr>
</tbody>
</table>

**Unsteady Computational Results**

### Vibration Mode 1

Unsteady computations were performed using the harmonic balance code at 100, 85, and 75 percent speeds to predict the aeroelastic stability of the blade. The first blade vibration mode and several nodal diameters covering the entire possible range were considered. Various steady conditions were considered from near peak-efficiency to near the stall line. This allowed the trend along the speed line to be assessed. Note that the results presented in this paper were all computed with only the 0th and 1st harmonics included in the aerodynamic analysis using the harmonic balance method.

Figure 5 shows the convergence of the unsteady computations as a plot of non-dimensional generalized force (real and imaginary parts) with iteration counter (100 iterations per counter) for various nodal diameters. These computations were for 85 percent speed and for an operating point near the peak-efficiency condition with a non-dimensional backpressure of 1.0. As can be noted, although the rates of convergence vary with nodal diameter, all the results are well-converged. Similar convergence characteristics were observed at the near-stall condition with a nondimensional backpressure value of 1.05.

A sensitivity study was performed to investigate the effect of the unsteady grid scaling parameter. The nominal value of this numerical input parameter was increased by factors of 2 and 5, and then decreased by the same factors. There was no change in the generalized force results within plotting accuracy – demonstrating that for the present calculations, the prescribed unsteady grid scaling has no effect on the results. Recall that in the present study, the harmonic balance aeroelastic code was run with very small amplitude vibrations of the fan blade to calculate linearized unsteady aerodynamics for a conventional linear flutter analysis.

![Figure 4.—Nondimensionalized fan map showing variability in results due to numerical smoothing parameters.](image)

![Figure 5.—HB unsteady solution convergence.](image)
Figure 6 shows the variation of the converged generalized force with nodal diameter for the two different values of backpressure at 85 percent speed. For a single vibration mode, the stability is determined by the imaginary part of the generalized force and Figure 6 shows that the stability varies significantly with nodal diameter. Further, with the change in backpressure from peak-efficiency towards stall, it can be seen that the imaginary part of the generalized force drops closer to zero at $ND = 2$. This trend is seen more clearly in Figure 7.

The imaginary part of the generalized force is seen to clearly move towards zero as the backpressure increases – going from peak-efficiency towards stall. Note that Figure 7 includes results at nondimensional backpressure values of 1.052 and 1.0522, with a change in the sign of the imaginary part occurring between these conditions.

The generalized force was used to calculate the aerodynamic damping, which is plotted in Figure 8(a). The results are presented for backpressure values near peak-efficiency (1.0) and near-stall (1.05) conditions. The aerodynamic damping is seen to vary by an order of magnitude with nodal diameter of the traveling wave, with the minimum occurring at $ND = 2$ (forward traveling wave). Note that although the damping value is nearly zero at the near-stall condition, it is still positive even at its minimum value, indicating stability at a nondimensional backpressure of 1.05.
The aerodynamic damping calculated from the TURBO code is shown in Figure 8(b) as a variation with nodal diameter. Note that the backpressure values are slightly different for the TURBO results and the HB results. Also, the TURBO computations were done for fewer nodal diameter than with the HB code. A comparison of the results in Figure 8(a) and (b) clearly shows that both methods predict very similar variations with nodal diameter. Further the variation in damping values is nearly the same, as are the trends going from peak efficiency to near stall operating conditions (increasing backpressure).

Additional calculations were carried out for a small increase in backpressure to identify the condition of zero aerodynamic damping (flutter). In the absence of these additional results at backpressure of 1.052 and 1.0522, it would be necessary to extrapolate the damping as a function of mass flow to identify the flutter point. Figure 9 shows the variation of aerodynamic damping with mass flow rate with a clear monotonic drop in stability along the speed line towards stall.

For the TURBO computations, the aerodynamic damping was linearly extrapolated to obtain the flutter condition since the last computed condition near stall showed a small positive aerodynamic damping.

Vibration Mode 2

To verify that the mode 1 is the most unstable mode, HB unsteady computations were carried out for mode 2 at 85 percent speed. The generalized force variation with nodal diameter, shown in Figure 10, is somewhat similar to that for mode 1 shown in Figure 6. Note that ND = 4 is the least damped nodal diameter. Unsteady computations were carried out for ND = 4 and 2 for various steady operating conditions (backpressure values or mass flow rate values). Figure 11 shows that mode 1 2ND is the most unstable and mode 2 4ND has a very slightly higher damping. The rate of decrease of damping with decreasing mass flow rate is very similar for both modes as the stall line is approached.

Other Rotational Speeds

The results of unsteady calculations at 75 percent speed show trends that are nearly the same as those in Figures 5 to 9. Figure 12 shows the corresponding variation of aerodynamic
damping along the 75 percent speed line. Note that the aerodynamic damping is clearly negative at the lowest mass flow rate condition and the flutter point is obtained by interpolation.

The results of unsteady calculations at 100 percent speed for modes 1 and 2 are shown in Figures 13 and 14. The variations with nodal diameter are somewhat similar to those for 85 percent speed. However, the variations with backpressure (mass flow rate) show a very small slope in contrast to those seen in Figures 11 and 12. Based on the calculated aerodynamic damping and its small variation with backpressure, it is inferred that no flutter will occur at 100 percent speed.

The calculated flutter points at 85 and 75 percent speeds are plotted on the fan map along with the measured flutter boundary in Figure 15. It can be seen that the calculated flutter point is very close to the measured value at 85 percent speed with a difference of less than 0.5 percent in mass flow rate. As noted previously, the pressure ratio is slightly under-predicted and the difference from the measured data is 3 percent at the flutter point. For comparison, the flutter point calculated using TURBO is also plotted. Both calculations are very close to the test data; the present calculated result is slightly closer to the measurement in mass flow rate, but farther in pressure ratio as compared to the TURBO result.
At 75 percent speed, the difference between the present results and measurements is higher (about 4 percent) than at 85 percent speed. The TURBO result also shows a larger difference from measurement at 75 percent speed than at 85 percent speed.

At 100 percent speed, no flutter point is predicted by the HB analysis and the flutter point predicted by TURBO is beyond the stall line, meaning that the operating condition will not be reached.

Conclusions

Steady and unsteady computations have been carried out using two Propulsion Aeroelasticity codes. These aeroelastic analysis codes solve the Reynolds-Averaged Navier-Stokes equations with blade vibrations using a time-marching method (TURBO) and a harmonic balance method (HB). The configuration selected was an experimental fan that encountered flutter during wind tunnel testing. The computational results were summarized and compared with experimental data. Overall, both TURBO and HB results are in good agreement with experimental data. The steady results were compared on the performance map and showed good correlation with data. However, the steady HB results under-predict the pressure ratio slightly and the steady TURBO results are closer to the measurements. Also, the HB analysis provides solutions at slightly lower mass flow rates than were obtained with TURBO at part-speed conditions. The HB results under-predict the pressure ratio by about 3%. A detailed look at the flowfields predicted by HB and TURBO may provide additional information regarding the source of the differences. A numerical study with the smoothing parameters in the HB analysis showed that the calculated performance is not sensitive to these numerical parameters.

The flutter results from both HB and TURBO correlated very well with the experimental data. The least stable vibration mode matched experimental observations. The least stable nodal diameter pattern (interblade phase angle) also matched correctly with the experimental observations. The calculated flutter conditions agreed closely with the experimental data. A sensitivity study on the grid motion scaling parameter showed that the computational results were insensitive to variations in the selected value of this parameter, thus demonstrating the linearity of the results for the selected small amplitude of blade vibration.

Computations for the first two vibration modes showed that the first mode had a slightly lower aerodynamic damping, correctly correlating with the experimental observations. Computations at three rotational speeds (100, 85, and 75 percent) showed that flutter would be encountered at the part-speed conditions but not at the design condition, matching the experimental observations.

In the present study, very small amplitude vibrations were prescribed to calculate linearized unsteady aerodynamics for a conventional linear flutter analysis as a first step to understand the characteristics of the harmonic balance code and of the flutter of an experimental fan. Future work will investigate the non-linear amplitude-dependent effects using the same harmonic balance aeroelastic code and the time-domain TURBO code.

References

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