Effect of Steady State Coning Angle and Damping on Whirl Flutter Stability

Krishna Rao Venkata Kaza*
Stanford University, Stanford, Calif.

The main object of this investigation is to find the effect of the steady state coning angle and the damping at the flapping hinge of the blades on the whirl flutter stability boundary and thus to determine the role they can play in narrowing down the gap between theory and experiment. The governing equations of motion, with these two parameters included are derived by the classical Lagrangian approach using quasi-steady blade element theory for aerodynamic forces. A linearised analysis of these equations is applied to two of the wind tunnel models of the previous investigations. The results indicate that these parameters have a marked effect on stability boundary and they may even change the mode of flutter from backward whirl to forward whirl. The destabilising effect of the damping at the flapping hinge for a certain range of the model parameters is analogous to that of the internal damping in gyroscopic systems for example a disk on a rotating mass less shaft. In view of these results, one must be careful in drawing conclusions on the basis of the analytical results without considering the above mentioned secondary effects.

Nomenclature†

$[AN]$	= inertia matrix
$[AD]$, $[AS]$	= aerodynamic damping and stiffness matrices
c	= blade chord
$C_{\beta'}, C_\theta, C_\psi$	= damping coefficients in flapping, pitch and yaw
C_β	= effective damping coefficient of the rotor, $N/2\, C_{\beta'}$
dL_n, dD_n, dT_n, dH_n	= elemental lift, drag, thrust, and inplane force respectively
h	= distance between the pivot point and the propeller center
I_0, I_1, I_2, I_β, etc.	= moments of inertias
$K_\beta', K_\theta, K_\psi$	= spring constants in flapping, pitch and yaw directions
K_β	= effective spring constant of the rotor, $N/2\, K_\beta'$
l_h	= nondimensional distance between the pivot point and the propeller center
O, O_s, O_β	= origin of the inertia axis system, propeller center and blade hinge position on its axis
$\{q\}$	= vector of the generalized coordinates, $\beta_\theta, \beta_\psi, \theta$, and ψ
R	= radius of the rotor
r	= running coordinate along the blade axis
$[SD]$, $[SS]$	= structural damping and stiffness matrices
V	= freestream velocity
U_P, U_T	= Perpendicular and in plane components of the velocity of the flow
U	= resultant of U_P and U_T
X_s, Y_s, Z_s, O_s	= inertia axis system
$X_{gn}, Y_{gn}, Z_{gn}, O_s$	= axis system fixed to the nacelle
X_r, Y_r, Z_r, O	= axis system fixed to the nth blade rotating but not flapping
X_b, Y_b, Z_b, O_β	= axis system fixed to the blade both rotating and flapping with origin at the hinge center
α	= effective angle of attack of the blade element
α_0	= effective angle of attack of the blade element at the unperturbed condition
β_n	= flapping angle of the nth blade
$\beta_0, \beta_\theta, \beta_\psi$	= coning angle, pitch and yaw flap of the rotor tip path plane respectively
ϵ	= nondimensional hinge offset of the blades
$\zeta_\beta, \zeta_\theta, \zeta_\psi$	= critical damping ratios in flap, pitch and yaw motions respectively
θ, ψ	= pitch and yaw angles
λ	= $V/\Omega R$
λ_c	= $\lambda \cos\beta_0$
$\gamma_1, \gamma_2, \gamma_3, \gamma_4$	= whirl frequency ratios, ω_1/Ω, ω_2/Ω, etc.
$\gamma_\theta, \gamma_\psi$	= pitch and yaw frequency ratios
γ_β	= flapping frequency ratio of a nonrotating blade ω_β/Ω
$\gamma_{\beta 1}$	= flapping frequency ratio of a rotating blade $\omega_{\beta 1}/\Omega$
τ	= nondimensional time, Vt/R
χ	= nondimensional coordinate along the blade axis, r/R
Ω	= propeller rotational speed in radians per second
$\omega_1, \omega_2, \omega_3, \omega_4$	= whirl frequencies
$\omega_\beta, \omega_\theta, \omega_\psi$	= natural frequencies in flapping, pitch and yaw directions
$\omega_{\beta 1}$	= rotating blade flapping frequency, $(\Omega^2 \cos 2\beta_0 + \Omega^2 \cos\beta_0\, I_0/I_\beta + \omega_\beta^2)^{1/2}$
$\omega_{\beta 1d}$	= damped flapping frequency, $\omega_{\beta 1}(1 - \zeta_\beta^2)^{1/2}$
ν	= helical angle of the typical blade element

Introduction

STRUCTURAL and dynamic considerations in a promising VTOL aircraft concept led to flapped blade rotor systems. These rotors may have their hinges on the axis of rotation or at a distance from it. The effects of such hinges on whirl flutter were studied both analytically and experimentally in Ref. 1. The analysis with quasi-steady aerodynamic forces neglecting drag indicated that the flutter occurs in the backward whirl mode but the wind tunnel tests showed a forward whirl mode flutter at a lower speed. In Ref. 2, Reed and Bennett conducted wind tunnel flutter tests for two hinge offset positions of the blades. They also made a theoretical flutter analysis by introducing aerodynamic lag effects and compared these results in Ref. 3 with the experimental ones. For the 13% hinge offset case both theory and experiment predicted backward whirl. But the experimental model for 8% hinge offset case fluttered in forward whirl mode and this for-

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Index categories: VTOL Handling, Stability, and Control; Aeroelasticity and Hydroelasticity; Structural Dynamic Analysis.

*Scientist, National Aeronautical Laboratory, Bangalore, at present Research Assistant in the Department of Aeronautics and Astronautics, Stanford University, Stanford, California. Member AIAA.

† Primes indicate differentiation with respect to nondimensional time τ, and dots represent the differentiation with respect to time.

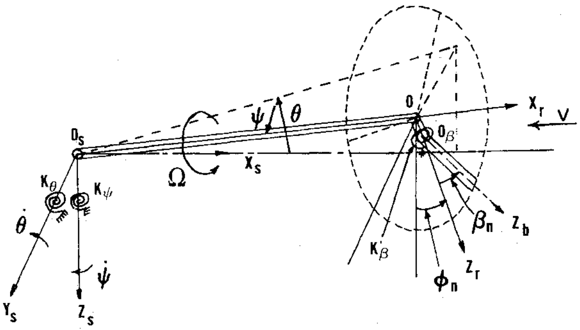

Fig. 1 Mathematical model of the system.

ward whirl flutter was not predicted by the analysis in spite of introducing lag effects as high as 45°. Reference 4 is an updated review on this problem.

Young and Lytwyn,[5] studied the influence of blade flapping restraint on whirl flutter of a fully articulated rotor. They also included the drag forces and anisotropy in pylon mounting. Their results indicate that there exists an optimum flapping frequency, $\omega_{\beta 1}/\Omega$ between 1.1 and 1.2 depending on the ratio of the pylon spring stiffness in pitch and yaw and that at the optimum tuning condition the nacelle stiffness requirements for neutral stability are at a minimum. Further, it was shown that forward whirl type instability is possible if the mounting is anisotropic or the flapping frequency ratio is less than the optimum value.

Both an analytical and experimental investigation was conducted for two hinge offset positions (10% and 13.6%) in Ref. 6. Both theory and experiment predicted backward whirl type instability for the case of isotropic mounting. Also it was shown that the hinge offset parameter can be clubbed into the flapping frequency parameter of Ref. 5 and there is a possibility for both backward and forward flutter when the pylon mounting is isotropic and the flapping frequency ratio is less than approximately 1.05. Moreover, the dominant mode is backward whirl in the range 1.05 to 0.95 and forward whirl in the range 0.95 and less of the flapping frequency ratio when the pylon mounting is isotropic. Thus, this study indicated the possibility of forward whirl even when the system is mounted isotropically. However, both this study and that of reference 5, are unable to defend the experimental observations of Refs. 1 and for 8% hinge offset case of Ref. 3.

While reviewing dynamic and aeroelastic problems of rotary-wing V/STOL, Loewy[7] pointed out that the effect of the coning angle may be the source of error for the discrepancy between theory and experiment. Another problem somewhat related to this is the stability of a disk on a rotating massless shaft with internal friction, wherein the internal damping has a destabilising effect at least in a certain domain of angular velocities. This was discussed in Ref. 8. In another related problem, Ref. 9 shows that the internal damping forces may strongly reduce the amplitudes of backward precession and they may even change the mode from backward to forward precession.

The above observations suggest that some sort of damping must be introduced at the flapping hinges of the blades. This may even be called as internal damping. Thus, this investigation is undertaken to study the effect of the steady state coning angle and the internal damping at the flapping hinges of the blades. The equations of motion for an idealized mathematical model shown in figure 1 were formulated using quasi-steady aerodynamic theory. A typical blade element and velocities experienced by it are shown in Fig. 2. A viscous type of damping at the flapping hinge is introduced in the rotating coordinate system and finally transfered to the inertial system along with stiffness and inertia parameters.

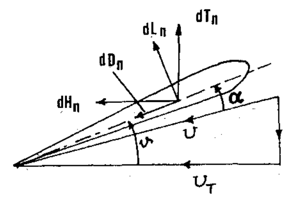

Fig. 2 A typical blade element.

The digital computer analysis of the above equations is applied to two models, 8% hinge offset one tested in Ref. 3 and a fully articulated version of the model tested in Ref. 6. The properties of these models are listed in the respective references. The results pertinent to the effects of the steady state coning angle and the viscous type damping at the flapping hinge are presented and discussed.

Mathematical Model

The discretised mathematical model consists of $N(N \geq 3)$ rigid blades, restrained elastically at the flapping hinges. A viscous type of damping at the flapping hinges of the blades is assumed. This damping might be due to friction at the flapping hinge in the case of flapped rigid blade rotors and might be due to hysteresis type damping of the blade material in the case of rotors with flexible blades and a rigid attachment to the hub. This rotor system along with the rigid pylon or nacelle is restrained elastically in pitch and yaw at a distance h from the rotor plane. A viscous type of damping is also assumed in pitch and yaw motions. This dynamic system has $N + 2$ degrees of freedom. The rotor also has a steady coning angle β_0, which might be due to the one resulted from the balance between centrifugal, gravity and aerodynamic forces at the unperturbed steady state position of the rotor in the case of flapped blade systems or due to the built in coning angle in the case of rotors with flexible blades attached rigidly to the hub. However, no attempt was made to explain the reasons for the presence of the coning angle and the damping but simply they are assumed in the system.

Quasi-steady blade element theory is used to generate the aerodynamic coefficients. If $N \geq 3$, the N degrees of freedom of the rotor may conveniently be reduced to two degrees of freedom through a sort of quasi-coordinates namely the pitch flap, β_θ and yaw flap, β_ψ. Finally, the system leads to a set of four simultaneous, ordinary, coupled second order differential equations with respect to a space fixed coordinate system.

Mathematical Analysis

Consider the system by locking the blade motions β_θ, β_ψ and without coning angle β_0. This has two degrees of freedom, one is pitch and the other is yaw. These are uncoupled in the absence of rotation Ω. When the rotation is introduced, these two motions are coupled, the higher frequency one is called as forward whirl and the lower frequency one as backward whirl. In the forward whirl mode a point on the hub traces a path in the direction of rotation of the rotor whereas in the backward whirl it traces in the direction opposite to the rotation.

When the pitch and yaw freedoms of the pylon are locked, the rotor blade has an undamped flapping frequency

$$\omega_{\beta 1} = \Omega(\cos 2\beta_0 + I_0/I_\beta \cos\beta_0 + \omega_\beta^2/\Omega^2)^{1/2}$$

and a damped frequency $\omega_{\beta 1d} = \omega_{\beta 1}(1 - \zeta_\beta^2)^{1/2}$ with respect to the rotating observer. In the absence of steady coning angle, β_0 and the damping ζ_β the flapping frequency, $\omega_{\beta 1}$, varies from Ω to infinity. The lower limit corresponds to fully articulated rotors without elastic restraint at the flapping hinge and the upper limit corresponds to

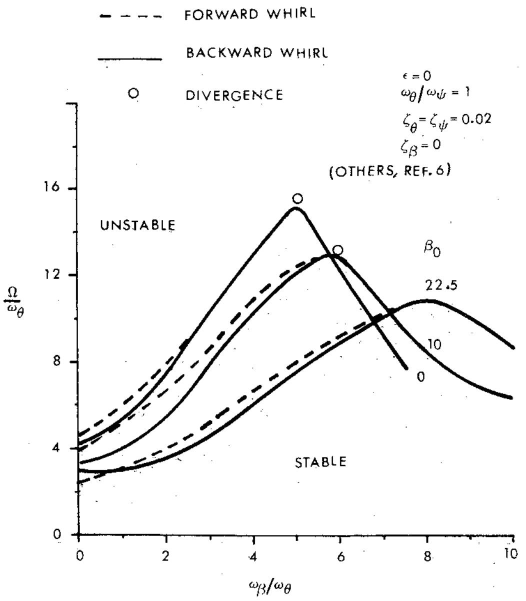

Fig. 3 Effect of flapping restraint and coning angle on flutter speed in the case of fully articulated rotor.

the rotors with rigid blades attached rigidly to the hub. But, in the presence of coning angle β_0 and the damping ζ_β the lower limit of $\omega_{\beta 1d}$ will be less than Ω depending on the value of β_0. When the blade motions are expressed with respect to a space fixed coordinate system, the flapping frequency $\omega_{\beta 1d}$ is split into two frequencies, $-\Omega \pm \omega_{\beta 1d}$ and $\Omega \pm \omega_{\beta 1d}$. The frequencies occur in pairs as expected from a gyroscopic system. The rotor tip path plane has two types of motions, depending on the parameters, with respect to a stationary observer. When $\Omega < \omega_{\beta 1d}$, the two positive frequencies are $-\Omega + \omega_{\beta 1d}$ and $\Omega + \omega_{\beta 1d}$, the first one corresponds to a backward whirl mode and the second one to a forward whirl mode of the rotor tip path plane with respect to a stationary observer. For case $\Omega > \omega_{\beta 1d}$, the two positive frequencies are $\Omega + \omega_{\beta 1d}$ and $\Omega - \omega_{\beta 1d}$ both representing the forward whirl modes of the tip path plane of the rotor. Thus the presence of the coning angle and the damping, ζ_β, might even change the rotor model characteristics, depending on the other parameters.

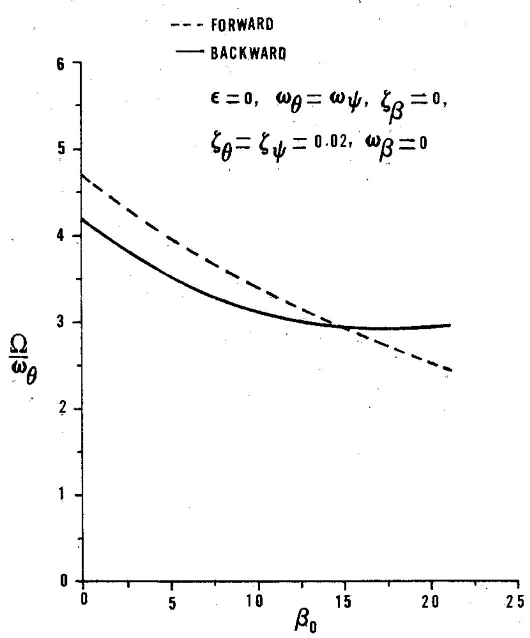

Fig. 4 Effect of coning on flutter speed in the case of a fully articulated rotor.

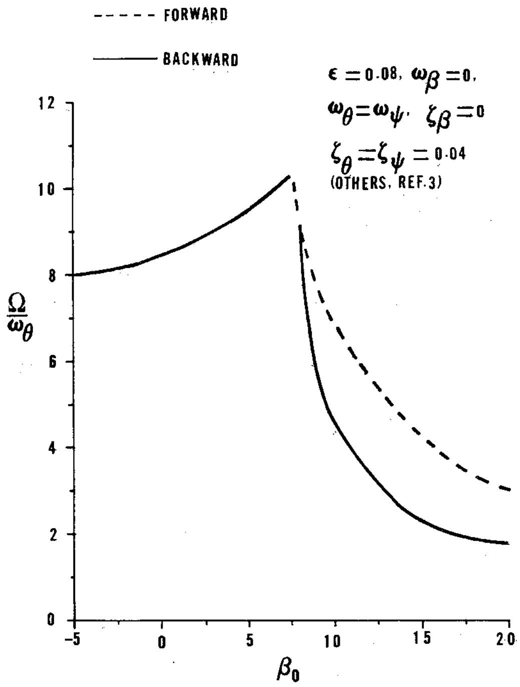

Fig. 5 Effect of coning angle on flutter speed in the case of a rotor with hinge offset blades.

When this model is placed in a uniform, incompressible inviscid flow at a speed V, the governing equations of motion can be derived in the same way as in Ref. 6 and can be written as

$$[AN]\{q''\} + [[SD] - [AD]]\{q'\} + [[SS] - [AS]]\{q\} = 0 \quad (1)$$

Assuming

$$\{q\} = \{q_0\} \exp^{(\mu_0 + i\gamma)\tau} = \{q_0\} e^{\mu\tau} \quad (2)$$

the Eq. (1) can be written as

$$[D]\{Q\} = -\frac{1}{\mu}\{Q\} \quad (3)$$

where

$$\left. \begin{aligned} [D] &= \begin{bmatrix} [D_0] & [D_1] \\ -[1] & [0] \end{bmatrix} \\ [D_1] &= [[SS] - [AS]]^{-1}[AN] \\ [D_0] &= [[SS] - [AS]]^{-1}[[SD] - [AD]] \\ \{Q\} &= \left\{ \begin{matrix} q_0 \\ \mu q_0 \end{matrix} \right\} \end{aligned} \right\} \quad (4)$$

This is a standard eigenvalue problem. The eigenvalues are four complex conjugate pairs, two pairs for the pylon and two pairs for the rotor. The general solution of the system will be obtained, if we consider the eigenvalues only with the positive imaginary parts. The associated eigenvectors will determine the direction of the whirl mode. When the flapping frequency ($\omega_{\beta 1d}$) of the blade with damping and the aerodynamics included is greater than Ω, there are two backward whirl modes with frequencies $\omega_1 = \gamma_1\Omega$ (pylon backward) and $\omega_3 = \gamma_3\Omega$ (rotor backward) and two forward whirl modes with frequencies $\omega_2 = \gamma_2\Omega$ (pylon forward) and $\omega_4 = \gamma_4\Omega$ (rotor forward). But when $\omega_{\beta 1d} < \Omega$, the backward rotor mode changes to forward whirl and thus there will be total three forward whirl modes and one backward whirl mode. The system is critical when the real part (μ_0) of any of the eigenvalues is zero. Keeping all the parameters, ϵ, β_0, ζ_β, ζ_θ, ζ_ψ, $\omega_\beta/\omega_\theta$, l_h, $\omega_\psi/\omega_\theta$, λ, α_0 and c as constants, Ω/ω_θ is varied until the system is unstable in one or possibly more than one modes and the stability boundaries are drawn.

Results and Discussion

The main attention is focused on the effect of β_0 and ζ_β on an isometrically mounted pylon system, which is the most critical case from the flutter point of view. $\omega_\beta/\omega_\theta$ is also included since the effect of β_0 and ζ_β depends on it. Two models, one fully articulated and another with hinge offset blades are selected. For the former one, the parameters of the fully articulated rotor version of the model tested in Ref. 6 and for the later one the parameters of the 8% hinge offset blade model tested in Ref. 3 are used.

The effect of the coning angle and the flapping restraint stiffness is shown in Fig. 3. Depending on the system parameters the instability either whirl flutter or static divergence might be possible in all the modes but for one, the rotor high frequency forward mode. However, which mode is predominant depends on the parameters β_0, $\omega_\beta/\omega_\theta$. The magnitude of the critical speed strongly depends on both β_0 and $\omega_\beta/\omega_\theta$. For each coning angle β_0 there exists an optimum flapping frequency ratio $\omega_\beta/\omega_\theta$ at which flutter speed is maximum and below that value there is a possibility of forward type whirl flutter even when the system pylon mounting is isotropic. At the optimum condition the flapping frequency of the rotor with respect to a rotating coordinate system is

$$\omega_{\beta 1} = \Omega\left(\cos 2\beta_0 + \left(\frac{\omega_\beta}{\omega_\theta}\right)^2 \left(\frac{\omega_\theta}{\Omega}\right)^2\right)^{1/2}$$

where Ω/ω_θ is the associated flutter speed. In the present case this value is approximately 1.05Ω for all values of β_0 considered and it is in agreement with those of Refs. 5 and 6. Hence, it appears that the optimum condition can be achieved either by changing β_0 or $\omega_\beta/\omega_\theta$. However an increase in β_0 causes a decrease in flutter speed when $\omega_{\beta 1}$ is less than 1.05Ω and an increase in flutter speed when $\omega_{\beta 1}$ is greater than 1.05Ω. In view of the fact both the flutter speed and its mode are very sensitive to $\omega_{\beta 1}$ in the range 1.00 and 1.1Ω which in turn depends on β_0, one must include β_0 in the analysis, even for the case of wind milling propeller where β_0 is minimum. In fact the models tested in Refs. 1, 3 and 6 have $\omega_{\beta 1}$ in the range 1.05 to 1.20Ω without β_0 effect. Figure 4 is a cross plot of Fig. 3, showing explicitly the effect of β_0 on both flutter speed and mode for the case of fully articulated rotor without elastic restraint spring at the flapping hinge and the pylon mounting is isotropic. Both pylon backward and forward modes flutter. Backward mode is predominant between 0 and 15° of the coning angle, β_0 and forward mode takes the lead when β_0 is more than 15°. The over-all effect is a decrease in flutter speed with an increase in β_0 and a transition in the predominant flutter mode from backward to forward.

Next the effect of coning angle for the case of a hinge-offset-blade rotor is considered. The parameters used are those of the 8% hinge offset model with windmilling blades of Ref. 3. The range of β_0 is extended to the negative side in order to account for the windmilling case. The flapping frequency of the rotor with respect to a rotating observer is given by

$$\omega_{\beta 1} = \Omega\sqrt{\cos 2\beta_0 + \cos\beta_0 I_0/I_\beta + \left(\frac{\omega_\beta}{\omega_\theta}\right)^2 \left(\frac{\omega_\theta}{\Omega}\right)^2}$$

Figure 5 shows the effect of the coning angle on the aforesaid model without elastic restraints at the blade hinges. An increase β_0 from $-5°$ to about 7° caused an increase in flutter speed; from there onwards it not only causes a decrease in flutter speed but also destabilizes the pylon forward mode in addition to the backward mode. However, the predominant flutter mode is backward except in the range 6° to 8°, which corresponds to $\omega_{\beta 1} \approx 1.05\Omega$. Studying closely Figs. 4 and 5, one can quickly conclude that the effects of β_0, ϵ and $\omega_\beta/\omega_\theta$ can be integrated as the effect of $\omega_{\beta 1}$ if β_0 is positive. But when the propeller is windmilling, β_0 is negative and hence, its effect should be considered explicitly. At the optimum condition both divergence and flutter occur almost at the same speed just like in the case of fully articulated rotor. This model exhibited a forward whirl instability in the wind tunnel. Both the analysis of Ref. 3 and the present one for $\beta_0 = 0$, predicted a backward whirl flutter and they are in good agreement. This model in the windmilling case might have a coning angle any where in between $-5°$ to $-2°$. In this range there is no possibility for forward flutter as expected from experiment. However, a forward whirl flutter at a much higher speed than observed in the wind tunnel

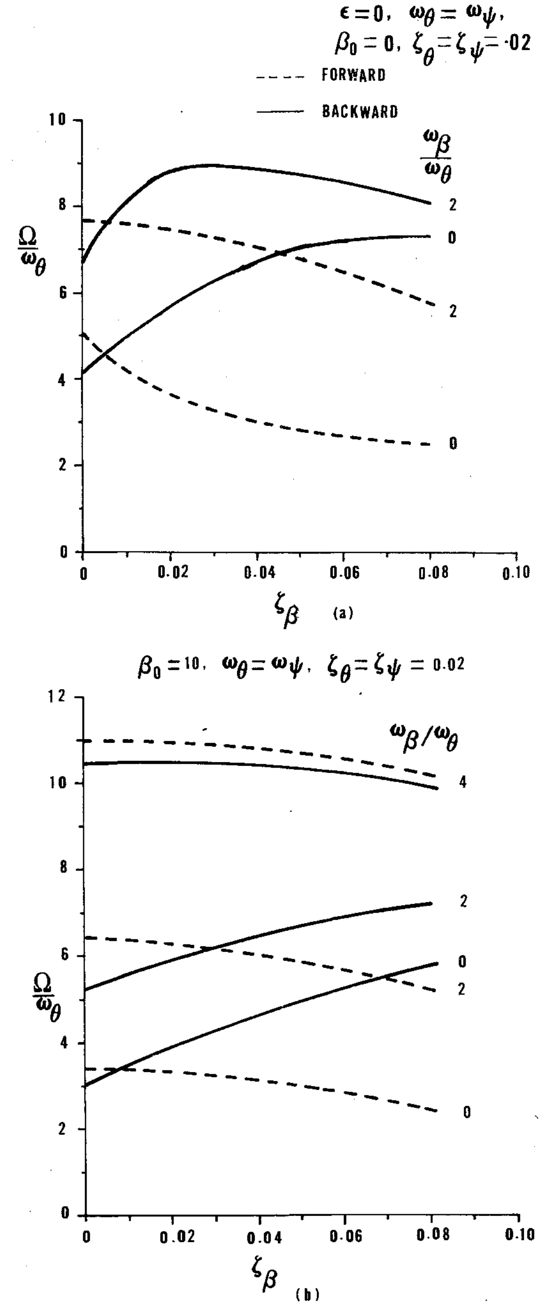

Fig. 6 Effect of flap damping on flutter speed in the case of a fully articulated rotor.

is possible when β_0 is in the vicinity of 7.5°. On further increasing the β_0 to 15°, the predominant flutter mode changed from forward to backward. The magnitude of this backward speed is in agreement with that of the forward speed noticed in the wind tunnel. Thus the effect of the coning angle might not be the reason for the disagreement between theory and experiment on this model. But it certainly has a marked influence on the stability, both divergence and flutter.

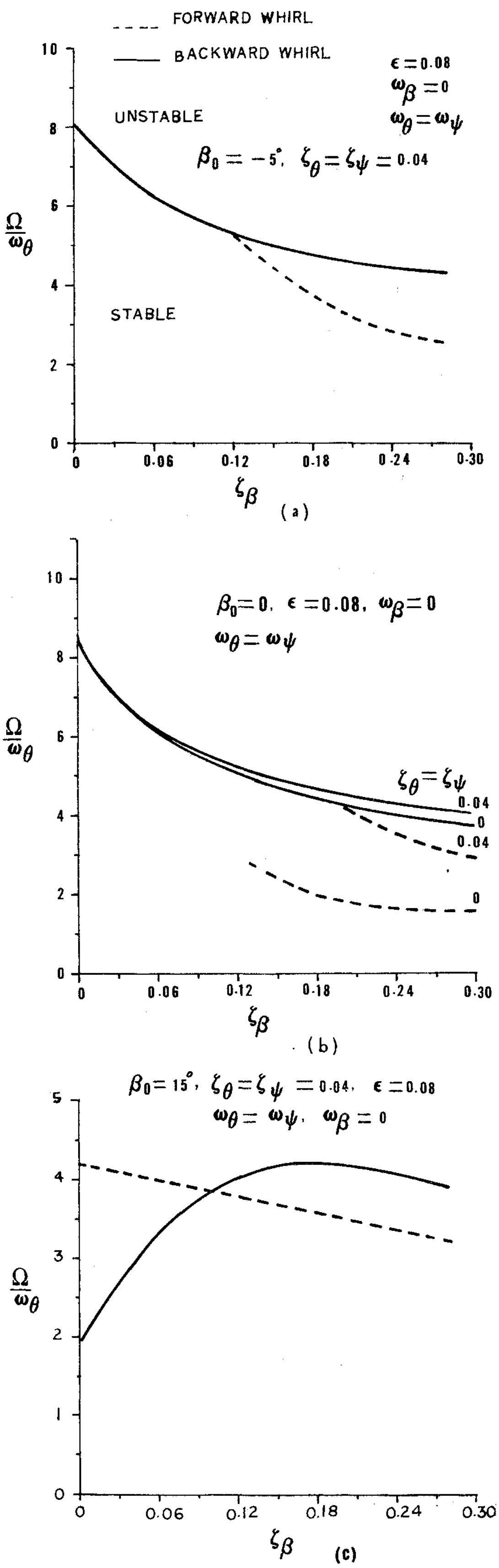

Fig. 7 Influence of flap and pylon damping on flutter speed in the case of a rotor with hinge offset blades.

The effect of ζ_β/ζ_θ on the flutter speed for the case of fully articulated rotors with $\omega_\beta/\omega_\theta$ as a parameter is shown in Figs. 6a and 6b for two values of β_0. Now, the damped flapping frequency of the rotor is given by $\omega_{\beta 1d} = \omega_{\beta 1}(1 - \zeta_\beta^2)^{1/2}$ with respect to the blade fixed axis system. It is observed that a small amount of damping, ζ_β (4% of critical) at the flapping hinge not only changes the predominant flutter mode from backward to forward but also reduces the flutter speed as it increases. This effect is analogous to that of the internal damping in the rotor as discussed in Ref. 8. The difference in flutter speed between the pylon backward and forward modes decreases with an increase in the elastic restraint parameter, $\omega_\beta/\omega_\theta$ and finally changing the predominant mode to backward whirl as expected.

Extending this study on the effect of flap damping to hinge offset bladed rotors, the parameters of the 8% hinge offset model are selected. Unfortunately the data on damping at the flapping hinge and the steady state coning angle are not available and hence the flutter boundary, ζ_β versus Ω/ω_θ, with β_0 as parameter is shown in Fig. 7. Pylon damping effect in the presence of flap damping is also included in Fig. 7b. As in the case of fully articulated rotors, the damping at the flapping hinge continuously reduces the backward flutter speed. It might even destabilize the forward whirl mode at a much lower speed than that of the backward whirl mode when ζ_β is more than 0.16. Also when $\zeta_\beta = 0$, the pylon damping has no effect on flutter speed. However, when ζ_β is more than 0.12, the pylon damping has a very little effect on backward whirl mode instability and has a marked stabilizing effect on forward whirl instability. Comparing the present value of the flutter speed, $\Omega/\omega_\theta = 8.4$ at $\beta_0 = 0$, $\zeta_\beta = 0$ with the corresponding one ($\Omega/\omega_\theta = 8.4$) evaluated from Fig. 7 of Ref. 3, they are in good agreement. However, the present theory does not account for aerodynamic lag effects whereas Ref. 3 does. But when $\zeta_\beta > 0.1$ and ζ_θ and ζ_ψ lie in the range 0 to 0.04, the predominant flutter mode is forward and the speed, Ω/ω_θ lies in the range 1.7 to 3.5. Hence, the forward flutter instability observed in the wind tunnel within the range $\Omega/\omega_\theta = 1.32$ and 2.4 (from Fig. 7, Ref. 3) might be due to the damping at the flapping hinge. Perhaps, this damping might even help to explain why some investigations show good correlation between theory and experiment and some do not.

Conclusions

From the analytical study made in this paper mainly on the influence of the steady state coning angle and the damping at the flapping hinges on flutter speed in both fully articulated rotors and rotors with hinge-offset blades the following conclusions are drawn:

1) As a general comment, the flutter speed and the mode are strongly dependent on the steady state coning angle and the damping at the flapping hinge.

2) Both the coning and damping might change the low frequency backward precession of the tip path plane of the rotor to forward precession depending on both the hinge offset of the blades and the flapping restraint spring constant.

3) A possibility for a predominant forward whirl flutter, even when the pylon mounting is isotropic, is established by consideration of either a large value of coning angle or the damping at the flapping hinge. The flutter speed is sensitive to β_0 and whereas both the flutter mode and the speed are sensitive to flap damping. It is believed that this new development might help to narrow down the gap

between the theory and the experiment noticed in the earlier investigations.

4) The pylon damping in the absence of flap damping does not have much influence on the flutter speed in the rotors with hinge offset blades unlike in prop-rotor with rigidly connected blades. However, in the presence of flap damping the pylon, damping has a stabilizing effect. The flap damping might reduce the backward flutter speed and may even change the predominant flutter speed from backward to forward.

5) The divergence speed of the system decreases with an increase in coning angle.

6) The coning angle β_0 is small for windmilling case of the propeller. However, in view of its marked effect on stability depending on other parameters the analytical results without considering it may lead to wrong conclusions.

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Active Flutter Control—An Adaptable Application to Wing/Store Flutter

William E. Triplett,* Hans-Peter F. Kappus,† and Robert J. Landy‡

McDonnell Aircraft Company, St. Louis, Mo.

An active flutter suppression system using an electronically compensated feedback signal can be adapted to stabilize widely differing wing/store flutter mechanisms. A generalized compensation network in the feedback loop modifying the signal of a single wing motion sensor is shown to provide sizable stability margins out to the aircraft performance limits for several airplane/store configurations. Flutter control can be maintained even though aileron actuators are rate saturated during flutter suppression. Usual levels of hydraulic system deadspace and freeplay do not impair the suppression system operation. Necessary hardware improvements include: increasing component reliabilities, increasing hydraulic flow rate, and improving actuator bandwidths.

Introduction

MILITARY aircraft are subjected to significant velocity restrictions when operating with external stores. A current fighter, for example, is restricted by flutter considerations to speeds below 550 KEAS (Knots Equivalent Air Speed) for several store configurations, even though the maximum velocity of the airplane is significantly greater. The ability to use the full velocity envelope while carrying external stores could enhance aircraft survivability with respect to ground fire and pursuing enemy interceptors. In addition the removal of flutter placards could improve bombing accuracy and effectiveness by lowering the minimum safe release altitude. Active flutter control, a concept using a feedback signal to actuate a control surface and eliminate the aeroelastic instability, is one means of removing flutter placards.

The feasibility of active control of wing/store flutter was previously established[1] on the basis of linear analyses of one wing/store configuration with a 370 gal fuel tank 90% full on the outboard store station. Those studies were the first steps in the design of an active flutter control system for wing/store flutter.

This paper reports on the results of an Air Force contract[2] which significantly expanded the scope of the previous studies to include the evaluation of 1) the ability of a single scheme to control flutter for several configurations; 2) the effects of system nonlinearities and equipment limitations; 3) the changes in aircraft stability resulting from the integration of the flutter suppression scheme into the existing aircraft flight control system; and 4) the system redundancy to satisfy flight safety requirements. Additional Air Force efforts in the area of flutter suppression are described in Refs. 3 and 4.

Four separate wing/store configurations, chosen to cover a broad spectrum of possible configurations, are presented. The general results obtained are believed to be applicable to most of the wing/store flutter mechanisms encountered on other low-to-moderate aspect ratio fighter/bombers.

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*Senior Technical Specialist, Structural Dynamics. Associate Fellow AIAA.

†Engineer, Structural Dynamics.

‡Group Engineer, Guidance and Control Mechanics.