THE USE OF ACTIVE CONTROLS TO AUGMENT ROTOR/FUSELAGE STABILITY

Friedrich K. Straub
Dynamics Engineer
Hughes Helicopters, Inc.
Culver City, California
and
William Warmbrodt
Aerospace Engineer
NASA Ames Research Center
Moffett Field, California

Abstract

The use of active blade pitch control to increase helicopter rotor/body damping is studied. Control is introduced through a conventional nonrotating swashplate. State variable feedback of rotor and body states is used. Feedback parameters include cyclic rotor flap and lead-lag states and body pitch and roll rotations. The use of position, rate, and acceleration feedback is studied for the various state variables. In particular, the influence of the closed loop feedback gain and phase on system stability is investigated. For the rotor/body configuration analyzed, rotor cyclic inplane motion ($\zeta_s, \dot{\zeta}_c, \ddot{\zeta}_s$) and body roll-rate and roll-acceleration feedback can considerably augment system damping levels and eliminate ground resonance instabilities. Scheduling of the feedback state, phase and gain with rotor rotation speed can be used to maximize the damping augmentation. This increase in lead-lag damping can even be accomplished without altering any of the system modal frequencies. Investigating various rotor design parameters (effective hinge offset, blade precone, blade flap stiffness) indicates that active control for augmenting rotor/body damping will be particularly powerful for hingeless and bearingless rotor hubs.

Notation

e	blade root hinge offset
h	offset of rotor hub from fuselage c.g.
k	blade index, k=1 .. N
K	feedback gain constant
NR	nominal rotor speed
q	vector of generalized coordinates
R_x, R_y	fuselage longitudinal, lateral motion
$\underline{u}$	vector of control inputs
$\underline{x}$	vector of state space variables
β, ζ	blade flap, lag motion
β_c, β_s	rotor cosine, sine cyclic flap degrees of freedom
β_p	precone
ε	order of magnitude
ζ_c, ζ_s	rotor cosine, sine cyclic lead-lag degrees of freedom
η	modal damping coefficient, % critical
η_ζ	blade lead-lag damping, % critical
θ_0	rotor collective pitch angle
θ_A	active control blade feathering angle
θ_{AC}, θ_{AS}	active control feathering inputs to nonrotating swashplate
θ_{Amax}	maximum active control blade feathering angle per degree of lead-lag motion
θ_G	blade aerodynamic pitch angle
θ_S	orientation of blade root springs at flat pitch
θ_x, θ_y	fuselage roll, pitch motion
σ	real part of eigenvalue, i.e., modal damping, rad/sec
ϕ	feedback phase
ψ	nondimensional time parameter, rotor azimuth
ω	imaginary part of eigenvalue, i.e., modal frequency, rad/sec
Ω	rotor speed
$(\bar{\ })$	nondimensional quantity
$(\ )_0$	steady-state equilibrium value
$(\dot{\ })$	$d(\ )/d\psi$

Introduction

Aeromechanical rotor/fuselage instabilities can occur for articulated, hingeless, and bearingless rotors. Due to the wide range of helicopter operating conditions, payload configurations, and flight regimes, it can be very difficult for the helicopter designer to tailor all rotor and fuselage body frequencies to avoid modal frequency coalescences or resonances for all conditions. Consequently the rotor designer often has to resort to including mechanical or elastomeric blade dampers to improve system aeromechanical stability. This results in increased cost, complexity, maintenance, weight, and hub drag for the rotor system. In addition, soft inplane hingeless rotor configurations without damping augmentation have inherently low rotor blade structural damping. These systems have not been used extensively in the helicopter industry, in part, because of poor aeroelastic stability characteristics. Consequently a means to increase aeromechanical stability in a reliable manner could significantly improve the operational characteristics of this rotor hub design.

The use of active blade pitch control has been successfully demonstrated for vibration reduction (Ref. 1). A significant amount of analytical and experimental research has been performed to develop this technology for both N per rev and gust-induced vibration control. The technology is now available for advanced applications. Showing analytically the feasibility of using active control to augment rotor damping would represent a further step towards an advanced, fully integrated, multimode helicopter rotor control system.

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Only a limited number of studies have been performed investigating active blade pitch feathering to affect helicopter rotor/fuselage stability (Refs. 2-4). References 3 and 4 use active control implementation approaches which do not utilize a conventional swashplate and consequently have limited application to current rotorcraft control systems. In Reference 2, Young, Bailey and Hirschbein investigated the aeromechanical stability of a hingeless helicopter rotor and the application of feedback control to augment system damping through conventional swashplate control. The use of active control was studied by implementing fuselage roll position and roll rate feedback into a set of swashplate actuators in order to generate longitudinal and lateral cyclic blade pitch commands. Feedback of fuselage pitching motion was not pursued since the unstable mode of the system being studied had only a relatively small pitch component. Their results showed that feedback of roll position and roll rate could stabilize the unstable roll mode both on the ground and in hover. This study extends these results by also investigating the influence of body acceleration feedback, various rotor state feedback systems, and the influence of control feedback gain and phasing.

The purpose of the present study is to evaluate the potential use of active blade pitch control to increase rotor/body system damping. This is accomplished by using state variable feedback with the appropriate gain and phase. Such an application could possibly eliminate the need for mechanical lead-lag dampers. In addition, a marginally stable rotorcraft configuration could be further stabilized by increasing the rotor damping levels and thereby expanding the rotorcraft's operating envelope. The detailed objectives of the present study are:

1) Investigate the influence of state variable feedback on system damping, including body acceleration and rotor state feedback systems which have not been previously considered.

2) Use of a systematic approach to study the effects of the feedback gain and the weighting between the time-dependent cyclic blade pitch controls on system stability levels.

3) Investigate the use of control scheduling with rotor speed to ensure stability at all rotation rates.

4) Assess the influence of rotor design parameters (hinge offset, blade precone, and blade flapping stiffness) on the performance of feedback control.

To carry out this investigation a new mathematical model was developed to analyze coupled rotor/fuselage dynamics. This model, which was used for the numerical simulations of active feedback control, is discussed in the next section. The manner in which active blade pitch feathering is introduced is also described. To validate the governing equations of motion, frequency and damping predictions without active controls are correlated with experimental data (Ref. 5). Numerical results are then presented based on state variable feedback control. These active control simulations are intended to show the effect of various feedback variables on system stability and response and provide a systematic approach in choosing the feedback parameters. Key rotor design parameters also investigated as to their influence on rotor behavior with feedback control.

Analytical Model

The details of the mathematical model used in this study are presented in this section. The approach used in modelling state variable feedback through a conventional swashplate is discussed and the method of solution for the governing equations of motion is reviewed. More details on the mathematical model, the control laws, or the solution method may be found in Ref. 6.

Rotor/Fuselage Model

A brief description of the mathematical model developed for this study follows. The math model is similar to the models used in Refs. 7 and 8. The helicopter body is represented as a rigid fuselage having pitch and roll rotations (θ_y,θ_x) about the center of mass and longitudinal and lateral translations (R_x,R_y) of the center of mass, see Fig. 1. The fuselage physical properties required for modelling are its mass, pitch and roll inertias, and effective landing gear stiffnesses and damping in rotation and translation. The rotor hub, having three or more blades, is located a distance h directly above the fuselage mass center. The blades are assumed to be rigid and rotate against spring and damper restraints about coincident flap and lead-lag hinges offset from the axis of rotation, see Fig. 2. The orientation of the hinges can be different from the aerodynamic pitch angle, thus allowing modelling of variable structural flap-lag coupling with blade feathering inboard or outboard of the hinges. Blade precone is included. This parameter is particularly important in this study since it directly contributes to the inplane Coriolis forces which augment blade lag damping. In deriving the governing equations, rotor rotation speed is assumed constant. The aerodynamic forces are based on two-dimensional quasi-steady theory. Apparent mass, compressibility and stall are neglected. No low frequency unsteady aerodynamic model (dynamic inflow) is used. The pitch control input is composed of two parts: the time-independent

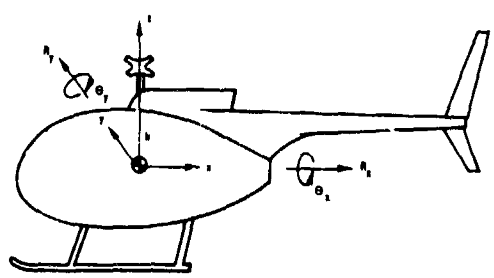


Fig. 1 Fuselage model.

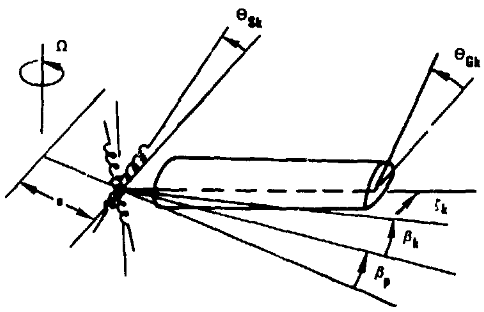


Fig. 2 Rotor blade model

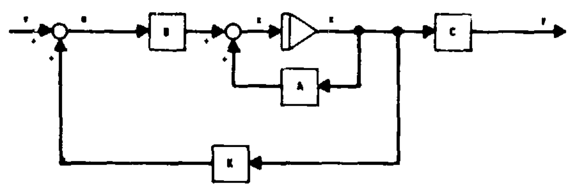


Fig. 3 State variable feedback control system.

collective pitch, identical for all blades; and the time-varying "active" pitch. The active control pitch input appears as aerodynamic forcing expressions in the equations of motion.

In deriving the equations of motion for this model a large number of small terms appear. Many of these were neglected systematically by use of an appropriate ordering scheme based on the magnitude of blade slopes (typically $0.1 < \varepsilon < 0.2$). The various parameters in the equations were assigned orders of magnitude. Fuselage motions are assumed to be of order $O(\varepsilon^{1.5})$. The active control portion of the blade pitch angle is assumed to be of order $O(\varepsilon^{1.5})$, based on experience with the higher harmonic control inputs of Ref. 1. In applying the ordering scheme it is assumed that terms of order $O(\varepsilon^2)$ are negligible in comparison with unity. In addition, all terms that contain products of the fuselage degrees of freedom are neglected.

Control Law Development

In implementing the active control, it is assumed that feedback is applied through a conventional swashplate, i.e., control motions are generated by actuators in the fixed system. The active pitch input to the k'th blade can then be expressed as

$$\theta_{Ak} = \theta_{AC(\psi)} \cos\psi_k + \theta_{AS(\psi)} \sin\psi_k \qquad (1)$$

where the control inputs θ_{AC} and θ_{AS} are to be determined functions of the nondimensional time parameter ψ. A block diagram for the state variable feedback system used in the current study is shown in Fig. 3. The system equations are

$$\dot{\underset{\sim}{x}} = [A]\underset{\sim}{x} + [B]\underset{\sim}{u} \qquad (2)$$

$$\underset{\sim}{y} = [C]\underset{\sim}{x}$$

where

$$\underset{\sim}{u}^T = [K\cos\phi, K\sin\phi]\, d^n q_i/d\psi^n \quad n=0,1,2 \qquad (3)$$

The vector $\underset{\sim}{x}$ denotes the state space variables, $\underset{\sim}{y}$ is the vector of output measurements, and $\underset{\sim}{u}$ is the vector of control inputs. K is the control gain. The angle ϕ, herein termed feedback phase, defines the relative weighting between the time dependent cyclic controls. In other words, ϕ defines the swashplate azimuthal position where the gain that individual blades experience has its maximum value. This point is 90 degrees from the axis of no feathering about which the swashplate oscillates; see Fig. 4. The quantity q_i is one of the system degrees of freedom. In this analysis state feedback is directly introduced into the second order equations, thus $\underset{\sim}{u}$ is proportional to q_i rather than x_i. State feedback can then be thought of as an additional contribution to the system stiffness, damping, and/or mass matrix, for n=0,1,2 respectively.

Solution Method

The nonlinear periodic equations of motion can be solved directly in the time domain. However, for parametric stability studies an eigenvalue analysis is much more convenient. The equations are therefore linearized. The steady-state, nonlinear equilibrium position is obtained assuming

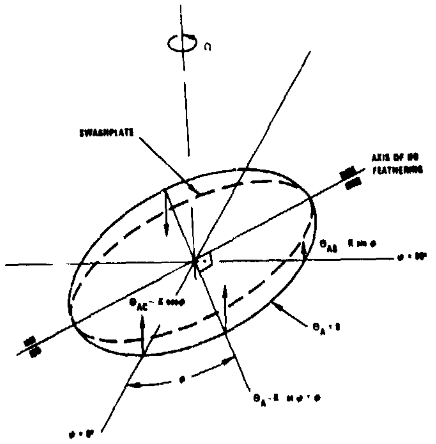


Fig. 4 Control implementation through swashplate.

that the fuselage degrees of freedom and the active blade pitch are zero. In the case of hover, the blade equilibrium position is independent of time and is obtained iteratively using the Newton-Raphson technique.

The linearized periodic coefficient perturbation equations are converted into a constant coefficient system using a multiblade or Fourier coordinate transformation. This is possible under the assumptions that all blades are identical and that the active pitch input is generated through a conventional swashplate with three "active" actuators in the fixed system. With the rotor being in a hover condition, only the first cyclic blade motions in flap and lead-lag couple with the fuselage motions. The collective and reactionless blade equations are not needed. The final set of equations is

$$[M(q_0)]\ddot{q} + [C(q_0)]\dot{q} + [K(q_0)]q + [F(q_0)]u = 0$$

$$q^T = [\zeta_c, \beta_c, \zeta_s, \beta_s, \theta_x, \theta_y, R_x, R_y], \qquad (4)$$

$$u^T = [\theta_{AC}, \theta_{AS}]$$

Stability of the ground resonance problem in the fixed system is then evaluated by transforming the equations into first order form and performing an eigenvalue analysis. This form of the governing equations is also used to compute the time history response and frequency response of the system

$$\dot{x} = \begin{bmatrix} 0 & [I] \\ -[M]^{-1}[K] & -[M]^{-1}[C] \end{bmatrix} x + \begin{bmatrix} 0 \\ -[M]^{-1}[F] \end{bmatrix} u \qquad (5)$$

$$x^T = [q^T, \dot{q}^T]$$

Discussion of Governing Equations

Development of control laws and their evaluation for this study were made with the objective to increase rotor/body system damping while observing constraints on state and control variables in order to avoid adversely affecting overall system performance.

The basic mechanism for influencing lead-lag dynamics is provided through aerodynamic, Coriolis, and kinematic coupling with blade flapping and feathering inputs. For elastic blades, elastic flap-lag coupling would also play a major role Fuselage dynamics are coupled with blade flapping through aerodynamic and gyroscopic forces. Looking closely at the governing equations of motion used in this study, the active control pitch input appears as aerodynamic forcing expressions in all equations. The values in the blade lag and fuselage translation equations are one order of magnitude smaller than in the flap equations and in the fuselage pitch and roll equations. From these equations it therefore seems that two primary mechanisms exist to stabilize ground resonance. First, the fuselage pitch and roll motion can be controlled through the pitch and roll moments arising from flapping. The magnitude of each is directly related to the blade root hinge offset and flap spring stiffness. The second mechanism is lead-lag damping augmentation through Coriolis coupling with blade flap motion. This requires presence of either steady blade coning deflection or built-in precone.

From the above it is clear that the aeromechanical stability of a helicopter is a multi-input/multi-output control problem. In this study, it is assumed that all the states are known. However, only one state at a time is used for feedback. Combined feedback of two or more state variables was not considered. Likewise no attempt was made at this stage to use multivariable optimal control techniques to maximize the damping augmentation since gaining a basic understanding of the problem was thought to be more important. Also, in the development of this simulation model it is recognized that unsteady aerodynamic effects (dynamic inflow) can at times have a considerable effect on the blade flap motion (Ref. 9). Since flapping plays an important role in stabilizing ground resonance perhaps the conclusions of the present study would be changed to some degree. In particular for high flap stiffness rotors unsteady aerodynamics should be included in a more refined model.

Validation of Analytical Model

Prior to presenting closed loop active control results, the fidelity of the math model to adequately predict aeroelastic stability characteristics is studied using experimental data from Ref. 5 (configuration 1). Rotor and body properties are listed in Table 1. No active controls are utilized for these results. The corresponding predictions from Ref. 10 using the E-927 analysis are also shown.

Table 1. Rotor/body properties

Number of blades	3
Radius, cm	81.1
Chord, cm	4.19
Nominal rotor speed, rpm	720
Hinge Offset, cm	8.51
Precone, deg	0
Blade airfoil	NACA 23012
Lift curve slope	2π
Profile drag coefficient	0.0079
Lock number	7.73
Solidity ratio	0.0494
Blade mass, Kg	0.209
Blade first mass moment, Kg cm	3.887
Blade second mass moment, Kg cm^2	173
Nonrotating flap frequency, Hz	3.13
Nonrotating lead-lag frequency, Hz	6.70
Damping in lead-lag, % critical	0.52
Height of rotor hub above gimbal, cm	24.1
Fuselage mass in pitch, Kg	22.60
Fuselage mass in roll, Kg	19.06
Fuselage inertia in pitch, Kg cm^2	6330
Fuselage inertia in roll, Kg cm^2	1830

Table 1. Rotor/body properties (cont)

Pitch frequency, Hz	1.59; 2*
Roll frequency, Hz	3.9; 4*
Damping in pitch, % critical	3.20
Damping in roll, % critical	0.929

*Body Frequencies used in study of active control.

Comparison between modal frequencies as a function of rotor speed at flat pitch show very good agreement in Fig. 5. In particular, regressing inplane mode resonances with the body pitch mode (550 rpm) and the body roll mode (765 rpm) are accurately predicted. In Fig. 6, the corresponding damping levels for the lead-lag regressing mode, body pitch mode, and body roll mode are presented. Lead-lag damping (Fig. 6a) shows relative good agreement. The corresponding pitch (Fig. 6b) and roll (Fig. 6c) damping levels are generally higher than the experimental data but in the same range as E-927 predictions.

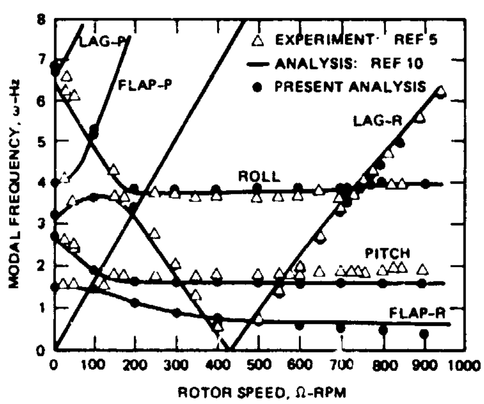


Fig. 5 Modal frequencies versus rotor speed at flat pitch.

Lead-lag damping for nine degrees of collective pitch as a function of rotor speed is shown in Fig. 7. Agreement of the present analysis (solid symbols) with experimental damping values is very good up to 650 rpm. This includes the crossover of the regressing lag mode with the body pitch mode. For higher rotor speeds (including crossover with the body roll mode), only general trends in damping are captured. This is certainly a shortcoming but it is felt that a better knowledge and/or adjustment of the body roll frequency and damping would improve these predicted results considerably. Furthermore, Fig. 8 shows that the current analysis predicts damping trends as a function of collective pitch angle quite well for the regressing lag mode (Fig. 8a) and body pitch motion (Fig. 8b) at 650 rpm. Only the trend with collective pitch is captured for the body roll mode (Fig. 8c).

From the correlation presented, it is clear that in certain cases considerable differences exist between analytical predictions and experimental results. However, the simple analytical model used for the present investigation can be expected to predict the frequency crossovers and

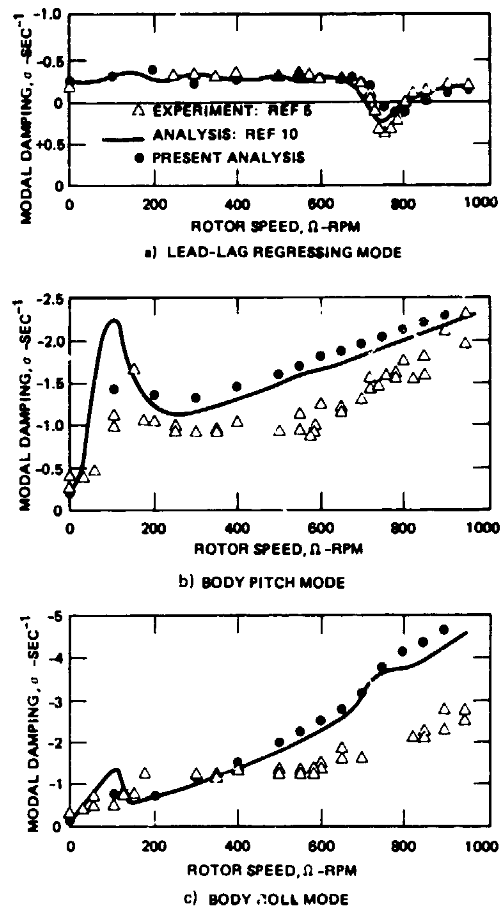


Fig. 6 Damping versus rotor speed at flat pitch.

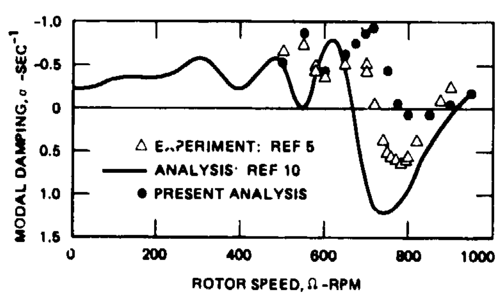


Fig. 7 Lead-lag regressing mode damping versus rotor speed, $\theta_0 = 9$ deg.

damping trends adequately for the rotor/body systems studied here. It is concluded that the present model is adequate to investigate the effects of active controls on rotor/body aeromechanical stability.

Active Control Results

All the active control simulations in this study were performed for the same rotor/body configuration used in the previous section (configuration 1 of Ref. 5). This is a soft inplane hingeless

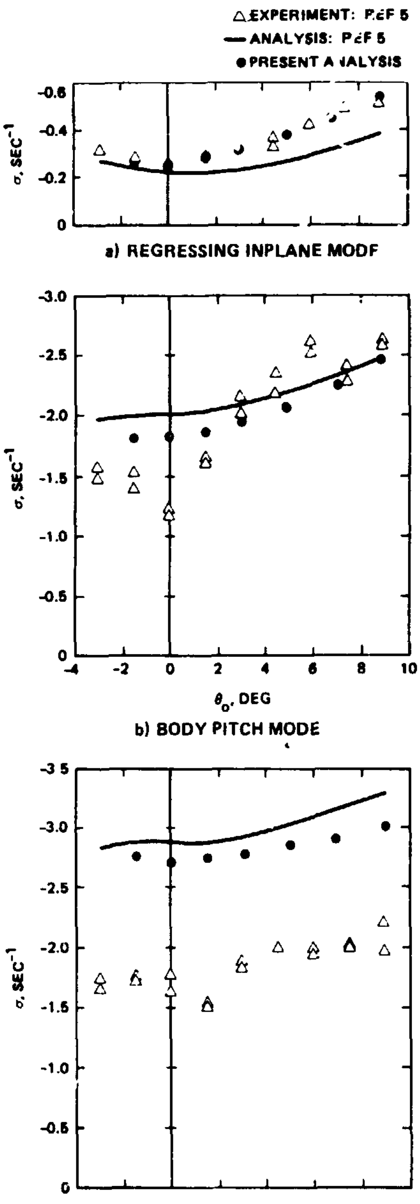


Fig. 8 Modal damping as a function of blade pitch angle, 650 rpm.

rotor supported on a gimbal with pitch and roll degrees of freedom. The baseline system parameters are listed in Table 1. Nominal rotor speed for this configuration is 720 rpm. All cases are for flat pitch operation. However, this rotor has a cambered airfoil which gives a small positive thrust at zero collective. The modal frequencies and damping for the baseline case without feedback are shown in Figs. 5 and 6. For this configuration, the regressing lag mode experiences an instability at the frequency crossover with the body roll mode at 765 rpm.

First, the effect of individual feedback state variables on system stability is explored by varying feedback gain and phase systematically. These studies are performed at the point of minimum stability, i.e., at the coalescence rotor speed of 765 rpm. Plots of system damping and frequency versus feedback phase are used to select candidate feedback states and define feedback phase angles for maximum damping augmentation. Next, these candidate feedback states are investigated in more depth by considering a range of rotor speeds to simulate rotor run up. Results show the sensitivity of the system dynamic behavior with respect to changes in feedback gain and phase. Following this, the effect of rotor configuration on active control damping augmentation is studied. To this end the blade root hinge offset, blade precone, and flap spring stiffness, which are key parameters in terms of control effectiveness, are varied to cover a range of values typical for articulated, hingeless, and bearingless rotors. Lastly, the rotor/body response behavior is considered. This provides a quantitative measure of the active blade feathering amplitudes required to achieve adequate stability margins. It also gives a better understanding of the participation of the individual degrees of freedom in the unstable or lightly damped rotor/body mode.

State Feedback Studies

Figures 9 through 13 show the effect of feedback on system damping (the real part of the eigenvalue) and frequencies (the imaginary part of the eigenvalue). Gain values of K=1, 2, and 3 and a complete range of feedback phase angles, $0<\phi<360$, are considered. Also shown are the damping and frequency of the baseline system without active controls, i.e., K=0. The rotor speed is 765 rpm which corresponds to coalescence of the body roll mode and regressing lead-lag mode frequencies, Figure 5.

Figure 9 shows the influence of cosine cyclic lead-lag position feedback (ζ_c) on system dynamics. The baseline (K=0) lead-lag regressing mode is unstable for this operating condition. Depending on the feedback phase, variations in feedback gain

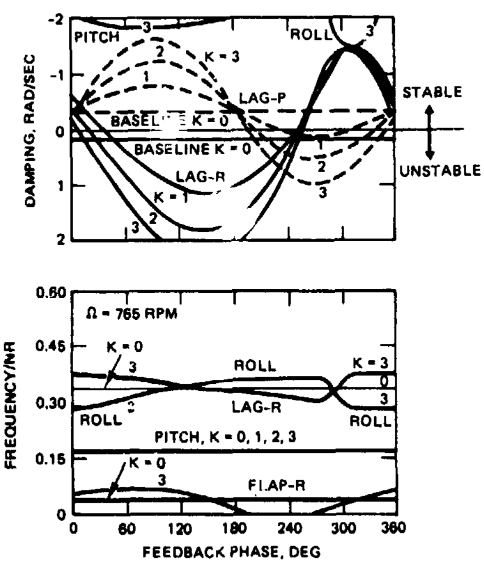


Fig. 9 Modal damping and frequencies versus feedback phase with cosine cyclic lag feedback, ζ_c.

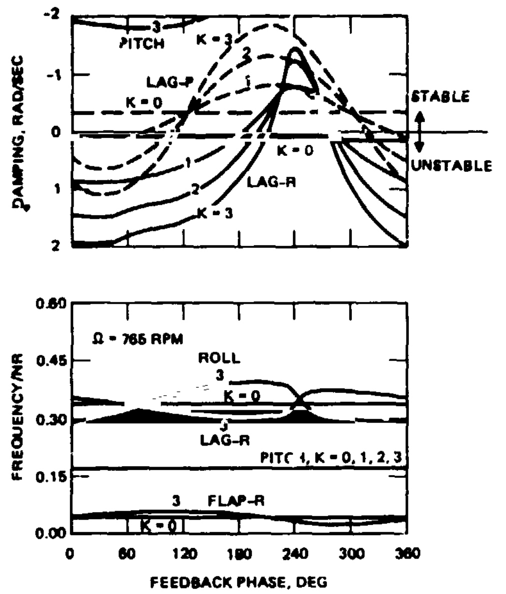


Fig. 10 Modal damping and frequencies versus feedback phase with sine cyclic lag feedback, ζ_s.

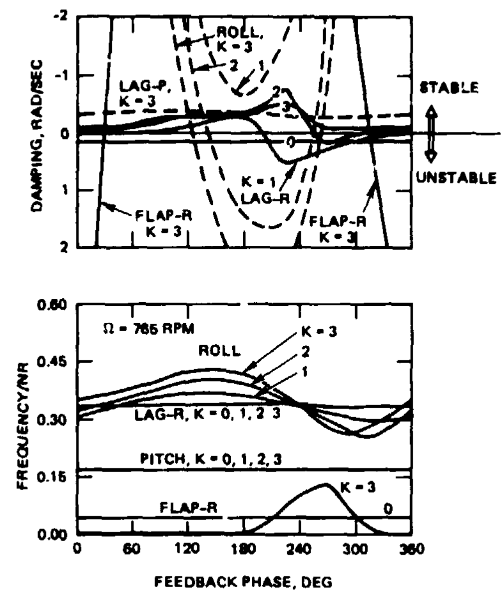
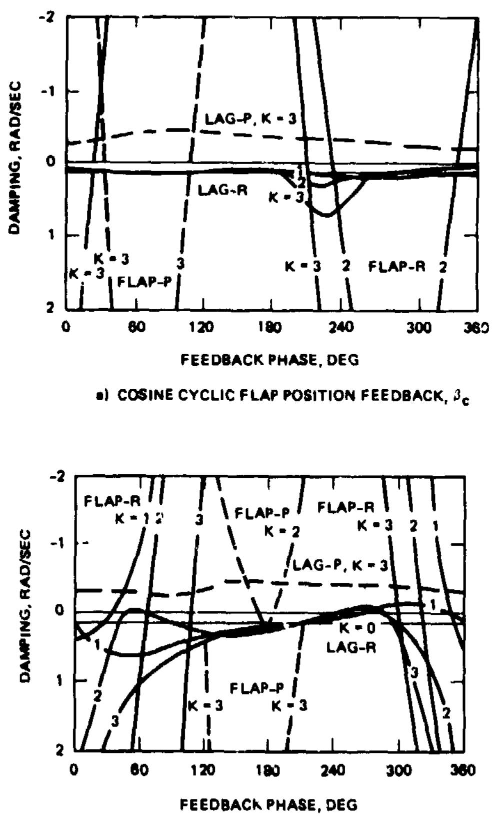


Fig. 12 Modal damping and frequencies versus feedback phase with roll feedback, θ_x.



a) COSINE CYCLIC FLAP POSITION FEEDBACK, β_c

b) SINE CYCLIC FLAP POSITION FEEDBACK, β_s

Fig. 11 Modal damping versus feedback phase with cyclic flap position feedback (Ω = 765 rpm).

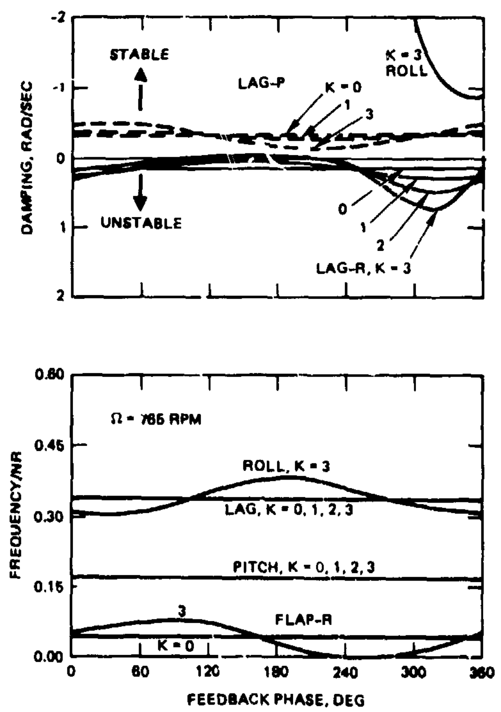


Fig. 13 Modal damping and frequencies versus feedback phase with roll rate feedback, $\dot{\theta}_x$.

can increase damping and stabilize this mode ($250<\phi<30$ deg) or decrease damping and further destabilize it ($30<\phi<250$ deg). The opposite behavior is observed for the progressing lead-lag mode which is stable for K=0. It's damping is decreased for feedback phase between 180 and 360 degrees and increased for feedback phase between 0 and 180 degrees. This makes the progressing lead-lag mode the least damped mode for feedback phase between 250 and 360 degrees and, depending on the gain value, can result in system instability. Therefore there exists only a small range of feedback phase angles, around $\phi=0$, where the rotor/body system could be stabilized through active control. Feedback of ζ_c is therefore not considered to be a suitable choice. Similar findings were made for $\ddot{\zeta}_c$ and $\dot{\zeta}_s$ feedback.

Figure 10 shows the influence of sine cyclic lead-lag feedback (ζ_s) on system dynamics. Again, depending on the feedback phase, the damping of the regressing and progressing lead-lag modes can be increased or decreased from the baseline values. This time, however, damping for both modes is increased over approximately the same range of feedback phase values. As a result the system can be stabilized for feedback phase between 220 and 270 degrees. The maximum increase in damping occurs at approximately 240 degrees feedback phase and is directly proportional to the feedback gain. It is seen that ζ_s feedback control changes the roll and regressing lag mode frequencies only to a limited extent. For clarity only the frequencies for the baseline system (K=0) and for K=3, which exhibits the largest frequency changes, are shown. Furthermore, at feedback phase angles of approximately 70 and 240 degrees these modal frequencies remain unchanged for all values of feedback gain. This clearly shows that the improved system stability at $\phi=240$ degrees is a direct result of increasing the inherent regressing lag mode damping and not due to a change in coalescence rotor speed. Inspection of the roll mode and its modal damping indicates that the source of the increased lag damping is a reduction in roll mode damping. However, the roll mode is well damped in the baseline system and this exchange of damping is therefore beneficial for overall system stability. Feedback of ζ_s is thus considered to be a suitable candidate for stability augmentation. Similar findings were made for feedback of $\dot{\zeta}_c$ at $\phi\cong60$ degrees and feedback of $\ddot{\zeta}_s$ at $\phi\cong60$ degrees.

The influence of flap feedback states on system damping is shown in Fig. 11 for β_c and β_s feedback. While leading to large changes in damping of the regressing and progressing flap modes, the damping of the regressing lag mode is not improved. This same result was found for all other flap feedback states ($\dot{\beta}_c,\ddot{\beta}_c,\dot{\beta}_s,\ddot{\beta}_s$). Consequently the flap state variables are not further considered for rotor/body damping augmentation.

The effect of roll attitude and rate feedback is shown in Figs. 12 and 13. Again, damping of the regressing and progressing lead-lag mode is increased or decreased depending on the feedback phase. In addition, roll attitude feedback (Fig. 12) can lead to considerable instability of the roll mode and regressing flap mode at certain values of feedback phase. Roll attitude feedback could be used to stabilize the system for feedback phase between 45 and 120 degrees. However, the frequency plot shows that in this range the roll mode frequency is raised considerably. Any gains in system damping at 765 rpm would thus largely be due to a shift of the coalescence rotor speed rather than an increase in inherent regressing lag mode damping. Feedback of roll attitude is therefore not further considered. Feedback of roll rate (Fig. 13) at a feedback phase between 90 and 120 degrees adds damping to the regressing lag mode while keeping the regressing lag and roll mode frequencies almost unchanged. However, the feedback gain would have to be increased to provide adequate system stability margins. On the other hand, too large a gain can result in the same roll and regressing flap mode instabilities found with roll attitude feedback. Similarly, it was found that roll acceleration feedback at a feedback phase between 240 and 270 degrees can stabilize the regressing inplane mode. Thus, both roll rate and acceleration seem to be suitable feedback states and will be studied in more depth.

Feedback of pitch attitude was found to have very little effect on damping of the regressing lead-lag mode. At the same time, damping of the pitch mode and regressing flap mode can be lowered to a point of considerable instability. Results from pitch rate and acceleration feedback also show no change in regressing lag mode damping and, for larger gains, can be expected to exhibit similar pitch mode instabilities as for feedback of pitch attitude. Pitch feedback is therefore not considered a suitable choice for eliminating the regressing lag/roll mode instability considered here.

Summarizing these results for the unstable resonant operating condition of 765 rpm, there exist rotor and fuselage states which can be used in a state feedback control approach to stabilize the entire system. The states most suitable for stability augmentation are $\dot{\zeta}_c, \zeta_s, \ddot{\zeta}_s, \dot{\theta}_x$ and $\ddot{\theta}_x$.

Based on the above results, feedback of ζ_s and $\dot{\theta}_x$ are further evaluated in Figs. 14 and 15 by considering rotor rpm sweeps and varying the gain K while keeping the feedback phase ϕ constant. The value of ϕ was chosen as the phase yielding the greatest stability augmentation at 765 rpm. The feedback results for ζ_s and $\dot{\theta}_x$ are representative of the damping augmentation results that can be obtained with $\dot{\zeta}_c$ and $\ddot{\zeta}_s$ and with $\ddot{\theta}_x$, respectively. In selecting the gains K, an attempt was made to obtain approximately the same range of regressing lag mode damping values for both feedback states. In both cases the system can be stabilized at all previously critical rotor speeds, although to a lesser degree with roll rate feedback. For clarity only the regressing lag mode damping, which governs system stability, is shown in Figs. 14 and 15. Feedback of the lead-lag state ζ_s, Fig. 14, adds considerable damping to the regressing lag mode above 700 rpm and stabilizes the system. System frequencies have been changed, particularly at the coalescence rotor speed. However much smaller feedback gains (K<.5), which would adequately stabilize the system, were found to have little effect on the system frequencies. At the crossover of the regressing lag mode with the body pitch mode (600 rpm) this feedback control can destabilize the system, depending on the value of feedback gain. Feedback of roll rate, Fig. 15, also augments the damping of the regressing lag mode above 700 rpm and could be used to stabilize the system. Roll rate feedback has no effect on the regressing lag mode damping at coalescence with the pitch mode. This is consistent with the previous observation that pitch feedback is not suitable to eliminate the coupled regressing lag/roll mode instability. It is further interesting to note that feedback of the body roll rate (and roll acceleration also) leads to considerable shifts in the frequency of the roll mode and therefore changes the coalescence

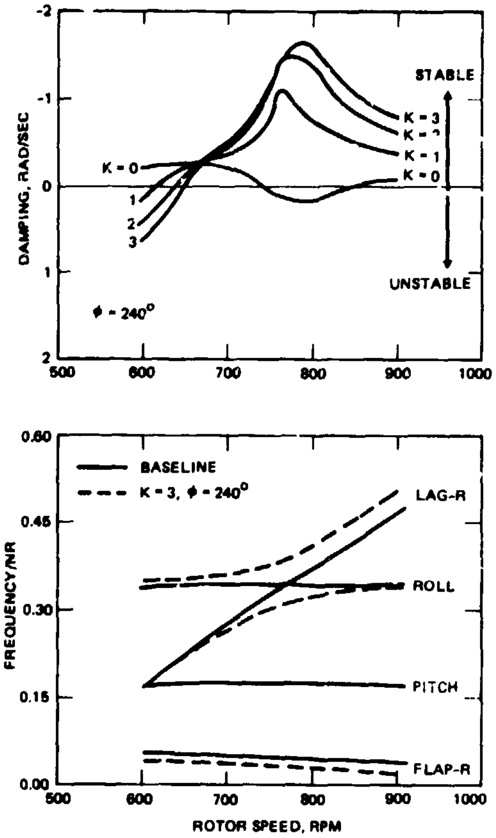


Fig. 14 Effect of sine cyclic lag (ζ_s) feedback gain on modal frequencies and regressing lag mode damping versus rotor speed.

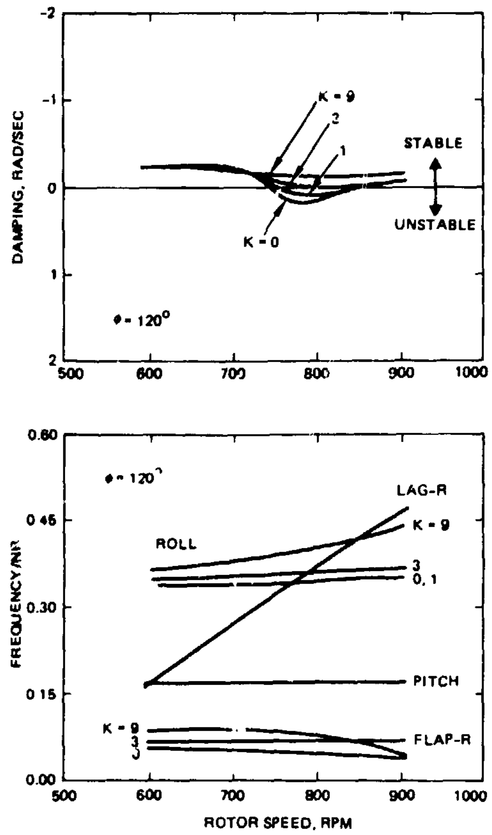


Fig. 15 Effect of roll rate ($\dot{\theta}_x$) feedback gain on modal frequencies and regressing lag mode damping versus rotor speed.

rotor speed. The stability gains seen in Fig. 15 are thus attributable to a combination of increased inherent damping and frequency shifts.

The sensitivity of the system dynamic behavior with respect to the feedback phase is explored in Figs. 16 and 17 for feedback of ζ_s and $\dot{\theta}_x$, respectively. In each case, three phase angles near the previously observed optimum value were chosen while the gain was kept at a particular value representing approximately similar control effort in terms of active blade pitch angle amplitudes. These values were determined from response studies to be K=0.3 and 9.0, for ζ_s and $\dot{\theta}_x$ feedback, respectively. For clarity only damping curves for the regressing lag mode are shown. Again, feedback of ζ_s for the gain K=0.3 keeps the system frequencies unchanged. Damping results show that feedback phase can be used to maximize the regressing lag mode damping at each rotor speed. This indicates that a phase schedule with rpm could be used. Feedback of the roll rate, Fig. 17, leads to roll mode frequency changes. However, the system is stable at the new coalescence rotor speed which means that inherent damping has been added to the regressing lag mode. Furthermore, while the feedback phase has little effect on system damping it is seen to be a powerful parameter for changing the roll mode frequency.

In summary, these results show that the feedback gain K can be varied to obtain a specified level of regressing lag mode damping at the coalescence rotor speed. The feedback phase ϕ can then be used to maximize the regressing lag mode damping augmentation at other rotor speeds or change the roll mode frequency which indirectly changes the regressing lag mode damping. Results also show that a different choice of feedback state variables and control parameters (K,ϕ) would be needed to eliminate an inplane/pitch instability. Quantitative results are given in Table 2. For $\dot{\zeta}_c,\zeta_s,\ddot{\zeta}_s$, and $\dot{\theta}_x$ feedback about 1 percent of critical damping is introduced for the regressing lag mode at a maximum active blade pitch angle, $\theta_{A_{max}}$, of approximately one third degree for a cyclic lead-lag amplitude of one degree. For $\dot{\theta}x$ about 1.5 percent of critical damping is introduced with the same control angle. The control angles shown in Table 2 are quite small in particular when considering the low frequency of the control motion. These results are very promising. They indicate that several ways exist to augment rotor/body stability. The important aspect of control mechanization can thus be approached with considerable flexibility.

Effects of Rotor Configuration

Very important rotor parameters in terms of control effectiveness are the blade root hinge

Table 2. Summary of state feedback results (Ω = 765 rpm)

Feedback State	K	ϕ deg	σ rad/sec	η % critical	θ_{Amax} deg
Baseline	0	-	0.145	-0.58	-
$\dot{\zeta}_c$	1	60	-0.164	0.65	0.32
ζ_s	.3	240	-0.137	0.54	0.29
$\ddot{\zeta}_s$	3	60	-0.178	0.71	0.34
$\dot{\theta}_x$	9	90	-0.149	0.59	0.33
$\ddot{\theta}_x$	27	270	-0.284	1.13	0.39

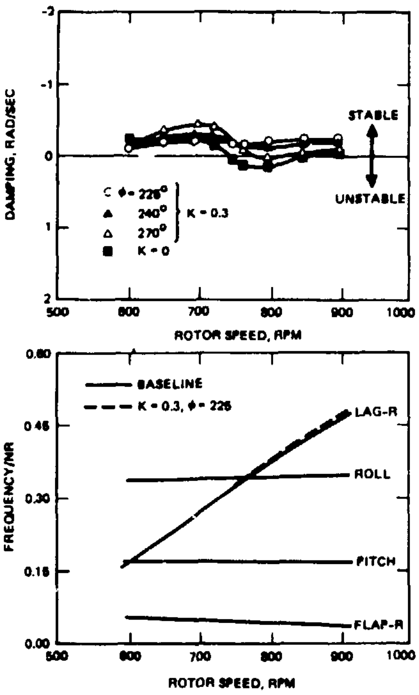


Fig. 16 Effect of sine cyclic lag (ζ_s) feedback phase on modal frequencies and regressing lag mode damping versus rotor speed.

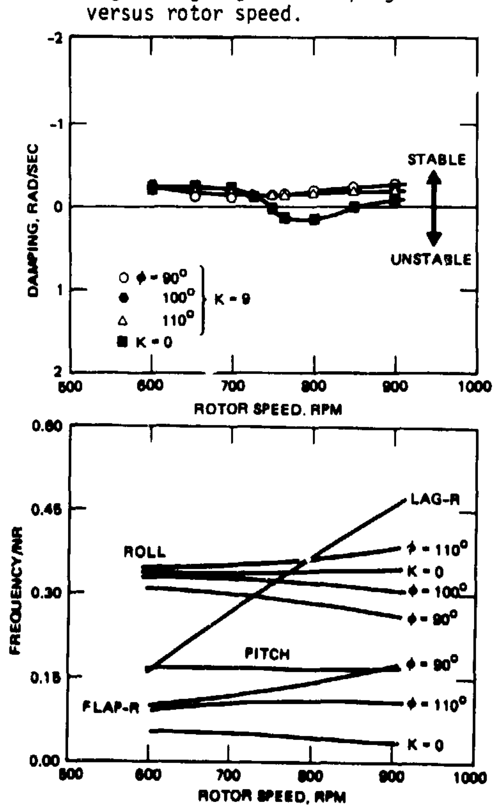


Fig. 17 Effect of roll rate ($\dot{\theta}_x$) feedback phase on modal frequencies and regressing lag mode damping versus rotor speed.

offset, precone, and flap spring stiffness. These parameters were varied from their baseline values (Table 1) to cover a range of values representative of articulated, hingeless, and bearingless rotors. At the same time the blade root spring stiffnesses, lead-lag damping and body roll stiffness were changed so that the modified rotor/body systems would closely approximate the baseline system dynamics, i.e. have the same coalescence rotor speed, roll frequency, and regressing lag mode frequency and damping. Thus, without application of active control (K=0), all the rotor configurations studied in this section exhibit the same instability at 765 rpm with η = -.58% critical as the baseline system (Table 2).

Investigation of active control with different rotor design parameters is limited to feedback of the sine cyclic lead-lag position (ζs) and roll acceleration ($\ddot{\theta}x$) state variables. For these two feedback states, the baseline configuration optimal feedback phase angles and feedback gain levels were used. Tables 3, 4, and 5 list the resulting system damping values for the three rotor parameter variations. Results for the various root hinge offsets (Table 3) and precone angles (Table 4) are obtained by keeping the active blade feathering angles constant (θ_{Amax} = 0.29 degrees for ζ_s feedback, approximately 0.4 degrees $\ddot{\theta}x$ feedback). It is seen that the system is stabilized for all the different rotor hub configurations. Increases in hinge offset increase the damping levels even though the flapping frequency is reduced. Similarly, increases in precone angle increase the damping levels. When reducing the flap spring stiffness to zero (Table 5), larger active blade feathering angles (~2 degrees) are required to obtain stability margins of approximately 0.5 percent critical damping. It should be pointed out, however, that typical articulated rotors have hinge offsets larger than the configurations in Table 5.

These results, while being of a limited nature, show that the root hinge offset, precone, and flap spring stiffness, have considerable influence on the active feedback control effectiveness. This was anticipated due to the action of hub moments and Coriolis coupling. It can be concluded that active control for rotor/body damping augmentation will be particularly powerful for hingeless and bearingless rotors which typically have a large virtual hinge offset and flap spring stiffness and in many cases also precone. Controlling the aeromechanical stability of typical articulated rotors will be a more difficult task. For these rotors it may be helpful to use collective blade pitch to introduce additional steady blade coning deflection.

Table 3. Effect of hinge offset on feedback results, equivalent dynamic systems (Ω = 765 rpm)

$\bar{e}$	σ rad/sec	η* % critical	θ_{Amax} deg	Feedback State	K	ϕ deg
0.10	-0.137	0.54	0.29	ζ_s	0.3	240
0.05	-0.097	0.38	0.29	ζ_s	0.3	240
0.02	-0.077	0.31	0.29	ζ_s	0.3	240
0.10	-0.284	1.13	0.39	$\ddot{\theta}_x$	27	270
0.05	-0.158	0.63	0.45	$\ddot{\theta}_x$	27	270
0.02	-0.119	0.47	0.37	$\ddot{\theta}_x$	27	270

Table 4. Effect of precone on feedback results, equivalent dynamic systems (Ω = 765 rpm, $\bar{e}$ = 0.02)

β_p deg	σ rad/sec	η* % critical	θ_{Amax} deg	Feedback State	K	ϕ deg
0	-0.077	0.31	0.29	ζ_s	0.3	240
2	-0.116	0.46	0.29	ζ_s	0.3	240
4	-0.157	0.62	0.29	ζ_s	0.3	240
0	-0.119	0.47	0.37	$\ddot{\theta}_x$	27	270
2	-0.288	1.14	0.43	$\ddot{\theta}_x$	27	270
4	-0.507	2.01	0.50	$\ddot{\theta}_x$	27	270

Table 5. Effect of flap stiffness on feedback results, equivalent dynamic systems (Ω = 765 rpm, $\bar{e}$ = 0.02)

Flap Stiff-ness N-m	σ rad/sec	η* % critical	θ_{Amax} deg	Feed-back State	K	ϕ deg
38.8	-0.077	0.31	0.29	ζ_s	0.3	240
19.4	-0.181	0.71	1.00	ζ_s	1.0	240
0	-0.132	0.52	1.83	ζ_s	2.0	225
38.8	-0.119	0.47	0.37	$\ddot{\theta}_x$	27	270
19.4	-0.232	0.91	1.17	$\ddot{\theta}_x$	27	270
0	-0.334	0.36	2.41	$\ddot{\theta}_x$	27	225

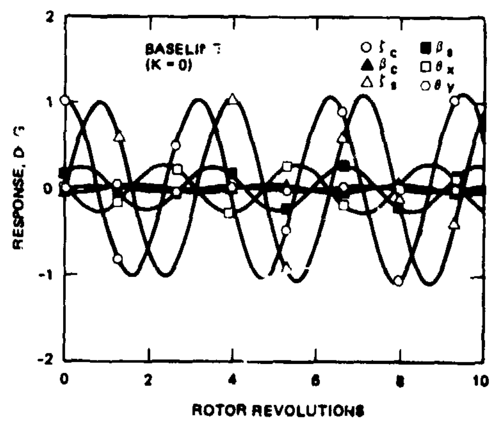
*Without active control (K=0) system damping is η = -0.58% critical for all rotor configurations.

This should have similar beneficial effects on control effectiveness as would introducing blade precone angle.

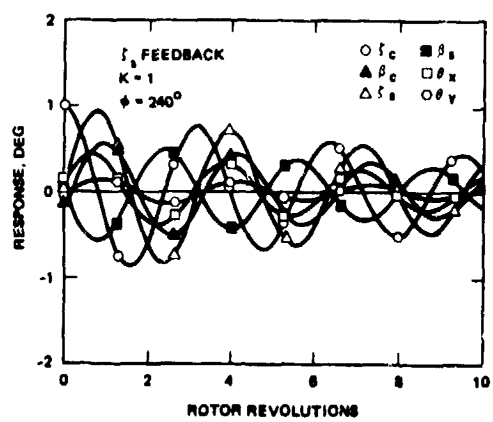
Rotor Response

Response results are intended to be of a qualitative nature, to give a better understanding of the rotor/body motion and give an indication of the required control input magnitudes.

Free response from a set of prescribed initial conditions and frequency response results are presented. The free response results, Fig. 18, are computed using the regressing lead-lag eigenvector from the stability analysis, normalized to a maximum lead-lag amplitude of one degree, as an initial condition. Figure 18a shows the response of the system with no feedback controls applied. The regressing lag mode is slightly unstable, with critical damping of η = -0.58%. It's modal components consist largely of the cyclic lead-lag motions (ζ_c,ζ_s), the body roll degree of freedom (θ_x), and lateral cyclic flapping (β_s). There is very little pitch and longitudinal flap motion. The inherent stability of the rotor/body system with sine cyclic lead-lag feedback control at K=1 and ϕ = 240 degrees is illustrated in Fig. 18b. The time history response of the regressing lag mode shows that with feedback this previously unstable mode is stabilized and both cyclic lead-lag degrees of freedom, ζ_c and ζ_s, reduce significantly in amplitude in only ten rotor revolutions. It is also seen that feedback control increases the participation of the flap and body pitch and roll motions in the regressing lag mode. This could be one source of the increased damping of this mode. The amplitude of the active blade feathering in Fig. 18b is 0.9 degrees initially and reduces to less than 0.5 degrees over ten rotor revolutions.



a) FREE RESPONSE WITHOUT FEEDBACK



b) WITH SINE CYCLIC LAG FEEDBACK, ζ_s

Fig. 18 System response using regressing lead-lag eigenvector from stability analysis as initial condition.

Frequency response analysis is used to compare the effect of increased blade lead-lag damping with the application of feedback control. Frequency response was computed by simulating a one degree blade pitch stick stir in the regressing direction. For frequency response the nondimensional excitation frequency is varied from 0.1 to 0.7. Rotor rotation rate is 765 rpm. Fig. 19 shows the influence of increasing the lead-lag damping from η_ζ = 0.52 percent to 2 percent and 8 percent critical. No feedback controls are applied. Fig. 20 shows, in the same manner, the influence of ζ_S feedback with increasing gain values, K = 0.3; 1.0, 3.0, and ϕ = 240 degrees. Here the damping is held at its nominal value of η_ζ = 0.52 percent. In both Fig. 19 and Fig. 20 only the frequency response of the cosine cyclic lead-lag motion is shown. Comparing both magnitude and phase plots qualitatively, it is seen that feedback control and additional blade damping have very similar effects in terms of system dynamics. This is an additional indication that active control can be used to augment rotor/body damping and reduce or possibly even eliminate the need for lead-lag dampers for articulated rotor systems.

Conclusions

This study shows that active control blade feathering through a conventional swashplate is a viable means to increase rotor/body damping levels and to eliminate ground resonance instabilities. Stability and response results have been presented using state variable feedback control for a model hingeless rotor. From these results, the following conclusions can be drawn.

1) Either rotor or fuselage states can be used to eliminate ground resonance instabilities by specifying appropriate closed-loop feedback phase and gain.

2) With the proper choice of feedback phase, damping of the regressing lag mode can be maximized without adversely affecting the damping of other system modes. The feedback gain K can then be adjusted to obtain a specified level of regressing lag mode damping at the coalescence rotor speed.

3) Approximately a one percent augmentation in critical damping in the regressing lead-lag mode can be obtained with an active blade feathering amplitude of 0.3 degree per degree of blade cyclic lead-lag motion.

4) Increased lead-lag damping with active control is due to several factors: (1) a reduction in damping of other, more highly damped system modes, and (2) an increased participation by other

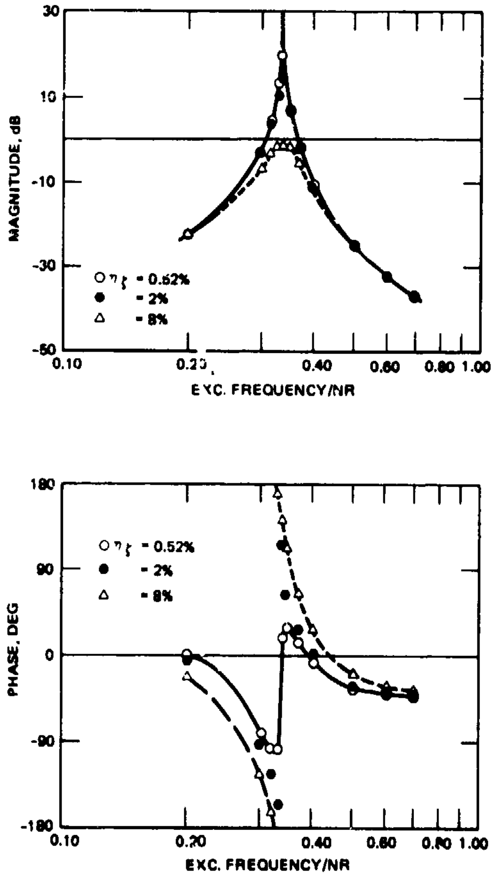


Fig. 19 Effect of blade lead-lag damping on frequency response of cosine cyclic lag motion, no feedback applied, (K=0).

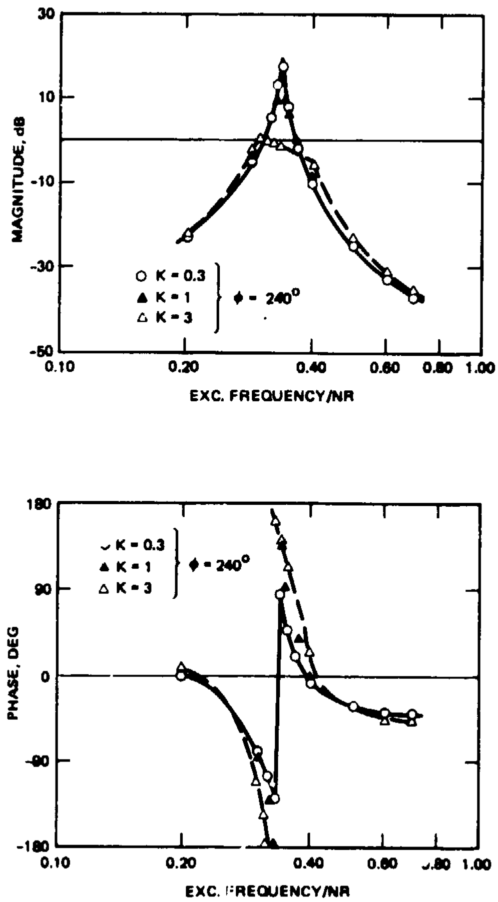


Fig. 20 Effect of sine cyclic lag (ζ_S) feedback gain on frequency response of cosine cyclic lag motion; (η_ζ = 0.52%).

system modes in the motion characterizing the regressing inplane mode.

5) System stability improvements can be a direct result of increased regressing lead-lag mode damping since system frequencies, and in particular the coalescence rotor speed, remain unchanged.

6) Rotor rpm sweeps show that with the feedback controls selected at the coalescence rotor speed, the system is stabilized throughout the range of previously unstable operating conditions. Furthermore, scheduling the feedback phase with rotor speed can be used to maximize the damping augmentation.

7) For roll feedback ($\dot{\theta}x, \ddot{\theta}x$) the feedback phase has a considerable effect on the roll mode frequency. Besides increasing the regressing lag mode damping, roll acceleration feedback in particular can be designed to shift the coalescence rotor speed. This would indirectly improve system stability through active control of frequency placement.

8) A different choice of feedback state variables and control parameters would be necessary to eliminate an inplane/pitch instability. For the present configuration the active controls should be applied only at rotor speeds above the crossover of the regressing lag mode with the body pitch mode.

9) Increasing the root hinge offset, flap spring stiffness, and blade precone improves the control effectiveness considerably. Active control for rotor/body damping augmentation will be particularly powerful for hingeless and bearingless rotors. Controlling the aeromechanical stability of articulated rotors will be a more difficult task.

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DISCUSSION
Paper No. 1

THE USE OF ACTIVE CONTROLS TO [illegible] ROTOR/FUSELAGE STABILITY
Fried[illegible] Straub
[illegible]
Willi[illegible]mbrodt

Euan Hooper, Boeing Vertol: I would like to [illegible]gratulate you on an outstanding contribution. In the practical ground resonance case, [illegible], you are concerned not just with sitting [illegible] on the ground, but with frequencies [illegible] cover a wide range of situations from blown tires to incorrectly inflated oleos [illegible] [where you become] airborne and all your natural frequencies will go from ground [illegible]lues] approaching zero. So you have to prepare for a wide range of situations. Have [illegible] at the applications of this system to practical takeoff situations?

Straub: No, I haven't looked at takeoff situations at all so far. Lately, I have considered some cases where the fuselage mass was varied widely to simulate different payload configurations. Actually what I saw was that even with one set of controls you can handle that kind of situation. I have not looked at what happens when you increase the collective pitch angle and simulate takeoff. That would certainly be something to look into.

Bob Blackwell, Sikorsky Aircraft: I also enjoyed your paper and look forward to reading it. I have two questions. First, would you comment on the kind of design philosophy that would allow this approach in lieu of frequency placement and conventional damping--is it more weight effective, or perhaps a means of controlling ground resonance? Second, have you given thought to the instrumentation to measure the lead-lag motion of all the blades or is that not necessary and so what kind of redundancy requirements would be required of a potential system?

Straub: Well, first, the idea is to eventually replace maybe the lead-lag dampers on some of our articulated rotors and that seems to be a feasible alternative. As far as the implementation is concerned the results I presented here were mostly for the feedback of the lead-lag motion; I think that the first choice of the designer is to use a fixed system feedback--either the roll or pitch degrees of freedom. I think it would be possible to do that rather than use the inplane motion. What I did here was to consider feedback of all degrees of freedom and their time derivatives just to see how good different feedback states would be. So the argument of how the control parameters were chosen was most easily illustrated here for the inplane model feedback, but roll would be just as good.

Dave Peters, Washington University: You know when there is no initial lift on the rotor and then you plot the lift versus angle of attack it's not linear. It starts off flat and builds up. There is no mass flow to build your lift up. And this means you make small changes in pitch angle you don't get much effectiveness in terms of force. How do you think that would affect your idea when you try to stabilize ground resonance at zero lift conditions?

Straub: That is a very good question. I am not sure I have an answer to it at this moment. Certainly as you point out it is a very valid concern and also the aerodynamic model that I have used is very simple. That probably could use some improvement too. For instance, what effects do unsteady aerodynamics have on this kind of phenomena because you are oscillating the blade at half per rev or something like that. Even though it is a much lower frequency than vibration control at 4 per rev, still the aerodynamics would definitely be something to look into further.

Dick Gabel, Boeing Vertol: In these days when we deal with ground resonance qualification we have to meet failure conditions of everything--the oleos, tires, lag dampers, everything. Can you foresee with such a system if you have to accommodate failures, what the approach would be if the system was off and you were in ground resonance? That could be a disaster.

Straub: That is certainly true, but I think that in any kind of fly-by-light or fly-by-wire control system for the primary controls you deal with these same kinds of issues. Your actuators have to be redundant; you have to be able to deal with failures. So certainly that would also apply if you use active controls to replace some lead-lag dampers. You would have to have multiple sensors and you would have to have a fail-safe operating mode.

Walter Gerstenberger, Consultant: Blackwell's question and Gabel's question were well taken so I'll ask the follow-on question. We always try to couple lag motion with flapping motion or something; with inclined hinges. How would you design the mechanical system to accomplish the [feedback] that does these wonderful things that you say. Have you thought about that? In other words doing it mechanically by coupling; measuring lag motion and feeding it into your cyclic pitch with a phase shift?

Straub: Well, you can add some roll accelerometers.

Gerstenberger: No, not electronically--mechanically.

Straub: How to do it mechanically?

Gerstenberger: Whiffle trees, mixing bars . . .

Straub: I think maybe a better way of doing that would be something that you just mentioned--inclined pitch links and other kinds of kinematic couplings that are designed into the blade root end. Maybe I should add one thought we had when we did this [paper]. Obviously, when you design a helicopter you make use of all the available methods to make it as stable as you can to begin with. But then if you talk about a situation where you have a fly-by-wire control system, you already have dynamic actuators on there for the vibration reduction, then it could be very nice just to put an additional sensor on the helicopter--if required at all--and to put an additional board into your controller and have a way of augmenting the rotor fuselage damping.

Gerstenberger: Well, let me rephrase the question. I don't like you to sell me electronic control, but that's not the point. Is your feedback--I don't understand the feedback completely--is the feedback simple enough so that it is possible to design some pure mechanical feedback to give you stability which nobody has been able to do in the past with a mechanical tilting of a hinge or something like that?

Straub: Well, I haven't really thought about it too much. I would suppose you could probably do it if you look at some of the early work that Bell had done on active control for the ?P control. They had a mechanical control system on there and I think the amount of linkages they had there was probably somewhat forbidding. I don't know.