demonstrated an ideal linear behavior to failure with reasonably repeatable performance.

The short column analysis and experimental tube data given in Table 3 indicate that a suitable analytical procedure exists for predicting the local instability of B/Al columns. It is important to note that such a capability, coupled with the generally more accurate prediction techniques relative to column buckling and strength, greatly enhances the capability to design and analyze B/Al tubular structures. The limited data suggest that an empirical knockdown factor of approximately 0.75 be applied to predictions using the analysis procedure to improve correlation between theoretical and test data. More correlation studies are required to demonstrate general application of the procedure over a wider range of specimen diameter to thickness (D/t) ratios.

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Effect of Nonsymmetric Damping on Trim Magnification of Rolling Re-entry Vehicles

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Nomenclature

A	=	$\pi d^2/4$, reference area, ft^2
C_m	=	pitching moment coefficient, M_y/QAd
C_{m_σ}	=	$(\partial C_m/\partial\sigma)_{\sigma\to 0}$ in-plane static stability derivative 1/rad
$C_{m_q}+C_{m_\alpha}$	=	$\partial C_m/\partial(qd/V)+\partial C_m(\alpha d/V)$, in-plane damping derivative, 1/rad
$C_{n_r}-\cos\alpha \times C_{n_\beta}$	=	$\partial C_n/\partial(rd/V)-\cos\alpha\ \partial C_n/\partial(\beta d/V)$, out-of-plane damping derivative, 1/rad
C_{m_ϵ}	=	$C_{m_\sigma}\delta_{t_0}$, aerodynamic asymmetry coefficient
d	=	model base diameter, ft
I	=	transverse moment of inertia, slug-ft^2
I_x	=	axial moment of inertia, slug-ft^2
M_x, M_y, M_z	=	moments about aerodynamic axes, ft-lb
Q	=	dynamic pressure, lb/ft^2
V	=	velocity fps
$p.q.r$	=	roll, pitch, and yaw rates, respectively, rad/sec
X,Y,Z	=	inertial axes (see Fig. 1)
x,y,z	=	aerodynamic axes (see Fig. 1)
δ_{t_0}	=	nonrolling trim angle, rad or deg
σ	=	resultant angle of atack, rad or deg
$\dot\phi$	=	coning rate, (see Fig. 1) rad/sec
$\dot\psi$	=	body roll rate, (see Fig. 1) rad/sec

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Introduction

THE symmetric assumption of the linear aeroballistic theory[1] requiring equality between the in-plane and out-of-plane aerodynamic derivatives, i.e.,

$$C_{m_q}+C_{m_{\dot\alpha}}=C_{n_r}-\cos\alpha\ C_{n_{\dot\beta}} \qquad (1)$$

has been shown to be theoretically[2,3] and experimentally[4-6] invalid when the restoring moment C_m is nonlinear with angle of attack. In present wind tunnel tests the nonlinearity of C_m and $C_{m_q}+C_{m_{\dot\alpha}}$ with angle of attack are usually determined while the inequalities of in-plane and out-of-plane damping derivatives are neglected. The same is true for computer stability and performance analyses. It is the purpose of this Note to demonstrate the effects of nonsymmetric damping derivatives on the resultant angle of attack of a re-entry vehicle with slight aerodynamic asymmetries passing through resonance.

Computer Investigations

The aerodynamic axis system[7] is shown in Fig. 1. The moment equations, for a constrained center of gravity, are

$$I_x\dot p=M_x$$

$$I\ddot\sigma+I_x p\dot\phi\ \sin\sigma-I\dot\phi^2\sin\sigma\ \cos\sigma=M_y$$

$$I\ddot\phi\ \sin\sigma+2I\dot\sigma\dot\phi\ \cos\sigma-Ixp\sigma=M_z$$

where

M_x = Constant roll moment

$$M_y=\left\{C_m+(\sigma d/V)\ (C_{m_q}+C_{m_{\dot\alpha}})+C_{m_\epsilon}\cos\psi\right\}QAD$$

$$M_z=\left\{(\phi d/V)\sin\phi(C_{n_r}-\cos\alpha C_{n_{\dot\beta}})+C_{m_\epsilon}\sin\psi\right\}QAd$$

$$\psi=p-\phi\ \cos\sigma$$

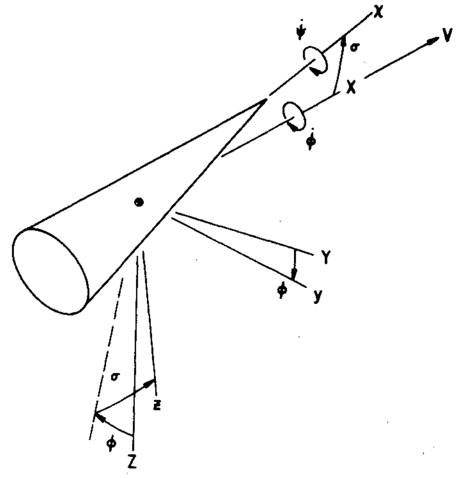

Fig. 1 Aerodynamic axes system.

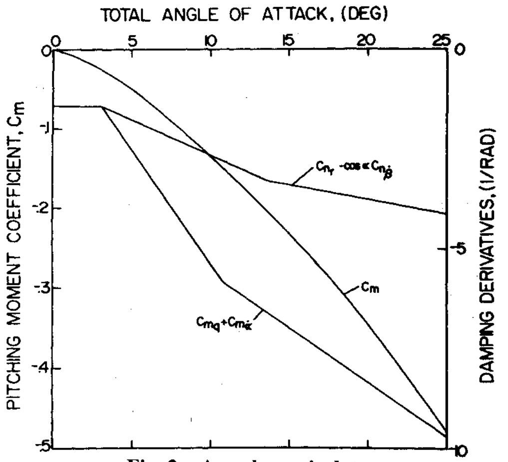

Fig. 2 Aerodynamic data.

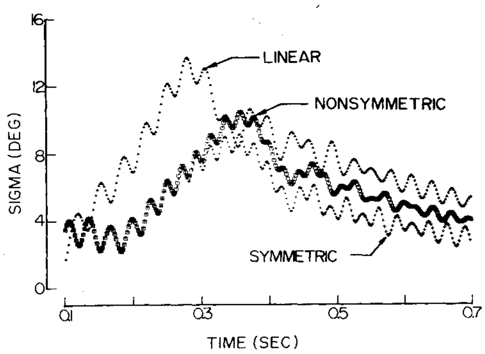

Fig. 3 Resultant angles of attack, $\delta_{t_0} = 2.15°$.

Table 1 Maximum resultant angle of attack at resonance

δ_{t_0}	σ_{max} (Deg)		
(Deg)	Linear	Symmetric	Nonsymmetric
1.10	7.02	5.66	5.85
2.15	13.69	9.20	10.21
3.53	17.46	11.80	14.09

These equations of motion were numerically integrated on a computer to determine the resultant angle-of-attack σ as a function of time. Typical mass parameters for a slender, symmetric, re-entry vehicle were employed. The $\sigma(t)$ was computed by giving the re-entry vehicle an assumed constant roll acceleration p which carried it through resonance. The computations were terminated when the roll rate equaled five-times the pitching frequency. Constant values of altitude and velocity were chosen so that the aerodynamic coefficients reported in Ref. 8 (Fig. 2) were applicable. The aerodynamic asymmetry coefficients C_{m_ϵ} were obtained for three typical values of the nonrolling trim angle, $\delta_{t_0} = 1.10$, 2.15, and 3.53°.

To distinguish between the different sets of stability coefficients, the following definitions were employed

"Linear" means

$$C_m(\sigma) = (\partial C_m / \partial \sigma)_{\sigma=0} \sigma; C_{m_q} + C_{m_{\dot\alpha}} = C_{n_r} - \cos\alpha C_{n_{\dot\beta}}$$

"Symmetrical" means

$$C_m = f(\sigma),\ C_{m_q} + C_{m_{\dot\alpha}} = C_{n_r} - \cos\alpha C_{n_{\dot\beta}} = g(\sigma)$$

"Nonsymmetrical" means

$$C_m = f(\sigma);\ C_{m_q} + C_{m_{\dot\alpha}} = g(\sigma),\ C_{n_r} - \cos\alpha C_{n_{\dot\beta}} = h(\sigma)$$

Results

A typical set of resultant angles of attack as functions of time is presented in Fig. 3. The maximum values of the resultant angles of attack at resonance are given in Table 1. It is seen that linear stability coefficients overpredict the trim magnification at resonance. More importantly, symmetric stability coefficients, with the incorrect assumption that $C_{m_q} + C_{m_{\dot\alpha}} = C_{n_r} - \cos\alpha C_{n_{\dot\beta}} = g(\sigma)$, underestimates the trim magnification at resonance and leads to overly optimistic stability predictions. In view of the current interest in high angle-of-attack maneuvering vehicles, emphasis should be placed on the measurement of nonsymmetric stability coefficients and the evaluation of their effects on stability and performance.

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Circular Earth Orbits Attainable with Fixed Two-Impulse Expendable Tug

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Introduction

ALTHOUGH solid rocket motors in expendable stages have been suggested as an alternative concept for Shuttle interim upper stage applications, a lack of mission flexibility is sometimes cited as a disadvantage of these fixed-impulse systems. Specifically, the sizes of the stages are dictated by the requirements of a specific mission, typically the delivery of a given payload to geosynchronous orbit. For other payloads and orbits, it is generally necessary to perform nonoptimal maneuvers.

This paper describes a limited assessment of the performance penalties inherent with fixed-impulse motors from the standpoint of mission flexibility. Only nonreturn missions to circular Earth orbits are considered. It is assumed that the

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