Influence of Bobweights on Pilot-Induced Oscillations

T. PETER NEAL*
Cornell Aeronautical Laboratory Inc., Buffalo, N. Y.

Historically, a major contributor to longitudinal pilot-induced oscillations (PIO) in service airplanes has been the use of bobweights, which cause the control system to couple dynamically with the airframe's short-period mode. The purpose of this article is to review and discuss the influence of bobweights on flying qualities, and to show how the unwanted effects can be minimized by careful design. A method of analysis is presented, followed by application of the method to several actual PIO situations. Finally, the various options available to the airplane designer are discussed.

Nomenclature

F_b = stick force due to control system mass unbalance, lb
F_{FS} = net stick force transmitted to the control system, lb, $F_{FS} = F_S - F_b$
F_S = total force applied to the stick by the pilot (positive for pull), lb
$(F_S/n)_{SS}$ = stick-force change required to achieve a unit change in normal acceleration, when the airplane is maneuvered at constant speed, lb/g
I_y = pitch moment of inertia about c.g.
K_n = stick force per unit n contributed by control-system mass unbalance for $\ddot{\theta} = 0$, lb/g
$K_{\ddot{\theta}}$ = stick force per unit $\ddot{\theta}$ contributed by control system mass unbalance, for $n_{cg} = 0$[lb/(rad/sec^2)]
l_b = distance of an equivalent point-mass bobweight ahead of c.g., ft, $l_b = g(K_{\ddot{\theta}}/K_n)$
l_{cp} = distance of airplane's center-of-percussion ahead of c.g., ft, $l_{cp} = (Z_{\delta_e}/M_{\delta_e})(I_y/ml_t)$
l_t = distance from c.g. to aerodynamic center of horizontal tail, ft
m = airplane mass, slugs
M = aerodynamic pitching moment about c.g. (positive nose up), ft-lb
M_{δ_e} = $(1/I_y)(\partial M/\partial \delta_e)$
n = normal load factor (positive for pullup), g
n_b = n measured a distance l_b ahead of c.g., g
n_{cg} = n measured at c.g., g
n/α = steady-state normal acceleration change per unit angle-of-attack change, when the airplane is maneuvered at constant speed, g/rad
V = true airpseed, fps
Z = aerodynamic vertical force acting on airplane (negative for a pullup), lb
Z_{δ_e} = $(1/m)(\partial Z/\partial \delta_e)$
δ_e = elevator deflection (positive for trailing edge down), rad
δ_S = stick displacement (positive for pull), in.
ζ_b = damping ratio of the bobweight zero
ζ_{FS} = open-loop damping ratio of the feel system
ζ_{SP} = open-loop (stick-fixed) damping ratio of the short-period mode
θ = airplane pitch attitude with respect to horizon (positive nose up), rad
$1/\tau_a$ = open-loop breakpoint frequency of the elevator actuator, rad/sec
$1/\tau_{n_1}, 1/\tau_{n_2}$ = numerator breakpoint frequencies of the n_{cg}/δ_e transfer function, rad/sec
$1/\tau_{\theta_2}$ = numerator breakpoint frequency of the $\ddot{\theta}/\delta_e$ transfer function, rad/sec
ω_b = undamped natural frequency of the bobweight zero, rad/sec
ω_{FS} = open-loop undamped natural frequency of the feel system, rad/sec
ω_{SP} = open-loop (stick-fixed) undamped natural frequency of the short-period mode, rad/sec
$(\ )'$ = primes added to the parameters ζ, ω, $1/\tau$ defined previously, signify the closed-loop (stick-free) values

Presented as Paper 70-1002 at the AIAA Guidance Control and Flight Mechanics Conference, Santa Barbara, Calif., August 17–19, 1970; submitted October 5, 1970; revision received March 22, 1971. This paper is based upon work performed by the Cornell Aeronautical Laboratory for the U.S. Air Force under Contract No. AF33(615)-3294.

* Research Engineer, Flight Research Department. Member AIAA.

I. Introduction

IN 1966 a three-year effort was initiated by CAL's Flight Research Department under the sponsorship of the Air Force Flight Dynamics Laboratory to develop a new military flying qualities specification.[1,2] As part of this effort a rather detailed study of the longitudinal pilot-induced-oscillation (PIO) problem was accomplished. The study showed that there are many factors which can contribute to PIO tendencies. Historically, however, the major contributor to PIO problems in service airplanes has been the use of normal-acceleration bobweights, which cause the control system to couple dynamically with the airframe's short-period mode.

Even today, the use of bobweights is attractive as a means to keep the maneuvering gradient of stick force per g within reasonable limits as flight condition and loading change. Since the use of bobweights is fairly common, it is important that the designer understand their undesirable features, as well as their desirable ones.

The purpose of this article is to summarize the primary influence of bobweights on flying qualities, and to show how the more important effects can be treated at the design stage so that the possibility of PIO is minimized. The many secondary factors which can influence PIO tendencies[3–5] are difficult to analyze and are not considered here. Some of the ideas presented are original; many represent new ways of viewing previous work by others; all should be useful to the designer. First, a method of analysis is presented. This is followed by application of the method to several PIO situations. Finally, the various options available to the airplane designer are discussed.

II. Method of Analysis

In order to understand how bobweights affect flying qualities it is appropriate to first determine how a bobweight affects the airplane's maneuvering dynamics. To this end the constant speed dynamics of an airplane, with a powered control system, can be represented as in Fig. 1. The open-loop control system characteristics are determined by the feel springs, the viscous damper (if any), control system inertia, mechanical gearing, and actuator dynamics. The airframe characteristics are represented by standard constant-speed transfer functions, referenced to the airplane's center of gravity. The $K_{\ddot{\theta}}$ and K_n gains represent the feedbacks caused by the combined effect of the bobweight plus any other mass unbalance in the control system.

Figure 1 can be simplified somewhat by operating on the transfer function of F_b to δ_e. Notice that

$$F_b/\delta_e = K_n(n_{cg}/\delta_e) + K_{\ddot{\theta}}(\ddot{\theta}/\delta_e) \quad (1)$$

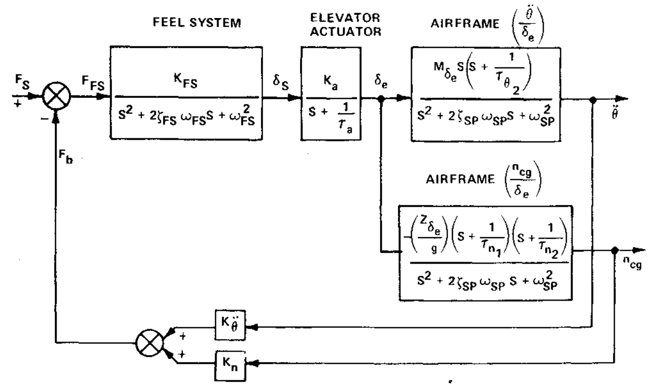


Fig. 1 Maneuvering dynamics of an airplane with a bobweight.

If we define a length, $l_b = g(K_{\ddot{\theta}}/K_n)$, Eq. (1) reduces to

$$F_b/\delta_e = K_n[(n_{cg}/\delta_e) + (l_b/g)(\ddot{\theta}/\delta_e)] \\ = K_n(n_b/\delta_e) \tag{2}$$

where n_b/δ_e is simply the transfer function of normal acceleration to δ_e, measured a distance l_b ahead of the center of gravity. Working with the expressions for (n_{cg}/δ_e) and $(\ddot{\theta}/\delta_e)$, it can be shown that

$$n_b/\delta_e = K_b(S^2 + 2\zeta_b\omega_b S + \omega_b^2)/(S^2 + 2\zeta_{SP}\omega_{SP}S + \omega_{SP}^2) \tag{3}$$

where

$$K_b = (1/g)(-Z_{\delta_e} + l_b M_{\delta_e}) \tag{4}$$

$$\omega_b^2 = -Z_{\delta_e}[(1/\tau_{n_1})(1/\tau_{n_2})]/(-Z_{\delta_e} + l_b M_{\delta_e}) \tag{5}$$

$$2\zeta_b\omega_b = \frac{-Z_{\delta_e}(1/\tau_{n_1} + 1/\tau_{n_2}) + l_b M_{\delta_e}(1/\tau_{\theta_2})}{-Z_{\delta_e} + l_b M_{\delta_e}} \tag{6}$$

Using Eqs. (2) and (3), the block diagram of Fig. 1 can be reduced to the form shown in Fig. 2. Physically, this block diagram can be thought of as replacing the actual bobweight and other control system mass unbalance with an equivalent point-mass bobweight, located a distance l_b ahead of the airplane center of gravity (see Fig. 3). Notice that if the basic control system is very lightweight, and virtually all the mass unbalance is due to a single bobweight, l_b is simply the distance of the actual bobweight ahead of the center of gravity.

Referring again to Fig. 2, it can be seen that a given set of airframe and control system characteristics and a fixed bobweight location, l_b will completely determine the open-loop dynamics. A typical open-loop pole-zero configuration is shown in Fig. 4. If the bobweight feedback loop is now closed, the locus of the roots shows how the airplane's dynamics change as the root-locus gain K is increased. The closed-loop transfer function will therefore be of the following form:

$$\frac{n_b}{F_S} = \frac{K_{bf}(S^2 + 2\zeta_b\omega_b S + \omega_b^2)}{(S^2 + 2\zeta_{FS}'\omega_{FS}'S + \omega_{FS}'^2) \times (S + 1/\tau_a')(S^2 + 2\zeta_{SP}'\omega_{SP}'S + \omega_{SP}'^2)} \tag{7}$$

where the primes distinguish the closed-loop roots from the open-loop poles. These closed-loop roots represent the stick-

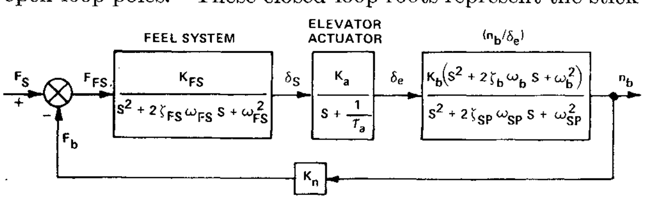


Fig. 2 Simplified maneuvering dynamics of an airplane with a bobweight.

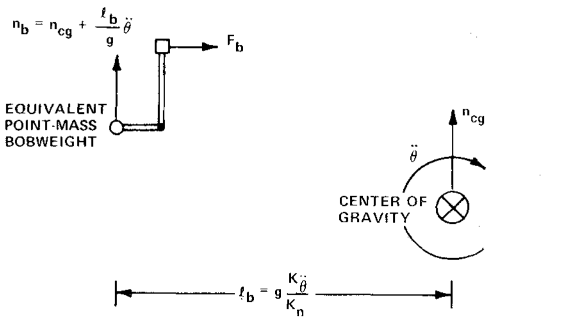


Fig. 3 Equivalent point-mass bobweight.

free dynamics of the airplane, while the stick-fixed dynamics are characterized by the open-loop poles of Fig. 4.

The root-locus gain K can be related to the bobweight characteristics in the following way. From Fig. 2, it can be seen that

$$F_S = F_{FS} + F_b \tag{8}$$

Therefore, the steady-state stick force per g is composed of two parts:

$$(F_S/n)_{SS} = (F_{FS}/n)_{SS} + (F_b/n)_{SS} \tag{9}$$

The first part is the contribution of the feel system, which can be determined from Fig. 2 as follows:

$$(F_{FS}/n)_{SS} = (\omega_{FS}^2/K_{FS})(1/\tau_a K_a)(\omega_{SP}^2/K_b\omega_b^2) \tag{10}$$

(Note that the subscript b has been dropped from n, since n is the same at any location in the airplane, for the steady-state case.) The second part is the contribution of the bobweight and is equal to K_n. Rearranging the expression for $(F_{FS}/n)_{SS}$ it follows that

$$(K_{FS}K_aK_b) = (F_{FS}/n)_{SS}^{-1}[\omega_{FS}^2(1/\tau_a)\omega_{SP}^2/\omega_b^2] \tag{11}$$

From Fig. 2, the root-locus gain is seen to be

$$K = K_nK_{FS}K_aK_b \tag{12}$$

Substituting for K_n and the product $K_{FS}K_aK_b$

$$K = [(F_b/n)_{SS}/(F_{FS}/n)_{SS}][\omega_{FS}^2(1/\tau_a)\omega_{SP}^2/\omega_b^2] \tag{13}$$

Thus, for a given set of open-loop poles and a given pair of bobweight zeros, the closed-loop (stick-free) roots are completely determined by the ratio of the stick force per g contributed by the bobweight to that contributed by the feel system. If this ratio is large, the stick force per g will be primarily determined by $(F_b/n)_{SS}$, which is invariant with flight condition and loading. Therefore, a good bobweight zero location is one which will permit high root-locus gains with a minimum of degradation in the closed-loop dynamics.

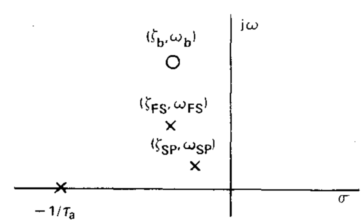
III. Effect of Bobweight Zero Location

In order to determine good locations for the bobweight zeros, it is first necessary to obtain some feeling for the types of zero locations which are obtainable for real airplanes.

Obtainable Zero Locations

As a first step in determining the possible zero locations, let us define a length l_{cp} which is the distance of the airplane's center of percussion ahead of the center of gravity. The center of percussion is where the initial normal acceleration response to a step δ_e input is zero. This definition can be ex-

Fig. 4 Typical open-loop singularities for airplane with a bobweight.



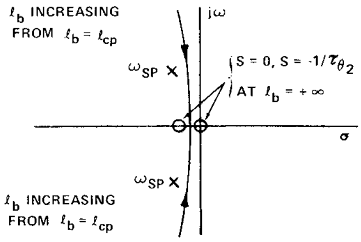


Fig. 5 Effect of l_b on bobweight zeros.

pressed mathematically as follows:

$$(n_{cp})_{\text{INITIAL}} = (n_{cg})_{\text{INITIAL}} + \frac{l_{cp}\ddot{\theta}_{\text{INITIAL}}}{g} = 0 \quad (14)$$

From the initial value theorem

$$0 = \lim_{S\to\infty}\left[S\,\frac{\delta_e}{S}\left(\frac{n_{cg}}{\delta_e} + \frac{l_{cp}}{g}\,\frac{\ddot{\theta}}{\delta_e}\right)\right] \quad (15)$$

Taking the limit, there results:

$$0 = \delta_e(-Z_{\delta_e}/g + l_{cp}/gM_{\delta_e}) \quad (16)$$

and

$$l_{cp} = Z_{\delta_e}/M_{\delta_e} \text{ (positive for aft-tailed airplanes)} \quad (17)$$

Introducing this expression for l_{cp} into Eqs. (5) and (6)

$$\omega_b{}^2 = \frac{(-Z_{\delta_e}/M_{\delta_e})[(1/\tau_{n_1})(1/\tau_{n_2})]}{-Z_{\delta_e}/M_{\delta_e} + l_b} = \frac{(-Z_{\delta_e}/M_{\delta_e})[(1/\tau_{n_1})(1/\tau_{n_2})]}{l_b - l_{cp}} \quad (18)$$

$$2\zeta_b\omega_b = \frac{(-Z_{\delta_e}/M_{\delta_e})(1/\tau_{n_1} + 1/\tau_{n_2}) + l_b(1/\tau_{\theta_2})}{-Z_{\delta_e}/M_{\delta_e} + l_b} \quad (19)$$

Since $(1/\tau_{n_1} + 1/\tau_{n_2})$ is nearly zero for most airplanes, Eq. (19) reduces to

$$2\zeta_b\omega_b = l_b(1/\tau_{\theta_2})/(l_b - l_{cp}) \quad (20)$$

In the steady state, $n = (V/g)\dot{\theta}$ so that:

$$\frac{(-Z_{\delta_e}/g)[(1/\tau_{n_1})(1/\tau_{n_2})]}{\omega_{SP}{}^2} = (n/\delta_e)_{SS} = (V/g)(1/S)(\ddot{\theta}/\delta_e)_{SS} = (V/g)(1/\tau_{\theta_2})M_{\delta_e}/\omega_{SP}{}^2 \quad (21)$$

Using this expression, Eq. (18) reduces to

$$\omega_b{}^2 = V(1/\tau_{\theta_2})/(l_b - l_{cp}) \quad (22)$$

Finally then, the bobweight zeros can be described as

$$S^2 + [(1/\tau_{\theta_2})l_b/(l_b - l_{cp})]S + [(1/\tau_{\theta_2})V/(l_b - l_{cp})] \quad (23)$$

Using Eq. (23), the movement of the bobweight zeros as a function of l_b can be shown on the complex plane (see Fig. 5). The shape of the path which the zeros follow as l_b varies is a function of V and l_{cp}. If the airplane has a very large tail length ($l_{cp} \approx 0$), the path of the zeros is almost a vertical line $[\zeta_b\omega_b \approx \text{const} = \frac{1}{2}(1/\tau_{\theta_2})]$. If V is low and the tail length is short (l_{cp} large), the path curves more rapidly to the left. Unless l_{cp} is very large, however, the path of the zeros will nearly always pass to the right of the short-period poles.

The following two examples illustrate the effects of the possible zero locations on the closed-loop dynamics.

The T-38A Airplane

In the early 1960's, the T-38A airplane was introduced into service. This airplane had PIO problems in the low-altitude, high-speed regime with pitch damper inoperative, and a concentrated campaign was conducted to modify the control system.[3–5] Figure 6 shows the locus of the airplane's roots as the contribution of the bobweight to stick force per g is increased, together with a similar plot showing the effects of a modified control system. The particular closed-loop roots shown are those corresponding to the actual bobweight used in the T-38A.

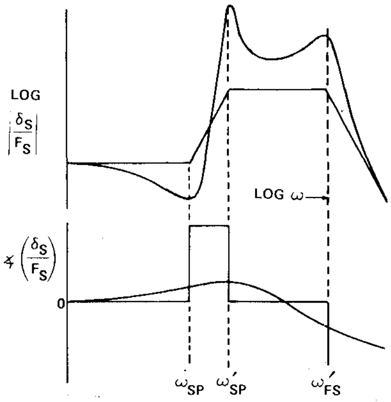


Fig. 7 Typical feel characteristics for $\omega_{SP}' > \omega_{SP}$.

Figure 6 shows that the original bobweight reduced the short-period damping ratio (stick-free) and increased the short-period frequency. A considerable part of the airplane's controllability problem was due to the reduced short-period damping ratio. However, the problem was aggravated by the poor stick-feel characteristics caused by the short-period frequency being greater stick-free than stick-fixed. This effect can be better understood by combining Eq. (7) with Fig. 2 to obtain the transfer function of stick position to stick force ($\tau_a' \approx \tau_a$)

$$\frac{\delta_S}{F_S} = \frac{n_b/F_S}{n_b/\delta_S} = \frac{(K_{bf}/K_bK_a)(S^2 + 2\zeta_{SP}\omega_{SP}S + \omega_{SP}{}^2)}{(S^2 + 2\zeta_{FS}'\omega_{FS}'S + \omega_{FS}'^2)(S^2 + 2\zeta_{SP}'\omega_{SP}'S + \omega_{SP}'^2)} \quad (24)$$

A typical δ_S/F_S Bode plot is shown in Fig. 7 for the case where $\omega_{SP}' > \omega_{SP}$. The fact that $|\delta_S/F_S|$ is higher at high frequencies than at low frequencies is believed to be the major source of pilot comments concerning light stick forces during rapid stick movements. Some of this increased amplitude is due to the resonance caused by low short-period damping ratio (stick-free), but the situation is further aggravated by the fact that the asymptotic Bode plot shows increased amplitude whenever the stick-free short-period and feel-system natural frequencies are greater than the stick-fixed short-period frequency. Notice that this latter effect is accompanied by stick position leading the stick force in phase, which is opposite to most feel characteristics.

The original T-38A control system was modified by simply reducing the size of the bobweight and increasing the size of the feel springs. The poles and zeros of the modified system are the same as those of the original system, except that the feel system natural frequency is increased because of the reduced control system inertia and increased feel spring size. Thus, no fundamental changes in the nature of the control system dynamics were made, but the severity of the problem was reduced by reducing $(F_b/n)_{SS}/(F_{FS}/n)_{SS}$ from 1.0 to 0.28.

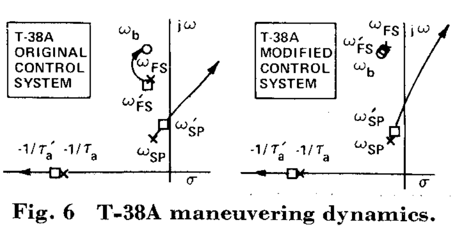


Fig. 6 T-38A maneuvering dynamics.

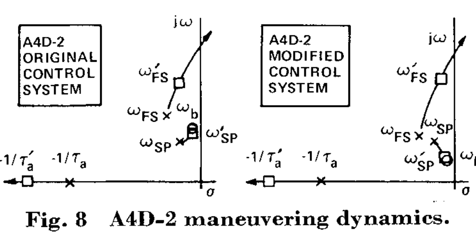


Fig. 8 A4D-2 maneuvering dynamics.

The A4D-2 Airplane

In the 1950's, a PIO problem occurred on the A4D-2 airplane during low altitude, high-speed flight.[6] The original control system of this airplane had much in common with the T-38A. The modifications to the A4D-2 to correct the PIO problem differed considerably from the approach employed on the T-38A, however, as will be shown below.

Figure 8 shows a root-locus plot of the A4D-2 dynamics as the bobweight's contribution to stick force per g is increased, together with a similar plot showing the effects of a modified control system which virtually eliminated the PIO problem. As with the T-38A airplane, the A4D-2 original control system resulted in a very low short-period damping ratio (stick free). The primary effect of the modified system was to move the bobweight zeros to a lower frequency (corresponding to an increase in the effective bobweight distance l_b), although the ratio $(F_b/n)_{SS}/(F_{FS}/n)_{SS}$ was also reduced from 1.8 to 0.9. It is interesting to note that both this ratio and the short-period damping ratio are greater for the A4D-2 (modified) than for the T-38A (modified) because of the improved bobweight zero location.

IV. Design Options

The design problem is to minimize the variations in stick force per g using large values of $(F_b/n)_{SS}/(F_{FS}/n)_{SS}$ while avoiding PIO problems due to low short-period damping (stick-free) or due to having the short-period frequency (stick-free) higher than the stick-fixed value.

As a first step, the previous analysis makes it clear that ω_b should be appreciably lower than ω_{SP} (l_b large). If the natural frequency of the zeros becomes too low, however, the short-period frequency (stick-free) will be reduced and will cause the airplane's response to become sluggish. A logical compromise, therefore, is to set the frequency of the bobweight zero equal to the appropriate minimum short-period frequency limit of MIL-F-8785B.[1] For satisfactory fighter maneuvering characteristics, this limit is in the form:

$$(\omega_{SP})_{\text{MIN}}{}^2 = 0.28(n/\alpha) \tag{25}$$

where n/α is the normal acceleration change per unit angle-of-attack change when the airplane is maneuvered at constant speed, and is very nearly equal to $[(V/g)(1/\tau_{\theta_2})]$. Setting $(\omega_{SP})_{\text{MIN}}{}^2 = \omega_b{}^2$, Eqs. (22) and (25) give

$$(1/\tau_{\theta_2})V/(l_b - l_{cp}) = 0.28[(V/g)(1/\tau_{\theta_2})] \tag{26}$$

and

$$l_b = 115 + l_{cp} \tag{27}$$

Note that the desired value of l_b is not likely to be obtained with a simple bobweight because of airplane size limitations. The blending of forward- and aft-mounted bobweights will therefore be required in most cases.

With the location of the bobweight zeros determined by Eq. (27), the primary limitation on $(F_b/n)_{SS}/(F_{FS}/n)_{SS}$ is the fact that the closed-loop feel-system roots can be driven unstable, as can be seen from Fig. 9. Fairly low values of ζ_{FS}' can be tolerated because ω_{FS}' will usually be high relative to ω_{SP}', so that the magnitude of the feel-system oscillations appearing in the maneuvering response will be small. But to prevent the desired values of $(F_b/n)_{SS}/(F_{FS}/n)_{SS}$ from actually driving ζ_{FS}' unstable, it may be necessary to increase ζ_{FS} by using a viscous stick damper in parallel with the feel springs. Of course, some viscous damper mechanizations may introduce additional feel-system dynamics, which must then be considered.

As can be seen from Fig. 9, the short-period damping ratio (stick-free) is likely to decrease somewhat as the contribution of the bobweight to stick force per g is increased, even for a favorable location of the bobweight zeros. Thus, it must be determined whether the final values of ζ_{SP}' obtained are adequate. Several requirements of MIL-F-8785B provide guidance in this area. For example, minimum values of ζ_{SP}' are established by paragraph 3.2.2.1.2, while paragraph 3.2.2.3.1 requires an increase in ζ_{SP}' when $(F_S/n)_{SS}$ is low. In addition, paragraph 3.2.2.2.1 establishes limits on $(F_S/n)_{SS}$ itself.

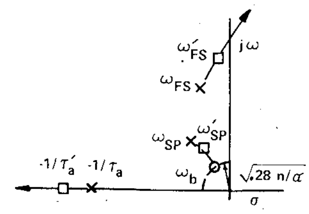


Fig. 9 Proposed bobweight zero location.

One final comment is in order. The design criterion for the selection of l_b is derived for a rather simple class of control systems, having only a second-order feel system and a first-order elevator actuator. For more complex control-system configurations, the criterion may not directly apply; but the basic analytical tools presented in this paper should be helpful in the design of a bobweight for any control system. For a more complete discussion of the effects of bobweights on airplane maneuvering dynamics, the reader is referred to the preprint Ref. 7 on which this article is based, and to the supporting document for MIL-F-8785B[2] (paragraph 3.2.2.3). Also included in this latter document is a discussion of the effects of elevator hinge moments, for airplanes having unpowered control systems. For a somewhat different approach to describing the effects of bobweight dynamics, the reader is referred to another CAL program.[8]

V. Conclusions

1) The use of a control-system bobweight without consideration of its effects on the airplane's dynamics can lead to serious PIO problems. 2) Potential PIO problems due to a bobweight can be minimized by increasing the sensitivity of the bobweight to pitch acceleration, using the following criterion: $l_b = 115 + l_{cp}$. 3) When the previous criterion is satisfied, the contribution of the bobweight to stick force per g may still be limited by the fact that the closed-loop feel-system roots can be driven unstable. This problem can usually be improved by the use of a viscous stick damper. 4) The final control-system design should be checked against the short-period damping ratio and stick force per g requirements of MIL-F-8785B.

References

[1] "Military Specification—Flying Qualities of Piloted Airplanes," MIL-F-8785B (ASG), Aug. 7, 1969.

[2] Chalk, C. R., Neal, T. P., Harris, T. M., et al., "Background Information and User Guide for MIL-F-8785B (ASG), Military Specification—Flying Qualities of Piloted Airplanes," AFFDL-TR-69-72, Aug. 1969, Air Force Flight Dynamics Lab., Wright-Patterson AFB, Ohio.

[3] Hirsch, D., "Investigation and Elimination of PIO Tendencies in the Northrop T-38A," SAE Paper, New York, July 1964.

[4] Finberg, Floyd, Lt. Col., "Report of the T-38 Flight Control System PIO Review Board," Feb. 15, 1963, Air Force Aeronautical Systems Div., Wright-Patterson AFB, Ohio.

[5] Ashkenas, I. L., Jex, H. R., and McRuer, D. T., "Pilot-Induced Oscillations: Their Cause and Analysis," Rept. NOR 64-143, June 20, 1964, Northrop Corp., Hawthorne, Calif.

[6] Terrill, W. H., Wong, J. G., and Springer, L. R., "Investigation of Pilot-Induced Longitudinal Oscillation in the Douglas Model A4D-2 Airplane," Rept. No. LB-25452, 15 May 1957, Douglas Aircraft Co., Long Beach, Calif.

[7] Neal, T. P., "The Influence of Bobweights on Pilot-Induced Oscillations," AIAA Paper 70-1002, Santa Barbara, Calif., Aug. 1970.

[8] Newell, F. D. and Wasserman, R., "In-Flight Investigation of the Effect on PIO of Control System Nonlinearities, Pitch Acceleration and Normal Acceleration Bobweights," AFFDL-TR-69-3, Jan. 1969, Air Force Flight Dynamics Lab., Wright-Patterson AFB, Ohio.