NACA

RESEARCH MEMORANDUM

AN ANALYSIS OF THE EFFECT OF STRUCTURAL FEEDBACK ON THE FLUTTER OF A CONTROL SURFACE HAVING A POWER-BOOST SYSTEM

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SUMMARY

In order to determine the causes of horizontal-tail flutter which had been observed on a fighter airplane, a wind-tunnel investigation was conducted of a production, half-span, horizontal tail. The longitudinal control system of the airplane comprised a radius nose, unbalanced elevator, and a full power-boost system. The flutter was unusual in that when it occurred the aerodynamic conditions were always the same, but it did not necessarily occur when these conditions were satisfied. This sporadic nature of the flutter was verified in the wind tunnel in that flutter occurred only once during the tests. Consequently, results of tests to determine potential causes were not considered conclusive.

An analytical study using an analogue computer was then conducted for a simplified system. The effects of several factors were considered and it was found that a structural feedback caused by stabilizer twist was a destabilizing factor. Further, structural feedback of the order of magnitude determined from the wind-tunnel results caused a significant reduction in flutter speed.

The suggested method of prevention is to make the control system as stable as possible without depending upon the restraint of the power-boost system, or to reduce the structural feedback.

INTRODUCTION

A wind-tunnel investigation of the horizontal tail of an operational fighter airplane was conducted to determine the cause or causes of flutter which had been observed in flight. It was also observed in flight that flutter occurred only under certain flight conditions (altitude not over 1,000 feet and velocity about 510 miles per hour), but did not always occur when these conditions were met. Since there were no signs of flutter at higher Mach numbers and altitudes (but at lower dynamic pressure), it appeared that the single-degree-of-freedom transonic type of flutter was not involved. Also, it was felt that buffeting from the wing wake was not a factor since the horizontal tail lies well above the wing wake.

The horizontal tail of this airplane was 7 percent thick, had a 25-percent-chord, radius-nose elevator unbalanced both statically and dynamically, and was controlled by a full power-boost system.

A two-dimensional analysis made by the manufacturer in accordance with Air Force requirements indicated marginal stability at sea level for the speed range of the airplane. A three-dimensional analysis indicated a greater margin of stability at 700 miles per hour but did indicate a flutter speed of 795 miles per hour.

The investigation reported herein consisted of two phases: first, the wind-tunnel tests conducted in the Ames 16-foot high-speed wind tunnel; and second, an analytical study using an analogue computer.

NOTATION

b	critical damping of boost, pound-seconds
$f(x)$	mode-shape function relating torsional deformation at any spanwise station to the deformation at the tip
l	span of stabilizer and elevator, feet
r	lever arm of boost, feet
t	time, seconds
x	spanwise coordinate, feet
$y(t)$	incremental displacement of boost cylinder, inches
$z(t)$	incremental displacement of control valve, inches
C	section torsional stiffness, pound-inches squared
F_α	feedback amplitude parameter, inches per foot-pound
I	section moment of inertia, slug-feet

J	inertia coupling between stabilizer and elevator, slug-feet
K	boost stiffness parameter, foot-pounds per inch
M	aerodynamic moment loading, pounds
T	time lag of feedback, seconds
V	free-stream velocity, feet per second
α	twist of stabilizer relative to root, radians
$\alpha(t)$	twist of stabilizer at tip, radians
β	twist of elevator relative to stabilizer, radians
$\beta(t)$	twist of elevator at tip, radians
δ	logarithmic decrement of damped sinusoidal wave
ζ	boost-damping ratio

Subscripts

α	complete surface about elastic axis
β	elevator about its hinge line

MODEL AND INSTRUMENTATION

The test vehicle for the wind-tunnel program consisted of the empennage assembly from a production airplane. This assembly comprised the horizontal stabilizer, the portion of the vertical stabilizer connecting the fuselage and the horizontal stabilizer, the elevators, and the rear part of the fuselage containing the control devices (except the hydraulic power source and the pilot's stick). The left horizontal stabilizer and elevator outboard of their intersection with the vertical stabilizer were removed and fittings were installed for mounting the assembly at this point. Photographs of the installation are shown in figures 1 and 2. The installation was made so that the right elevator and as much as practicable of the right horizontal stabilizer were exposed to the air stream. The principal support was at the top of the wind tunnel and was made as rigid as was practical. The support at the forward end of the fuselage tail cone (fig. 2) was principally for supporting the weight of this portion of the fuselage. The stabilizer angle of attack was 0.5° for all tests.

In order to complete the simulation of aircraft operating conditions, a hydraulic test stand was used to provide a source of hydraulic power and a dummy control stick was provided. This stick was provided with stops to restrain its motion.

A schematic drawing of the elevator control system is shown in figure 3 and photographs showing the hydraulic cylinder, valve, and control quadrant are presented in figure 4. It can be seen from figure 3 that the system was of the full-power-boost type since the pilot's stick moved only the control valve, which in turn actuated the hydraulic cylinder in such a direction that the valve was returned to neutral.

The instrumentation comprised accelerometers, position indicators, and strain gages. Nine accelerometers were mounted within the horizontal stabilizer at three spanwise stations and one was mounted at a point estimated to be the node in the torsion and second bending mode. The location of these accelerometers is shown in figure 5. Two types of accelerometers were employed, a commercial product (MB pickup) and the NACA accelerometer. The MB pickup operated on the principle of a spring-restrained coil moving in a permanent magnetic field, the natural frequency being of the order of 2-1/2 cycles per second. The NACA accelerometer operated on the principle of a spring-restrained mass with a strain gage measuring the stress of the spring. The natural frequency of this type was of the order of 400 cps. The frequencies encountered during the tests were about 35 cps, thus being well removed from the resonant frequencies of both types of accelerometer. Calibrations were made of each instrument for frequencies from 10 to 50 cps on a shake table.

Two slide-wire position pickups were mounted between the stabilizer and elevator to measure elevator deflection. The spanwise locations of these are indicated in figure 5 and a close-up of the installation near the tip is shown in figure 6. These pickups had resolutions better than 0.1°.

Three additional position pickups were installed as shown in figure 3 to measure deflections in the elevator control system. The resolution of these pickups was 0.003 inch.

A strain gage was mounted on the elevator torque tube to measure the elevator hinge moment.

The outputs of all the above-mentioned instruments were fed to two recording oscillographs. The records of these oscillographs were correlated by means of time signals introduced into one channel of each unit.

As a safety precaution to prevent excessive elevator motion or failure in the event of flutter, adjustable stops were provided on both surfaces at two spanwise positions and also on the torque-tube bell crank. One of these stops may be seen in figure 6.

TEST PROCEDURE AND RESULTS

The first test in the wind tunnel was performed by increasing the Mach number from 0.4 to 0.82 in steps, the latter value being the choking Mach number. No signs of flutter were encountered during this test. The second test was identical to the first except that after each Mach number was reached the elevator was vibrated by means of a variable-frequency vibrator mounted on the elevator bell crank and having a static unbalance of 3/4 inch-pound. The frequency was set at the various resonant frequencies which had been determined in still air. During this second test a distinct flutter developed at a Mach number of approximately 0.78 as the speed was being changed and without the vibrator operating. The dynamic pressure was 625 pounds per square foot, the same as during the flutter experienced in flight. This correspondence of dynamic pressure is a significant indication that the phenomenon is primarily dependent on the dynamic pressure rather than the Mach number.

In an attempt to determine the cause of flutter, a process of elimination was employed in the subsequent tests. Thus, one at a time, conditions which conceivably could influence the onset of flutter were eliminated as much as possible. The conditions considered included the following:

1. Hydraulic system operative but with the elevator locked by closing adjustable stops at the elevator horn

2. Changes in initial elevator displacement

3. Air introduced into the hydraulic system

4. Changes in free play at elevator-horn-hydraulic-cylinder connection to simulate effect of wear

Subsequent tests, with each of the above factors altered or eliminated, failed to produce flutter with one exception. This exception occurred when the free play of the elevator was progressively increased from $\pm 0.03^{\circ}$ to $\pm 0.68^{\circ}$ by installing various sized bolts to connect the elevator-control horn and the hydraulic cylinder. With the maximum free play, flutter occurred at about 0.2 Mach number and was of the stabilizer-bending-elevator-rotation type. As the free play was reduced the speed at which flutter occurred increased and when the free play was reduced to, or less than, the manufacturer's tolerance of $1/8^{\circ}$ total, no flutter occurred. These results point out the critical effect of free play on the flutter behavior, but do not indicate the cause of the flutter under study as it is known that the free play was less than $1/8^{\circ}$ total. In addition, these results indicate that the elevator-stabilizer system alone (i.e., without the boost-system restraint) is unstable. It can readily be seen, then, that if the restraint system tends to, or actually does, become unstable the aerodynamic system (stabilizer and elevator) will not provide any stabilizing effect. Considered from another viewpoint, the aerodynamic system will extract energy from the air if given the opportunity and is normally prevented from doing this only by the action of the boost system.

Because of the failure of the various modifications to influence the occurrence of flutter, it was considered advisable to repeat the test in which flutter occurred. When this was done no flutter occurred and several attempts to induce flutter also failed.

Thus, out of several tests under as nearly identical conditions as could be established, only one produced flutter. In view of this, the results of tests to determine the effects of various factors cannot be considered indicative. It is worthy of note that the behavior of the test vehicle and that of the airplane were similar with respect to intermittent occurrence of flutter.

Analysis of the oscillograph records taken during flutter furnishes some information on the behavior of the elevator and the power-boost system. In figure 3 are noted the amplitudes of motion indicated by the position pickups. Also noted is the angular displacement amplitude of the inner end of the elevator and the amplitude of the hinge moment. The directions of motion indicated in the figure are those considered positive in relation to the signs of the amplitudes. The double-sign notation is employed since all the motions were either nearly in phase or nearly out of phase. Note that the valve rod - hydraulic cylinder relative travel was 180° out of phase from the other motions. No signs are noted on the hinge-moment amplitude since the phase of this quantity relative to the motions was not known due to failure of the time synchronization device. In order to obtain a complete picture of the motions which took place, it is necessary to know the motion of the elevator horn which, in turn, requires that it be known whether the boost system was driving the elevator or vice versa. Consideration of the hinge-moment amplitude and the characteristics of the boost system indicates the answer to this question. In order for the elevator to be driving the boost system with a hinge moment of 210 foot-pounds, the frequency of oscillation would have to be near the resonant frequency of the boost system. However, information from the manufacturer based on experimental tests indicates that the resonant frequency was considerably greater than the frequency of flutter (36 cps). Since the potential output of the boost system was about 1750 foot-pounds for the measured valve motion, it is evident that the boost system was driving the elevator during the observed flutter. Then, using the measured hinge moment to determine the twist of the torque tube, the motion of the elevator horn was calculated.

In order for the boost system to drive the elevator, it is necessary for something to cause the valve rod to move. If the structural system between the elevator hinge line and the control quadrant was perfectly rigid, such a motion could not occur since no disturbance was introduced into the control quadrant. Comparison of the various motions measured will show that inconsistencies are present which can be explained only by admitting the existence of structural deformation.

If it is assumed that the control quadrant was fixed and there was no structural deformation, then the motion of the control valve relative to the hydraulic cylinder (position pickup A) would be equal to the motion of the elevator horn. The motion of pickup A has been shown to have been ±0.02 inch. Correction of the measured elevator motion for twist of the torque tube, assuming the boost to be driving the elevator, indicates a motion of ±0.08 inch as in figure 3. Thus, the sum of these motions was not zero. Rather, the sum was ±0.06 inch. The motions measured by pickups B and C are seen to have been ±0.09 inch and ±0.04 inch, respectively, which are of the same order of magnitude as the discrepancy noted above. Consequently, it is apparent that a structural deformation was occurring. The nature of this deformation is not susceptible to analysis, but it is assumed to have been the result of stabilizer torsion. The basis for this assumption is that since the structure (vertical stabilizer) which connected the elevator hinge line and the control quadrant also supported the horizontal stabilizer, the loads causing deformation of this structure must have come initially from the horizontal stabilizer. Since the deformation under consideration is one which caused a variation of the distance between the elevator hinge line and the control-quandrant axis, the imposed load would appear to have been a torsion load on the horizontal stabilizer. For any given loading condition, the torsional deformation of the horizontal stabilizer would have been proportional to this load. Thus a proportionality was assumed between the twist of the stabilizer and deformation of the vertical stabilizer.

Nothing was found in the literature on flutter which treated a system of this type. The uniqueness of this system lies in inclusion of a servo-control system with the aerodynamic and structural characteristics.

Although the above discussion of the test results shows that the control system was not acting entirely as a rigid control, this fact alone is not sufficient proof that the structural deformation was the cause of the flutter.

ANALYTICAL STUDY

In view of the test results showing that the deformations of the vertical stabilizer were of significant amplitude, an analytical study was conducted to indicate the effect of structural feedback on the stability of the system. The study was conducted with the aid of an electronic analogue computer from which time histories of the responses to impulse disturbances were obtained for analysis.

In figure 7 are shown the geometric and physical characteristics of the horizontal stabilizer-elevator combination as formulated for the analysis. An untapered surface was assumed, the chord of which equaled that of the test vehicle at approximately the 75-percent-span location. Both surfaces were assumed to have uniform structural characteristics, that is, they were uniform beams. The stabilizer was assumed to be fixed at its root and the elevator was considered to be restrained by the boost, thus permitting motion at this point. These and other simplifications to be mentioned were made to facilitate the analysis. It was recognized that the results would be of a qualitative nature, but this was felt to be sufficient to indicate the source of flutter.

The mass per unit span of the hypothetical stabilizer and of the elevator was the same as those for the test vehicle at the 75-percent-span location. The section spring constants C_α and C_β were determined from the mass characteristics assumed and the natural frequency measurements made on the test vehicle in still air by means of a forced-vibration technique. During these measurements the boost system was operative, that is, it was supplying the elevator restraint and normal hydraulic pressure was maintained. Thus the frequencies measured included the effects of the stiffness of the hydraulic boost. However, simple calculations indicate that the natural frequencies would be nearly the same under conditions of rigid restraint owing to the large stiffness of the boost.

In the analysis to be presented the bending degree of freedom has not been considered since the test results showed the flutter to be of the stabilizer torsion-elevator rotation type.

Considering elements of span dx of the stabilizer plus elevator and of the elevator, the equations of dynamic equilibrium about the elastic axis and hinge line, respectively, are:

$$- I_\alpha \frac{\partial^2\alpha}{\partial t^2} dx - J \frac{\partial^2\beta}{\partial t^2} dx + C_\alpha \frac{\partial^2\alpha}{\partial x^2} dx + M_\alpha dx = 0 \qquad (1)$$

$$- I_\beta \frac{\partial^2\beta}{\partial t^2} dx - J \frac{\partial^2\alpha}{\partial t^2} dx + C_\beta \frac{\partial^2\beta}{\partial x^2} dx + M_\beta dx = 0 \qquad (2)$$

The terms M_α and M_β represent the aerodynamic moment loading (moment per unit span) about the elastic axis and hinge line, respectively. Expressions for these quantities contain some terms dependent upon the complex function $C(k)$ which, in turn, is a function of the frequency of oscillation at a given airspeed. Values of $C(k)$ given in reference 1 are for two-dimensional flow. In reference 2, a method is given for determining $C(k)$ for three-dimensional flow. This method was employed in the present study. It should be noted that the unsteady aerodynamic coefficients are derived from incompressible-flow theory but are considered a satisfactory approximation for the analysis at hand.

Returning to consideration of equations (1) and (2), it is seen that they are partial differential equations in both x and t. That is, in general, α and β will be dependent upon both x and t. The customary procedure in flutter analysis is to assume the x dependency to be that which exists under still-air conditions (M_α=M_β=0) even though the frequency may be different under conditions of flutter. The choice between coupled or uncoupled (J=0) modes is a subject of considerable research and study but for this analysis, for reasons of simplicity, the uncoupled modes will be used. Thus, only the first and third terms of equations (1) and (2) remain. Solutions of these modified equations which also satisfy the boundary conditions of the problem will indicate the possible relations between α, β, and x which are termed the mode shapes.

Considering now the boundary conditions, it is seen that for the stabilizer the rate of change of twist with respect to x, $\partial\alpha/\partial x$, which is proportional to the moment acting at any section, must be zero at the tip since the tip is free, and α must be zero at the root since the root is fixed. The boundary condition for the elevator at its tip is that $\partial\beta/\partial x$ be zero for the same reason as for the wing. At the root of the elevator, however, two conditions must be satisfied to allow for action of the boost. First, $\partial\beta/\partial x$ must be finite since there will be a moment applied by the boost, and second, β must be finite since the moment applied by the boost will result in elevator displacements.

Then solving equations (1) and (2), as modified in the preceding discussion and for the above boundary conditions, it is found that for the stablizer the possible mode shapes will be combinations of one or more torsional modes corresponding to a fixed root. For the elevator, however, at least two modes must be present, one corresponding to the fixed-root condition and one corresponding to the free-root condition. For the remainder of this report, the first type will be termed the rigid-restraint mode, the second type, the free-surface mode. For the analysis performed, only the fundamental mode of stabilizer torsion and the fundamental modes of the two necessary types of elevator torsion were considered, that is,

$$\alpha = \left[f_{\alpha_1}(x)\right] \alpha_1(t)$$

$$\beta = \left[f_{\beta_1}(x)\right] \beta_1(t) + \left[f_{\beta_2}(x)\right] \beta_2(t) \tag{3}$$

The subscript 1 will be assigned to the rigid-restraint modes and the subscript 2 to the free-surface mode. The time-dependent functions $\alpha_1(t)$, $\beta_1(t)$, and $\beta_2(t)$ represent the motions of the tip in the designated modes.

Since the x dependent functions have now been specified, equations (1) and (2) may now be written as ordinary differential equations as follows:

$$-I_\alpha[f_\alpha(x)] \frac{d^2\alpha(t)}{dt^2} dx - J \left\{ \left[f_{\beta_1}(x)\right] \frac{d^2\beta_1(t)}{dt^2} + \left[f_{\beta_2}(x)\right] \frac{d^2\beta_2(t)}{dt^2} \right\} dx +$$

$$C_\alpha \frac{d^2[f_\alpha(x)]}{dx^2} \alpha(t)dx + M_\alpha\, dx = 0 \tag{4}$$

$$-I_\beta \left\{ \left[f_{\beta_1}(x)\right] \frac{d^2\beta_1(t)}{dt^2} + \left[f_{\beta_2}(x)\right] \frac{d^2\beta_2(t)}{dt^2} \right\} dx - J\, [f_\alpha(x)] \frac{d^2\alpha(t)}{dt^2} dx +$$

$$C_\beta \left\{ \frac{d^2\left[f_{\beta_1}(x)\right]}{dx^2} \beta_1(t) + \frac{d^2\left[f_{\beta_2}(x)\right]}{dx^2} \beta_2(t) \right\} dx + M_\beta\, dx = 0 \tag{5}$$

These equations are applicable to an element of span dx of the stabilizer and elevator and may be solved for any spanwise station. However, solutions for different spanwise locations should not be expected to indicate the same degree of stability. It is desirable, of course, to obtain an average solution which will give the most accurate indication of the over-all stability of the aerodynamic system. The simplest method of averaging would be to integrate equations (4) and (5) with respect to x. However, if this is done, the integrals of the free-surface mode shape $f_{\beta_2}(x)$ will vanish since it represents a vibration with an average spanwise amplitude of zero. Such a result would indicate that this mode has no effect on the stability of the system, and further that the action of the boost has no effect inasmuch as this mode results from allowing for action of the boost. These indications seem implausible. However, if a weighted average is obtained by multiplying equations (4) and (5) by some function of x before integrating, the free-surface modes can be retained. If the weighting function for equation (4) is taken to be the mode shape itself $f_{\alpha_1}(x)$, the equation, before integration, represents the ratio of the rate of change of energy of the element to the angular velocity of the reference station. After integration with respect to x, the equation represents the total rate of change of energy about the elastic axis. It is seen that this method of averaging has a physical interpretation. To retain this interpretation, equation (4) should be multiplied by the angular velocity of the element and then be divided by a reference angular velocity. Since there are two modes of elevator motion present, however, there will not be a common angular velocity. As a result the equation would become nonlinear. To circumvent this difficulty the weighting function for equation (5) was taken to be the rigid-restraint mode $f_{\beta_1}(x)$, the assumption being that this mode will be dominant. This assumption was borne out by the analytical results.

The question of weighting functions can be considered from the viewpoint that weighting the equations is equivalent to considering the stability of some sections to have greater influence than others on the over-all stability. As a consequence, the over-all stability will be equivalent to that of some representative spanwise station. The representative station for the simple averaging process will be the mode point of the free-surface mode (midspan); whereas for the weighted average used, the representative station is at approximately 70-percent span.

Equations (4) and (5) will now be reduced to a solvable form except that there are three unknowns, $\alpha(t)$, $\beta_1(t)$, and $\beta_2(t)$. The additional relations will be obtained from the boundary conditions.

The moment acting at the root of flap can be related to the amplitude of the rigid-restraint mode as follows:

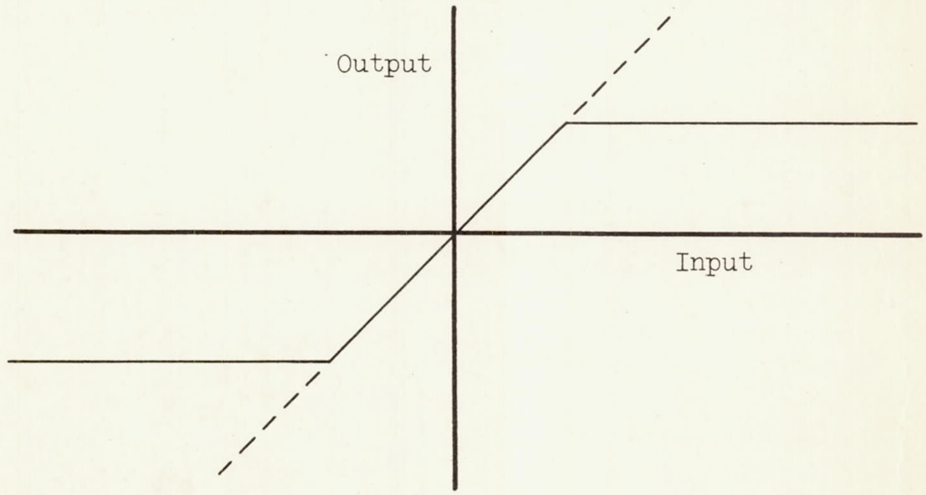
$$\text{Moment} = -\,C_\beta \frac{d\left[f_{\beta_1}(x)\right]}{dx} \beta_1(t) \qquad x = 0 \tag{6}$$

This moment is equal to the output of the boost system so it will be necessary now to consider its performance characteristics. In figure 3, let the displacement of the valve rod relative to the control-quadrant axis be Z and that of the hydraulic cylinder, Y. Considering z and y to be incremental values of Z and Y, respectively, the output of the boost is assumed to be determined by the following relationship:

$$\text{Moment} = K(z-y) + b\zeta \left(\frac{dz}{dt} - \frac{dy}{dt}\right) \tag{7}$$

This is an idealized assumption in that it assumes linearity and a first-order system.

Consider for the moment that there is no input signal to the boost, that is, z is zero. The boost output will then be determined solely by y which can be related to the elevator displacement at its root. This behavior would then correspond to having the elevator restrained by a spring of stiffness K and with damping equal to $b\zeta$ where b is the critical-damping constant and ζ the damping ratio. Therefore, the factor K will be termed the boost stiffness parameter and ζ the boost-damping ratio. The only information available on the performance characteristics of the boost device was the static input-output relationship. This relationship was idealized as indicated by the following sketch:



It is seen that over a range of input signals the output is proportional to the input, and for inputs outside this range the output is constant. The constant of proportionality in the first range, then, is the boost stiffness parameter. For the analysis the break in the curve was not considered, that is, the performance curve was assumed to continue as indicated by the dotted line. The effect of the flat portion of the curve would be to reduce the effective proportionality constant. Thus, by considering smaller values of the boost stiffness parameter, it was felt that this effect would be included. Although it was known that the natural frequency of the boost was considerably greater than the flutter frequency encountered, it is possible that there was attenuation of the static performance characteristics. Thus, consideration of a smaller value of the boost stiffness parameter can be considered to represent this effect.

Inasmuch as the damping characteristics of the boost were not known, a range of values was considered by varying the damping ratio ζ. The reference damping constant was obtained by considering the load on the boost to be pure inertia (i.e., nonelastic surface). Then equation (7) becomes

$$K(z-y) + b\zeta\left(\frac{dz}{dt} - \frac{dy}{dt}\right) = \frac{I_\beta l}{r}\frac{d^2y}{dt^2}$$

Then if z is assumed to be zero, that is, no input signal, the above equation can be written in the general form

$$\frac{d^2y}{dt^2} + 2\zeta\omega\frac{dy}{dt} + \omega^2 y = 0$$

where ζ is termed the "damping ratio." Assuming ζ to be unity,

$$b = 2\sqrt{\frac{K\ I_\beta l}{r}}$$

It can be seen by reference to figure 3 that the displacement y will be related to the elevator motion at the root as follows:

$$y(t) = r\beta_{x=o} = -\ r\beta_2(t) \tag{8}$$

In order to obtain a complete system of equations, it is necessary to have a relationship defining z. In view of the earlier discussion of the experimental results, the following relationship was assumed:

$$z(t) + \frac{dz(t)}{dt}T = F_\alpha C_\alpha \frac{\partial\alpha}{\partial x} \qquad (x = 0,t) \tag{9}$$

The product $C_\alpha \frac{\partial\alpha}{\partial x}$ (x=0,t) is equal to the torsional moment acting at the root of the stabilizer. The factor F_α will be termed the "feedback amplitude parameter." The time lag T is included to allow for the fact that disturbances require a finite time to propagate through the structure. Equation (9) can be interpreted as relating the feedback signal z to the torsional deformation α since $\partial\alpha/\partial x$ will be proportional to α. This interpretation will permit comparison of the analytical and experimental results.

In summary, equations (4), (5), (6), (7), (8), and (9) are the simultaneous equations the solution of which will determine the stability of the system. Such solutions were obtained by means of an electronic analogue computer from which time histories of the response to a pulse-type disturbance were obtained. These responses were analyzed to obtain the logarithmic decrement of the dominant oscillation. For all the results to be presented the frequency of this oscillation was approximately 50 cps.

The parameters considered were feedback amplitude parameter F_α, velocity V, time lag T, boost stiffness parameter K, and boost-damping-ratio ζ. Except where noted otherwise, all results were obtained for zero time lag and a boost-damping ratio of 0.4.

The system was first considered with locked elevator and no structural feedback ($z=\beta_2=0$) at several velocities from 420 to 820 feet per second and a density of 0.00161 slugs per cubic foot corresponding to that in the wind tunnel at the occurrence of flutter. This was done to establish the effect of speed on the aerodynamic damping of wing-flap combination for the purpose of comparison with the test results, and also to establish a basis to which the subsequent results could be referred. The results are shown in figure 8 by the curve labeled $K = \infty$, $F_\alpha = 0$. On this curve, as well as the remaining figures, points below the axis represent a stable condition, points above an unstable condition, and points on the axis a neutrally stable or flutter condition. The curve labeled $F_\alpha = 0$ represents the condition of restraint being supplied by the boost ($K = 160{,}000$ foot-pounds per inch) and no feedback. Comparison of these two curves shows that the flutter speed is the same although there are differences in stability at lower speeds.

The remaining curves of figure 8 are for several values of the feedback amplitude parameter and indicate a significant reduction in flutter speed due to structural feedback. The value of F_α derived from the experimental measurements is approximately 10×10^{-6}, considerably greater than any of the values shown in figure 8.

Results similar to those shown in figure 8 were obtained for boost stiffness parameters of 120,000 and 80,000 foot-pounds per inch. The effect of feedback amplitude parameter was similar to that shown in figure 8; therefore these results are not presented in this form, but rather as shown in figure 9. In this figure, the effect of changing the boost stiffness parameter is shown for two representative values of the feedback amplitude parameter. It will be noticed that for zero feedback reducing the boost stiffness from 160,000 to 120,000 foot-pounds per inch does not change the flutter speed, and it appears that the same is true when the stiffness is further reduced to 80,000.

Considering now the curves of figure 9 for a feedback amplitude parameter of 4×10^{-6}, it is seen that as the boost stiffness is reduced the flutter speed is reduced. This trend is believed to be due to the presence of a relatively greater amount of the free-surface mode than was the case for zero feedback. For the zero feedback condition, the amplitude of the free-surface mode of the elevator is less than 5 percent of the amplitude of the rigid-restraint mode. On the other hand, for the conditions represented by these curves the amplitude of the free-surface mode is approximately 30 percent of that of the rigid-restraint mode and, in addition, this ratio increases as the boost stiffness decreases. It is reasoned that this amount of the free-surface mode is significantly destabilizing since, in general, a free surface is less stable than a fixed surface. It might seem that as the boost stiffness is reduced the effect of feedback, which has been shown to be destabilizing, would be reduced and thus, other factors remaining unchanged, there would be an increase in stability. However, since the free-surface mode increases as the boost stiffness decreases, it appears that the effect of this mode is predominant. This is not to imply that structural feedback is less important than the boost stiffness insofar as affecting the flutter speed is concerned. Rather the opposite is the case, as can be seen from figures 8 and 9.

The effect of time lag of the feedback signal at two velocities is shown in figure 10. The lower velocity considered here is approximately that for maximum stability with zero time lag and the higher velocity is that for an unstable condition. (See fig. 8.) It is seen that the effect of time lag is essentially the same for both of these velocities. A time lag of 0.001 second represents a phase lag of approximately 20° at 50 cps, or it can be interpreted as the time required for sound to travel approximately 20 feet in solid aluminum. It is concluded that time lag has little effect.

The effect of the boost-damping ratio is shown in figure 11 for the same velocities and boost stiffness parameter as in figure 10, but for a different value of F_{α}. The results show that the damping ratio has little effect, especially for values greater than 0.2.

The results of this analytical study show that of the parameters considered the one having the greatest influence on the flutter speed was the feedback amplitude. The feedback amplitude parameters considered in the analysis were smaller than, but of the same order of magnitude as, that indicated by the experiments. Thus there is a strong indication that this phenomenon was the cause of the flutter experienced at the unexpectedly low speed. The objection may well be raised that the observed phenomenon was sporadic in its appearance under supposedly identical conditions, whereas the method of analysis requires that the phenomenon always occurs under specified conditions. The very fact that the observed flutter was sporadic both in flight and under laboratory conditions and even under deliberate attempts to induce it is an indication that the characteristics of the system are nonlinear. For example, the performance of the boost or the stiffness of the structure may be nonlinear. Another manifestation would be variable coupling of the modes present during flutter. As a result of these possibilities, the response of the system could depend upon the amplitude and/or the type of disturbance which must exist to initiate flutter. In view of these complexities, it is evidently impractical to perform an analysis, even a nonlinear one, which will consider all these possibilities, assuming that they could be expressed analytically. Thus, the most to be expected of a linear analysis is an explanation of the fact that the flutter speed is lower than that predicted by conventional analysis.

Inasmuch as the analysis shows that feedback of the magnitude indicated by experiment will reduce the flutter speed by a significant amount, it is felt that structural feedback is a very possible cause of the flutter.

In order to minimize or eliminate the effect of structural feedback on the occurrence of flutter, the initial stability should be increased or the feedback decreased. The first method requires that the flutter speed, neglecting structural feedback, be as high as possible so that the energy required to produce flutter in the design speed range be increased. The second method requires either a more rigid structure or care in locating the boost device.

CONCLUSIONS

The results of experimental and analytical investigation of stabilizer torsion-elevator rotation flutter of a production horizontal tail indicate the following conclusions:

1. The flutter observed in flight and during the experimental investigation was sporadic and occurred under similar conditions of dynamic pressure.

2. Structural feedback in conjunction with a power-boost device can be a destabilizing factor.

3. In order to minimize the possibility of this type of flutter the stability of the system, neglecting feedback, should be made as great as possible, and the effect of feedback should be reduced by careful design.

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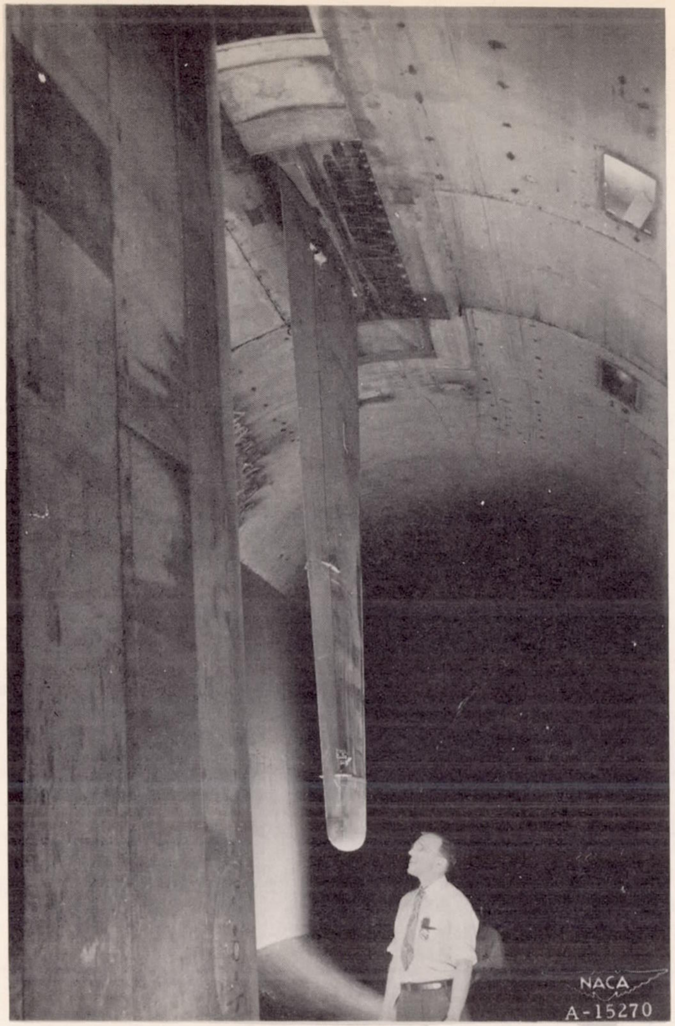
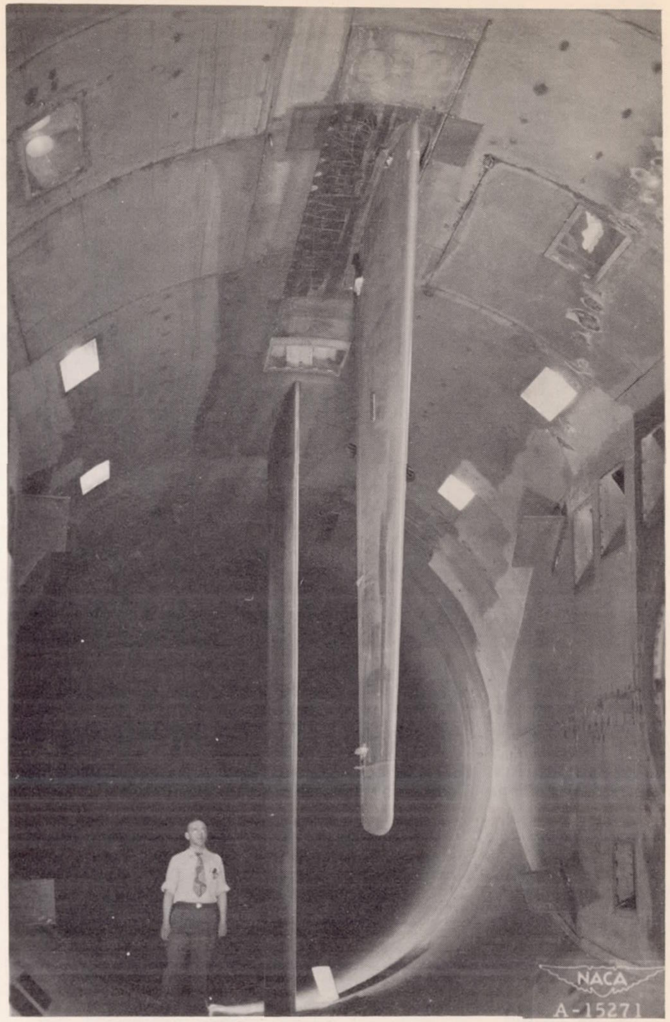
2. Jones, Robert T.: The Unsteady Lift of a Wing of Finite Aspect Ratio. NACA Rep. 681, 1940.

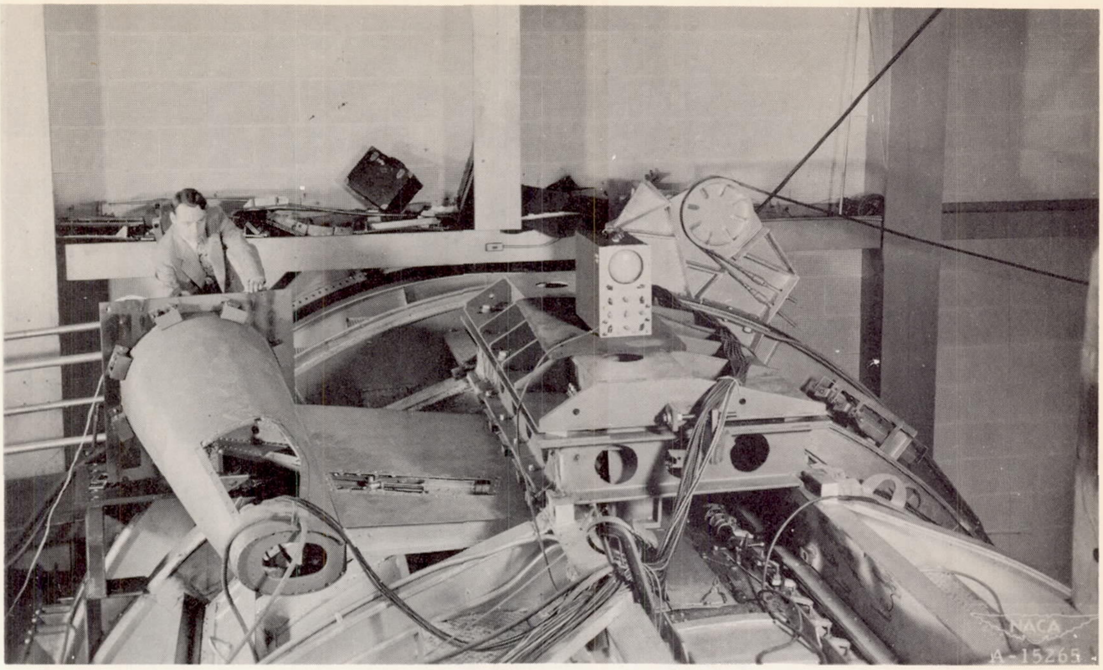


(a) Rear view.

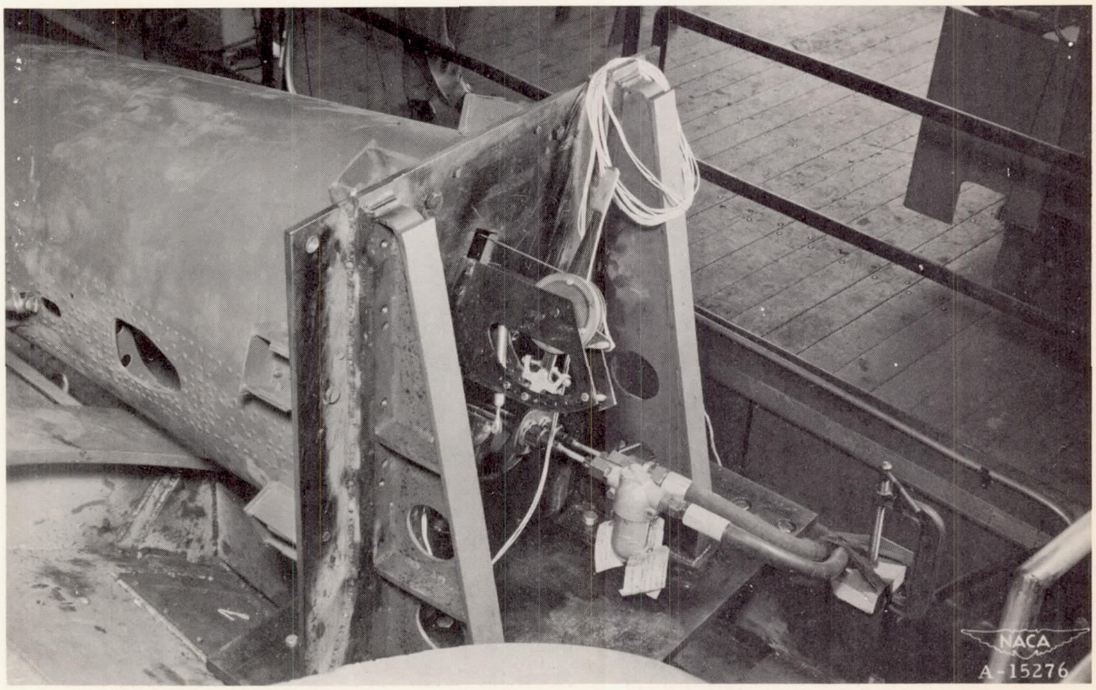


(b) Front view.

Figure 1.– The stabilizer installed in the wind tunnel.



(a) General view.



(b) Detail view.

Figure 2.- The supporting structure for the stabilizer.

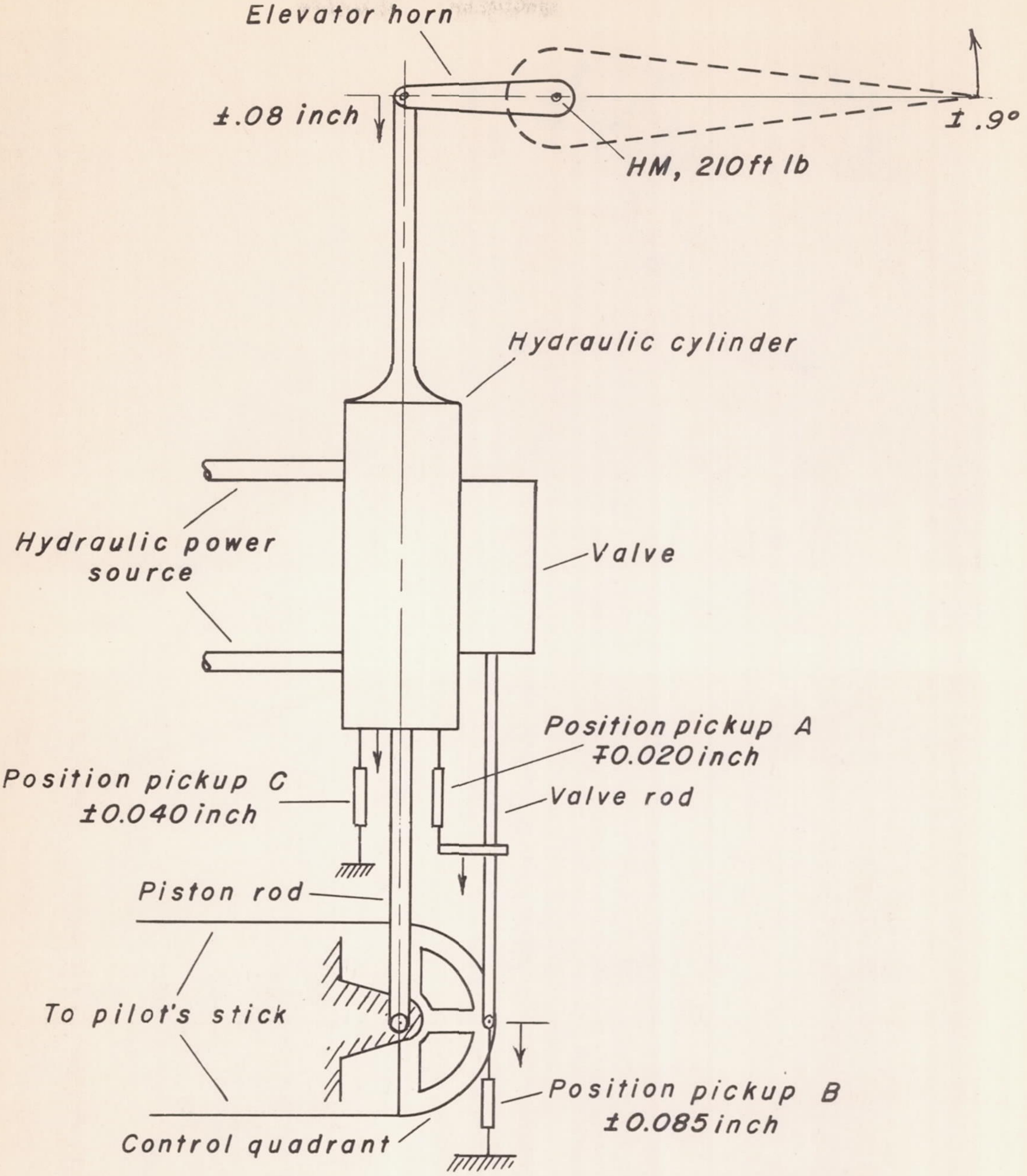
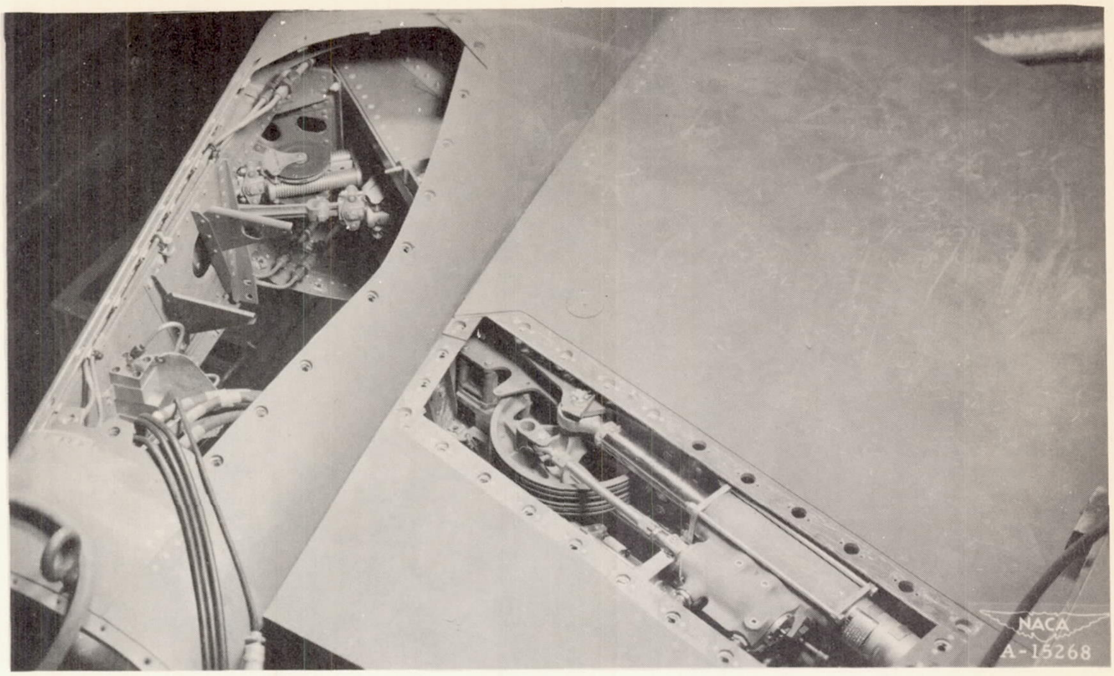
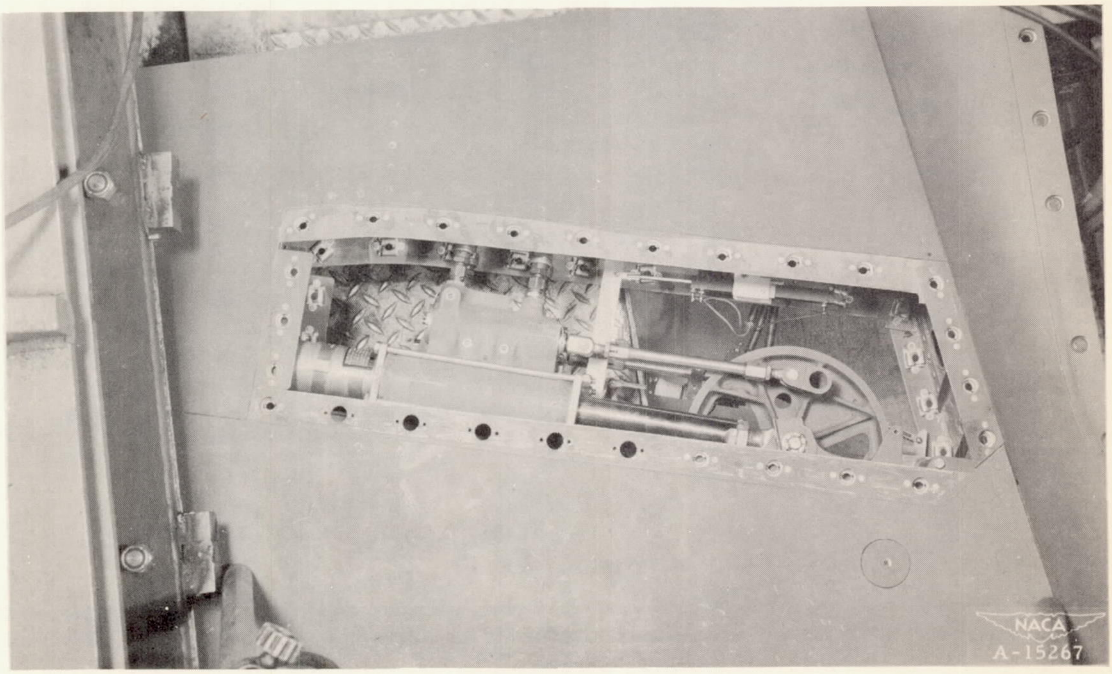


Figure 3.- Schematic diagram of elevator control system, test instrumentation, and amplitudes of motion measured during flutter.



(a) Three–quarter rear view.



(b) Side view.

Figure 4.– The hydraulic control system.

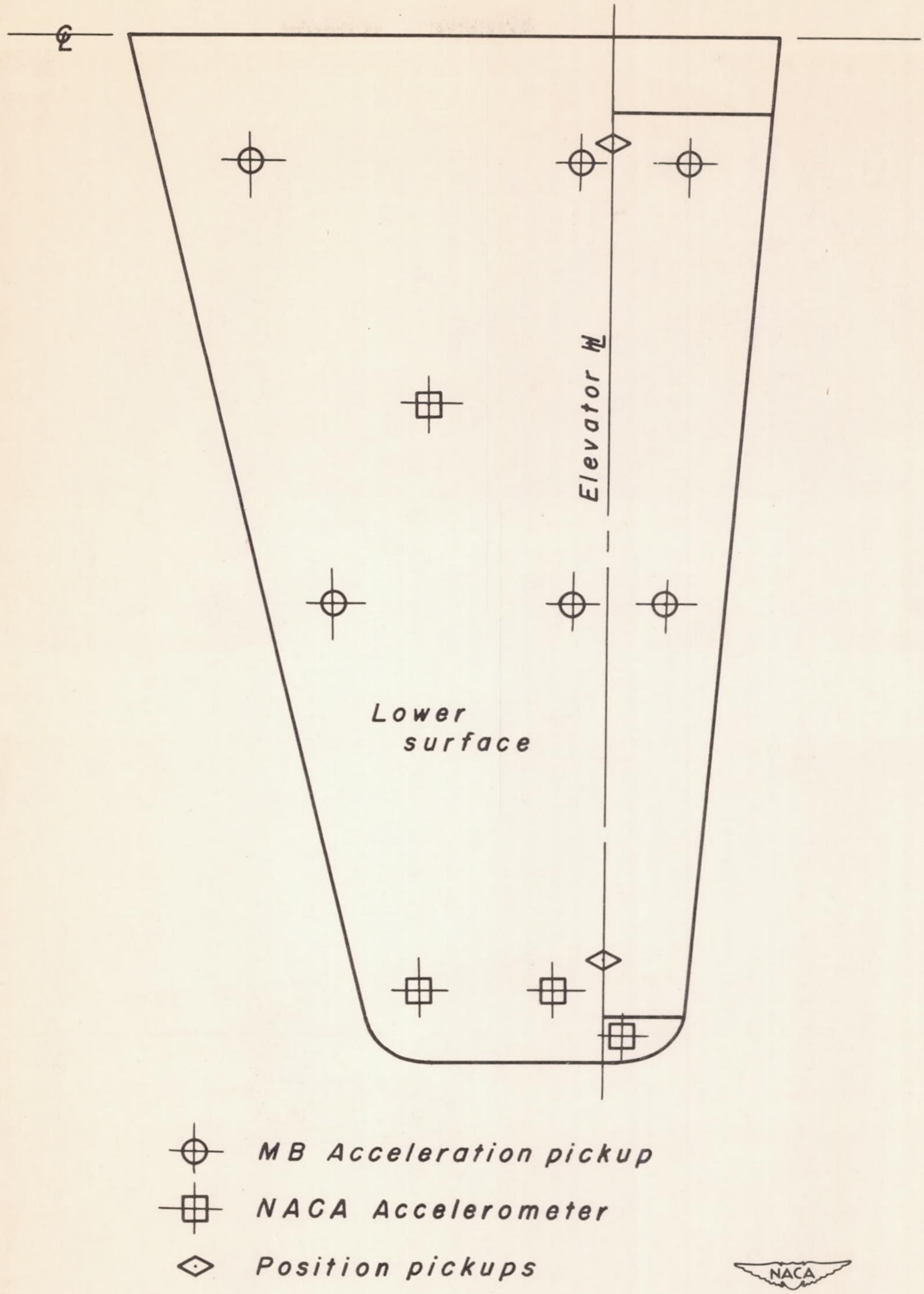


Figure 5.- Location of velocity pickups, accelerometers, and position pickups on horizontal tail.

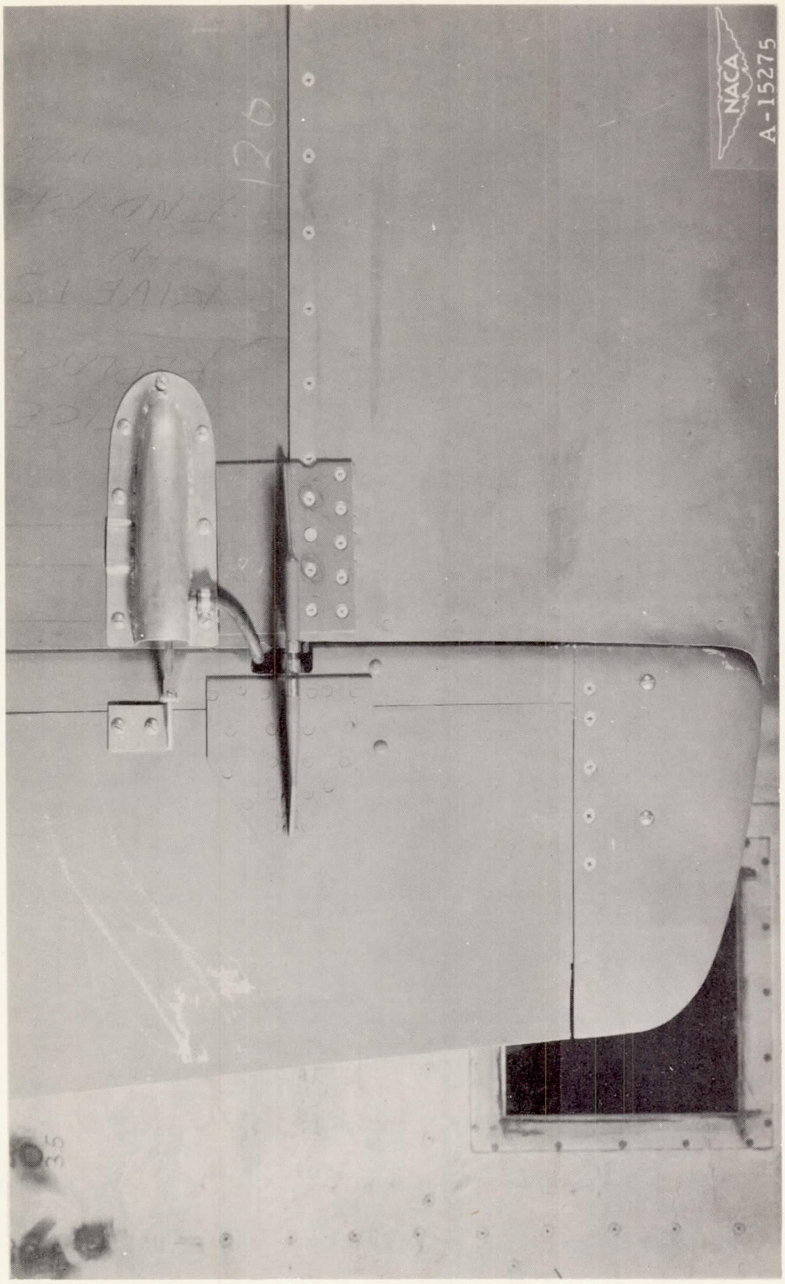


Figure 6.- Details of installation of position pickup.

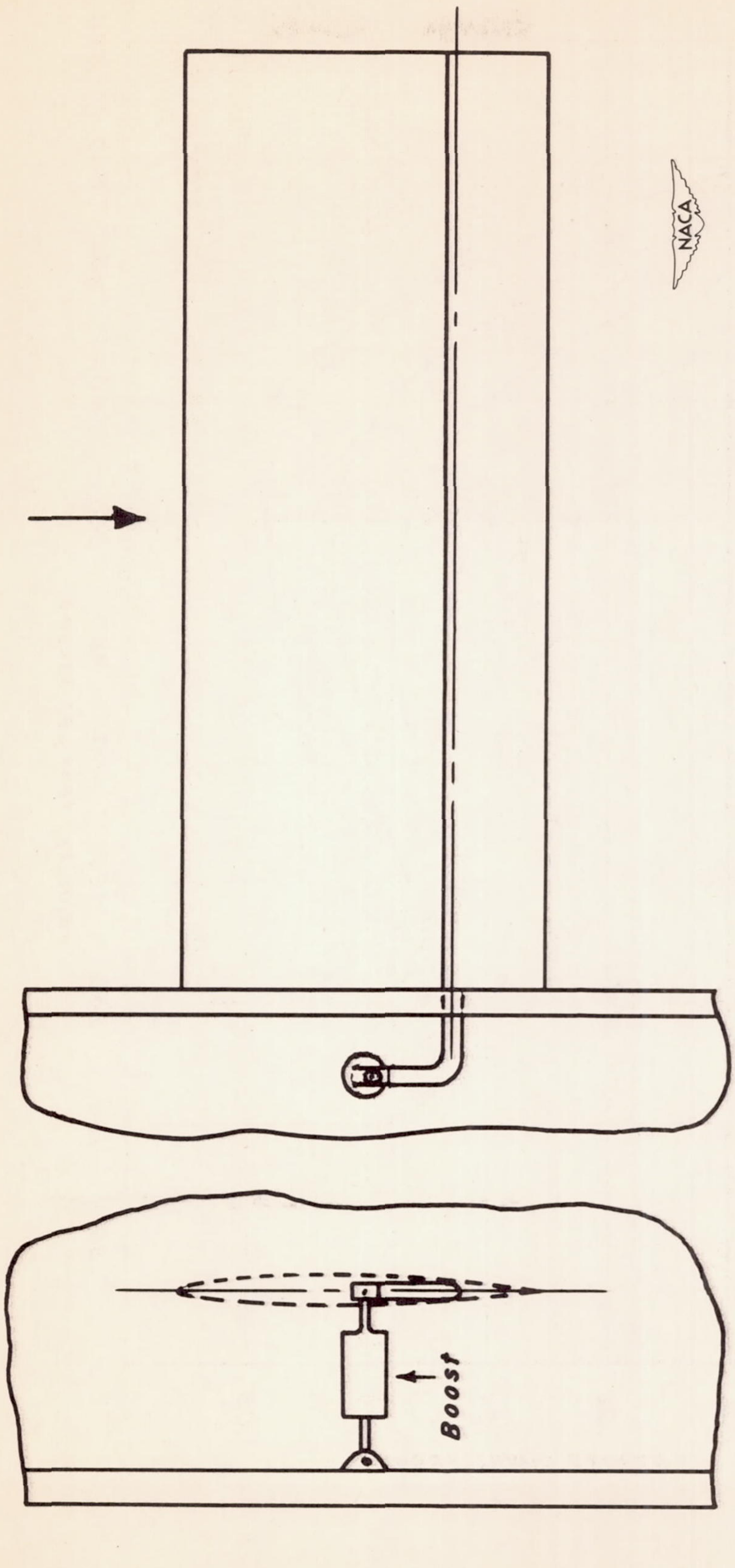


Figure 7. Schematic drawing of system considered in analytical study.

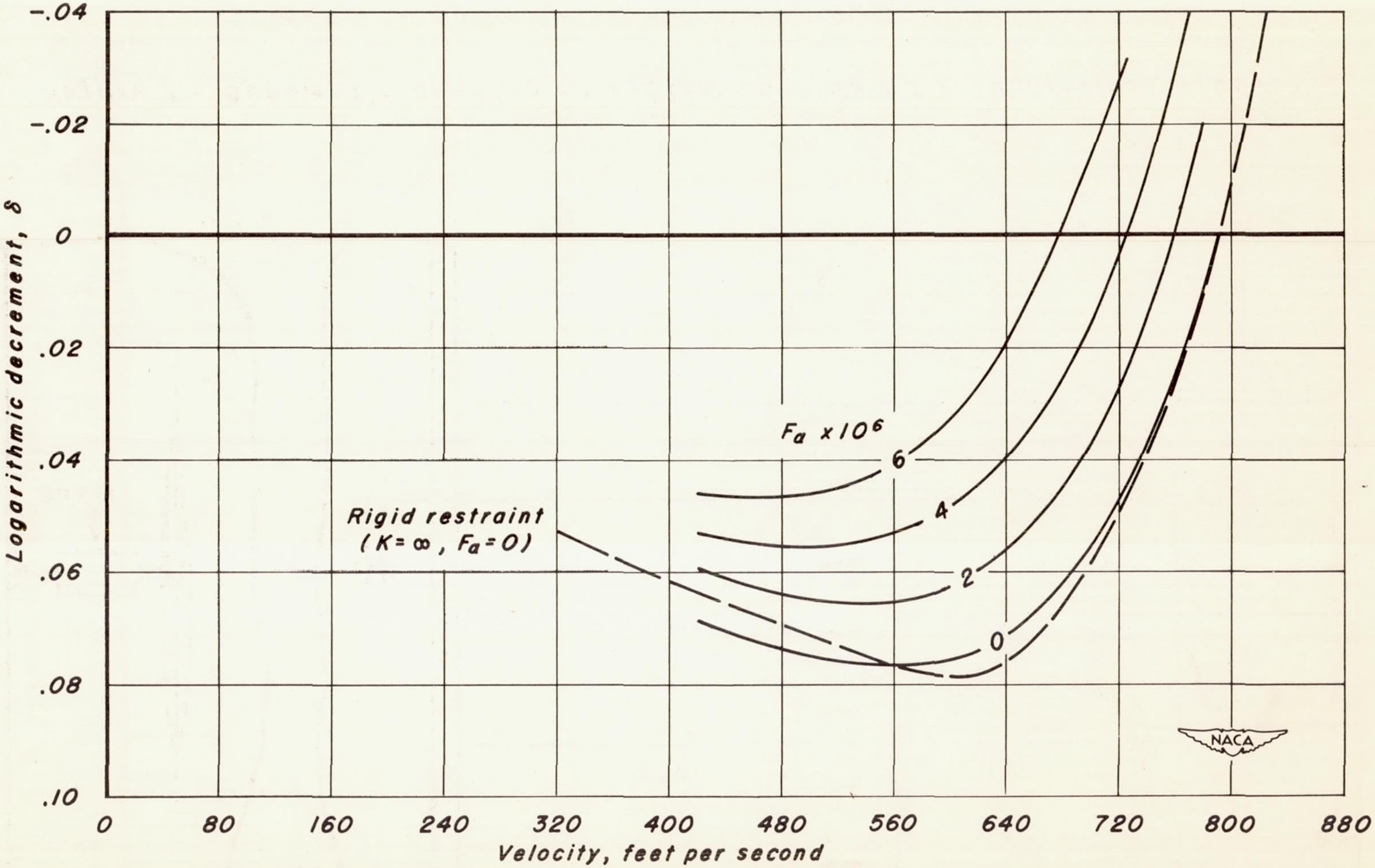


Figure 8.- Variation of the logarithmic decrement with airspeed for various values of feedback amplitude parameter. K, 160,000 foot-pounds per inch.

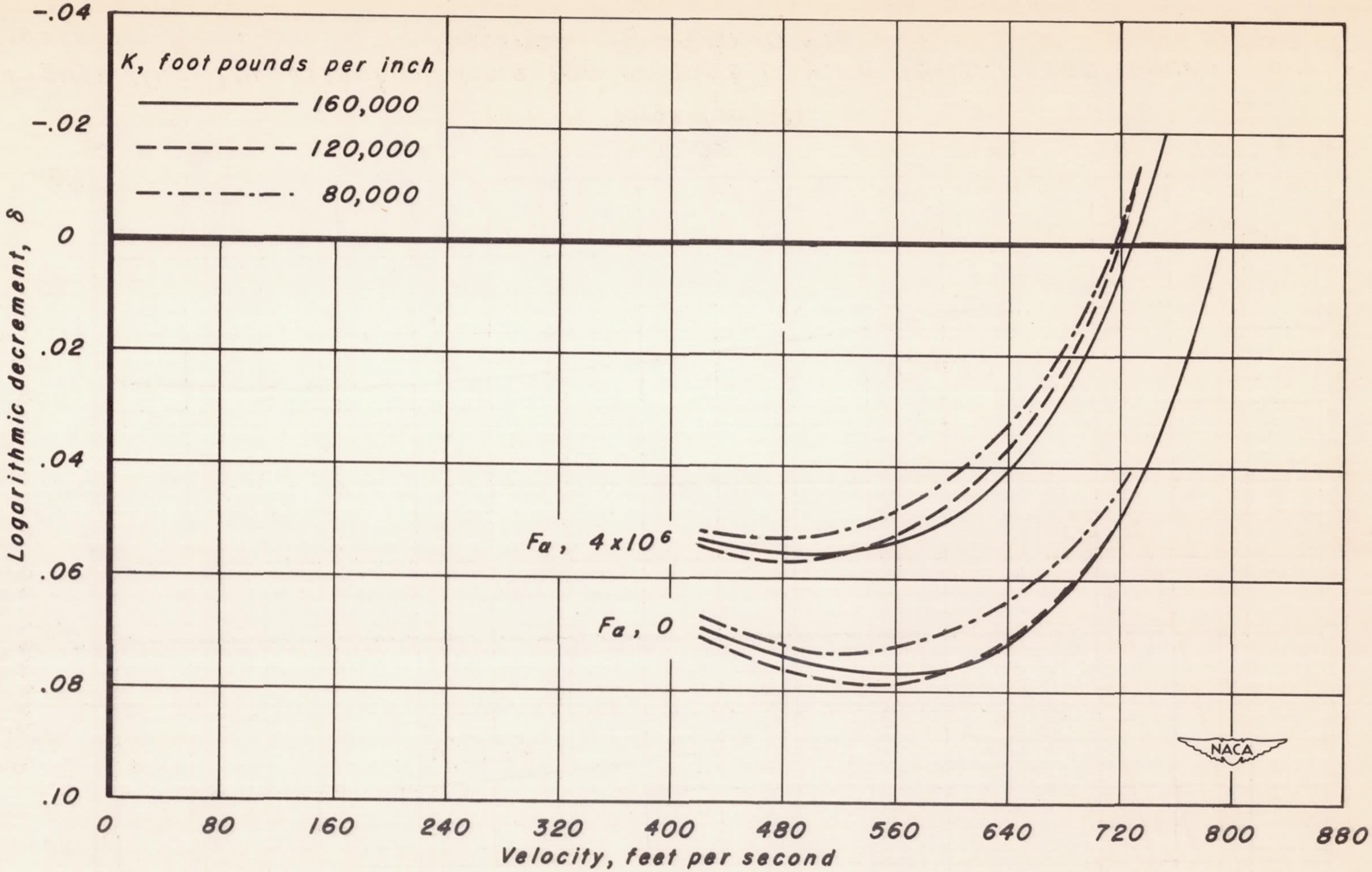


Figure 9.- The effect of variations in the boost stiffness parameter.

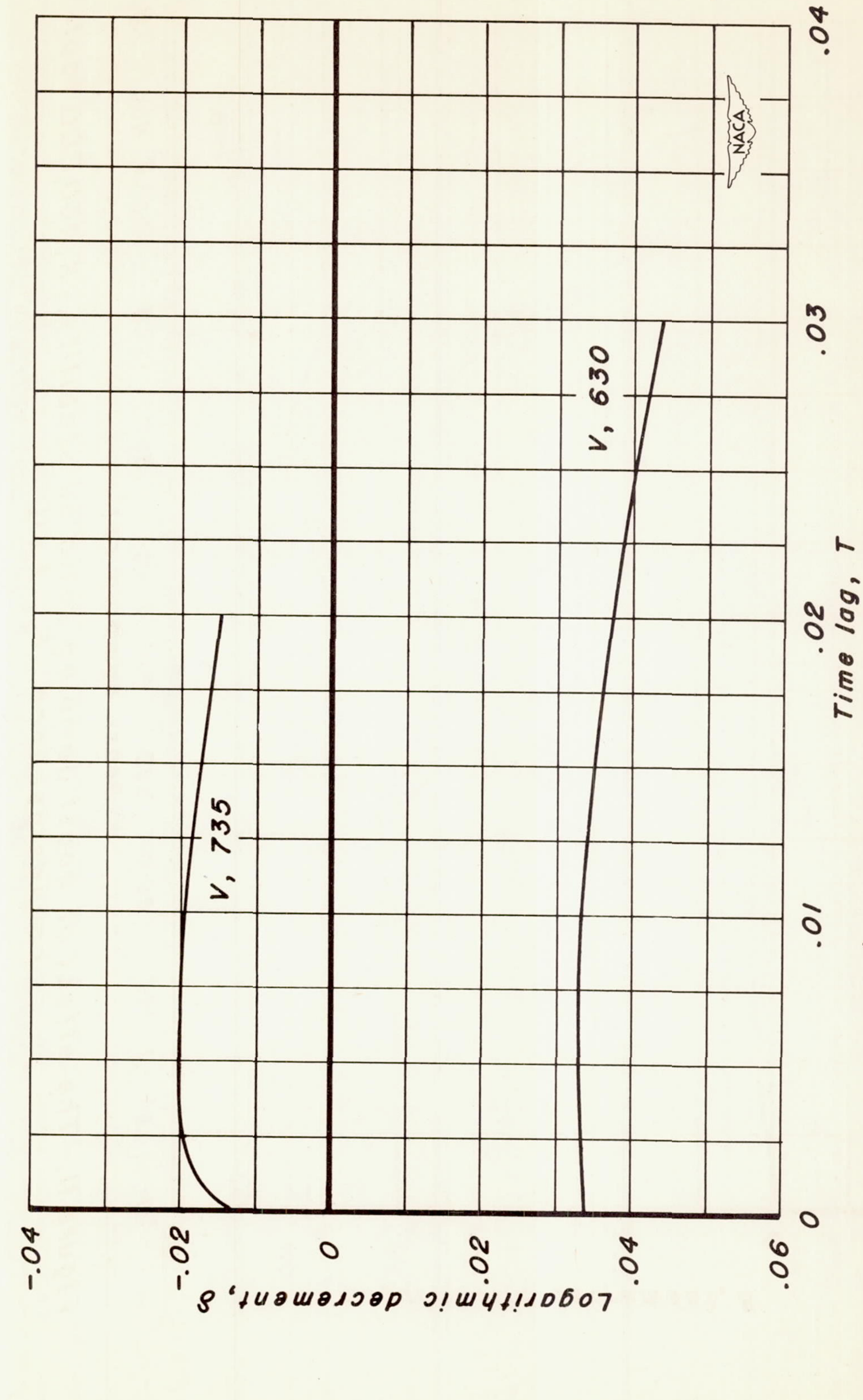


Figure 10.- The effect of time lag on stability. K, 160,000 foot-pounds per inch. F_a, 4.5×10^{-6}.

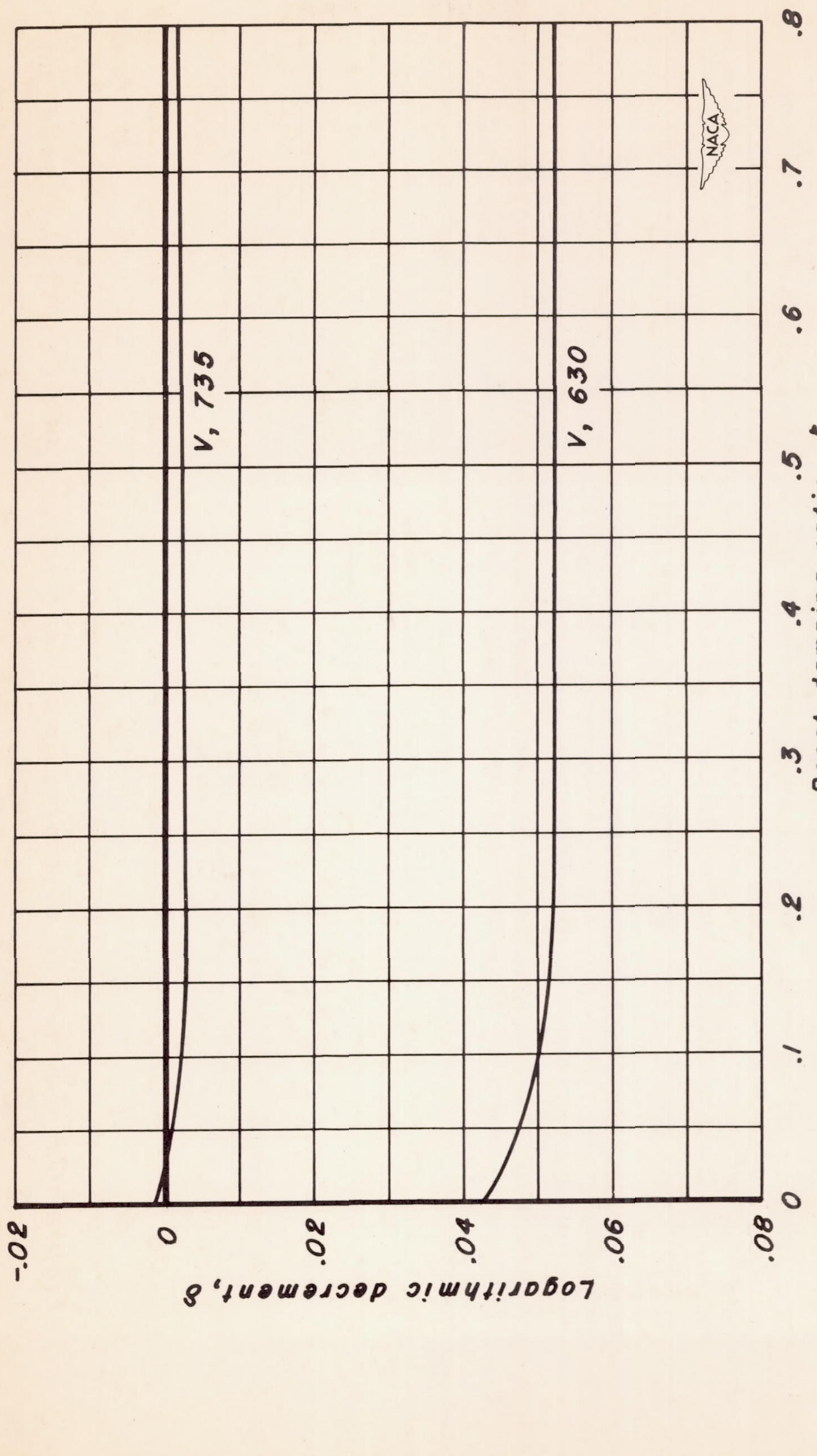


Figure 11.- The effect of boost damping ratio on stability. K, 160,000 foot-pounds per inch. F_a, 3×10^{-6}.