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INFLUENCE OF BOOM TORSIONAL RIGIDITY AND THERMALLY INDUCED TORSION ON THE STABILITY OF A SEMIPASSIVE GRAVITY-STABILIZED SATELLITE

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16. Abstract

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A laboratory test for the presence of the instability is suggested.

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NOMENCLATURE

C	boom torsional stiffness (see footnote to eq. (4))
C^*	dimensionless C, $C/L\omega_o{}^2 I_1$
D_R, D_y	roll and yaw damping coefficients of body damper
$D_R{}^*, D_y{}^*$	dimensionless D_R and D_y, $D_R/\omega_o I_1$, $D_y/\omega_o I_1$
D_b	damping coefficient of boom (excluding tip damper)
$D_b{}^*$	dimensionless D_b, $D_b/\omega_o I_1$
D_T	damping coefficient of tip damper
$D_T{}^*$	dimensionless D_T, $D_T/\omega_o I_1$
h_{2k}	pitch wheel angular momentum
$h_{2k}{}^*$	dimensionless h_{2k}, $h_{2k}/\omega_o I_1$
i_R, i_y	roll and yaw moments of inertia of body damper mass
$i_R{}^*, i_y{}^*$	dimensionless i_R and i_y, i_R/I_1, i_y/I_1
I_1, I_2, I_3	satellite principal moments of inertia
$I_3{}^*$	dimensionless I_3, I_3/I_1
I_b	axial moment of inertia of boom including the outer shell of the tip damper
$I_b{}^*$	dimensionless I_b, I_b/I_1
J_1	dimensionless inertia parameter, $(I_1 - I_3)/I_1$
K_1	thermal torque gradient with respect to boom twist (constant sun angle at the root)
$K_1{}^*$	dimensionless K_1, $K_1/\omega_o{}^2 I_1$
K_2	thermal torque gradient with respect to the sun angle at the root (constant boom pretwist)
$K_2{}^*$	dimensionless K_2, $K_2/\omega_o{}^2 I_1$
L	boom length

$\bar{m}_1, \bar{m}_2, \bar{m}_3$ — unit vectors defining the roll, pitch, and yaw axes system fixed in the satellite. In equilibrium $\bar{m}_1 = \bar{0}_1$, $\bar{m}_2 = \bar{0}_2$, $\bar{m}_3 = \bar{0}_3$

$\bar{0}_1, \bar{0}_2, \bar{0}_3$ — unit vectors defining the orbital reference frame (fig. 1), $\bar{0}_2$ is along the normal to the orbital plane in a direction opposite to the orbital angular velocity vector, $\bar{0}_3$ is toward the center of the earth and $\bar{0}_1$ completes a right handed coordinate system

s — Laplace transform variable

s^* — dimensionless s, s/ω_o

T_T — thermal torque

$\sum T_1, \sum T_3$ — external torques about the roll and yaw axes

$\sum T_b$ — external torque acting on the boom

$\phi_{m/o}, \psi_{m/o}$ — satellite roll and yaw angles relating the $\bar{m}_1, \bar{m}_2, \bar{m}_3$ vectors to the $\bar{0}_1, \bar{0}_2, \bar{0}_3$ vectors

$\psi_{b/o}$ — angle of twist of the boom tip, relative to the orbital reference frame, about the $\bar{0}_3$ axis

$\phi_{b/o}$ — angle of rotation of the boom, relative to the orbital reference frame, about the $\bar{0}_1$ axis

$\psi_{b/m}$ — angle of twist of the boom tip relative to the boom root; in equilibrium, $\psi_{b/m} = 0$ by definition

λ — reciprocal of the thermal time constant

λ^* — dimensionless λ, λ/ω_o

ω_o — orbital angular velocity of the satellite

ε — orbital eccentricity

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SUMMARY

A semipassive gravity-stabilized satellite has been observed to have large and unexpected yaw oscillations. A linearized stability analysis demonstrates that the oscillations may be caused by thermoelastic torques generated, in a boom, by solar radiation.

Thermoelastic torque gradients are influenced by boom twist and by rotation of the entire boom about its axis. Unstable oscillations will occur at a frequency close to the satellite natural yaw frequency if $K_1 - K_2 > C/L$, where K_1 and K_2 are the thermoelastic torque gradients with respect to twist and rigid rotation, C is the boom torsional stiffness, and L the length. It is shown that if the instability is confined to the yaw frequency, it can be removed at little cost by the addition of damping to the main satellite body.

A laboratory test for the presence of the instability is suggested.

INTRODUCTION

Gravity stabilization promises to provide an inexpensive, reliable, and long-lived means of orienting a satellite payload toward the earth. Unfortunately, the performance of most gravity-stabilized satellites in orbit has fallen short of the analytical predictions. Three-axis stabilized satellites, in particular, have been notably poor performers. The unpredicted performance characteristics are large oscillations about all axes, occasional inversions about the earth pointing axis, and in one case inversions about a transverse axis (refs. 1, 2, 3).

Common to all the gravity-stabilized satellites that have performed poorly is the use of long extendible tubular members (booms). Analysis and supporting experiments have demonstrated that these booms can oscillate in sunlight because of thermoelastic instability (refs. 4, 5, 6). In particular, instability can be caused by thermally induced torques (ref. 5) or by thermally induced bending moments (ref. 6). Although, in a given boom, both thermal torques and thermal bending moments are present and are coupled, it has been shown that boom length is a significant factor in deciding which of these thermal effects will predominate (ref. 4). Thermal torques are most important for short booms and thermal bending moments for long booms.

Over the past three years, evidence has accumulated to support the conjecture that thermoelastic instabilities are the cause of the poor performance of gravity-stabilized satellites (ref. 1). Supporting evidence consists mostly of observations of low-amplitude oscillations at the boom flexural frequencies that often accompany the large oscillations at the rigid body frequencies (refs. 1, 2). Interpretation of this evidence would be immeasurably easier if comprehensive mathematical models of the various satellites could be developed that included thermoelastic effects. Since most three-axis stabilized satellites have at least three booms, such a model is likely to be extremely complex. One important exception is a recently launched semipassive gravity-stabilized satellite of a uniquely simple design (fig. 1). Furthermore, the anomalous behavior of this satellite is strikingly simple. This satellite was launched by the Naval Research Laboratories and is in a circular, 1000-km altitude orbit inclined at roughly 70° to the equator.

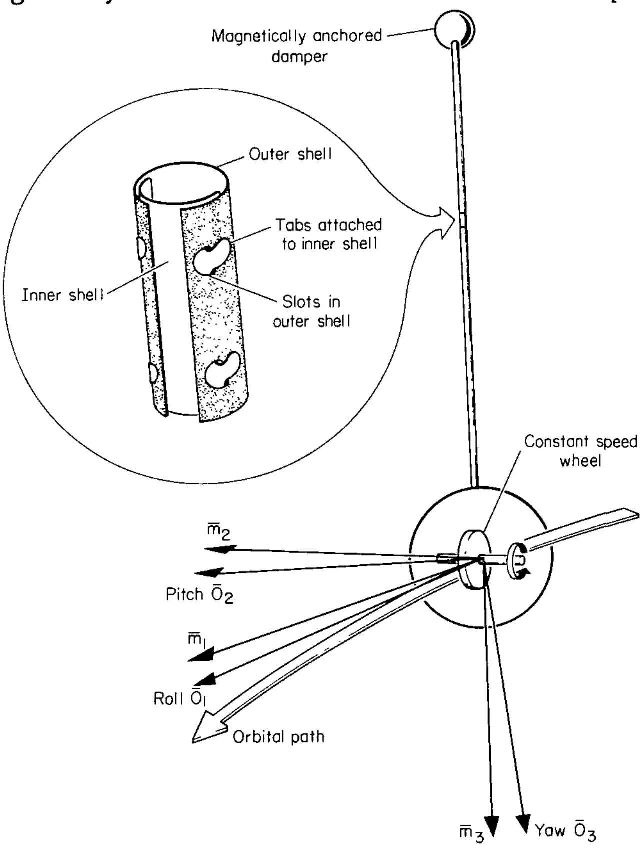

Figure 1.- Semipassive gravity-stabilized satellite.

The semipassively stabilized satellite uses a single boom to connect an almost spherical main body and a spherical magnetically anchored damper (ref. 7). The main body contains a constant speed wheel with its spin vector normal to the boom axis. Roll and pitch stabilization come directly from the gravity torques and yaw stabilization from the gyroscopic precessional torques that tend to aline the spin vector of the wheel with the orbital angular velocity vector. Rotation of the satellite about any axis is damped directly by the magnetically anchored damper.

On several occasions the satellite has been observed to oscillate continuously in yaw at a frequency close to the natural rigid body frequency. The amplitude of the oscillation appears to be related to the angle between the sunline and the orbit normal, reaching a maximum of about 30° when the sunline is closest to the orbit normal. Since the unstable oscillations occur about an axis alined with the torsional axis of the boom, which is known to be torsionally weak, it is relevant to inquire into the effect of boom stiffness on the damping of the yaw mode. This is one of the purposes of this study. The second and more important purpose is to outline the results of an investigation to determine if thermal torques induced in the boom could destabilize the satellite in yaw.

A simple linearized analysis is presented, which includes the roll and yaw degrees of freedom of the satellite and the twist degree of freedom of the boom. In this analysis a number of assumptions are made, particularly with respect to the form of the thermoelastic effects. These assumptions were suggested by the results of references 4 and 5 and permit a simple representation of those aspects of thermoelastic boom behavior thought to be relevant to the present investigation. The analysis is used to formulate a criterion for satellite stability, to assess the relative importance of the various satellite parameters and to evaluate the effectiveness of a passive, body-mounted damper on the stability of the satellite. Finally, a simple laboratory test is outlined that could provide a strong indication of whether or not the boom thermoelastic properties can significantly degrade the performance of the satellite.

DERIVATION OF THE STABILITY POLYNOMIAL

The equations of motion of the satellite are based on the assumption that the only significant structural flexibility is that of boom twist. It is assumed, further, that the inner magnetic sphere of the boom tip damper (ref. 7) is rigidly locked to the orbital reference frame (fig. 1). The justification for this idealized representation of the damper is that the natural frequencies of vibration of the magnetic inner sphere are much higher than those of the main satellite and the axis about which the magnetic torque is zero rarely coincides with one of the satellite stabilization axes. The effects of this assumption are to remove the dynamics of the magnetic inner sphere of the damper from the problem and to make the damping coefficient the same about all axes. For a rigid satellite, the assumption maintains the usual decoupling between the pitch and roll-yaw motions. Fortunately, the addition of the boom twist degree of freedom to the otherwise rigid satellite does not affect this decoupling, but influences only the roll-yaw stability. Consequently, only the roll, yaw, boom twist, and thermal torque equations are needed to fully define the problem.

The linearized roll and yaw equations of a rigid satellite with a constant speed pitch wheel are well known. Those given below have been taken from reference 8 (eq. (23)).

$$I_1\ddot{\phi}_{m/o} - 4\omega_o^2(I_3 - I_1)\phi_{m/o} - \omega_o I_3\dot{\psi}_{m/o} - h_{2k}(\dot{\psi}_{m/o} + \omega_o\phi_{m/o}) = \sum T_1 \tag{1}$$

$$I_3\ddot{\psi}_{m/o} + \omega_o I_3\dot{\phi}_{m/o} + h_{2k}(\dot{\phi}_{m/o} - \omega_o\psi_{m/o}) = \sum T_3 \tag{2}$$

where

I_1, I_2, I_3 principal moments of inertia of the satellite

$\phi_{m/o}, \psi_{m/o}$ roll and yaw angles of the satellite relative to the orbital frame

ω_o orbital angular velocity

h_{2k} angular momentum of the constant-speed wheel

$\sum T_1, \sum T_3$ roll and yaw torques (other than those due to gravity)

It has been assumed in equations (1) and (2) that for this type of satellite, the pitch and roll moments of inertia are equal ($I_2 = I_1$) and the boom inertia about its axis I_b is small compared with the yaw inertia of the satellite ($I_b/I_3 << 1$). Since the only torques, about the roll axis, other than those due to gravity, arise from the action of the damper, it follows that

$$\sum T_1 = -D_T \dot{\phi}_{m/o} \qquad (3)$$

where D_T is the tip damper coefficient. It is now necessary to specify an assumed model for the thermoelastic behavior of the boom. The assumption here is that the boom behaves as if it had equal but opposite thermal torques applied to its ends. Thus, the net external torque acting on the boom is zero. The magnitude of the thermal torque is such that it twists the boom through an angle whose value is equal to that caused by the thermoelastically induced thermal stresses. The rationale for this approach is given in reference 9. It follows that the torque about the yaw axis of the main body of the satellite is transmitted by the boom and must include the effect of the thermal torque and boom internal damping. Thus,

$$\sum T_3 = \psi_{b/m} \frac{C}{L} + D_b \dot{\psi}_{b/m} - T_T \qquad (4)$$

where

$\psi_{b/m}$ angle of twist of the boom tip relative to the main body, measured positive along the $\bar{m}_3$ axis

L length of the boom

C torsional stiffness of the boom[1]

D_b internal damping coefficient of the boom in twist

T_T thermal torque measured positive along the $\bar{m}_3$ axis

[1]The definition of C is simply the stiffness that would be obtained from a structural test on a boom of the correct length multiplied by the boom length. Thus, C is neither the conventional torsional stiffness nor the warping stiffness but a function of both.

By analogy to equation (2), the equation of motion of the boom about its axis is

$$I_b\ddot{\psi}_{b/o} + \omega_o I_b\dot{\phi}_{b/o} + D_b\dot{\psi}_{b/m} + \frac{C}{L}\psi_{b/m} = \sum T_b \tag{5}$$

where

I_b effective inertia of the boom, including the outer shell of the tip damper, about the boom axis

$\psi_{b/o}$ angle of twist of the boom tip, relative to the orbital frame, about the $\bar{0}_3$ axis

$\phi_{b/o}$ angle of rotation of boom tip, relative to the orbital frame, about the $\bar{0}_1$ axis

$\sum T_b$ external torques acting about the boom axis

It follows, immediately, that

$$\left.\begin{aligned}\psi_{b/o} &= \psi_{b/m} + \psi_{m/o}\\ \phi_{b/o} &= \phi_{m/o}\end{aligned}\right\} \tag{6}$$

The torque acting about the boom axis can be expressed in the form

$$\sum T_b = -D_T\dot{\psi}_{b/o} + T_T \tag{7}$$

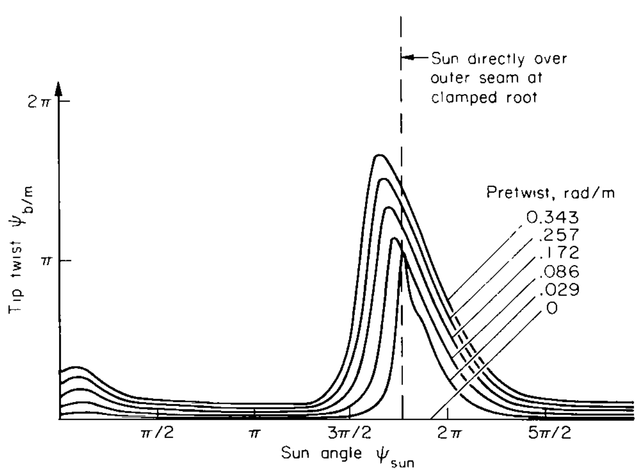

Figure 2.- Variation of thermally induced tip twist with relative sun orientation for booms having linear pretwist.

It now remains to develop a suitable thermal torque relationship. For a 10-ft-long boom blackened on the outside and having a simple open overlapped cross section, figure 2 (ref. 4, fig. 31) shows the variation of thermally induced tip twist with angle of the sun, relative to the seam, at the root ψ_{sun}. Curves are presented for the boom with no pretwist and with various values of linear pretwist ψ_{pre}. It follows that for a given boom, the thermal twist and therefore the thermal torque are given by

$$T_T = f(\psi_{pre}, \psi_{sun}) \tag{8}$$

It follows from equation (8) that for small deviation about some point,

$$\Delta T_T \approx \frac{\partial f}{\partial \psi_{pre}} \Delta\psi_{pre} + \frac{\partial f}{\partial \psi_{sun}} \Delta\psi_{sun} \tag{9}$$

Suppose, now, that the satellite is in equilibrium. At this point the boom will have an equilibrium thermal twist and therefore an equilibrium thermal torque. This will in no way change the orientation of the body, however, and irrespective of how the boom is twisted, $\psi_{m/o} = 0$. It is clear, moreover, that since we are concerned only with changes about the equilibrium state, T_T and $\psi_{b/m}$ can be defined to be zero in this state; that is, the equilibrium tip twist can be charged to pretwist. Therefore, for small variations about equilibrium, equation (9) can be expressed as

$$T_T = \frac{\partial f}{\partial \psi_{pre}} \psi_{b/m} + \frac{\partial f}{\partial \psi_{sun}} \psi_{m/o} \tag{10}$$

Equation (10) represents the steady-state thermal torque resulting from small changes in $\psi_{m/o}$ and $\psi_{b/m}$. However, it has been shown that the transient changes of T_T can be expressed as a simple exponential lag (ref. 5); therefore, the expression representing the dynamic behavior of the thermal torque must be of the form

$$\dot{T}_T + \lambda T_T = \lambda \frac{\partial f}{\partial \psi_{pre}} \psi_{b/m} + \lambda \frac{\partial f}{\partial \psi_{sun}} \psi_{m/o} \tag{11}$$

Equation (11) is of the same form as the last of equations (17) of reference 5. The analysis of reference 5 is for a boom with zero pretwist, however, and equation (11) can be regarded as a generalization of equations (17) of reference 5. Equation (11) will be used in the form

$$\dot{T}_T + \lambda T_T = \lambda K_1 \psi_{b/m} + \lambda K_2 \psi_{m/o} \tag{12}$$

where

$$K_1 = \frac{\partial f}{\partial \psi_{pre}} \text{ and } K_2 = \frac{\partial f}{\partial \psi_{sun}}$$

The value of K_2 is proportional to the slope of the curve of constant pretwist passing through the equilibrium point. Similarly, the value of K_1 is proportional to the slope of a curve that is the crossplot of those shown in figure 2 for the constant equilibrium value of the sun angle. Both K_1 and K_2 can be calculated for the simple, open-cross-section booms treated in references 4 and 5. However, the satellite under consideration used a type of boom (see fig. 1) consisting of one open section shell inside another open section shell with interlocking tabs passing from one section to the other (fig. 1). This type of boom, called by the manufacturers an interlocked BISTEM, has a complex cross section and is not readily analyzed. Therefore, the stability of the satellite system will be examined for various values of K_1 and K_2.

The set of equations (1) through (7) and (12) completely define the satellite roll-yaw stability. With appropriate substitutions these equations can be reduced to:

$$I_1\ddot{\phi}_{m/o} + D_T\dot{\phi}_{m/o} + [4\omega_o^2(I_1 - I_3) - \omega_o h_{2k}]\phi_{m/o} - (\omega_o I_3 + h_{2k})\dot{\psi}_{m/o} = 0 \quad (13)$$

$$(\omega_o I_3 + h_{2k})\dot{\phi}_{m/o} + I_3\ddot{\psi}_{m/o} - \omega_o h_{2k}\psi_{m/o} - D_b\dot{\psi}_{b/m} - \frac{C}{L}\psi_{b/m} + T_T = 0 \quad (14)$$

$$\omega_o I_b\dot{\phi}_{m/o} + I_b\ddot{\psi}_{m/o} + D_T\dot{\psi}_{m/o} + I_b\ddot{\psi}_{b/m} + (D_b + D_T)\dot{\psi}_{b/m} + \frac{C}{L}\psi_{b/m} - T_T = 0 \quad (15)$$

$$\lambda K_2\psi_{m/o} + \lambda K_1\psi_{b/m} - \dot{T}_T - \lambda T_T = 0 \quad (16)$$

The stability of the satellite is therefore given by the following polynomial, given here in dimensionless determinant form.

$$\begin{vmatrix} s^{*2}+D_T^*s^*+4J_1-h_{2k}^*, & -(I_3^*+h_{2k}^*)s^*, & 0 & , & 0 \\ (I_3^*+h_{2k}^*)s^* & , & I_3^*s^{*2}-h_{2k}^*, & -D_b^*s^*-C^* & , & 1 \\ I_b^*s^* & , & I_b^*s^{*2}+D_T^*s^*, & I_b^*s^{*2}+(D_T^*+D_b^*)s^*+C^*, & -1 \\ 0 & , & -\lambda^*K_2^* & , & -\lambda^*K_1^* & , & s^*+\lambda^* \end{vmatrix} = 0 \quad (17)$$

where $s^* = s/\omega_o$, $D_T^* = D_T/\omega_o I_1$, $D_b^* = D_b/\omega_o I_1$, $J_1 = (I_1 - I_3)/I_1$, $I_3^* = I_3/I_1$, $I_b^* = I_b/I_1$, $h_{2k}^* = h_{2k}/\omega_o I_1$, $C^* = C/L\omega_o^2 I_1$, $K_1^* = K_1/\omega_o^2 I_1$, $K_2^* = K_2/\omega_o^2 I_1$, $\lambda^* = \lambda/\omega_o$, and s is the conventional Laplace transform variable.

The development of the stability polynomial must now be expanded to include the effects of passive damping added,to the main satellite body, about both the roll and yaw axes. This is a straightforward procedure involving the addition of two more degrees of freedom to the system; the final result is

$$\left|\begin{array}{cccccc} \multicolumn{4}{c}{} & -D_R^*s^*\,, & 0 \\ \multicolumn{4}{c}{\text{Matrix of the}} & 0\,, & -D_y^*s^* \\ \multicolumn{4}{c}{\text{determinant given}} & 0\,, & 0 \\ \multicolumn{4}{c}{\text{in equation (17)}} & 0\,, & 0 \\ i_R^*s^*, & 0\,, & 0, & 0, & i_R^*s^*+D_R^*, & 0 \\ 0\,, & i_y^*s^*, & 0, & 0, & 0\,, & i_y^*s^*+D_y^* \end{array}\right| = 0 \qquad (18)$$

where

D_R damping coefficient of body damper in roll

D_y damping coefficient of body damper in yaw

i_R moment of inertia of body damper mass about the roll axis

i_y moment of inertia of body damper mass about the yaw axis

and $D_R^* = D_R/\omega_o I_1$, $D_y^* = D_y/\omega_o I_1$, $i_R^* = i_R/I_1$, $i_y^* = i_y/I_1$. Note that an s^{*2} term has been factored out of equation (18); this term merely represents the fact that the damper mass has no preferred position in either roll or yaw.

A standard digital computer program was used to extract the roots of equation (18).

ANALYSIS OF THE STABILITY POLYNOMIAL

There is little hope of deriving analytical relationships that determine the stability from the characteristic equation of the entire satellite system. A more tractable approach is to first simplify the equations, by using prior information about the behavior of the system, and then consider certain limiting cases wherein one or more of the parameters are assumed to be either very large or very small. The limiting behavior of the system can provide useful information, and even approximate solutions, that give insight into how certain parameters influence stability. The ultimate justification for this approach lies in how well these solutions agree with those computed from equation (17). This comparison is made in the next section.

Since the roll and yaw degrees of freedom are coupled, both through the orbital motion and the constant-speed wheel, it is not strictly accurate to speak of a yaw or roll stability as though the two were independent. However, the coupling is of such a form that roll and yaw retain a measure of their independence and the terms "roll stability" and "yaw stability" are meaningful. Since the boom axis coincides with the yaw axis it seems reasonable to presume that the principal effects of boom torsion will be felt about the yaw axis. These observations indicate one possible simplification, which should not destroy the essential character of the phenomenon under investigation; that is, to eliminate the roll degree of freedom from the problem. The characteristic polynomial then becomes

$$\begin{vmatrix} I_3{}^*s^{*2}-h_{2k}^* \ , & -D_b{}^*s^*-C^* \ , & 1 \\ I_b{}^*s^{*2}+D_T{}^*s^*, & I_b{}^*s^{*2}+(D_T{}^*+D_b{}^*)s^*+C^*, & -1 \\ -\lambda^*K_2{}^* \ , & -\lambda^*K_1{}^* \ , & s^*+\lambda^* \end{vmatrix} = 0 \tag{19}$$

Two limiting cases of equation (19) will now be considered.

In the first case it is assumed that the inertial torques acting on the main satellite body are of much greater magnitude than the inertial, thermal, or damping torques acting on the boom, or

$$|I_3{}^*s^{*2}| \gg |I_b{}^*s^{*2}|, \quad |D_T{}^*s^*|, \quad |D_b{}^*s^*|, \quad |K_1{}^*|, \quad |K_2{}^*|$$

With these conditions a close approximation to equation (19) is

$$\left(I_3{}^*s^{*2} - h_{2k}^*\right)\begin{vmatrix} I_b{}^*s^{*2}+(D_T{}^*+D_b{}^*)s^*+C^*, & -1 \\ -\lambda^*K_1{}^* \ , & s^*+\lambda^* \end{vmatrix} = 0 \tag{20}$$

The first factor of equation (20) represents the undamped yaw mode of the main body of the satellite, whereas the second factor shows the influence of thermal torques on stability. This second factor is essentially the same as the stability polynomial, derived in reference 5, for a boom rigidly clamped at one end. Although the conditions imposed on the parameters for this case are not representative of those for the satellite under investigation, the results allow contact with the work of reference 5. Note that for this case the stability of the satellite is independent of K_2. Following reference 5, the conditions for stability of the satellite are

$$K_1{}^* < C^*$$

$$K_1{}^* > \frac{-(D_T{}^*+D_b{}^*)}{\lambda^*I_b{}^*}\left[(D_T{}^* + D_b{}^*)\lambda^* + C^* + \lambda^{*2}I_b{}^*\right] \tag{21}$$

If $K_1{}^*$ were negative and of magnitude such that inequality (21) is not satisfied, then the satellite would be unstable, and the boom would appear to have an oscillatory divergence in twist at a frequency close to the natural torsional frequency of the boom $\sqrt{C/LI_b}$ and not, in general, the main body natural yaw frequency. The main satellite body would also be oscillating at the same frequency but with a much smaller amplitude. The situation described, although not that observed in flight, could occur if for some reason the tip damper coefficient became very small.

In the second case, it is assumed that the torques from the tip damper and boom twist are of much greater magnitude than the inertial and damping torques acting on the boom, or

$$|C^*|, \quad |D_T{}^*s^*| >> |I_b{}^*s^{*2}|, \quad |D_b{}^*s^*|$$

In addition, it is assumed that λ^* is an order of magnitude larger than s^*. These conditions are thought to be representative of the satellite under investigation. More experimental data on the booms are required to verify the above assumptions. With these assumptions, a close approximation to equation (19) is

$$\begin{vmatrix} I_3{}^*s^{*2}-h^*_{2k}, & -C^*, & 1 \\ D_T{}^*s^*, & D_T{}^*s^*+C^*, & -1 \\ -K_2{}^*, & -K_1{}^*, & 1 \end{vmatrix} = 0 \tag{22}$$

The expanded form of equation (22) is

$$I_3{}^*D_T{}^*s^{*3}+I_3{}^*(C^*-K_1{}^*)s^{*2}+D_T{}^*(K_2{}^*-K_1{}^*+C^*-h^*_{2k})s^*+h^*_{2k}(K_1{}^*-C^*) = 0 \tag{23}$$

Applying the criterion of reference 10 to equation (23), the following conditions for stability can be derived.

$$h^*_{2k}(K_1{}^* - C^*) > 0 \tag{24}$$

$$K_2{}^* - K_1{}^* + C^* - h^*_{2k} > 0 \tag{25}$$

$$K_2{}^* - K_1{}^* + C^* > 0 \tag{26}$$

For the type of satellite under investigation $h^*_{2k} < 0$, and condition (25) therefore is included in condition (26). The stability conditions then can be reduced to

$$C^* - K_1{}^* > 0 \tag{27}$$

$$K_2{}^* - K_1{}^* + C^* > 0 \tag{28}$$

It can be shown that if stability condition (27) is not satisfied ($K_1^* > C^*$), then the system has a simple divergent mode. If, on the other hand, stability condition (28) is not satisfied, the system has a divergent oscillatory mode. Thus if $K_2^* = 0$ and $K_1^* > C^*$, a simple divergence and a divergent oscillation occur simultaneously. In other words, an unstable oscillation alone cannot be produced by K_1^* alone. However, when $K_1^* = 0$ and $K_2^* < -C^*$, the stability conditions show that a divergent oscillation occurs unaccompanied by a simple divergence. Apparently, therefore, if only an unstable oscillation is observed, it must be due to a K_2^* effect rather than a K_1^* effect. A particularly important point to note about the stability conditions is that they are independent of λ^*, h_{2k}^*, I_3^*, and D_T^*. Thus, if instability occurs, changes in the values of any of these parameters, within the limits of the order of magnitude assumptions, will not solve the problem. It remains to determine the frequency of the unstable oscillation. Consider the special case with $K_1^* = 0$ and $K_2^* = -C^*$. The stability conditions (27) and (28) show that the system has a neutrally stable oscillatory mode. When $K_2^* < -C^*$ this mode becomes unstable. To find the frequency of this mode the chosen values of K_1^* and K_2^* are substituted into equation (23) to give

$$I_3^* D_T^* s^{*3} + I_3^* C^* s^{*2} - D_T^* h_{2k}^* s^* - h_{2k}^* C^* = 0 \tag{29}$$

which can be factored into the form

$$(I_3^* s^{*2} - h_{2k}^*)(D_T^* s^* + C^*) = 0 \tag{30}$$

The frequency of the neutrally stable mode is clearly the satellite yaw frequency $\sqrt{|h_{2k}^*/I_3^*|}$. Thus with $K_2^* < -C^*$ it would appear to an observer as if the satellite were unstable in yaw.

APPLICATION OF THE THEORY TO A SPECIFIC SATELLITE

The principal characteristics of the satellite to be analyzed are shown in table 1. Note, however, that the values given in table 1 for C and D_b are open to considerable doubt. It is known from tests performed on the type of boom used on the satellite that the variation of twist with applied torque is nonlinear for small values of the twist, because the interlocking tabs on the boom exert control over the cross-sectional warping in a nonlinear way. Thus, for small angles of twist some of the edges of the tabs attached to one shell of the boom do not touch the edges of the matching slots in the other shell of the boom and therefore cannot exert control over the cross-sectional warping. As the applied torque is increased, the tab edges progressively touch the extremities of the slots, or "lock up," and the boom stiffness increases. Adjustment of the relative sizes of tabs and slots during manufacture permits some control over the boom torsional characteristics in the region of incomplete lock up. In fact, a boom can be made with the tab edges

TABLE 1.- CHARACTERISTICS OF SATELLITE AND ORBIT

Parameter	Dimensional		Dimensionless	
	Symbol and units	Value	Definition	Value
Roll moment of inertia	$I_1 = kgm^2$	477	I_1/I_1	1
Pitch moment of inertia	$I_2 = kgm^2$	477	I_2/I_1	1
Yaw moment of inertia	$I_3 = kgm^2$	4.9	$I_3^* = I_3/I_1$	0.0103
Wheel angular momentum	$h_{2K} = kgm^2/s$	-0.367	$h_{2K}^* = h_{2K}/\omega_o I_1$	-0.77
Tip damper coefficient	$D_T = nms$	0.007	$D_T^* = D_T/\omega_o I_1$	0.0147
Boom torsional stiffness	$C = nm^2$	0.024	$C^* = C/L\omega_o^2 I_1$	4.0
Thermal torque gradient[a]	$K_1 = nm/rad$	±0.001	$K_1^* = K_1/\omega_o^2 I_1$	±2.1
Thermal torque gradient[a]	$K_2 = nm/rad$	±0.004	$K_2^* = K_2/\omega_o^2 I_1$	±8.4
Thermal lag parameter[a]	$\lambda = 1/s$	0.3-3.0	$\lambda^* = \lambda/\omega_o$	3×10^2-3×10^3
Boom pretwist rate	ψ_{pre}/L rad/m	1.29		
Boom axial moment of inertia	$I_b = kgm^2$	2.5×10^{-3}	$I_b^* = I_b/I_1$	5.3×10^{-6}
Boom damping coefficient	$D_b = nms$	3×10^{-4}	$D_b^* = D_b/\omega_o I_1$	0.63×10^3
Orbital angular rate	ω_o = rad/s	10^{-3}		
Orbital eccentricity	ε	0		

[a]Ranges of values considered.

always bearing against the slot extremities, thus minimizing the torsional nonlinearities. The boom used on the satellite was designed such that it had a low torsional stiffness in a twist region of about ±30°. A 6-m-long piece of a similar boom was tested and yielded the zero twist value of torsional stiffness of 0.024 nm^2 used in the analysis. When the twist was such that all the tabs were locked, the stiffness rose to 0.42 nm^2, or about 18 times larger than the zero twist value. The principal uncertainties in these values is that no data are available to show the effects of nonuniform boom temperature distribution and continuous twist cycles, both of which occur in operation. The measured torsional stiffness must depend to some extent on "solid" friction,which may be modified by the continuous rubbing action during the twist cycles. A cutting action between the boom tabs and slots may also occur, which could modify the torsional stiffness for small values of the twist. No experimental values are available for the boom structural damping in torsion. This is perhaps not too serious since the boom damping does not appear to be a significant factor in the phenomenon investigated here. The value of D_b given in table 1 has been derived from the assumption that the boom, including the outer sphere of the tip damper, is critically damped in torsion. Damping of this order of magnitude appears to be consistent with measurements made on other types of boom.

The situation with regard to values of K_1, K_2, and λ is perhaps even more difficult than that of C and D_b. No experimental work has been performed from which K_1, K_2, and λ can be obtained. The value of λ for a simple open overlap boom cross section is known to be of the order of 1/second. Fortunately, as in the case of D_b, the value of λ does not seem to be too significant. Therefore, in the hope that the value of λ for a BISTEM boom will not differ too much from 1/second, the range of values of λ considered has been restricted to 0.3 to 3/second, and when specific results are quoted, a value of λ of 1/second is used.

The values of K_1 and K_2 are as important as the value of C. Although values of K_1 and K_2 have been evaluated for a simple open overlap cross section they are of no significance since, like the torsional stiffness, they must depend considerably on the details of the boom cross section. All that can be done is to treat K_1 and K_2 parametrically and so determine values that result in dynamic instability. It is important to note that K_2 could depend on the boom pretwist, as shown clearly for an open cross-section boom in figure 2. The pretwist for the boom flown is 1.29 rad/m, or about four times larger than the largest value shown in figure 2. The value of K_2 for the satellite boom may therefore be quite high. Also, while the value of λ can be established from tests on relatively short samples of boom this technique may not be adequate for establishing values of K_1 and K_2. The complicating factor here is that the equilibrium twist of the boom, for a given satellite orientation relative to the sun, will differ considerably from the boom pretwist on the ground. The value of the equilibrium twist and the appropriate values of K_1 and K_2 probably can be determined only from tests on a full-size boom with fully simulated heating conditions. The possibility of performing such a test is discussed in a later section of this report.

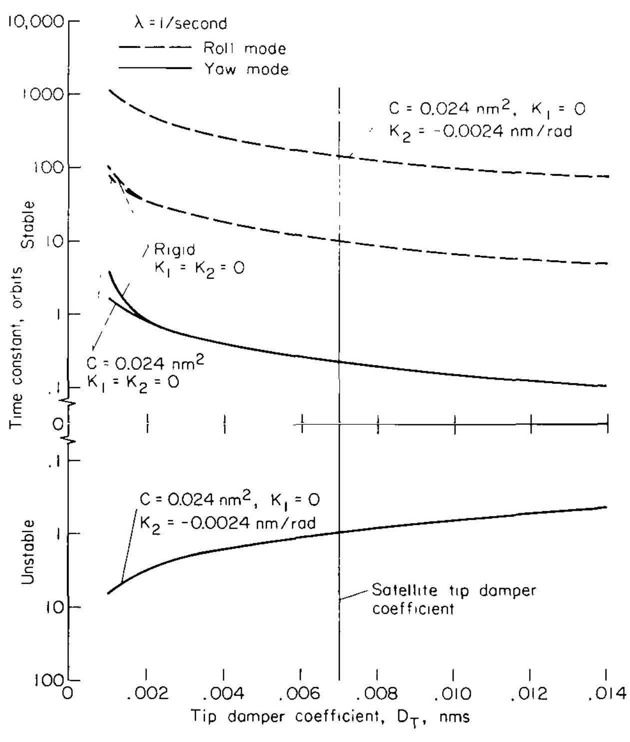

Figure 3.- Variation of roll and yaw damping with tip damper coefficient.

To provide a basis for comparison with later results, the damping of the roll and yaw modes of the satellite have been calculated assuming that the thermal torque gradients (K_1 and K_2) are zero and the satellite is rigid ($C = \infty$). The results of these calculations for various values of the tip damper coefficient are given in figure 3, in which the ordinate is the

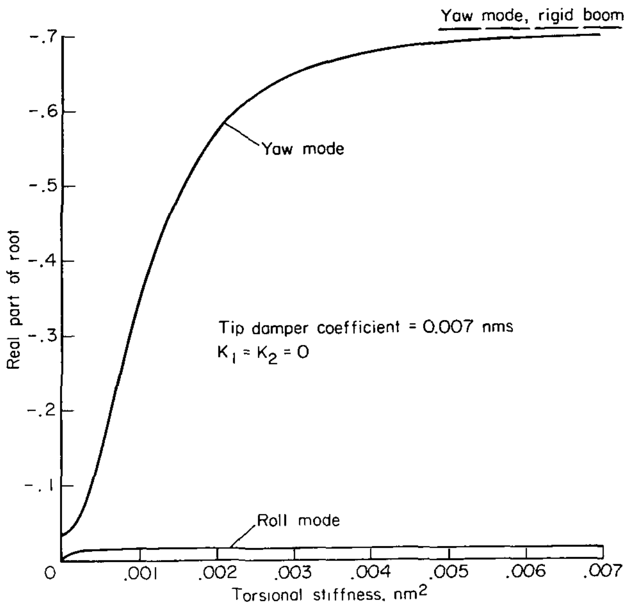

Figure 4.- Variation of roll and yaw damping with boom torsional stiffness.

damping time constant expressed as the number of orbits to 36.8% (1/e) of the initial amplitude. At the design tip damper coefficient (0.007 nms), figure 3 shows that the damping time constant in yaw is 0.23 orbit and in roll 10.4 orbits. It is clear that the rigid satellite is satisfactorily damped. The effect of boom torsional stiffness on roll and yaw damping is shown in figure 4, in which the ordinate is the real part of the particular root of the stability polynomial being considered.[2] It can be seen that the damping of the yaw mode is much more sensitive to the boom torsional stiffness than is the damping of the roll mode. For torsional stiffness greater than 0.003 nm^2, the damping in yaw is within 10% of its value when the boom is rigid. Thus with the torsional stiffness of 0.024 nm^2 obtained from the boom test data, the satellite should have essentially rigid body values for the roll and yaw damping. Figure 3 shows that differences between the damping of a satellite with a rigid boom and with a boom stiffness of 0.024 nm^2 are very slight; furthermore, these differences occur at very low values of the tip damper coefficient. Analyses of the records of the satellite transient attitude behavior following an eclipse shows that the damping is somewhat greater than the rigid body value. This could occur if the tip damper coefficient was larger than the assumed value. Perhaps of greater importance, because the observed damping is close to the rigid body value, the boom stiffness must be greater than about 0.003 nm^2. Thus the assumed stiffness value of 0.024 nm^2 is at least a possibility.

The effect of setting $K_1 = 0$, $K_2 = -0.0024$ nm/rad and $\lambda = 1$/second while maintaining the boom torsional stiffness at the measured value of 0.024 nm^2 is shown in figure 3. Over the complete range of values of the tip damper coefficient investigated the satellite is unstable in yaw. Furthermore, the frequency of the yaw mode is almost identical to the rigid body value. Table 2 lists the frequencies and damping time constants (or divergent time constants) of the roll and yaw modes, for the design value of the tip damper coefficient. Another important effect of including the thermal torques is that the roll mode damping is reduced (fig. 3, table 2). It is shown in figure 3 that

[2]The relationship between the damping time constant and the real part of root is

$$\text{damping time constant} = \frac{-1}{2\pi(\text{real part of root})}$$

TABLE 2.- FREQUENCY AND DAMPING OF THE ROLL AND YAW MODES

Condition	Roll mode		Yaw mode	
	Frequency, oscillations per orbit	Damping orbits to (1/e or e) × amp	Frequency, oscillations per orbit	Damping orbits to (1/e or e) × amp
Rigid, $K_1 = K_2 = 0$	1.63	10.4	11.51	0.23
$C = 0.024$ nm^2 $K_1 = K_2 = 0$	1.63	10.4	11.54	0.23
$C = 0.024$ nm^2 $\lambda = 1$/second $K_1 = 0$ $K_2 = -0.0024$ nm/rad	1.63	125	11.51	Divergence 0.775

whereas the damping of the roll mode increases slightly with increasing tip damping coefficient, the yaw mode displays no corresponding tendency toward stability.

The effect of varying K_2 on the stability of the roll and yaw modes is shown in figure 5. The tip damper coefficient and boom stiffness are the design values, while $K_1 = 0$, and $\lambda = 1$/second. It can be seen from figure 5 that the damping of both the roll and the yaw modes decreases as K_2 becomes more negative. For values of K_2 less than -0.00192 nm/rad the satellite is unstable in yaw; for values of K_2 less than -0.0026 nm/rad, the roll mode also becomes unstable. The roll divergence, however, always has a much longer time constant than the yaw divergence. At $K_2 = -0.003$ nm/rad, for example, the ratio of the two time constants is 162:1.

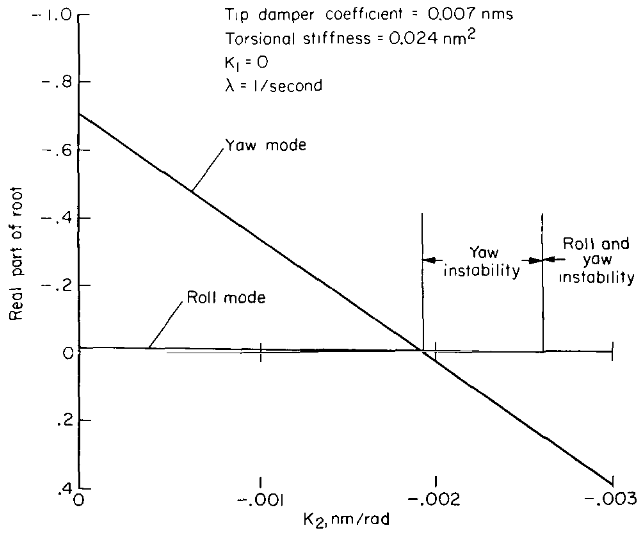

Figure 5.- Variation of roll and yaw damping with K_2.

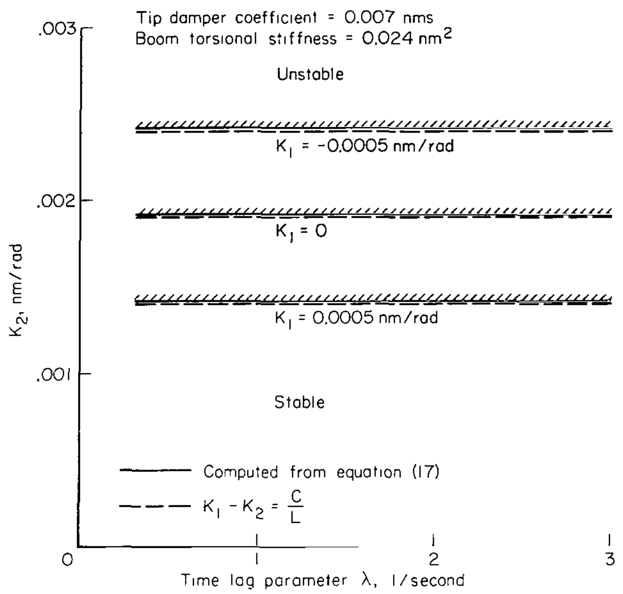

Figure 6.- Stability boundaries as functions of K_1, K_2, and λ.

Stability boundaries for the satellite are shown in figure 6. The boundaries are presented to demonstrate the influence of the poorly known quantities K_1, K_2, and λ. It is clear from figure 6 that the time-lag parameter λ is not a critical factor in deciding the stability of the satellite. This result agrees with the analysis of the simplified stability polynomial presented in the previous section. Thus, when $K_1 = 0$, a value of K_2 which is less (more negative) than -0.00192 nm/rad will drive the satellite unstable irrespective of the value of λ, at least for all values of λ greater than the lowest value (0.3/second) considered. The effect of larger values of K_1 than those shown in figure 6 has also been explored. The results agree closely with those of the previous section and show that the satellite can be made unstable in yaw with values of K_1 greater than about 0.00192 nm/rad, even with $K_2 = 0$. As predicted, the oscillatory yaw instability, in this case, is always accompanied by a simple divergence type of instability. If this situation were to occur in flight, the boom probably would diverge in twist to a new equilibrium condition. This new equilibrium condition would be dictated by the extremely nonlinear characteristics of the boom. The new equilibrium twist would have associated with it new values of K_1 and K_2, which may or may not promote oscillatory instability.

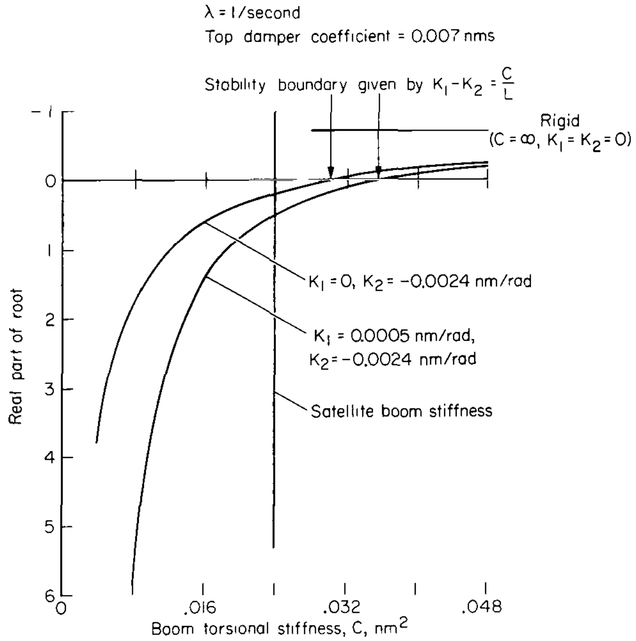

Figure 7.- Influence of torsional stiffness on the damping of the yaw mode.

It follows from the approximate stability criteria given by inequalities (27) and (28) that a possible way of stabilizing the satellite is to increase the torsional stiffness of the boom. However, this increase must not be accompanied by a corresponding increase in the values of K_1 and K_2. The effect of boom torsional stiffness on the stability of the roll and yaw modes is shown in figure 7 for two sets of values of K_1 and K_2. It can be seen that the approximate stability criteria, indicated by the arrows, predicts the onset of yaw instability accurately.

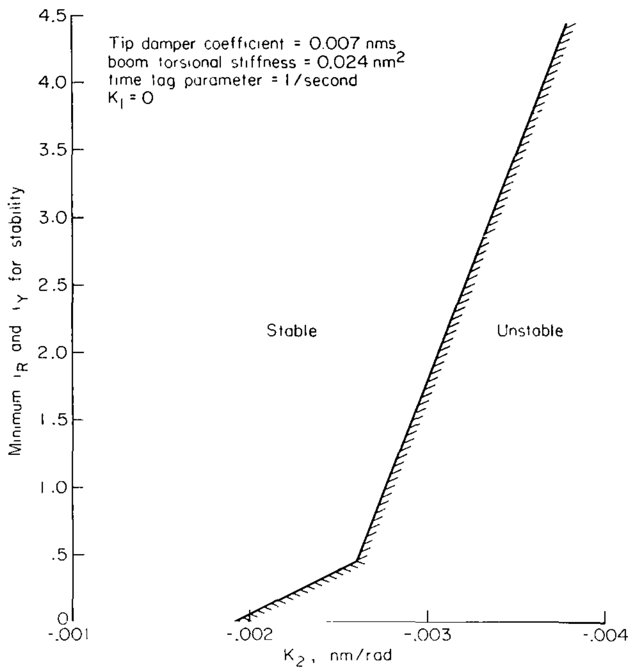

Figure 8.- Minimum damper inertia for stability.

Another possible way of removing the satellite instability is to add a passive damper to the main body. This damper could take the form of a closed vessel, rigidly mounted at the end of a short boom (say 1 m long), and containing a ball free to move through a viscous fluid. For this type of arrangement $D_R = D_Y$; if the boom is mounted along the pitch axis, $i_R = i_Y$. The results of a preliminary investigation into this kind of damper are given in figure 8, which shows the minimum damper ball inertia, about the main body center of mass, needed to stabilize the satellite. For values of K_2 between -0.00192 nm/rad and -0.0026 nm/rad, it is necessary only to stabilize the yaw mode, which can be accomplished with a very small damper. For values of K_2 less than -0.0026 nm/rad, the roll mode must also be stabilized and; in fact, becomes the major factor in determining the size of the damper. Thus, with K_2 = -0.0026 nm/rad, the damper inertia required to stabilize the satellite is only 0.45 kg/m^2, whereas with K_2 = -0.0035 nm/rad the inertia required becomes 3.50 kg/m^2, or about eight times larger.

SUGGESTED TEST FOR YAW INSTABILITY

It appears feasible to test whether future semipassive satellites of the type considered in this paper will have yaw instabilities due to thermally induced boom torsion. The suggested test apparatus is shown in figure 9. The proposed technique exploits the fact that the yaw instability is affected only slightly by the presence of the roll degree of freedom. Therefore, a meaningful test can be performed using an apparatus that duplicates the essential features of the dynamics of the satellite with its roll motion suppressed. It is important to recall that the onset of yaw instability is independent of the yaw inertia of the satellite, the tip damper coefficient, and the angular momentum of the wheel - provided these quantities satisfy certain order of magnitude relationships. This characteristic allows the designer of the test apparatus considerable latitude in selecting an arrangement compatible with the available equipment.

As shown in figure 9, the gyroscopic torque can be simulated by means of a torsion wire suspension. The boom should be the flight article; if this is not possible, however, it should be a duplicate of the flight article and, in particular, should have the same pretwist. The float at the bottom serves two

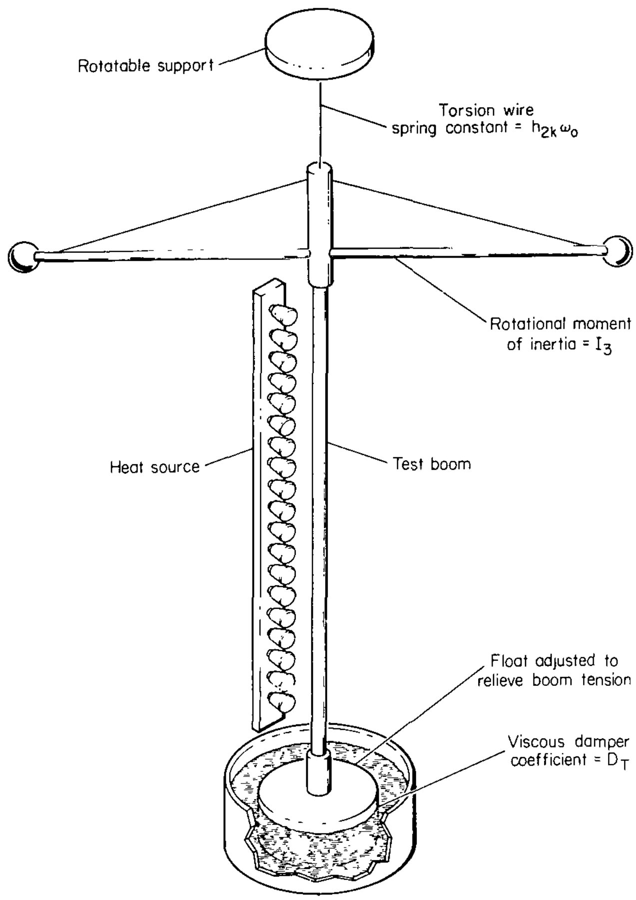

Figure 9.- Laboratory test to determine whether satellite will have yaw instability from thermally induced torsion.

functions. First, it provides rotational damping of the boom tip and thus simulates the magnetically anchored damper. Second, it supplies a buoyant force sufficient to relieve the tensile load on the boom. This force could alter the torsional properties of the boom, particularly the equilibrium twist.

The apparatus should be located so that it can be heated from one side. Standard infrared heat lamps have been found to be suitable (ref. 5). It is particularly desirable to be able to move the lamps close to the boom so as to submit it to a heat flux greater than the solar heat flux. This would give assurance that the instability is not incipient when the heat flux is equal to the solar heat flux.

It is possible that the results could be invalidated by the presence of aerodynamic forces due both to the motion of the test specimen and convection currents from the heat lamps. For this reason, it might be desirable to perform the test at reduced air densities in a facility similar to the Ames Structural Dynamics Laboratory, where the air pressure can easily be reduced to a few millimeters of mercury.

It was observed that the peculiar attitude behavior of many gravity-stabilized satellites did not start immediately after the booms had been erected. There are two possible explanations for this fact: first, at the time the booms were erected, the solar flux was not in the direction that promotes instability (fig. 2); and second, frictional forces within the boom dissipate with time (and presumably boom motion). Both these factors should be considered in testing for the instability. The frictional forces could be reduced by oscillating the apparatus for some time at an amplitude well below that which will cause any local buckling of the boom. The subsequent test for instability must cover all possible solar flux directions in a plane transverse to the boom. This requirement could be met by a very slow continuous rotation of the torsion wire support, with care taken to ensure that the time for one revolution is sufficiently long for the detection of instabilities.

CONCLUSIONS AND RECOMMENDATIONS

The results of an investigation to determine if the observed instability of a semipassive gravity-stabilized satellite may be caused by thermoelastic torques in the boom leads to the following conclusions and recommendations.

1. Thermoelastic torque gradients may exist with respect to both boom twist and rotation of the entire boom about its axis. The latter effect depends strongly on boom pretwist. Both types of torque gradient can destabilize the satellite in yaw and, if they are large enough, in roll.

2. If the thermoelastic torque gradient with respect to boom twist causes an oscillatory instability, there is always an accompanying simple divergence. For this reason, any observed satellite instability must be associated with thermoelastic torque gradients with respect to the rotation of the entire boom.

3. The presence, or absence, of the instability is independent of the thermal time constant of the boom.

4. If the boom torsional stiffness is increased sufficiently, the instability will be suppressed.

5. A body-mounted passive damper is an attractive method of suppressing the instability, provided such instability exists in yaw only.

6. It is recommended that the booms used on future satellites be as torsionally stiff as practicable and have as little pretwist as possible.

7. Prior to the launch of further satellites of this type, it is recommended that a dynamic test be performed of the type described in this paper. This test would establish, directly, whether the instability is likely to occur under flight conditions.

Ames Research Center
National Aeronautics and Space Administration
Moffett Field, Calif., 94035, May 28, 1971

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