$\mathbf{N} = (d\mathbf{L}/dt) + \omega_1 \times \mathbf{L}$, and in order that $\mathbf{J}$ remain finite, angular accelerations, but not angular velocities, are permitted to tend toward infinity during the limiting process; thus $\mathbf{J} = \Delta\mathbf{L}$ and

$$J_x = A\Delta\omega_x \qquad J_y = A\Delta\omega_y \qquad J_z = C\Delta\omega_z + C_2\Delta\dot{\gamma}$$
$$J_{z_1} = C_1\Delta\omega_z \qquad J_{z_2} = J_z - J_{z_1} \tag{20}$$

Hence, given any $\mathbf{J}$, new ω's may be computed which may be used as initial conditions in the solutions previously obtained for the free motion of the system. It is seen that, if γ and $\dot{\gamma}$ are initially zero, and if $J_{z_1} = J_{z_2} = 0$, then γ is not excited, and the system will respond as if it were a rigid body.

As an example, J_{z_2} is considered with γ and $\dot{\gamma}$ initially zero, a situation that might accompany a correction made by the spin control jets of a rotating space station. From the first, second, and fourth expressions of Eqs. (20), ω_x, ω_y, and ω_z remain initially unchanged by the impulse, since the associated impulse components are zero; $\dot{\gamma}$ changes in accordance with the remaining relations:

$$J_z = C_2\Delta\dot{\gamma} = C_2\dot{\gamma} \qquad \text{at} \quad t = 0^{(+)} \tag{21}$$

The angular velocity components, prior to application of the impulse, are defined as $\Omega_{xy}{}^0$ and $\Omega_z{}^0$. From Eqs. (2) and (21), the angular momentum following the impulse is

$$\mathbf{L} = \mathbf{n}_{xy}A\Omega_{xy}{}^0 + \mathbf{k}(C\Omega_z{}^0 + J_z) \tag{22}$$

The system immediately begins precessing about the new angular momentum vector (Fig. 3); θ may be obtained from

$$\tan\theta = A\Omega_{xy}{}^0/(C\Omega_z{}^0 + J_z) \tag{23}$$

Since $\tan\theta$, prior to the impulse, is $(A\Omega_{xy}{}^0)/(C\Omega_z{}^0)$, the angular variation is given by

$$\tan(\theta^0 - \theta) = A\Omega_{xy}{}^0J_z/[(C\Omega_z{}^0)^2 + C\Omega_z{}^0J_z + (A\Omega_{xy}{}^0)^2] \tag{24}$$

The precessional velocity is changed in accordance with Eq. (15)

$$\dot{\psi} = L/A = [(A\Omega_{xy}{}^0)^2 + (C\Omega_z{}^0 + J_z)^2]^{1/2}/A \tag{25}$$

In view of the values of γ and $\dot{\gamma}$ immediately after application of the impulse, Eq. (9) reveals that $\Omega_z = \Omega_z{}^0 + J_z/C$; thus, from Eq. (16),

$$\dot{\varphi} = -(\Omega_z{}^0 + J_z/C)(C/A - 1) - \dot{\gamma}C_2/C \tag{26}$$

It may be verified that the new θ and $\dot{\psi}$ are the same values that would occur if the system were rigid and subjected to the same angular impulse $\mathbf{k}J_z$ under the same initial conditions. Also, again assuming that the solutions to Eq. (8) are stable, $\dot{\varphi}$ and $\dot{\varphi}_2$ approach the corresponding values for the rigid system as time increases.

If $g(\gamma, \dot{\gamma})$ is linear, as in the previous example, then, from Eq. (18), $\zeta = \pi/2$, and $E = J_z/C_2p$. Hence, the relative angular motion and the spin velocities of parts 1 and 2 are, respectively,

$$\gamma = (J_z/C_2p)\exp(-qt)\sin pt \tag{27}$$

$$\dot{\varphi} = -[\Omega_z{}^0 + (J_z/C)][(C - A)/A] - (J_z/Cp)\exp(-qt)(q\sin pt - p\cos pt) \tag{28}$$

$$\dot{\varphi}_2 = -[\Omega_z{}^0 + (J_z/C)][(C - A)/A] - [(1/C_2) - (1/C)](J_z/p)\exp(-qt)(q\sin pt - p\cos pt) \tag{29}$$

Conclusions

The free motion is characterized by a relative elastic vibration between members which decays if an energy dissipation mechanism is present, whereas the over-all system precesses at a uniform rate about the system angular momentum vector and at a constant half-cone angle θ. When there is no initial vibration, an applied impulse torque, which lies in the x, y plane of the body axes, does not induce elastic motion, and the system responds as if it were rigid. If the impulse torque, applied to the outer portion, contains a z component, the new θ and precessional velocity are the same values as would occur for a rigid body, whereas the spin velocity approaches the corresponding rigid-body value as time increases.

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Handling Qualities Criteria for Manned Spacecraft Attitude-Control Systems

Robert O. Besco*
Hughes Aircraft Company, Culver City, Calif.

In specifications of system performance and handling qualities criteria for spacecraft, there does not yet appear to be common definitions of maneuvers, forcing functions, or even the control systems. The precise definitions of control modes and maneuvers are usually given in system and mission specific terms that do not permit comparisons among different systems and missions. This occurs even with an organization. When the differences between local definitions are superimposed, it is nearly impossible to compare or utilize data from various sources. For example, the direct control of reaction jets by an astronaut has been referred to as direct manual, reaction-jet, acceleration, on-off, and manual pulse-width modulation.

In the first phase of this program,[1] an attempt was made to define spacecraft attitude-control systems in the most fundamental of terms that would be common to all types, i.e., in terms of the resulting angular-acceleration response. Angular-acceleration response was defined as having the following five fundamental characteristics: amplitude, dura-

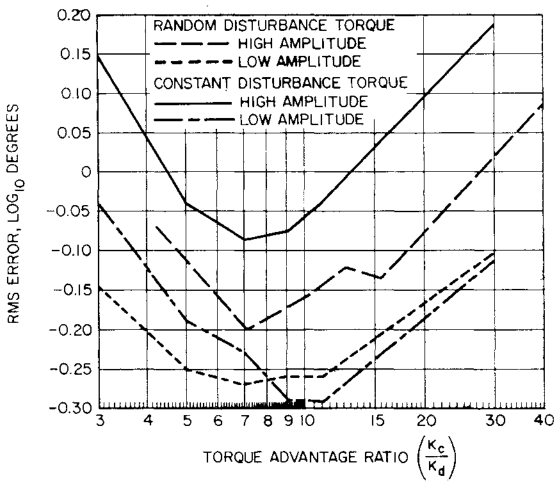

Fig. 1 RMS error as a function of TAR and type of disturbance torque.

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* Group Head, Display Systems Department, Aerospace Group.

Table I Acceleration response characteristics

Mode[a]	Amplitude, rad/sec²	Duration, msec	Initiation time, msec	Frequency, pulse/sec
Proportional acceleration	Variable, 0.008 to 0.04	Continuously variable	Continuously variable	Variable, 0 to 4
On-Off acceleration	Preset at 0.04	Continuously variable	Continuously variable	Variable, 0 to 4
Single-pulse	Preset at 0.04	Preset at 50	Restricted by requirement to complete previous pulse; maximum delay, ∽250	Variable, 0 to 5
Repeated-pulse	Preset at 0.05	Preset at 20	Restricted by requirement to complete previous pulse; maximum delay, ∽67	Preset at 15
Proportional rate	Preset at 0.04	Predetermined by control-system dynamics	Relatively unlimited except for control-system delays	Predetermined by control-system delays

[a] The direction parameter was constant for these modes involving clockwise and counterclockwise accelerations about three orthogonal axes.

tion, initiation time, frequency, and direction. It appeared that all spacecraft attitude-control systems had these characteristics, and that they could serve, at least for the present, as "common denominators" of spacecraft attitude-control systems. The determination of the relationships among 1) the vehicle response characteristics, 2) the response requirements of the maneuvers, and 3) the resultant system performance would permit a quantitative specification of handling qualities applicable to a wide variety of systems and missions. These characteristics can be equated for vehicles with different thruster sizes, masses, lever arms, mass distributions, and bending moments. Table 1 describes five modes of attitude control and their definitions using the fundamental energy release or acceleration response characteristics.

The results reported herein are concerned with specifications of the acceptable region and optimum value of the amplitude parameter. Recommended optimum values range from 0.01° to 20.0°/sec², depending on the system and/or engineer. The maneuvers or missions for which the two extreme values were recommended were quite different. The question now becomes whether these diverse recommendations can be normalized by the mission or maneuver requirements so that comparable specifications will result.

The optimum acceleration amplitude for handling qualities was specified by the ratio between the control-system torque and the peak torque required or applied by external forces, termed the torque advantage ratio (TAR). If this ratio were constant for all mission requirements and amplitudes of externally applied accelerations, it would be a very convenient parameter for specifying angular-acceleration responses to disturbance torques such as occur during earth and lunar boost, retrofire, midcourse corrections, and vacuum landings.

For other mission phases that involve stabilization and orientation of the undisturbed vehicle in free orbit, an acceleration-amplitude parameter need only consider the characteristics of the vehicle and control system. The torque-to-inertia ratio has proved to be a fundamental parameter that is very critical to system performance.[1–5] Since torque-to-inertia ratio (T/I) refers to the maximum, continuous response capabilities, a correction for duty cycle is necessary to equate the response of pulsed modes with on-off modes. The simple duty cycle correction of T/I has been termed the "Control-System Authority."[1]

Experimental Program

All simulations were conducted in the Manned Space Flight Simulation Facility in the Display Systems Department of Hughes Aircraft Company. The facility consists of general-purpose computing gear, a crew station, controls and displays, symbol generation and signal conversion equipment, and assorted equipment for recording, storage, and program control. The simulator was mechanized with the complete three-degree-of-freedom Euhler equations described in detail in Refs. 1 and 2. The control system could be set at any type of desired response. The following body rate/cross-coupling equations were mechanized:

$$\dot{p}_c = [L + (I_{yy} - I_{zz})qr]/I_{xx} \tag{1}$$

$$\dot{q} = \dot{q}_c + [M + (I_{zz} - I_{xx})pr]/I_{yy} \tag{2}$$

$$\dot{r} = \dot{r}_c + [N + (I_{xx} - I_{yy})pq]/I_{zz} \tag{3}$$

The hand controller was a three-axis stick grip with shape, force, and displacement characteristics patterned after the Gemini hand control.

Data collection was accomplished by experienced pilot engineers, trained and practiced specifically on spacecraft attitude-control problems. Experimental runs were conducted under controlled conditions using replicated and counterbalanced experimental designs. All data points were based on at least 16 observations consisting of four runs by each of four pilots. System performance was measured by rms angular error and by fuel consumption. The forcing functions or disturbance torques were specified by their amplitude and frequency. The frequency was systematically varied between a constant bias torque or zero frequency up to an upper cutoff of 2.57 rad/sec. The amplitude was varied to result in angular-acceleration disturbances up to 2.31°/sec².

The results of these studies are graphed in Figs. 1 and 2. It can be seen that accuracy is greatest for 5 < TAR < 11. Errors go up sharply outside of this region. This region was optimum for all types of displays, controls grips, and thruster

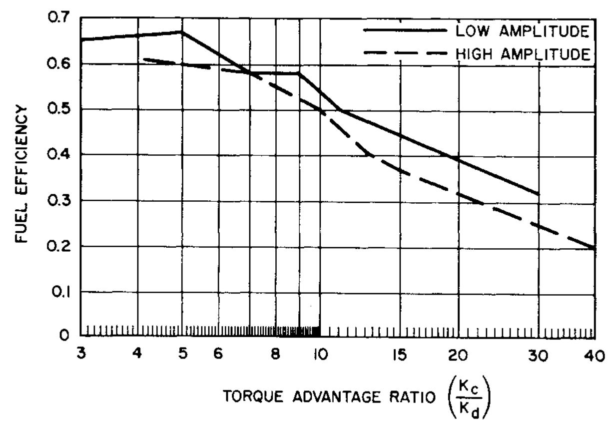

Fig. 2 Fuel efficiency as a function of TAR and disturbance torque amplitude.

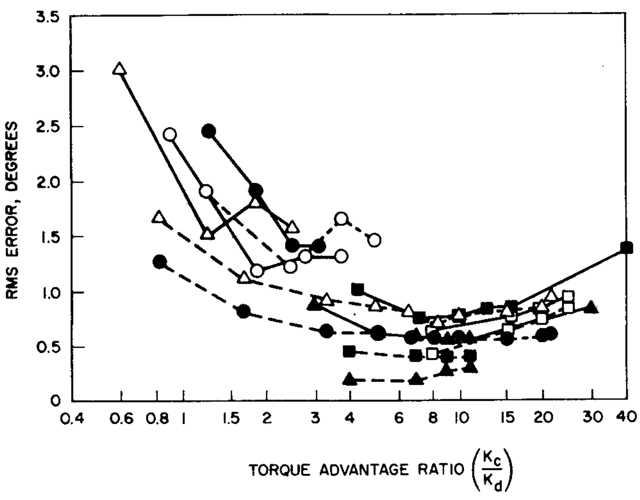

Fig. 3 Composite plot from twelve studies of the effects of TAR on rms error.

pulse characteristics as well as the amplitude and frequency characteristics of the disturbance torques. The fuel efficiency curves in Fig. 2 show knees occurring in this same TAR region. This break point was common to all of the previously mentioned system characteristics;

$$\text{fuel efficiency} = \frac{\int_0^t |\text{disturb torques}|dt}{\int_0^t |\text{control torques}|dt}$$

Figure 3 contains error plots representing more than 1000 runs from eight different experiments accomplished at Hughes and four different studies published by other agencies and companies. Most of these runs were conducted to test other system parameters but are plotted here as a function of TAR. The optimum value of TAR is near 7, with an acceptable region between 5 and 11.

Figure 4 contains a summary of the recommended angular-acceleration response for optimum handling qualities. The recommended response for free orbit, undisturbed maneuvers is indicated by the shaded region running horizontally. The CSA is expressed in degrees per second². The cross-hatched

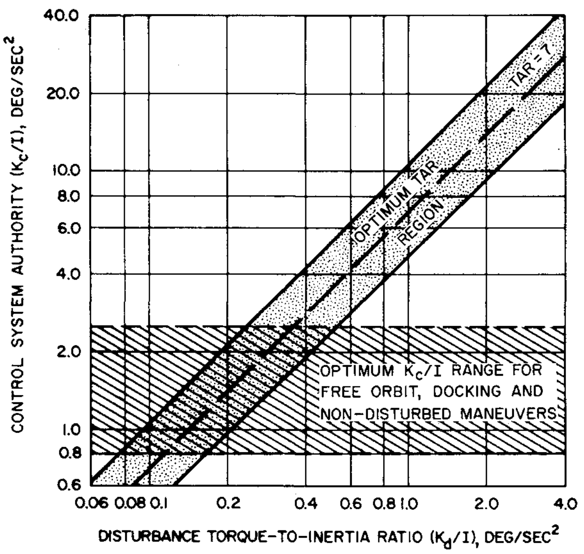

Fig. 4 Recommended angular-acceleration response for optimum handling qualities.

region running diagonally contains the recommended region for maneuvers involving disturbance torques, $5 < \text{TAR} < 11$, with the optimum at a ratio of 7. The area common to both optimum regions would indicate that a single level of acceleration response would be adequate for both disturbed and free orbit maneuvers. If the disturbance torques get too high, the optimum TAR will result in a vehicle response or CSA that is too high for optimum control during undisturbed maneuvers. This situation would indicate the need for variable or multiple levels of thrust in order to achieve satisfactory manual control.

Conclusions

It does seem possible to describe spacecraft attitude-control systems with parameters or characteristics that will be common to all types of vehicle and mission applications. The five basic characteristics presented herein constitute an acceptable set, and they can be useful in both analysis and synthesis. The handling qualities parameters derived from these basic characteristics are critical to system effectiveness. The TAR and CSA, as defined herein, are suggested as handling qualities parameters that can be defined for all systems.

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Experimental Heat-Transfer Study of Shock Impingement on Fins in Hypersonic Flow

W. Leon Francis*
Ford Motor Company, Newport Beach, Calif.

The interaction of two shock waves (e.g., that generated by a vehicle with fins) may produce a flow that impinges on a vehicle surface. This localized heating will be greater than that normally predicted because of a region of high vorticity downstream of the shock intersection. To provide data pertinent to this problem, tests were made on fin-body combinations in the Jet Propulsion Laboratory (JPL) 21-in. Hypersonic Wind Tunnel. The models used were a 5° half-angle wedge body and a 6.23° cone, both with cylindrical and wedge fins in a single plane. Data on both types of fins were taken simultaneously with a single body shock generator. Tests were conducted at $M_\infty = 9$ (nominal) and $Re_\infty = 15{,}000$ and 95,000 over a range of angle of attack

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* Aeronutronic Division. Member AIAA.