Analysis and Design of Launch Vehicle Flight Control Systems

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This paper describes the fundamental principles of launch vehicle flight control analysis and design. In particular, the classical concept of “drift-minimum” and “load-minimum” control principles is re-examined and its performance and stability robustness with respect to modeling uncertainties and a gimbal angle constraint is discussed. It is shown that an additional feedback of angle-of-attack or lateral acceleration can significantly improve the overall performance and robustness, especially in the presence of unexpected large wind disturbance. Non-minimum-phase structural filtering of “unstably interacting” bending modes of large flexible launch vehicles is also shown to be effective and robust.

I. Introduction

Note to Session Organizer/Reviewers: This draft manuscript summarizes very preliminary results obtained during an early phase of a project for the launch vehicle flight control systems analysis and design as applied to Ares-I Crew Launch Vehicle. During the next several months, a more detailed, rigorous study will be conducted in the areas of drift-minimum vs load-minimum control, flexible-body stabilization and analysis, gain scheduling vs. adaptive control, etc. A companion paper on dynamic modeling of large flexible launch vehicles is also being submitted to this Space Exploration and Transportation GNC session.

II. Rigid-Body Control Analysis

Consider a simplified linear dynamical model of a launch vehicle [15], as illustrated in Fig. 2, as follows:

\[ \ddot{\theta} = M_\alpha \alpha + M_\delta \delta \]  \hspace{0.5cm} (1)

\[ \ddot{Z} = -\frac{F}{m} \theta - \frac{N_\alpha}{m} \alpha + \frac{T}{m} \delta \]  \hspace{0.5cm} (2)

\[ \alpha = \dot{\theta} + \frac{\dot{Z}}{V} + \alpha_w \]  \hspace{0.5cm} (3)

\[ F = T_\alpha + T - D \]  \hspace{0.5cm} (4)

where \( \theta \) is the pitch attitude, \( \alpha \) the angle of attack, \( Z \) the inertial Z-axis drift position of the center-of-mass, \( \dot{Z} \) the inertial drift velocity, \( m \) the vehicle mass, \( T_\alpha \) the ungimbled sustainer thrust, \( T \) the gimbaled thrust, \( N = N_\alpha \alpha \) the aerodynamic normal (lift) force acting on the center-of-pressure, \( D \) the aerodynamic axial (drag) force, \( F \) the total

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Fig. 1. Comparison of Space Shuttle, Ares I, Ares V, and Saturn V Launch Vehicles [1].

x-axis force, \( \delta \) the gimbal deflection angle, \( V \) the vehicle velocity, \( \alpha_w = V_w/V \) the wind-induced angle of attack, \( V_w \) the wind disturbance velocity, and

\[
M_{\alpha} = x_c N_{\alpha} / I_y \quad (5)
\]
\[
M_{\delta} = x_c g T / I_y \quad (6)
\]
\[
N_{\alpha} = \frac{1}{2} \rho V^2 SCN_{\alpha} \quad (7)
\]

where \( I_y \) is the pitch moment of inertia. For an effective thrust vector control of a launch vehicle, we need

\[
M_{\delta} \delta_{\text{max}} > M_{\alpha} \alpha_{\text{max}} \quad (8)
\]

where \( \delta_{\text{max}} \) is the gimbal angle constraint and \( \alpha_{\text{max}} \) is the maximum wind-induced angle of attack.

The open-loop transfer functions from the control input \( \delta(s) \) can then be obtained as

\[
\frac{\theta(s)}{\delta(s)} = \frac{s}{\Delta(s)} \left[ M_{\delta} \left( s + N_{\alpha} / mV \right) + M_{\alpha} T / mV \right] \quad (9)
\]
\[
\frac{Z(s)}{\delta(s)} = \frac{1}{\Delta(s)} \left[ \frac{T}{m} \left( s^2 - M_{\alpha} \right) - \frac{M_{\alpha} (F + N_{\alpha})}{m} \right] \quad (10)
\]
\[
\frac{\alpha(s)}{\delta(s)} = \frac{s}{\Delta(s)} \left[ \frac{T}{m V} s^2 - M_{\delta} s + \frac{M_{\alpha} F}{m V} \right] \quad (11)
\]

where

\[
\Delta(s) = s \left[ s^2 + \frac{N_{\alpha} \rho V}{m V} s^2 - M_{\alpha} s + \frac{M_{\alpha} F}{m V} \right] \quad (12)
\]

Consequently, the 4th-order system described by Eq. (1) - (3) is completely controllable by \( \delta \) and is observable by \( Z \); however, the system is not observable by \( \theta \) and \( \alpha \).

In 1959, Hoelkner introduced the “drift-minimum” and “load-minimum” control concepts as applied to the launch vehicle flight control system [6]. The concepts have been further investigated in [7-14]. Basically, Hoelkner’s controller utilizes a full-state feedback control of the form

\[
\delta = -K_1 \theta - K_2 \dot{\theta} - K_3 \alpha \quad (13)
\]
for a 3rd-order dynamical model of the form

$$\frac{d}{dt} \begin{bmatrix} \theta \\ \dot{\theta} \\ \alpha \end{bmatrix} = \begin{bmatrix} 0 & 1 & 0 \\ 0 & 0 & M_\alpha \\ -F/(mV) & 1 & -N_\alpha/(mV) \end{bmatrix} \begin{bmatrix} \theta \\ \dot{\theta} \\ \alpha \end{bmatrix} + \begin{bmatrix} 0 \\ M_\delta \\ T/(mV) \end{bmatrix} \delta + \begin{bmatrix} 0 \\ 0 \\ \dot{\alpha}_w \end{bmatrix}$$

(14)

This 3rd-order system is observable by $\theta$ or $\alpha$. The feedback gains are to be properly selected to minimize the lateral drift velocity $\dot{Z} = V(\alpha - \dot{\theta} - \alpha_w)$ or the bending moment caused by the angle of attack. Note that

$$\frac{\dot{Z}}{V} \equiv \gamma = \alpha - \dot{\theta} - \alpha_w$$

(15)

where $\gamma$ is often called the flight-path angle.

Instead of measuring the angle-of-attack, we may employ a body-mounted accelerometer, as illustrated in Fig. 2, as follows:

$$\delta = -K_1 \theta - K_2 \dot{\theta} + K_a \ddot{z}_m$$

$$= -K_1 \theta - K_2 \dot{\theta} + K_a \left( -\frac{N_\alpha}{m} \alpha + \frac{T}{m} \delta + \frac{x_a}{m} \dot{\theta} \right)$$

$$= -K_1 \theta - K_2 \dot{\theta} + K_a \frac{x_a}{m} \dot{\theta} - K_a \frac{N_\alpha}{m} \alpha + K_a \frac{T}{m} \delta$$

Because the resulting effect of $\ddot{z}_m$ feedback is basically the same as the $\alpha$ feedback, we consider here only the control logic described by Eq. (13).

Substituting Eq. (13) into Eq. (1) - (2) or Eq. (14), we obtain the closed-loop transfer function from the wind disturbance $\alpha_w(s)$ to the drift velocity $\dot{Z}(s)$ as

$$\frac{\dot{Z}}{\alpha_w V} = -\frac{A_2 s^2 + A_1 s + A_0}{s^3 + B_2 s^2 + B_1 s + B_0}$$

(16)
where

\[ B_2 = M_3 K_2 + \frac{T}{mV} \left( K_3 + \frac{N_\alpha}{T} \right) \]

\[ B_1 = M_3 (K_1 + K_3) - M_\alpha + \frac{K_2 T}{mV} \left( M_\alpha + \frac{M_3 N_\alpha}{T} \right) \]

\[ B_o = \frac{TK_1}{mV} \left( M_\alpha + \frac{M_3 N_\alpha}{T} \right) - \frac{F}{mV} (M_3 K_3 - M_\alpha) \]

\[ A_2 = \frac{T}{mV} \left( K_3 + \frac{N_\alpha}{T} \right) \]

\[ A_1 = \frac{K_2 T}{mV} \left( M_\alpha + \frac{M_3 N_\alpha}{T} \right) \]

\[ A_o = B_o \]

For a unit-step wind disturbance of \( \alpha_w(s) = 1/s \), the steady-state value of \( \dot{Z} \) can be found as

\[ \frac{\dot{Z}_{ss}}{V} = \lim_{s \to 0} \frac{-(A_2 s^2 + A_1 s + A_o)}{s^3 + B_2 s^2 + B_1 s + B_o} = \frac{-A_o}{B_o} = -1 \] (17)

The launch vehicle drifts along the wind direction with \( \dot{Z}_{ss} = -V_w \) and also with \( \theta = \dot{\theta} = \alpha = \delta = 0 \) as \( t \to \infty \).

It is interesting to notice that the steady-state drift velocity (or the flight path angle) is independent of feedback gains provided an asymptotically stable closed-loop system with \( B_o \neq 0 \).

If we choose the control gains such that \( B_o = 0 \) (i.e., one of the closed-loop system roots is placed at \( s = 0 \)), the steady-state value of \( \dot{Z} \) becomes

\[ \frac{\dot{Z}_{ss}}{V} = \lim_{s \to 0} \frac{-(A_2 s + A_1)}{s^3 + B_2 s^2 + B_1 s + B_o} = \frac{-A_1}{B_1} = \frac{-1}{1 + C} \] (18)

where

\[ C = \frac{mV[M_3 (K_1 + K_3) - M_\alpha]}{M_3 K_2^2 + M_3^2 N_\alpha / T} \] (19)

For a stable closed-loop system with \( M_3 (K_1 + K_3) - M_\alpha > 0 \), we have \( C > 1 \) and

\[ |\dot{Z}_{ss}| < V_w \] (20)

when \( B_o = 0 \). The drift-minimum condition, \( B_o = 0 \), can be rewritten as

\[ \frac{M_3 K_3 - M_\alpha}{M_3 K_1} = \frac{N_\alpha}{F} \left( 1 + \frac{x_{cg}}{x_{cg}} \right) \] (21)

Consider the following closed-loop transfer functions:

\[ \frac{\alpha}{\alpha_w} = -\frac{s(s^2 + M_3 K_2 s + M_3 K_1)}{s^3 + B_2 s^2 + B_1 s + B_o} \] (22)

\[ \frac{\delta}{\alpha_w} = -\frac{s(K_3 s^2 + M_3 K_2 s + M_\alpha K_1)}{s^3 + B_2 s^2 + B_1 s + B_o} \] (23)

For a unit-step wind disturbance of \( \alpha_w(s) = 1/s \), we have \( \alpha = \delta = 0 \) as \( t \to \infty \). However, for a unit-ramp wind disturbance of \( \alpha_w(s) = 1/s^2 \), we have

\[ \lim_{t \to \infty} \alpha(t) = M_3 K_1 \]

\[ \lim_{t \to \infty} \delta(t) = M_\alpha K_1 \]

Consequently, the bending moment induced by \( \alpha \) and \( \delta \) can be minimized by choosing \( K_1 = 0 \), which is the “load-minimum” condition introduced by Hoelkner [6]. The closed-loop system with \( K_1 = 0 \) is unstable because

\[ B_o = -\frac{F}{mV} (M_3 K_3 - M_\alpha) < 0 \] (24)
However, the load-minimum control for short durations has been known to be acceptable provided a deviation from the nominal flight trajectory is permissible.

A set of full-state feedback control gains, \((K_1, K_2, K_3)\), can be found by using a pole-placement approach or the linear-quadratic-regulator (LQR) control method [21-22], as follows:

\[
\min_{\delta} \int_{0}^{\infty} (x^T Q x + \delta^2) dt
\]

subject to \(\dot{x} = Ax + B\delta\) and \(\delta = -K x\) where \(x = [\theta \; \dot{\theta} \; \alpha]^T\) and \(K = [K_1 \; K_2 \; K_3]\).

### III. Rigid-Body Control Example

Consider a launch vehicle control design example discussed by Greensite in [15]. Its basic parameters are given as in [15]

\[
\begin{align*}
I_y &= 2.43E6 \text{ slug-ft}^2, \quad m = 5830 \text{ slug}, \quad T = 341,000 \text{ lb} \\
F &= 375,000 \text{ lb}, \quad x_{cp} = 38 \text{ ft}, \quad x_{cg} = 32.3 \text{ ft} \\
V &= 1320 \text{ ft/sec}, \quad V_w = 132 \text{ ft/sec}, \quad \alpha_w = 5.73 \text{ deg} \\
N_a &= 240,000 \text{ lb/ rad}, \quad M_{\alpha} = 3.75 \text{ s}^{-2}, \quad M_\delta = 4.54 \text{ s}^{-2}
\end{align*}
\]

The open-loop poles of this example vehicle are: -1.9767, 0.0488, 1.8967

Note that the wind-induced angle of attack of 5.73 deg considered for this example in [15] is somewhat unrealistic because it will require a maximum gimbal deflection angle of

\[
\delta_{\max} > \frac{M_\alpha}{M_\delta} \alpha_w = 4.73 \text{ deg}
\]

Most practical thrust vector control systems have a maximum gimbal angle constraint of about \(\pm 5\) deg. In this paper, we also assume a second-order gimbal actuator dynamics of the form

\[
\delta(s) = \frac{\omega_n^2}{s^2 + 2\zeta \omega_n s + \omega_n^2} \delta_c(s)
\]

where \(\zeta = 1\) and \(\omega_n = 50 \text{ rad/s}\).

<table>
<thead>
<tr>
<th>Case No.</th>
<th>Controller Type</th>
<th>Feedback Gains ((K_1, K_2, K_3))</th>
<th>Closed-Loop Poles</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>((\theta, \theta))-Feedback Control [15]</td>
<td>(2, 0.8, 0)</td>
<td>-1.7488±1.3934j, -0.1596</td>
</tr>
<tr>
<td>2</td>
<td>Drift-Minimum Control [15]</td>
<td>(2, 0.8, 3.614)</td>
<td>-1.9087±4.2774j, 0.0</td>
</tr>
<tr>
<td>3</td>
<td>Load-Minimum Control [15]</td>
<td>(0, 0.8, 3.614)</td>
<td>-1.9323±3.0533j, 0.0471</td>
</tr>
<tr>
<td>4</td>
<td>LQR Control ((Q = 0))</td>
<td>(0.6852, 0.8491, 0.9542)</td>
<td>-1.9767, -1.8967, -0.0488</td>
</tr>
<tr>
<td>5</td>
<td>Drift-Minimum Control</td>
<td>(0.3220, 0.8352, 1.2765)</td>
<td>-1.9767, -1.8967, 0.0</td>
</tr>
<tr>
<td>6</td>
<td>Load-Minimum Control</td>
<td>(0, 0.8352, 1.2765)</td>
<td>-3.1323, -0.7816, 0.0405</td>
</tr>
</tbody>
</table>

### IV. Flexible-Body Control Analysis

More detailed control and stability analysis results for Figs. 10 and 11 will be included in the final manuscript.
Figure 3. $(\theta, \dot{\theta})$-feedback control (Case 1).

Figure 4. Drift-minimum control (Case 2).
Figure 5. Load-minimum control (Case 3).

Figure 6. LQR control (Case 4).
Figure 7. Drift-minimum control (Case 5).

Figure 8. Load-minimum control (Case 6).
Figure 9. Case 5 (drift-minimum control) with $\delta_{\text{max}} = \pm 5$ deg.

Figure 10. Illustrations of dominant bending modes and sensor locations (Ref. 2).
Figure 11. Nichols plot for a baseline pitch-axis flight control system (Ref. 2).
V. Conclusions

References