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DEVELOPMENT OF HANDLING QUALITY CRITERIA FOR AIRCRAFT WITH INDEPENDENT CONTROL OF SIX DEGREES OF FREEDOM

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Final Report for period October 1978 - July 1981

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20. ABSTRACT (Continue on reverse side if necessary and identify by block number) A tentative flying quality criterion has been developed for aircraft with direct force controls which allow independent control over the six inertial degrees of freedom. The criterion is based on analysis of existing flight test and simulation data as well as the results of an abbreviated flight test conducted during the program.			

PREFACE

The work reported herein was accomplished during the period from October 1978 to July 1980 under Contract F33615-78-C-3616. Mr. Samuel Craig served as the STI project engineer during the initial analytical work. Mr. Roger Hoh was the project engineer during the flight test and criteria development phases of the study.

The authors wish to express their gratitude to Messrs. David Moorhouse and Robert Woodcock (AFWAL) for their significant contributions to this final report. We would also like to thank Lt. Jack Browne (AFWAL) for his considerable assistance at Princeton University during the flight test phase of the study. Lt. Browne was the USAF Contract Technical Monitor on the project.

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TABLE OF CONTENTS

	<u>Page</u>
I. INTRODUCTION.....	1
II. BACKGROUND CONSIDERATIONS.....	3
A. Literature Review.....	3
B. Independent Control and Decoupling.....	7
C. Practical (Imperfect) Decoupling.....	12
D. Response Qualities with Direct Force Controls.....	18
E. Approaches Considered and Rejected.....	41
III. BANDWIDTH HYPOTHESIS.....	43
A. Basic Considerations.....	43
B. Bandwidth and Response.....	45
C. Physical Significance of Bandwidth.....	50
D. Systems Implication of High Bandwidth.....	56
E. The Bandwidth Hypothesis.....	60
F. Justification for Bandwidth Hypothesis.....	61
IV. DESCRIPTION OF FLIGHT TEST.....	66
A. Air-to-Air Tracking Task.....	67
B. Development of Test Configurations.....	75
C. In-Flight Vehicle Identification.....	76
V. FLIGHT TEST RESULTS.....	95
A. Control Sensitivity.....	98
B. Lateral Translation Configurations.....	103
C. Comparison with Ground-Based Simulation.....	105
VI. PROPOSED FLYING QUALITIES SPECIFICATIONS.....	106
A. MIL-Standard Requirements for Direct Force Controls.....	106
B. Handbook Discussion for Direct Force Controls.....	107
APPENDIX A. BASIC DATA FOR YF-16 CCV ANALYSIS.....	113
APPENDIX B. FORMULATION OF EQUIVALENT STABILITY DERIVATIVE MODEL IMPLEMENTED ON THE VRA.....	178

	<u>Page</u>
APPENDIX C. PILOT COMMENTARY AND RATINGS.....	186
APPENDIX D. DISCUSSION OF COUPLING NUMERATORS, LIMITING FORMS, AND DECOUPLED MODES.....	218
REFERENCES.....	227

LIST OF FIGURES

	<u>Page</u>
1. Generalized Control Interconnect.....	9
2. Equivalent Lateral Control System Structure for YF-16 CCV.....	14
3. Comparison of YF-16 CCV and Ideal Rudder Crossfeeds for β_2 Mode.....	15
4. General Rudder Crossfeed Effect on Heading Response — WLT Mode.....	17
5. Sketch Showing Piper Error Geometry.....	21
6. Sketch Showing Longitudinal Aim Error Response for Conventional Aircraft.....	23
7. Sketch Showing Aim Error Response in Pitch Pointing Mode.....	26
8. Sketch Showing Ideal Aim Error Response in Vertical Translation Mode.....	27
9a. Longitudinal Pilot/Vehicle/FCS Loop Structures.....	34
9b. Lateral-Directional Pilot/Vehicle/FCS Loop Structures.....	35
10. First-Order Bandwidth/Response Relations.....	46
11. The Phase-Margin/Closed-Loop Damping Relationship for $G(s) = Ke^{-Ts}/s$	47
12. "Desirable" Closed-Loop Second-Order Characteristics Deduced Using Analytic Expressions for Y_p Consistent with Describing Function Data.....	47
13. Effect of Using Gain and Phase Margins to Define Bandwidth.....	49
14. Classical Roll Dynamics with $\omega_\phi = \omega_d$	51
15. Effect of Roll-Yaw Coupling on Bandwidth.....	55
16. Effective Airframe Dynamics Pilot-Command/Disturbance Aircraft Response Relationships.....	58
17. Implications of Bandwidth Criterion on Aircraft Design.....	60

	<u>Page</u>
18. Correlation of Bandwidth with Pilot Rating; Moving-Base Simulation, Dive Bombing Task.....	64
19. Tracking Kinematics and Dynamics for Wings-Level Turn.....	68
20. Effect of Range on Tracking Kinematics.....	69
21. Comparison of Heading Dynamics and Pipper Error Dynamics for Wings-Level Turn.....	71
22. Typical Pilot Switching Program.....	73
23. Frequency Content of Input Used for Target Aircraft.....	74
24. Photo of VRA in Flight.....	75
25. VRA as Mechanized for Simulation.....	77
26. Response to Step Input of DFC Control: Wings-Level Turn Mode.....	78
27. Typical DFC Control "Frequency Sweep" and Response for Configuration Identification.....	81
28. Fourier Transformed Heading Response.....	82
29. Fourier Transformed Sideslip Angle Response.....	93
30. Correlation of Pilot Ratings with Heading Bandwidth; Wings-Level Turn Mode; Air-to-Air Tracking Task.....	96
31. Effect of DFC Manipulator Sensitivity, Configuration WLT1.....	99
32. Effect of DFC Manipulator Sensitivity, Configuration WLT5 (High Favorable Yaw Coupling).....	100
33. Effect of DFC Manipulator Sensitivity, Configuration WLT12 (Very High Favorable Roll Coupling).....	101
34. Correlation of Pilot Ratings with Heading Bandwidth for Conventional Aircraft; Task is ILS Approach.....	110
35. Longitudinal Flight Control System, Conventional Flight Mode.....	118
36. Longitudinal Flight Control System, Direct Lift (A_N) Flight Mode.....	119
37. Longitudinal Flight Control System, Pitch Point (α_1) Mode.....	120

	<u>Page</u>
38. Longitudinal Flight Control System, Vertical Translation (α_2) Mode.....	121
39. Longitudinal Flight Control System, Maneuver Enhancement (ME) Mode.....	122
40. Lateral-Directional Flight Control System, Conventional Flight Mode.....	123
41. Lateral-Directional Flight Control System, Direct Side Force (A_Y) Mode.....	124
42. Lateral-Directional Flight Control System, Yaw Pointing (β_1) Mode.....	125
43. Lateral-Directional Flight Control System, Lateral Translation (β_2) Mode.....	126
44. YF-16 Attitude Response, Conventional Flight Mode.....	128
45. YF-16 CCV Variation of Aim Error Response with Range, Conventional Flight Mode.....	130
46. YF-16 Variation of Aim Error Response with Range, Conventional Flight Mode.....	46
47. Pilot Loop Closure, YF-16 CCV Conventional Flight Mode.....	132
48. YF-16 CCV Control Activity, Conventional Flight Mode, 3 g Turn.....	135
49. YF-16 CCV Aim Error Response, Direct Lift Mode.....	136
50. Comparison of YF-16 CCV Aim Error Response.....	137
51. Control Activity, YF-16 CCV Maneuver Enhancement Mode, 3 g Turn.....	138
52. YF-16 CCV Aim Error Response, α_1 Flight Mode.....	140
53. YF-16 CCV Path Angle Response, Vertical Translation Mode.....	142
54. YF-16 CCV Aim Error Response, Vertical Translation Flight Mode.....	144
55. Equivalent DFC Control Structure Used in Analyses of Vertical Translation Mode.....	145

	<u>Page</u>
56. Path Angle Response, Vertical Translation Mode with Ideal Crossfeed.....	147
57. Aim Error Response, Vertical Translation Mode with Ideal Crossfeed.....	148
58. Response of +1 lb F_B	150
59. Comparison of Actual and Ideal Crossfeeds, Pitch Pointing Mode.....	151
60. Aim Error Response, Pitch Pointing Mode, with Improved Crossfeed.....	152
61. Comparison of Actual and Ideal Crossfeeds, A_N Mode.....	153
62. Roll Response, Conventional Flight Mode.....	155
63. Aim Error Response, Direct Side Force Mode.....	157
64. Canard-to-Aileron Crossfeed Comparison, A_Y Mode.....	159
65. Canard-to-Rudder Crossfeed Comparison, A_Y Mode.....	160
66. Aim Error Response, A_Y Mode with Improved Crossfeed Gains.....	161
67. Comparison of Sideslip Responses, Original and Revised Crossfeeds for A_Y Mode.....	162
68. Aim Error Response, Yaw Pointing (β_1) Mode.....	164
69. Canard-to-Aileron Crossfeed Comparison, Yaw Pointing (β_1) Mode.....	165
70. Canard-to-Rudder Crossfeed Comparison, Yaw Pointing (β_1) Mode.....	166
71. Aim Error Response with Improved Crossfeed Gains, Yaw Pointing (β_1) Mode.....	168
72. Aim Error Response, Lateral Translation (β_2) Mode.....	169
73. Canard-to-Aileron Crossfeed Comparison, Lateral Translation (β_2) Mode.....	170
74. Canard-to-Rudder Crossfeed Comparison, Lateral Translation (β_2) Mode.....	171

	<u>Page</u>
75. Aim Error Response with Improved Crossfeed Gains, Lateral Translation (β_2) Mode.....	172
76. Aim Error Response with Exact Match to $Y_{\delta_e}^{\delta R}$	174
77. Comparison of Yaw Rate Responses; Original and Revised Crossfeeds, Lateral Translation (β_2) Mode.....	177
78. Yaw Rate Response to Rudder.....	180
79. Sideslip Angle Response to Rudder.....	181
80. Sideslip Angle Response to Roll Control.....	182
81. Time Domain Comparison of Basic and Lower-Order Models.....	184
82. Pilot Briefing Summary.....	188
83. Pilot Comment Card.....	188
84. Generic Representation of Lateral Translation Mode.....	219

LIST OF TABLES

	<u>Page</u>
1. Summary of Useful Literature.....	4
2. Limiting Responses for DFC Response Modes.....	13
3. Aim Error Responses, Longitudinal Degrees of Freedom.....	30
4. Essential Descriptors for Key Flying Tasks.....	31
5. Pilot Loop Closures for Three Flying Tasks.....	33
6. Pilot/Vehicle System Requirements.....	62
7. Summary of Test Configurations.....	80
8. Summary of Cooper-Harper Pilot Ratings for Lateral Translation Mode.....	104
9. Typical Aircraft Parameters Subject to Bandwidth Limitation in MIL Standard.....	108
10. Tentative Bandwidth Limitations.....	109
11. Longitudinal Data.....	114
12. Lateral-Directional Data.....	115
13. Pilot Rating Summary, Wings-Level Turn Configurations.....	187

LIST OF SYMBOLS

a	Speed of sound in air (ft/sec)
a_n	Normal acceleration at center of mass (ft/sec ² or g)
\hat{a}_n	Normal acceleration at accelerometer (ft/sec ² or g)
a_y	Lateral acceleration at center of mass (ft/sec ² or g)
\hat{a}_y	Lateral acceleration at accelerometer (ft/sec ² or g)
\hat{a}_z	z-axis acceleration at a distance l_x forward of the center of mass; $\hat{a}_z = a_z - l_x \dot{q}$ (ft/sec ² or g)
A	Characteristic matrix
b	Reference wing span (ft)
B	Control derivative matrix
c	Reference wing chord (ft)
c.g.	Center of gravity
d	Height above target or path deviation (ft)
D	Aerodynamic force (drag) along the total velocity vector, positive aft (lb)
e	Base for natural logarithms
F	Generalized force vector
F_b	CCV button force
F_s	Stick force (lb)
g	Gravitational constant (32.2 ft/sec ²)
G_f	Generic feedback loop element
h	Altitude (ft)
I_x, I_y, I_z	Moments of inertia referred to body-fixed stability axes (slug-ft ²)
I_{xz}	Product of inertia referred to body axes (slug-ft ²)

K	Generic gain
K_p	Pilot gain
l_x	Distance along the x-body axis forward of the center of mass, positive down (ft)
L	Rolling moment about the x-axis due to aerodynamic torques, positive right wing down (ft-lb)
L	Aerodynamic force (lift) perpendicular to the total velocity vector in the aircraft's plane of symmetry, positive up (lb)
m	Mass (slugs)
M	Mach number
M	Pitching moment about the y-axis due to aerodynamic torques, positive nose up (ft-lb)
N	Aerodynamic normal force along the z-body axis, positive up (lb)
N	Yawing moment about z-axis due to aerodynamic torques, positive nose right (ft-lb)
p	Perturbation roll rate about x stability axis (rad/sec)
p_g	Perturbation roll rate about gyro axis (rad/sec)
q	Perturbation pitch rate (rad/sec)
\bar{q}	Dynamic pressure, $(1/2)\rho V_{T_0}^2$ (lb/ft ²)
r	Perturbation yaw rate (rad/sec)
r_G	Yaw rate gyro signal (rad/sec)
R	Range
s	Laplace variable (1/sec)
S	Reference planform area (ft ²)
T	Engine thrust (lb)
u	Perturbation velocity, x-axis (ft/sec)
U_0	Equilibrium velocity, x-axis (ft/sec)

v	Linear perturbed velocity along the y-axis, positive out right wing (ft/sec)
V_T	Total velocity (ft/sec)
V_{T_0}	Total linear steady-state velocity (ft/sec)
w	Perturbation heave velocity, z-axis (ft/sec)
W	Weight (lb)
W_0	Linear steady-state velocity along the z-axis, positive down (ft/sec)
X	Response command vector
y	Lateral displacement (ft)
Y	Generic transfer function
Y_p	Pilot describing function
Z	Aerodynamic force along z-axis, positive down (lb)
α	Perturbation angle of attack (rad)
α_G	Orientation of gyro with respect to x axis
β	Perturbation sideslip angle (rad)
γ	Perturbation flight path angle (rad)
γ_0	Equilibrium flight path angle
δ	Control vector
δ_A	Roll control deflection (rad)
δ_C	Canard deflection command (rad)
δ_{DFC}	Direct force cockpit control deflection (in.)
δ_L	Flap deflection (direct lift control) (rad)
δ_e	Horizontal tail deflection (pitch control) (rad)
δ_p	Generic cockpit controller deflection
δ_R or δ_r	Rudder deflection
δ_{SF}	Side force surface deflection (generic decoupled side force control) (rad)

δ_{th}	Throttle deflection
δ_T	Thrust control variable
δ_{vc}	Vertical canard deflection (rad)
Δ	Transfer function denominator
ϵ	Pipper error (rad)
ζ	Damping ratio
θ	Perturbation pitch attitude (rad)
λ	Perturbation lateral path angle (rad)
ρ	Mass density of air (slug-ft ³)
τ	Time delay (sec)
ϕ	Perturbation bank angle (rad)
ψ	Perturbation heading (rad)
ω	Natural frequency (rad/sec)

NONDIMENSIONAL DERIVATIVES

Longitudinal, Normal/Chord Force

$$C_N = N/\bar{q}S, \text{ positive up}$$

$$C_X = -(X/\bar{q}S), \text{ positive aft} \quad C_M = M/\bar{q}Sc$$

$$C_{N\alpha} = \partial C_N / \partial \alpha \quad C_{M\alpha} = \partial C_M / \partial \alpha$$

$$C_{N\dot{\alpha}} = (2V_{T_0}/c)(\partial C_N / \partial \dot{\alpha}) \quad C_{M\dot{\alpha}} = (2V_{T_0}/c)(\partial C_M / \partial \dot{\alpha})$$

$$C_{NM} = \partial C_N / \partial M \quad C_{MM} = \partial C_M / \partial M$$

$$C_{N\delta} = \partial C_N / \partial \delta \quad C_{Mq} = (2V_{T_0}/c)(\partial C_M / \partial q)$$

$$C_{X\alpha} = \partial C_X / \partial \alpha$$

$$C_{XM} = \partial C_X / \partial M$$

$$C_{X\delta} = \partial C_X / \partial \delta$$

Longitudinal, Lift/Drag

$$C_L = L/\bar{q}S, \text{ positive up}$$

$$C_D = D/\bar{q}S, \text{ positive aft}$$

$$C_{L\alpha} = \partial C_L / \partial \alpha$$

$$C_{L\dot{\alpha}} = (2V_{T_0}/c)(\partial C_L / \partial \dot{\alpha})$$

Pitching moment derivatives are identical to those above

$$C_{LM} = \partial C_L / \partial M$$

$$C_{L\delta} = \partial C_L / \partial \delta$$

$$C_{D\alpha} = \partial C_D / \partial \alpha$$

$$C_{DM} = \partial C_D / \partial M$$

$$C_{D\delta} = \partial C_D / \partial \delta$$

**Longitudinal, Conversion from Lift/Drag
to Normal/Chord Force**

$$C_N = C_L \cos \alpha_0 + C_D \sin \alpha_0$$

$$C_X = C_D \cos \alpha_0 - C_L \sin \alpha_0$$

$$C_{N\alpha} = C_{L\alpha} \cos \alpha_0 - C_L \sin \alpha_0 + C_{D\alpha} \sin \alpha_0 + C_D \cos \alpha_0$$

$$C_{N\dot{\alpha}} = C_{L\dot{\alpha}} \cos \alpha_0$$

$$C_{NM} = C_{LM} \cos \alpha_0 + C_{DM} \sin \alpha_0$$

$$C_{N\delta} = C_{L\delta} \cos \alpha_0 + C_{D\delta} \sin \alpha_0$$

$$C_{X\alpha} = C_{D\alpha} \cos \alpha_0 - C_D \sin \alpha_0 - C_{L\alpha} \sin \alpha_0 - C_L \cos \alpha_0$$

$$C_{XM} = C_{DM} \cos \alpha_0 - C_{LM} \sin \alpha_0$$

$$C_{X\delta} = C_{D\delta} \cos \alpha_0 - C_{L\delta} \sin \alpha_0$$

Lateral

$$\begin{aligned}
 C_y &= Y/\bar{q}S & C_l &= L/\bar{q}Sb & C_N &= N/\bar{q}Sb \\
 C_{y\beta} &= \partial C_y/\partial\beta & C_{l\beta} &= \partial C_l/\partial\beta & C_{n\beta} &= \partial C_n/\partial\beta \\
 C_{y\delta} &= \partial C_y/\partial\delta & C_{l_p} &= (2V_{T_0}/b)(\partial C_l/\partial p) & C_{n_p} &= (2V_{T_0}/b)(\partial C_n/\partial p) \\
 & & C_{l_r} &= (2V_{T_0}/b)(\partial C_l/\partial r) & C_{n_r} &= (2V_{T_0}/b)(\partial C_n/\partial r) \\
 & & C_{l\delta} &= \partial C_l/\partial\delta & C_{n\delta} &= \partial C_n/\partial\delta
 \end{aligned}$$

DIMENSIONAL STABILITY DERIVATIVE DEFINITIONS

Longitudinal

$$\begin{aligned}
 X_u^* &= X_u + T_u \cos \xi_0 & & 1/\text{sec} \\
 X_u &= \frac{\rho S U_0}{m} - \frac{M}{2} C_{X_M} - C_X + \frac{W_0}{2U_0} C_{X_\alpha} & & 1/\text{sec} \\
 X_w &= \frac{\rho S U_0}{2m} - C_{X_\alpha} - 2 \frac{W_0}{U_0} C_X + \frac{M}{2} C_{X_M} & & 1/\text{sec} \\
 X_\delta &= - \frac{\rho S V_{T_0}^2}{2m} C_{X_\delta} & & \text{ft/sec}^2/\text{rad} \\
 \\
 Z_u^* &= Z_u - T_u \sin \xi_0 & & 1/\text{sec} \\
 Z_u &= \frac{\rho S U_0}{m} - \frac{M}{2} C_{N_M} - C_N + \frac{W_0}{2U_0} C_{N_\alpha} & & 1/\text{sec} \\
 Z_w &= \frac{\rho S U_0}{2m} - C_{N_\alpha} - 2 \frac{W_0}{U_0} C_N + \frac{M}{2} C_{N_M} & & 1/\text{sec} \\
 Z_{\dot{w}} &= - \frac{\rho S c}{4m} \frac{U_0}{V_{T_0}} C_{N\dot{\alpha}}
 \end{aligned}$$

$$Z_{\delta} = -\frac{\rho S V_{T_0}^2}{2m} C_{N_{\delta}} \quad \text{ft/sec}^2/\text{rad}$$

$$M_u^* = M_u + \frac{l_{jm}}{I_y} T_u \quad 1/(\text{sec-ft})$$

$$M_u = \frac{\rho S c U_0}{2I_y} \frac{M}{2} C_{mM} + C_m - \frac{W_0}{2U_0} C_{m\alpha} \quad 1/(\text{sec-ft})$$

$$M_w = \frac{\rho S c U_0}{2I_y} C_{m\alpha} + \frac{2W_0}{U_0} C_m + \frac{M}{2} C_{mM} \quad 1/\text{ft}$$

$$M_{\dot{w}} = \frac{\rho S c^2}{4I_y} \frac{U_0}{V_{T_0}} C_{m\dot{\alpha}} \quad 1/\text{ft}$$

$$M_{\alpha} = U_0 M_w \quad 1/\text{sec}^2$$

$$M_{\dot{\alpha}} = U_0 M_{\dot{w}} \quad 1/\text{sec}$$

$$M_q = \frac{\rho S c^2 V_{T_0}}{4I_y} C_{mq} \quad 1/\text{sec}$$

$$M_{\delta} = \frac{\rho S c V_{T_0}^2}{2I_y} C_{m_{\delta}} \quad 1/\text{sec}^2/\text{rad}$$

$$T_u = \frac{1}{am} (\partial T / \partial M) \quad 1/\text{sec}$$

$$C_m = \frac{-T_0 l_j}{\bar{q} S c}$$

$$T_0 = \frac{\bar{q} S C_x + W \sin \theta_0}{\cos \xi_0}$$

Lateral

$$\begin{aligned}
 Y_v &= \frac{\rho S V T_o}{2m} C_{y\beta} && 1/\text{sec} \\
 Y_\beta &= V_{T_o} Y_v && \text{ft}/\text{sec}^2 \\
 Y_\delta &= \frac{\rho S V T_o^2}{2m} C_{y\delta} && \text{ft}/\text{sec}^2/\text{rad} \\
 Y_\delta^* &= \frac{\rho S V T_o}{2m} C_{y\delta} = \frac{Y_\delta}{V_{T_o}} && 1/\text{sec}/\text{rad} \\
 L_\beta &= \frac{(\rho S V T_o^2)}{2I_x} C_{l\beta} && 1/\text{sec}^2 \\
 L_p &= \frac{(\rho S V T_o b^2)}{4I_x} C_{lp} && 1/\text{sec} \\
 L_r &= \frac{(\rho S V T_o b^2)}{4I_x} C_{lr} && 1/\text{sec} \\
 L_\delta &= \frac{(\rho S V T_o^2 b)}{2I_x} C_{l\delta} && 1/\text{sec}^2/\text{rad} \\
 N_\beta &= \frac{(\rho S V T_o^2 b)}{2I_z} C_{n\beta} && 1/\text{sec}^2 \\
 N_p &= \frac{(\rho S V T_o b^2)}{4I_z} C_{np} && 1/\text{sec} \\
 N_r &= \frac{(\rho S V T_o b^2)}{4I_z} C_{nr} && 1/\text{sec}
 \end{aligned}$$

$$N_{\delta} = \frac{(\rho S V_{T_0}^2 b)}{2I_z} C_{n\delta} \quad 1/\text{sec}^2/\text{rad}$$

$$L_{\beta} = \frac{(L_{\beta} + I_{xz}N_{\beta})}{I_x} G \quad 1/\text{sec}^2$$

$$L_{p} = \frac{(L_p + I_{xz}N_p)}{I_x} G \quad 1/\text{sec}$$

$$L_{r} = \frac{(L_r + I_{xz}N_r)}{I_x} G \quad 1/\text{sec}$$

$$L_{\delta} = \frac{(L_{\delta} + I_{xz}N_{\delta})}{I_x} G \quad 1/\text{sec}^2/\text{rad}$$

$$N_{\beta} = \frac{(N_{\beta} + I_{xz}L_{\beta})}{I_z} G \quad 1/\text{sec}^2$$

$$N_{p} = \frac{(N_p + I_{xz}L_p)}{I_z} G \quad 1/\text{sec}$$

$$N_{r} = \frac{(N_r + I_{xz}L_r)}{I_z} G \quad 1/\text{sec}$$

$$N_{\delta} = \frac{(N_{\delta} + I_{xz}L_{\delta})}{I_z} G \quad 1/\text{sec}^2/\text{rad}$$

$$G = \frac{1}{1 - (I_{xz}^2/I_x I_z)}$$

SECTION I
INTRODUCTION

The object of the program reported herein was to develop a tentative flying quality specification for aircraft with direct force controls which allow independent control over the six inertial degrees of freedom. Such aircraft are frequently referred to as control-configured vehicles (CCV); see, for example, Refs. 1 and 2. However, this terminology is also used to describe configurations without direct lift and side force controls but which depend on automatic control systems for stability, load alleviation, and flutter prevention. Accordingly, there is some confusion with the use of "CCV" to represent airplanes which in particular are also direct force controlled (DFC) vehicles. The latter designation will be used in this report to distinguish these from the general class of CCVs.

The primary problems with developing flying qualities criteria for DFC aircraft are: 1) the unconventional responses of such aircraft exceed the scope of MIL-F-8785B (Ref. 3) in that there are no provisions for highly augmented and unconventional aircraft motions; and 2) the existing data base is very incomplete. Typical problems with the existing data base are that many tests have been run without specific pilot commentary or pilot ratings; in many cases the tasks were not well defined and/or were not tailored to separate good from bad handling qualities; and finally, the controlled element plus manipulator characteristics were not well defined. A review of the data base indicates that in most cases one or more of the above deficiencies make it impossible to perform quantitative pilot rating and commentary correlations upon which even tentative flying qualities criteria could be developed.

We should note at the outset that we intended no more than a cursory evaluation of various DFC modes such as pitch pointing, wings-level turn, maneuver enhancement, etc., in terms of their usefulness in any

given task. Rather, the objective here is to define what is and what is not acceptable once it has been decided to use a given DFC mode.

The addition of control surfaces which, when deflected, exert aerodynamic forces along the aircraft y and z axes allows an almost infinite number of combinations of coupling between the aircraft degrees of freedom. The coupling can be favorable or unfavorable. For example, maneuver enhancement modes such as direct lift control (DLC) are examples of favorable coupling to augment the aircraft heave damping. Unfavorable coupling can occur when attempting to produce a purified response, such as lateral translation or wings-level turn, with inappropriate feedback or crossfeed gains or equalization. Such inappropriate feedbacks or crossfeeds can and do occur due to problems with gain scheduling throughout the flight envelope. This will be discussed later in the report. Clearly, it would be impossible to specify all possible modes of coupling for all DFC combinations that could be generated utilizing direct force control. Such a dilemma forced us to focus on requirements which were based on more fundamental aspects of DFC pilot/vehicle dynamics.

The "bandwidth hypothesis" was a result of such consideration. It is based on fundamental principles of closed-loop pilot/vehicle analysis and is measurable from open-loop response characteristics. In Section II we provide the basic background leading to the formulation of the bandwidth hypothesis. Section III is devoted to a comprehensive discussion of the bandwidth hypothesis, including physical applications as well as supporting arguments. A limited flight test program was accomplished to verify the bandwidth hypothesis. A description of this program is given in Section IV. The results of the flight test program (Section V) did indeed validate the hypothesis, leading to the tentative flying quality specification material in Section VI.

Appendix A, an analysis of the YF-16 design, includes an assessment of design implications for flight control mechanization, pilot opinion, and task performance.

SECTION II

BACKGROUND CONSIDERATIONS

A. LITERATURE REVIEW

A number of studies in recent years have addressed the potential operational advantages of aircraft having direct force capabilities (Refs. 4, 5). Many of the earliest studies were of direct lift control (DLC) sponsored by the Navy to enhance path control performance during carrier approach (Refs. 6-8). DLC has found operational application in the landing of large commercial transports as well (e.g., the L-1011), again as solutions to path control problems in the terminal area (see Refs. 9-11). There are, of course, many additional applications of DLC-like surfaces including gust relief, flutter suppression, spanwise load relief, operating point scheduling, etc. These are not addressed in this report because their operation does not require the pilot's continuous active participation in the sense of a feedback loop.

More recently a number of studies have considered direct side force control (DSFC), Refs. 12-14. The typical flying task studied is ground attack where the DSFC capability offers significant advantages over conventionally responding aircraft when using a depressed-reticle, fixed sight. Another application of DSFC is landing approach where it offers a potential resolution of the issue of wing-low versus crabbed approach technique in crosswinds (Refs. 15, 16).

Results from some of the most useful DFC studies are summarized in Table 1. One of the most important is the YF-16 Fighter CCV program, Refs. 17 and 18. This program involved modification of the prototype YF-16 aircraft to produce a Control Configured Vehicle (CCV) and a flight test program involving contractor and Air Force pilots. The basic airframe modification required for direct force control capability was the addition of ventral canards (for side force) and the use of the existing wing flaps as direct lift surfaces. Also, the control system

TABLE 1. SUMMARY OF USEFUL LITERATURE

REFERENCE	CONCEPT/MODE	MANIPULATORS (SEPARATED MODES ONLY)	TASKS	DATA	RESULTS/REMARKS
Stickler, Patton and Henry (Ref. 10)	Separated direct lift control using drooped ailerons with an elevator interconnect to cancel pitching moment (P-8C).	Thumbwheel switch on stick spring-loaded to center. Surface deflec- tion proportional to thumbwheel deflection.	Field approach and landing.	Step responses to DLC actuation. Touchdown error histograms. Narra- tive summary of pilot comments.	Non-ideal basic aircraft charac- teristics, resulting in high work- load in the baseline configura- tion. Authority limited ($\pm 0.1 g$). But DLC with favorable pitching moment improved opinion and eased workload.
Billue and Senn (Ref. 5)	As above (same air- plane: F-8C)	As above.	Carrier and field approach.	Similar in nature to above, but more limited.	Interconnect gain chosen resulted in mild adverse pitch down with up DLC). But pilots enthusiastic about improved path control in final stages of carrier approach.
Brulle, Moran and Marsh (Ref. 11)	Direct side force (flat turn) and lateral translation separated modes, fixed, roll compen- sated, and impact prediction sights. This was a fixed- base simulation.	Rudder pedals (thumb button for lateral translation during ini- tial phase). Both pro- portional and integral responses in lateral translation modes tried.	Dive bombing.	Step responses, pilot opinion, system performance, narrative summary of commentary. Data reasonably complete.	Large matrix of conditions evalu- ated to arrive at criteria for direct side force systems design. Results appear to show that ad- vanced sights with conventional aircraft give better performance than DFCS aircraft with fixed sight. Note: a fixed based simulation.
Wood, Garland and Maschko (Ref. 16); McAllister, et al. (Ref. 17)	Direct lift in one integrated (maneu- ver enhancement) and three separated modes; direct side force in three separated modes (YF-16 Fighter CCV).	Separated modes con- trolled by trim button on isometric sidestick; in lateral axes rudder pedals could be used as an alternate to the trim button.	Air-to-air tracking. Air-to-ground (dive bombing). Air-to-ground (strafe).	Step responses, system definition, performance, narra- tive summary of pilot commentary. Data reasonably complete with the addition of YF-16 aerodynamics.	Large matrix of systems evaluated; however, systems not optimized for tasks. The direct lift and direct side force (flat turn) separated modes and the maneuver enhancement integrated mode appear most use- ful. Baseline configuration has "pitch bobble" at elevated load factors.

(continued on following page)

TABLE 1. (Concluded)

REFERENCE	CONCEPT/MODE	MANIPULATORS (SEPARATED MODES ONLY)	TASKS	DATA	RESULTS/REMARKS
Moofj (Ref. 20)	Integrated and separated direct life control with elevator interconnect (Navion).	Thumbwheel control of direct lift surface in proportional or bang-bang mode.	Simulated carrier approach.	Pilot opinion rating vs. configuration. Configuration identified by key handling qualities parameters.	Principal value is as a handling qualities investigation of integrated DLC allowing independent control of Z_0 and M_0 for a variety of w_{ep} and $1/T_{\theta 2}$ values.
Binnie and Stengel (Ref. 15)	Separated direct side force control; integrated side force control with pedals, stick, or both.	Separated DSFC control with thumbwheel lever (panel rate) or trim switch (panel position).	Approach and landing; take-off in light turbulence.	Stability derivatives and pilot ratings but incomplete configuration definition.	Utility of DSFC in crosswind approaches was primary thrust of effort; but results are completely documented.
Hall and Weingarten (Ref. 21)	Separated direct side force (flat turn) using wing tip drag devices and rudder (T-33).	Rudder pedals.	Air-to-ground (dive bombing and strafing).	Pilot opinion and performance vs. configuration and basic handling qualities parameters.	Simulation of A-9A and A-10 aircraft with a variety of dutch roll (and other) lateral parameters. Low authority and high drag.
Hall (Ref. 22)	As above.	Thumb controller, lateral stick and rudder pedals. No spring return on thumb controller.	Dive bombing.	Narrative summary of pilot commentary plus limited time response plots to step inputs.	Poor implementation and low authority. But pilots felt that an enormous improvement in dive bombing accuracy was obtainable. Pitch coupling intolerable.
Boothe and Ledder (Ref. 23)	Separated direct side force surfaces on wings (TRFS airplane), roll and yaw decoupling.	Center detent thumbwheel on left side of yoke or on power lever; automatic (ILS) control.	STOL crosswind approach to touchdown.	Performance, pilot opinion and pilot commentary.	Problems with implementation, but pilots liked capability of making wings level, zero heading error approach in a crosswind.
Miller, Deal and Champine (Ref. 23)	Decoupled control of pitch, path angle and speed; heading rate and sideslip (XC-142).	Integrated concept. Column controls γ , flap handle controls θ . Throttle controls speed.	STOL approaches with two segment glide slope. Fixed-base simulation.	Narrative summary of results and pilot commentary. Some time response data.	Pilots preferred decoupled longitudinal controls but saw no advantage to lateral decoupling.

was modified to coordinate the control surfaces in the various DFC modes.

Four longitudinal and three lateral-directional DFC modes were implemented. Six of these are "decoupled" modes which may be defined simply in terms of motion constraints as indicated in Ref. 17. Since each longitudinal mode has a lateral-directional counterpart, the DFC modes may be classified collectively as "direct force," "pointing," and "translation" modes. One additional longitudinal DFC mode with no lateral-directional counterpart, the maneuver enhancement mode, was also mechanized. This mode differs somewhat from the decoupled modes in that the controller (longitudinal stick) and response appear basically conventional to the pilot, but the direct lift capability is used to "quicken" the load factor response.

The YF-16 CCV was evaluated by a number of contractor and Air Force pilots in a program involving 87 test flights. With regard to the present study, the most relevant tests were the Handling Qualities During Tracking (HQDT) evaluations for both air-to-air and air-to-ground tasks. The HQDT concept (Ref. 17) was developed as a standardized, quantifiable flying qualities evaluation task related to actual combat aircraft operation. The air-to-air tasks required the YF-16 to track a target aircraft performing either a 3 g, or a slow wind-up, turn. For the runs involving DFC modes, pilots were allowed to use both the conventional controls (for example, lateral stick) and the DFC controllers (such as the CCV button controller), but were instructed to emphasize the latter. During each run, tracking error data were obtained from gun camera film for later statistical analysis. Some Cooper-Harper pilot ratings (primarily from one pilot) were also obtained.

Because of problems noted in Ref. 18, usable tracking performance data could not be generated for the air-to-ground HQDT evaluations. For the air-to-air HQDT evaluations, formal statistical analysis of the tracking scores indicated no performance advantage for the DFC modes over the conventional YF-16 except for a 16 percent reduction with the maneuver enhancement mode. As such the use of tracking data performance statistics for flying qualities evaluation presents a problem in

that such data generally do not adequately define pilot workload. Use of the Cooper-Harper pilot rating is the most common technique to overcome this problem. However, analysis of the pilot rating data available for the YF-16 CCV proved inconclusive with regard to comparisons between DFC modes and the conventional augmented airframe. In addition to the limited amount of data (primarily from only one pilot), the protocols and procedures for the HQDT tests complicated comparison of the DFC modes with conventional control. Specifically, when evaluating DFC modes, pilots were allowed to use both DFC and conventional controls with instructions to emphasize use of the DFC control. However, examination of the flight traces indicates that (especially for the lateral-directional CCV modes) little use was made of the CCV controls.

An additional issue is that the basic air-to-air HQDT tracking task involves target bandwidths which are quite low relative to high performance tracking. The value of a relatively high-bandwidth tracking task for flying qualities evaluation is that it forces the pilot into using high-gain loop closures which emphasize deficiencies in the aircraft's dynamics. However, the purpose of the YF-16 CCV flight test was somewhat different from this program, which accounts for differences in the experimental procedures. In particular, much of the YF-16 CCV effort was devoted to refining the CCV control system. Because the configuration was considered a prototype, emphasis was placed on evaluating the potential usefulness of each DFC mode.

Because of the relevance of the configuration and the extensive documentation, the YF-16 Fighter CCV was used as a basic case study for much of the analysis in this program. This analytical work is reviewed in the following articles of this section.

B. INDEPENDENT CONTROL AND DECOUPLING

Any discussion of independent control implicitly assumes a particular set of independent motion coordinates (a vector, \underline{x} , of "response variables") equal in number to the airframe degrees of freedom. For instance,

$$\underline{x} = (\alpha, \theta, u, \beta, r, \phi)^T \quad (1)$$

This choice is not unique; for example, flight path angle, γ , could be substituted for pitch attitude, θ . The concept of independent control implies that \underline{x} may be specified arbitrarily as a function of time.

Decoupled response is a special case of independent control in which all but one response variable, the "commanded variable," are identically zero.* For example, the Vertical Translation (α_2) mode in the YF-16 CCV may be defined by the response vector:†

$$\underline{x} = (u, \theta, w)$$

and the constraints

$$u \equiv 0, \quad \theta \equiv 0$$

The terms "decoupling" and "coupling" are used with various shades of meaning that sometimes lead to misinterpretation. Lebacqz and Chen, Ref. 25, noting this ambiguity, define four categories of decoupling:

- 1) Input (control) decoupling: control interconnects so that one controller gives input force or moment to only one axis.
- 2) Static decoupling: control interconnects so that one controller produces a steady response in only one variable.
- 3) Mode decoupling: control interconnects and possibly state feedbacks so that pole-zero cancellations occur (e.g., no dutch roll mode in roll rate response).

*This definition implies that the response variables are perturbation quantities.

†The YF-16 CCV has been analyzed in terms of separated three-degree-of-freedom longitudinal and lateral-directional models.

- 4) Dynamic decoupling: control interconnects and state feedbacks so that each input gives response in only one variable; others are zero both in transient and in steady state.

The first category refers to decoupling of the control forces and moments, $\underline{F} = B\underline{\delta}$, which is neither necessary nor sufficient for decoupling the response, \underline{x} . Furthermore, control surfaces decoupled in one axis system ("FRL" body axes, say) will not, in general, be decoupled in some other system (stability axes, for instance). However, assumption of decoupled (or "purified") controls often simplifies analysis and might, in some cases, simplify practical mechanization. In theory, control surface forces and moments can always be decoupled with respect to the cockpit controls by adding "interconnects" or "crossfeeds" between appropriate control surfaces. All possibilities may be represented as a "mixing box" (interconnect matrix) between the cockpit controls, $\underline{\delta}_p$, and the control surface deflections, $\underline{\delta}$, Fig. 1.

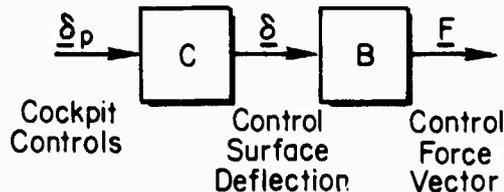


Figure 1. Generalized Control Interconnect

The last three classifications refer to response decoupling over various frequency regions. In static decoupling, only the steady-state response is decoupled, whereas in dynamic decoupling the response is decoupled even during the transient. In specifying the flying qualities of decoupled DFC response modes in precision tracking tasks, the dynamic response of the mode becomes the critical issue. This is particularly important because exact response decoupling will not be achieved in real DFC aircraft.

Returning to the vertical translation mode example, it should be noted that the heave, w , response has not been specified a priori (although in theory it could be).^{*} Thus, w will be defined by the airframe longitudinal dynamics subject to the two constraints. It is therefore of interest to determine the w response including any residual effects of the basic airframe parameters. For perturbations from symmetrical, straight and level flight about the stability axes, the three longitudinal equations of motion and the constraints are:[‡]

$$\begin{bmatrix} s - X_u^* & g & -X_w \\ -M_u^* & s(s - M_q) & -M_w \\ -Z_u^* & -U_0 s & s - Z_w \end{bmatrix} \begin{bmatrix} u \\ \theta \\ w \end{bmatrix} = \begin{bmatrix} X_{\delta_T} & 0 & 0 \\ 0 & M_{\delta_e} & 0 \\ 0 & 0 & Z_{\delta_L} \end{bmatrix} \begin{bmatrix} \delta_T \\ \delta_e \\ \delta_L \end{bmatrix} \quad (2)$$

$$u = 0$$

$$\theta = 0$$

for five equations in six unknowns (u , θ , w , δ_T , δ_e , δ_L). Thus, five of the unknowns may be solved in terms of the sixth, say δ_L . The solutions for u and θ are immediate from the constraints, and the system of equations may be reduced by straightforward substitution to:

^{*}This situation corresponds to the YF-16 CCV design in which the control system is mechanized to simply constrain all but one degree of freedom to zero in each CCV mode. Some designs, notably the USAF/MDC AFTI configuration in Ref. 11, do attempt to completely specify \underline{x} . However, this situation requires no special treatment for flying qualities specification and will not be considered further.

[‡]The equations are written with decoupled (i.e., "pure") controls for simplicity; however, there is no loss in generality since, in principle, control decoupling may always be accomplished with appropriate interconnects (crossfeeds). The characteristic matrix is written for a bare airframe, but basic augmentation would not change the problem conceptually.

$$\begin{bmatrix} -X_{\delta_T} & 0 & -X_w \\ 0 & -M_{\delta_e} & -M_w \\ 0 & 0 & (s - Z_w) \end{bmatrix} \begin{bmatrix} \delta_T \\ \delta_e \\ w \end{bmatrix} = \begin{bmatrix} 0 \\ 0 \\ Z_{\delta_L} \end{bmatrix} \delta_L \quad (3)$$

Physically, we may say that speed is constrained with thrust, and pitch attitude is constrained with elevator; so that δ_T and δ_e replace u and θ , respectively. Solving for the w/δ_L transfer function by Cramer's rule gives:

$$\frac{w}{\delta_L} = \frac{\begin{vmatrix} -X_{\delta_T} & 0 & 0 \\ 0 & -M_{\delta_e} & 0 \\ 0 & 0 & Z_{\delta_L} \end{vmatrix}}{\begin{vmatrix} -X_{\delta_T} & 0 & -X_w \\ 0 & -M_{\delta_e} & -M_w \\ 0 & 0 & (s - Z_w) \end{vmatrix}} = \frac{Z_{\delta_L}}{(s - Z_w)} \quad (4)$$

It should be noted that the numerator (containing the three columns from the original control matrix) is, by definition, a "coupling-coupling numerator" and that the denominator (with two control columns) is a coupling numerator (see Appendix D). Thus, we may write the vertical translation mode response as:

$$\frac{w}{\delta_L} = \frac{N_{\delta_L \delta_e \delta_T}^{w \theta u}}{N_{\delta_e \delta_T}^{\theta u}} = \frac{Z_{\delta_L}}{(s - Z_w)} \quad (5)$$

This last observation is important because it is general, i.e., any decoupled mode (for a three-degree-of-freedom system) may be written as a ratio of a coupling-coupling numerator and a coupling numerator.* The

*Similarly, if only one zero constraint is applied to a three-degree-of-freedom system, the responses of the unconstrained variables are ratios of coupling numerators and standard numerators.

commanded variable responses for all the YF-16 CCV decoupled modes are summarized in Table 2. These idealized expressions neglect lags and time delays due to sensor, computation and actuator dynamics (see Appendix D for the lateral translation, β_2 , example). The forms of the longitudinal and lateral-directional direct force, pointing, and translation modes are identical and contain analogous dynamic parameters. However, the mode response dynamics increase in complexity in going from direct force to translation to pointing modes. That is, idealized direct force modes are pure gains (infinite bandwidth) with the basic control authority determined by the effective direct force control power. The translation modes have first-order responses with bandwidths determined by translational damping derivatives (Z_w and Y_v). Pointing modes have second-order responses with static (pointing) derivatives (M_α and N_β) setting limits on the bandwidths.

C. PRACTICAL (IMPERFECT) DECOUPLING)

A key aspect of the practical mechanization of a DFC mode centers about the departures of the feedback and crossfeed equalization from the ideal (completely decoupled) values. Such departures whether caused by imperfect sensors, aerodynamic uncertainties, limited gain scheduling, etc., can be very significant from the flying qualities standpoint. Establishing acceptable deviations from ideal response is an important aspect of the flying qualities requirements picture.

No matter how complicated the actual block diagram of the DFC control system, it can always be reduced to a series of feedbacks and crossfeeds, e.g., as for the YF-16 case in Fig. 2 (derived from Refs. 17 and 18). Utilizing the YF-16 transfer functions, the ideal crossfeeds were calculated and compared with the actual crossfeeds used in the aircraft (Ref. 17). [Set $N_{\delta_{CC}}^\psi = Y_{\delta_{CC}}^{\delta_C} (N_{\delta_C}^\psi + Y_{\delta_C}^{\delta_R} N_{\delta_R}^\psi + Y_{\delta_C}^{\delta_A} N_{\delta_A}^\psi)$ to zero and similarly with $N_{\delta_{CC}}^\phi$, where the individual N_s account for FCS feedbacks. Then solve these two equations for $Y_{\delta_C}^{\delta_R}$ and $Y_{\delta_C}^{\delta_A}$.] A comparison of the rudder crossfeeds given in Fig. 3 shows that the shaping is approximately ideal but that the actual crossfeed is approximately a factor of two less than ideal. This is not shown to imply that the YF-16 was

TABLE 2. LIMITING RESPONSES FOR DFC RESPONSE MODES

MODE	CONSTRAINTS	LIMITING FORMS OF RESPONSES
Direct Lift, or Normal Acceleration ($\alpha = 0$) (A_N)	$w \rightarrow \delta_e, u \rightarrow \delta_T$	$\frac{a_z}{\delta_L} \rightarrow \frac{N_{\delta_L}^{azw} u}{N_{\delta_e}^w u} = Z_{\delta_L}$
Pitch Pointing (α_1)	$a_z \rightarrow \delta_L, u \rightarrow \delta_T$	$\frac{\theta}{\delta_e} \rightarrow \frac{N_{\delta_L}^{\theta azu}}{N_{\delta_L}^{az} u} = \frac{M_{\delta_e}}{[s^2 - M_q s - M_\alpha]}$
Vertical Translation (α_2)	$\theta \rightarrow \delta_e, u \rightarrow \delta_T$	$\frac{w}{\delta_L} \rightarrow \frac{N_{\delta_L}^{w \theta u}}{N_{\delta_e}^{\theta u}} = \frac{Z_{\delta_L}}{(s - Z_w)}$
Direct Side Force, or Wings Level Turn (A_y)	$\beta \rightarrow \delta_R, \varphi \rightarrow \delta_A$	$\frac{a_y}{\delta_{SF}} \rightarrow \frac{N_{\delta_{SF}}^{ay \beta \varphi}}{N_{\delta_R}^{\beta \varphi}} = Y_{\delta_{SF}}$
Yaw Pointing (β_1)	$a_y \rightarrow \delta_{SF}, \varphi \rightarrow \delta_A$	$\frac{\psi}{\delta_R} \rightarrow \frac{N_{\delta_R}^{\psi ay \varphi}}{N_{\delta_{SF}}^{ay \varphi}} = \frac{N'_{\delta_R}}{[s^2 - N'_R s + N'_\beta]}$
Lateral Translation (β_2)	$\psi \rightarrow \delta_R, \varphi \rightarrow \delta_A$	$\frac{\beta}{\delta_{SF}} \rightarrow \frac{N_{\delta_{SF}}^{\beta \psi \varphi}}{N_{\delta_R}^{\psi \varphi}} = \frac{Y_{\delta_{SF}}^*}{(s - Y_\psi)}$

*Primes denote effective derivatives that account for cross products of inertia (see Ref. 27, page 257).

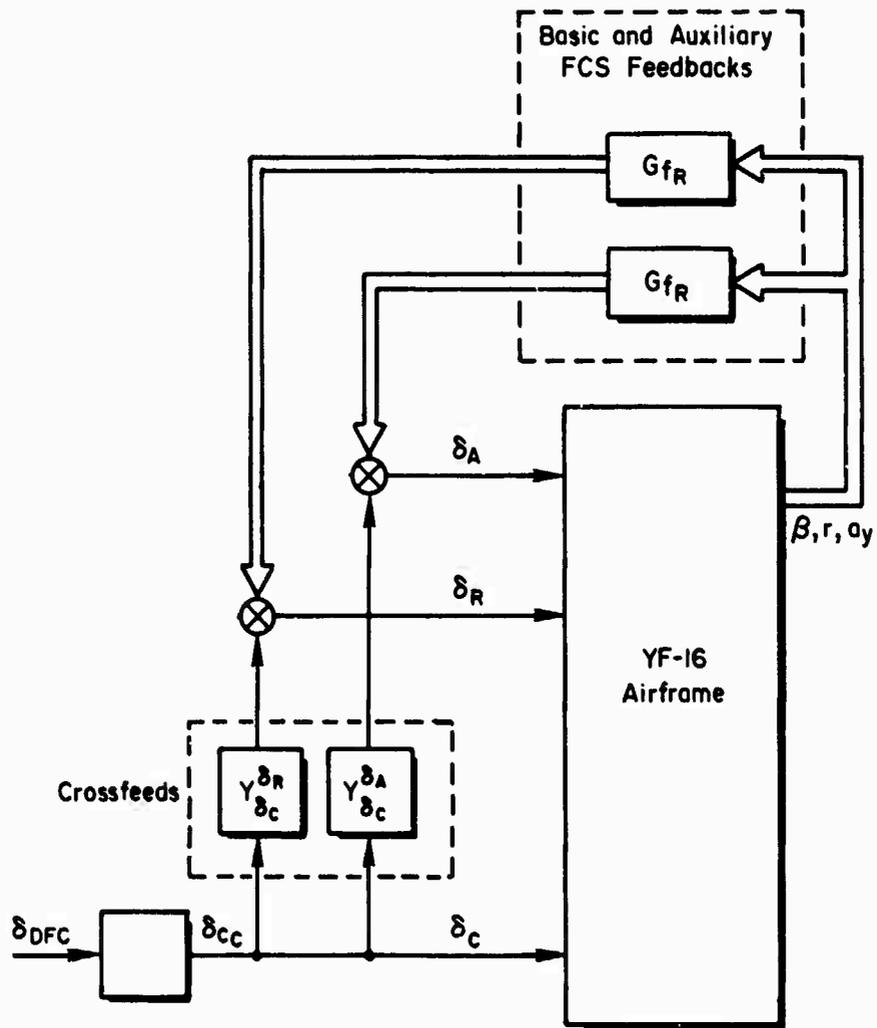


Figure 2. Equivalent Lateral Control System Structure for YF-16 CCV

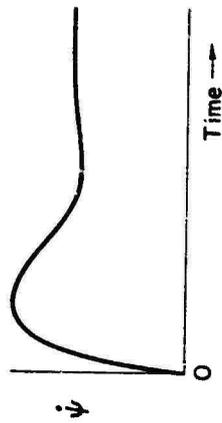
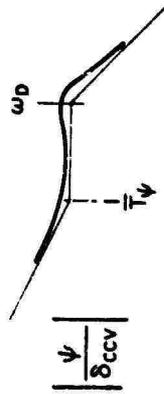
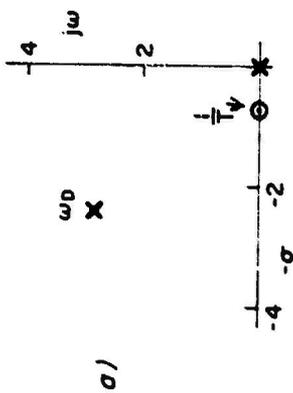
erroneously mechanized, but rather to show what sort of variations from ideal can be expected.

The generic effects of imperfect crossfeeds as they, for example, contaminate the wings-level turn mode by inducing proverse and adverse coupling are shown in Fig. 4.* Inasmuch as crossfeed variations do not affect the characteristic equation, or denominator, of the heading to DFC control transfer function, the primary effect of varying the crossfeed gain is on the numerator. The dominant numerator zero is labeled $1/T_\psi$ in Fig. 4. For a perfect crossfeed, $1/T_\psi$ is equal to infinity, as shown in Fig. 4b. This is considered to be a "decoupled" case, in that the complete dynamics are represented by only the dutch roll mode. Increasing the rudder crossfeed above its nominal value results in $1/T_\psi$ moving in toward the origin. This is seen to result in a "shelf" in the frequency response plot, as well as an overshoot in the time response of yaw rate to a step DFC control input. A rudder crossfeed gain which is less than the ideal value results in $1/T_\psi$ moving from the left half plane around to the right half plane on the real axis and in toward the origin to a typical location as shown in Fig. 4c. A zero in the right half plane is indicative of an "adverse" response which first moves in a direction opposite to the control command, as shown in the time history in Fig. 4c. The closer the zero to the origin, the longer the response in the wrong direction (opposite to the DFC input). The magnitudes of the adverse and proverse yaw coupling frequency responses looks similar (compare Figs. 4a and 4c); however, the phase plot shown in Fig. 4b indicates that proverse yaw coupling results in increasing phase, whereas adverse yaw coupling results in decreasing phase. A combination of a shelf-like magnitude plot and a rapidly decreasing phase results in severe restrictions to system bandwidth as discussed shortly in Section III. Similar characteristics occur for roll coupling except that the $1/T_\psi$ zero is replaced by a second-order pair (ω_ϕ).

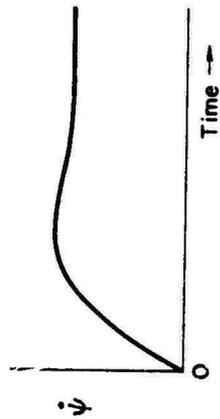
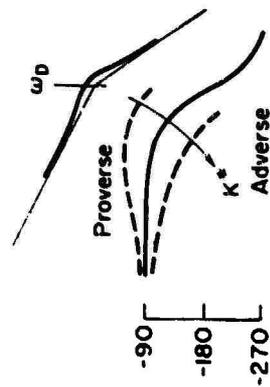
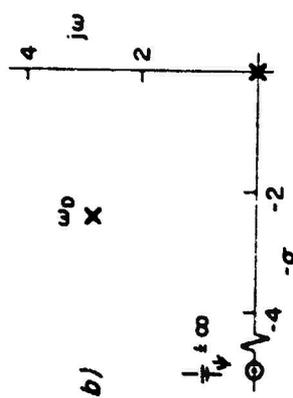
*In the analysis of Fig. 4 we have assumed that the crossfeed is a pure gain for simplicity. In addition, it was assumed that the feedback gains were not infinite (as was assumed in Table 2). Hence, the appearance of the dutch roll mode, ω_ψ , which does not show up in Table 2.

$$\frac{\psi}{\delta_{CCV}} = \frac{N\delta_{CCV}}{\Delta} = \frac{N\delta_C + K\delta_C N\delta_R}{\Delta}$$

PROVERSE YAW COUPLING



DECOUPLED



ADVERSE YAW COUPLING

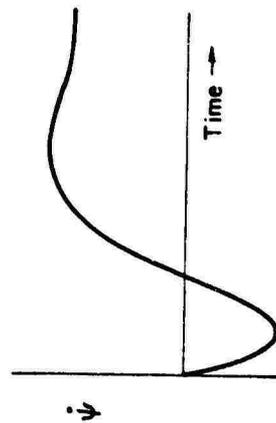
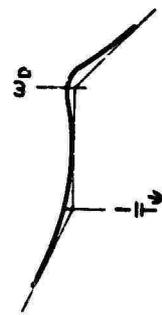
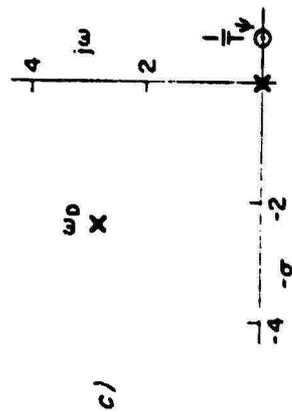


Figure 4. General Rudder Crossfeed Effect on Heading Response — WLT Mode
(Contamination from roll axis assumed to be zero)

While the above example centers about the wings-level turn configuration, the same basic considerations apply for all DFC responses. These are summarized as follows:

- In the absence of coupling, the response will be limited by characteristic modes. These modes may be basic airframe dynamics, or they may be modified as a result of feedbacks.
- All DFC mechanizations may be reduced to cross-feeds and feedbacks as in Fig. 2.
- Coupling due to imperfect crossfeeds is directly traceable to numerator zeros. Adverse coupling will show up as a zero in the right half plane.

D. RESPONSE QUALITIES WITH DIRECT FORCE CONTROLS

This subsection of the report outlines a number of ideas and concepts that relate flying tasks to aircraft response qualities when using direct force controls. While the discussion is not all-inclusive, it serves as a framework for understanding certain characteristics inherent in the several unconventional response modes. It further discusses the suitability of these modes in the flying tasks considered and the effects of "impurities" (coupling) in their responses.

1. Separated Versus Integrated Controls

From the pilot's point of view, the direct force controls either act to modify the responses to the conventional cockpit control manipulators, or are actuated by separate manipulators that are distinct from the normal cockpit controls. In the former case the direct force controls can be categorized as "blended" or "integrated." Actuation of the auxiliary surfaces is accomplished through the stick or pedals, usually in combination with some feedback and interconnect signals. The maneuver enhancement (ME) mode of the YF-16 CCV falls into this category, as do several Navy and commercial transport direct lift control (DLC) mechanizations. The distinguishing feature is the absence of auxiliary cockpit manipulators, aside from the possible switches used to select

the response qualities (mode) most suited to the flying task at hand. To the degree that the responses retain the essence of "conventional" motion responses, current handling qualities criteria apply.

Use of separate manipulators for actuation of the direct force controls is the other major category. These manipulators may be trim buttons or thumbwheels with various force versus displacement properties, located on the yoke, the center or side stick, or the power lever or throttle. In those aircraft normally flown with feet on the floor, the rudder pedals can be used, although conventional usage is then precluded. In the YF-16 CCV, for example, the pedals proved to be a good choice for direct side force control.

The "separated" manipulators can be used as trimming devices or for continuous tracking. In the first case the control is used only intermittently, to establish a new trim condition or operating point. It operates like a trim button; changes are "beeped in." In the second case the auxiliary manipulator is used continuously, as in tracking a target. Presumably the conventional controls are used to establish the operating point.

If the system design is such that both conventional and auxiliary manipulators are used continuously, then it violates a pilot-centered requirement for frequency separation of controls. The pilot cannot easily coordinate more than two control axes continuously, simultaneously, and in the same frequency range of operation. He must timeshare his attention between the multiple controls.

Even when conventional controls are used to establish an operating point, i.e., as "trim" controls, use of the auxiliary manipulators implies additional pilot workload relative to using the conventional controls alone. The increased workload is presumably traded for significant performance advantages obtainable only by using the auxiliary controllers. This implies careful tailoring of the response characteristics when using the separated manipulator to achieve flying task performance that is significantly better than that obtainable with conventional control responses.

2. Maneuver Enhancement Mode

It is appropriate, here, to discuss the maneuver enhancement (ME) mode response properties by way of contrast to the decoupled modes discussed earlier. The ME mode has as its purpose the augmentation of the responses to the conventional manipulators in order to enhance certain response properties due to the "conventional" control surfaces. In the longitudinal degrees of freedom these typically include:

- 1) More rapid path angle or normal acceleration response to the stick, or to attitude changes. This includes augmented $-Z_w$ and zero or favorable " Z_δ effect" on the path responses.
- 2) Greater suppression of normal accelerations caused by atmospheric turbulence.

Although not discussed in this report, one can also consider enhancing the conventional responses in the lateral-directional degrees of freedom as well. These might take the form of:

- 1) More rapid lateral path changes to the lateral stick; perhaps suppression of the sideslip responses in rolling maneuvers.
- 2) Greater resistance to crosswind gusts.

In flight regimes where the rudder pedals are used to roll the aircraft, one might also consider enhancing this mode of control or, alternatively, scheduling the flight control system gains to retain the roll-with-stick response about an appropriate effective roll axis through the operating angle of attack and load factor range.

3. Pippier Error Responses

The dynamics of the aiming error are a combination of both the attitude and path angle responses to the controls. The situation can be diagrammed for the longitudinal axis as shown in Fig. 5. The aiming error expressed in an angle, ϵ , is dependent upon the aircraft's path deviation, d (the rate of change of deviation is proportional to path angle, i.e., $\dot{d} = U_0 \gamma$); and upon the pitch attitude, θ ; thus

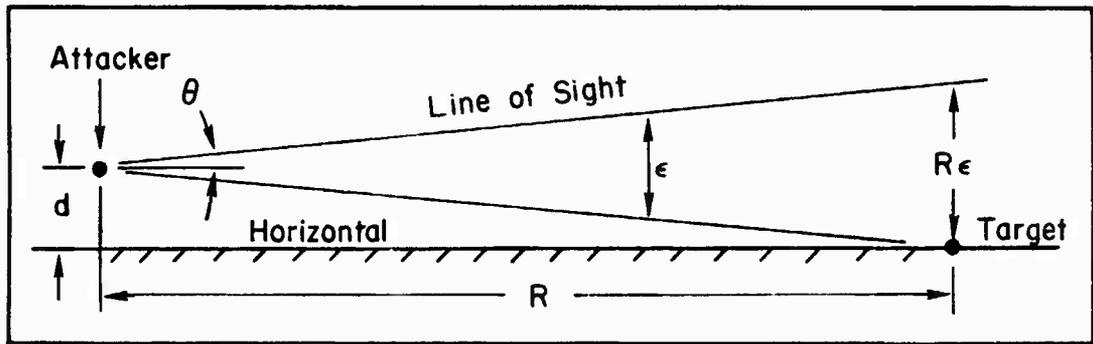


Figure 5. Sketch Showing Pippier Error Geometry

$$R\epsilon = d + R\theta = U_0 \int \gamma dt + R\theta \quad (6)$$

Or,

$$\epsilon = \theta + \frac{U_0}{R} \int \gamma dt \quad (7)$$

This equation immediately shows that aiming error is equivalent to attitude at long range (R very large), but that flight path enters in at high speeds at close range, particularly at low frequencies where the integral term has time to take effect.

For conventionally responding aircraft, the ratio of path angle change to attitude change is characterized by the time constant T_{θ_2} :

$$\frac{\gamma}{\theta}(s) = \frac{1/T_{\theta_2}}{(s + 1/T_{\theta_2})} \quad (8)$$

When this expression is combined with the equation for pippier error the result is:

$$\begin{aligned}
\frac{\epsilon}{F_s}(s) &= \frac{\theta}{F_s}(s) \left[1 + \frac{U_o/R}{s} \frac{1/T_{\theta_2}}{(s + 1/T_{\theta_2})} \right] \\
&= \frac{\theta}{F_s}(s) \left[1 + \frac{U_o}{R} \times \frac{1}{T_{\theta_2}} \times \frac{1}{s(s + 1/T_{\theta_2})} \right] \\
&= \frac{\theta}{F_s}(s) \frac{[s^2 + 2\zeta_o\omega_o s + \omega_o^2]}{s(s + 1/T_{\theta_2})} \tag{9}
\end{aligned}$$

where

$$\omega_o^2 = U_o/RT_{\theta_2} \tag{10}$$

$$2\zeta_o\omega_o = 1/T_{\theta_2} \tag{11}$$

The conventional attitude response of a highly augmented aircraft typically will have a "rate command" characteristic at low frequency (e.g., Fig. 44) and can be represented as:

$$\frac{\theta}{F_s}(s) = \frac{K_{\theta}e^{-\tau s}}{s(T_f s + 1)} \tag{12}$$

where the time delay accounts for all high-frequency lags in the response, and the time constant, T_f , accounts for the short-period response lag. Because the short-term θ , response is integral-like at low frequencies, the aim error response has a K/s^2 -like character at frequencies below ω_o or $1/T_{\theta_2}$. see Fig. 6. This low-frequency acceleration type response character allows the pilot to "stay with" an accelerating target (as in a circling tail chase) by means of a constant stick deflection — of crucial importance in air-to-air tracking. In nulling the aim error the pilot is not only achieving a tracking "solution" for gunfire, he is also controlling the aircraft path — all with a single-loop closure, $\epsilon + F_s$.

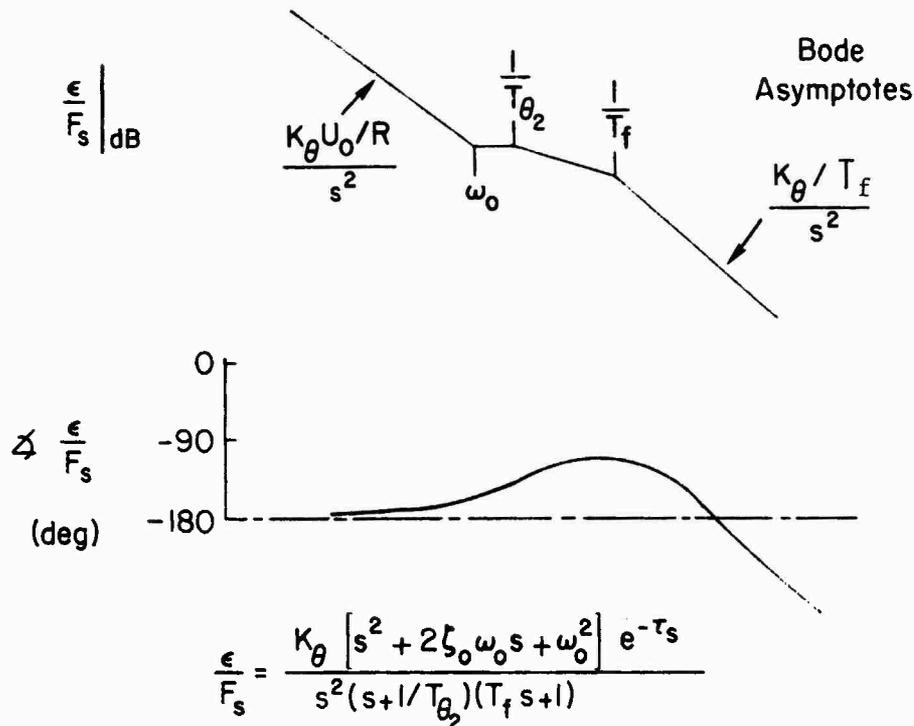


Figure 6. Sketch Showing Longitudinal Aim Error Response for Conventional Aircraft

The bandwidth or crossover frequency he can achieve with this effective controlled element determines his ability to regulate the aim error; that is, his ability to minimize the variance in this error, given the nature of atmospheric disturbances, the enemy's air wake, buffet, etc. The amount the crossover frequency can be increased depends upon his ability to generate lead equalization, and the rapidity with which the phase lag increases with increasing frequency is in turn dependent upon T_f and τ in Eq. 12.

The remaining parameters explicit or implicit in Fig. 6 also affect the obtainable pilot/vehicle closed-loop performance. Consider range; as range shortens, the damping, ζ_0 , decreases and the frequency, ω_0 , increases (e.g., Fig. 46, Appendix A). The system becomes conditionally stable, confining the pilot to a progressively smaller span of allowable equalization characteristics as the range continues to decrease.

Airspeed also influences the aim error response. The attitude numerator zero which characterizes the path angle response lag to attitude changes is given by:

$$\frac{1}{T_{\theta_2}} = -z_w = \frac{\rho S U_0}{2m} (C_{L\alpha} + C_D) \quad (13)$$

Thus, $1/T_{\theta_2}$ is seen to be proportional to speed. At a fixed range, substituting Eq. 13 into Eq. 10 shows ω_0 to be proportional to speed; Eq. 11 shows ζ_0 to be invariant with speed at a fixed range. Lower speeds imply a reduced frequency for the aim error numerator zeros. In addition, the low-frequency (below ω_0 and $1/T_{\theta_2}$) asymptote has a gain which is proportional to U_0/R (see Fig. 6). The low-frequency aim-error acceleration varies directly with speed and inversely with range.

Taken together, these several effects of airspeed and range on the low-frequency character of the aim error response may be a partial explanation for difficulties encountered in close-range tasks by relatively inexperienced pilots. Typical of these tasks are formation flight and air-to-air refueling. The overall form of the aim error response for conventionally responding aircraft can serve as a basis of comparison for the unconventional flight mode responses in those tasks involving nulling of the aim error — air-to-air and air-to-ground tracking. In the following, only the longitudinal degrees of freedom with airspeed constrained are discussed. As pointed out earlier, the analysis carries over to the directional degrees of freedom in the unconventional response modes.

a. Direct Force Mode (A_N)

In the absence of significant angle-of-attack and speed responses, $\gamma = \theta$ and the direct lift mode can be characterized by:

$$\frac{Y}{F_b} = \frac{K_{AN}}{s(s + 1/T_f)} = \frac{\theta}{F_b} \quad (14)$$

where $1/T_f$ accounts for all filtering and actuation lags in implementing the control mode in an actual aircraft. Substituting this expression for both θ and γ into Eq. 7 results in:

$$\frac{\epsilon}{F_b} = \frac{K_{AN}(s + U_0/R)}{s^2(s + 1/T_f)} \quad (15)$$

which is directly analogous to that for the conventional aircraft given in Fig. 6. For all but the closest-range tasks, $U_0/R < 1/T_f$, which leads to a relatively broad region of allowable pilot equalization possibilities. In general, the response can have less lag at high frequencies than the conventional response; to start the normal acceleration it is quicker to move a control surface than to rotate the entire aircraft.

The analysis of the YF-16 responses given in Appendix A bears this out; the A_N mode aim-error response is faster than the conventional flight mode aim error (has less high-frequency lag), thereby allowing the pilot to operate at a higher crossover frequency, at least in principle.

b. Maneuver Enhancement

Analysis of the YF-16 CCV response characteristics suggests an attempt on the part of the designers to minimize angle-of-attack excursion. To the extent that this is true, the A_N and ME modes have similar response properties. In fact, the Appendix A analysis shows the ME mode aim error response to be intermediate in its phase characteristics — between the conventional and A_N mode responses.

c. Pointing Modes (α_1, β_1)

When the idealized response of Table 2 is realized, but with an additional lag to account for filtering and the actuator, the result is as sketched in Fig. 7. The response characteristic is that of a third-order low-pass filter with a bandwidth governed by $-M_\alpha$ and T_f . There is

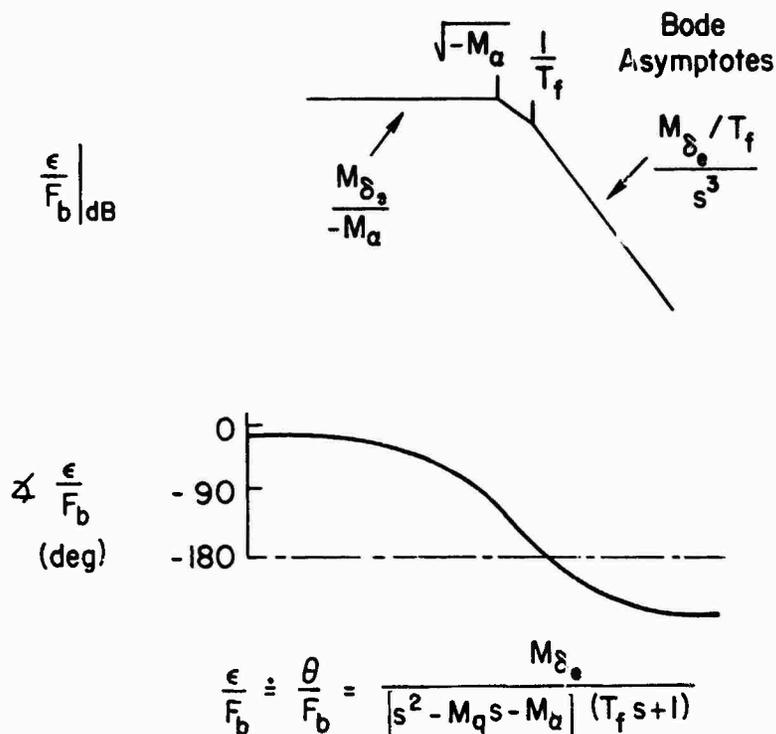


Figure 7. Sketch Showing Aim Error Response in Pitch Pointing Mode

no low-frequency integration characteristic; the pilot can track a fixed target, but not one which can accelerate away from him — at least not for long, because he will run out of pointing authority and the flight path does not follow the attitude change as with the conventional, A_N , or ME modes. In attempting to regulate aim error, the pilot must lag-equalize this controlled element to achieve the desired K/s -like character in the open-loop pilot/vehicle response. This contributes even more lag to the response, with the result that the achievable crossover frequency for error regulation in a tracking task tends to be limited by $\sqrt{-M_\alpha}$ or $1/T_f$, whichever is lower. For tracking, this mode has less potential than the conventional response. Because of this, its best use, to judge by pilot commentary on the YF-16 CCV, was to establish a trim attitude change (as in strafing).

Suppose the constraint on path angle, γ , is less than perfect. Under these circumstances, an integral-like characteristic will be introduced in the aim error response at low frequencies. This can be favorable or unfavorable, depending on the sign of the γ response relative to θ . The magnitude is presumably less than that of the conventional aircraft; after all, γ is supposed to be zero! If it is of the wrong sign, the pilot will not be able to use the pointing mode except on an intermittent basis. In sustained tracking, the aim error drifts the wrong way and the closed-loop pilot/vehicle system will exhibit a slow divergence.

d. Translational Mode (α_2)

The ideal aim error in this response consists only of the path change contribution; the attitude contribution is zero. Allowing for an equivalent filtering/actuation lag time constant, T_f , the resulting aim error response is as shown in Fig. 8. It exhibits the K/s-like characteristic at low frequencies desired for normal control.

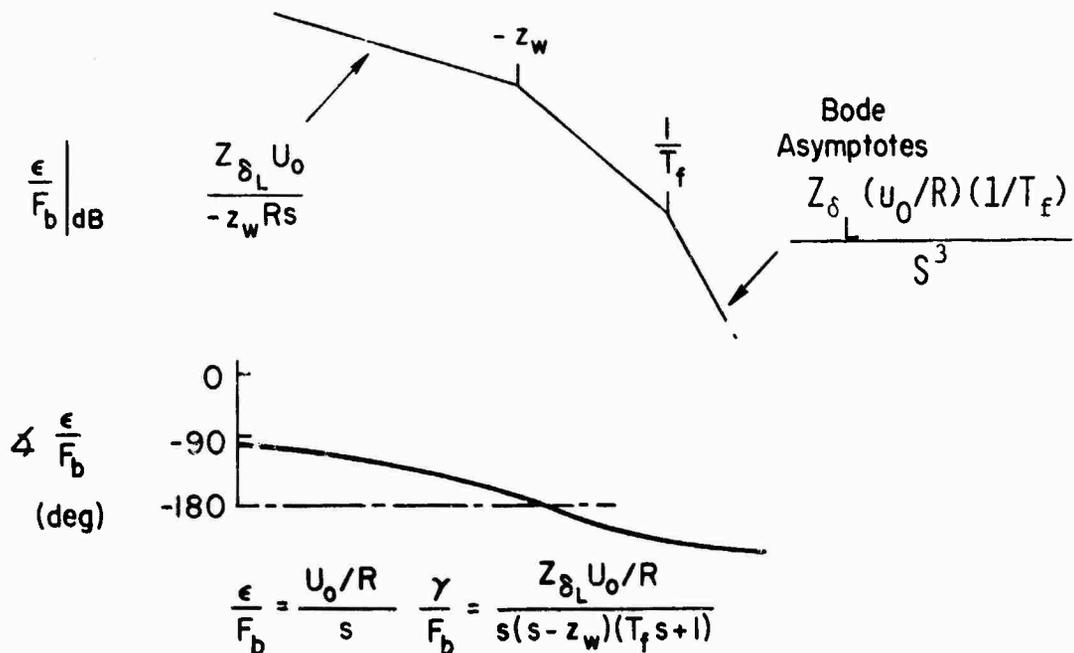


Figure 8. Sketch Showing Ideal Aim Error Response in Vertical Translation Mode

The key features of the response are the low gain when used for aim error tracking at long range and the limited (by $-Z_w$) closed-loop bandwidth potential. Unless $-Z_w$ is augmented to relatively large values, the response will be characterized by pilots as sluggish, and incapable of the rapid response required to tightly regulate errors. These features tended to limit its application to short-range tracking tasks of relatively benign targets — air-to-air refueling and formation flight. Even here the low gain or lateral velocity command authority tend to compromise the aircraft's maneuvering properties. On the other hand, unlike the conventional aircraft, there is no conditional stability characteristic; at a low enough pilot gain the system will be stable.

Residual attitude changes significantly alter the aim error responses at both low and high frequencies, because the attitude contribution to ϵ is direct and not reduced by U_0/R (see Eq. 7). At low frequencies, attitude changes introduce an integral characteristic in the γ response (results in the ϵ/F_b response). The major effect is at high frequencies; the pilot will see the pointing effect in the ϵ response because of its higher effective gain (at moderate to long range) before he sees the intended γ effect on ϵ . Because the pointing change can be of either sign, the result either increases or decreases the phase lag in the γ/F_p response; it can deteriorate or improve the pilot's ability to null the aim error. The pronounced effect of residual attitude change further restricts application of this mode to short-range aiming tasks. It also imposes stiff requirements for mode purity — tight control of the vehicle attitude. Consider, for example, the effect of small amounts of yaw when the lateral translation mode is used to correct for crosswinds in a dive bombing run. The drift across the target only appears to be corrected. In actuality, the velocity vector is still not pointing at the target as is required for bombing accuracy.

e. Summary of Pipper (Aim) Error Responses

Table 3 compares the longitudinal aim error responses for the conventional and the three unconventional flight modes. Analogous expressions hold for the directional degrees of freedom for the unconventional modes. The remarks column in this table indicates how actual responses can be expected to depart from the ideal limiting forms shown. The pointing mode (α_1) will likely have some path change which will affect the low-frequency aim error responses; the translational mode (α_2) will have some attitude change which will dominate at high frequencies, particularly at long range. In both cases, these response contributions can be of the wrong sign.

The implications are clear. The piper error responses in those tracking tasks for which piper error is a key pilot cue will likely be quite sensitive to the exact nature of the responses realized (i.e., mode "purity") in the implementation of the pointing and translational modes. On the other hand for conventional, A_N , and (by interpolation) maneuver enhancement modes, the sensitivity is considerably less; these three modes have both attitude and path angle changes in their aim error responses.

4. Tasks and Loop Structures

The three flying tasks considered in this report are summarized in Table 4 in terms of the outer-loop controlled variables (i.e., those which establish error performance) and the major disturbances. The selection of only these three tasks is not as restrictive as may first appear, see the last column of the table. The key point here is the categorization of task according to the outer-loop controlled variables. Tight control of one of these variables is crucial to performance of many tasks.

Reading down Table 4 the emphasis is first on attitude control, next on path angle control, and finally on path deviation control. In air-to-air gunnery, the direction in which the aircraft is pointed when the gun is fired largely determines where the shells will go. In the dive

TABLE 3. AIM ERROR RESPONSES, LONGITUDINAL DEGREES OF FREEDOM

MODE	AIM ERROR RESPONSE DYNAMICS	REMARKS
Conventional ^a	$\frac{K_{\theta} [s^2 + 2\zeta_0 \omega_0 s + \omega_0^2]}{s^2 (s + 1/T_{\theta 2}) (T_{fs} + 1)} e^{-\tau s}$	$e^{-\tau s}$ accounts for all high-frequency lags in attitude response
α_N Direct Force	$-\frac{Z_{\delta L}}{U_0} \frac{(s + U_0/R)}{s^2 (T_{fs} + 1)}$	$1/(T_{fs} + 1)$ represents the actuation and filtering lags in the mechanization of this and the following responses
α_1 Pointing	$\frac{M_{\delta e}}{[s^2 - M_{qs} - M_{\alpha}] (T_{fs} + 1)}$	Residual path angle change implies low-frequency K/s-like character of either sign relative to the attitude change
α_2 Translation	$-\frac{Z_{\delta L} U_0/R}{s(s - Z_w)(T_{fs} + 1)}$	Residual attitude change will dominate high-frequency character; it can be of either sign relative to the path angle change

^aThe conventional attitude response is here taken to be that of an augmented aircraft, viz.:

$$\frac{\theta}{F_2} = \frac{K_{\theta} e^{-\tau s}}{s(T_{fs} + 1)}$$

TABLE 4. ESSENTIAL DESCRIPTORS FOR KEY FLYING TASKS

TASK	OUTER-LOOP CONTROLLED VARIABLES	MAJOR DISTURBANCES	RELATED TASKS
Air-to-air gunnery	<p>Longitudinal pipper error</p> <p>Roll angle error and/or Lateral pipper error</p>	Target motions and buffeting	Air to ground gunnery is governed by similar outer loop controlled variables; however, target motions are more benign and turbulence becomes a factor.
Dive bombing	<p>Speed (target range)</p> <p><u>Longitudinal path angle</u></p> <p><u>Lateral path angle</u></p> <p>Dive speed</p>	Winds, wind shears and turbulence	Change in emphasis from attitude to path caused by nature of weapon. Sight depression angle is a significant factor in lateral axis task (pendulum effect).
Landing approach	<p><u>Longitudinal path deviation</u></p> <p><u>Lateral path deviation</u></p> <p>Airspeed</p>	Winds, wind shears and turbulence	Formation flight and air-to-air refueling also demand precision path control, but add precision range control as well. These tasks are at significantly different flight conditions.

bombing task, it is the orientation of the velocity vector at bomb release which largely defines the miss distance. When dealing with a conventionally responding aircraft, not only are we interested in attitude, but also in the path angle response lag relative to attitude. If the path can be controlled independently of attitude, we are interested in path response properties directly. In the landing approach task it is the integral of path angle, or the path deviation, which receives emphasis. This is particularly true for instrument flight, where deficiencies in visual information compel path deviation error nulling (e.g., flight director) as a means of assuring the proper flight situation upon visual breakout.

Table 5 presents a matrix of task versus flight mode for a total of twelve combinations. In each block of the table are listed the key pilot loop closures (see Fig. 9) in accomplishing the tracking portions of the task. These are in addition to those loops and crossfeeds which may be closed within the flight control system or perhaps by the pilot to make up stability and response deficiencies. Thus, Table 5 does not show rate damper loops, turn coordination features, etc.

Each loop closure shown implies certain requirements upon the associated response characteristic. The fundamental requirements in each case are that the pilot be able to establish a gain or crossover frequency in each of these loops sufficient to follow commands and to suppress the disturbance encountered.

a. Air-to-Air Gunnery

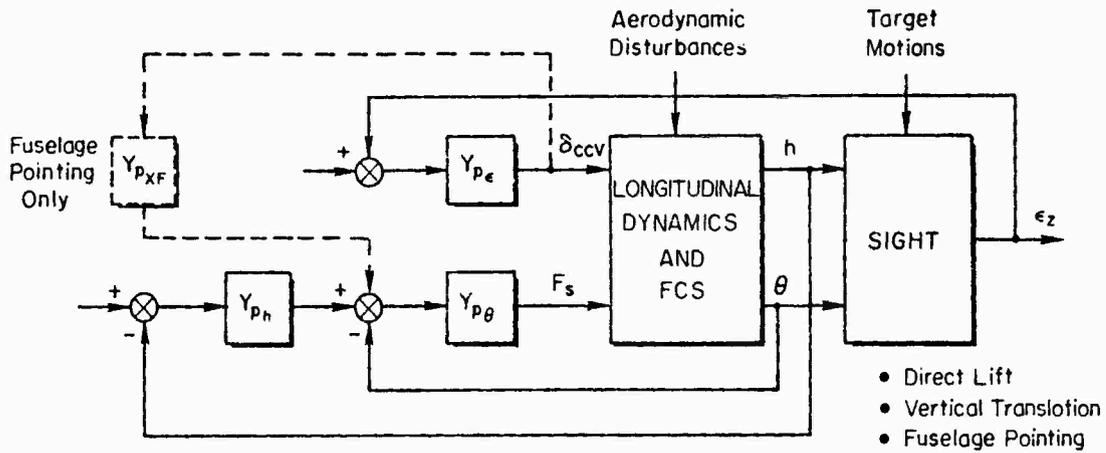
The basic requirements for successful execution of this task are for rapid and precise target acquisition and subsequent tracking. Range to the target is controlled intermittently with the throttle. The task context (i.e., vehicle operating point) is air combat maneuvering, including maximum g turns, maximum rate rolls and roll reversals, maximum performance climbs, accelerations, decelerations, etc., in all combinations, verging upon departure from controlled flight from time to time. Aircraft resistance to attitude disturbances caused by moderate

TABLE 5. PILOT LOOP CLOSURES FOR THREE FLYING TASKS

MODE	AIR-TO-AIR GUNNERY*	DIVE BOMBING†	LANDING APPROACH
Conv. and M.E.	$\epsilon_{long} \rightarrow F_s$ $\dot{R} \rightarrow \delta_{th}$ $\varphi_e \rightarrow F_s$	$\gamma, \theta \rightarrow F_s$ $u \rightarrow \delta_{th}$ $\varphi \rightarrow F_s, \epsilon_{lat} \rightarrow \varphi_c$	$\theta \rightarrow F_s, d \rightarrow \theta_c$ $u_a \rightarrow \delta_{th}$ $\varphi \rightarrow F_s, \gamma \rightarrow \varphi_c$ OR $\varphi \rightarrow F_s, \psi \rightarrow F_p, \gamma \rightarrow \psi_c$
AH and A_y (ped)	$\dot{\theta} \rightarrow F_s, \epsilon_{long} \rightarrow F_b$ $\dot{R} \rightarrow \delta_{th}$ $\varphi_e \rightarrow F_s, \epsilon_{lat} \rightarrow F_p$	$\gamma, \theta \rightarrow F_s, F_b$ $u \rightarrow \delta_{th}$ $\varphi \rightarrow F_s, \epsilon_{lat} \rightarrow F_p$	$\theta \rightarrow F_s, d \rightarrow F_b, \theta_c$ $u_a \rightarrow \delta_{th}$ $\varphi \rightarrow F_s, \gamma \rightarrow \varphi_c$ OR $\varphi \rightarrow F_s, \gamma \rightarrow F_p$
α_1 and β_1 (ped)	$\dot{\theta} \rightarrow F_s, \epsilon_{long} \rightarrow F_b$ $\dot{R} \rightarrow \delta_{th}$ $\varphi_e \rightarrow F_s, \epsilon_{lat} \rightarrow F_p$	$\gamma, \theta \rightarrow F_s$ $u \rightarrow \delta_{th}$ $\varphi \rightarrow F_s, \epsilon_{lat} \rightarrow \varphi_c$	$\theta \rightarrow F_s, d \rightarrow \theta_c$ $u_a \rightarrow \delta_{th}$ $\varphi \rightarrow F_s, \gamma \rightarrow \varphi_c$
α_2 and β_2 (ped)	$\dot{\theta} \rightarrow F_s, \epsilon_{long} \rightarrow F_b$ $\dot{R} \rightarrow \delta_{th}$ $\varphi_e \rightarrow F_s, \epsilon_{lat} \rightarrow F_p$	$\theta \rightarrow F_s, \gamma \rightarrow F_b$ $u \rightarrow \delta_{th}$ $\varphi \rightarrow F_s, \psi \rightarrow \varphi_c, \epsilon_{lat} \rightarrow F_p$	$\theta \rightarrow F_s, d \rightarrow F_b$ $u_a \rightarrow \delta_{th}$ $\varphi \rightarrow F_s, \psi \rightarrow \varphi_c, \gamma \rightarrow F_p$

* Fixed sights; φ_e is roll error

† Fixed depressed sight



Note: Front side operation assumed and/or insignificant throttle control task

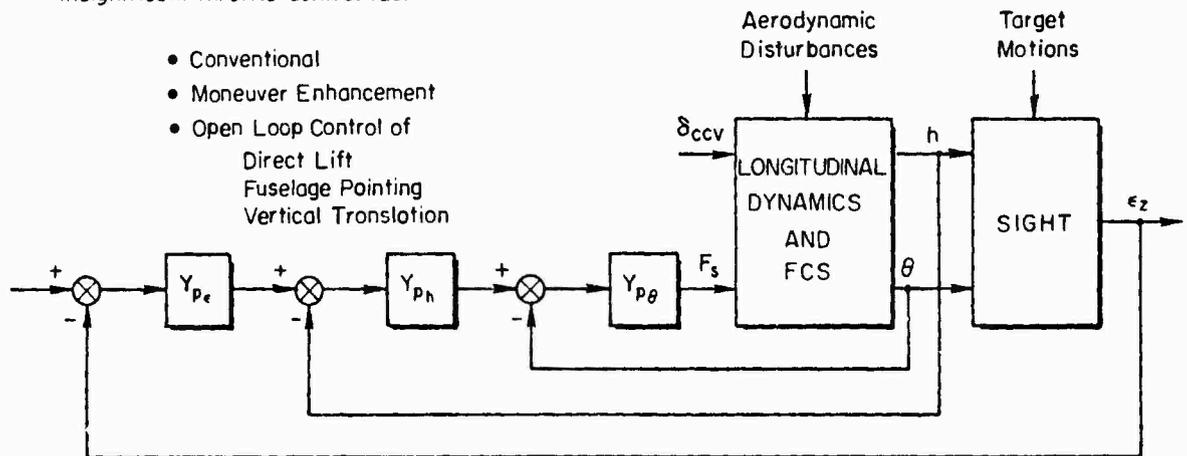
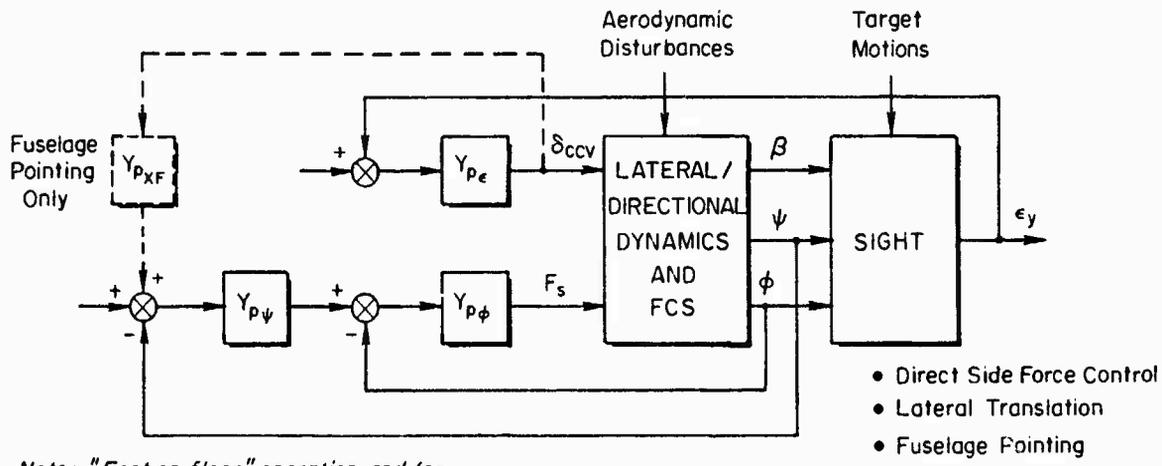


Figure 9a. Longitudinal Pilot/Vehicle/FCS Loop Structures



Note: "Feet on floor" operation and/or insignificant rudder control task assumed

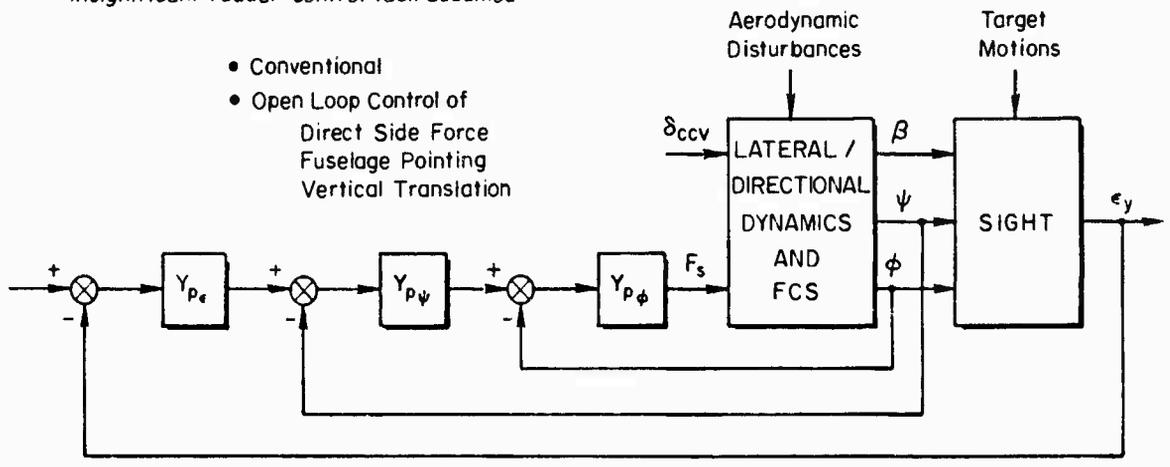


Figure 9b. Lateral-Directional Pilot/Vehicle/FCS Loop Structures

to heavy buffeting, turbulence, and the defending aircraft's air wake is an additional requirement.

"Rapid and precise" for the conventional and ME modes translate into a K/s -like characteristic in the pitch and roll attitude responses to the stick (and pedals for rudder controlled turns in some aircraft) at mid and high frequencies for both large and small maneuvers with filtering and actuation lags low enough to not limit the pilot. Based upon the YF-16 CCV flight testing of Ref. 17, these phase lags at small motion amplitudes should be such as to permit pilot crossovers (with maximum pilot effort) at frequencies approaching 2 Hz in pitch, somewhat less than this in roll. The pilot, not the aircraft response, should be the limiting factor in pilot/vehicle tracking performance.

For the direct force modes, Table 5 lists two additional loop closure possibilities which can presumably effect improved tracking performance. The conventional controls are used to establish the operating point while the pipper error is nulled using the auxiliary manipulator. Additional pilot workload is therefore implied — a factor which is presumably traded for significant performance advantages obtainable only by using the auxiliary controllers. At the same level of performance as before, the workload should be less on balance; otherwise there is no point in using the control. This in turn implies careful tailoring of the direct force mode characteristics.

The flight testing reported in Ref. 17 does not establish a significant performance advantage for the direct force modes. Examination of the response characteristics shows far from ideal responses in the lateral-directional axis, making it difficult for the pilot to use this control to advantage, particularly in the air-to-air tracking task where ordinarily the lateral pipper error is not nulled in a compensatory sense. Additional potential difficulties lie in the choice of manipulator for the directional task. Use of the rudder pedals precludes more conventional usage; use of the trim button aggravates the already difficult manipulator design problem of four fast-responding controls in one hand. Another design problem lies in tailoring for operation on the edges of the flight envelope.

In summary, use of direct force modes in air-to-air tracking has a number of conceptual disadvantages from the standpoint of pilot-centered requirements. Flight testing so far has failed to reveal any significant advantage for the tracking situation. On the other hand, the directional responses were distinctly non-optimum, suggesting that the system did not get a fair trial. Further testing of this concept appears warranted with careful attention to tailoring of the response properties and measurement of how the well-practiced pilot uses the CCV direct force mode capability.

For the pointing modes, Table 5 lists the same two additional loop closures with, however, distinctly different pipper error response dynamics relative to the direct force modes. These responses are not as well suited to the air-to-air tracking task as are the conventional responses. A more conventional-appearing response would appear to be deserving of additional testing, although the conceptual difficulties (no frequency separation of the controls and limited authority) appear quite formidable.

The translational modes appear ill-suited to air-to-air tracking of an evading target because of slow pipper error response.

b. Dive Bombing

The emphasis in this task shifts to path angle control; the attitude loop closures are inner loops. Typical weapon delivery maneuvers involve close to maximum performance maneuvering near the ground followed by a target acquisition and tracking phase, weapon release, and pullout. To establish the correct velocity vector at the desired bomb release altitude requires a brief period (less than 10 sec, often less than 5) of wings-level sight lineup in the lateral-directional axes, and a preselected throttle/speed-brake trim and dive angle (pitch attitude) in the longitudinal axis. The weapon is released when the depressed sight pipper moves up to and across the target.

This bomb delivery technique is vulnerable to winds and wind shears which cause an apparent lateral drift in aim, and a too fast or too slow

pipper velocity up to the target. Compensation for these environmental disturbances requires that the pilot "lead" the target laterally and advance or delay his weapon release.

This in turn may require a sidestepping lateral correction which is complicated by the apparent "wrong way" target motion inherent in a rolling maneuver with a fixed, depressed sight (see Ref. 11). This difficulty has motivated solutions ranging from simple roll-compensating sights to much more elaborate sight compensation laws implemented via head-up display; it has also motivated unconventional aircraft response properties which avoid rolling the aircraft (see subsequent discussions).

For conventionally responding aircraft, rapid target acquisition and subsequent tracking requires rapid, precise attitude maneuvering and fixed-target tracking capability; and close control of speed to assure the desired "canned" relationship between fuselage attitude and the velocity vector. It further requires that the path angle lag changes in attitude by an amount sufficiently small not to prolong the required target tracking period beyond a few seconds.

The loop structure shown in Table 5 for the direct force modes in the dive bombing task shows the essential change to be use of the flat turn capability for turning and sidestepping maneuvers, thereby simplifying the lateral tracking task. Despite the less than optimum dynamic characteristics for this mode, the flight test results showed significant advantages with this mode of response using a fixed, uncompensated sight.

As presently implemented, the direct side force mode commands both a lateral acceleration and a yaw rate. One might also consider an implementation which would allow "cross control" with the rudder pedals to cancel the yaw rate. The result would be to allow continuous crosswind correction while maintaining aim.

Because the essentials of the dive bombing task are related to path angle control, use of the pointing modes is inappropriate for tracking in this task.

The translational modes, particularly the lateral translational mode, allow the pilot to trim out crosswind effects; large lateral maneuvers require reversion to the conventional roll-to-turn technique.

c. Landing Approach

The essential requirements in this task are for precision path control, sometimes, as in carrier approach, in the presence of powerful and relatively high bandwidth disturbances. In addition, the airspeed must be maintained at a level comfortably above stall speed, and there are requirements for flare and establishing landing attitude and orientation for touchdown.

The loop closures shown for the longitudinal degrees of freedom in Table 5 in the conventional or ME modes presume frontside operation. At speeds close to or slower than minimum drag speeds, low-frequency closures of $d + \delta_{th}$, $u_a + \theta$ can be employed. The alternatives shown for the lateral-directional degrees of freedom are for the wing-low and crabbed approach techniques, respectively. The requirements are for rapid correction of path deviation errors, a process which requires inner attitude loop closures as shown and a rapid path change response to attitude.

The loop structure shown in Table 5 for the direct lift mode in the landing approach has the path deviation loop closed to the auxiliary controller, at least for small inputs. The principal attributes are a faster path response and the ability to control path and attitude independently in satisfaction of touchdown requirements. The faster response is particularly important for those aircraft having a sluggish attitude response and/or path response to attitude change characteristic. This is particularly true for carrier approach.

In the lateral-directional degrees of freedom, the direct force mode allows a wings-level correction for crosswinds which result in a crabbed approach. If implemented using an auxiliary control on the stick, with the pedals still active in their usual role, the pilot can cross-control with the pedals to cancel the yaw rate commanded in the direct force

mode. The results allows a runway-aligned, wings-level approach technique. The result is equivalent to the lateral translation mode (Table 5), but with the pilot providing the heading constraint using the pedals.

The loop structure shown in Table 5 for the fuselage pointing modes is identical to that for conventional flight; this mode is not useful for path control.

5. Summary

This section of the report has outlined the essential features of the several modes of response in literal and graphical terms, thereby relating response properties (e.g., dynamics and control authority) to aircraft-dependent equivalent stability derivatives (which include effects of any stability augmentation). In so doing it has been possible to point out how these responses compare with those of the conventional airplane, at least in a qualitative fashion, in three flying tasks. The tasks each emphasize a different response variable, but the results can be generalized to related piloting situations.

The several separated modes of response, i.e., those controlled with separate manipulators, generally can be criticized as offering poorer task performance (slower response, lower authority) at increased workload (additional manipulators to be controlled). The obvious exception to this is the flat turn mode in the dive bombing task. The translational modes may prove to be of positive advantage for short-range tracking tasks such as formation flight and air-to-air refueling if the residual fuselage pointing responses are small or in a favorable direction. However, augmentation of $-Z_w$ and $-Y_v$ will be required for large closed-loop bandwidth.

The integrated response mode, maneuver enhancement, appears to be of positive benefit across the board because of the potentially faster responses. In effect, both $-Z_w$ and the effective Z_{δ_e} can be modified to reduce the path response delays to attitude changes, although this was not specifically demonstrated in this section. ME will likely be

tailored differently for different tasks, e.g., air-to-air gunnery as contrasted with landing approach. We recommend exploring the possibility of an equivalent enhancement of the lateral-directional responses as well.

Analyses in the appendices explore the difference between the "ideal" responses discussed here and the analytically predicted responses of the YF-16 CCV. For example, Appendix A shows that small differences in the crossfeeds between the forward canard surface commands and the rudder and aileron lead to large differences in the piper error responses — particularly where the piper error is sensitive to motion in a degree of freedom which is ideally zero, according to the "logic" of the CCV response mode.

The translational modes are particularly sensitive in this respect because it is extremely difficult to avoid some angular perturbation, to which the piper error at long range is quite sensitive. This result supports the conclusion that the utility of the mode is restricted to close-range applications where the angular perturbations have relatively little effect on the aim error.

Adherence to the motion constraints implied in the pointing modes is somewhat easier. Only the low-frequency responses are affected by path angle changes, and the amount is inversely proportional to range.

In the direct lift and direct side force (flat turn) modes, elimination of changes in the angles of attack and sideslip is important to the degree that the flying task demands precise control over path angle by means of tightly controlled attitude. Improved bombing accuracy should result, even with a shorter period of constant-attitude, 1 g flight prior to release, when the sideslip excursions are minimized in the "flat turn" mode. A similar result should hold in the longitudinal task as well when using either the A_N or ME response modes.

E. APPROACHES CONSIDERED AND REJECTED

Several approaches to determining flight qualities requirements related to imperfect decoupling were pursued and rejected during the course of this study, as briefly summarized below:

- The first approach is based on work of Brulle, et al., Ref. 12, where it was shown that pilot ratings degraded rapidly when the coupling became unfavorable or in a direction opposite to the commanded motion. Problems with this approach center about the need for more than simple regulation against unfavorable coupling. Defining specifically how much unfavorable coupling would be tolerable would depend upon the specific mode in question and would entail an unreasonably large matrix of requirements. In addition, it would be necessary to account for coupling that occurs simultaneously in several axes. Finally, there is the basic question as to whether the undesirable features of adverse coupling are associated with secondary aircraft motions, which simply annoy the pilot, or are more directly associated with the inability of the pilot to close a tight loop for desirable (primary) performance. If the latter is true, then the specification should be more directly associated with the ability of the pilot to close a tight loop, e.g., attain a high closed-loop bandwidth relative to that of the forcing function. It is on this latter basis that the bandwidth hypothesis was originated.
- The specification of closed-loop performance was seriously considered. The drawback to this approach is that it requires a universal pilot model upon which everyone can agree. For control over single degrees of freedom such a model can be accepted for design use, as evidenced by the Neal-Smith criterion in Ref. 25. However, the rules for establishing a pilot model are not well known when coupling between axes is involved. We do know that when faced with a limiting situation the pilot will revise his technique, with the result that a predicted degradation in flying qualities based upon a one-degree-of-freedom model will not be valid. An example of this is turn coordination in conventional airplanes, where pilots use only lateral stick for aircraft with low values of adverse yaw. Based on heading bandwidth utilizing only lateral stick, we would expect a rapid degradation of pilot ratings, with increasing adverse yaw. However, as noted by Hoh and Ashkenas in Ref. 26 (page 313), the pilots are willing to use a secondary control (rudders) to counteract the adverse yaw without a degradation in opinion, depending on the specifics of the roll/yaw coupling. These specifics have been quantified in terms of a correlating parameter (in Ref. 26). However, the concept of developing parameters similar to μ for all possible DFC modes would be awkward for a specification. Finally, there was considerable resistance to the approach of utilizing pilot model parameters directly in a flying qualities criterion at the 1978 Flying Qualities Workshop (Ref. 26).

SECTION III

BANDWIDTH HYPOTHESIS

A. BASIC CONSIDERATIONS

The fundamental reason for going to the extra complication of independent control of each of the the six degrees of freedom is to allow improved performance in some specified task. This nearly always entails closed-loop tracking with attendant improvements in performance through faster closed-loop responses. The increased closed-loop response is fundamental and necessary whether the loop is closed via an automatic system or by the human pilot, i.e., there is a guidance and control, as opposed to a pilot-centered, requirement.

The maximum benefit which can be obtained from direct force control can be estimated by considering the limiting aircraft responses for DFC modes. Such limiting responses, derived by assuming infinitely tight feedbacks, have already been presented in Table 2. An example showing the derivation of coupling numerators and the resulting limiting form in Table 2 is given in Appendix D for the lateral translation mode (β_2) mode. Examination of the limiting forms in Table 2 leads to the following important observations:

- The response characteristics are symmetric between axes, that is, the basic form is the same for normal and lateral acceleration; pitch and yaw pointing and vertical and lateral translation. The implication of this is that it may not be necessary to separately specify requirements for longitudinal and lateral DFC modes. Of course, it must be recognized that the response requirements are dependent upon the frequency content of the input, which may be different for the lateral and longitudinal axes, depending on the specific details of the piloting task. In developing a flying qualities criterion we would expect that extensive data correlation need only be accomplished in one axis; the requirements for the other axis may then be scaled up or down by pertinent task differences.

- The use of DFC pointing and translation modes (α_1 , α_2 and β_1 , β_2) is inherently limited by basic airframe characteristics. For example, the basic response of the lateral translation (β_2) mode is limited by the inverse time constant Y_v , which tends to be a small number, on the order of 0.2 to 0.3, for contemporary aircraft. Physically this means that even with perfect decoupling, the lateral translation mode could require a special piloting technique due to a tendency for the aircraft to continue drifting laterally upon release of the CCV control. An example of this is quoted below from Ref. 18 (CCV Flight No. 38-F16):

"A technique not previously evaluated using lateral translation involves reversing the command before the original side velocity had coasted to a stop, thereby providing increased deceleration to expedite the stop. This method of operation substantially improved the usefulness of the β_2 mode. In previous evaluations of this mode the side velocity was allowed to coast to a stop after the applied command was removed."

The above pilot commentary indicates that the basic DFC response was unacceptably slow (low Y_v), but that a special piloting technique could be utilized to make the mode acceptable, that is, effectively generating lead to augment Y_v . It follows logically that a more successful lateral translation mode could be developed by augmenting Y_v via feedback of sideslip to the direct side force controller. This of course has implications on the frequency response characteristics of the servo drive as well as the authority required for the direct side force control. Similar observations regarding inherent airframe limitations and the necessary feedbacks required to overcome these limitations can be derived from other expressions for the limiting DFC response modes in Table 2.

- Whereas the responses of the pointing and translation modes are inherently limited, those of the normal acceleration and wings-level turn modes are basically infinite (open-loop phase > -90 deg), assuming a pure DFC response. The implication of this is that in the normal acceleration and wings-level turn mode the inherent closed-loop response limitations will be due to coupling and/or imperfect cancellations in the DFC feedback and crossfeed mechanizations.

B. BANDWIDTH AND RESPONSE

Bandwidth is defined by Ref. 27 as that frequency at which the closed-loop amplitude is down 3 dB from the low-frequency value (which is usually zero dB when the closed-loop system is low pass). For a closed-loop system characterized by a first-order response, the bandwidth as defined above is also the crossover frequency (corresponding to an open-loop magnitude of zero dB) of the constituent rate-ordering K/s open loop as shown on the left side of Fig. 10. In this figure the crossover frequency is labeled ω_c and the bandwidth $1/T$; the latter to signify that bandwidth is here a direct measure of the closed-loop time response to a step command as shown on the right side of Fig. 10. In this case then, crossover frequency, bandwidth, and (inverse) response time are identical.

In general, such exact unity does not carry over to higher-order systems. Nevertheless in many cases, including those of flying qualities interest, the bandwidth as defined above is close, but though exactly equal, to the crossover frequency. In the field of aircraft flying qualities, "bandwidth," defined by the highest open-loop crossover frequency attainable with good closed-loop dynamics, is typically used to measure the speed of response a pilot can expect when tracking with rapid control inputs. Bandwidth indicates how tightly he can close the loop without threatening the stability of the pilot/vehicle system; it is a measure of tracking precision and disturbance rejection. For precise tracking tasks, maximizing open-loop stability and damping allows the pilot to track high-frequency inputs and reject disturbances without unacceptable oscillations due to low damping in the closed-loop system.

The relationship between closed-loop damping and open-loop phase margin for an ideal open-loop plant ($G = Ke^{-\tau s}/s$ where τ is the pilot's time delay, e.g., Ref. 28) is shown in Fig. 11, taken from Ref. 27. Based on a study of simulation data using pilot/vehicle analysis techniques, Ref. 29 showed that a closed-loop damping ratio of 0.35 sets the approximate boundary between undesirable and desirable flying qualities (see Fig. 12). This is in close agreement with a long-standing

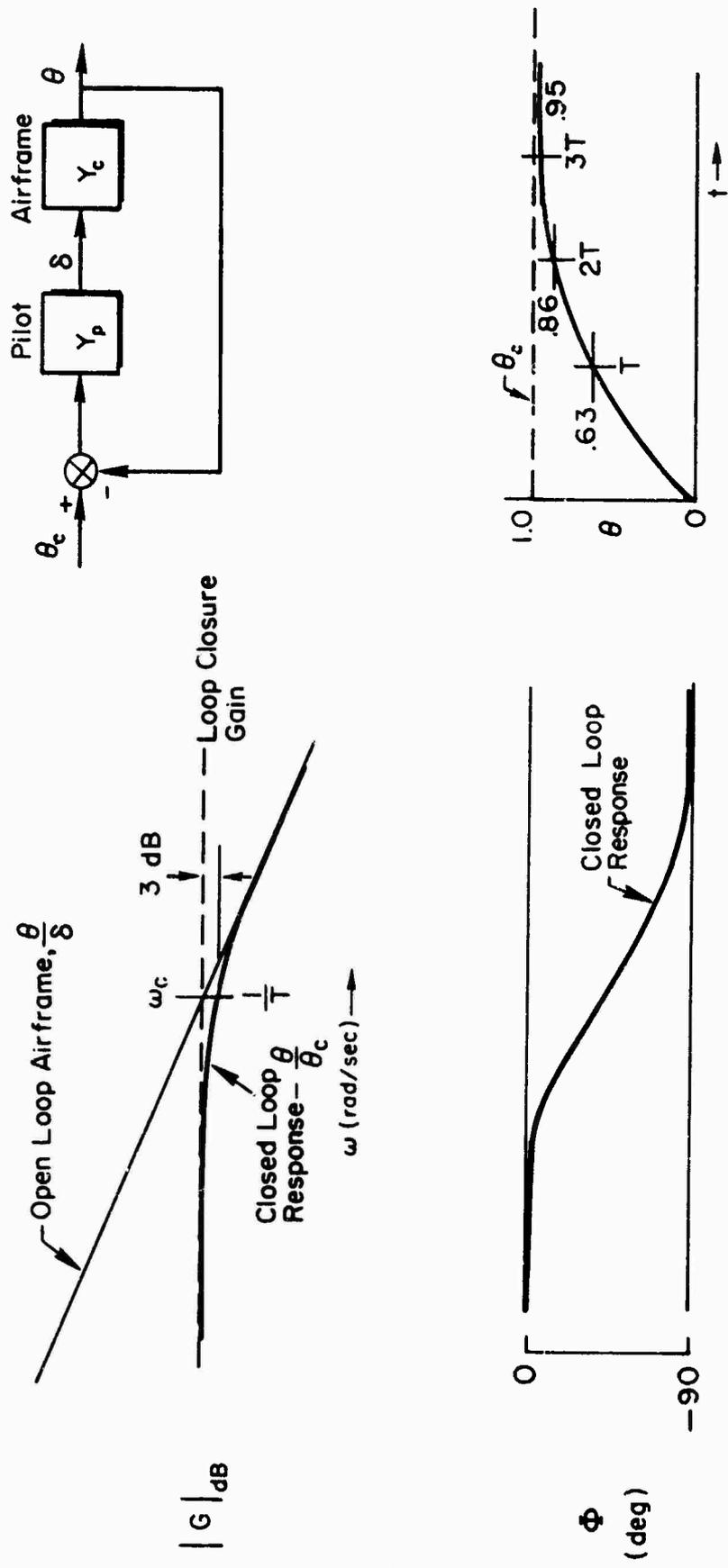


Figure 10. First-Order Bandwidth/Response Relations

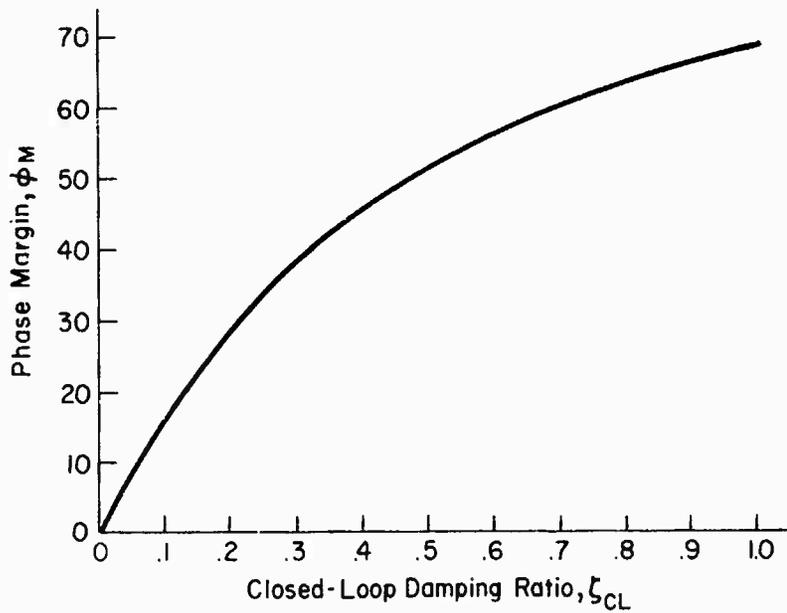


Figure 11. The Phase-Margin/Closed-Loop Damping Relationship for $G(s) = Ke^{-Ts}/s$

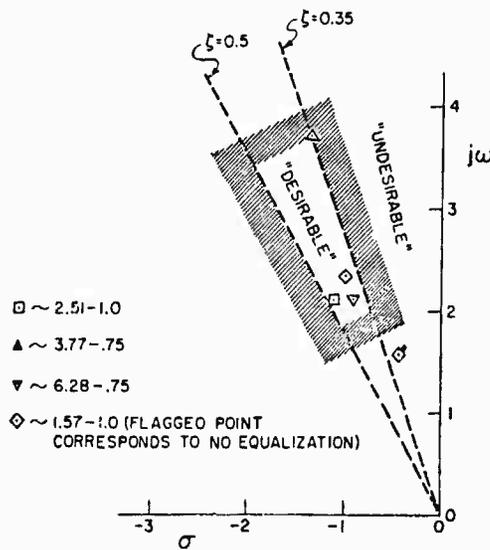


Figure 12. "Desirable" Closed-Loop Second-Order Characteristics Deduced Using Analytic Expressions for Y_p Consistent with Describing Function Data

requirement (Ref. 30) for short-period damping to 1/10 amplitude in one cycle or less. From Fig. 11, the corresponding open-loop phase margin is about 42 deg for aircraft which are K/s-like in the region of crossover. On this basis we have picked 45 deg phase margin to define a maximum crossover frequency, and thereby "bandwidth." This choice is consistent with servoanalysis practice and usage.

In some cases the shape of the open-loop frequency responses is such that a slight increase in gain results in a rapid decrease in phase margin. In these cases adequate stability is more properly set by specifying gain margin rather than phase margin. Such cases are characterized by flat regions or "shelves" in the amplitude plot, as sketched in Fig. 13, which is typical of conventional "short period" pitch attitude response. As illustrated, significantly lower bandwidths can result when gain as well as phase margin criteria are used to define bandwidth. A gain margin of 6 dB (factor of 2 in gain) is typically used to establish the crossover frequency and has been adopted in our complete definition of bandwidth, which is:

The bandwidth of the specified response to a particular control input is defined as the lowest frequency for which the (open-loop) phase margin is at least 45 deg and the gain margin is at least 6 dB.

Note that in this definition the (pilot-) closed-loop system bandwidth is implicitly defined as the open-loop crossover frequency. "Open-loop" refers to the vehicle with any stability and control augmentation operating.

It is well established that pilots will attempt to equalize the open-loop response characteristics ($Y_p Y_c$ of Fig. 10) to a K/s shape, supplying whatever lead or lag is required to make the slope of the magnitude plot -20 dB/dec and the phase -90 deg (see, for example, Ref. 28). Controlled elements requiring lag equalization are generally downgraded a minimal amount, whereas requirements for significant amounts of pilot-generated lead ($T_L > 1$ sec) are symptomatic of unsatisfactory flying qualities (Ref. 28).

Open Loop Transfer Function

$$\frac{\theta}{\delta} = \frac{(s+1/T)e^{-\tau s}}{s^2 + 2\zeta\omega s + \omega^2}$$

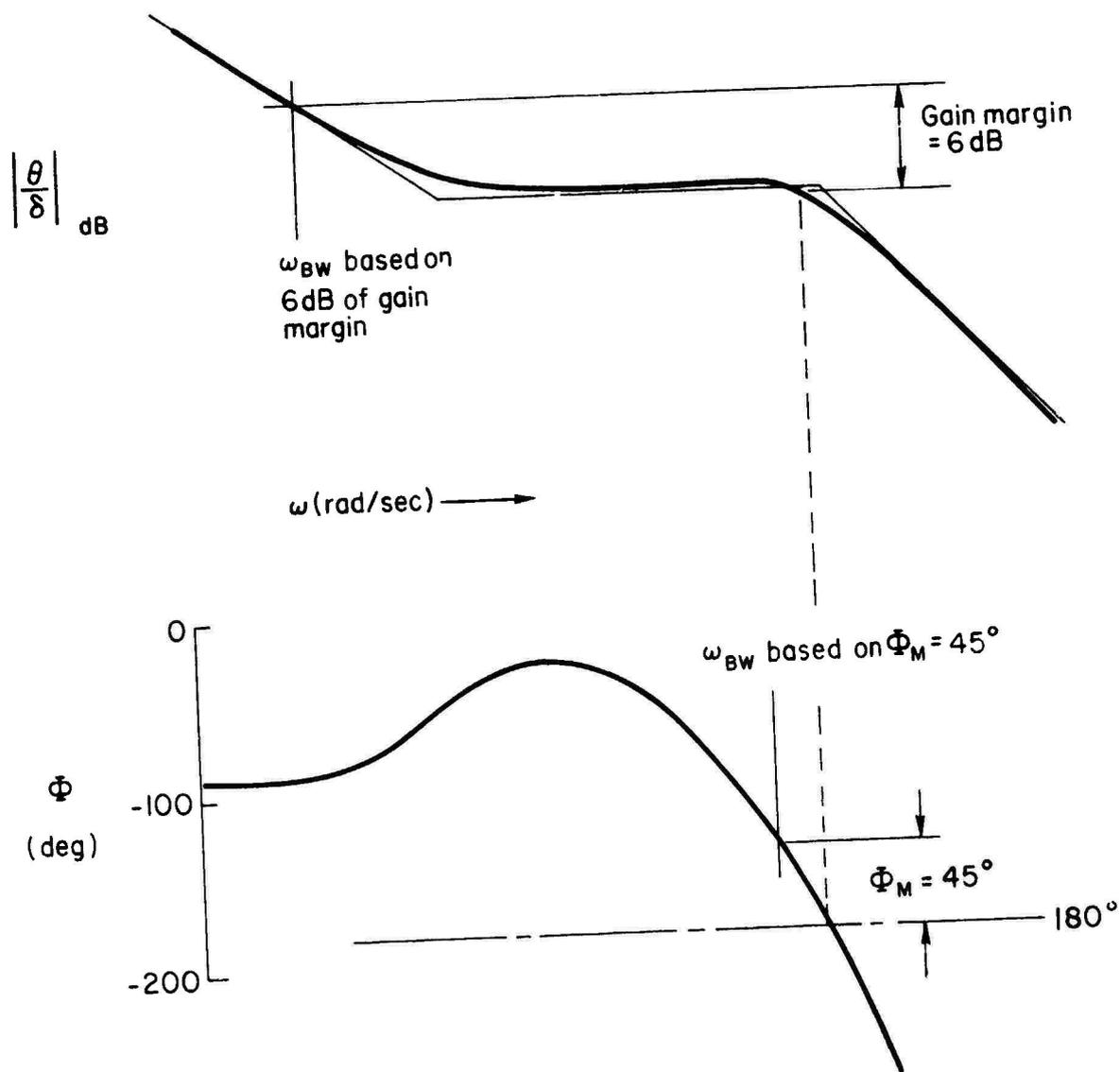


Figure 13. Effect of Using Gain and Phase Margins to Define Bandwidth

The equalization requirement (if any) for configurations with high bandwidth is lag, whereas low-bandwidth cases by definition require lead. A detailed analysis of flying qualities characteristics necessarily involves consideration of the case-by-case pilot equalization requirements. However, direct force control is intended to provide superior flying qualities; flight is generally possible without it. Accordingly, for the purpose of defining levels of good flying qualities it seems appropriate to consider only whether or not a pure gain pilot must add lead to equalize the open-loop aircraft response to a K/s shape in the region of crossover, i.e., the frequency range where the pilot closes the loop (1/T in Fig. 10).

C. PHYSICAL SIGNIFICANCE OF BANDWIDTH

From a pilot's point of view, a high-bandwidth response would be described as "crisp" or perhaps "rapid and well damped." Typical commentary for a low-bandwidth response might be "sluggish response to control input" or "tends to wallow." There is a long history of correlating such commentary with basic aircraft stability derivatives and/or parameters made up of such derivatives (e.g., $\omega_{sp} \pm \sqrt{Z_w M_q - M_{\dot{\alpha}}}$, etc.). The term bandwidth comes more naturally into play when feedbacks and crossfeeds are combined to produce aircraft responses which are unconventional in that the classical modes are no longer appropriate definitions. Thus, "bandwidth" may be thought of as a dual or equivalent parameter to the short-period or dutch-roll frequencies as well as to the heave-mode and roll-mode time constants, all of which may also be considered representative of non-augmented, modal bandwidths.

As an example, consider the bank angle response of a conventional aircraft to a lateral stick input, which has the transfer function form (see Ref. 30):

$$\frac{\phi}{\delta_a} = \frac{L_{\delta_A}(s^2 + 2\zeta_{\phi}\omega_{\phi}s + \omega_{\phi}^2)}{(s + 1/T_s)(s + 1/T_R)(s^2 + 2\zeta_d\omega_d s + \omega_d^2)} \quad (16)$$

The (Bode) frequency response of the idealized open-loop transfer function and the closed-loop time response are sketched in Fig. 14 for

$$\frac{\phi}{\delta_a} = \frac{L\delta_a (s^2 + 2\zeta\omega_n s + \omega_n^2)}{(s + 1/T_S)(s + 1/T_R)(s^2 + 2\zeta_d\omega_{d_n}s + \omega_{d_n}^2)} = \frac{L\delta_a}{s(s + 1/T_R)}$$

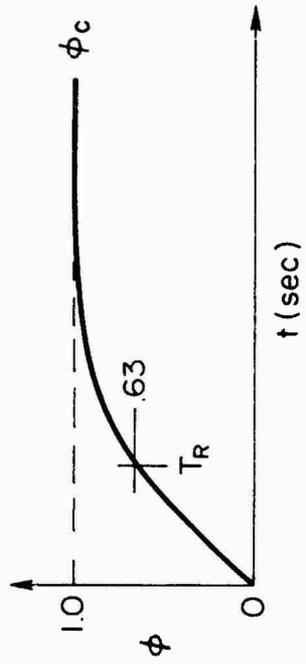
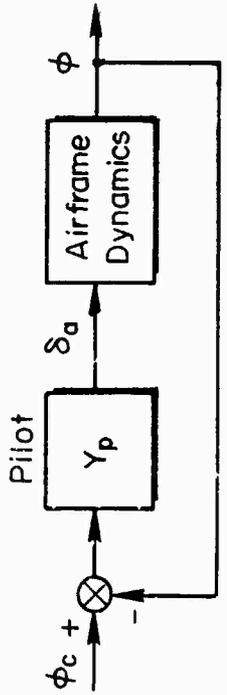
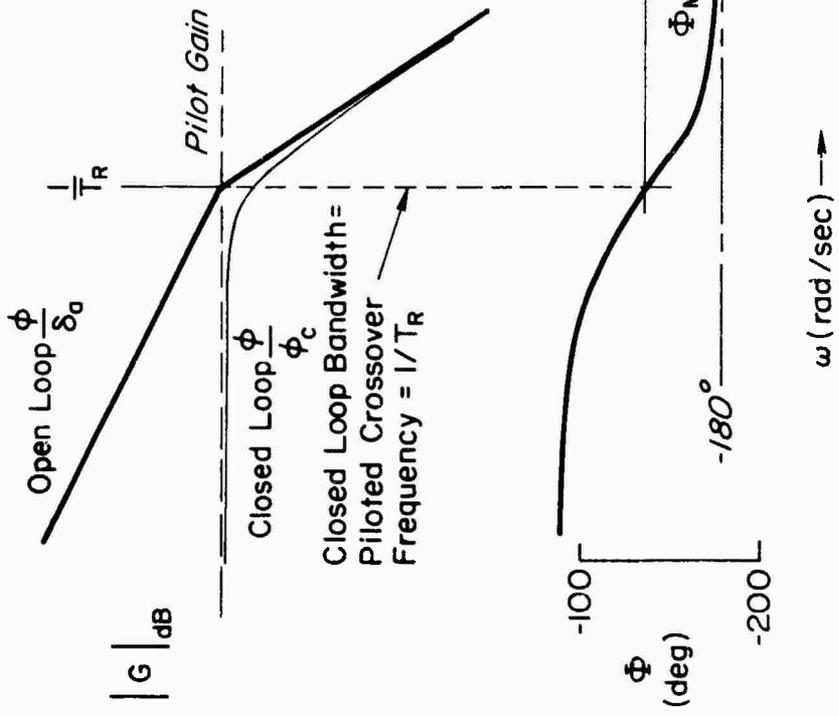


Figure 14. Classical Roll Dynamics with $\omega_\phi = \omega_d$

the case with minimal inter-modal coupling ($\omega_\phi \neq \omega_d$); the usually small spiral root, $1/T_S$, set to zero; and a pure-gain pilot transfer function (K_p). The bandwidth as defined by 45 deg phase margin occurs at a frequency near $1/T_R$ (exactly $1/T_R$ for $\omega_\phi = \omega_d$). It is well known that T_R defines the crispness (rapidity) of the roll response to lateral stick control inputs. Physically, $1/T_R$ is defined by the roll damping. For the unaugmented airplane

$$\frac{1}{T_R} \approx -L_p = -\frac{\rho S U b^2}{4 I_x} C_{l_p} \quad (17)$$

Since the bandwidth is defined by $1/T_R$, the airplane design implications of specifying an increased level of bandwidth are to require:

- An increase in aspect ratio which increases both C_{l_p} and b^2 .
- A decrease in the rolling moment of inertia (I_x).

Values for minimum $\phi \rightarrow \delta_a$ "bandwidth" have been defined for classical configurations in Para. 3.3.1.2 of MIL-F-8785B (Ref. 3) in terms of maximum values for T_R .

T_R was picked as a correlating parameter in Ref. 31 precisely because it defines the bandwidth of the roll response. Because all of the test cases were of conventional form (all looked like Fig. 14) there was no need to define a more general criterion (such as bandwidth). That is, T_R defines the rise time or speed of response. Where we are trying to specify criteria for independent control over six degrees of freedom, the large number of possible response characteristics makes it impractical to identify a simple parameter such as T_R . However, the physical implications of specifying a minimum bandwidth are identical to those of specifying a minimum $1/T_R$ (or ω_{sp} , ω_d , etc.). For example, if $1/T_R$ is too low, feedback augmentation (roll rate to aileron or $p \rightarrow \delta_a$) is typically used to increase L_p , e.g.,

$$L_{p_{aug}} = L_p + K_p L_{\delta_a}$$

If the basic L_p is low and the aileron control sensitivity (L_{δ_a}) is low, large values of K_p will be required if a high bandwidth is specified. All the well-known factors associated with high feedback gains apply to this case as well as to the less conventional DFC cases. Some examples of the effect of requiring increased bandwidth through augmentation are:

- Increased bandwidth implies increased control power to avoid saturation ($\delta_a = K_p p / L_{\delta_a}$). This may affect control surface/actuator sizing and travel and maximum rate.
- Failures of components which make up the feedback path may cause large transients. This has implications on redundancy and failure monitoring requirements.
- Servo actuator dynamic response characteristics must be well above the required bandwidth (required value of augmented $1/T_R$).
- Sensor noise must be kept to a minimum, as it will be amplified by large values of K_p .

Although these factors are presented using the classical bank angle response as an example, they are basic and apply to the general DFC case with equal validity.

Consider now the effect of coupling on bandwidth using the same classical bank angle response as an example. If ω_ϕ is significantly different from ω_d , the roll response bandwidth may not be well defined by $1/T_R$.

A number of possibilities arise depending on the damping of ω_ϕ and ω_d as well as their relative magnitudes. This has resulted in significant complications in writing a specification, even for conventional aircraft. The p_{osc}/p_{av} and $(\Delta\beta)_{max}/k$ requirements of Refs. 3 and 31 as well as the μ parameter (see Ref. 26, page 331) are all attempts to account for such complications. As an example, consider the case where $\omega_\phi > \omega_d$ and the corresponding roots are both are lightly damped, as in

Fig. 15. It is apparent that the bandwidth is considerably less than $1/T_R$ due to coupling, which is manifested by $\omega_\phi > \omega_d$. Values of $\omega_\phi > \omega_d$, corresponding to proverse yaw, typically result from use of spoilers for roll control or from an incorrect aileron-rudder crossfeed (e.g., one mechanized for a different flight condition). It is important to note that in this conventional aircraft augmentation example the characteristics of the coupling depend on the relative location of the poles and zeros. Such dependence is of course inherent for all transfer characteristics including the DFC coupling discussed in Section II-C (see in particular Fig. 4).

In the above example we have illustrated that the use of bandwidth is really not a new flying quality concept, nor one that is necessarily universally applicable. Instead, it is a generalization which takes into account the same fundamental principles which guided the selection of many familiar "primary" flying qualities parameters, e.g., T_R , ω_d , ω_{sp} , n/α , etc. However, in the DFC case, the use of secondary controls by the pilot to improve the response to the primary control (such as using rudder to eliminate adverse yaw) is specifically prohibited. This follows inasmuch as the sole purpose of independent control over six degrees of freedom is to simplify the piloting task; it therefore seems fundamentally inconsistent to require secondary control usage. Some experimental verification of this was obtained during the flight tests (see Section V) where the pilots objected to using lateral stick to counter the effects of adverse roll coupling in the wings level turn mode.

Disallowing the pilot to use secondary controls simplifies the task of writing a flying qualities specification. That is, we do not have to consider possible improvements in bandwidth in a given axis due to the pilot's use of a control in some other axis. It should be noted that the use of secondary controls at frequencies well below the crossover frequency is quite acceptable. Such low-frequency control usage would of course be ineffective for improving the system bandwidth and would be considered more of a trim function.

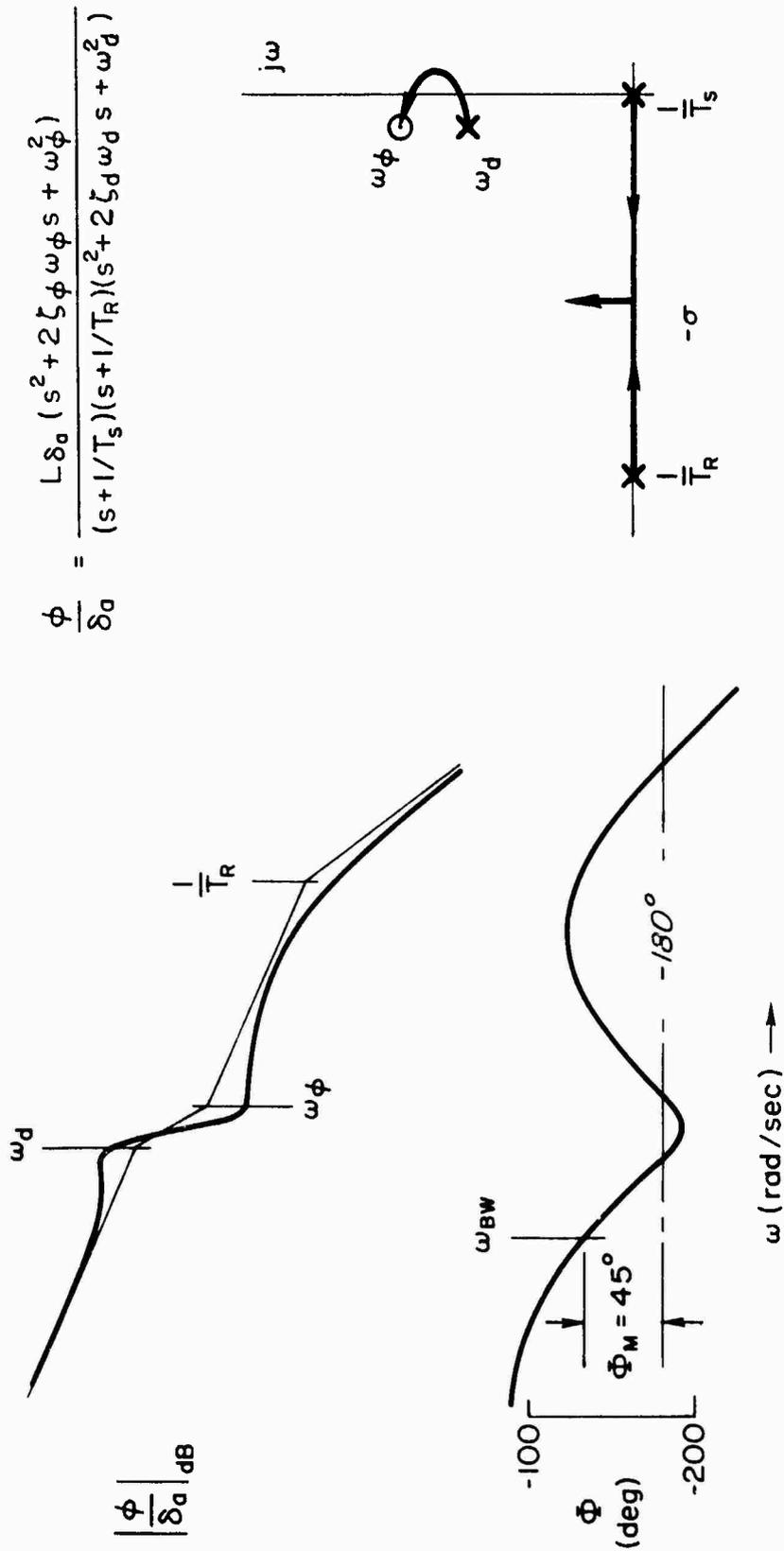


Figure 15. Effect of Roll-Yaw Coupling on Bandwidth

D. SYSTEMS IMPLICATIONS OF HIGH BANDWIDTH

The very fact that we are hypothesizing a handling qualities criterion in terms of a parameter heretofore limited to analysis and synthesis of feedback control systems is indicative of the importance and impact of highly augmented airframes (see Ref. 27, Chapters 5 and 6). For highly augmented aircraft it is not always possible to specify the "rapidity or crispness" of response (bandwidth) in terms of a single mode or modal parameters such as ω_{sp} , ω_d , T_R . This, of course, is due to the higher-order nature of the augmented response transfer functions. One approach to analyzing highly augmented aircraft which has been used with considerable success is to draw an analogy between the higher-order system (HOS) and its lower-order classical system (LOS) equivalent. Such a procedure was utilized, e.g., in Refs. 32 and 33, where it was noted that good correlations with pilot ratings could be obtained by matching longitudinal HOS responses with the short-period approximation

$$\frac{\dot{\theta}}{\delta_s} = \frac{K_q(s + 1/T_{\theta_e})e^{-\tau_e s}}{s^2 + 2\zeta_e \omega_e s + \omega_e^2} \quad (18)$$

Setting boundaries on L_α , ω_e , and τ effectively specifies the system bandwidth. Hence the bandwidth and equivalent systems approaches are basically very similar (but the former is more compact). Accordingly, the physical implications of these approaches are also similar and can be related to the basic aerodynamic derivatives by considering the lower-order approximations for classical airplanes (see Ref. 27, Chapters 5 and 6). For example, a large value of bandwidth in the lateral axis is equivalent to a large value of roll damping ($-L_p$) as already noted. The primary difference between highly augmented airplanes and unaugmented airplanes having a similar large bandwidth lies in their responses to external disturbance. A conventional aircraft with large static stability will have a high short-period frequency ($\omega_{sp} \doteq \sqrt{-M_\alpha + Z_w M_q}$), and will also be highly sensitive in pitch to vertical gusts by virtue of its large $M_\alpha \doteq \omega_{sp}^2$, viz.:

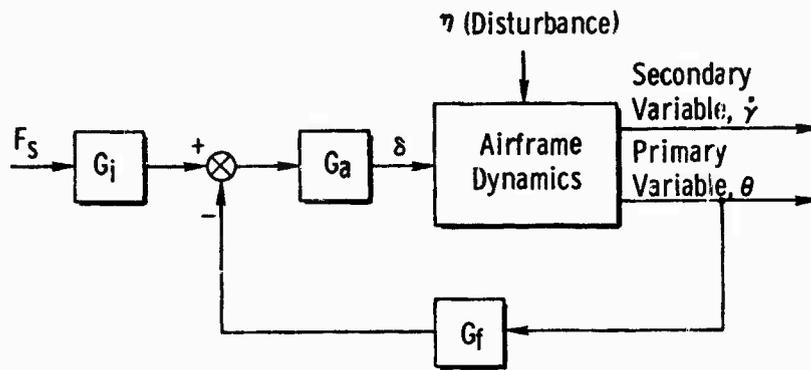
$$\frac{\theta}{w_g} = -\frac{1}{U_0} \frac{M_\alpha}{(s^2 + 2\zeta_{sp}\omega_{sp}s + \omega_{sp}^2)} \quad (19)$$

In principle, augmentation of stability by angle of attack feedback will produce the same result. However, if an equivalent bandwidth (approximately ω_{sp}) is achieved via a large pitch attitude feedback gain K_θ (as in an attitude hold, rate command system), the response to vertical gusts does not occur at the augmented short period and is much smaller. That is, for large values of K_θ ,

$$\left. \frac{\theta}{w_g} \right|_{\theta+\delta} \approx -\frac{1}{U_0 K_\theta M_\delta} \frac{M_\alpha}{(s + 1/T_{\theta_2})} \quad (20)$$

(derived from the Ref. 27 transfer function numerator approximations). Comparing Eqs. 19 and 20 it is apparent that the magnitude of the attitude response is much reduced by the large K_θ . Furthermore, the dominant response frequency is reduced from the augmented short-period to the classical heave damping inverse time constant, $(1/T_{\theta_2} \approx -Z_w)$. Hence the pitching response of the highly augmented aircraft to vertical gusts will be considerably suppressed compared to the very statically stable aircraft. Both aircraft will have the same response to control inputs.

The foregoing considerations which pertain to the specification of handling quality criteria for aircraft employing advanced control concepts can be expanded and generalized by virtue of simple block diagram algebra as in Fig. 16. This figure illustrates that the equalization to achieve a desired set of control and disturbance response characteristics can be allocated to the forward loop (G_a), the feedback path (G_f), or operations on the input (G_1). Several key concepts are illustrated. First, the command response (Fig. 16a) can be made essentially independent of the basic vehicle dynamics (in heavy brackets). The disturbance response (Fig. 16b) can be made arbitrarily small by increasing the overall loop gain $G_a G_f$. Even after consideration of the practical



- a) ● Primary response/command

$$\frac{\theta}{F_s} = \frac{G_i}{G_f} \left\{ \frac{G_a G_f [G_{\delta}^{\theta}]}{1 + G_a G_f [G_{\delta}^{\theta}]} \right\} \rightarrow \frac{G_i}{G_f}$$

- b) ● Primary response/disturbance

$$\frac{\theta}{\eta} = \frac{[G_{\eta}^{\theta}]}{1 + G_a G_f [G_{\delta}^{\theta}]} \rightarrow \frac{[G_{\eta}^{\theta}]}{G_a G_f [G_{\delta}^{\theta}]}$$

- c) ● Secondary response/command

$$\frac{\dot{y}}{F_s} = \frac{\theta}{F_s} \left[\frac{G_{\delta}^{\dot{y}}}{G_{\delta}^{\theta}} \right]$$

- d) ● Secondary response/disturbance

$$\frac{\dot{y}}{\eta} = \frac{G_{\eta}^{\dot{y}} + G_a G_f [G_{\delta \eta}^{\theta \dot{y}}]}{1 + G_a G_f [G_{\delta}^{\theta}]} \rightarrow \left[\frac{G_{\delta \eta}^{\theta \dot{y}}}{G_{\delta}^{\theta}} \right]$$

Note: Heavy brackets imply transfer functions which are dependent only on basic airframe dynamics.

Figure 16. Effective Airframe Dynamics Pilot-Command/Disturbance Aircraft Response Relationships

limits imposed by actuator dynamics, control system lags, gain limits, etc., the disturbance response can be highly attenuated and the command response tailored nearly independently of the basic vehicle dynamics. Using the Fig. 16 example, the separation of command from disturbance response is an important consideration for attitude control systems. Systems which utilize a large $G_a G_f$ for disturbance suppression and a stick filter G_1 to avoid overly abrupt command response characteristics are frequently categorized as "model-following attitude systems." The bandwidth criterion is especially attractive for such highly augmented aircraft in that the effects of G_1 , G_a , and G_f are implicitly included. Experience with recent fighter aircraft has shown that the effect of the stick filter G_1 can be easily underestimated using conventional analysis of the "dominant modes."

Secondary responses (Figs. 16c and d) are defined by variables which are not fed back to a control. In the example shown in Fig. 16c, the secondary response, γ , to a command input F_s can be tailored somewhat by the attitude augmentation G_1 and G_f blocks, e.g., the two-degree-of-freedom short-period approximation (Ref. 27) yields

$$\frac{\gamma}{F_s} = \frac{\theta}{F_s} \frac{G_\delta^Y}{G_\delta^\theta} \pm \frac{G_1}{G_f} \frac{1}{(T_{\theta 2}s + 1)} \quad (21)$$

recognizing that $\theta/F_s \pm G_1/G_f$ and $\gamma/\theta \pm 1/(T_{\theta 2}s + 1)$. The secondary, γ , response to disturbances is not as susceptible to change by high K_θ ; in the limit, and using the short-period approximation,

$$\frac{\gamma}{\eta} \pm \frac{-(Z_\eta - M_\eta Z_\delta/M_\delta)}{s + 1/T_{\theta 2}} \quad (22)$$

In summary, requiring a minimum value of bandwidth is equivalent to insisting on rapid responses to control inputs without overshoots or any other undesirable characteristics of low damping. If such response is

not available through the basic airframe, it must be achieved via stability augmentation. If the basic values of the limiting aerodynamic derivatives are low, high feedback and/or crossfeed gains will be required with the resulting implications listed in Fig. 17.

- If the basic values of the limiting aerodynamic derivatives are low:
 - High feedback and/or crossfeed gains will be required.
 - Failures of single-channel systems will tend to be violent.
 - Redundancy and failure monitoring requirements will be high.
 - High authority controls will be required to avoid saturation.
- Obtaining adequate bandwidth via basic aerodynamics could lead to unfavorable turbulence response as well as high drag.
- The feedback sensors should be gustproofed, e.g., by sensing a_y instead of β , θ instead of α .

Figure 17. Implications of Bandwidth Criterion on Aircraft Design

E. THE BANDWIDTH HYPOTHESIS

The bandwidth hypothesis is stated as follows:

- Specification of bandwidth is an adequate flying qualities criterion for DFC dynamics.
- Secondary aircraft motions or coupling affect the pilot evaluation of a given DFC mode only insofar as such motions decrease the bandwidth to less satisfactory or unacceptable levels.
- The following characteristics must be separately specified: 1) control authority; 2) manipulator characteristics, such as gain, deadband, break-out, etc.; and 3) maximum pilot acceleration as a function of pilot restraint and task.

The bandwidth of interest here is dependent on the open-loop transfer function between the output the pilot is trying to control (aiming error, heading, pitch attitude, etc.) and the DFC manipulator.

F. JUSTIFICATION FOR BANDWIDTH HYPOTHESIS

The basic requirements for acceptable flying qualities for piloted aircraft, evolved over a period of years (e.g., see Ref. 34), consist of two fundamental subsets:

- Guidance and control requirements — fundamental and independent of whether the controller is an automatic or human pilot.
- Pilot-centered requirements — relate to the controller as a human operator.

A summary of these requirements is given in Table 6 (taken from Ref. 34). The guidance and control requirements listed in Table 6 all depend on adequate system bandwidth. The relationships between bandwidth and command following can be shown via the well-known "1/3 law" (see Ref. 28).

$$\frac{\bar{e}^2}{\bar{\sigma}_i^2} = \frac{1}{3} \frac{\omega_i^2}{\omega_c^2} \quad (23)$$

where ω_c is the piloted crossover frequency and ω_i is the frequency of the forcing function inputs. \bar{e} and $\bar{\sigma}_i$ represent the root-mean-square error and input signals, respectively. As we have seen, higher gain to suppress disturbances implies high bandwidth; while closed-loop stability and damping at the necessary frequency are inherent in the bandwidth definition.

The pilot-centered requirement for "minimum pilot compensation" depends entirely on the characteristics of the open-loop system (aircraft response to control input) which allow the pilot to close the loop at crossover frequencies well above the input frequency without using substantial amounts of lead equalization (see Ref. 28). As already discussed, the definition of bandwidth used in this study is directly related to the ability of the pilot to achieve tight closed-loop control with minimum pilot compensation.

TABLE 6

PILOT/VEHICLE SYSTEM REQUIREMENTS

<p><u>Guidance and Control</u></p> <ul style="list-style-type: none">● Command following● Disturbance regulation● Stability and Damping
<p><u>Pilot-Centered</u></p> <ul style="list-style-type: none">● Minimum pilot compensation<ul style="list-style-type: none">- feedbacks- equalization● Frequency separation of controls● Insensitivity to pilot response variations

The pilot-centered requirement for "insensitivity to pilot response variations" is related to the specification of open-loop bandwidth by including a gain margin requirement in the bandwidth definition, i.e., the pilot must be able to increase his gain by a factor of two without destroying the stability of the closed-loop system (6 dB gain margin).

The pilot-centered requirement for "frequency separation of controls" relates to achieving the specified open-loop bandwidth via the primary DFC alone. As noted previously, secondary control activity defeats the purpose of the DFC in the first place, and therefore should be disallowed. This was verified experimentally during the flight tests of the adverse roll coupling case (see discussion of adverse roll coupling in Section V).

Based on the above discussion and Eq. 23, it can be seen that it is very important to determine specification boundaries experimentally with a task involving input frequencies at or above those expected in the intended mission of the aircraft. A possible deficiency of the use of

pilot ratings from the handling qualities during tracking (HQDT) maneuvers of Ref. 35 is that the target aircraft motions (wind-up turn and constant g turns) may not provide high enough input frequencies to expose deficiencies in the tracking aircraft. The maneuvers usually employed are steady turns and occasional turn reversals.

In addition to the observation that the open-loop system bandwidth is directly related to the pilot-centered and guidance and control requirements for good handling qualities (Table 6), there are some experimental data which further support the bandwidth hypothesis. For example, the data presented in Ref. 12 indicate that adverse coupling results in degraded ratings. A review of the pilot commentary, however, leads one to suspect that the underlying problem was the inability of the pilot to tighten the loop — not simply residual motions which were annoying.

Finally, a study (Ref. 36) on the NASA Ames Flight Simulator for Advanced Aircraft (FSAA) showed a direct correlation between pilot rating and system bandwidth in an air-to-ground gunnery task. In that study an idealized wings-level turn mode was implemented on the simulator, and the frequency and damping were systematically varied from very low to very high values. The correlations obtained between pilot rating and closed-loop system bandwidth (defined by a 45 deg phase or 6 dB gain margin)* are shown in Fig. 18. These initial data correlations as well as the results of the Ref. 12 study, when combined with the analytical justification based on the guidance and control and pilot-centered requirements, were felt to be encouraging enough to warrant further development of the bandwidth hypothesis as a foundation for a DFC flying qualities criterion.

*These data were supplied in raw form by Mr. Robert Sammonds of NASA Ames Research Center, and were later reported in Ref. 43, where bandwidth was defined on the basis of phase margin alone, with a slightly different conclusion.

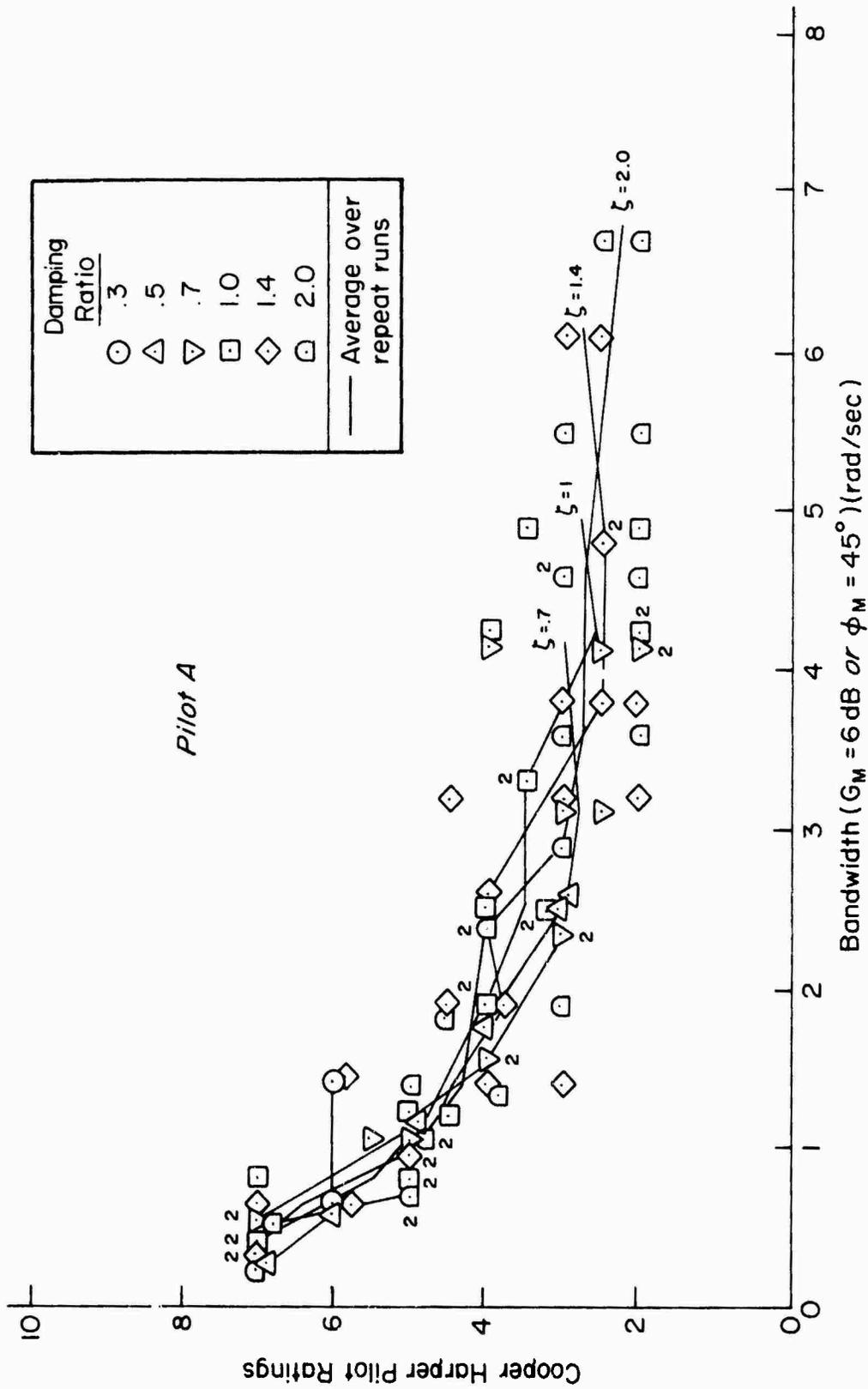


Figure 18. Correlation of Bandwidth with Pilot Rating; Moving-Base Simulation, Dive Bombing Task

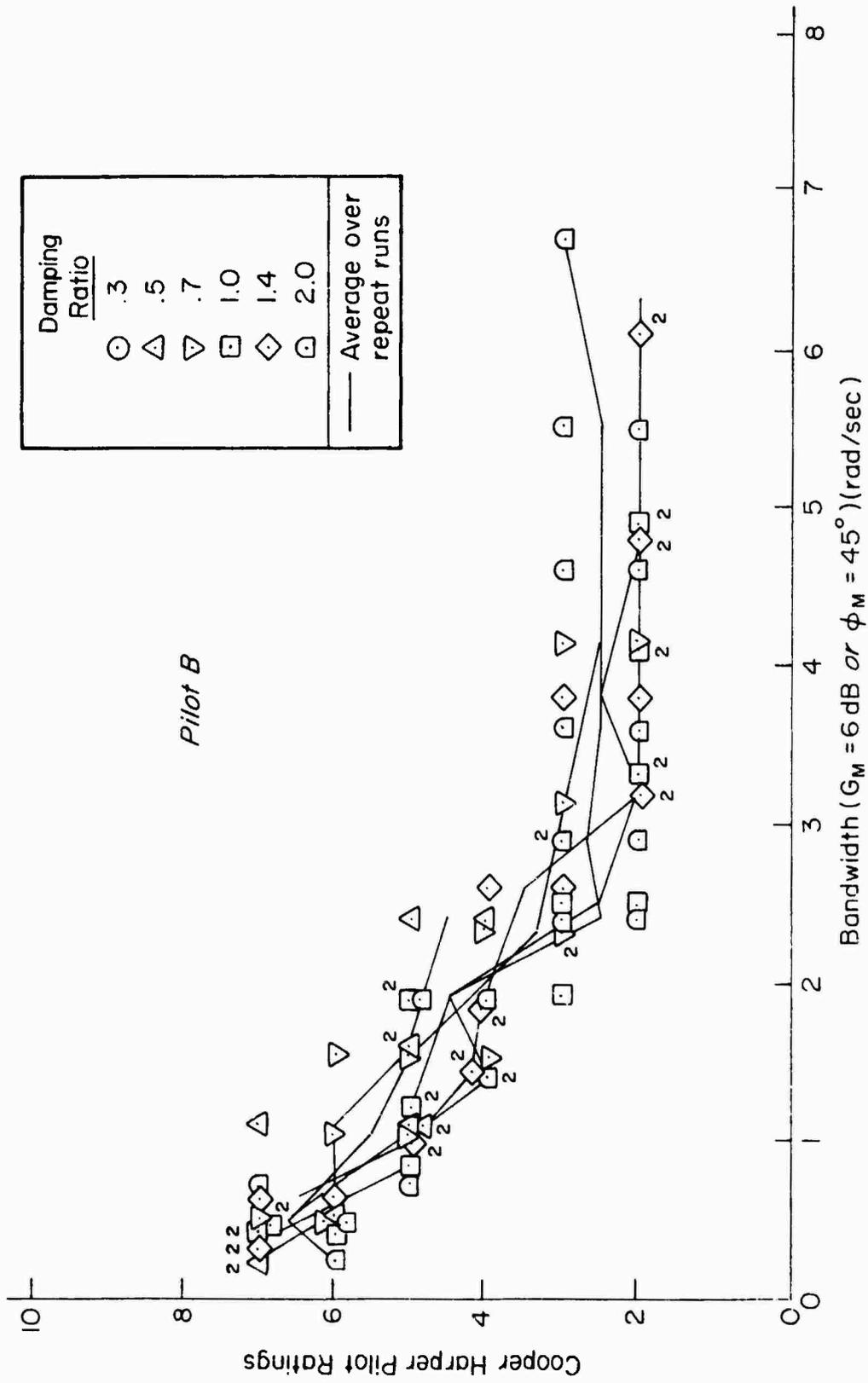


Figure 18. (Concluded)

SECTION IV
DESCRIPTION OF FLIGHT TEST

Because a systematic variation of parameters to define uncertain handling boundaries was well beyond the resources available, the scope of the flight test program was quite limited. The more practical approach actually taken was to validate, extend, modify, or disprove the bandwidth hypothesis based on the results obtained in the flight test program.

As noted earlier in the report, a tight tracking task was required to separate good and bad configurations, i.e., to force the pilot to maximum effort and thereby expose deficiencies which might not otherwise have been evident.

The primary task selected was air-to-air tracking. This task was ideal because the target motions could be tailored to exercise a broad spectrum of frequencies in the tracking aircraft. Formation flying, chosen as a secondary task, is one of the few tasks appropriate for the lateral translation (β_2) mode — a mode which we felt warranted testing because of its unique decoupled characteristics and its inherent bandwidth limitations (Y_v in Table 2). The primary mode selected was the wings-level turn (A_y mode), which has considerable potential for air-to-air and air-to-ground applications (see Ref. 18).

The approach taken to test the bandwidth hypothesis was to generate a series of configurations with adverse and proverse roll and yaw coupling in the wings-level turn mode. If the bandwidth hypothesis is valid, the pilot ratings should correlate with bandwidth regardless of the type of coupling. Based on this line of reasoning, the following configurations were developed:

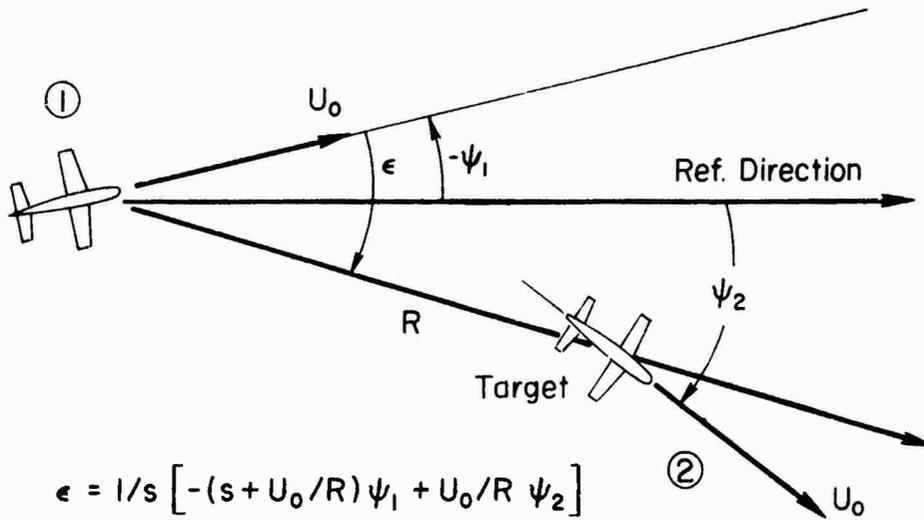
- 1) Wings-level turn with adverse and proverse yaw coupling designed to vary heading bandwidth from nearly zero to 7 rad/sec.
- 2) Wings-level turn configurations with adverse and proverse roll coupling, designed to give the same heading bandwidth as the configurations in Item 1.
- 3) Lateral translation configurations with low intrinsic bandwidth similar to the F-16's low Y_v (see Table 2).
- 4) A lateral translation configuration with proverse roll coupling designed to increase the bandwidth via favorable coupling.

If the bandwidth hypothesis is indeed valid, the configurations for Items 1 and 2 above with similar values of heading bandwidth should receive similar pilot ratings and commentary. Likewise, it should be possible to improve the lateral translation mode by inducing proverse roll coupling, inasmuch as this would increase the lateral displacement/DFC bandwidth, albeit via coupling instead of increasing Y_v .

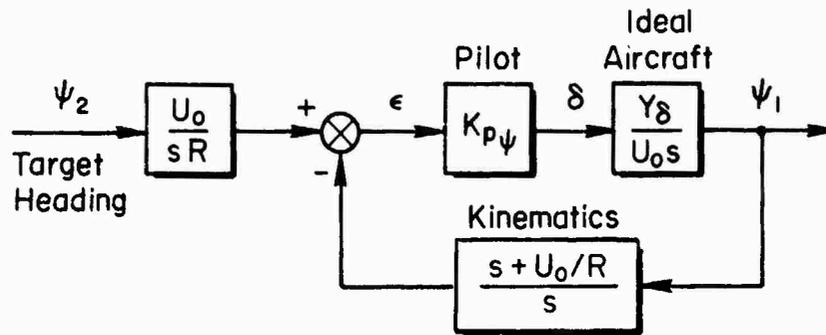
A. AIR-TO-AIR TRACKING TASK

The tracking kinematics for the ideal case (no inter-axis coupling or uncanceled aircraft modes) in the wings-level turn mode are summarized in Fig. 19. Figure 19a may be compared with Fig. 5, page 21. The block diagram in Fig. 19 indicates the interrelationships among the pilot, the idealized aircraft dynamics, the air-to-air tracking kinematics, and the target heading, ψ_2 . The tracking kinematics, which appear in the feedback transfer function of this block diagram, result in a numerator zero at U_0/R (aircraft speed/range). The effect of this zero on the piloted loop closure is shown in the root locus plot, Fig. 19c. The plot shows good closed-loop damping when U_0/R is small, that is, at large values of range; and low damping when U_0/R is large, i.e., short range. Physically this stems from the fact that ϵ is primarily set by heading when U_0/R is small, as in formation flying; whereas when U_0/R is large, ϵ is strongly affected by lateral displacement (which involves an additional integration).

a) Kinematics of Angular Aim Error



b) Dynamics of Pure Gain Piloted Control



c) Root Locus for Positive Value of $K_{p\psi} Y_{\delta} / U_0$

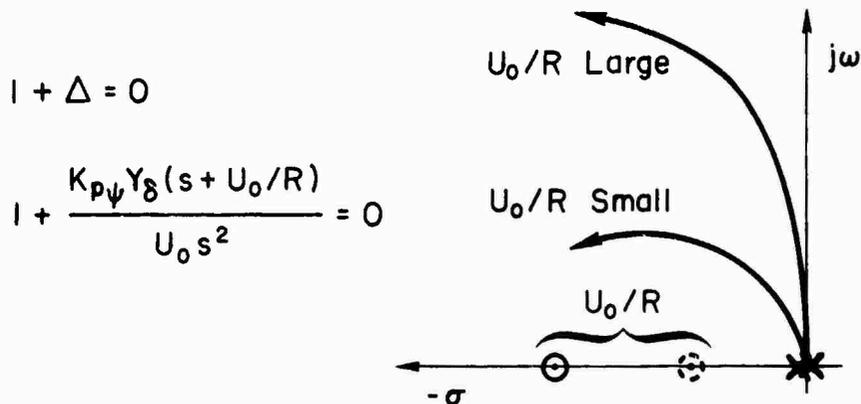
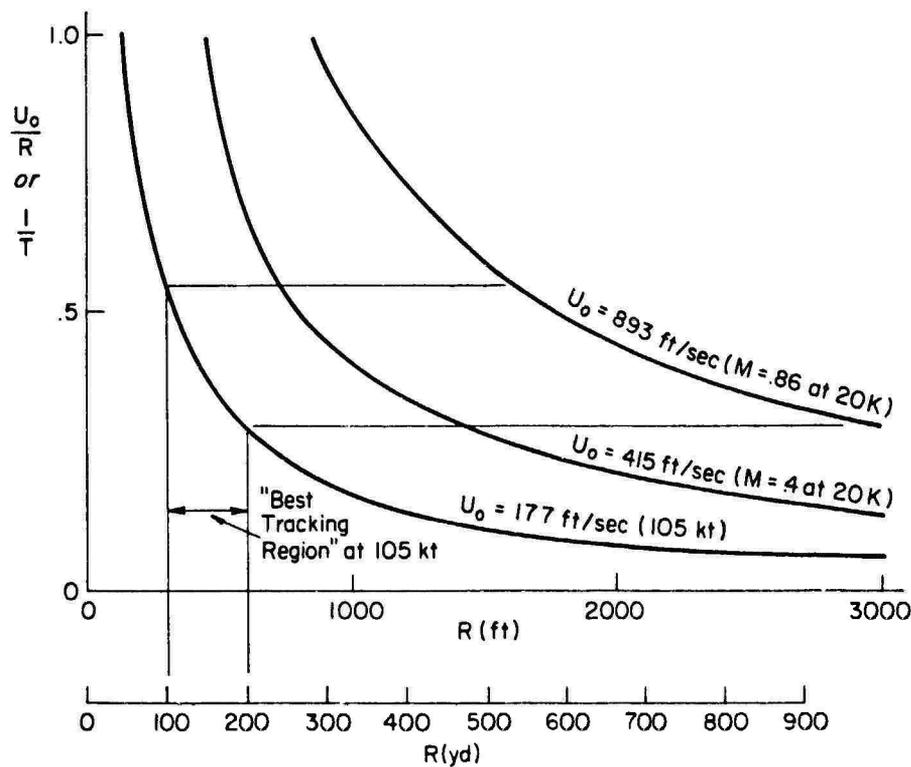


Figure 19. Tracking Kinematics and Dynamics for Wings-Level Turn

Due to structural limitations of the side force generators, the Princeton University Variable Response Research Aircraft (VRA) has a maximum maneuvering speed of 105 kt — well below typical air-to-air combat speeds. It was therefore necessary to adjust the range between the target and attacker in our experiment to make the parameter U_0/R consistent with a typical air combat encounter. The effect of range on U_0/R for our test conditions (105 kt TAS) is shown in Fig. 20. Here we can see that typical combat parameters of $M = 0.86$ at 20,000 ft and a range of 600–1200 yards converts to 100–200 yards at the VRA testing speed (105 kt). Shorter ranges (than 100–200 yards) result in relatively large values of U_0/R , which approach the piloted crossover region



R of 100-200 yards in Navion equals R of 600-1200 yards
in typical air-to-air encounter

Figure 20. Effect of Range on Tracking Kinematics

in the vicinity of 1 rad/sec. The effect of this on sight, ϵ , dynamics is shown in Fig. 21 for the baseline configuration (WLT1) used in the flight test experiment.* The frequency response phase plot in Fig. 21 indicates that for frequencies well below U_0/R the sighting error dynamics are very lightly damped, whereas for frequencies well above U_0/R the sighting error dynamics are equivalent to the heading dynamics. Hence, for values of range where U_0/R is well below the piloted crossover frequency, it is appropriate to use heading as the controlled variable when applying the bandwidth hypothesis. Tracking at close ranges where U_0/R is large enough to be near the region of piloted crossover (1 rad/sec) was found to be impractical during initial flight test evaluations because of the very light damping of the sighting error dynamics. The formal runs were conducted so that the safety pilot had control over range, which he maintained at a nominal 150 yards throughout the data runs. This was accomplished by using a series of concentric range circles painted on the aircraft windscreen and sized so that the target aircraft's wingspan would be coincident with the target circle at a range of 150 yards. The evaluation pilot's sight consisted of a reticle mounted inside the cockpit and a bead mounted on the cowl to define a sight line fixed with respect to the VRA airframe.

The primary disadvantage of testing at speeds well below $M = 0.8$ is that it is not possible to correctly simulate the 0.8 M aircraft dynamics and the pilot acceleration cues simultaneously. This may be seen from the equation for sensed lateral acceleration (Ref. 27, page 354):

$$a_{y_{cg}} = U_0(\dot{\beta} + r) - g\phi \quad (24)$$

If the $\dot{\beta}$ and r responses are correct, the lateral acceleration will be scaled down by the inertial speed U_0 . In the present experiment we elected to maintain the integrity of the sideslip and yaw rate responses

*The shorthand convention used for the numerical transfer function shown is first-order terms in parentheses, i.e., (1/T); second-order terms in brackets, i.e., [ζ , ω].

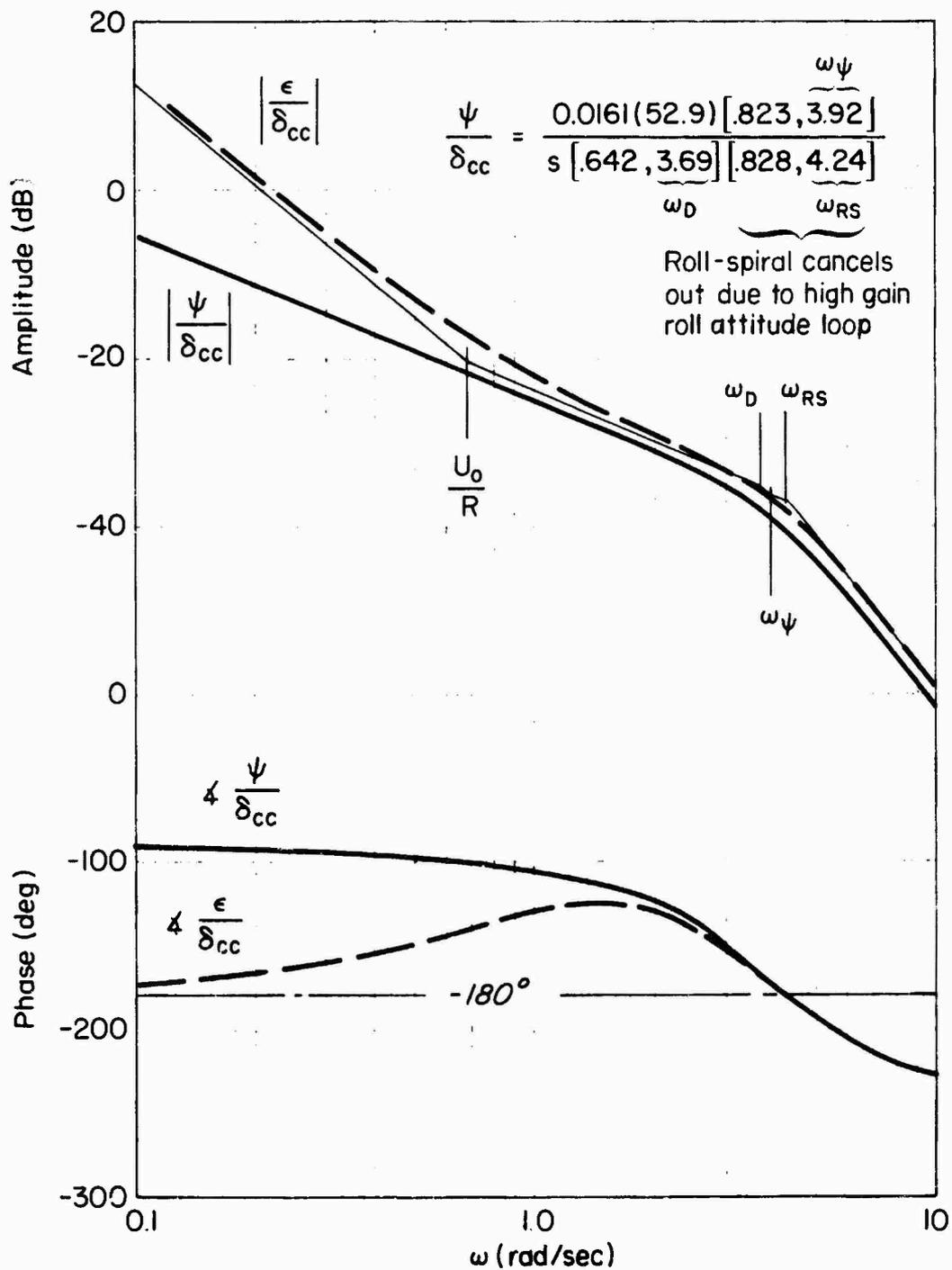


Figure 21. Comparison of Heading Dynamics and Piper Error Dynamics for Wings-Level Turn

at the expense of side acceleration cues, which were about a factor of 5 less than those corresponding to $M = 0.80$. This was done in accordance with the notion that visual cues are more dominant than acceleration cues in air-to-air tracking, and with the VRA's maximum lateral acceleration (0.5 g) capacity. Lateral accelerations as high as 0.5 g were utilized frequently during the experiment. This would translate to about 2.5 g at $M = 0.8$. There is a requirement for additional work to determine: 1) if 2.5 g lateral a_y is reasonable with any kind of practical restraint; and 2) the effect of reduced authority on pilot opinion. An informal discussion with an Air Force pilot who flew the YF-16 evaluation up to $a_y = 0.9$ g indicated that large a_y might be acceptable if the pilot could be appropriately restrained. Also, McAllister noted (Ref. 26) that a 1 g command was acceptable, but a 1 g failure transient was objectionable.

The air-to-air tracking scenario was developed to maximize the probability of exposing deficiencies in the tracking aircraft. This exposure was obtained by tracking a target aircraft whose heading (ψ_2 in Fig. 19) varied in a random-appearing fashion corresponding to a power spectrum concentrated in, but evenly spaced over, the frequency range of interest. I. A. M. Hall developed such a signal in Ref. 37 for the purpose of identifying the frequency response characteristics of aircraft in flight. The signal developed in Ref. 37 is shown in Fig. 22. The frequency content of this input signal as given in Ref. 37 is shown in Fig. 23. This signal was selected because it has adequate power at and above the roll mode time constant of most fighter aircraft. The square wave signal was introduced as a hardover signal into the target aircraft lateral autopilot servo via a left/right command switch controlled by the target aircraft pilot. This signal resulted in approximately three-quarters of full aileron travel at the testing speed of 105 kt, resulting in roll rates of approximately 30 deg/sec. The pilot of the target aircraft selected left and right signals via the schedule in Fig. 22 where the numbers are the length of time in seconds that the switch was held in the left or right position. This was accomplished by taping the sequence as audible right/left commands and playing it back to the target pilot during each run. The target aircraft was maintained at

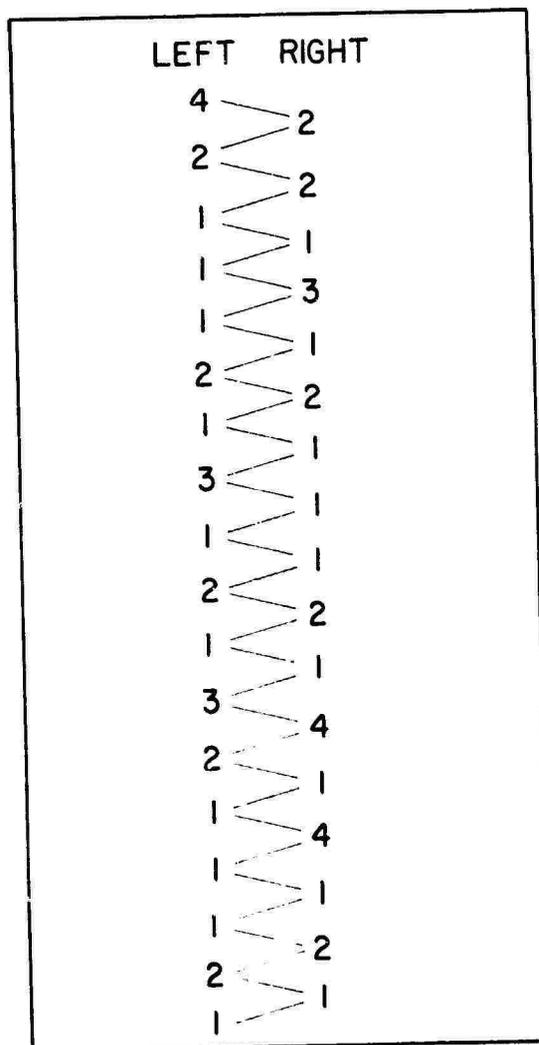


Figure 22. Typical Pilot Switching Program (from Ref. 34)

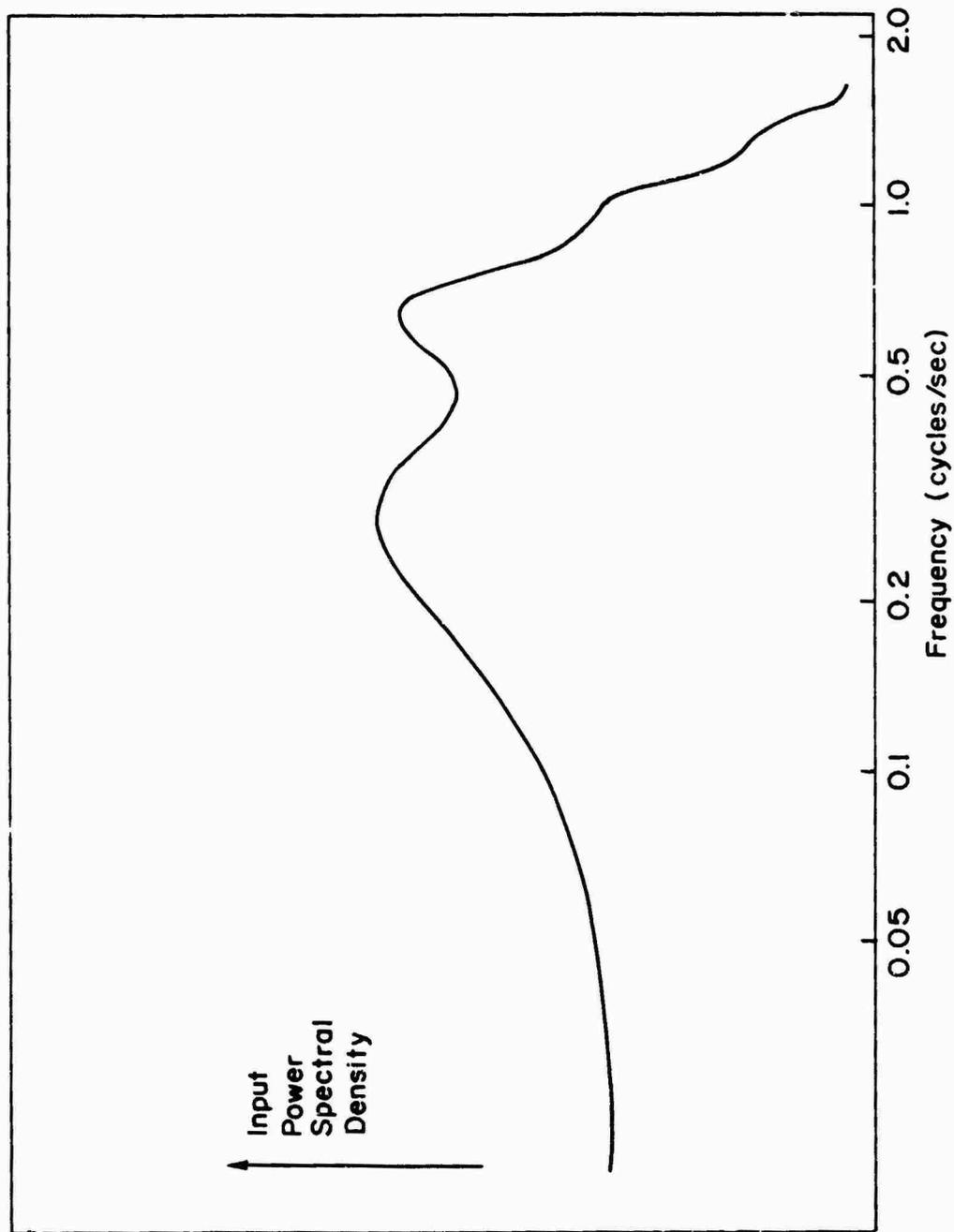


Figure 23. Frequency Content of Input Used for Target Aircraft
(From Ref. 37)

constant altitude during each data run, which was conducted in relatively smooth air. While we intended to utilize variations in the input series, such as playing it backwards or from the middle to the ends, etc., the evaluation pilots felt that the task remained unlearned and random without such variations, which therefore were not utilized.

B. DEVELOPMENT OF TEST CONFIGURATIONS

The Princeton University Variable Response Research Aircraft (VRA) has a fly-by-wire response-feedback system utilizing hydraulically actuated controls. These controls include flaps which move up as well as down, and side force generators as pictured in Fig. 24. A block diagram of the VRA as mechanized for the in-flight simulation in this program is shown in Fig. 25. The C_B and C_F matrices in Fig. 25 were calculated to allow the VRA to respond like the YF-16 at a flight condition of $M = 0.8$ at 20,000 ft. This mechanization was somewhat less than straightforward because the YF-16 utilizes equalization in the feedbacks, whereas the VRA is for all practical purposes a pure gain feedback mechanization. However, it was felt that the additional effort was warranted to allow comparison of the flight results with results from the F-16 CCV simulation scheduled to run concurrently on the LAMARS simulator at the Flight

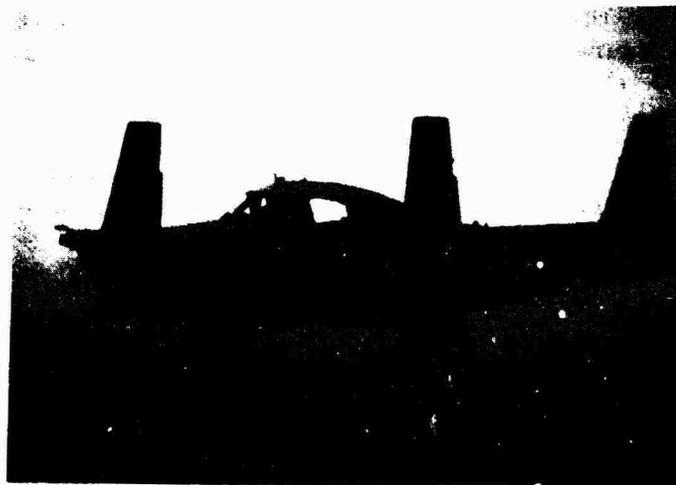


Figure 24. Photo of VRA in Flight

Dynamics Laboratory, Wright-Patterson AFB. The details of how the VRA mechanization was accomplished are given in Appendix B. The generic variation of roll and yaw coupling in the flight test experiment was achieved via the aileron and rudder crossfeed boxes in Fig. 25. The steps taken to generate the configurations are summarized as follows:

- 1) An initial flight test was performed in which the aileron and rudder crossfeed gains were varied and initial pilot ratings taken to determine the practical range of interest, i.e., the magnitude of coupling which approximately defined Levels 1, 2, and 3 flying qualities.
- 2) Values of aileron and rudder crossfeed gain within the practical range of interest determined in Step 1 were mechanized on the analog computer to determine the corresponding magnitude and shape of the time responses. An example of these responses is shown in Fig. 26.
- 3) Using the analog computer responses as a guide, a number of configurations were developed which entailed systematic variation of roll and yaw coupling. The aileron and rudder crossfeed gains for each configuration were then entered into a computer program (TRFN, from the STI library) which generated the frequency response plots for heading and lateral sighting error to DFC control input. The bandwidth for each configuration was calculated based on the rule in Section III, i.e., 45 deg phase or 6 dB gain margin, whichever gives the lower frequency. The bandwidths for configurations with roll coupling were compared with the bandwidths for configurations with yaw coupling to determine if a sufficient number of configurations had approximately equal bandwidth to allow testing of the bandwidth hypothesis. New configurations were generated wherever necessary.

C. IN-FLIGHT VEHICLE IDENTIFICATION

A primary problem with much of the DFC data generated to date is that the actual controlled element tested was not quantitatively defined. In order to avoid any uncertainties in defining the controlled element for each configuration tested in this experiment it was decided to run a frequency sweep for the DFC input; the input and the resulting

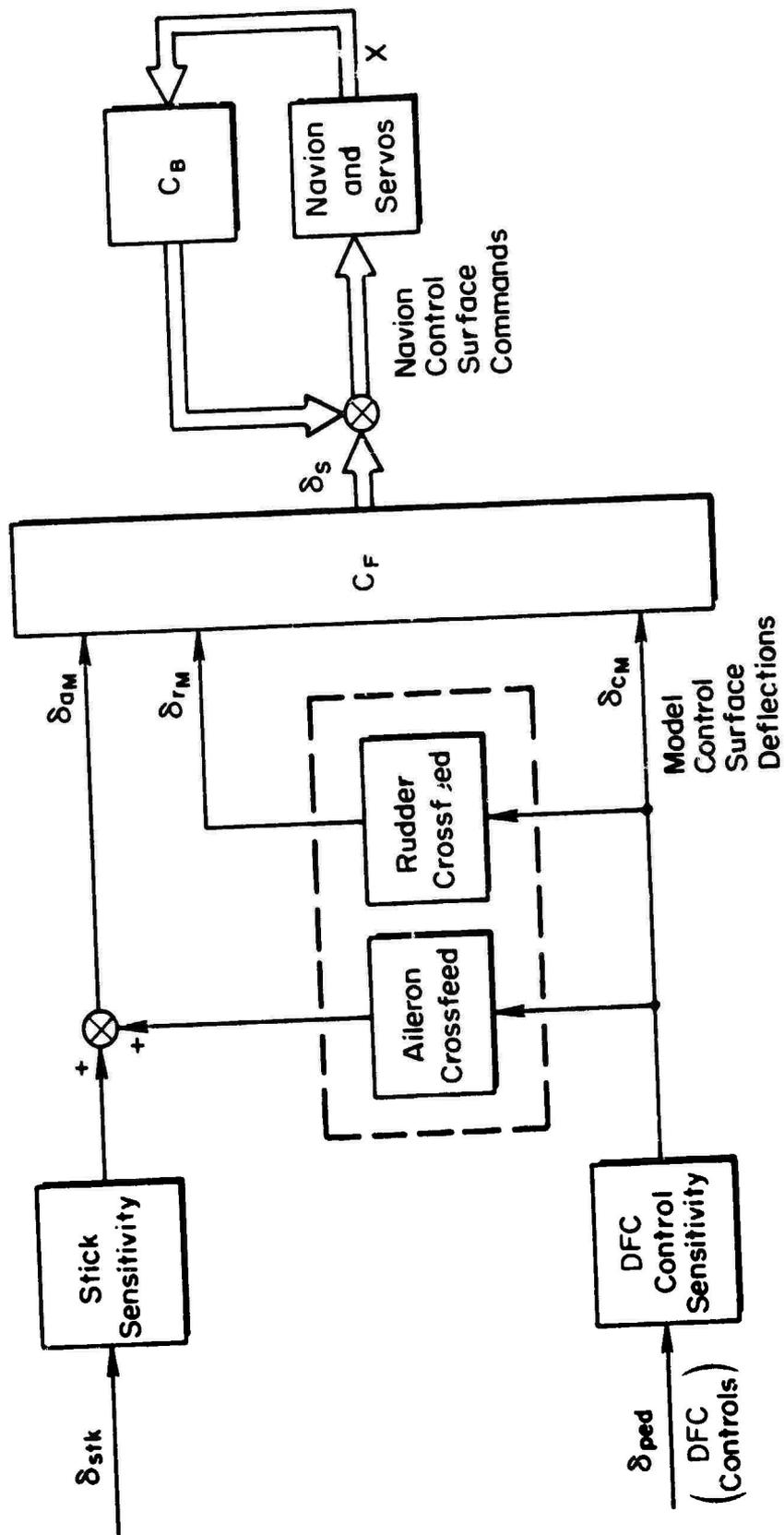
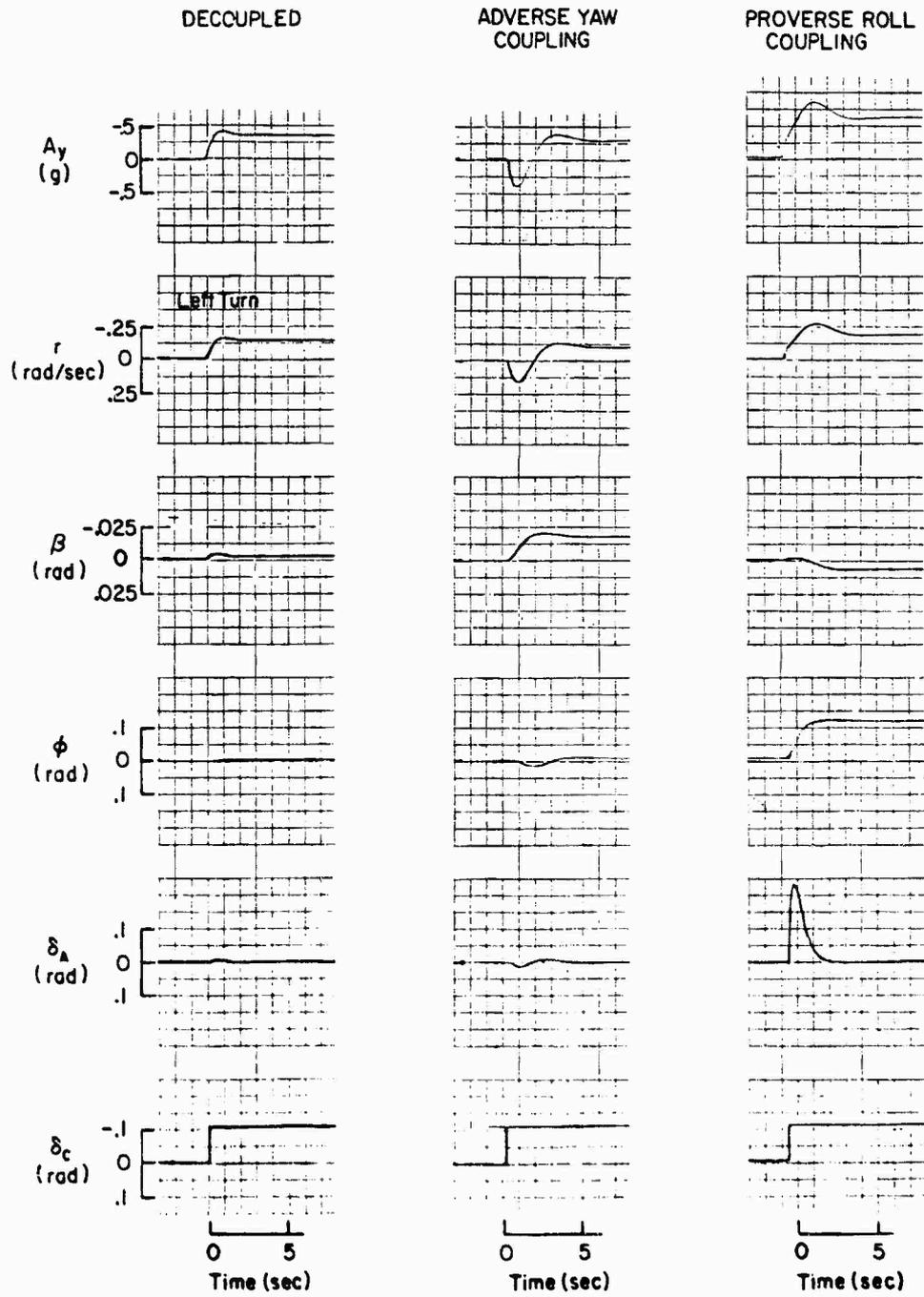


Figure 25. VRA as Mechanized for Simulation



**Figure 26. Response to Step Input of DFC Control:
Wings-Level Turn Mode**

aircraft heading could then be fast-Fourier-transformed (FFT) to obtain the frequency response directly. This technique had the additional advantage of determining whether or not it is practical to formulate flying qualities criteria in terms of frequency response characteristics. The method for generating the frequency sweep was extremely simple, the pilot simply exercising the DFC control (rudder pedals) at ever-increasing frequency during a single run. Rudder pedal input and output yaw rate were recorded and FFTd with excellent results, i.e., very little data scatter in the frequency range of interest. An example of a pilot-generated input and the resulting yaw rate is given in Fig. 27. The Fourier transformed responses obtained from similar inputs is given for each of the wings-level-turn configurations in Figs. 28a through 28k, for the lateral translation modes in Figs. 29a and b. The bandwidths of these configurations are summarized in Table 7. If the bandwidth hypothesis is valid we would expect similar pilot commentary and ratings for WLT3 and 14 and for 4, 13, and 15—the unfavorable coupling cases. Favorable coupling was tested by comparison of WLT5 with WLT10. Configurations WLT 6, 7, 8, and 9 were dropped from the test matrix during the evaluation.

TABLE 7

SUMMARY OF TEST CONFIGURATIONS

CONFIGURATION	COUPLING	MEASURED BANDWIDTH ^a (rad/sec)
WLT1	Minimal	1.25 G
WLT2	Favorable yaw	1.30 G
WLT3	Unfavorable yaw	1.10 G
WLT4	Unfavorable yaw	0.80 G
WLT5	Favorable yaw	4.10 P
WLT10	Favorable roll	6.0 P
WLT11	Unfavorable roll	0.43 G
WLT12	Favorable roll	1.75 G
WLT13	Unfavorable roll	0.70 G
WLT14	Unfavorable roll	1.15 G
WLT15	Unfavorable yaw	0.79 G
LT1	Minimal	1.50 G
LT1Y	Favorable yaw	4.0 P

^aG = Gain margin of 6 dB;

P = Phase margin of 45 deg.

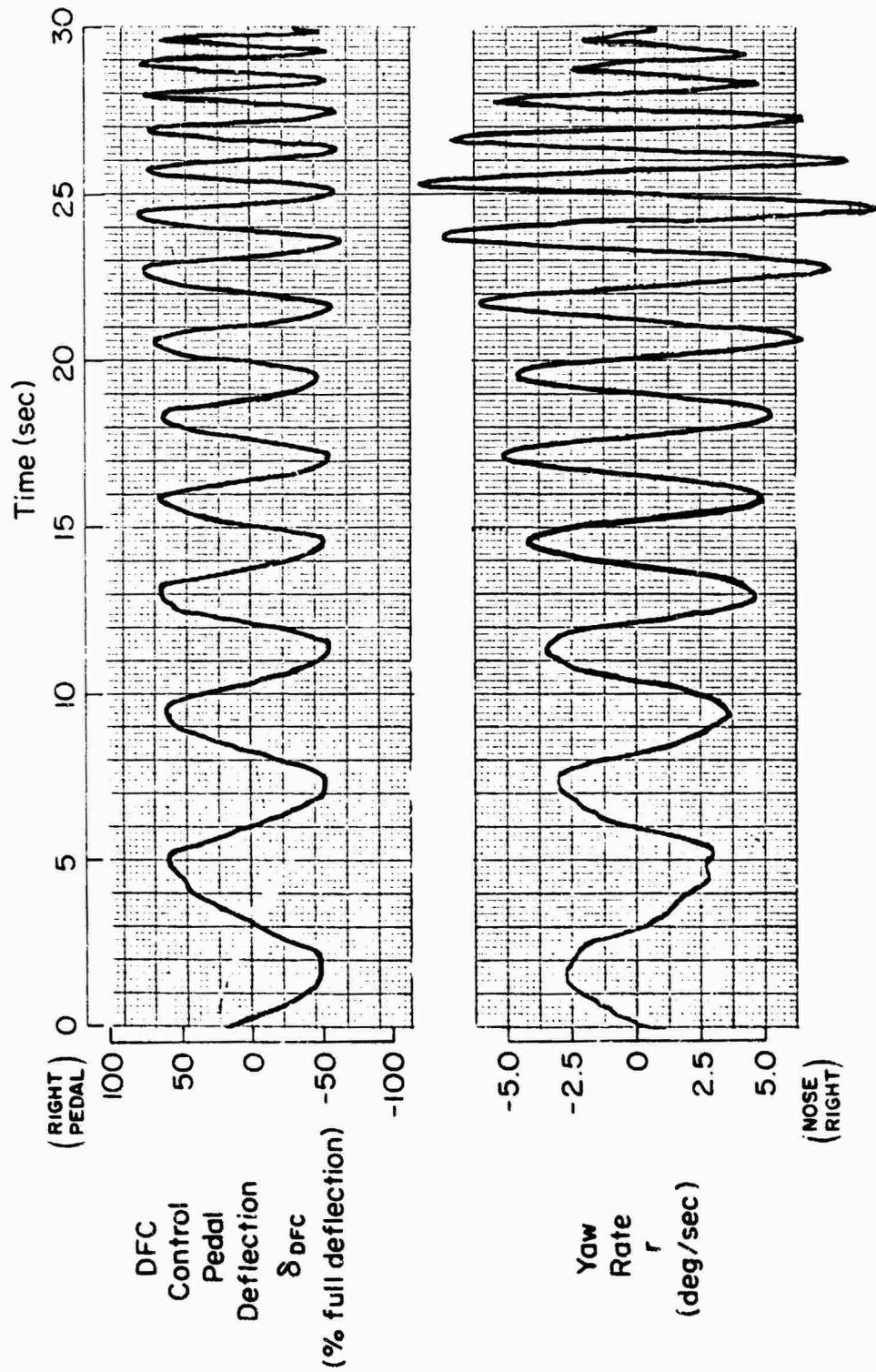
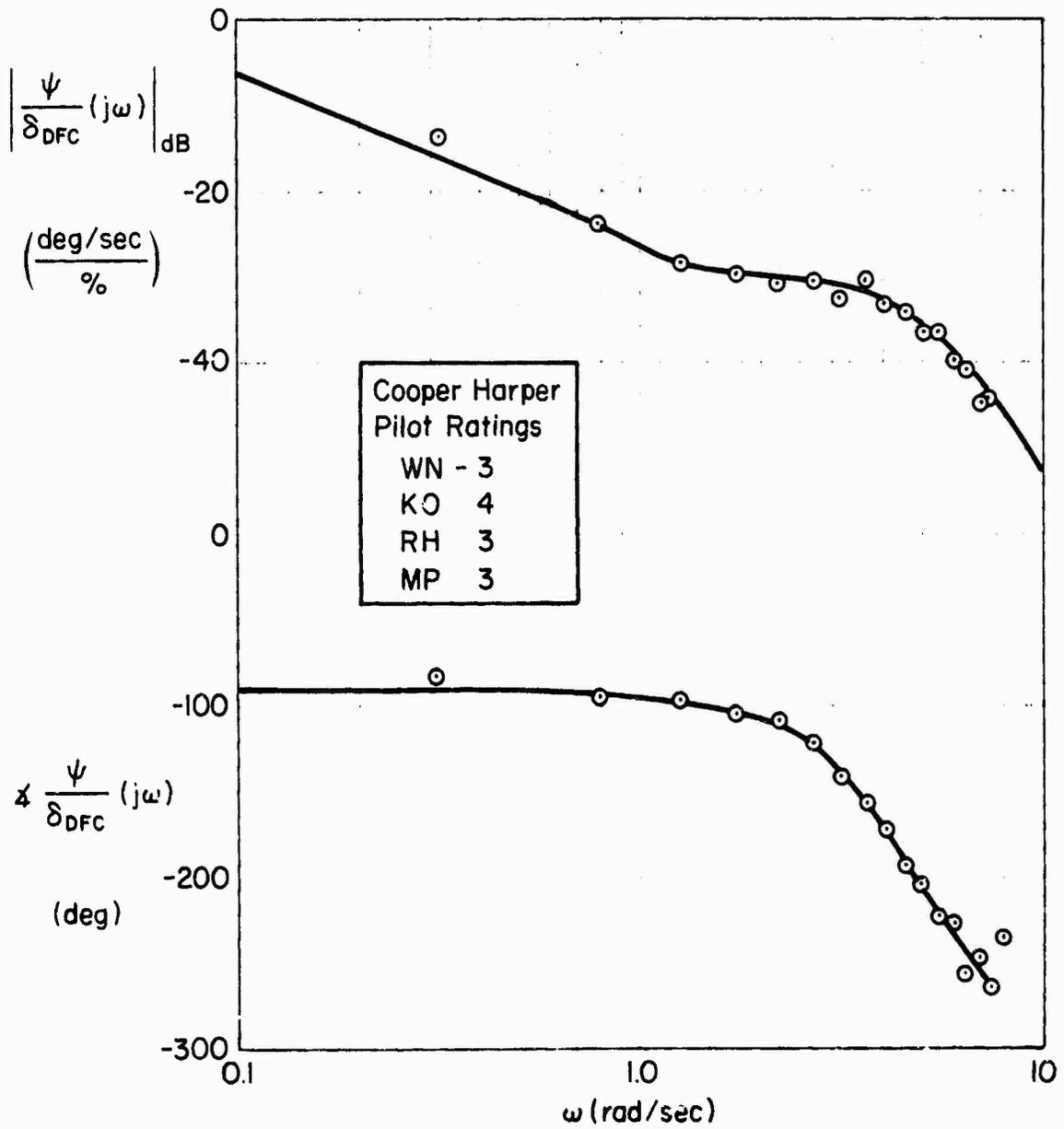
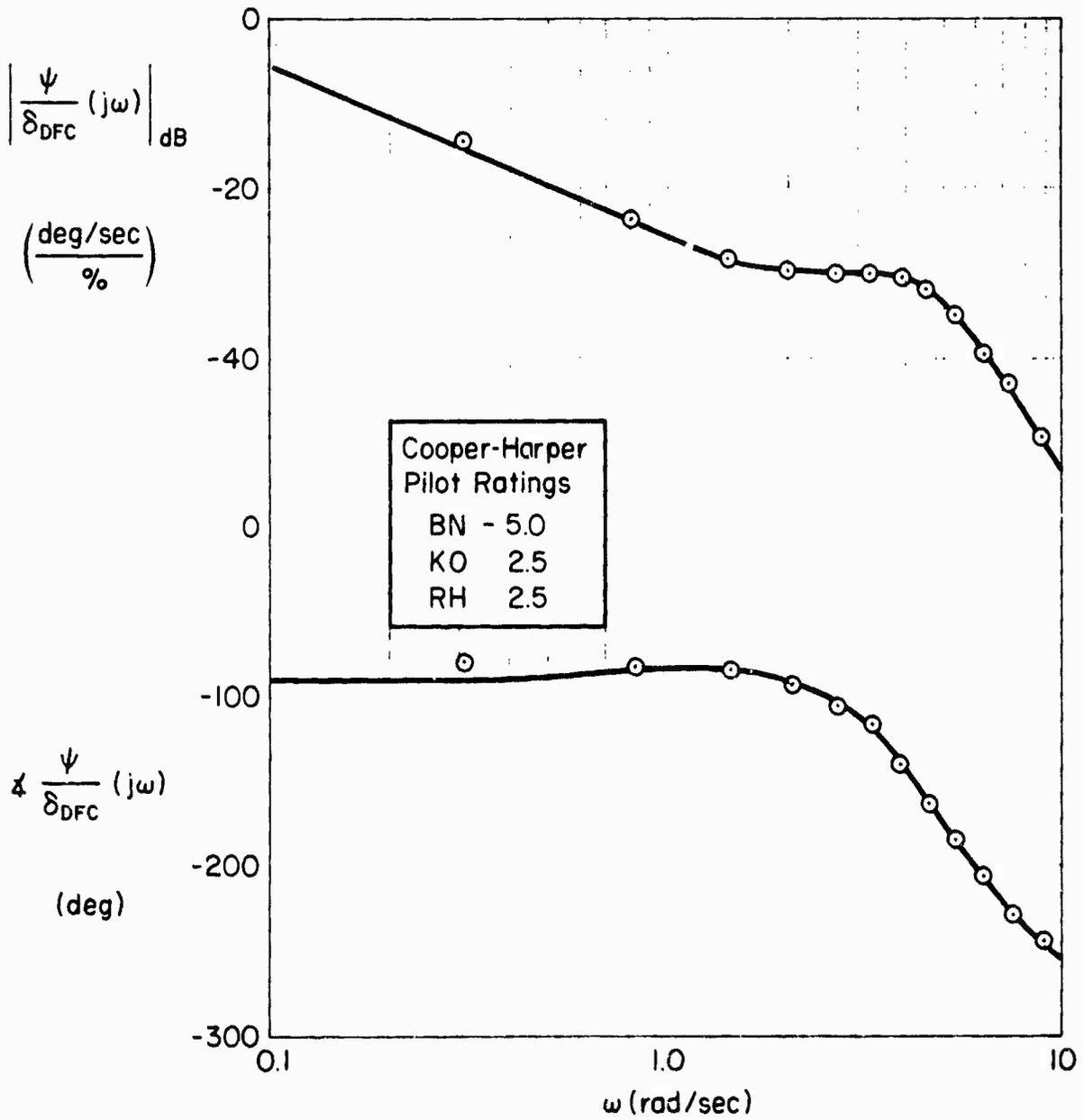


Figure 27. Typical DFC Control "Frequency Sweep" and Response for Configuration Identification (Configuration WLT 2, Run No. 5, Favorable Yaw Coupling)



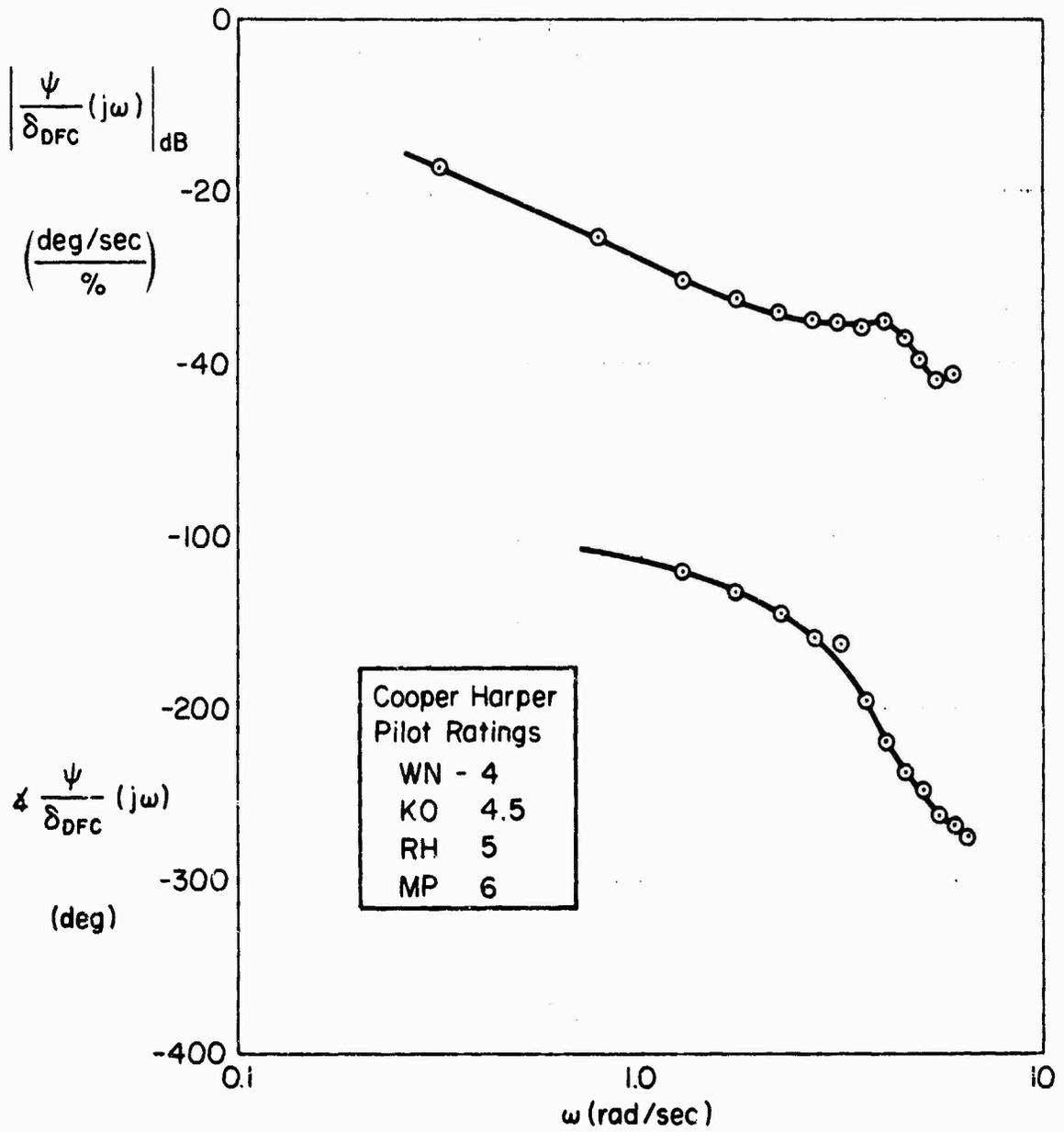
a) Configuration WLT1 (Minimal Coupling)

Figure 28. Fourier Transformed Heading Response



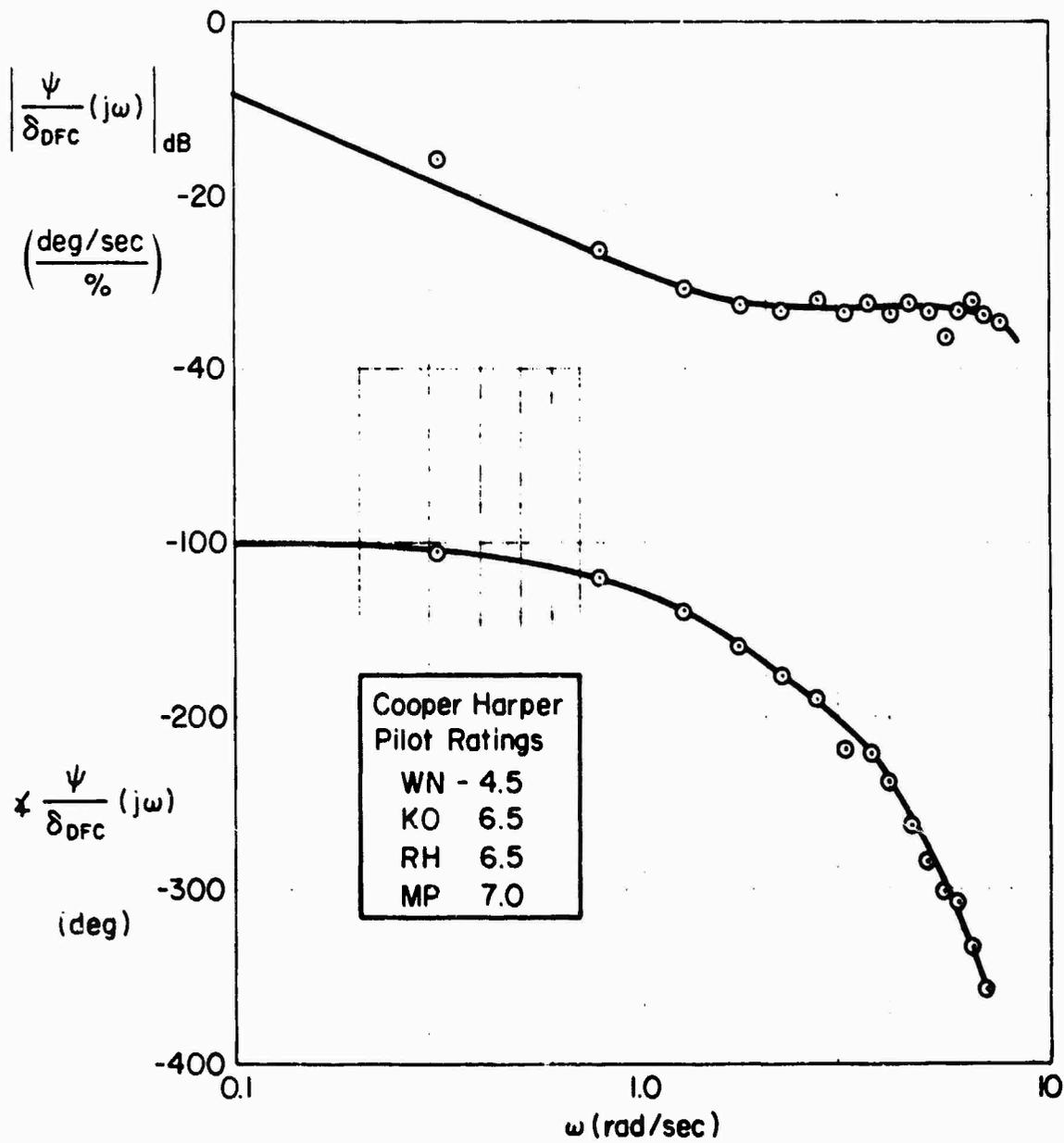
b) Configuration WLT2 (Favorable Yaw Coupling)

Figure 28. (Continued)



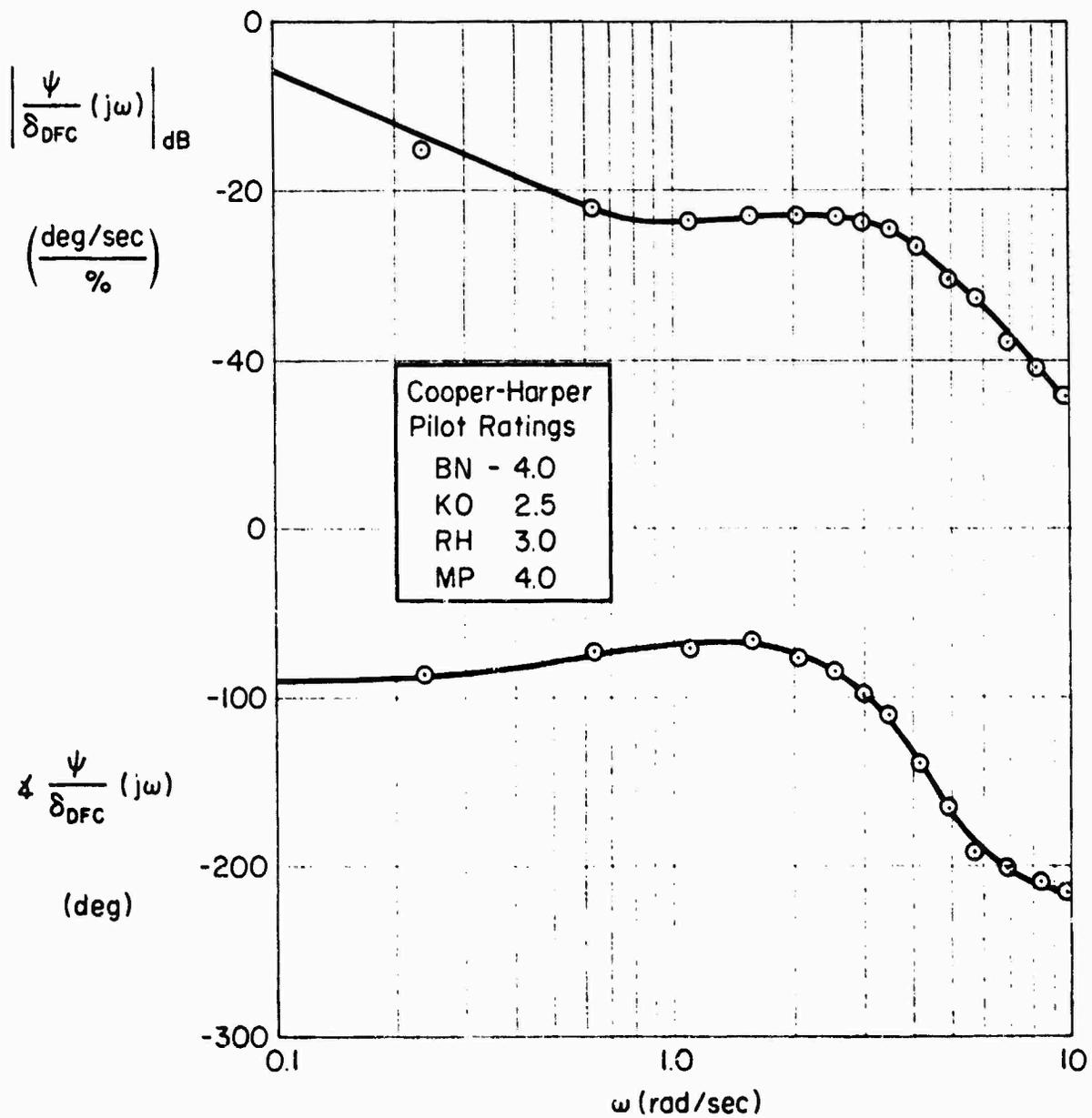
c) Configuration WLT3 (Unfavorable Yaw Coupling)

Figure 28. (Continued)



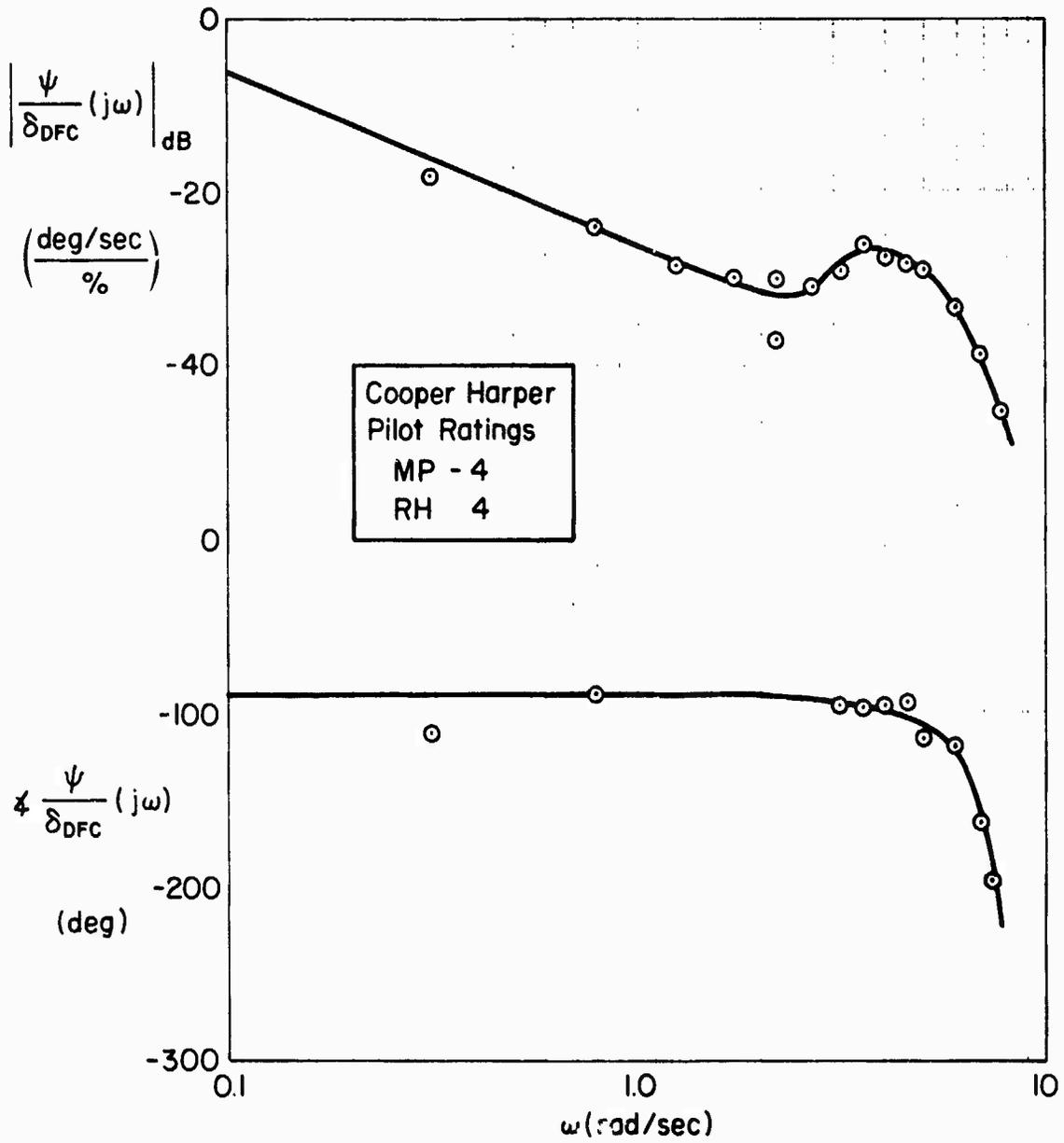
d) Configuration WLT4 (Unfavorable Yaw Coupling)

Figure 28. (Continued)



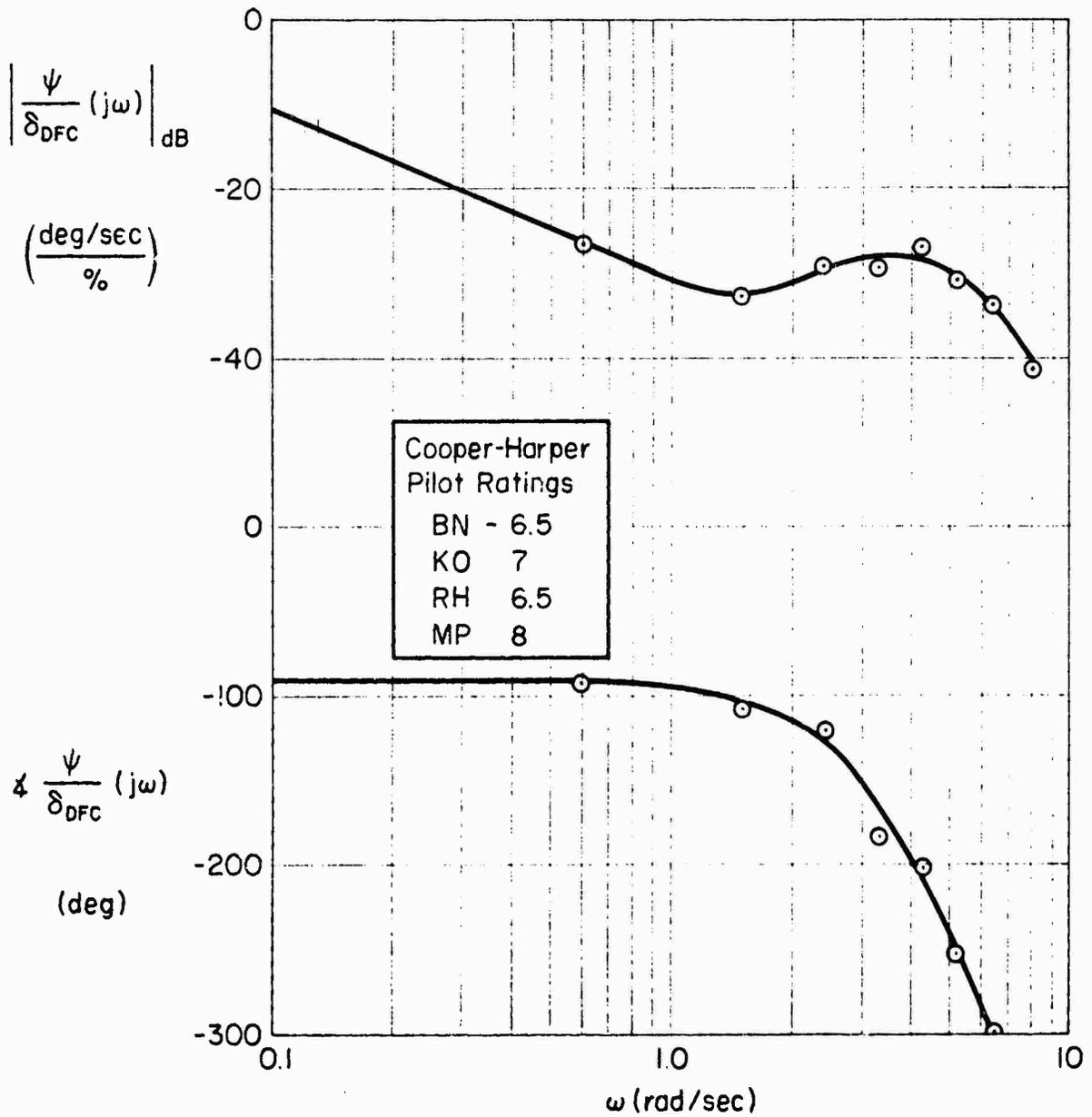
e) Configuration WLT5 (Favorable Yaw Coupling)

Figure 28. (Continued)



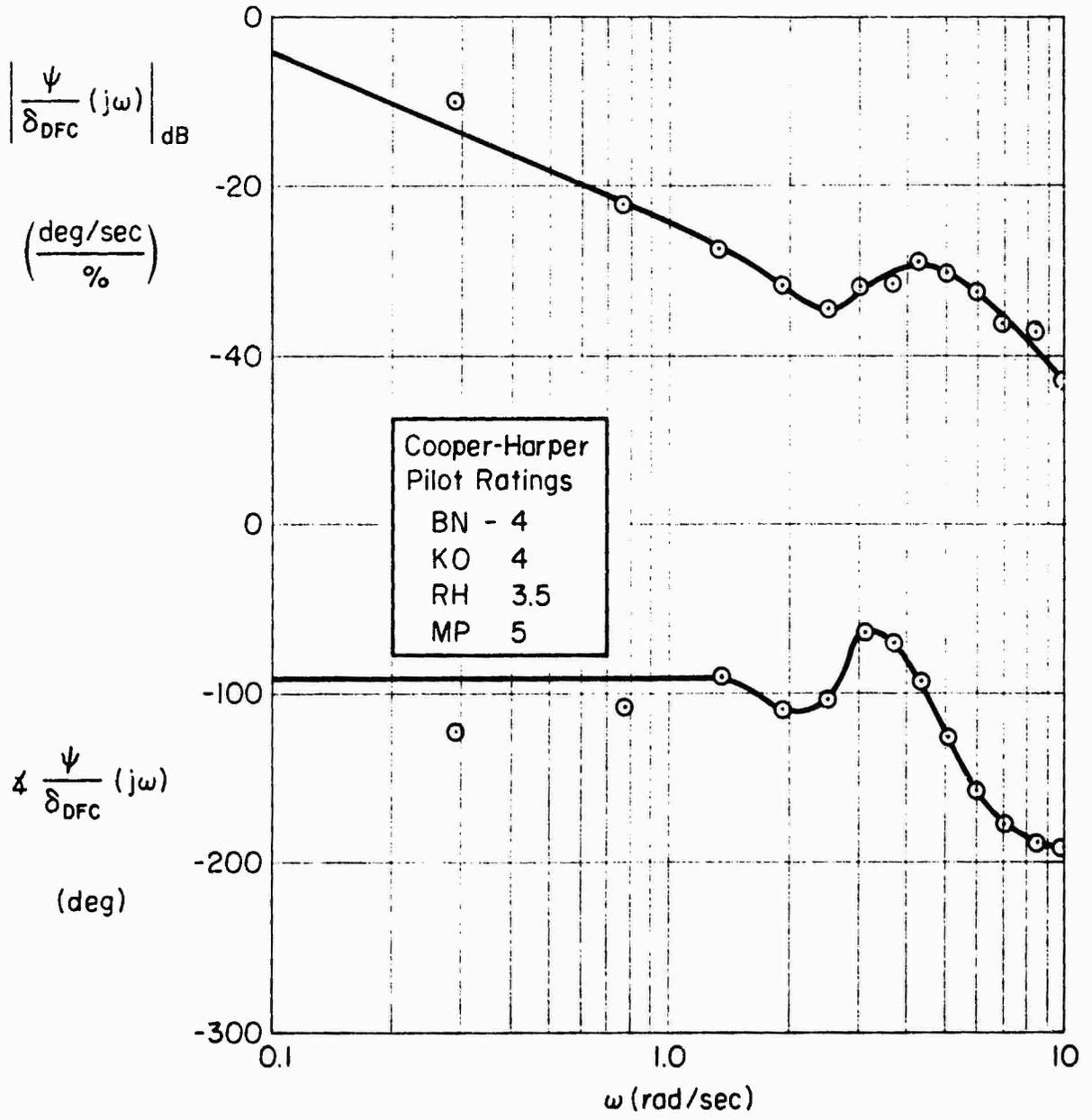
f) Configuration WLT10 (Favorable Roll Coupling)

Figure 28. (Continued)



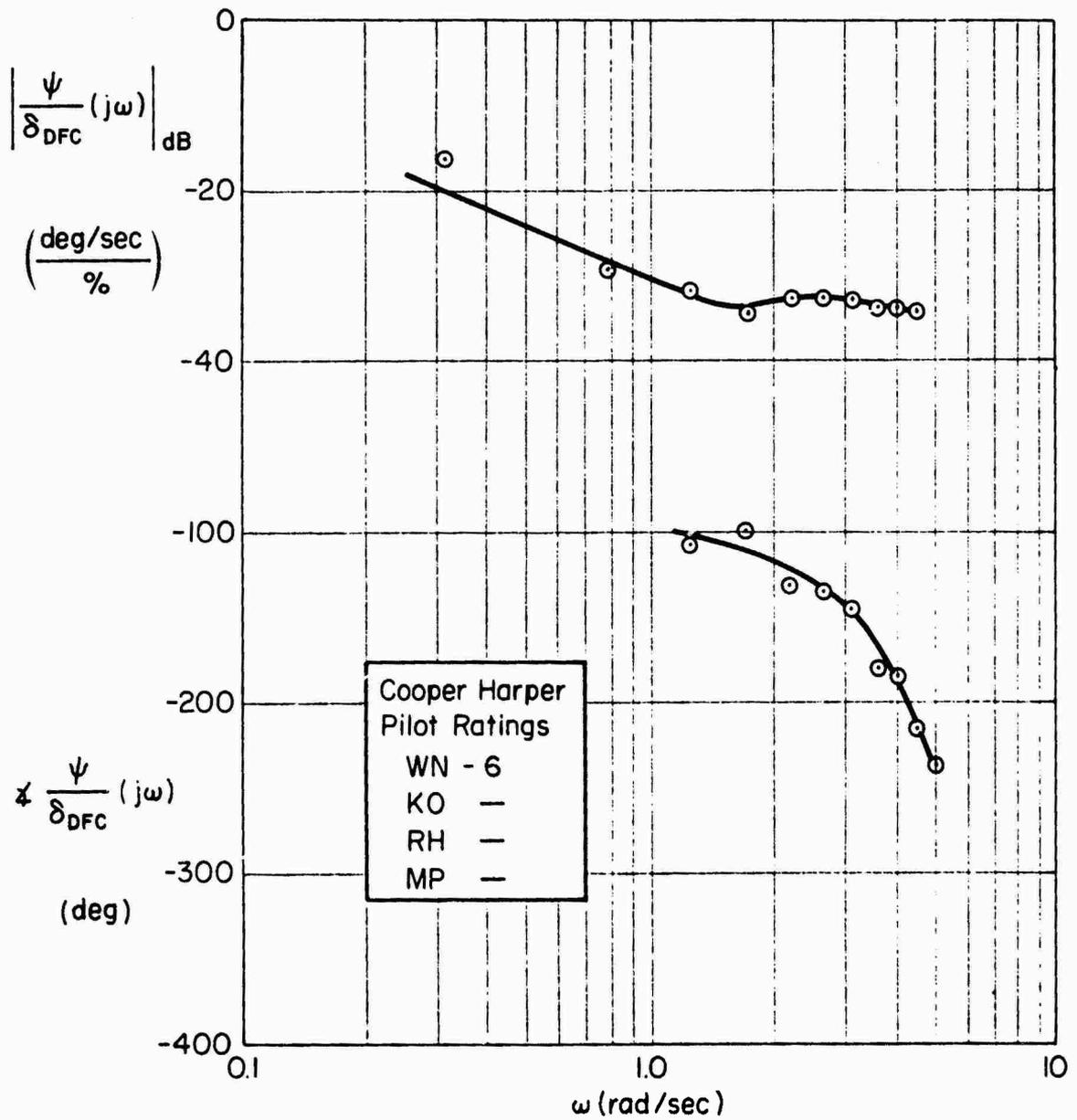
g) Configuration WLT11 (Unfavorable Roll Coupling)

Figure 28. (Continued)



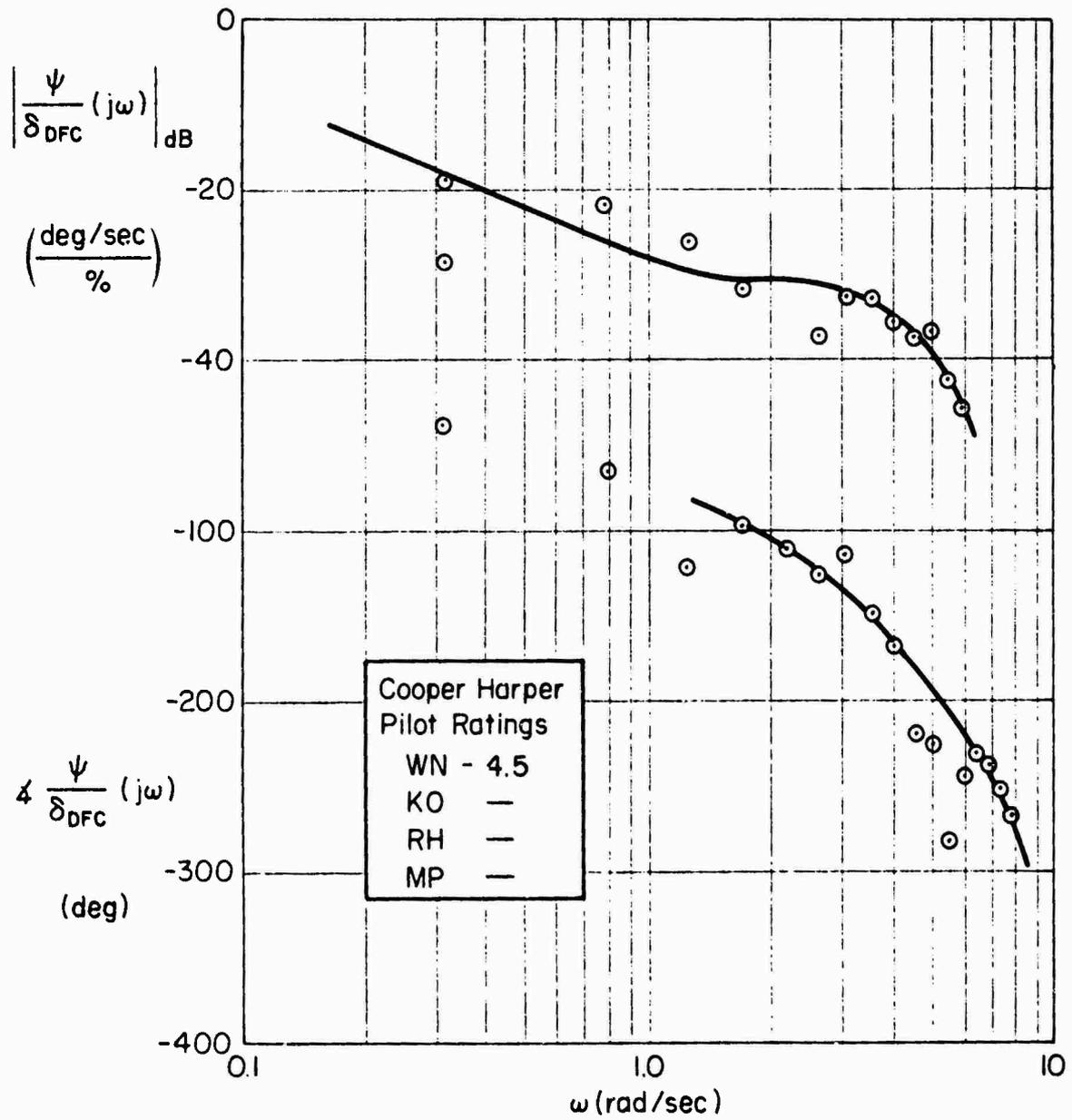
h) Configuration WLT12 (Favorable Roll Coupling)

Figure 28. (Continued)



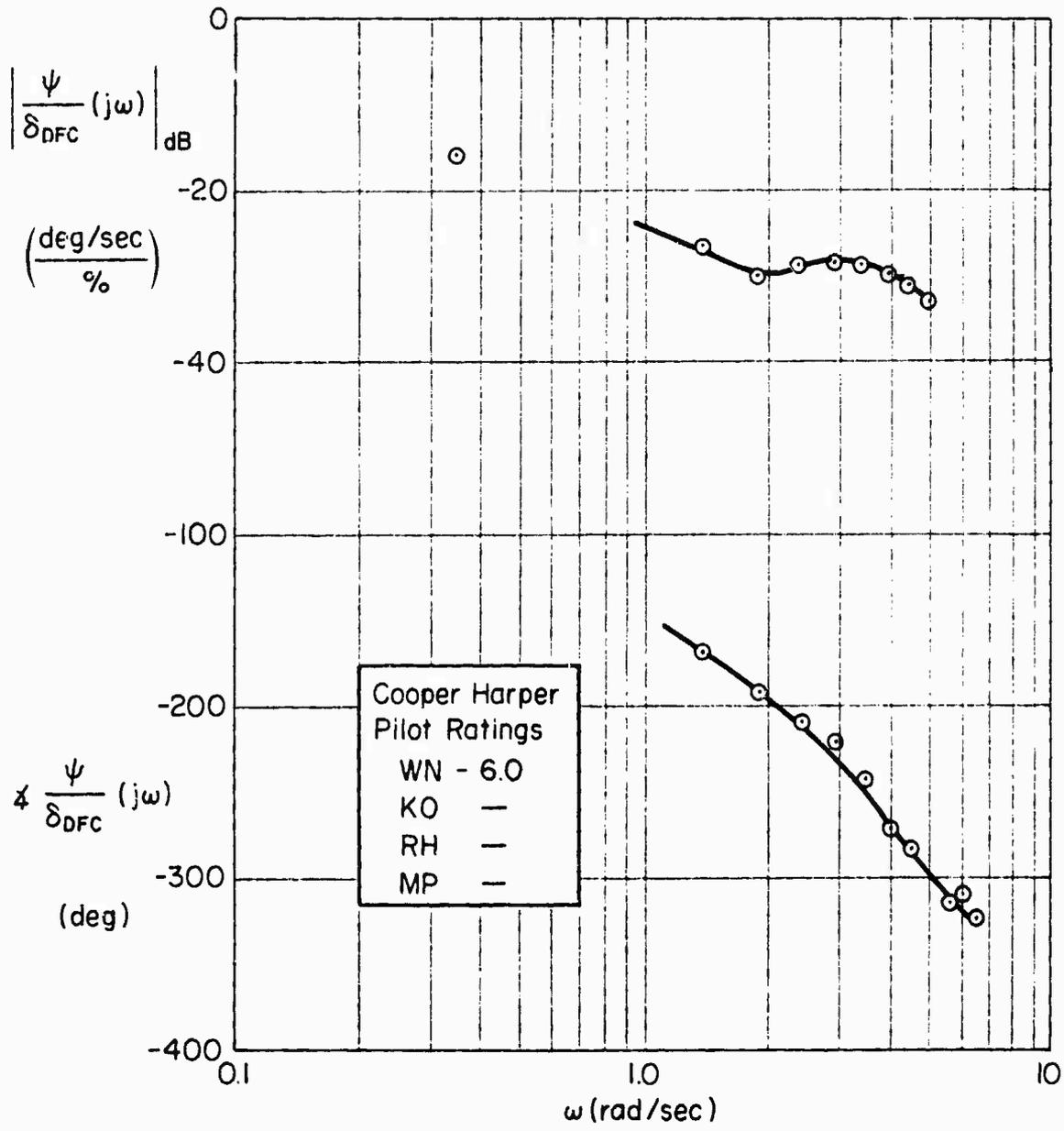
1) Configuration WLT13 (Unfavorable Roll Coupling)

Figure 28. (Continued)



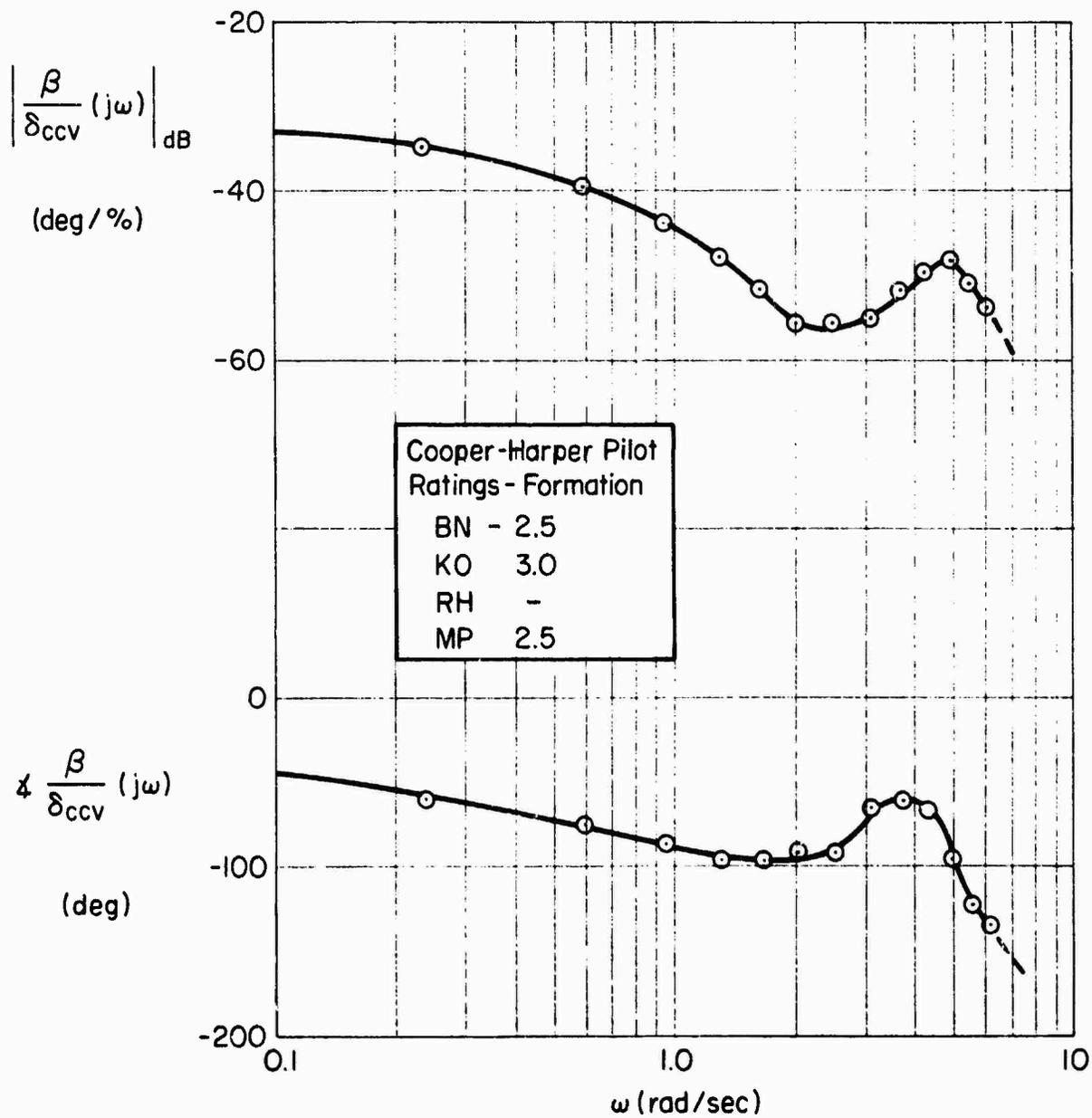
j) Configuration WLT14 (Unfavorable Roll Coupling)

Figure 28. (Continued)



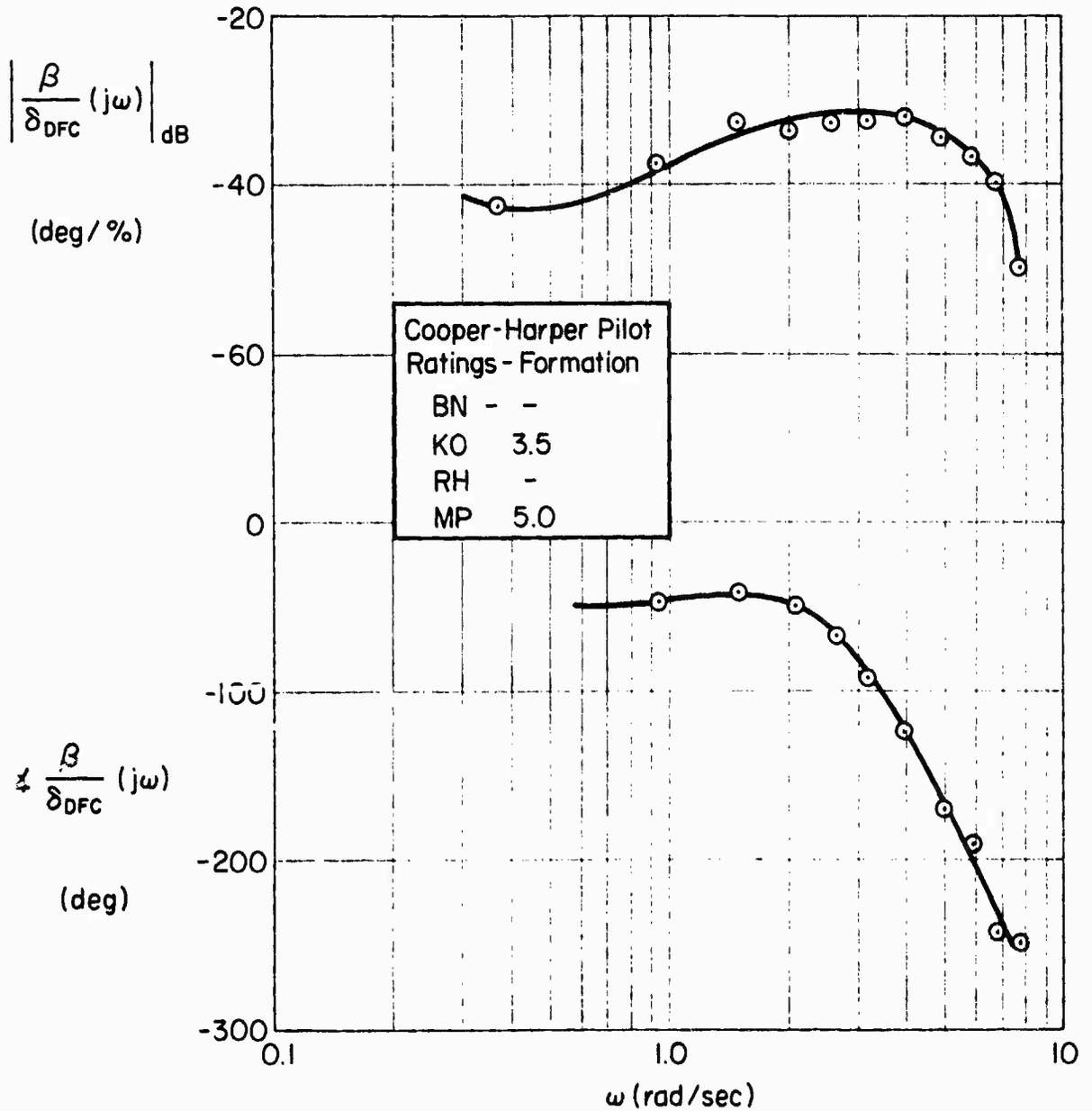
k) Configuration WLT15 (Unfavorable Yaw Coupling)

Figure 28. (Concluded)



a) Configuration LTI (Decoupled Lateral Translation Mode)

Figure 29. Fourier Transformed Sideslip Angle Response



b) Configuration LTLY (Lateral Translation Mode with Unfavorable Yaw Coupling)

Figure 29. (Concluded)

SECTION V

FLIGHT TEST RESULTS

As evident from the foregoing, most of the rather limited flight test program, conducted to test the bandwidth hypothesis, was accomplished using the wings-level turn (WLT) as a representative mode of control. The selected task was air-to-air tracking with the target aircraft maneuvering through a random series of bank angle reversals. It should be emphasized that the limited scope of the flight test program did not allow taking data to establish bandwidth boundaries for a wide variety of DFC configurations. The primary objective was simply to establish whether bandwidth is indeed the appropriate handling qualities parameter to separate satisfactory, acceptable, and unacceptable flying qualities for DFC modes. The wings-level turn maneuver was picked to test the hypothesis because it showed considerable potential operational utility in the YF-16 flight tests (Ref. 38).

The Cooper-Harper pilot ratings are plotted versus heading bandwidth in Fig. 30 for the air-to-air tracking task using the wings-level turn mode. Pilot commentary was taped in flight after each evaluation, and is presented in Appendix C. The open symbols in Fig. 30 indicate that variations in heading bandwidth were achieved via yaw coupling. That is, the crossfeed gain from DFC control (pedal) to the rudder was increased above its nominal value to achieve favorable yaw coupling and reduced below its nominal value to achieve unfavorable yaw coupling. (Refer to Section II-C for a discussion of the generic effects of such variations.) The closed symbols in Fig. 30 indicate that the heading bandwidth was varied via changes in roll coupling, i.e., the DFC control to aileron gain. To the pilot, favorable yaw coupling appears as a tendency for the nose to move in the direction of the commanded turn, whereas unfavorable yaw coupling appears as a tendency for the nose initially to swing away from the commanded turn. When flying a configuration with favorable roll coupling, the pilot will observe a tendency for

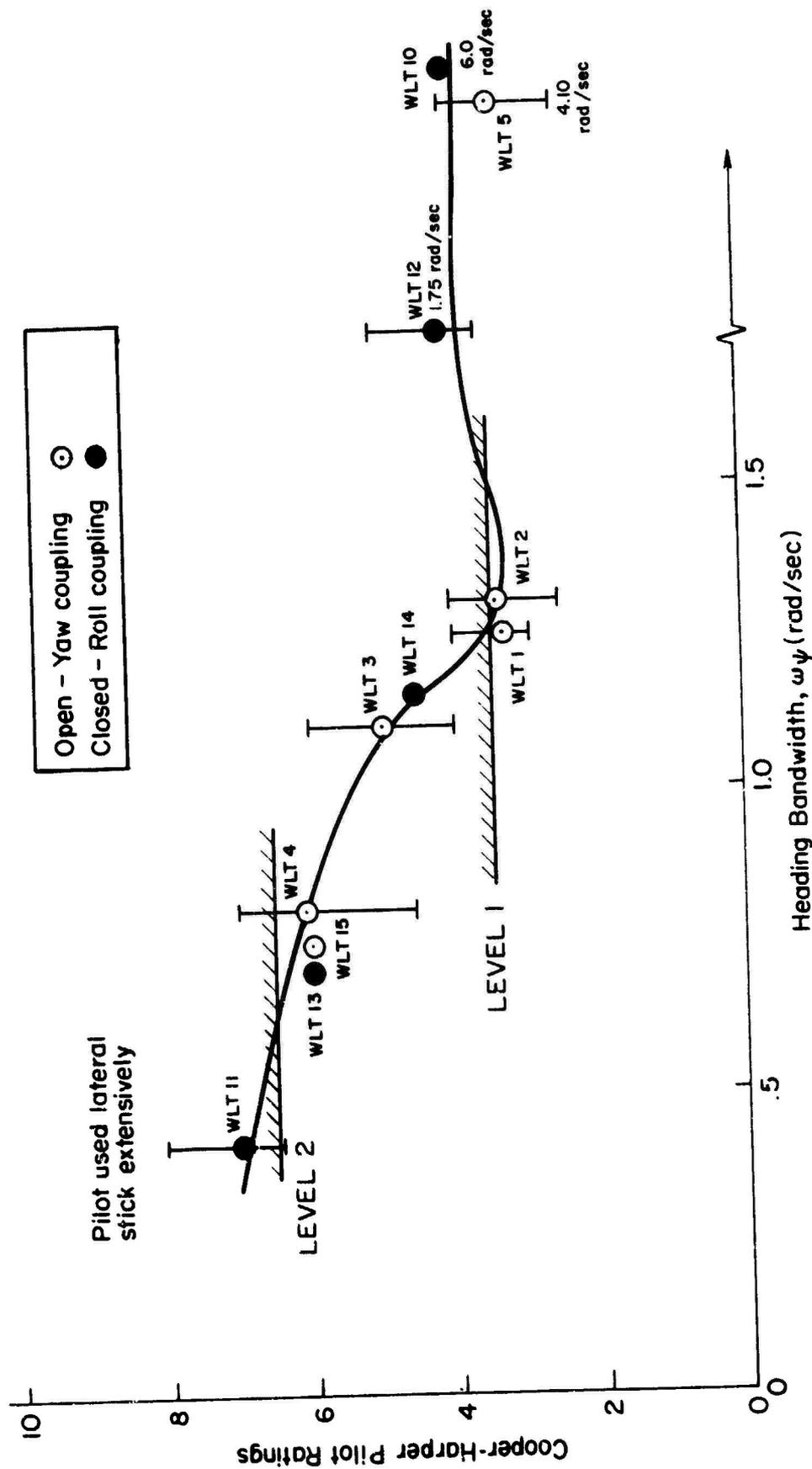


Figure 30. Correlation of Pilot Ratings with Heading Bandwidth; Wings-Level Turn Mode; Air-to-Air Tracking Task

the aircraft to roll in the direction of the commanded wings-level turn, thereby improving the basic response characteristics (provided roll is not too large). Finally, adverse roll coupling appears to the pilot as a tendency for the aircraft to bank away from the commanded wings-level turn.

If the bandwidth hypothesis is valid, the pilot ratings and commentary should be similar for aircraft with approximately equal values of heading bandwidth, regardless of the secondary aircraft motions. The results shown in Fig. 30 confirm that this is indeed the case; more specifically:

- The pilot rating for Configurations WLT4 and WLT15 (adverse yaw coupling) are approximately the same as the pilot rating for Configuration WLT13 (adverse roll coupling). As can be seen from Fig. 30, all of these configurations have approximately the same heading bandwidth of between 0.7 and 0.8 rad/sec.
- Configuration WLT3 (slight adverse yaw coupling) has approximately the same pilot rating as Configuration WLT14 (slight adverse roll coupling). The bandwidth of these configurations are both approximately 1.1 rad/sec.
- Configurations WLT10 and WLT12 have significant favorable roll coupling and correspondingly high values of heading bandwidth. Configuration WLT5 also has a large value of heading bandwidth (4.1 rad/sec) by virtue of its highly proverse yaw coupling. Figure 30 indicates that these configurations are all rated approximately the same.

The above examples provide strong evidence to indicate that satisfactory DFC flying qualities depend primarily on the ability of the pilot to increase his tracking bandwidth to some established level by tightening up on the controls.

The rating data in Fig. 30 indicate that even the best wings-level turn configurations barely meet the classical definition of Level 1 flying qualities (e.g., Cooper-Harper pilot rating equal to or better than 3.5). However, when one considers that the task involves tracking

a target undergoing large and rapid bank angle reversals, it is difficult to conceive of any configuration that would correspond to the adjectival descriptions of a pilot rating of 3 (i.e., "minimal pilot compensation required for desired performance"). The pilot commentary in Appendix C indicates that the WLT1 configuration had very acceptable flying qualities and that the desired performance in tracking task was "easily" attained (but apparently involved more than "minimal compensation"). Hence, the inability to attain average pilot ratings better than 3 is felt to be attributable not to the configuration but rather to the difficulty of the task involved. An example of pilot ratings of 2 for the wings level turn mode was shown in Fig. 18. The tracking task in that case was a ground target which performed a discrete step change in position, a significantly less demanding task than the air-to-air tracking utilized in this program.

A. CONTROL SENSITIVITY

The VRA in-flight simulator was set up so that DFC control sensitivity could be varied in flight. The pilots were asked to vary the control sensitivity of each new configuration to determine the optimum value, thereby eliminating it as a variable in the problem. It was found that the pilot ratings were not dependent on small variations in control sensitivity for either uncoupled or adversely coupled configurations. Inasmuch as the objective of the study was to test the bandwidth hypothesis, rather than to evaluate control sensitivity, flight tests documenting pilot ratings for systematic variations in control sensitivity were minimized. The results are given in Figs. 31-33.

The acceptability of configurations with large values of favorable yaw or roll coupling tended to be significantly more dependent on control sensitivity than the lightly coupled configurations. This is shown by comparing Fig. 32 for high favorable yaw coupling and Fig. 33 for very high favorable roll coupling with Fig. 31 for low coupling. It is interesting to note that the nominal value of control sensitivity used for the latter case (0.008 g/lb) was found to be unacceptably high for the favorable coupling cases. The scatter in the data shown in Fig. 33

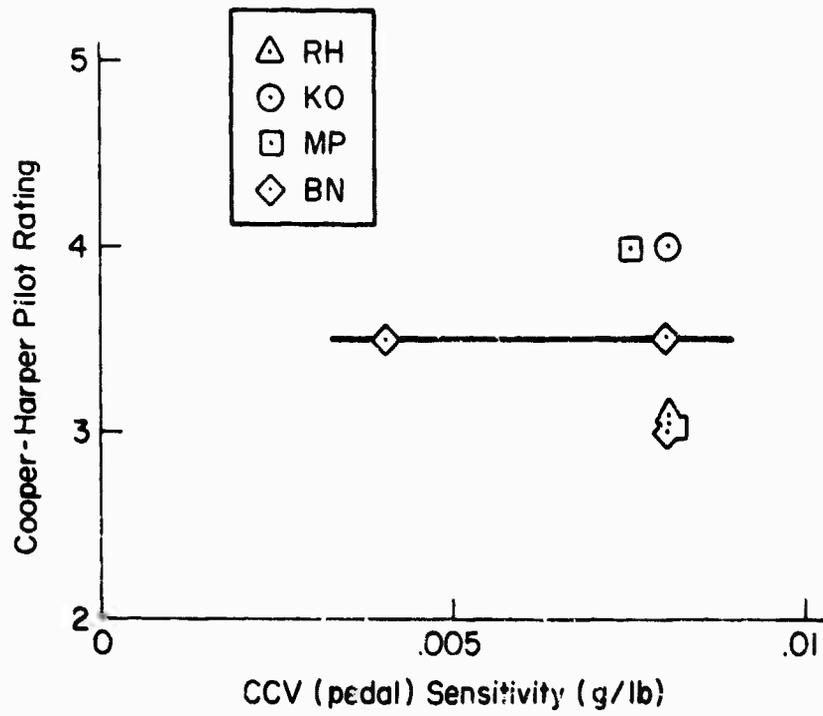


Figure 31. Effect of DFC Manipulator Sensitivity, Configuration WLT1 (Very Low Coupling)

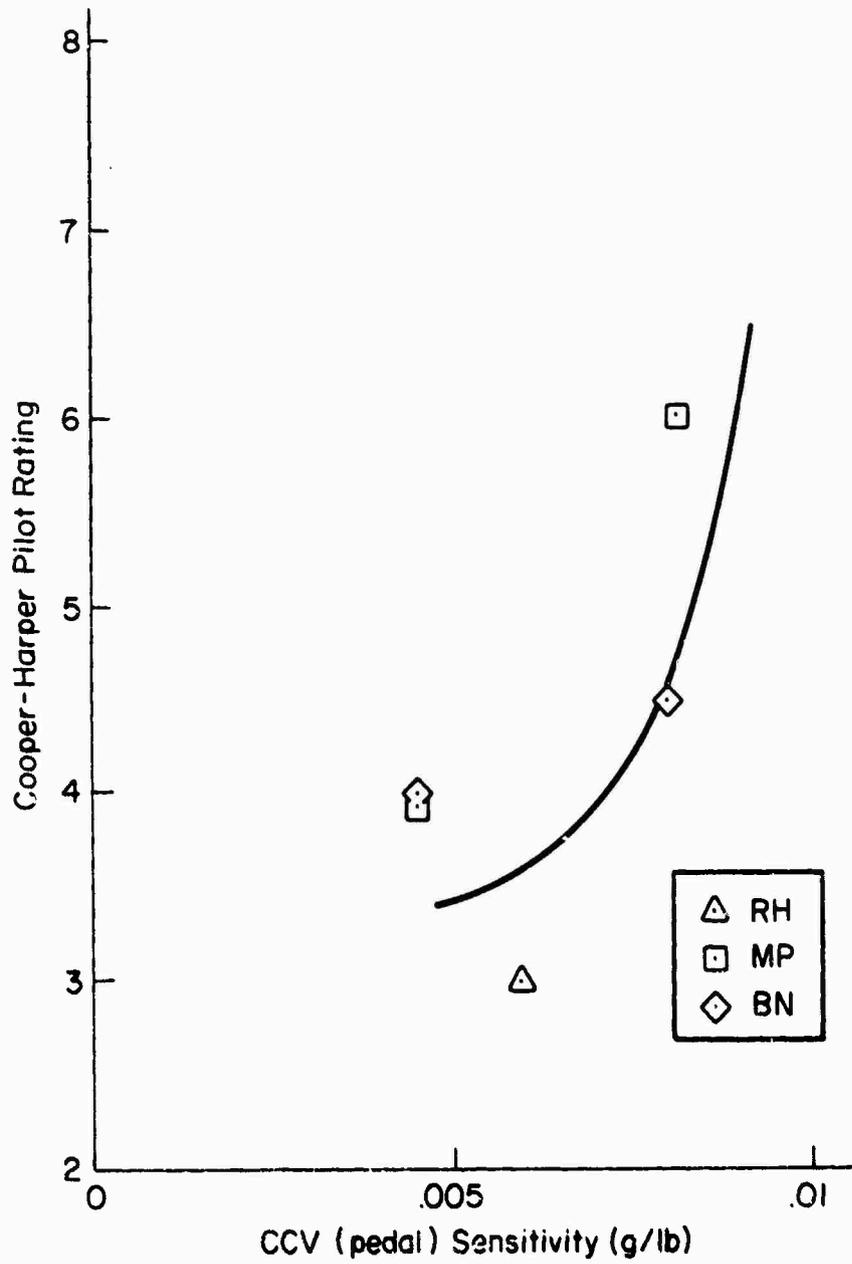


Figure 32. Effect of DFC Manipulator Sensitivity, Configuration WLT5 (High Favorable Yaw Coupling)

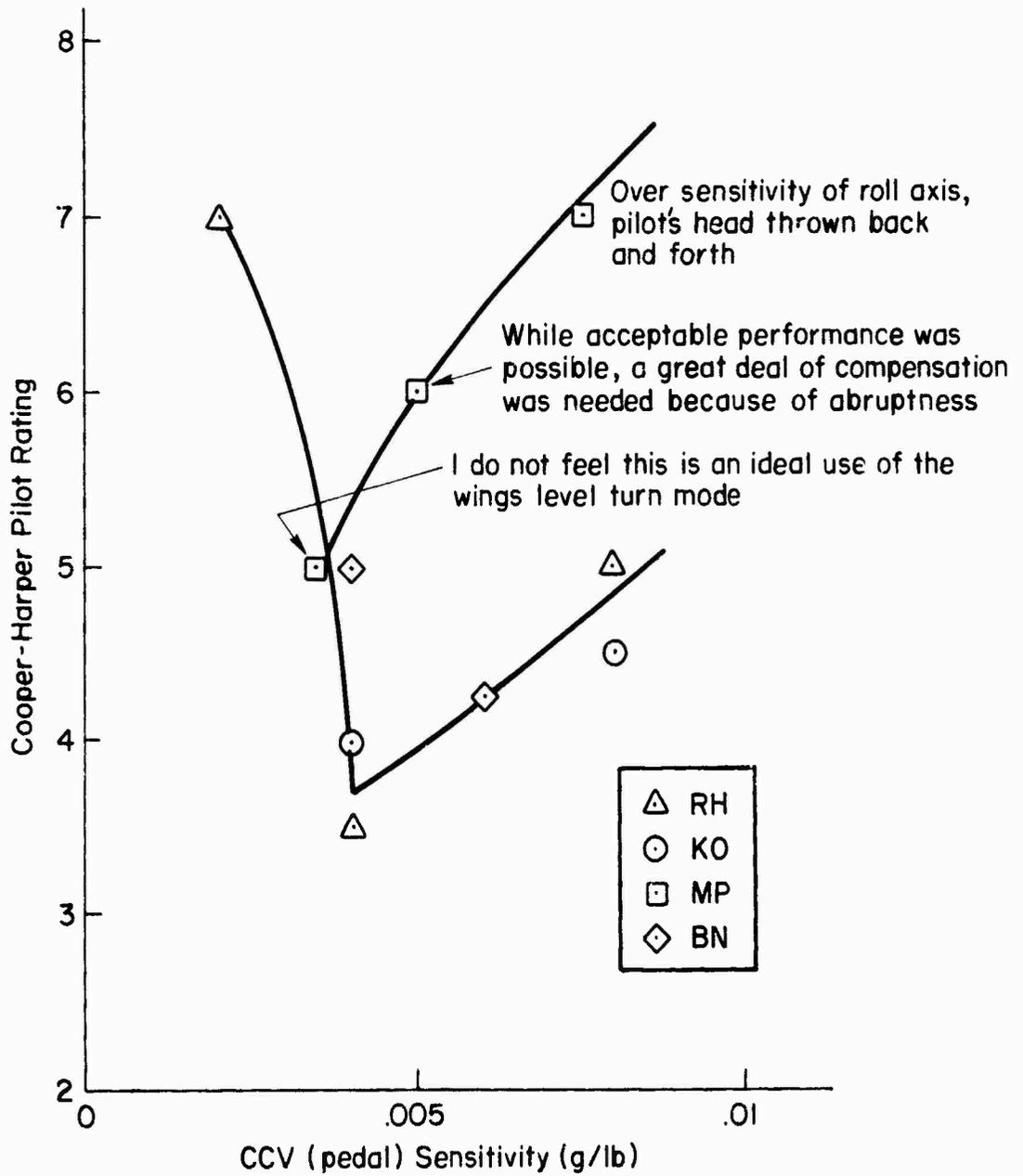


Figure 33. Effect of DFC Manipulator Sensitivity, Configuration WLT12 (Very High Favorable Roll Coupling)

is primarily due to pilot MP. In order to help explain why MP's ratings are higher than the other pilots his comments have been annotated near the appropriate data points in Fig. 33. It is clear that his poor ratings are based on his fundamental objection to utilizing roll coupling to improve tracking bandwidth, although his comments for the lowest sensitivity case indicate that adequate performance could be obtained in this mode. Our interpretation is that Pilot MP's rating of 5 was given to discourage intentional design of proverse roll coupling to improve tracking bandwidth. Hence, even though large values of favorable roll coupling may be inferred as acceptable to produce Level 1 flying qualities, the MIL Handbook should contain a warning against using such coupling to overcome an inherently low bandwidth. Such warning would be especially pertinent for configurations where the pilot was farther from the roll axis (than in the Navion) and therefore subject to more roll-induced lateral acceleration.

The use of secondary controls was allowed in the experiment. That is, the pilots were specifically instructed to utilize the center stick to improve tracking if such control techniques seemed warranted. This was done for consistency with the real-world situation where pilots use the DFC control for fine tuning and the basic aircraft controls for gross maneuvering. Such control usage conforms with the pilot-centered requirement for separation of controls, i.e., only one control can be utilized at the primary closed-loop frequency with all other controls limited to performing trimming-like functions. In the present experiment, the pilots utilized the center stick any time it appeared as if the target bank angle was excessively large to the point where the DFC side force generators were approaching their limit. Such low-frequency secondary control usage was found to be entirely acceptable. However, attempts to utilize the secondary control to improve the tracking bandwidth of the primary DFC control were unsuccessful. This point is best illustrated by considering a configuration with severe unfavorable roll coupling, WLT11. The pilots attempted to fly this configuration by coordinating the secondary center stick with the primary DFC pedal inputs to maintain the integrity of the wings-level turn mode. Such

control actions did improve performance. However, the workload was excessively high, as illustrated by the pilot ratings of 6.5 to 8 in Fig. 30.

It should be pointed out that the secondary control (center stick) was not optimized during this experiment. More specifically, it was necessary to utilize a bank angle command mode (lateral stick inputs command bank angle with a 0.2 sec time constant instead of roll rate) due to problems associated with a drifting integrator in the roll rate command system. Attitude control seems ill suited for any task involving strenuous maneuvering. Nevertheless, the basic conclusions regarding the use of secondary control obtained in this experiment appear valid.

B. LATERAL TRANSLATION CONFIGURATIONS

The lateral translation (β_2) mode was tested on a secondary basis. This mode warranted testing because it is completely decoupled from angular motion; and because of its inherent bandwidth limitation due to the aerodynamic derivative Y_v (Table 2). This derivative is characteristically low and takes on a value of -0.25 for the YF-16 at Mach 0.8 and 20,000 ft altitude. As discussed in Section II, pilots complained of the YF-16's tendency to drift laterally upon release of the DFC control in the lateral translation mode. A lateral translation mode identical to the YF-16 was mechanized on the VRA in an attempt to reproduce such pilot commentary.

The decoupled lateral translation configuration, LT1, was designed to have low bandwidth (order of 0.25) to allow comparison with higher bandwidth configurations such as LT1Y; the expected pilot ratings were on the order of 6. Figure 29a shows the expected first-order lag characteristics at low frequencies (below 1.0 rad/sec), but also an unexpected phase lead between 2 and 3 rad/sec, probably due to favorable yaw or roll coupling that was not anticipated. This was not obvious from qualitative assessments in flight or from analysis of step responses. However, it does explain why the pilot ratings for formation flying were much better than expected for this configuration (Cooper-Harper of 2.5,

2.5, 3). Despite these favorable ratings, pilot commentary indicated that the lateral translation mode would have little benefit for formation flight over conventional control, even if mechanized ideally.

An attempt was also made to utilize the lateral translation mode in an air-to-air tracking task. The target aircraft was maneuvered using the same sequence as for the wings-level turn evaluations, but with significantly smaller bank angles. The pilot ratings for these evaluations, as well as for the formation flying task, are shown in Table 8. Configuration LT1Y was developed to test the bandwidth hypothesis by increasing the inherent bandwidth of Configuration LT1 via favorable yaw coupling. Unfortunately, the priority attached to the lateral translation task meant that it was always performed at the end of each evaluation when little time remained. Because of this the control sensitivities were not systematically varied for the LT1Y configuration. A review of the pilot comments (Appendix C) indicates that the primary deficiency of the LT1Y mode was the jerky or abrupt nature of heading changes to CCV control inputs. Such comments are typical for aircraft with excessive control sensitivity, and the evaluation of Configuration LT1Y cannot be confidently ascribed to its dynamics or compared directly with Configuration LT1. The scatter in pilot ratings for LT1Y in Table 8 is probably a measure of the degree to which each pilot objected to excessive control sensitivity.

TABLE 8

SUMMARY OF COOPER-HARPER PILOT RATINGS FOR
LATERAL TRANSLATION MODE

CONFIGURATION	BANDWIDTH (rad/sec)	FORMATION				AIR TO AIR			
		MP	WN	RH	KO	MP	WN	RH	KO
LT1	1.5	2.5	2.5	--	3	6	4	5.0	4
LT1Y	4.0	5	--	--	3.5	--	5	2.5	2.5

C. COMPARISON WITH GROUND-BASED SIMULATION

Unfortunately, scheduling problems prevented having identical configurations simulated on LAMARS and the VRA. However, one pilot flew both the LAMARS (Large Amplitude Motion Aircraft Research Simulator) and the VRA (Mike Phillips, see Appendix C). He felt that the primary advantage of the VRA was its ability to produce sustained lateral accelerations. The real-world cues in the VRA were considered advantageous but to a lesser extent than sustained lateral g. Adverse features of the VRA were stated to be: 1) lack of cockpit fidelity (does not look like a fighter cockpit; and 2) lack of high-speed flight realism. Lack of a sidestick was a hindrance in relating the VRA to the LAMARS CCV YF-16 simulation, but is not a fundamental limitation of the VRA.

SECTION VI

PROPOSED FLYING QUALITIES SPECIFICATIONS

An effort is currently underway to revise the flying qualities specification (Ref. 3) and to reformat it into a MIL-Standard and Handbook (see, e.g., Weingarten in Ref. 26). The MIL-Standard will contain only the basic outline of the requirements, with blanks for the numerical requirements. The Handbook will contain a justification for the basic requirement, suggested criteria including numerical values, substantiation, and guidance in applying and complying with the requirements. In short, the Handbook will contain all the information necessary to fill in the blanks in the Standard and tailor it into a detailed specification for a particular aircraft system or mission. The objective is to present mission-oriented flying qualities requirements. Direct force controls are particularly responsive to this objective, being of use only if they can improve mission or task performance. In the remainder of this section, therefore, we will express the results as requirements for the MIL-Standard, and then indicate the discussion items for the Handbook.

A. MIL-STANDARD REQUIREMENTS FOR DIRECT FORCE CONTROLS

If the DFC is used in a blended fashion to enhance a conventional mode (as in maneuver enhancement), then we have a simple requirement for the DFC to increase the bandwidth of that conventional response to control input.

For decoupled use of DFC we need a requirement that can be applied in any axis deemed appropriate by the procuring activity:

- Dynamic response to direct force control input. The bandwidth of the open-loop response of _____ to _____-control input shall be greater than _____ for Flight Phase _____.
- Steady-state response to direct force control input. Maximum force control input shall produce at least _____.
- Direct force control forces and deflections. Use of the _____ control shall not require use of another control manipulator to meet the above dynamic response requirement. The controller characteristics shall meet the following requirements: _____.
- Pilot accelerations. Abrupt, large DFC inputs shall not produce pilot head or arm motions which interfere with task performance. Pilot restraints shall not obstruct his normal field of view nor interfere with manipulation of any cockpit control required for task performance.

**B. HANDBOOK DISCUSSION FOR
DIRECT FORCE CONTROLS**

**1. Dynamic Response to Direct
Force Control Input**

The requirements presented in the Standard are based on the premise that direct force controls are designed to improve either tracking capability or flight path control. Accordingly, the response variables appropriate to different tasks are presented in Table 9. This information allows tailoring of the response variable and control input to the appropriate Flight Phase(s) (or tasks) in any axis deemed necessary by the procuring activity for mission performance.

TABLE 9

TYPICAL AIRCRAFT PARAMETERS SUBJECT TO BANDWIDTH
LIMITATION IN MIL STANDARD

TASK	CONTROL VARIABLE
Air-to-air tracking	Pitch or yaw angle if angle of attack or sideslip are not an importance factor for weapon release Path angle if angle of attack or sideslip must be left small for weapon release
Air-to-ground tracking	
Pointing tasks	Pitch or yaw angle
Strafing	
Photo	
Flight path tasks	Path angle, normal or lateral velocity
Dive bombing	
Path deviation tasks and landing	Path angle, normal or lateral velocity

The work presented in the preceding sections of this report has substantiated bandwidth as the basic criterion. Table 10 presents the suggested values of required bandwidth, separated according to tracking or path control tasks. The flight test data obtained in this program and presented in Fig. 29 were utilized to set the limits for air-to-air tracking in Table 10. The assumption that these data can be extended to the longitudinal axis is based on the symmetry of the longitudinal and lateral limiting forms in Table 2. Clearly, there should be additional longitudinal data to validate this assumption. Until such data are available, the assumption of symmetry implicit in Table 10 will at least provide a guideline for DFC design. There is some question as to whether the value of 1.25 rad/sec specified in Table 10 for pitch control in air-to-air tracking is not too low; however, the number may be justified on the basis that the requirement is simply to track a target with

a relatively high path mode frequency content not likely to exceed 1.25 rad/sec. In fact, the bandwidth requirement based on pitch response to conventional control, as for inner-loop stabilization of path mode control, may well be higher (see Ref. 3).

The bandwidth requirements stated for the path deviation task (CAT C in Table 10) are based on the lateral translation mode results given in Table 8, as well as heading control results obtained for conventional aircraft in previous programs. An example of such results is shown in Fig. 34, taken from Ref. 39. Figure 34 indicates that most points below a heading bandwidth of 0.3 rad/sec are Level 2 or worse. For lack of any better data, the Level 2 boundary was defined (from Fig. 34) as 0.12 rad/sec.

TABLE 10
TENTATIVE BANDWIDTH LIMITATIONS

TASK	REQUIRED BANDWIDTH (rad/sec)	
	LEVEL 1	LEVEL 2
Tracking (CAT A) Air-to-air gunnery Strafing Photo Dive bombing	1.25	0.60
Path deviation (CAT C) Formation Air-to-air refueling Approach	0.30	0.12
Short final and landing path response ("CAT D")	$(\dot{H}_F - 3)/10^*$?

* \dot{H}_F = sink rate in ft/sec on visual or instrument glide slope

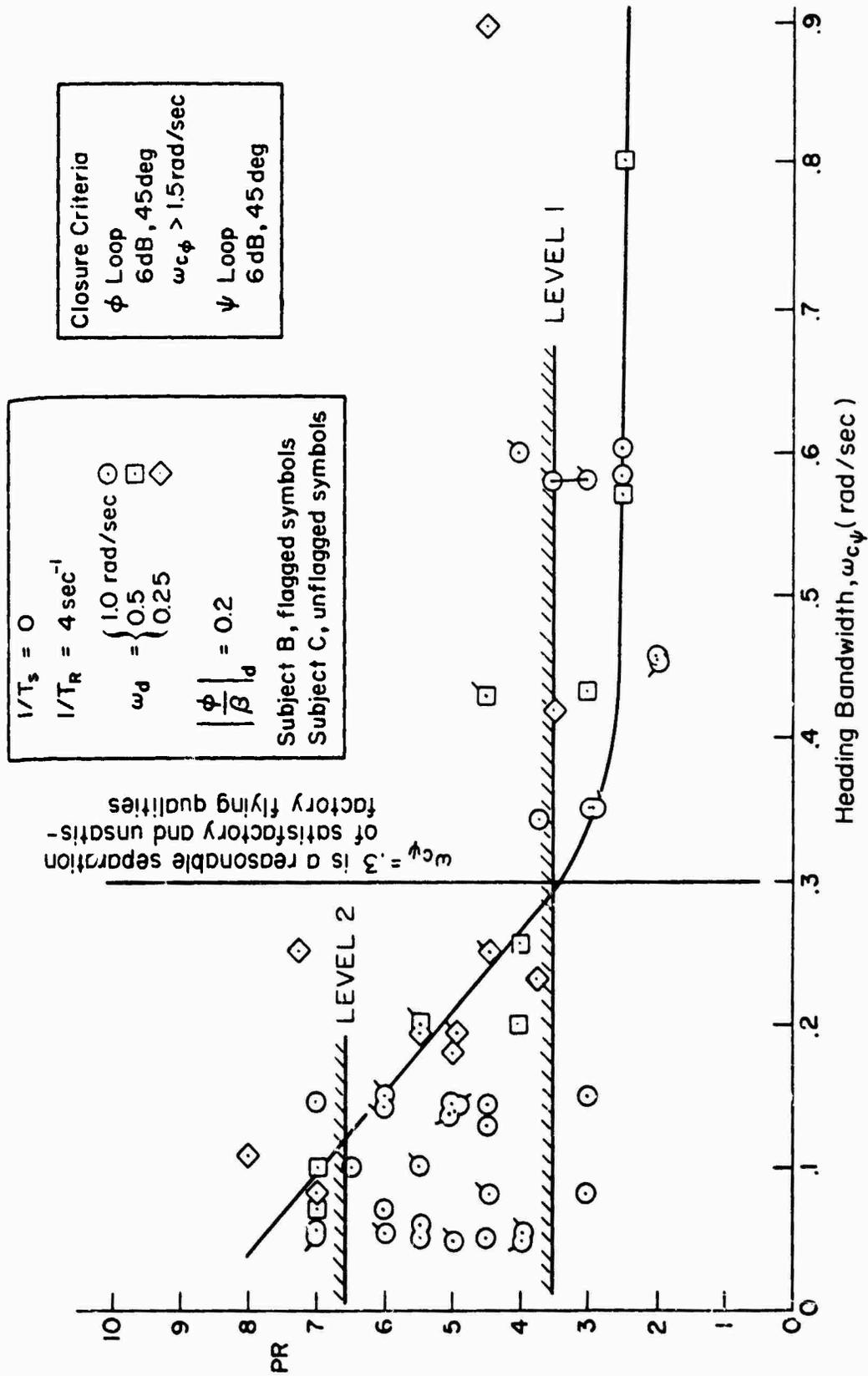


Figure 34. Correlation of Pilot Ratings with Heading Bandwidth for Conventional Aircraft; Task is ILS Approach

Short final and landing is called out separately in Table 10. This reflects the fact that mission requirements for precise landing, such as STOL capability, carrier landings, etc., are significantly more critical than the tasks listed as Category C in MIL-F-8785B and C. Two alternatives are indicated for the required bandwidth. The first comes directly from Ref. 40, where it is shown that the bandwidth required to flare depends on the flare-initiation height, the sink rate at flare initiation, and the desired touchdown sink rate. The value suggested is based on a flare height of 50 ft and a touchdown sink rate of 3 ft/sec; the analysis is given in Ref. 41. The second alternative is based on the suggestion that a precise landing is closer to a tracking task (Category A in MIL-F-8785C) than a path control task. Qualitative support for this is given in Ref. 41. The first of these alternatives is shown in Table 10.

2. Steady-State Response to Direct Force Control Inputs

A detailed investigation of control power was well beyond the scope of the present program. The following discussion treats the subject of control power in somewhat general terms.

The control authority required clearly depends on the task. Reference 12 indicated that, in fixed-based simulations, a control authority of 1 lateral g results in very satisfactory flying qualities for the wings-level turn and lateral translation modes in a dive bombing task. That study also showed that the Level 1 minimum boundary was approximately 0.5 g.

For the air-to-air tracking task using a wings-level turn mode, the question becomes one of defining the minimum acceptable allocation of control authority between the DFC control for fine tuning and the conventional use of bank angle for gross maneuvering.

For blended DFC modes, such as maneuver enhancement, the control power required depends on the effects of control saturation. If the aircraft is highly augmented (large feedback gains to the DFC control), saturation effects will be dramatic. In this case the pilot will see

saturation as a sudden and very pronounced change in the response characteristics to control inputs as the aircraft degrades to its unaugmented state. The tendency for such saturation to occur will of course also depend on the size of the maneuvers required to accomplish the specified tasks.

Until data can be obtained to quantify the above considerations it may be necessary to define control authority indirectly, that is, to specify that enough authority shall be available to do the prescribed tasks. Such a requirement could be contained in a type specification for each airplane to allow specific mission requirements to be accounted for.

3. Direct Force Control Forces and Deflections

We have established the requirements for frequency separation of controls, which is expressed in the first part of the requirement. The appropriate relationships of the controlled responses to controller forces and deflections are unavailable and should be the subject of future research.

4. Pilot Accelerations

This requirement states the obvious, but it is expected to become important, especially for lateral accelerations.

APPENDIX A

BASIC DATA FOR YF-16 CCV ANALYSIS

BASIC DATA FOR YF-16 CCV CONFIGURATION

The flight condition chosen for analysis in this program corresponds to the YF-16CCV air-to-air HQDT evaluation (0.8 Mach at 20,000 ft and 19,500 lb gross weight). The required airframe and control system data were obtained from Refs. 17 and 18 and private communications from the YF-16 contractor. The model of the airframe and control system is a conventional, small-perturbation set of separated longitudinal and lateral-directional equations linearized about straight and level flight. The basic longitudinal and lateral-directional airframe data are summarized in Tables 11 and 12, respectively. Stability and control derivatives are in stability axes.

In Table 11 note that $C_{m\alpha}$ and the related M_{α} stability derivative are positive; the airframe is statically unstable. Note that both the flap and the elevator are moment-producing controls; a significant elevator deflection is required to counter that produced by flap deflection in the unconventional flight modes. The variable l_{x_a} refers to the location of the normal accelerometer ahead of the center of gravity.

Except for the differing sign on the yawing moment derivatives, the rudder and vertical canards are closely equivalent. To balance the yawing forces in the unconventional flight modes therefore requires very close coordination of the two surfaces. Otherwise, significant transient yawing motions will occur.

The linearized control system model was derived from the complete system schematics in Refs. 17 and 18. At the operating point, gains were selected from corresponding schedules, controller sensitivities were extracted from input/output diagrams, and nonlinearities such as breakouts and preloads were approximated by linearized characteristics.

TABLE 11. LONGITUDINAL DATA

Geometry

V_{T0} (fps)	α_0 (deg)	γ_0 (deg)	Accel., z_x (ft)
829.6	0.0	0.0	12.606
a (fps)	ρ (slug-ft ⁻³)	M	h (ft)
1037.0	0.0012673	0.8000	20000.
S (ft ²)	C (ft)	W (lb)	I_y (slug-ft ²)
280.0	10.900	19501.	47000.

Nondimensional Derivatives

C_L	$C_{L\alpha}$ (1/rad)	$C_{L\dot{\alpha}}$ (sec/rad)	$C_{L\ddot{\alpha}}$
0.15974	4.110	1.0500	0.0
$C_{M\alpha}$ (1/rad)	$C_{M\dot{\alpha}}$ (sec/rad)	$C_{M\ddot{\alpha}}$ (sec/rad)	$C_{M\ddot{\alpha}}$
0.4070	-1.5533	-4.457	0.0
C_D	$C_{D\alpha}$ (1/rad)	$C_{D\dot{\alpha}}$	
0.03394	-0.3930	0.0	
$C_{D\delta_e}$ (1/rad)	$C_{L\delta_e}$ (1/rad)	$C_{M\delta_e}$ (1/rad)	
0.03300	0.6314	-0.7466	
$C_{D\delta_L}$ (1/rad)	$C_{L\delta_L}$ (1/rad)	$C_{M\delta_L}$ (1/rad)	
0.04300	0.6784	-0.17500	

Dimensional Derivatives

X_u (1/sec)	X_u^* (1/sec)	X_w (1/sec)	
-0.16484	-0.16484	0.13422	
Z_u (1/sec)	Z_u^* (1/sec)	$Z_{\dot{w}}$	Z_w (1/sec)
-0.7758	-0.7758	-0.0016742	-1.0063
M_u (1/sec-ft)	M_u^* (1/sec-ft)	$M_{\dot{w}}$ (1/sec-ft)	M_w (1/sec-ft)
0.0	0.0	-0.0003483	0.013892
$M_{\dot{\alpha}}$ (1/sec)	$M_{\dot{\alpha}}$ (1/sec ²)	$M_{\ddot{\alpha}}$ (1/sec)	
-0.2889	11.524	-0.8290	
X_{δ_e} (ft/sec ² -rad)	Z_{δ_e} (ft/sec ² -rad)	M_{δ_e} (1/sec ² -rad)	
-6.648	-127.19	-21.14	
X_{δ_L} (ft/sec ² -rad)	Z_{δ_L} (ft/sec ² -rad)	M_{δ_L} (1/sec ² -rad)	
-8.662	-136.66	-4.955	

TABLE 12. LATERAL-DIRECTIONAL DATA

<u>Geometry</u>				
V_{T_0} (fps)	α_0 (deg)	γ_0 (deg)	Accel. l_x (ft)	
829.6	0.0	0.0	12.834	
I_x (slug-ft ²)	I_y (slug-ft ²)	I_{xy} (slug-ft ²)	a_G (deg)	h (ft)
8100.	53300.	354.0	2.000	20000.
S (ft ²)	B (ft)	β (slug-ft ⁻³)	W (lb)	a (fps)
280.0	29.00	0.0012673	19501.	1037.0
<u>Nondimensional Derivatives</u>				
$C_{y\delta}$ (1/rad)	$C_{l\beta}$ (1/rad)	$C_{n\beta}$ (1/rad)		
-1.2610	-0.11547	0.15840		
C_{lP}	C_{nP}	C_{lR}	C_{nR}	
-0.03051	-0.02248	0.2215	-0.3804	
$C_{y\delta_A}$ (1/rad)	$C_{l\delta_A}$ (1/rad)	$C_{n\delta_A}$ (1/rad)		
0.14706	-0.11208	-0.02782		
$C_{y\delta_R}$ (1/rad)	$C_{l\delta_R}$ (1/rad)	$C_{n\delta_R}$ (1/rad)		
0.13078	0.017867	-0.06355		
$C_{y\delta_c}$ (1/rad)	$C_{l\delta_c}$ (1/rad)	$C_{n\delta_c}$ (1/rad)		
0.07885	0.013099	0.06891		
<u>Unprimed Dimensional Derivatives</u>				
Y_V (1/sec)	L_β (1/sec ²)	N_β (1/sec ²)		
-0.3062	-50.47	10.522		
L_P (1/sec)	N_P (1/sec)	L_R (1/sec)	N_R (1/sec)	
-2.331	-0.02610	1.6927	-0.4417	
Y_{δ_A} (ft/sec ² -rad)	L_{δ_A} (1/sec ² -rad)	N_{δ_A} (1/sec ² -rad)		
29.62	-48.99	-1.8481		
Y_{δ_R} (ft/sec ² -rad)	L_{δ_R} (1/sec ² -rad)	N_{δ_R} (1/sec ² -rad)		
26.34	7.810	-4.222		
Y_{δ_c} (ft/sec ² -rad)	L_{δ_c} (1/sec ² -rad)	N_{δ_c} (1/sec ² -rad)		
15.884	5.725	4.578		
<u>Primed Dimensional Derivatives</u>				
Y_β (ft/sec ²)	L_β (1/sec ²)	N_β (1/sec ²)		
-254.0	-50.03	10.190		
L_P (1/sec)	N_P (1/sec)	L_R (1/sec)	N_R (1/sec)	
-2.333	-0.04160	1.6739	-0.4306	
$Y_{\delta_A}^*$ (1/second)	$L_{\delta_A}^*$ (1/sec ² -rad)	$N_{\delta_A}^*$ (1/sec ² -rad)		
0.03571	-49.09	-2.174		
$Y_{\delta_R}^*$ (1/sec-rad)	$L_{\delta_R}^*$ (1/sec ² -rad)	$N_{\delta_R}^*$ (1/sec ² -rad)		
0.03176	7.828	-4.171		
Y_c^* (1/sec-rad)	$L_{\delta_c}^*$ (1/sec ² -rad)	$N_{\delta_c}^*$ (1/sec ² -rad)		
0.019148	5.927	4.617		

The resulting linearization was a "high-order" model (including wash-outs, filters, shaping networks, etc.) as opposed to the lower-order "equivalent system" model implemented on the Princeton VRA (see Appendix B).

Figures 35 through 43 present the linearized flight control system block diagrams for the conventional and DFC flight modes in the longitudinal and lateral-directional degrees of freedom. In these diagrams the combination of command servo and power actuator lag are approximated by a single 13 rad/sec first-order lag.

The conventional longitudinal flight control system (Fig. 35) employs conventional pitch rate plus normal acceleration feedback, operating through a proportional-plus-integral element in the forward loop — an "autotrim" feature. The angle of attack is fed back for static stability of the statically unstable airframe.

For the direct lift mode (Fig. 36) the pilot actuates the trim button. The signal, proportional to "coolie hat" deflection, is filtered, and then deflects the flaps. A crossfeed to the elevator is used for moment cancellation, and pitch rate and normal acceleration commands are fed to the conventional flight control system. The complex topology of this mode and also the α_1 and α_2 modes were "boiled down" to combinations of flap command and an equalized crossfeed to the elevator.

In the pitch pointing mode (Fig. 37), the flap command is likewise fed to the elevator and also provides an angle-of-attack command to the conventional FCS. The crossfeed to the elevator uses a nonminimum-phase approximation to a time delay, a feature found necessary to avoid undesirable transients in the response.

Similar topology is used in the vertical translation mode (Fig. 38). In addition, a pitch attitude hold loop is added, and the elevator crossfeed has changed sign at high frequency; the low-frequency (trim) equalization is nearly the same as for the α_1 mode (-0.1945 versus -0.1894).

Comparison of Fig. 39 with 36 shows that the maneuver enhancement mode is derivable from the A_N mode by forming a normal acceleration

error from the stick command and the normal accelerometer's response. The error commands the A_N mode by forming a normal acceleration error from the stick command and the normal accelerometer's response. The error commands the A_N mode topology. In this instance, the equivalent simplified block diagram has an equalized crossfeed from the stick to the flap and a feedback of normal acceleration to the flap. The conventional FCS has modified equalization on the stick input and normal acceleration feedback.

The lateral-directional system shown in Fig. 40 has conventional topology: roll rate feedback to the aileron, an aileron-to-rudder interconnect (ARI), and a combination of lateral acceleration and stability-axis yaw rate feedback to the rudder (the small gain on p_g reflects the operating-point angle of attack).

The topology of the unconventional modes, while appearing relatively complex, in actuality is rather simple. Moment-cancelling crossfeeds are used to the rudder and aileron in addition to a roll attitude hold loop. "Purifying" feedbacks are used to the rudder: β in Fig. 41, a_y in Fig. 42, and r in Fig. 43. Finally, the vertical canard input commands the conventional directional control system: lateral acceleration and yaw rate in Fig. 41 and washed-out lateral acceleration (resembles lateral velocity) in Fig. 43. None of these modes uses feedback to the canard surfaces.

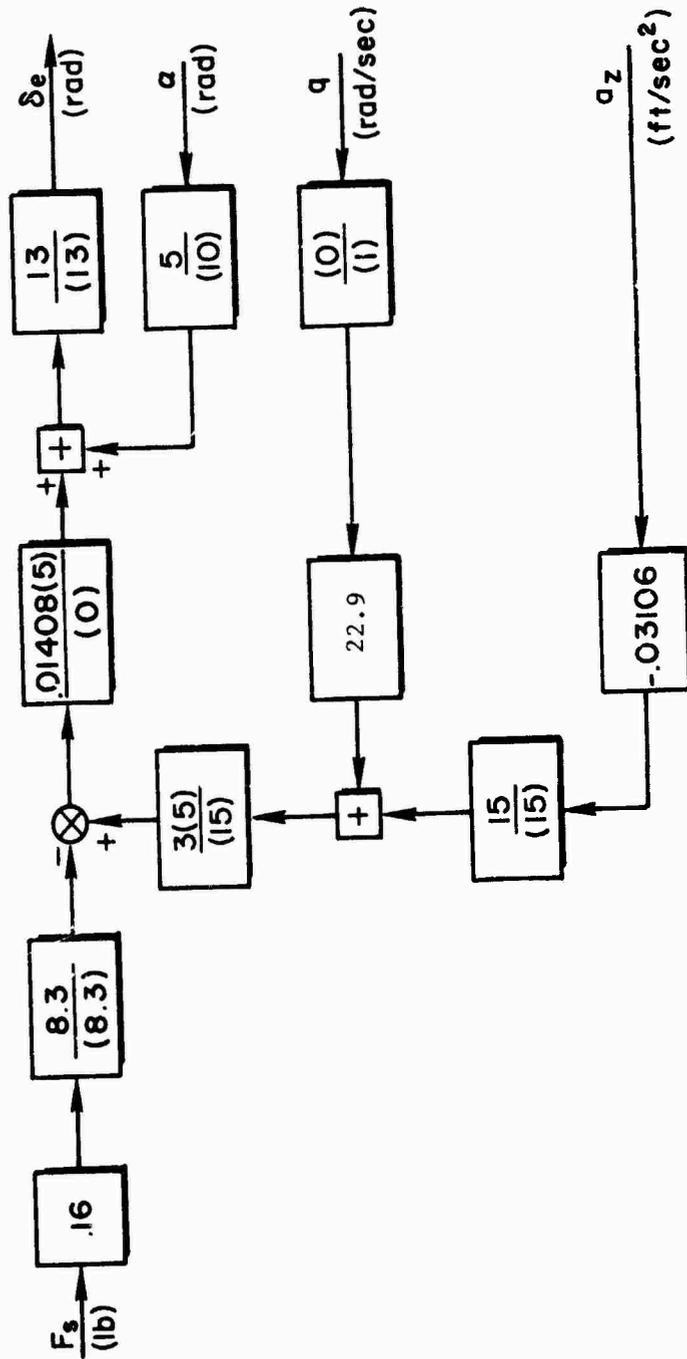


Figure 35. Longitudinal Flight Control System, Conventional Flight Mode

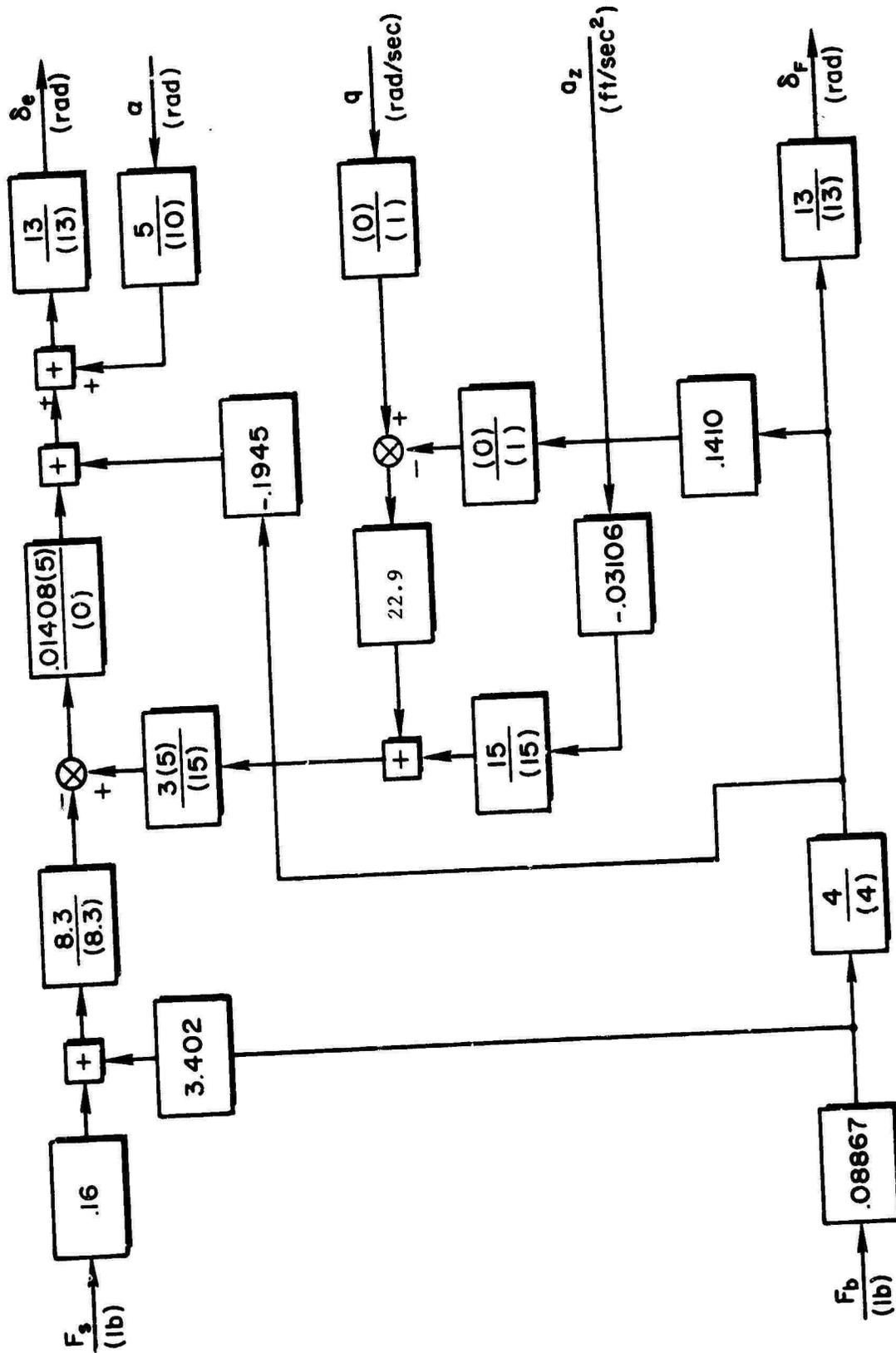


Figure 36. Longitudinal Flight Control System, Direct Lift (AN) Flight Mode

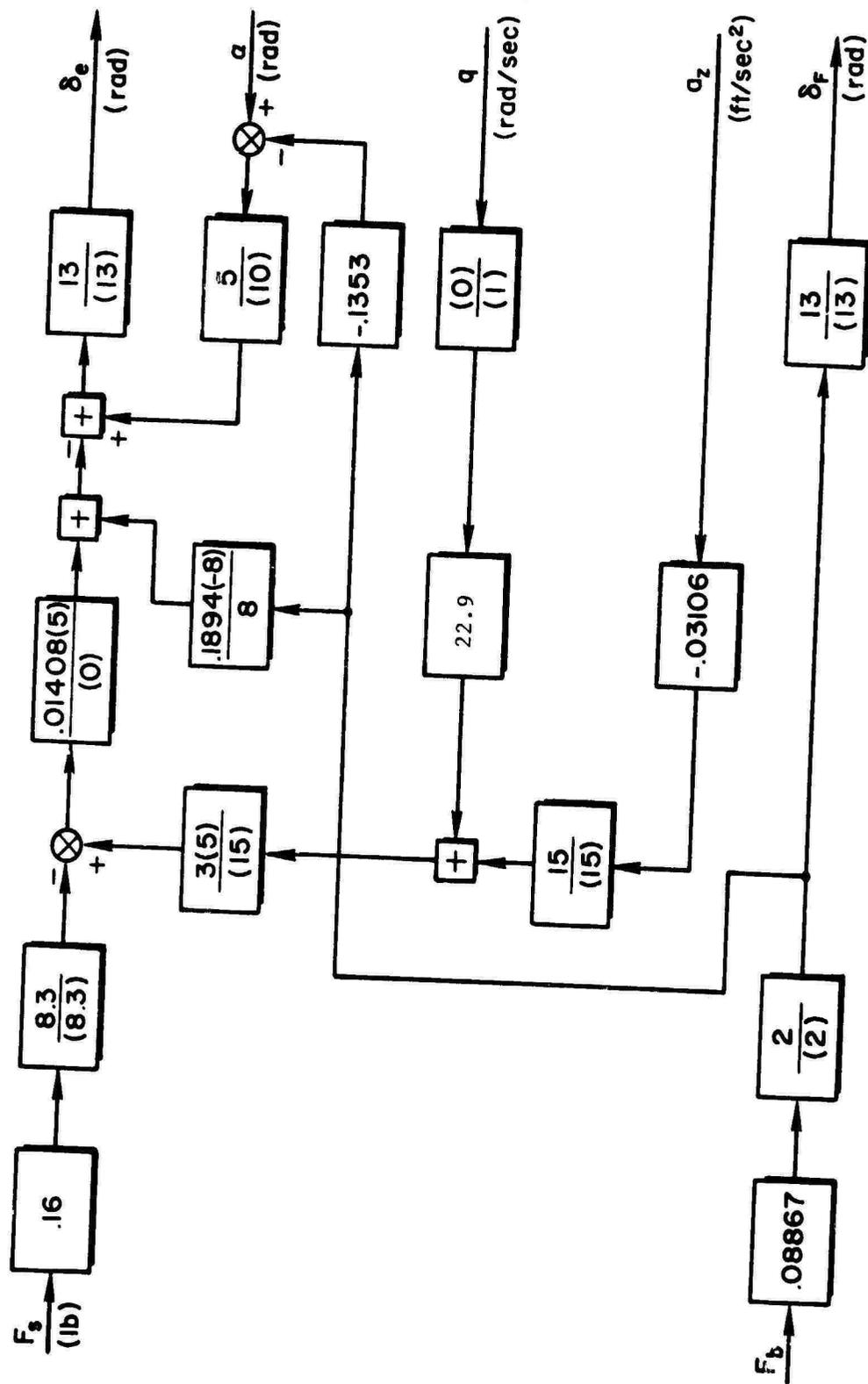


Figure 37. Longitudinal Flight Control System, Pitch Pointing (α_1) Mode

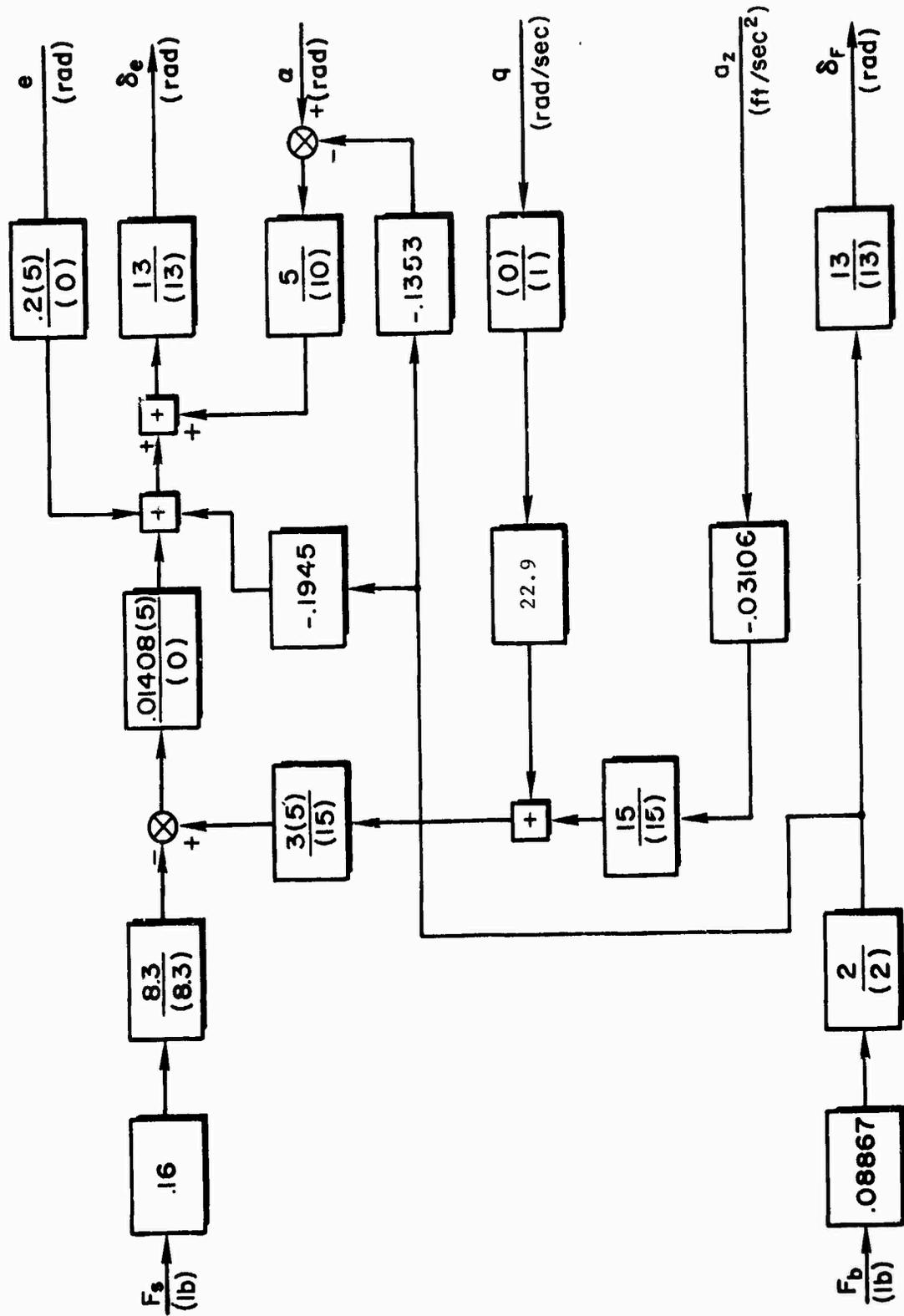


Figure 38. Longitudinal Flight Control System, Vertical Translation (α_2) Mode

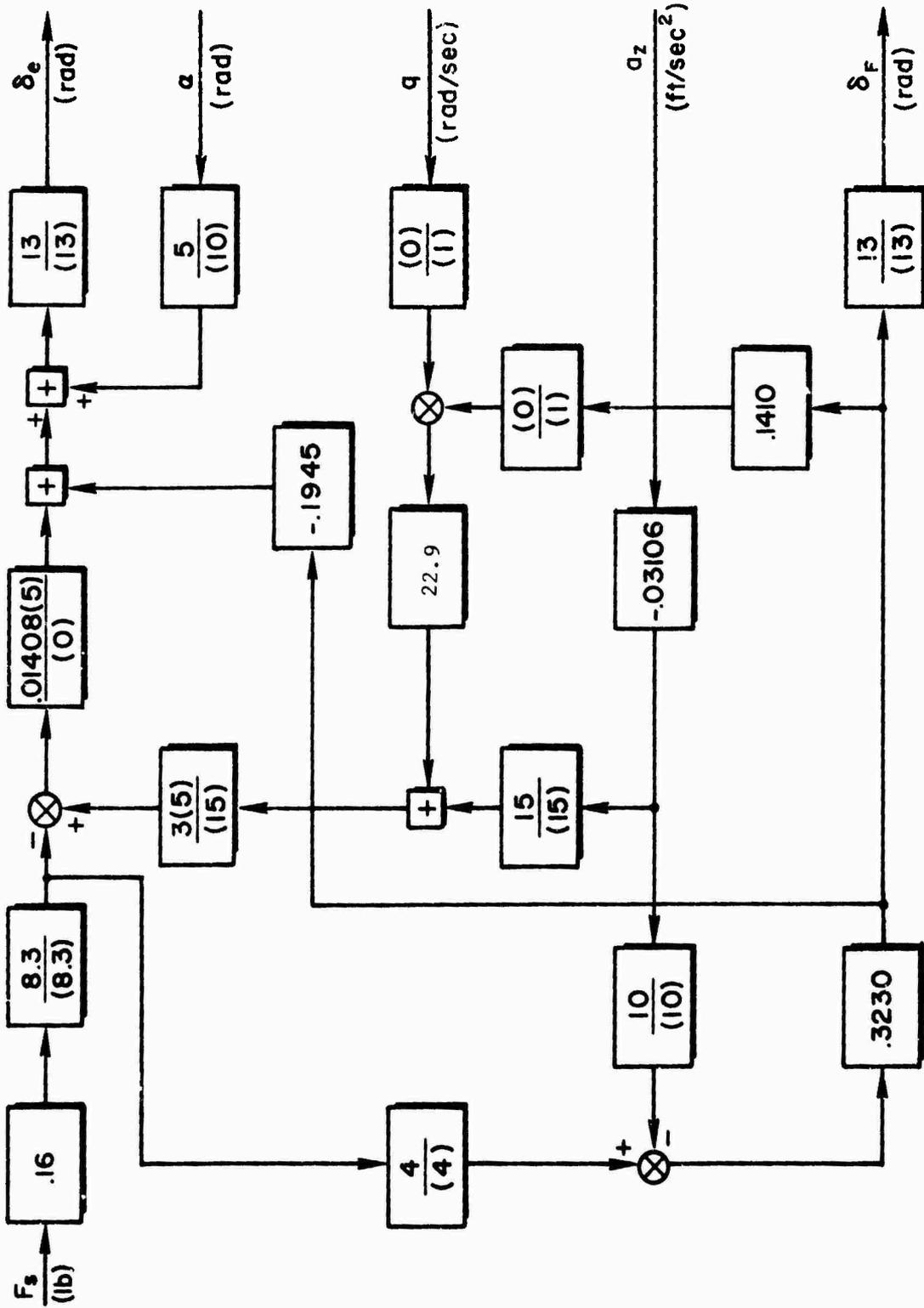


Figure 39. Longitudinal Flight Control System, Maneuver Enhancement (ME) Mode

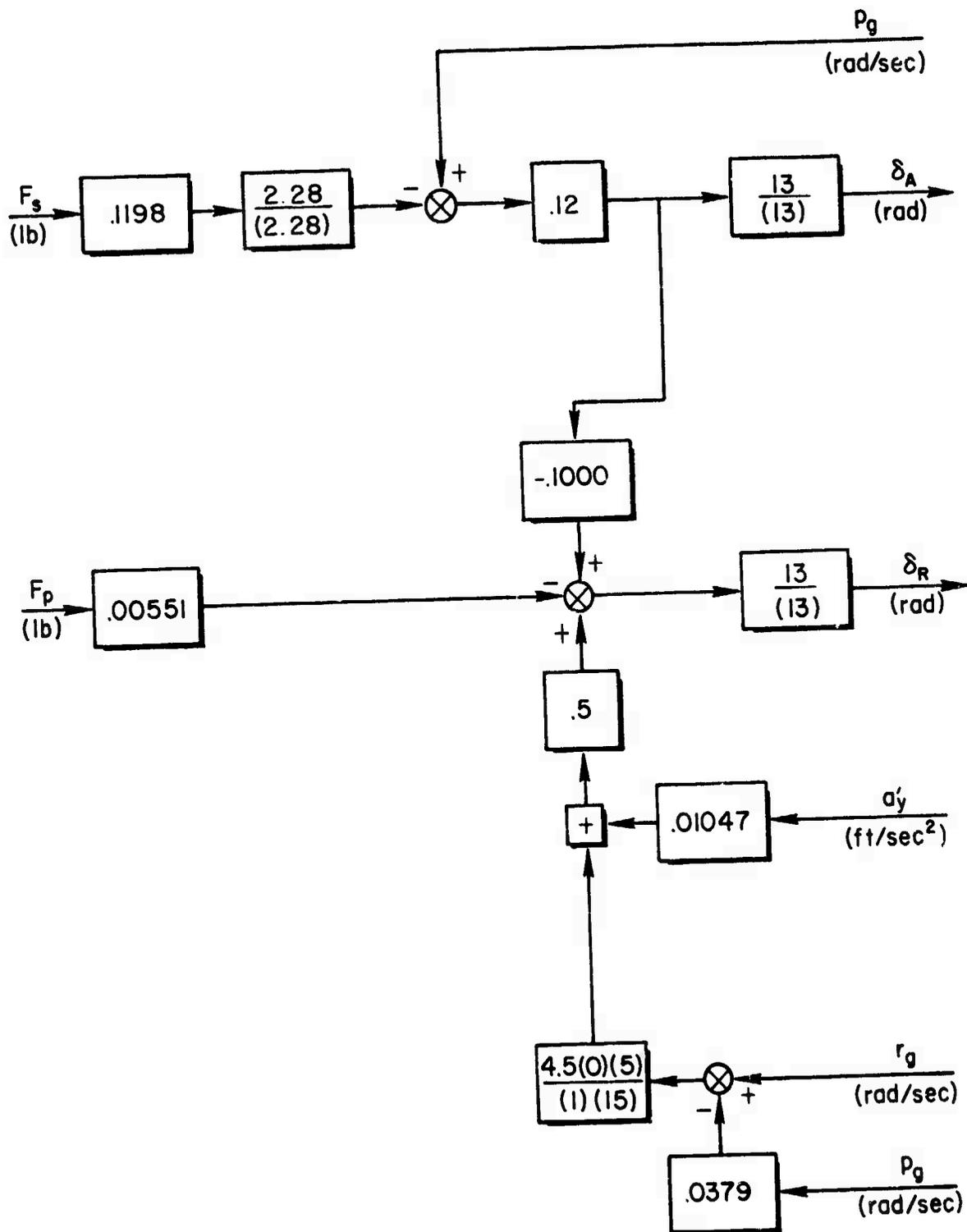


Figure 40. Lateral-Directional Flight Control System, Conventional Flight Mode

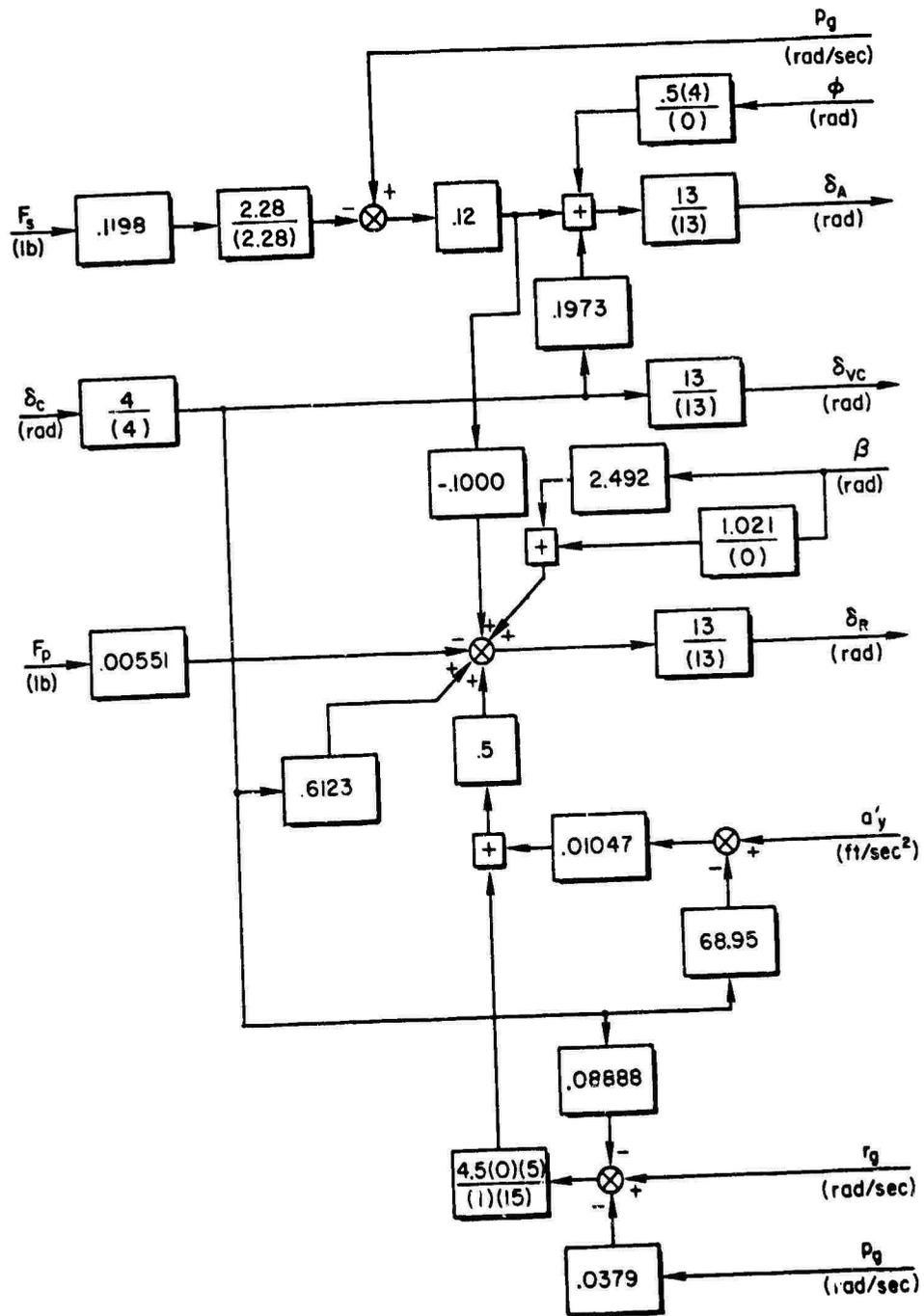


Figure 41. Lateral-Directional Flight Control System, Direct Side Force (A_y) Mode

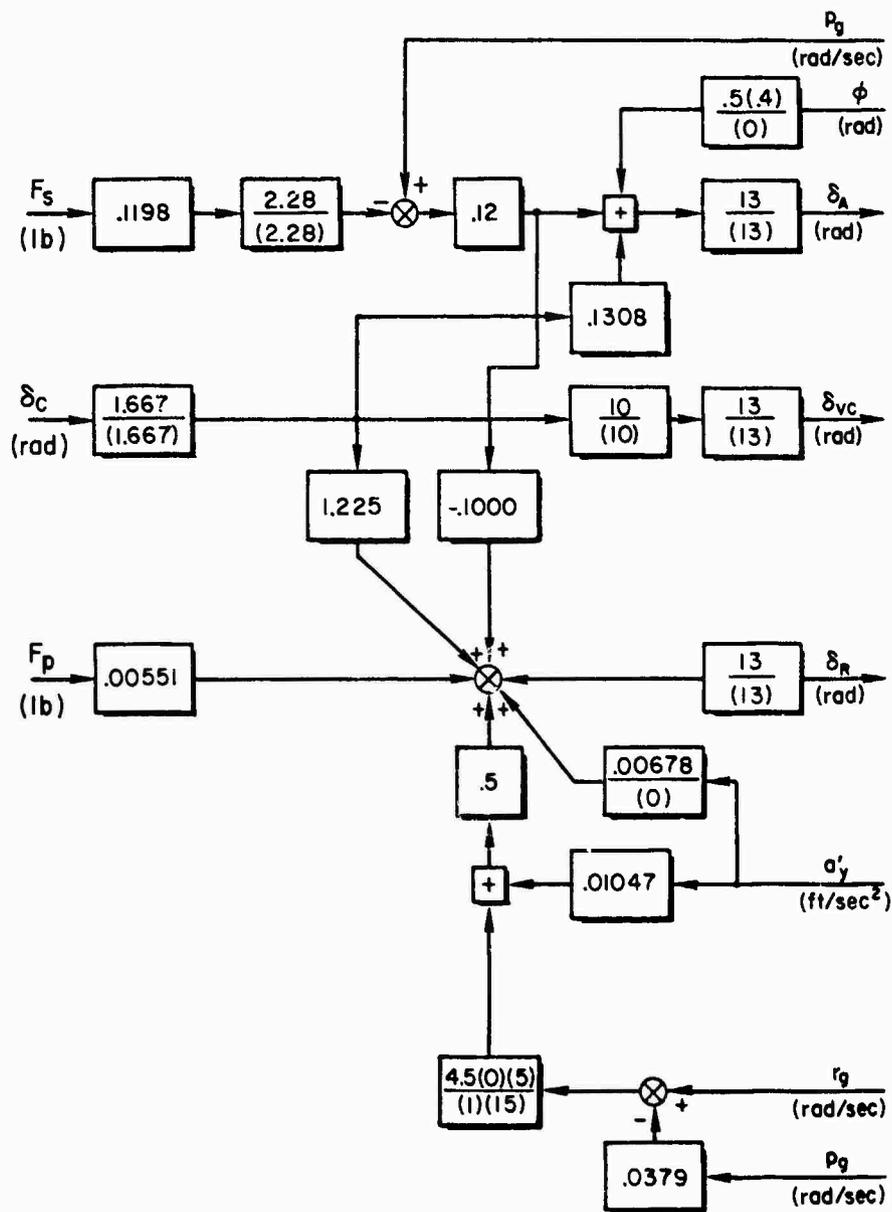


Figure 42. Lateral-Directional Flight Control System, Yaw Pointing (β_1) Mode

YF-16 CCV LONGITUDINAL ANALYSES

This section presents the analysis of the YF-16 CCV longitudinal response properties for the flight condition (0.8 Mach/20,000 ft/19,500 lb) and the model given in the preceding section of this appendix. Because of their essential similarity the conventional, maneuver enhancement (ME), and direct lift (A_N) response modes are considered in a comparative manner. This is followed by a discussion of the pitch pointing (α_1) and vertical translation (α_2) modes and their response impurities. The last subsection discusses mode "purification" (decoupling).

Conventional, A_N , and ME Modes

The transfer function of attitude response to pilot stick force, θ/F_s , of the basic YF-16 is plotted as a function of frequency, ω , in Fig. 44. The upper plot shows the amplitude response in decibels (solid line is the frequency response itself, the dotted line shows the Bode asymptotes); the lower shows the phase angle in degrees. The dotted line across the bottom of the plot denotes 180 degrees of lag. If the pilot were represented as a pure gain in controlling attitude, this figure shows that he would be limited in crossover frequency (at zero degrees phase margin) to about 5 rad/sec.

The relatively high-order system represented in Fig. 44 can be approximated well by a lower-order equivalent system of the form*:

$$\frac{\theta}{F_s} = \frac{K_\theta(1/T_L)e^{-\tau s}}{(0)[\zeta_{wp}, \omega_{sp}](1/T_f)} \quad (25)$$

*Terms in parentheses are first-order polynomial factors; e.g., $(1/T_L)$ implies $(s + 1/T_L)$; terms in brackets are second-order; e.g., $[\zeta_o, \omega_o]$ implies $[s^2 + 2\zeta_o\omega_o s + \omega_o^2]$.

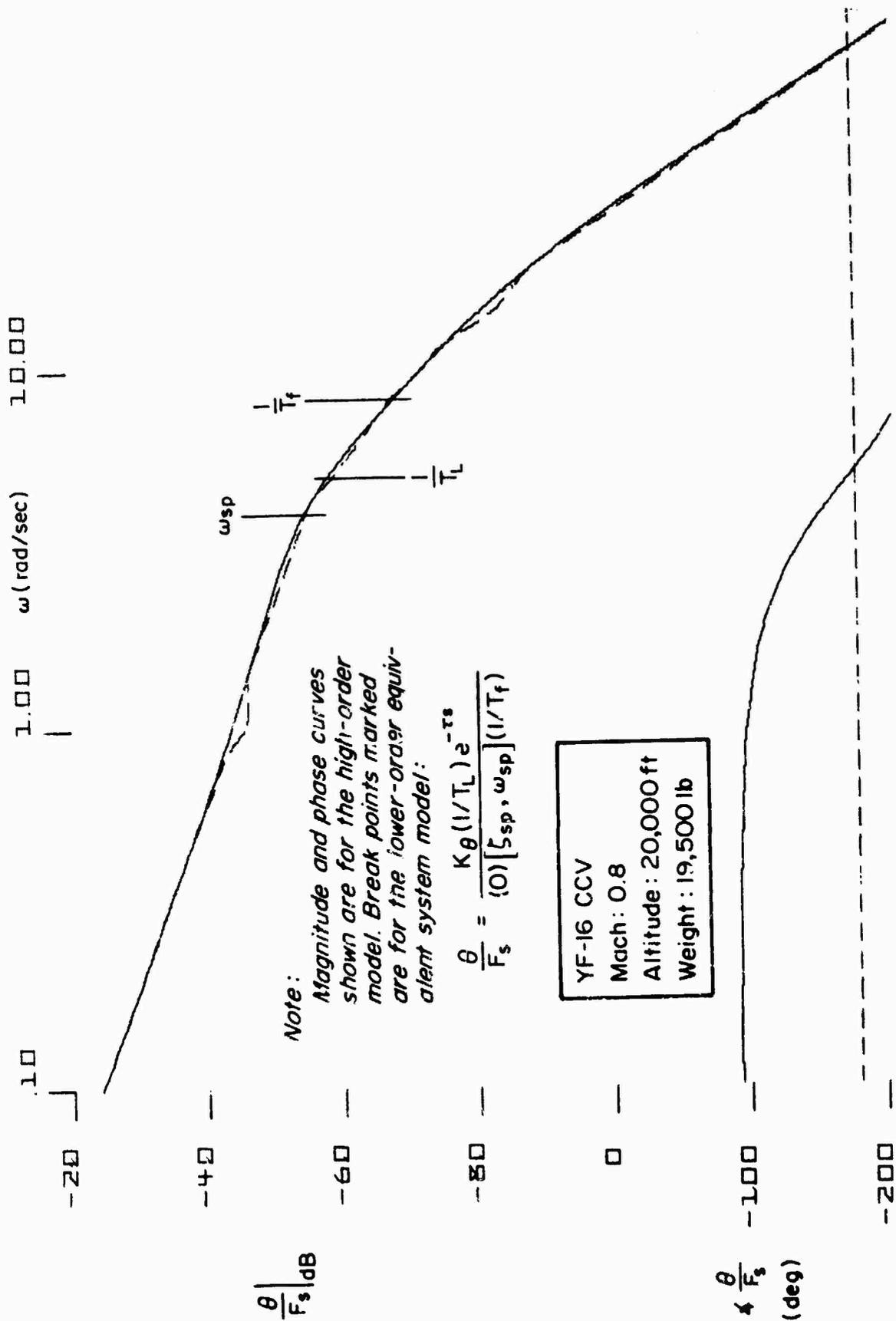


Figure 44. YF-16 Attitude Response, Conventional Flight Mode

where the approximate locations of the poles and zero are indicated in Fig. 44. The time delay term is used to account for the remaining higher-frequency actuation and filtering lags in the system.

The ratio of path angle change to attitude change for stick force inputs is shown in Fig. 45. It shows a response which is characteristic of aft-elevator control.

The piper error response can be approximated from Eq. 9, and using the conventional short-period approximation from Ref. 27.

$$\frac{\epsilon}{F_s} = \frac{K_\theta [\zeta_o, \omega_o] (1/T_L) e^{-\tau s}}{(0)^2 (1/T_{\theta_2}) [\zeta_{sp}, \omega_{sp}] (1/T_f)} \quad (26)$$

Recall from the discussion in Section II that $[\zeta_o, \omega_o]$ are variable, depending on range and speed. The piper error response for the flight condition analyzed here is plotted for three values of range, R , in Fig. 46. At high frequency, the response plot is equivalent to the attitude response plot of Fig. 44; at frequencies of 1 rad/sec or less the zeros at ω_o and the poles at $1/T_{\theta_2}$ and the origin are introduced, producing a K/s^2 -like characteristic at low frequencies. The presence of this open-loop characteristic makes it possible for the pilot to track an accelerating target (e.g., as in a circular tail chase) with a more-or-less-fixed stick deflection. Physically, this corresponds to a constantly changing path angle. The variation in the low-frequency character with range shown in Fig. 46 demonstrates the increasing effect of the path change contribution to the overall aiming error at close range. Note also that at very close range a conditional stability results, and the pilot is confined to a progressively narrower range of gain as he gets closer and closer to the target. This is a partial explanation for piloting difficulties in, for example, air-to-air refueling.

Figure 47 shows a root locus which represents the pilot's loop-closing efforts in controlling aim error. The pilot transfer function shown, namely:

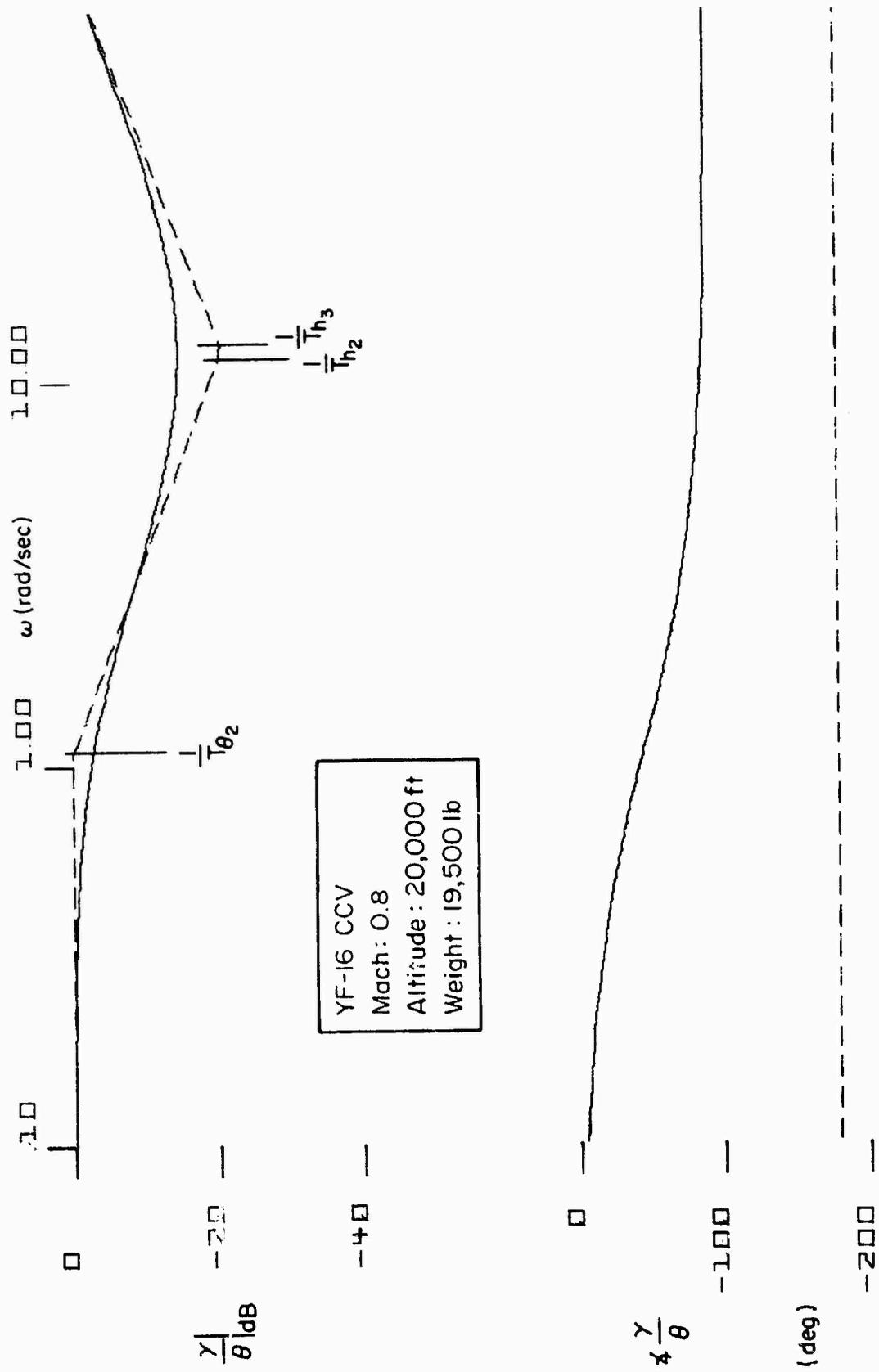


Figure 45. YF-16 CCV Variation of Aim Error Response with Range, Conventional Flight Mode

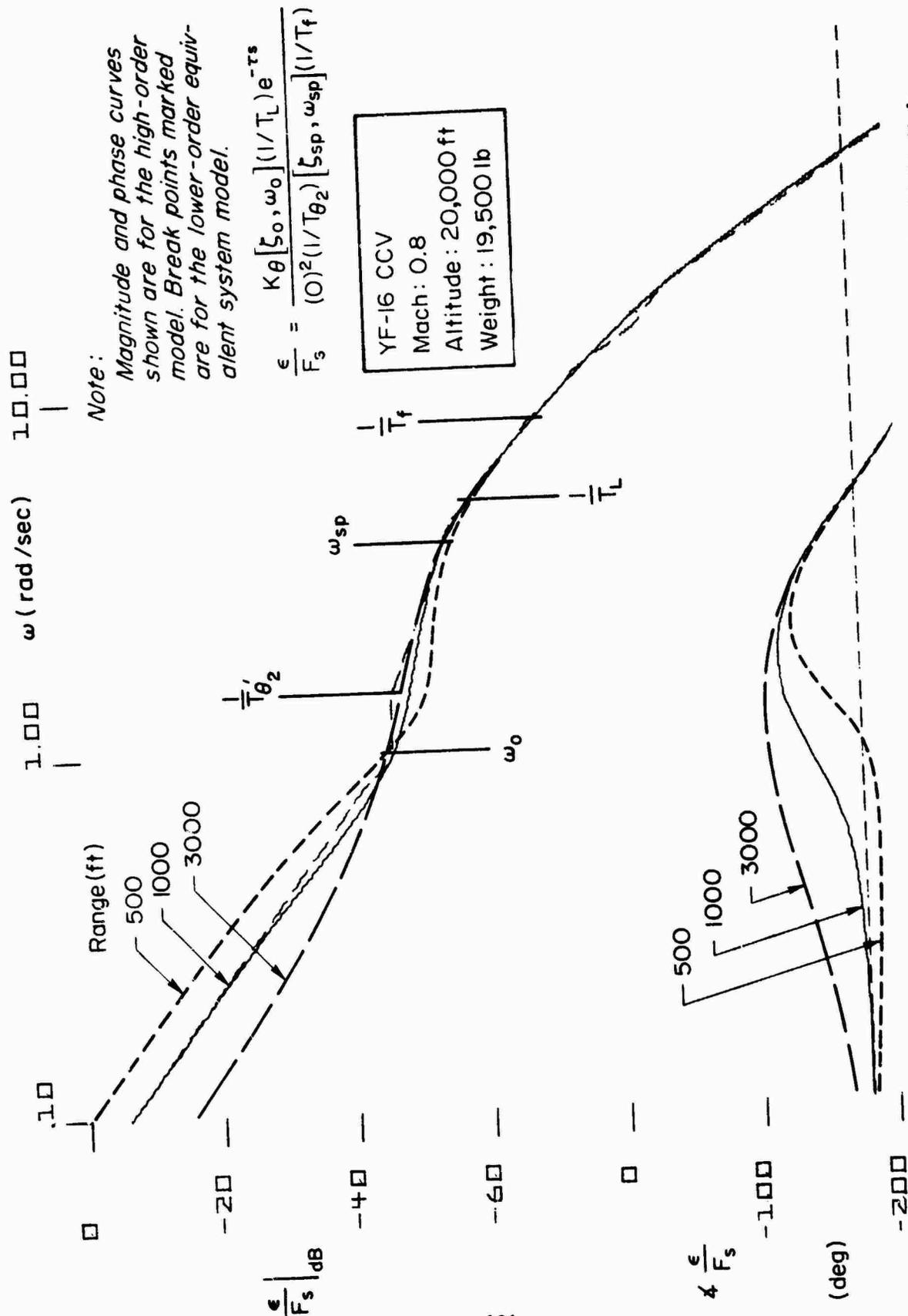


Figure 46. YF-16 CCV Variation of Aim Error Response with Range, Conventional Flight Mode

Pilot Loop Closure, Conventional Flight Mode

$$Y_c = \frac{e}{F_s} = \frac{5.097(.023)(1)[.57, .96](5)(10)(15)^2}{(0)^2(.023)(.84)(1.49)[.62, 4.45](8.3)[1.0, 11.5][.65, 19.4]}$$

$$Y_p = K_p(s + 3)e^{-0.1s} \quad (\text{Maximum equalization effort for } Y_c)$$

Range : 1000 ft

YF-16 CCV
 Mach : 0.8
 Altitude : 20,000 ft
 Weight : 19,500 lb

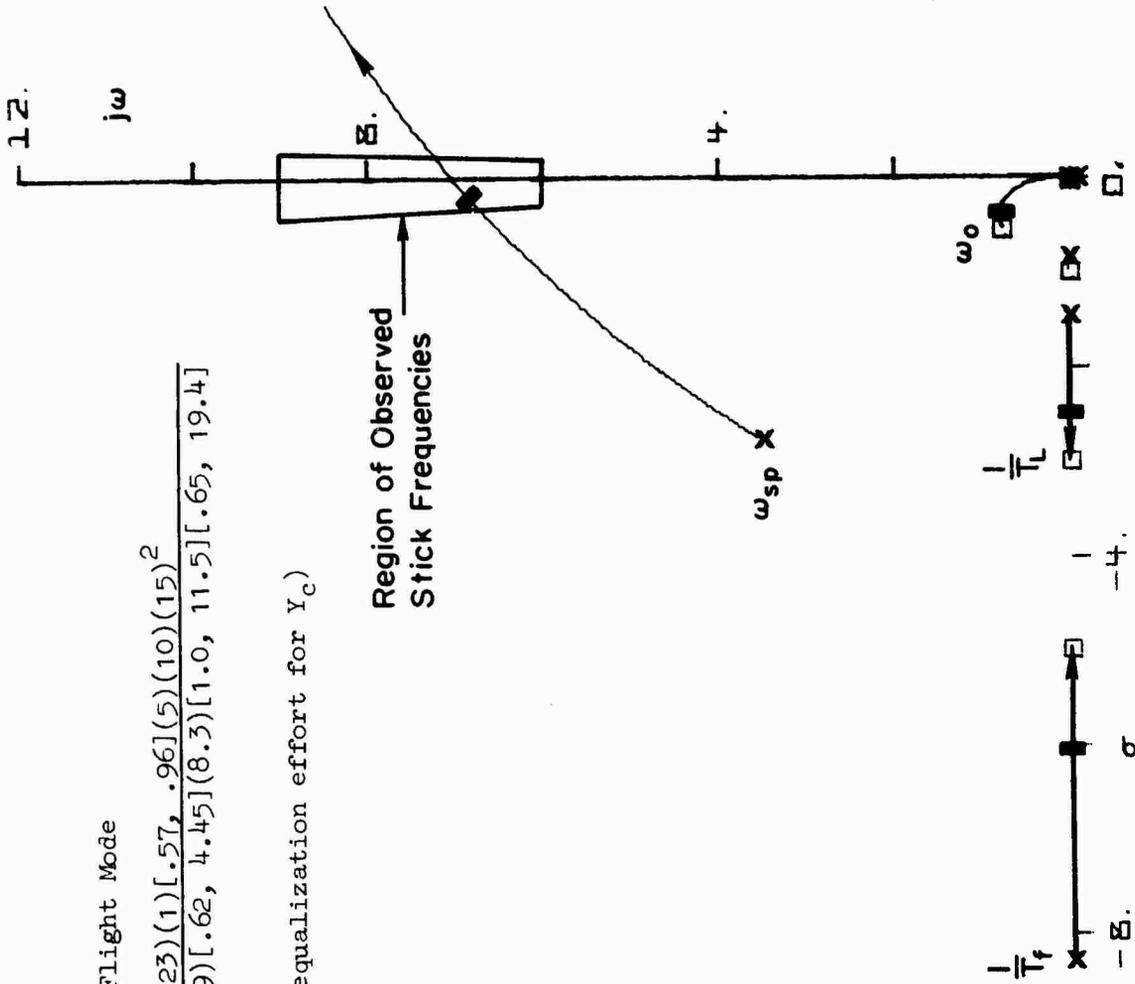


Figure 47. Pilot Loop Closure, YF-16 CCV Conventional Flight Mode

$$Y_p = K_p(s + 3)e^{-.1s} \quad (27)$$

represents a maximum effort on the pilot's part with full motion cues and moderate lead equalization. This allows crossover at near-zero phase margin at a frequency corresponding to that seen in flight (Fig. 48; note the pitch attitude and longitudinal stick force traces). To operate at higher gains (and better error nulling capability) would require less effective lag in the attitude response characteristic of the aircraft. In this particular case, the high-frequency lag characteristic of the YF-16 CCV attitude response to the stick limits the pilot.

The effect of decreasing range can be inferred from Fig. 47 by considering the effect on the root locus as ω_0 is increased. The zero moves upward parallel to the $j\omega$ -axis (the total damping remains fixed; $2\zeta_0\omega_0 = 1/T\theta_2$) as the range decreases. The locus that terminates in this zero bulges farther and farther into the right half-plane, and so it becomes progressively more difficult to stabilize the low-frequency oscillatory mode that results using only feedback of the piper error. At short range the pilot will find it necessary to revise his technique in such a way as to stabilize this path mode while retaining adequate stability of the short-period mode. Typically he will either shift his aimpoint forward (e.g., in air-to-air refueling use the belly of the tanker as a reference rather than the drogue) or adopt a multiple-loop closure technique: pitch attitude as an inner loop, path deviation as an outer loop.

Attention is now directed to the A_N or direct-lift flight mode. This mode of response allows the pilot to command a normal acceleration in concert with a pitch rate so as to develop changes in both path angle and attitude without any change in angle of attack. Response lags are introduced by actuator dynamics, filtering and the attitude responses. For the YF-16 CCV, the response can be approximated in the mid-frequency range by:

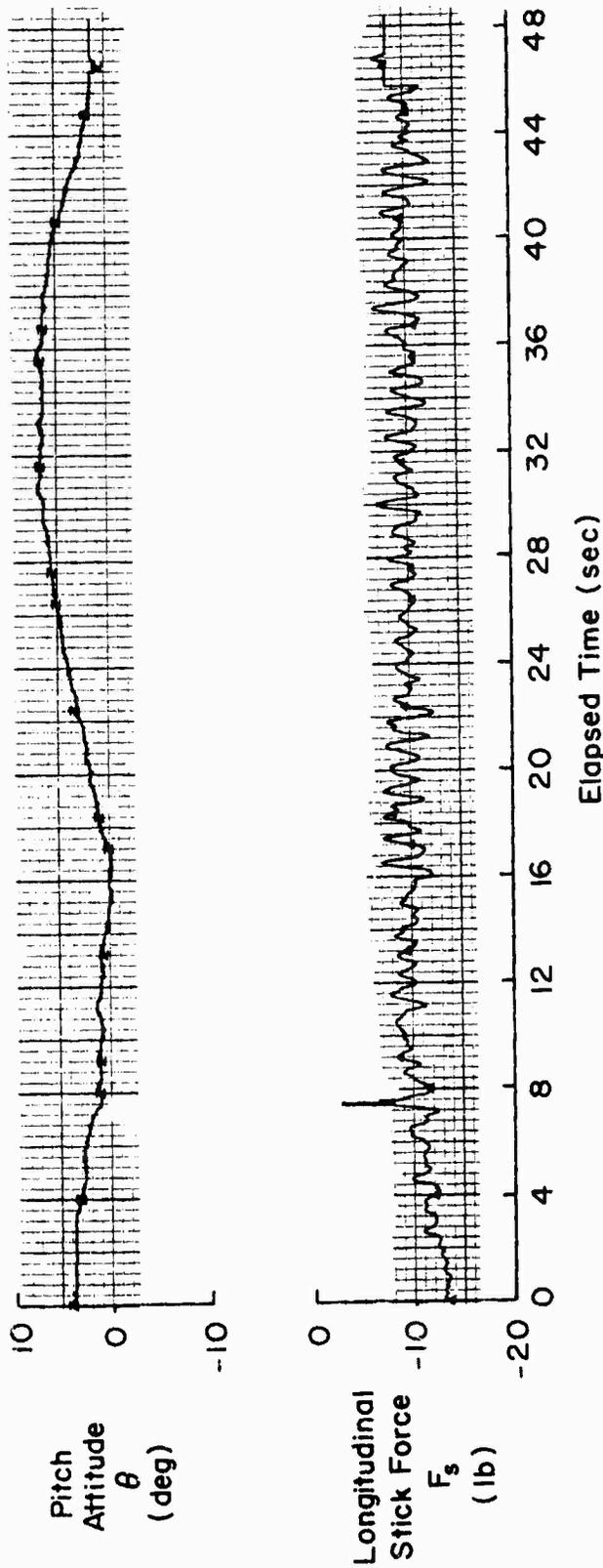


Figure 48. YF-16 CCV Control Activity, Conventional Flight Mode, 3 g Turn (Ref. 18)

$$\frac{Y}{F_b} = \frac{K_{AN}}{(0)(1/T_f)} = \frac{\theta}{F_b} \quad (28)$$

This results in an approximate aim error response of:

$$\frac{\epsilon}{F_b} = \frac{K_{AN}(s + U_o/R)}{s^2(s + 1/T_f)} \quad (29)$$

The complete transfer function is plotted in Fig. 49.

The key feature here is the somewhat reduced high-frequency lag in what otherwise is a response similar to that of the conventional airplane. The pilot now has two means of controlling the aim error: the conventional responses which have more authority but are somewhat slower; and the direct lift responses which are faster (less lag) but have less authority. Which will the pilot use?

The Ref. 17 flight records examined (not shown here) suggest intermittent use of the direct lift control button and attitude crossover frequencies equivalent to that of the conventional airplane. The pilot cannot or will not take advantage of the higher bandwidth offered by the A_N controller; its use is subordinated to the stick. He uses this technique rather than attempting to coordinate the two manipulators in the same range of frequencies.

The maneuver enhancement flight mode can be thought of as a combination of conventional and direct lift using the normal sidestick. Figure 50 shows the aim error response of this mode with plots of the other two superimposed. Ignoring the gain difference in the direct lift mode (different manipulator), the maneuver enhancement configuration falls part way between the two. In particular, its phase curve shows somewhat reduced lag at high frequencies relative to the conventional flight mode using the same sidestick.

Figure 51 shows flight records for the maneuver enhancement mode. The pilot is able to control at a frequency slightly in excess of 10 rad/sec, significantly higher than before and indicative of improved aim error nulling capability. The higher-frequency lags are still limiting, but not to the same extent as before. Without the harmony and

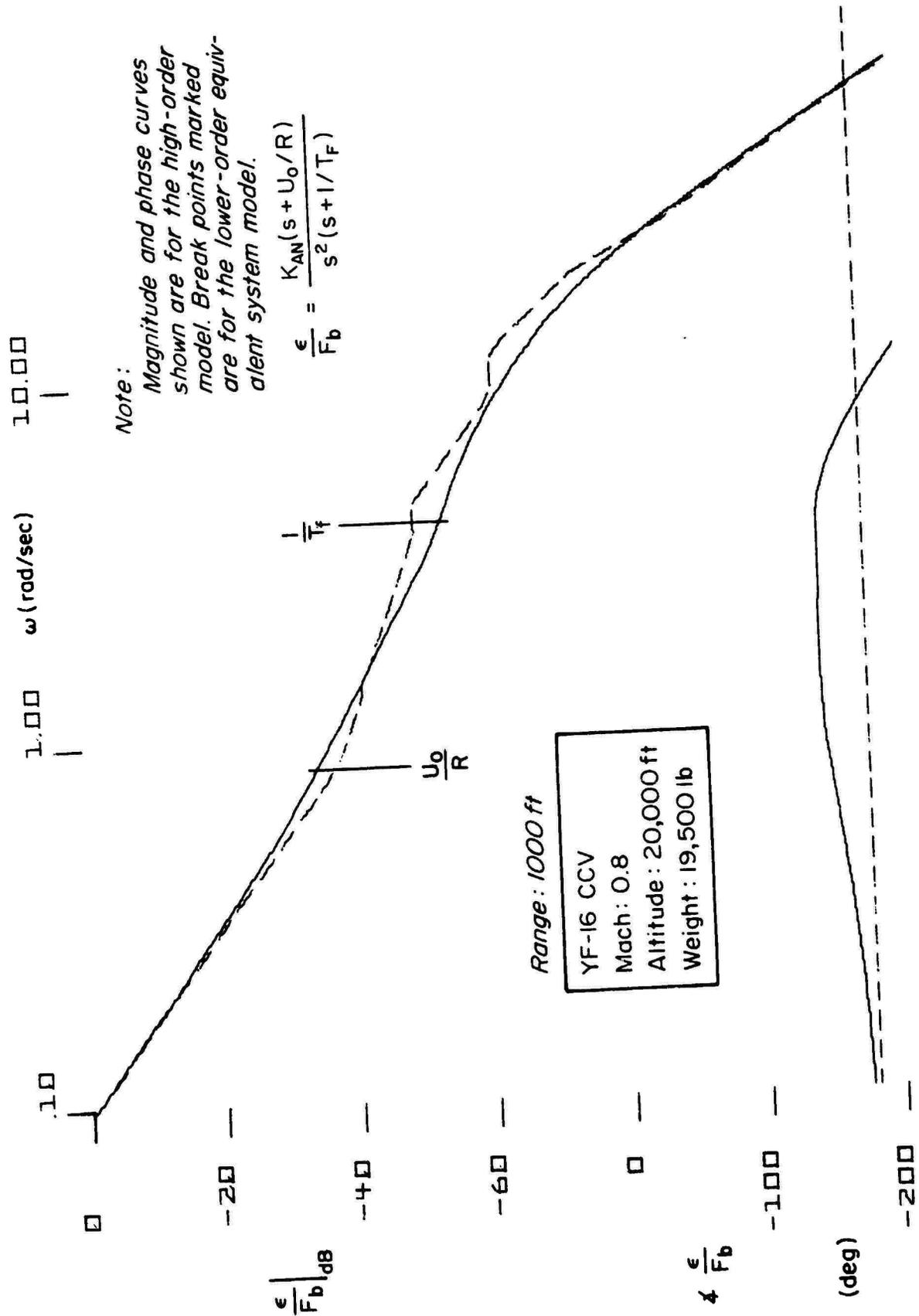


Figure 49. YF-16 CCV Aiming Error Response, Direct Lift Mode

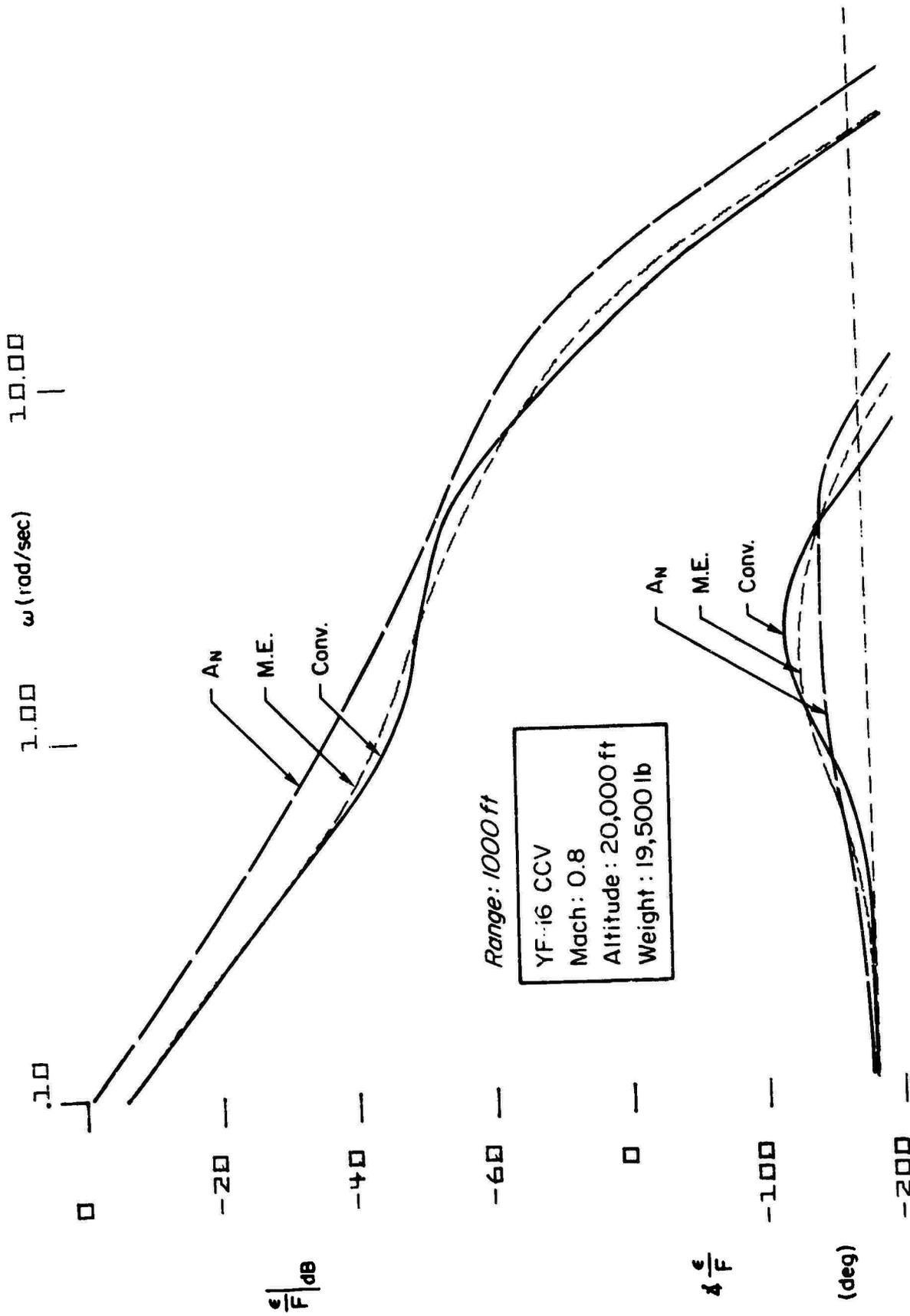


Figure 50. Comparison of YF-16 CCV Aim Error Response

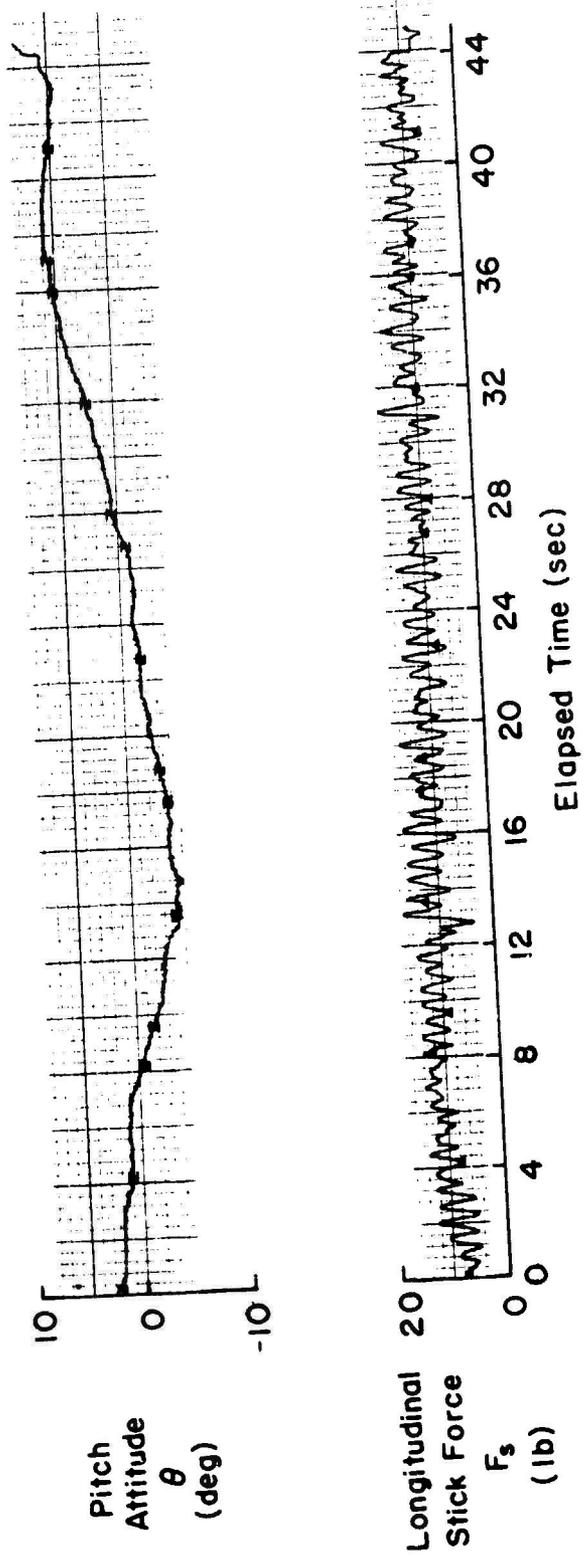


Figure 51. Control Activity, YF-16 CCV Maneuver Enhancement Mode, 3 g Turn (Ref. 18)

frequency separation problem inherent in the separately actuated direct lift modes, the pilot is able to take advantage of the small improvement in the response character. For this particular task the result also suggests that bandwidth improvement in the conventional aircraft attitude response, regardless of how achieved, can yield improved performance.

α_1 (Pointing) and α_2 (Translation) Modes

Both of these modes significantly alter the relationship between the attitude and flight path angle contributions to the pipper error response. Ideally the path angle contribution is zero for the pitch pointing, α_1 , mode; the attitude contribution is zero for the vertical translation, α_2 , mode. In practice, as exemplified by the YF-16 CCV, the situation only approaches this ideal; there are significant departures from it in the frequency range of interest to the pilot.

For the pointing mode, the button commands a pitch attitude change, resulting in a response characteristic approximated by (if the path angle change is indeed zero):

$$\frac{\theta}{F_b} = \frac{K_{\alpha_1}}{[\zeta_{sp}, \omega_{sp}](1/T_f)} = \frac{\epsilon}{F_b} \quad (30)$$

However, the aim error response for the YF-16 CCV shows a K/s-like characteristic at low frequencies as shown in Fig. 52. This comes about because of impurities in the response; there is a residual change in path.

If we presume tracking using the button alone, this response characteristic suggests the following pilot difficulties:

- He cannot track an accelerating target without continually increasing button deflection to the point of running out of authority.
- A fixed target gives trouble in that after initially establishing an aim, the pipper drifts through the desired aim unless the pilot commands additional aircraft response. Again, he may reach an authority limit.

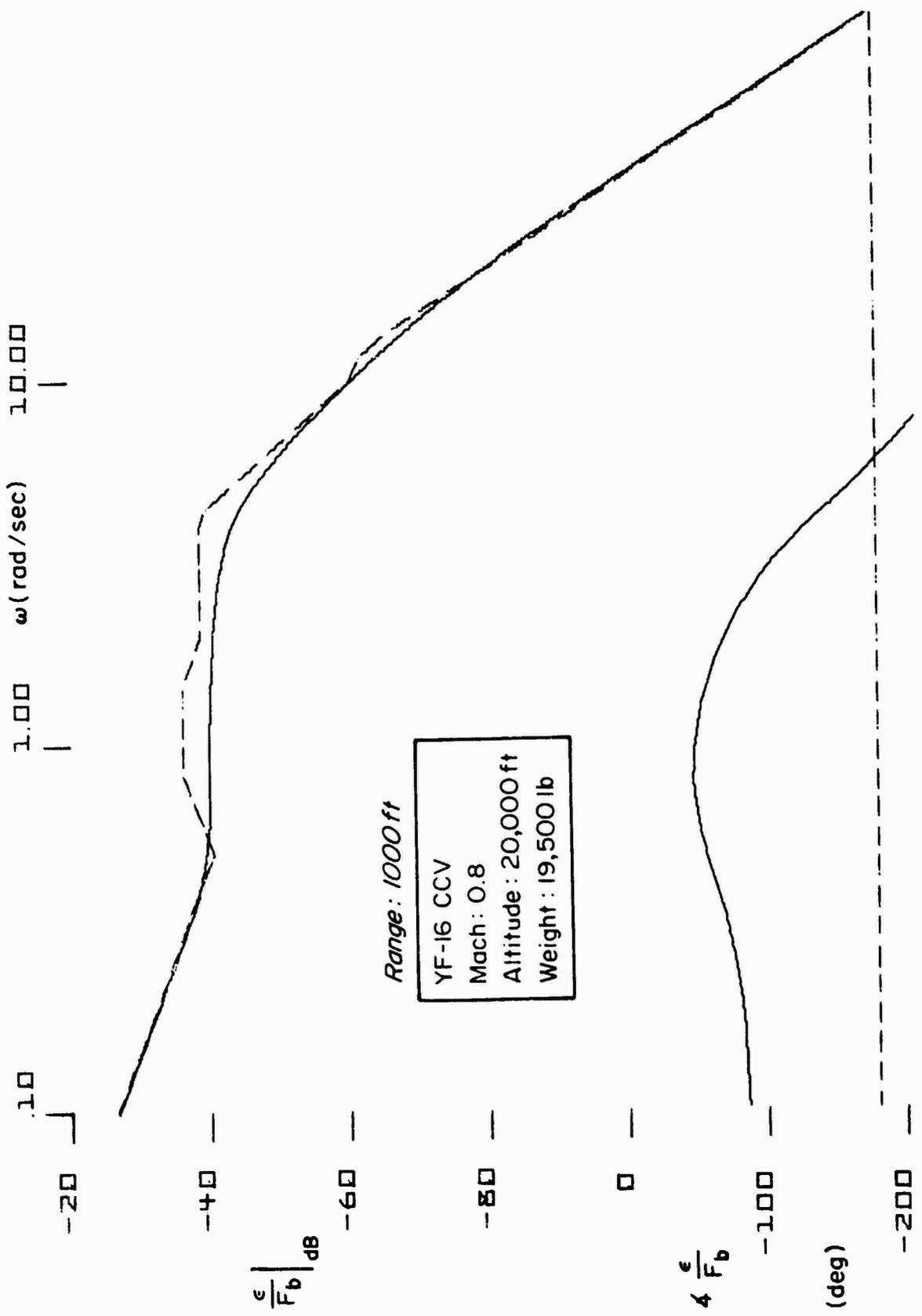


Figure 52. YF-16 CCV Aim Error Response, α_1 Flight Mode

- The proportional response character at higher frequencies requires either low-frequency lag equalization to establish the desired K/s characteristic (that additional lag would severely limit bandwidth attainable) or higher-frequency lead equalization. The latter leads to poor error regulation in the frequency region where the open-loop response is flat.

Each one of these difficulties is overcome by reverting to the conventional flight mode, which in fact is what the flight test tracking results suggest. The crossover frequency in pitch is equivalent to that seen in conventional flight, and most of the control activity is on the stick, not the force button.

The pilots' request for integral implementation noted in Ref. 18 (pointing rate proportional to button force) would result in an aiming response characteristic which resembles the conventional aircraft response, except with narrower response bandwidth. Further, for tracking an accelerating target, the conventional low-frequency path response character is a desired attribute, not one to be eliminated.

In the case of the translational mode, the aim error is (ideally):

$$\frac{\epsilon}{F_b} = \frac{U_0/R}{s} \frac{\gamma}{F_b} = \frac{K_{a2}}{[s + 1/T_{\theta_2}][s + 1/T_f]} \quad (31)$$

The response bandwidth is limited by $1/T_{\theta_2} = -Z_w$; the developing angle of attack generates a force which resists changes in the translational velocity. T_f is a higher-frequency filter or actuation lag.

Unfortunately, there are "residual" attitude changes which contribute in two ways. First, higher-frequency attitude changes alter the path with greater effect than the direct lift control response at these higher frequencies. The result is a bimodal response in path angle, Fig. 53, where ω_1 represents the approximate frequency above which the γ response is caused by attitude changes, primarily.

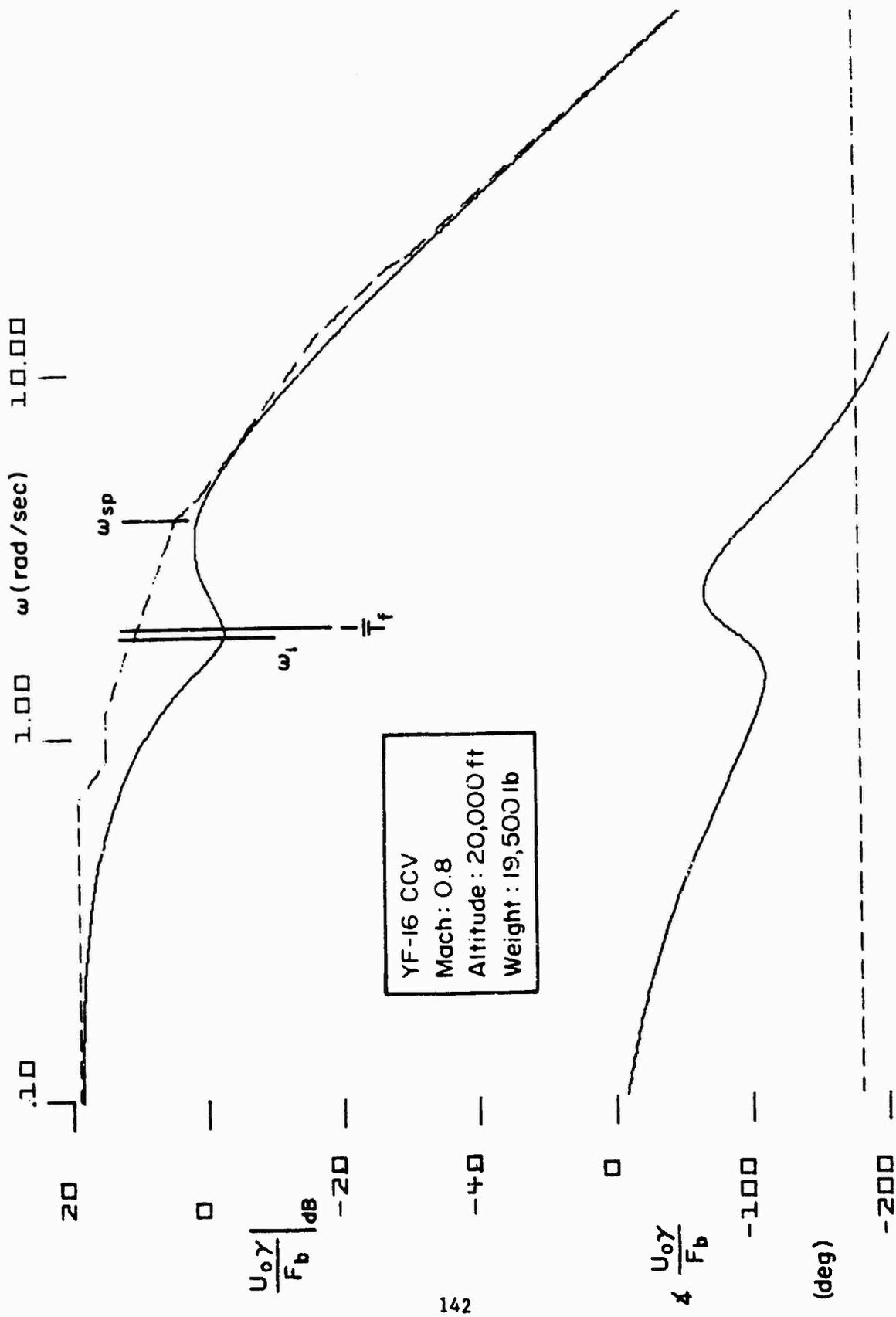


Figure 53. YF-16 CCV Path Angle Response, Vertical Translation Mode

The second effect is caused by the attitude response contribution to the aim error at moderate to long range. In the YF-16 CCV implementation this contributes a non-minimum-phase zero in the vicinity of 1 rad/sec (Fig. 54). While there is significant pipper error response above 1 radian, it is of the wrong sign.

Mode "Purification"

In view of the foregoing results, a short investigation was conducted to determine what implementation changes might be made to "purify" or more perfectly decouple the response characteristics. This presumes that such purification is both desirable and necessary. The vertical translation mode was considered first, but the same analysis techniques apply to the pointing mode and also (though the need is not obvious) to the A_N mode of response.

For purposes of analysis it is useful to work with the equivalent block diagram (Fig. 55) discussed previously, in which these crossfeed terms are separately identified as Y_{CF} . Ideally, in the vertical translation (α_2) mode the FCS constrains pitch attitude change to zero, i.e.,

$$\frac{\theta}{\delta_{FC}} = \frac{N_{\delta_L}^{\theta \dots} + Y_{CF} N_{\delta_e}^{\theta \dots}}{(T_{AS} + 1) \Delta^{\dots}} \equiv 0 \quad (32)$$

Here the triple prime (\dots) serves to indicate that the pertinent numerators and the denominator are for the three feedback loops being closed. Since all feedback loops of the basic FCS go only to the horizontal tail, the augmented horizontal tail numerator $N_{\delta_e}^{\theta \dots}$ is the same as the bare airframe numerator $N_{\delta_e}^{\theta}$. The flap numerator $N_{\delta_L}^{\theta \dots}$ is modified by the basic FCS; it may be written explicitly (in the nomenclature of Appendix D) as:

$$N_{\delta_L}^{\theta \dots} = N_{\delta_L}^{\theta} + G_{\alpha}^{\delta_e} N_{\delta_L}^{\theta \alpha} \delta_e + G_{a_n}^{\delta_e} N_{\delta_L}^{\theta a_n} \delta_e + \cancel{G_{\theta}^{\delta_e} N_{\delta_L}^{\theta \theta} \delta_e} \quad (33)$$

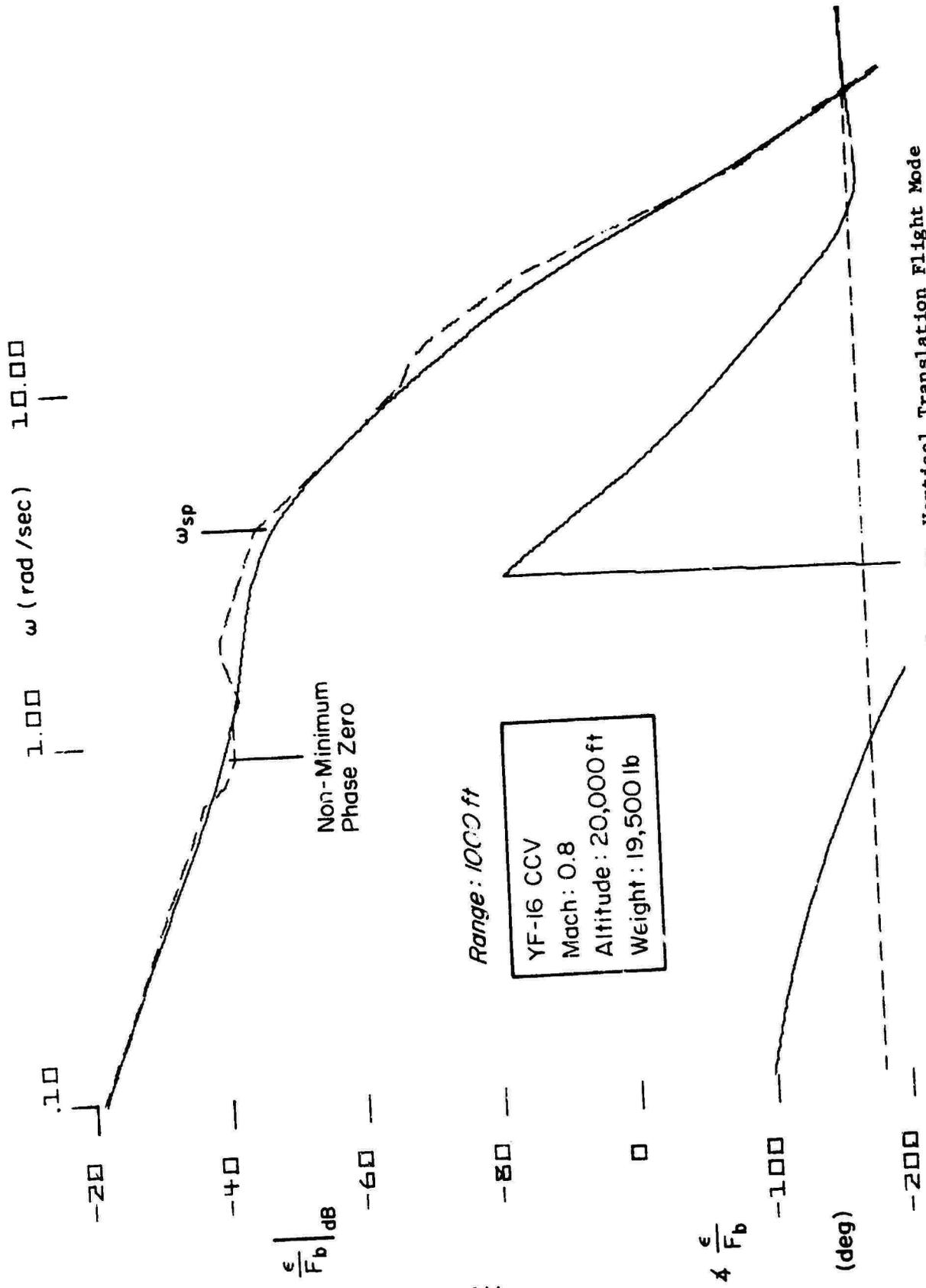


Figure 54. YF-16 CCV Aim Error Response, Vertical Translation Flight Mode

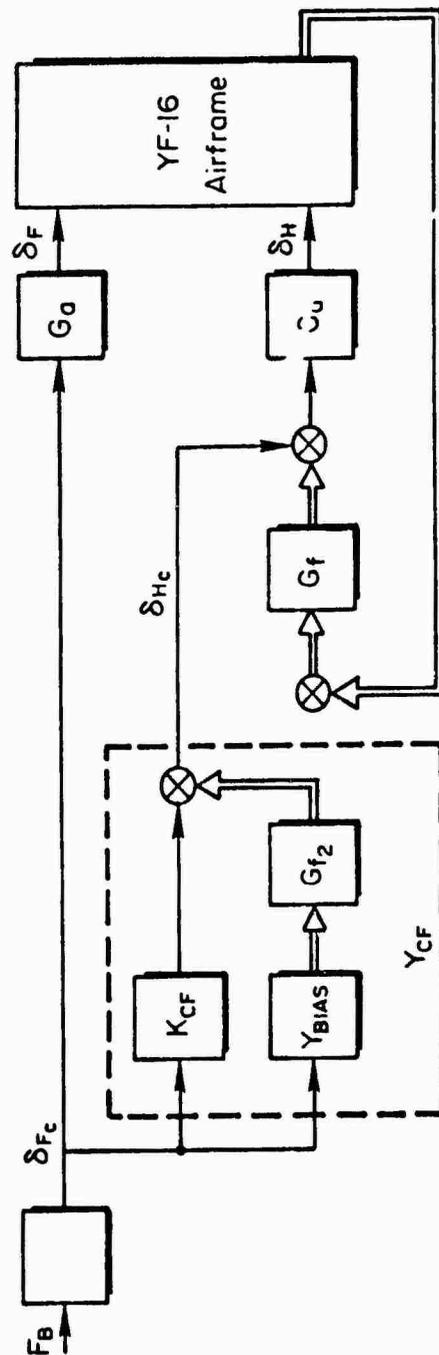


Figure 55. Equivalent DFC Control Structure Used in Analyses of Vertical Translation Mode

where $N_{\delta_L}^\theta$ is the bare airframe numerator and $G_{\alpha}^{\delta e}$ and $G_{a_n}^{\delta e}$ are the feedback transfer functions of the α and a_n loops, respectively. For Eq. 32 to be identically zero, the numerator must be zero and thus we may solve for Y_{CF} :

$$Y_{CF} = \underbrace{-\frac{N_{\delta_L}^\theta}{N_{\delta_e}^\theta}}_{\text{Bare Airframe Contribution}} - \underbrace{\frac{1}{U_0} G_{\alpha}^{\delta H} - s G_{a_n}^{\delta H} \frac{N_{\delta_L}^\theta w}{N_{\delta_e}^\theta}}_{\text{FCS Contribution}} \quad (34)$$

When this crossfeed, which is dominated by factors originating in $N_{\delta_e}^\theta$ and $N_{\delta_L}^\theta$, is compared with the crossfeed used in the YF-16 CCV, the difference between the two is between 3 and 6 dB in gain, less than 15 degrees in phase.

This ideal crossfeed was approximated by an expression that ignores the higher-frequency dynamics near and above 10 rad/sec, viz.:

$$Y_{CF} = \frac{\overbrace{-0.2982(0.0188)(1.664)}^{1/T_{\theta 1L} \quad 1/T_{\theta 2L}}}{\underbrace{(0.0260)(1.08)}_{1/T_{\theta 1H} \quad 1/T_{\theta 2H}}} \quad (35)$$

When used with the remainder of the Fig. 55b block diagram this crossfeed results in a more "purified" path angle response in Fig. 56, in the sense that Fig. 56 more closely resembles the idealized response for this mode than does Fig. 53. However, the response, while more monotonic in character, actually has more lag in the short-period region than that shown in Fig. 53 because the attitude contribution has been removed.

The aim error response when using this improved crossfeed has less gain and less lag in the vicinity of 1 to 2 rad/sec, Figs. 57 vs. 54. This response character represents an improvement in that the response

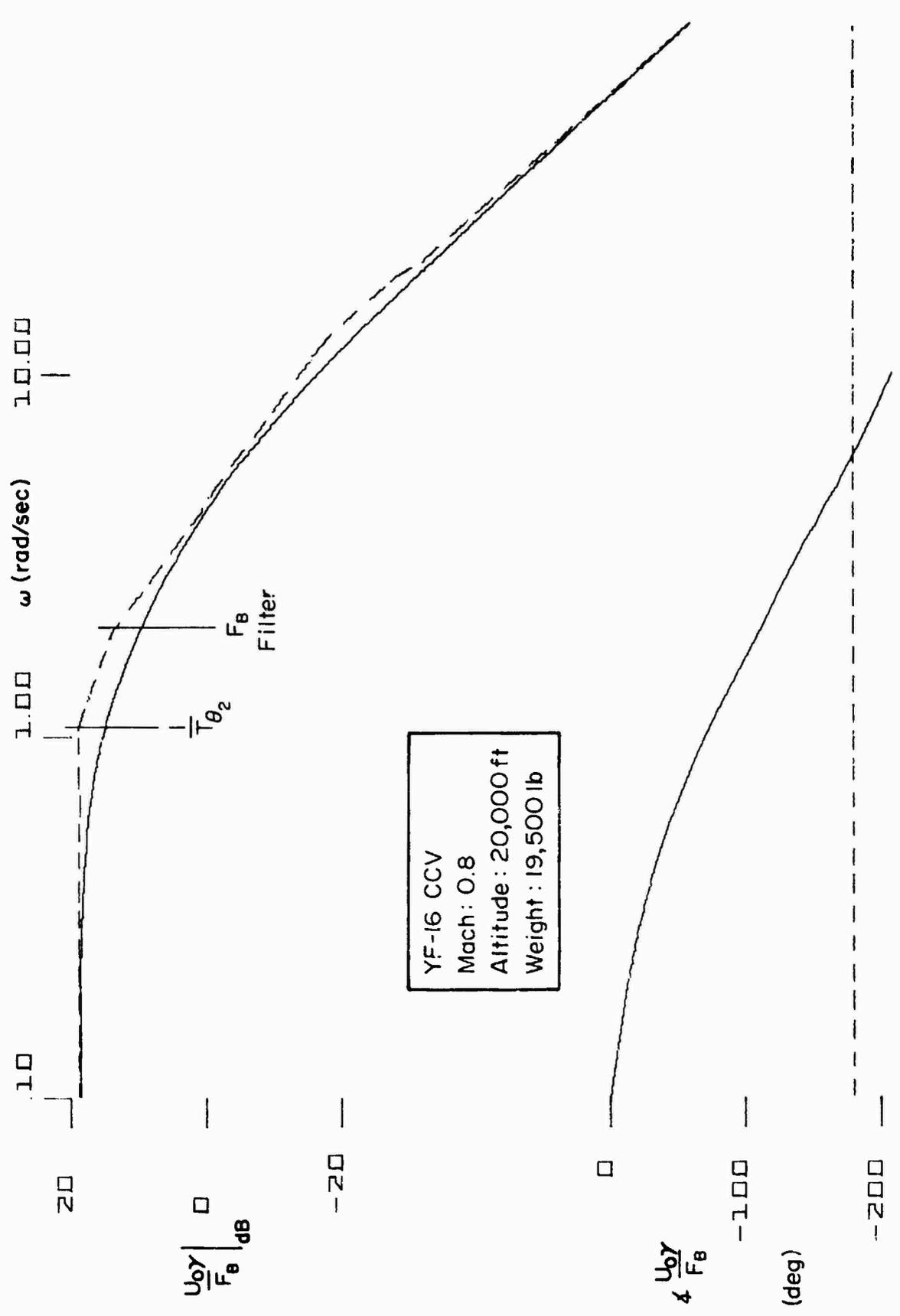


Figure 56. Path Angle Response, Vertical Translation Mode with Ideal Crossfeed (Eq. 35)

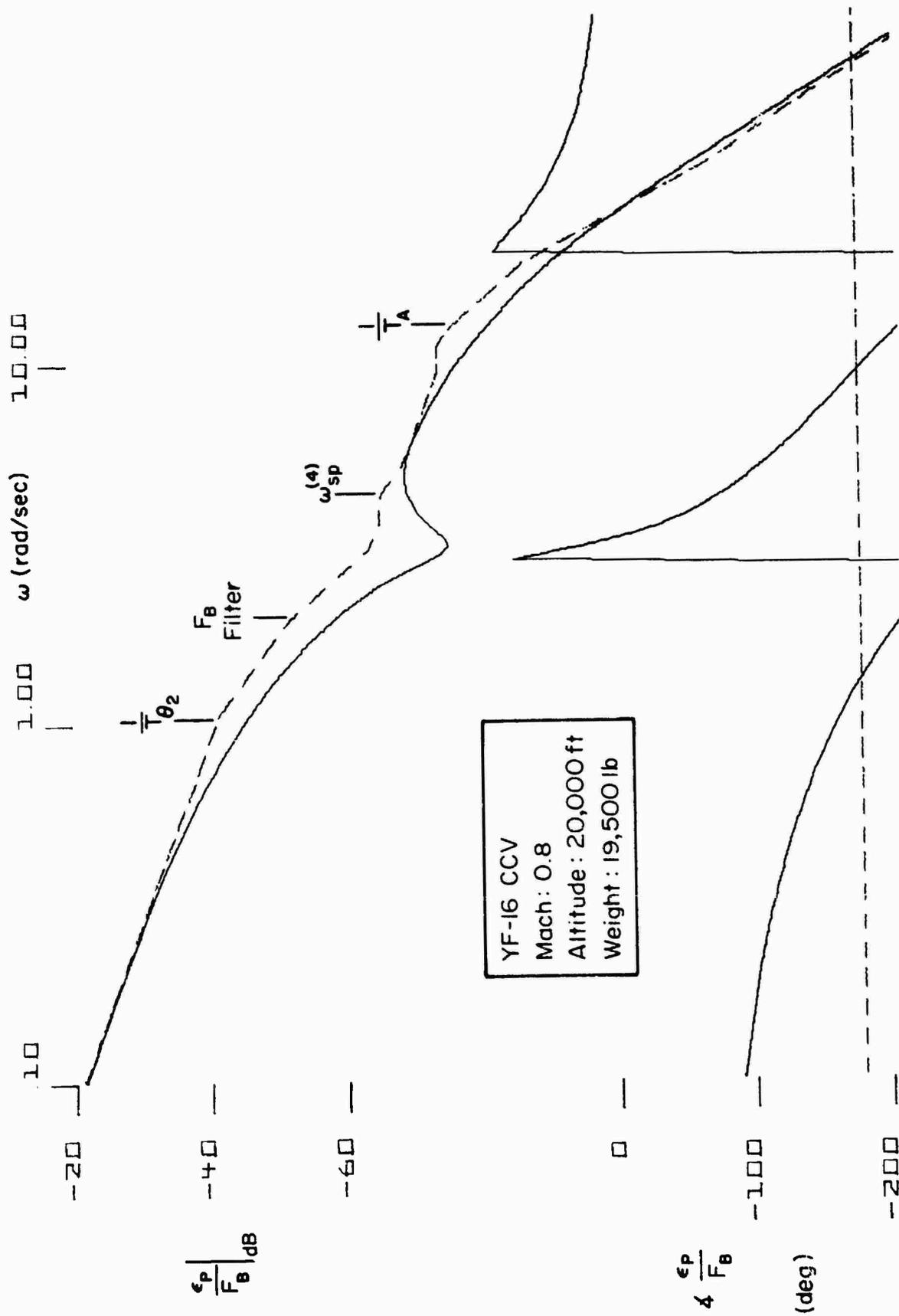


Figure 57. Aim Error Response, Vertical Translation Mode with Ideal Crossfeed (Eq. 35)

to the button is more "predictable." However, it is still relatively slow as shown by the time traces given in Fig. 58 and will not allow the pilot to close the loop tightly; he is limited by the complex non-minimum phase zero near 3 rad/sec, even if he is successful in generating the lead equalization demanded by the path response and filtering lags at lower frequency.

The non-minimum phase zero can be removed by more closely matching the ideal crossfeed characteristics. This is demonstrated in the subsequent discussion of the β_2 mode. However, in practice some mismatch is likely; it will be extremely difficult to actuate flaps and elevator in such a way as to avoid attitude change.

Attention is briefly directed to the α_1 , pitch pointing, mode. An expression similar to Eq. 32 holds with θ replaced by γ , viz.:

$$\frac{\gamma}{\delta_{FC}} = \frac{N_{\delta_L}^{Y} + Y_{CF} N_{\delta_e}^{Y}}{(T_{rs} + 1)\Delta} \equiv 0 \quad (36)$$

The resulting ideal crossfeed is plotted in Fig. 59 and compared both with that used in the YF-16 CCV. In the low to intermediate frequency region of interest, where the "residual" path angle response contributes to the aim error, the crossfeed gain is increased by approximately 6 dB. The resulting aim error is shown in Fig. 60. When compared with Fig. 52, the "purification" effects are obvious: at this flight condition the low-frequency drift in the pointing response has been eliminated with a 6 dB crossfeed gain adjustment.

Finally, a similar analysis for the A_N mode results in a crossfeed defined by minimum angle-of-attack response. Figure 61 shows the resulting crossfeed and compares it with that used in the YF-16 CCV. The match is excellent except at the very lowest frequencies where the pilot would be forced to use the throttle. The need for purification is not so great here, primarily because the pilot is not sensible of angle-of-attack changes (as distinct from normal load factor).

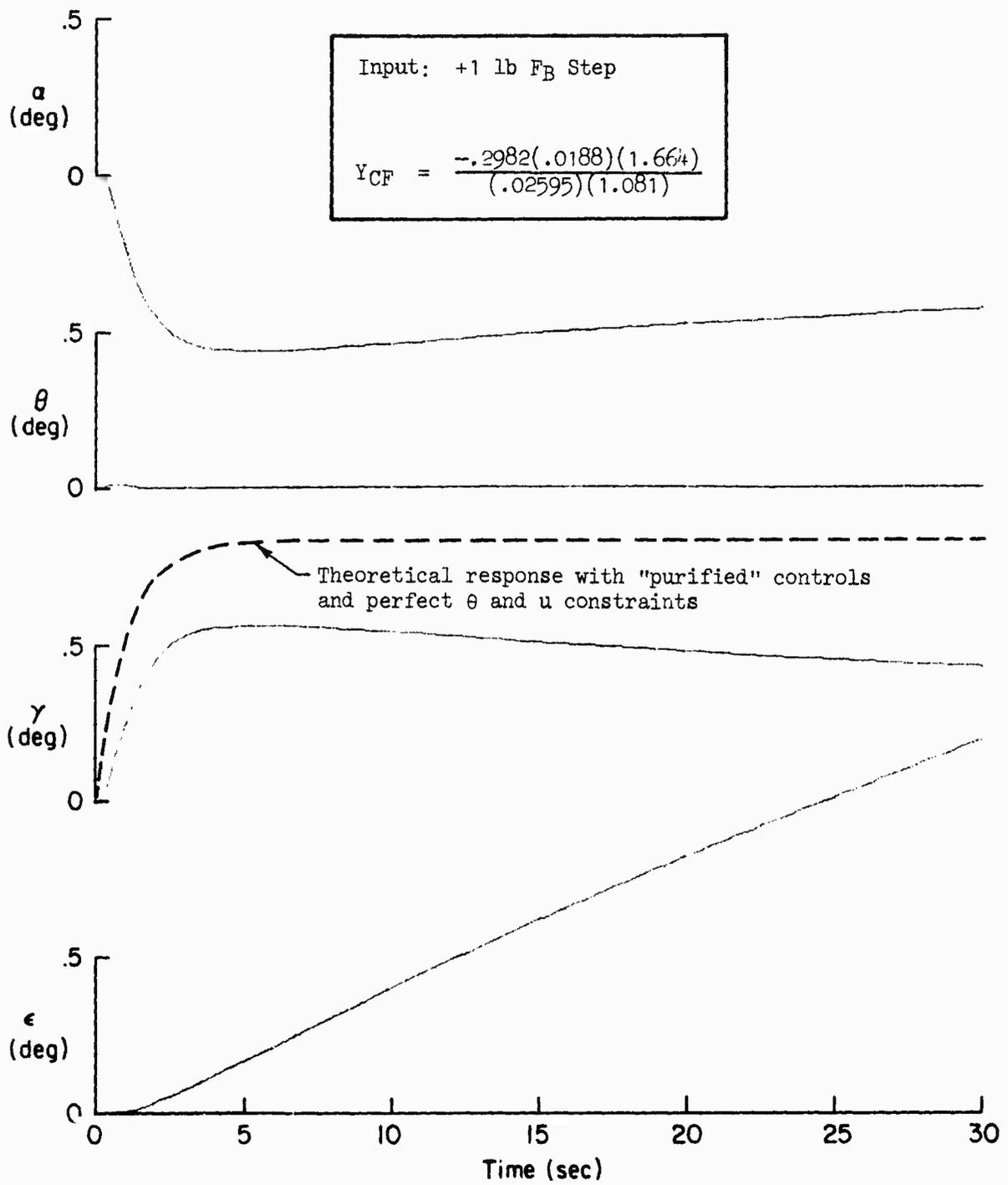


Figure 58. Response to +1 lb F_B

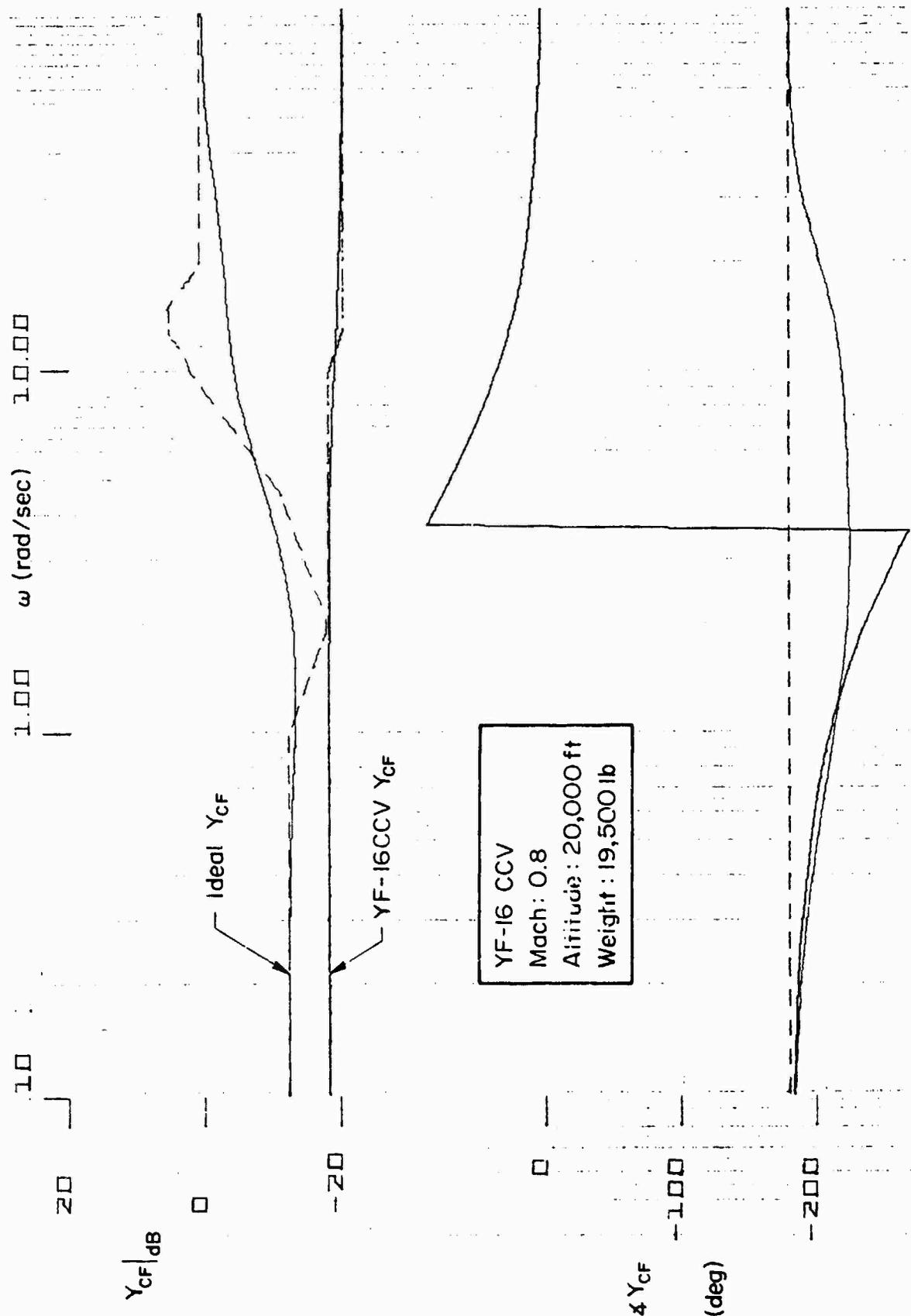


Figure 59. Comparison of Actual and Ideal Crossfeeds, Pitch Pointing Mode

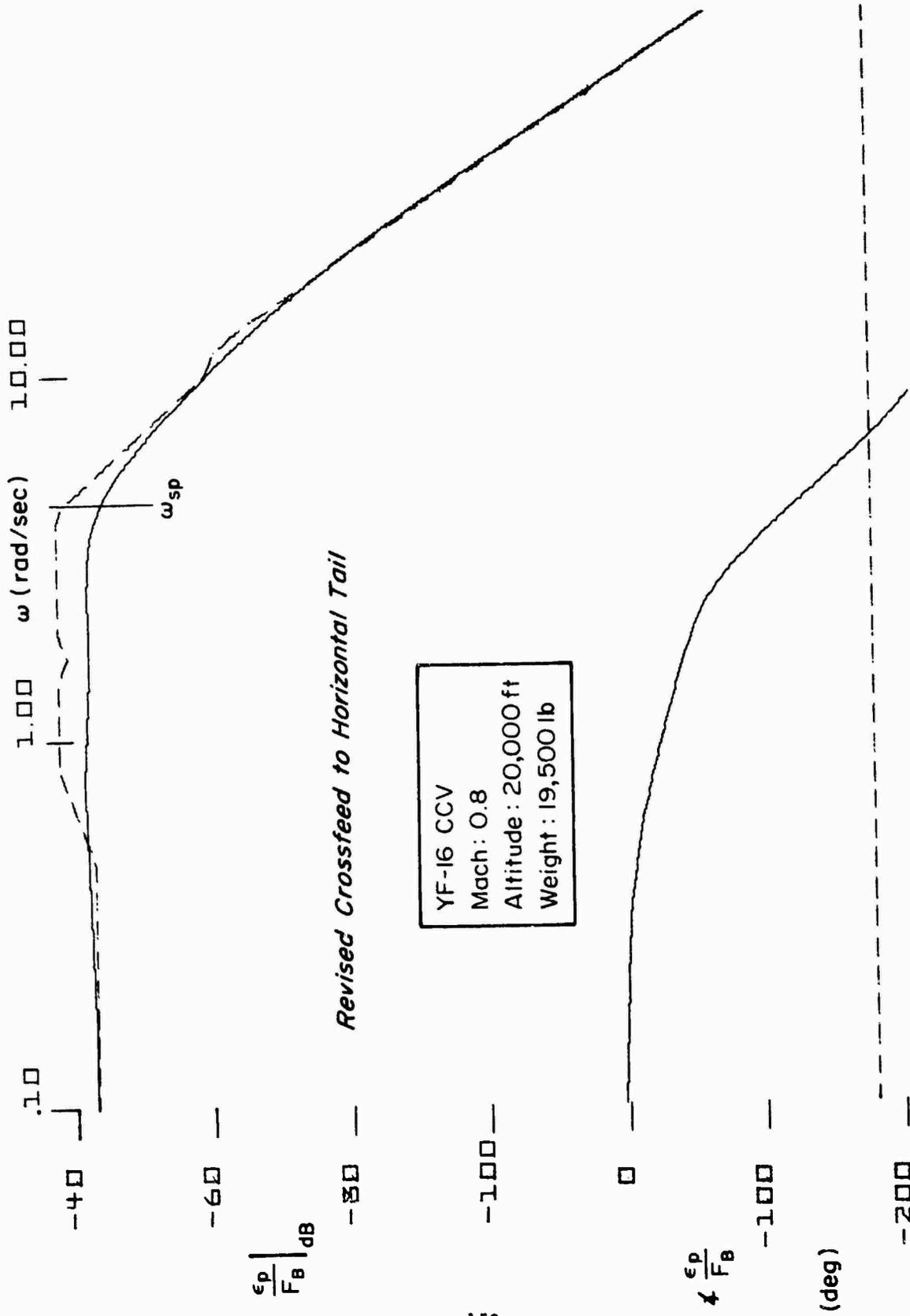


Figure 60. Aim Error Response, Pitch Pointing Mode, with Improved Crossfeed

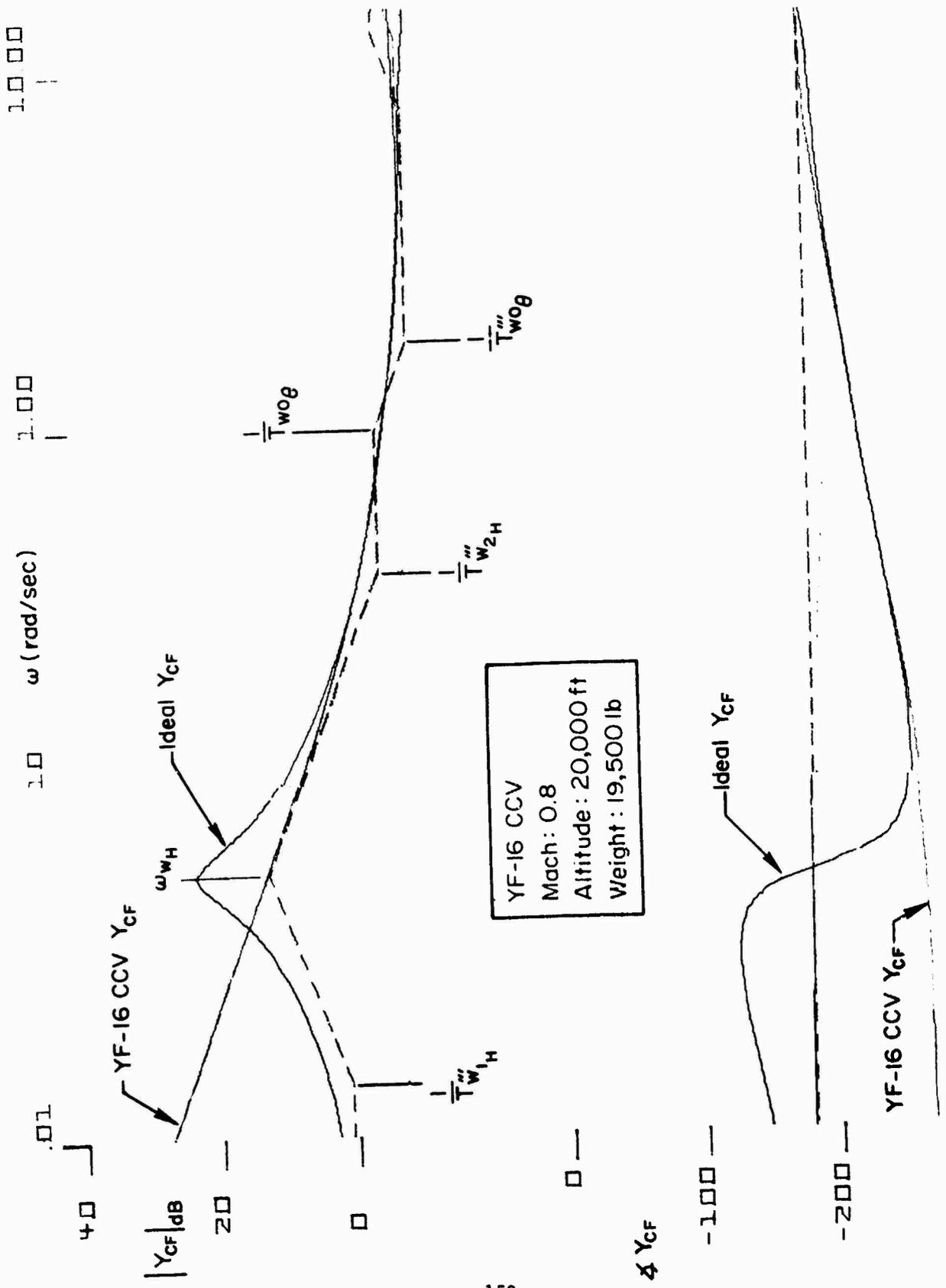


Figure 61. Comparison of Actual and Ideal Crossfeeds, AN Mode

YF-16 CCV LATERAL-DIRECTIONAL ANALYSIS

This subsection presents the analysis of the lateral-directional response properties of the YF-16 CCV for the flight condition (0.8 Mach/20,000 ft/19,500 lb) and model given in the first subsection of this appendix.

Conventional Mode

The pilot's task, as in the longitudinal axis, is to minimize his aiming error. With conventional aircraft he attempts to match his adversary's roll angle — the orientation of his lift vector — so that upon pulling (or pushing) on the stick the target is acquired longitudinally for brief intervals of time. The key pilot loop closure is $\phi \rightarrow F_S$.

Rolling is the most common way to achieve a target rapidly and to track it accurately. Also, examination of the dynamics of an outer heading loop closure with an inner roll loop closure as might be used in landing approach shows the responses to be limited in bandwidth by the achievable roll closure bandwidth. In the case of the YF-16, adverse yaw characteristics also limit the bandwidth.

The roll response to lateral stick force is shown in Fig. 62. The desired K/s-like characteristic is seen out to moderate frequencies, at which point filter lag (the break point at $1/T_f$) begins to contribute phase lag to the response. This lag approximates the more complex, non-linear stick filter network used in the YF-16.

Direct Side Force (A_y) Mode (Wings-Level Turn, WLT)

With the unconventional capability of moving laterally to null pipper error, the pilot now can use a different tracking technique. For the following discussions we will assume a sight depression angle of zero, a situation which reasonably well approximates the facts for air-to-air gunnery. In CCV modes which constrain roll attitude during the maneuver, the effects of sight depression angle — the so-called pendulum effect (Ref. 12) — should not appear in any event.

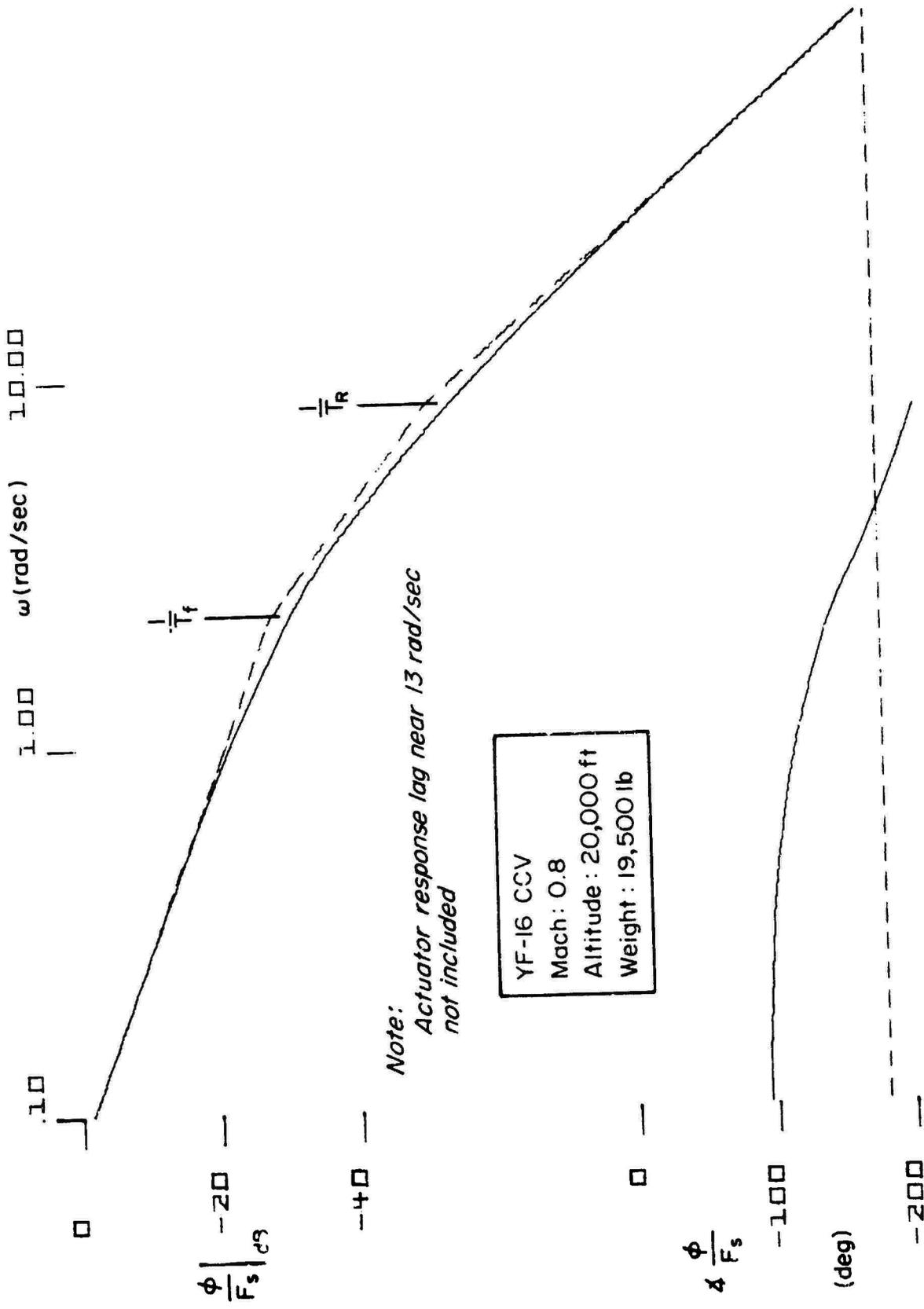


Figure 62. Roll Response, Conventional Flight Mode

Under such circumstances the lateral piper error, expressed as an angle, is given by:

$$\epsilon = \psi + \frac{U_0}{R} \int \lambda dt \quad (37)$$

where $\lambda = \psi + \beta$, the lateral path angle. The expression, directly analogous to the similar expression for the longitudinal piper error, has the same kinematic properties. In particular, the yaw angle (ψ) dominates except at short range and low frequencies, where the integral of the lateral path angle begins to contribute.

For the direct side force mode the sideslip angle is ideally zero, whence $\lambda = \psi$ and the piper error transfer function is related to the heading response to the button (or pedal) according to:

$$\frac{\epsilon}{\delta_c} = \frac{\psi}{\delta_c} \left(1 + \frac{U_0/R}{s} \right) \quad (38)$$

This leads to an expression similar to the equivalent expression for the A_N mode of response:

$$\frac{\epsilon}{\delta_c} = \frac{K_{a_y}(s + U_0/R)}{s^2(T_f s + 1)} \quad (39)$$

However, the YF-16 CCV response dynamics for this mode reveal the existence of an additional second-order lead-lag in the response, Fig. 63. The lead falls near U_0/R ; the lag, here identified by ω_β , originates in a directional short period caused by the $\beta + \delta_R$ feedback loop in the flight control system. While the low-frequency asymptote of this response shows the desired (if one is to track an accelerating target) K/s^2 -like response, the lead-lag implies an oscillatory tendency when attempting to make fine, rapid corrections. The aim error response has less than optimum characteristics; this observation motivates an analysis of the crossfeed requirements for mode "purification," that is, determination of the flight control system requirements that constrain the sideslip response.

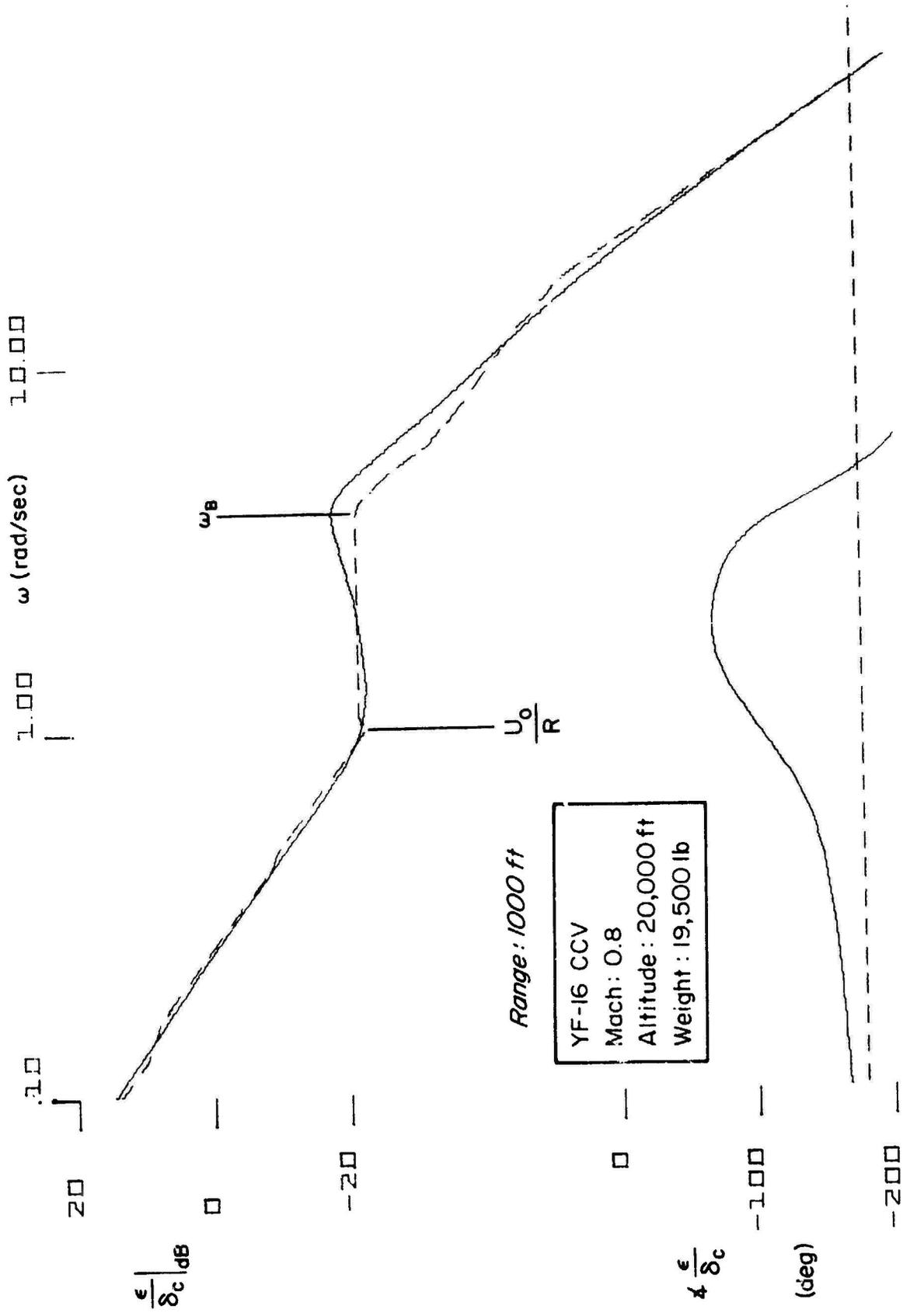


Figure 63. Aim Error Response, Direct Side Force Mode

Figures 64 and 65 compare the exact (Section II) crossfeeds to those used in the YF-16 CCV. There is a 3 dB mismatch in the canard-to-aileron crossfeed and a 9 dB mismatch in the canard-to-rudder crossfeed.

Figure 66 shows the piper error response for the A_y mode after the crossfeeds have been adjusted to match the ideal crossfeed at low frequencies. For this mode the crossfeed match is quite good out to 10 rad/sec. Figure 66 is seen to exhibit near-ideal (cf. Eq. 39) response, except for a closely spaced zero-pole combination at 3 to 4 rad/sec due to residual coupling.

What has happened, of course, is that the improved crossfeed gains have acted to attenuate the sideslip response. Figure 67 shows a significant reduction in this response — better than 20 dB at ω_β , even more at the lower frequencies where the approximation to the ideal crossfeed is best. This reduction is important to the dive bombing task, because it results in the lateral path angle closely tracking heading. By contrast, the unaltered response characteristic shows that sideslip washes out relatively slowly at this flight condition.

The change also seems to reduce the control authority because the lateral acceleration loses the Y_β contribution. Indeed the $\dot{\lambda}$ response (not shown here) shows a 3 dB reduction in the mid-frequency range. In effect, we have traded authority for mode "purity."

Other response comparisons not shown here produce similar results; the roll response is attenuated by 20 dB and more at frequencies below ω_β . Likewise, when the ideal crossfeed is utilized, the yaw rate response is significantly reduced in the 1 to 10 rad/sec region.

All of these changes underscore the sensitivity of the aircraft response properties to relatively small changes in the system parameters. In this discussion the sensitivity was shown to be a function of the departure from ideal of the control crossfeeds employed. But the ideal crossfeed itself is a function of the aircraft parameters and, in general, the feedback loop equalization (recall from page 143 that these are effective coupling numerators — those obtained as a result of the feedback loop closure).

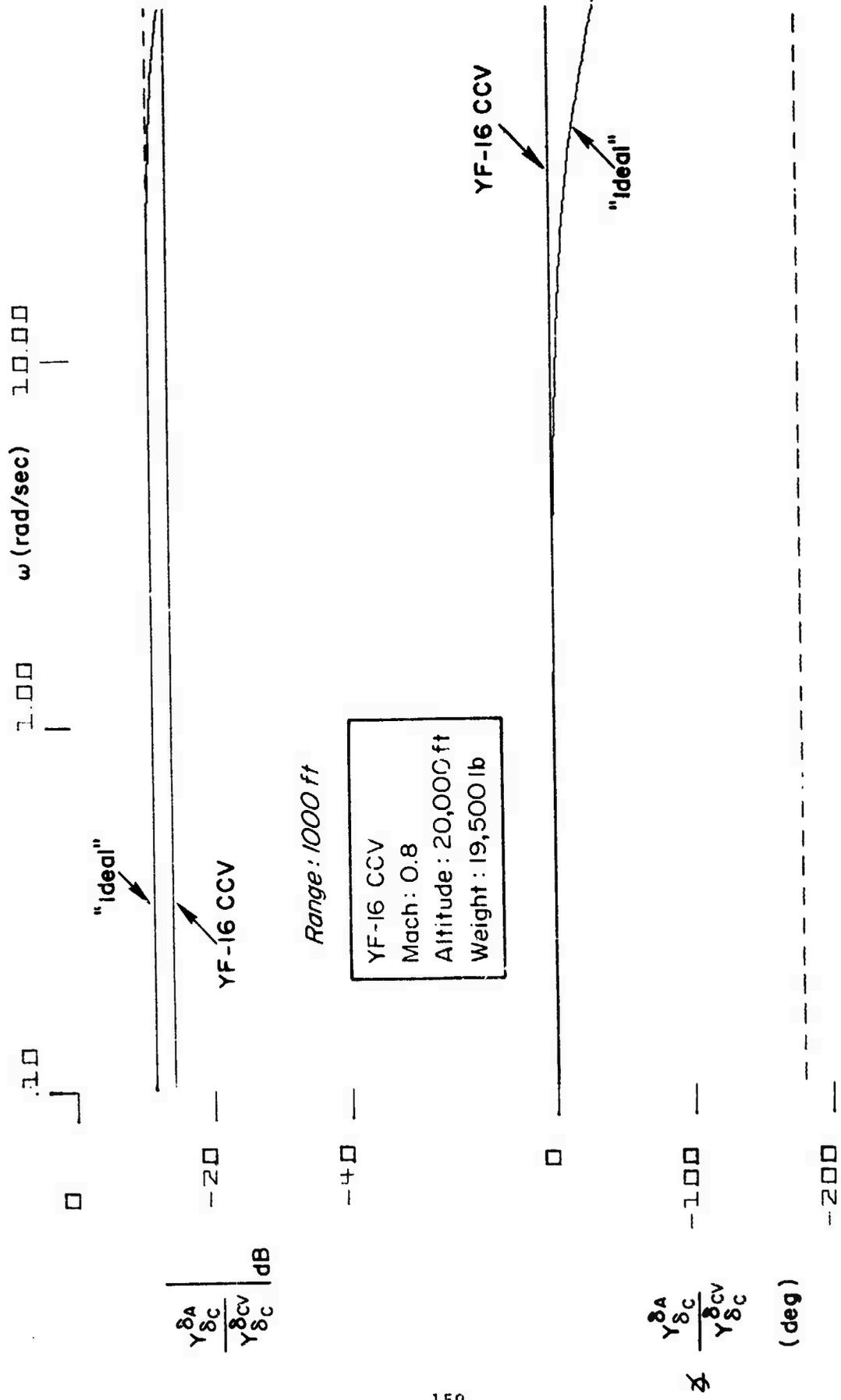


Figure 64. Canard-to-Aileron Crossfeed Comparison, Ay Mode

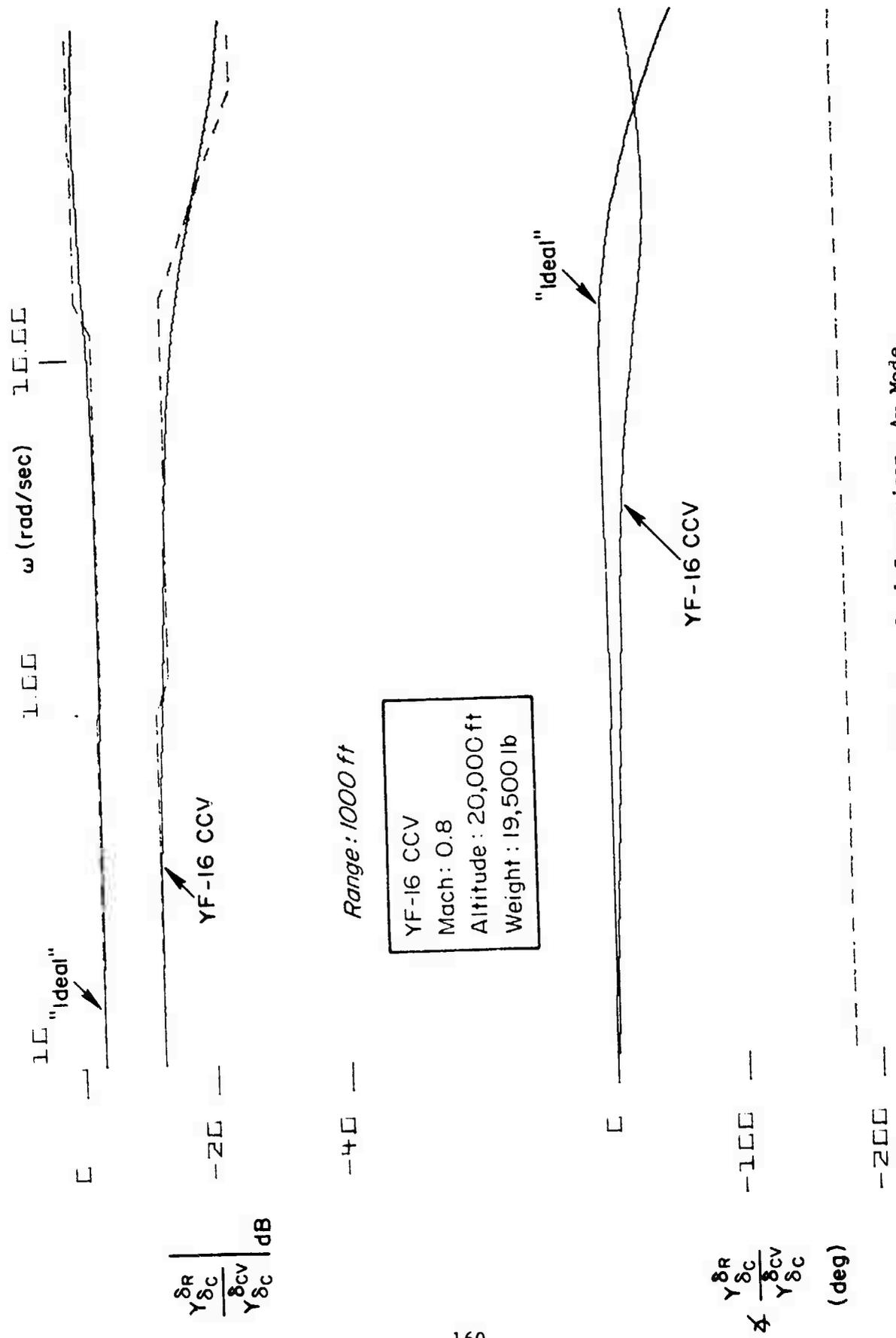


Figure 65. Canard-to-Rudder Crossfeed Comparison, Ay Mode

ω (rad/sec)

1.00

10.00

100

1000

10000

100000

1000000

$\frac{\epsilon}{\delta_c}$ dB

20

10

0

-10

-20

-30

-40

-50

-60

$\frac{1}{T_R} \omega \beta$

$\frac{U_0}{R}$

Range : 10000 ft

YF-16 CCV
Mach : 0.8
Altitude : 20,000 ft
Weight : 19,500 lb

$\angle \frac{\epsilon}{\delta_c}$
(deg)

-100

-200

-300

-400

-500

-600

-700

-800

-900

-1000

Figure 66. Aim Error Response, Ay Mode with Improved Crossfeed Gains

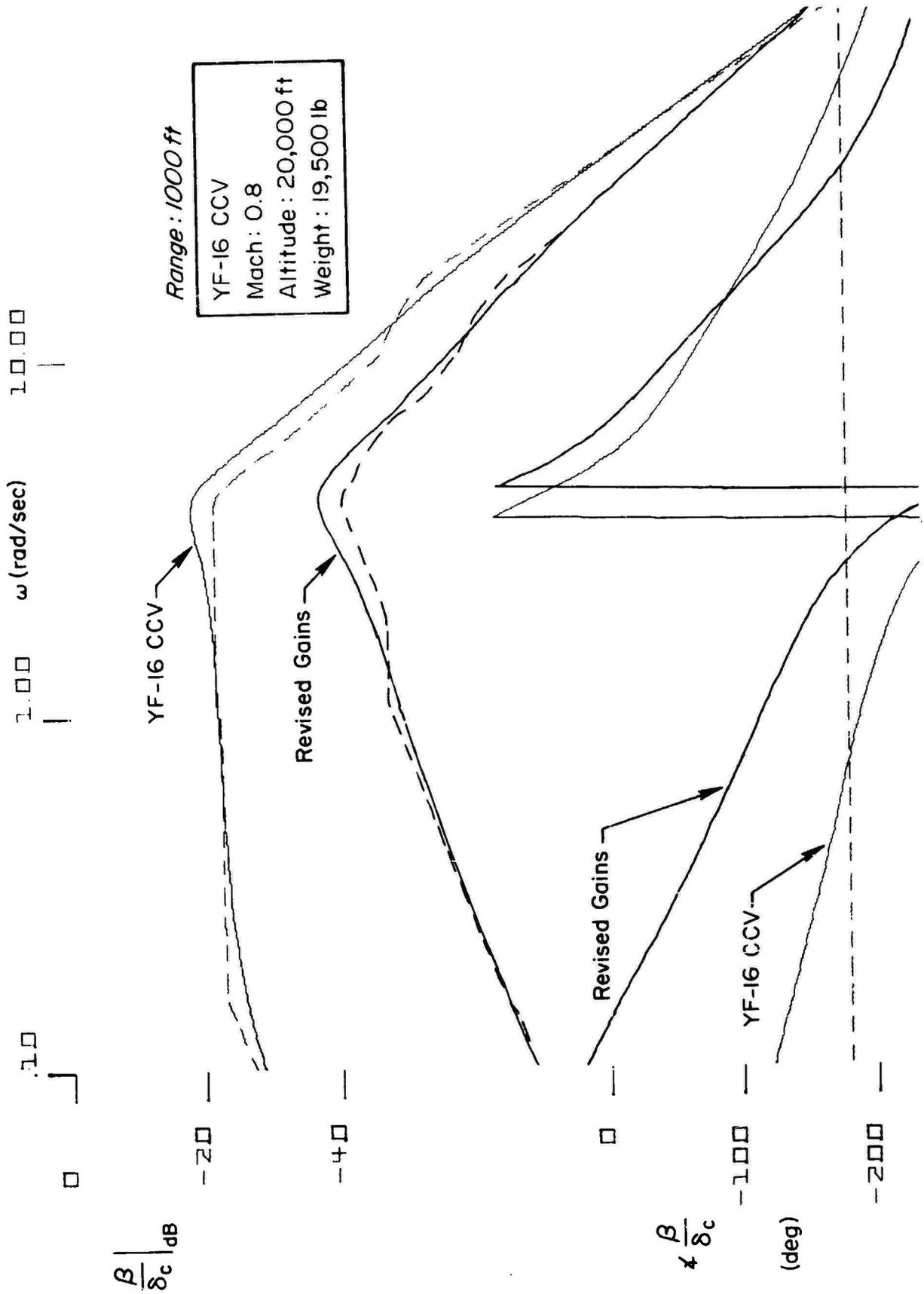


Figure 67. Comparison of Sideslip Responses; Original and Revised Crossfeeds for Ay Mode

This sensitivity to implementation details has implications for flight control design, pilot opinion, and system performance. The design implications are clear: the flight control systems must cope with small parameter uncertainties. Pilot opinion can be sensitive to the response differences if pilots are required by the nature of the task to suppress the undesired motion. Finally, system performance — especially in the air-to-ground weapon delivery task — will be sensitive to sideslip existing at weapon release. Even with the flat turn response mode for precision aiming, the pilot may be required to wait some seconds while holding a nulled error for the path or sideslip response to die out after the roll-in maneuver with lateral stick.

Yaw Pointing (β_1) Mode

The yaw pointing mode, analogous in all respects to the pitch pointing mode, has similar idealized response characteristics:

$$\frac{\epsilon}{\delta_c} = \frac{N_{\delta_c}}{[s^2 + 2\zeta_{a_y}\omega_{a_y}s + \omega_{a_y}^2]} \quad (40)$$

In this expression the subscript a_y is used to indicate that the feedback variable used to "purify" the response results in a "directional short-period mode." N_{δ_c} is understood to include the effects of both rudder and forward canard.

The actual frequency response plot (Fig. 68) has these idealized characteristics in the frequency range above about 1 rad/sec. Below this point the response shows a K/s-like characteristic accompanied by a non-minimum phase zero (designated by NMF in the plot). This means that the ϵ response has the wrong sign at low frequency. Positive δ_c results in negative ϵ : the aircraft first yaws nose left but eventually drifts back to the right. This characteristic provoked adverse comment by the pilot (Ref. 17).

Examination of the crossfeed requirement by means presented for the A_y mode (Figs. 69 and 70) shows a 2.5 dB mismatch in $Y_{\delta_c}^{\delta_R}$ at low

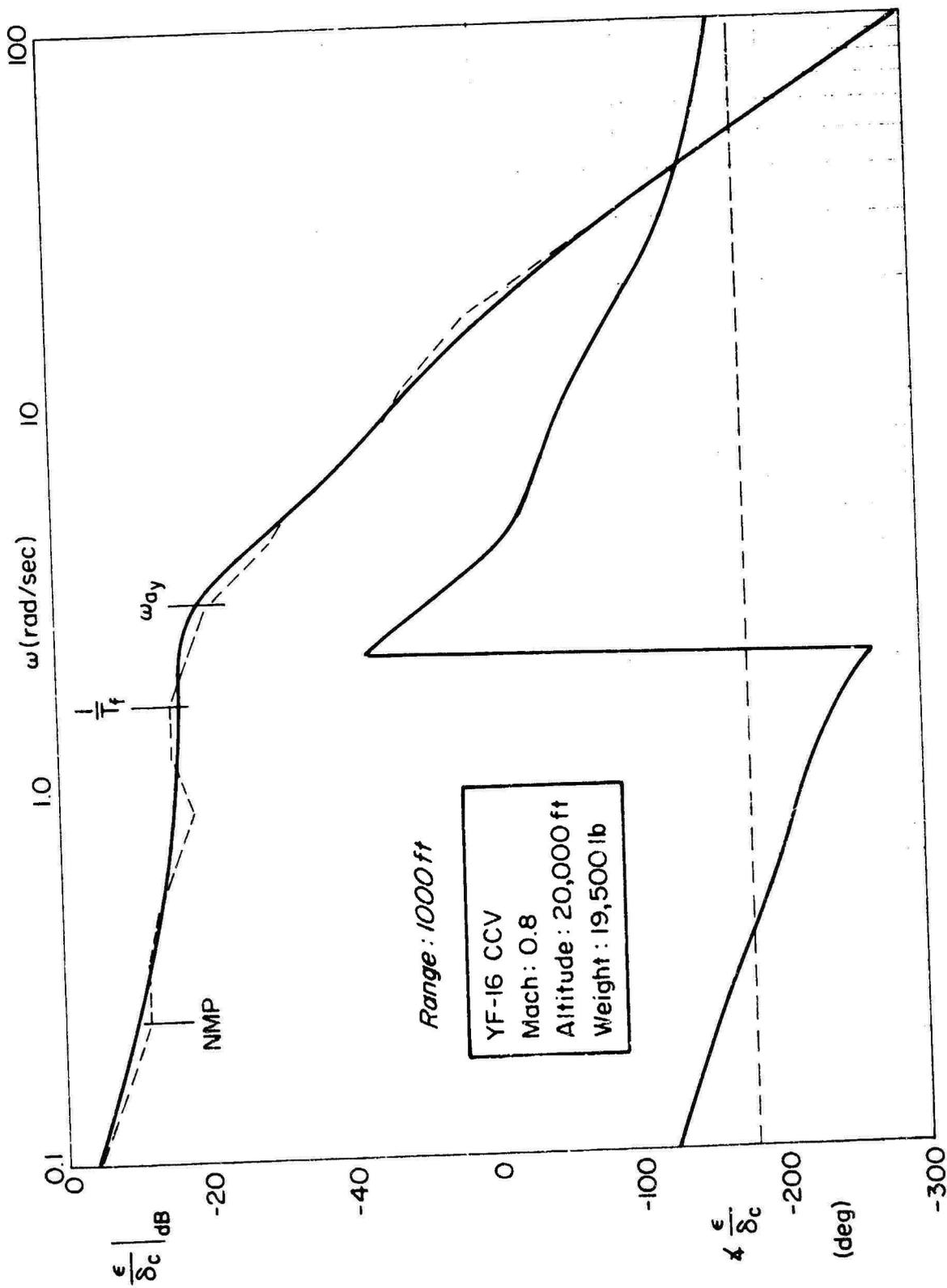


Figure 68. Aim Error Response, Yaw Pointing (β_1) Mode

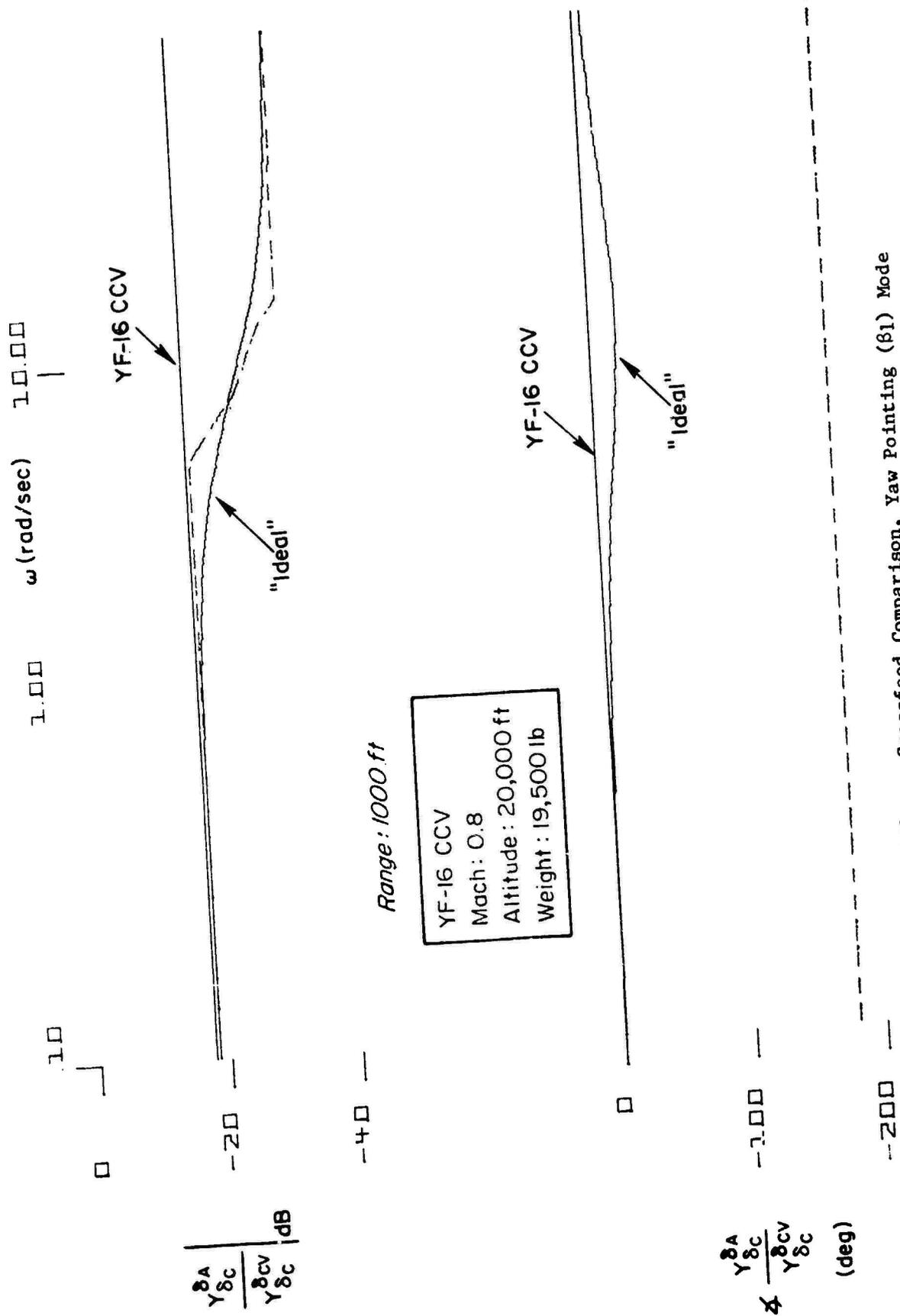


Figure 69. Canard-to-Aileron Crossfeed Comparison, Yaw Pointing (81) Mode

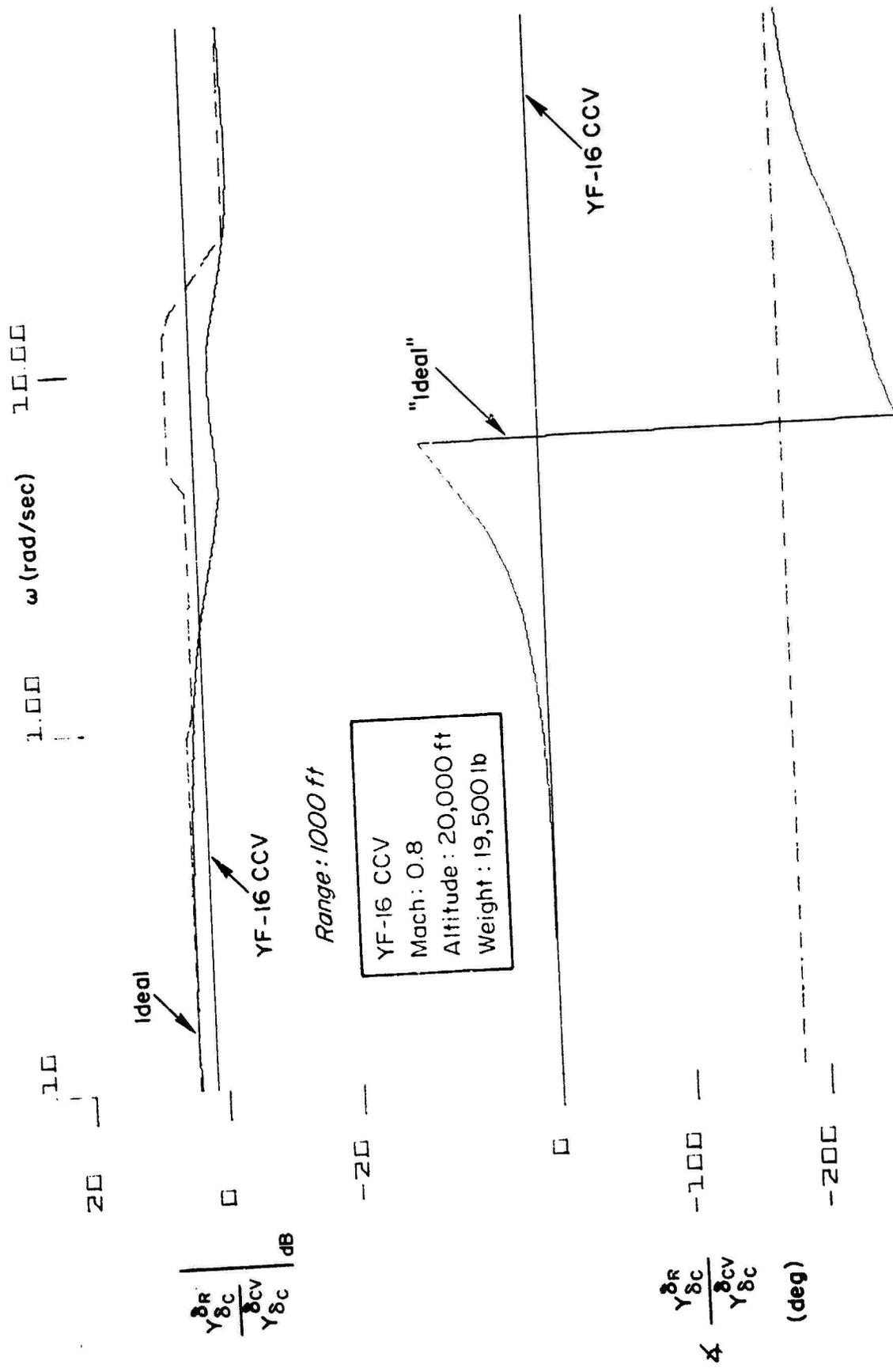


Figure 70. Canard-to-Rudder Crossfeed Comparison, Yaw Pointing (β_1) Mode

frequencies which, when removed, results in a significant attenuation of the low-frequency shift due to λ . The result is shown in Fig. 71, a response characteristic which closely approximates that implied by Eq. 40.

In this case, correcting for low-frequency errors in the crossfeed is sufficient for "purifying" the aim error response shown. However, if the range were reduced to a very low value, residual path changes at the higher frequencies would creep in because of the crossfeed mismatch at these frequencies. Close-range tracking tasks ($U_0/R \gg 1$) typically require path changes, a response the β_1 mode is specifically designed to avoid. Therefore it would appear that the broadband crossfeed matching is not required because the β_1 mode is not suited to the close-range path control task in any event.

Lateral Translation (β_2) Mode

The ideal response is similar to that for the α_2 mode:

$$\frac{\epsilon}{\delta_c} = \frac{Y\delta_c/R}{s(s - Y_V)(T_{fs} + 1)} \quad (41)$$

The YF-16 CCV response is considerably different, see Fig. 72. As before, this can be attributed to the chosen crossfeeds, Figs. 73 and 74, which cause significant yawing, thereby altering the aim error at both high and low frequencies (reduced bandwidth).

To iron out these differences proved to require very close attention to the crossfeeds, particularly to the rudder, to eliminate the yawing motion. The system is considerably more sensitive to small crossfeed differences than is the vertical translation mode. This sensitivity is to be expected when the control authority over lateral translation is considerably lower, thereby intensifying the relative importance of the yawing contribution to ϵ .

Merely modifying the actual crossfeed gains in Figs. 73 and 74 resulted in the ϵ/δ_c response shown in Fig. 75 — clearly a closer approach to the ideal response. But to eliminate the effects of the poles and zeros near ω_y required that the "ideal" crossfeed in Fig. 74

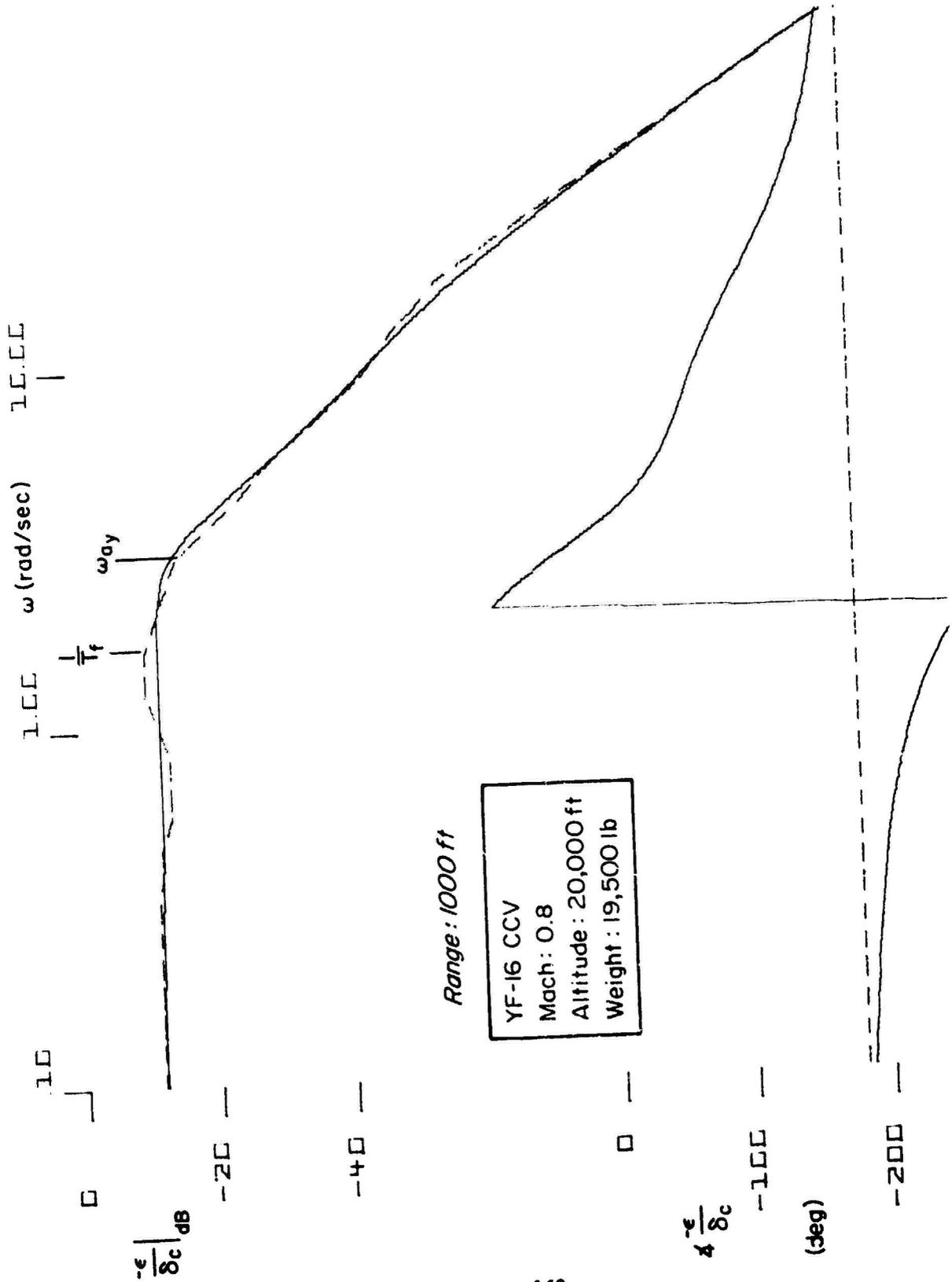


Figure 71. Aim Error Response with Improved Crossfeed Gains, Yaw Pointing (β_1) Mode

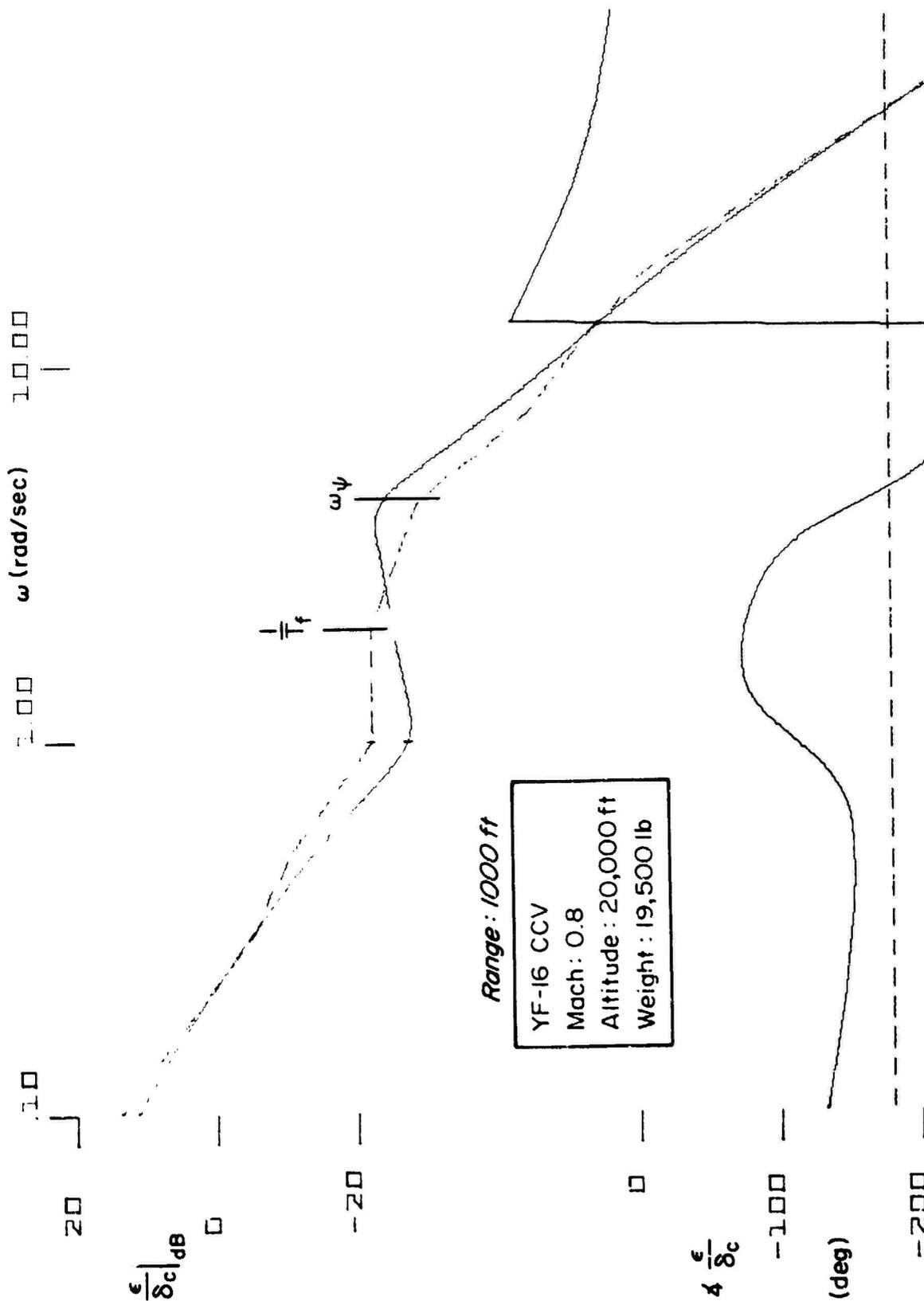


Figure 72. Aim Error Response, Lateral Translation (β_2) Mode

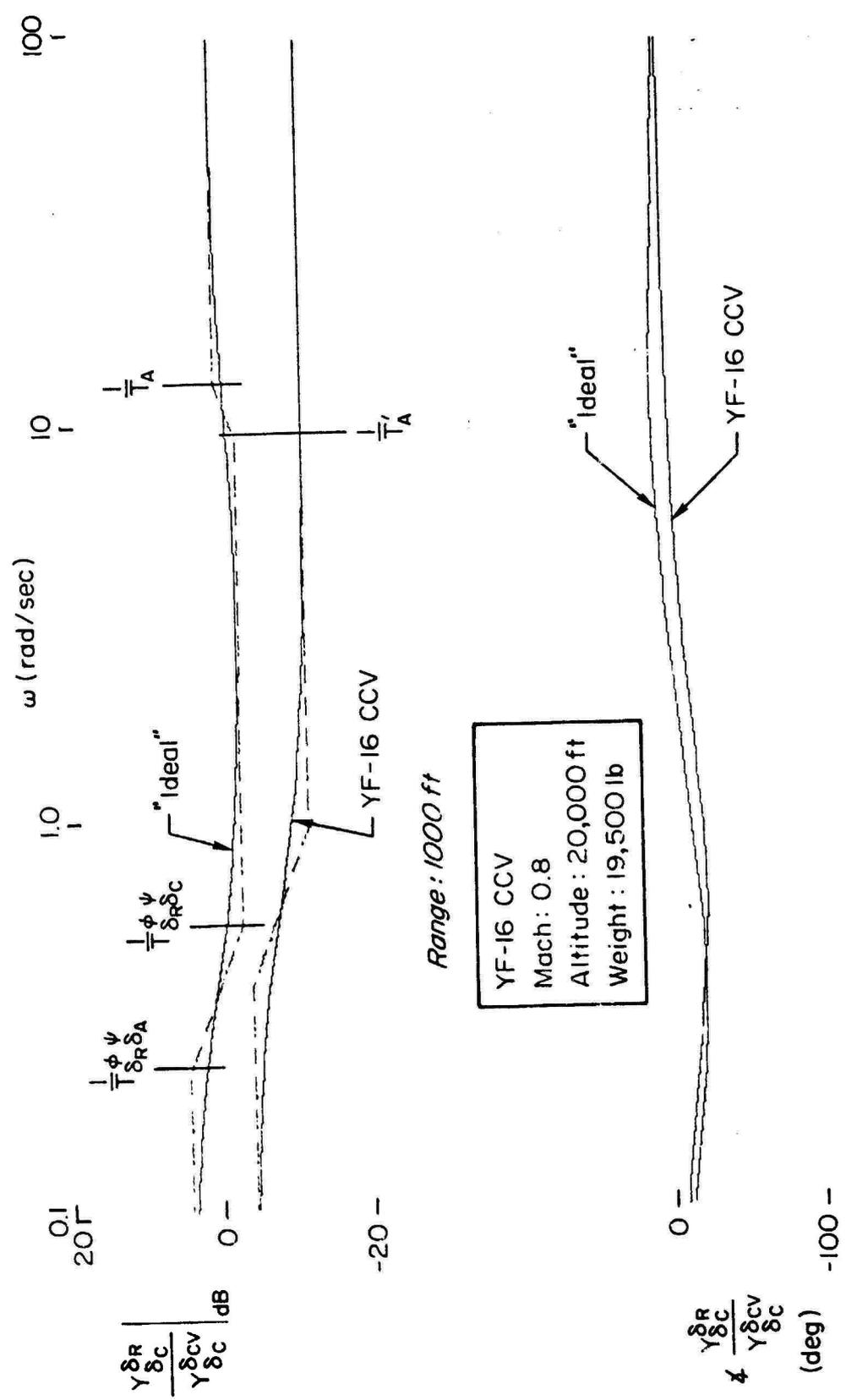


Figure 74. Canard-to-Rudder Crossfeed Comparison, Lateral Translation (β_2) Mode

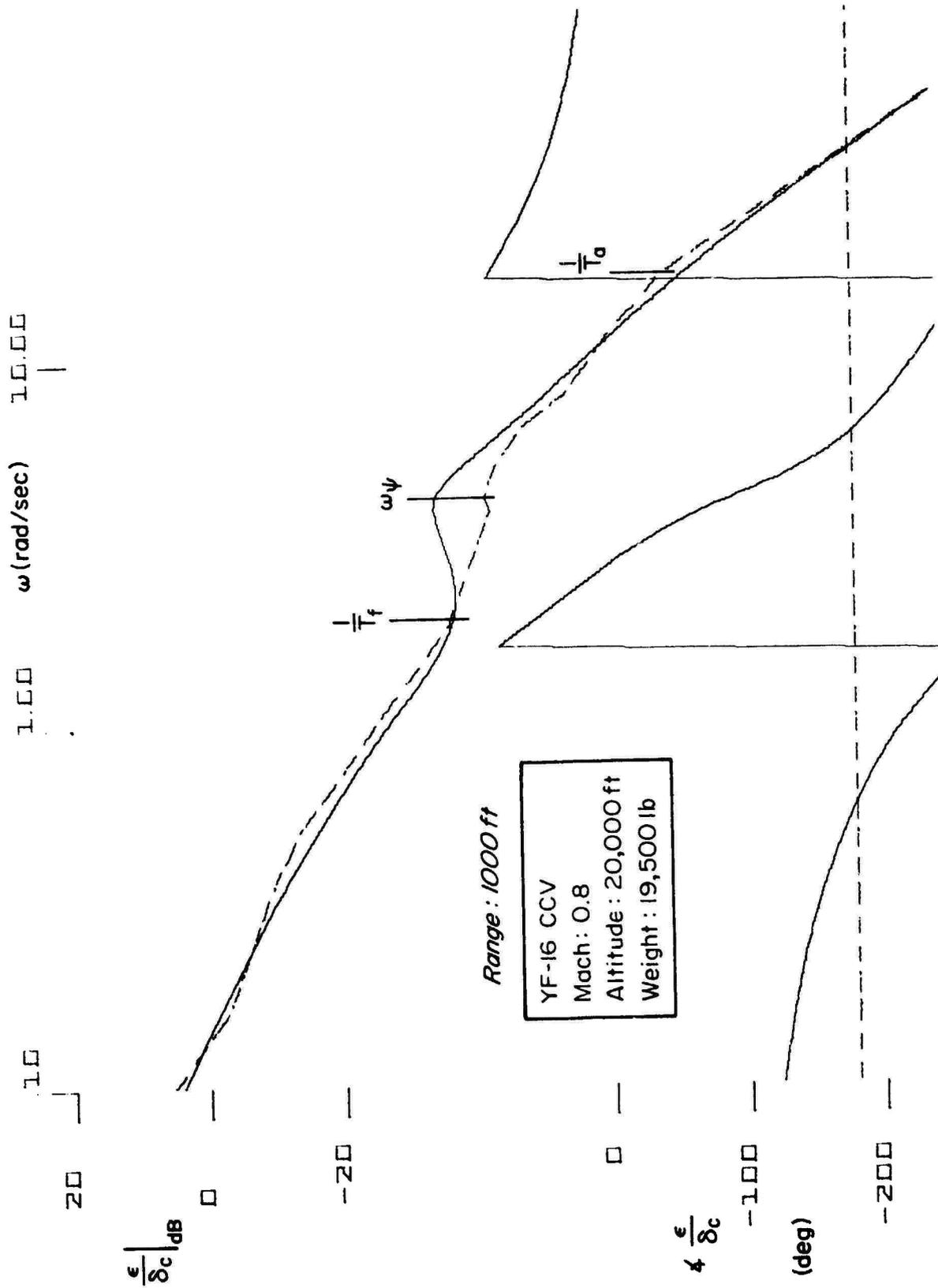


Figure 75. Aim Error Response with Improved Crossfeed Gains, Lateral Translation (β_2) Mode

be matched almost exactly, not only at low frequencies but also at high; the lead-lag in the vicinity of 10 rad/sec is important. The result of this exercise is shown in Fig. 76; the pole-zero dipole is still evident, but the response overall closely approximates the ideal response implied by Eq. 41.

This sensitivity of the response to system parameters can be appreciated by establishing the connection between the rudder to canard crossfeed of Fig. 74 and various aircraft and flight control system parameters. The ideal crossfeed for the β_2 mode is defined*

$$\frac{Y_{\delta_c}^{\delta_R}}{Y_{\delta_c}^{\delta_{vc}}} = \frac{N_{\delta_A}^{\phi r} \delta_c}{N_{\delta_R}^{\phi r} \delta_A} \left| \begin{array}{l} p, r + \delta_A, \delta_R \\ a_y + \delta_R \end{array} \right. \quad (42)$$

where arrows emphasize the effective feedback loop closures, which affect the coupling numerator ratio. This expression can be expanded in terms of airframe-alone coupling numerators as follows:

$$\frac{Y_{\delta_c}^{\delta_R}}{Y_{\delta_c}^{\delta_{vc}}} = \frac{N_{\delta_A}^{\phi r} \delta_{vc} + Y_{a_y}^{\delta_R} \cdot N_{\delta_A}^{\phi r} \delta_{vc} \delta_R}{N_{\delta_R}^{\phi r} \delta_A} \quad (43)$$

where $Y_{a_y}^{\delta_R}$ represents the flight control system transfer function between the a_y feedback signal and the δ_R surface deflection. Referring to the block diagram in Fig. 43, this signal is:

$$Y_{a_y}^{\delta_R} = \underbrace{-0.0052}_{\text{Gain}} \times \underbrace{13/(13)}_{\text{Actuator Response}} \quad \frac{\text{deg}}{\text{ft/sec}^2}$$

*We have assumed that the $\delta_c + \delta_A$ crossfeed has a negligible effect on this generic sensitivity analysis. Hence $Y_{\delta_c}^{\delta_A}$ is assumed to be zero for clarity.

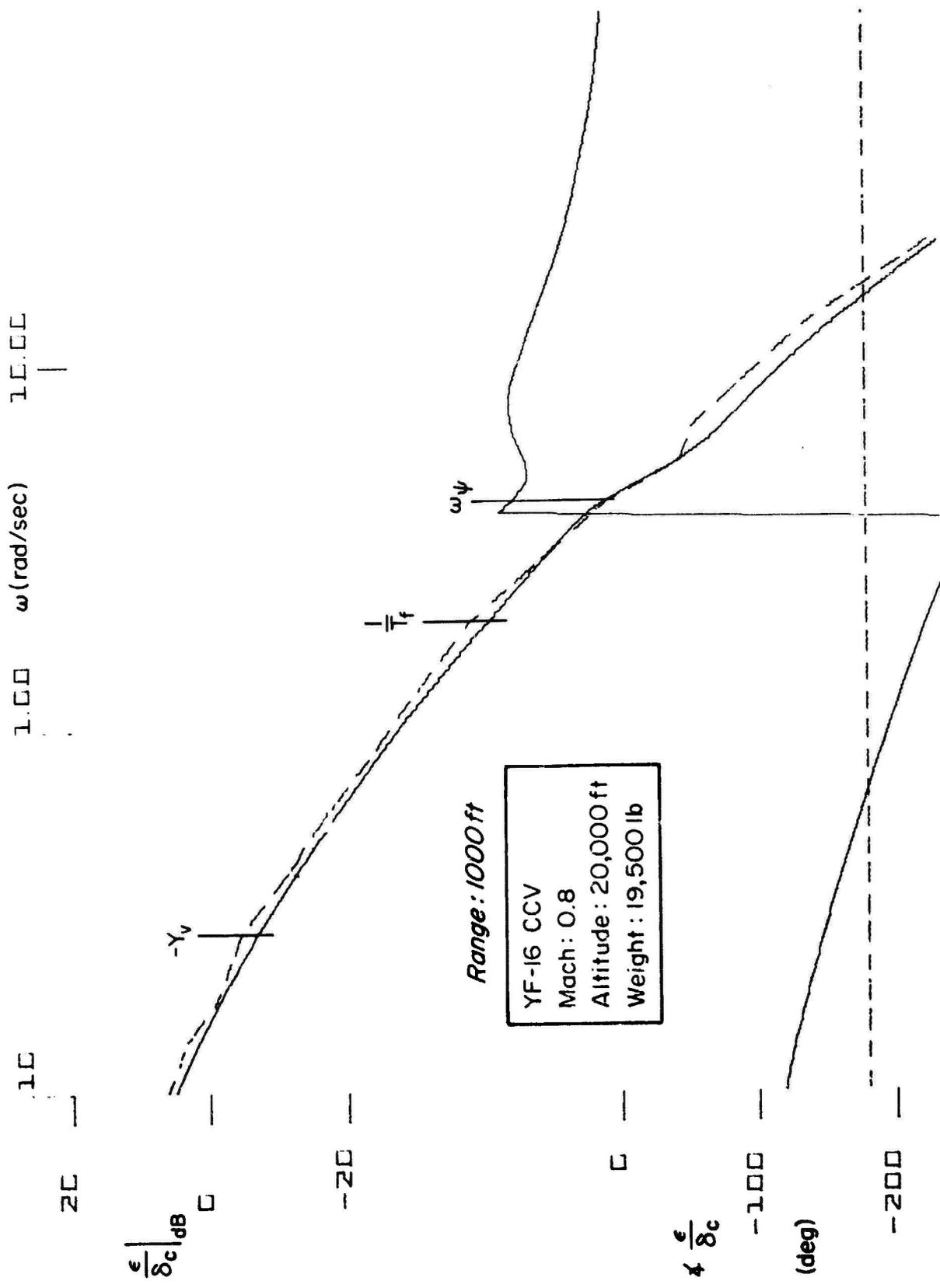


Figure 76. Aim Error Response with Exact Match to $Y\delta_e$

The inverse time constant at 13 rad/sec is symbolized by $1/T_A$ in Fig. 74.

The relevant numerators are given by (literal form is approximate, numerical form is exact):

$$\begin{aligned}
 N_{\delta_A \delta_R}^{\phi r} &= -213.75(s + 0.40943) \\
 &\doteq L_{\delta_A} N_{\delta_{vc}}^{\delta} s - Y_v + \frac{Y_{\delta_{vc}}^*}{N_{\delta_{vc}}^{\delta}} N_{\beta}^{\delta}
 \end{aligned} \tag{44}$$

$$\begin{aligned}
 N_{\delta_R \delta_A}^{\phi r} &= -221.22(s + 0.23996) \\
 &\doteq -L_{\delta_A} N_{\delta_R}^{\delta} s - Y_v + \frac{Y_{\delta_R}^*}{N_{\delta_R}^{\delta}} N_{\beta}^{\delta}
 \end{aligned} \tag{45}$$

$$\begin{aligned}
 N_{\delta_A \delta_{vc} \delta_R}^{\phi r a_y} &= -10922.3s \\
 &\doteq V_{T_0} L_{\delta_A} (Y_{\delta_R}^* N_{\delta_{vc}}^{\delta} - Y_{\delta_{vc}}^* N_{\delta_R}^{\delta}) s
 \end{aligned} \tag{46}$$

When these expressions are combined according to Eq. 42 the ideal crossfeed results:

$$\begin{aligned}
 \frac{1}{T_{\delta_A \delta_{vc}}^{\phi r}} \bigg|_{a_y \rightarrow \delta_R} & \quad \frac{1}{T_r} \\
 Y_{\delta_{vc}}^{\delta_R} &= \frac{\underbrace{0.9658(s + 0.5685)}_{1/T_{\delta_R \delta_A}^{\phi r}} \underbrace{(s + 9.3631)}_{1/T_a}}{\underbrace{(s + 0.23996)}_{1/T_{\delta_A \delta_{vc}}^{\phi r}} \underbrace{(s + 13)}_{1/T_a}}
 \end{aligned} \tag{47}$$

This short analysis reveals the following essentials:

- 1) The low-frequency pole-zero combination is located in the vicinity of $-Y_v$ on the Bode plot (Fig. 74). The zero is greater than $-Y_v$ and the pole is less by virtue of the differing signs of $N_{\delta_{vc}}$ and N_{δ_R} , respectively.
- 2) The high-frequency pole-zero combination is located in the vicinity of the actuator inverse time constant and comes about because of the $a_y + \delta_R$ feedback in the flight control system. More complex actuator representations will result in correspondingly more complex high-frequency dynamics in the crossfeed.
- 3) The high-frequency gain of the crossfeed is proportional to the ratio of $N_{\delta_{vs}}$ to N_{δ_R} , approximately. If the vertical canards generated zero yaw moment, the crossfeed requirement between canard and rudder would change significantly.

This analysis can be summarized as follows. The vertical canards and the rudder are equally powerful in generating the yawing moments that are to be suppressed in the β_2 mode. Thus, their deflections in this mode (including the effects of feedbacks and crossfeeds) must be precisely related to avoid unbalanced yawing moments that cause the response to depart from the ideal prototype of Eq. 42. In this particular example, exactly matching the ideal canard crossfeed to the rudder makes possible the enormous reduction in yaw rate evident in the comparison shown in Fig. 77.

As a practical matter the flight control system designer is unlikely to do this well, because of gain scheduling difficulties, regardless of the implementation he chooses. At least transient (high frequency), if not also steady-state (low frequency) mismatches will occur that will significantly disturb the desired ϵ/δ_c response at all but the shortest range.

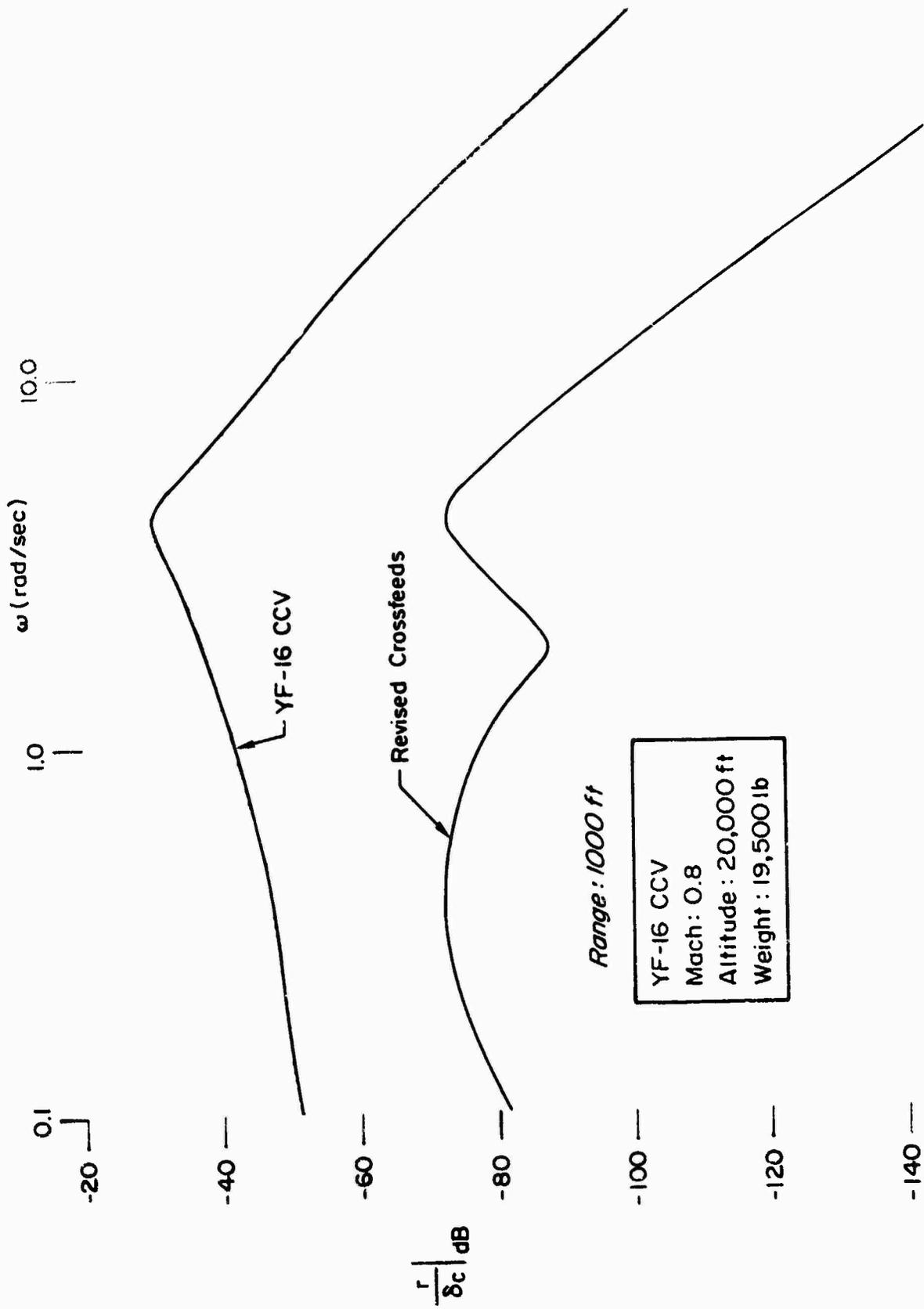


Figure 77. Comparison of yaw Rate Responses; Original and Revised Crossfeeds, Lateral Translation (β_2) Mode

APPENDIX B

FORMULATION OF EQUIVALENT STABILITY DERIVATIVE MODEL IMPLEMENTED ON THE VRA

The lateral-directional model simulated on the Princeton VRA (Navion) was essentially a "lower-order equivalent system" representation of the YF-16 CCV. The basic approach to mechanizing the CCV modes on the VRA was analogous to the mechanization of the actual YF-16 CCV, i.e., the CCV feedforward and feedback loop structure was added "on top of" the existing augmented YF-16. Thus, in formulating the VRA model the first step was development of a lower-order equivalent system model of the augmented YF-16 airframe without the CCV control structure. The formulation of this model will be discussed in this appendix.

A linearized small-perturbation model of the augmented YF-16 ($M = 0.8/20,000$ ft/19,500 lb) was developed in the initial analytical work (see Appendix A). The "bare" F-16 airframe is conventional, i.e., its linearized equations of motion consist of the same basic stability and control derivatives of any conventional airframe including the Navion. The basic F-16 augmentation loops are also conventional (see Fig. 36); however, these loops contain equalization, filters, and actuators. This creates a basic problem in simulating the F-16 on the VRA, which is essentially a stability derivative augmentation (response feedback) system utilizing pure gain feedbacks. Simulating the detailed transfer functions of the control system would have required additional electronic fabrication but more importantly was not necessary for the purposes of the program. The primary requirement was to formulate a model which would appear to the pilot to be representative of the F-16.

Since $a_{\dot{\gamma}} = U_0 \dot{\lambda} = U_0 (\dot{\beta} + 1)$, the basic control system effectively consists of feedbacks of ϕ , r , and β to rudder and aileron. An obvious pure gain approximation to these loops would be to replace the feedback transfer functions with their low-frequency (DC) gains. Unfortunately

this approach does not give an adequate representation, largely because of the yaw damper ($r_g + \delta_R$) washout at $1/T_{wo} = 1$ rad/sec.

It is possible, however, to obtain a good equivalent system representation using the STI Multi-Input-Output Frequency Response Parameter Identification program (MFP). For this procedure the basic F-16 augmentation system was represented as

$$\begin{aligned}\delta_A &= -K_p p \\ \delta_R &= -K_r r - K_\beta \dot{\beta} - K_\phi \phi - K_\beta \beta\end{aligned}\tag{48}$$

where K_p , K_r , etc., are unknown gains to be determined. Frequency domain parameter identification was then performed with the MFP program to obtain gain values which gave a "best fit" to the actual YF-16 control system. The process was constrained such that three independent transfer functions (in this case β/δ_R , ϕ/δ_R , and r/δ_R) were matched simultaneously for each control point (surface) in the frequency domain of interest ($\omega < 10$ rad/sec). This insures a good match of any other lateral response, since any other response quantity may be represented as a linear combination of β , ϕ , and r .

Example comparisons of the frequency response of the original system and the lower-order equivalents (without washout) are shown in Figs. 78, 79, and 80. It may be seen that the lower-order model is a good approximation of the basic model, though it does not contain the yaw damper washout dipole (zero $1/T_{woN}$ and pole $1/T_{woD}$). Since the washout is in the $r + \delta_R$ loop, it does not affect the rudder numerators, i.e., $1/T_{woN} = 1.0$ rad/sec, the open-loop value. However, the yaw damper does affect the roll control β and ϕ numerators except $N_{\delta_A}^r(j\omega)$, as may be seen from the general expression for the roll control numerators in the presence of the yaw damper

$$N_{\delta_A}^r \Big|_{r \rightarrow \delta_R} = N_{\delta_A}^r + G_r^{\delta_R} N_{\delta_A}^r \frac{r}{\delta_R}\tag{49}$$

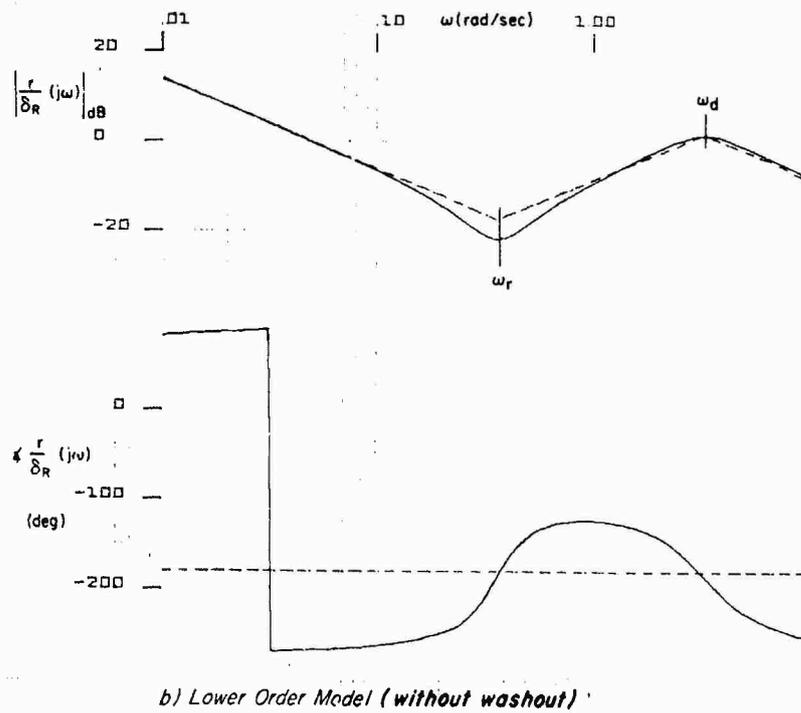
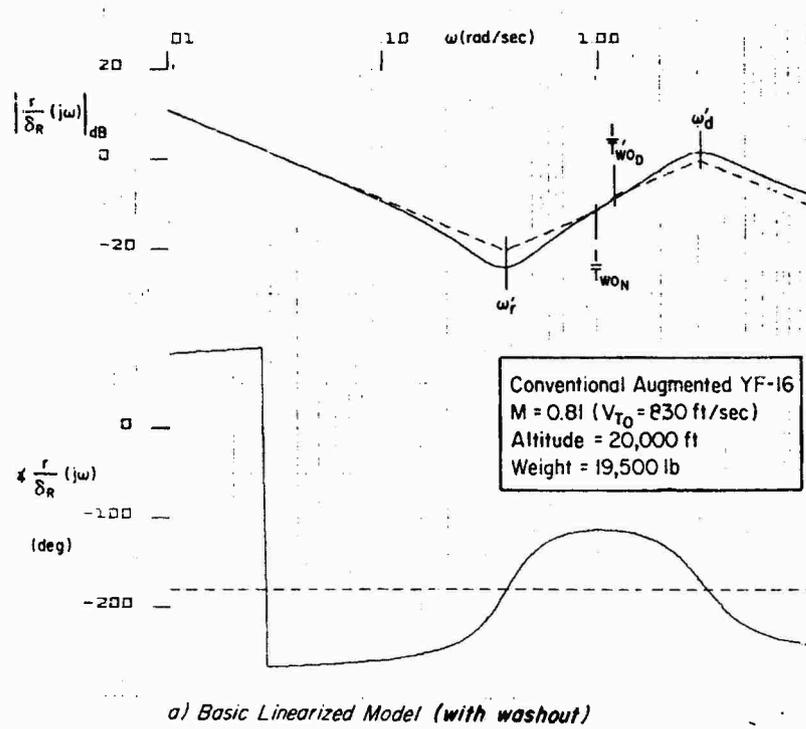
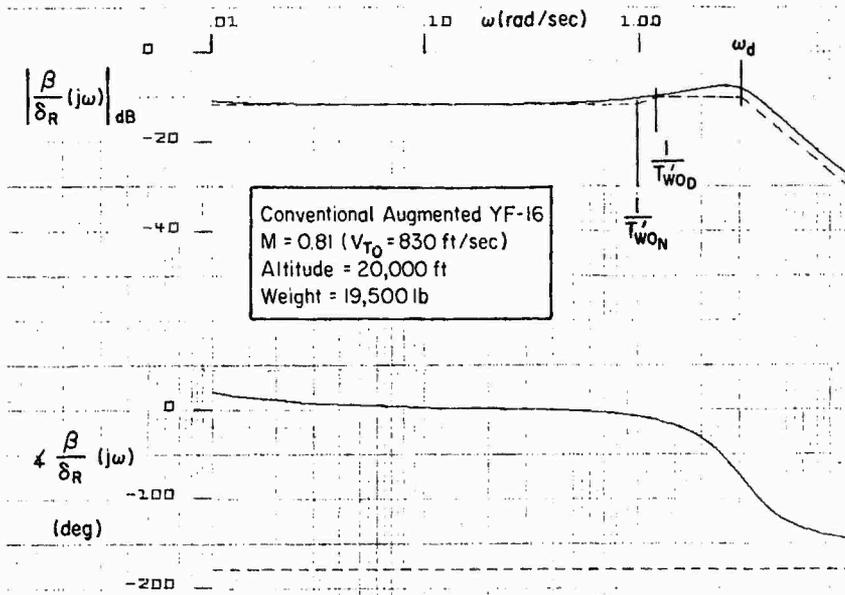
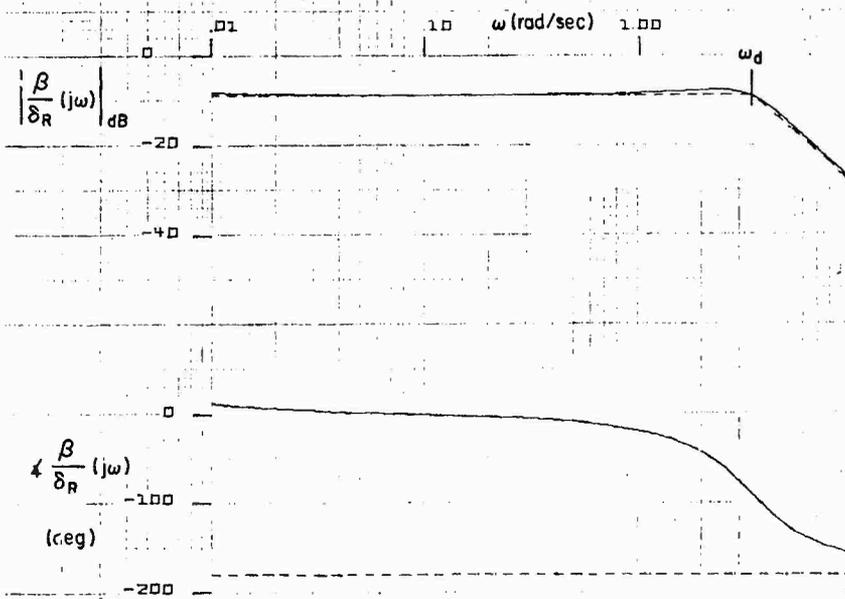


Figure 78. Yaw Rate Response to Rudder



a) Basic Linearized Model (with washout)



b) Lower Order Model (without washout)

Figure 79. Sideslip Angle Response to Rudder

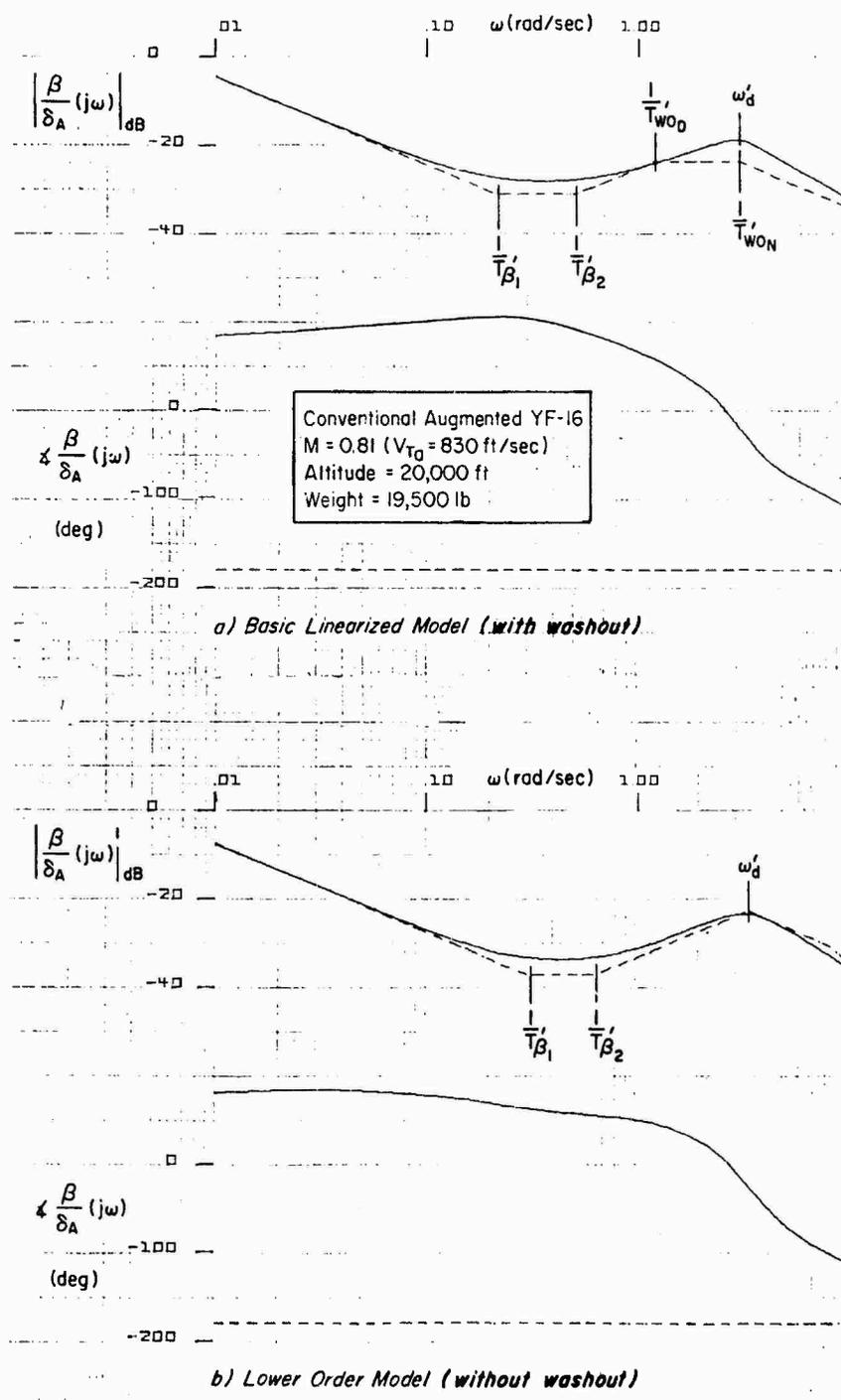


Figure 80. Sideslip Angle Response to Roll Control

where $x = \beta, r, \text{ or } \phi$ and $G_r^{\delta R}$ is the yaw damper feedback transfer function containing the washout. The "splitting" of the washout dipole ($T_{\omega_{OD}}^{\delta R}$ and $T_{\omega_{ON}}^{\delta R}$) in the $\beta \rightarrow \delta_A$ response (Fig. 80a) provided the primary complication in formulating the lower-order model.

Time domain comparisons of the basic and lower-order models (no washout) for several responses are shown in Fig. 81. It can be seen that the essential dynamics are reproduced, but with some differences in gain, especially for β . However, the low-order yaw rate response, which dominates the piper error response in the simulation scenarios, is quite close to the basic response.

With the control system reduced to pure gain feedbacks, the augmented airframe may be represented by the conventional 3 DOF equations of motion written in terms of equivalent stability derivatives, e.g.,

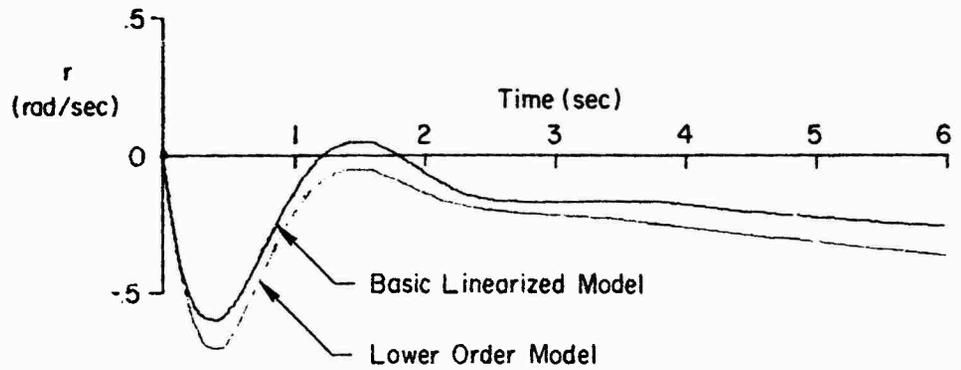
$$N_{\beta}^{\text{equiv}} = N_{\beta} - K_{\beta} N_{\delta R} \quad (50)$$

In addition to the usual derivatives, such as N_{β} , unconventional derivatives, $Y_{\phi}^{\text{equiv}} = -K_{\phi} Y_{\delta R}^*$, for example, also appear. Equivalent $\dot{\beta}$ derivatives result from the $a_{\dot{y}} \rightarrow \delta_R$ feedback. These derivatives posed a special problem for explicit mechanization on the VRA due to the problem of generating accurate, low-noise $\dot{\beta}$ signals. This problem was avoided by making the $\dot{\beta}$ derivatives implicit in the model. For example, in the roll moment equation the term $L_{\beta} \dot{\beta}$ was expanded, using $\dot{\beta}$ from the side force equation, as

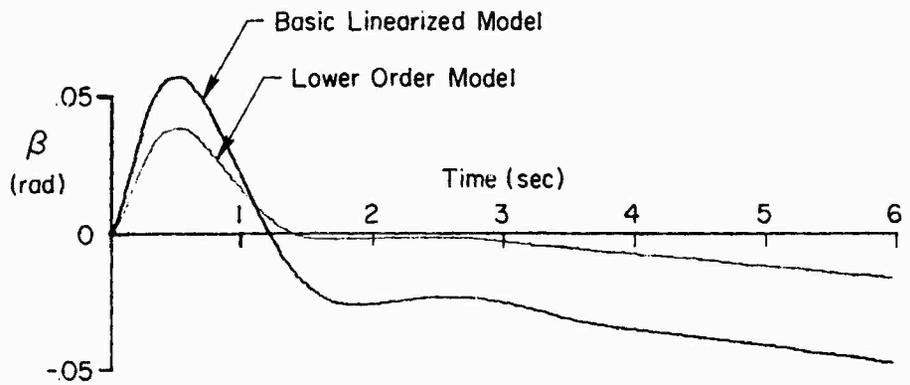
$$L_{\beta} \dot{\beta} = L_{\beta} (Y_{\beta} + Y_{pD} + \dots) \quad (51)$$

This allowed "absorbing" the $L_{\beta} \dot{\beta}$ into the other roll moment derivatives such as

$$L_{\beta}^{\text{equiv}} = (L_{\beta} - K_{\beta} L_{\delta R}) + L_{\beta}^* Y_{\beta}^{\text{equiv}} \quad (52)$$



a) Yaw Rate Response to 1 rad Step Rudder



b) Sideslip Angle Response to 1 rad Step Roll Control

Figure 81. Time Domain Comparison of Basic and Lower-Order Models

The final equivalent stability derivative model of the basic augmented YF-16 for implementation on the VRA is given in numerical form below (angular quantities in radians):

$$\begin{bmatrix} s + 0.2965 & -0.004084s - 0.03795 & 0.9796 \\ 47.71 & s^2 + 8.359s + 0.2077 & -6.564 \\ -8.917 & 0.2720s - 0.1136 & s + 3.105 \end{bmatrix} \begin{bmatrix} \beta \\ r \\ r \end{bmatrix} \\
 = \begin{bmatrix} 0.01802 & 0.02260 & 0.02988 \\ 5.655 & -49.60 & 7.178 \\ 4.766 & -1.897 & -3.925 \end{bmatrix} \begin{bmatrix} \delta_C \\ \delta_A \\ \delta_R \end{bmatrix} \quad (53)$$

APPENDIX C

PILOT COMMENTARY AND RATINGS

Pilot ratings and commentary are presented in an unabridged form in this appendix. Minor editing was done only where it was necessary to clarify the point being made. The Cooper-Harper pilot ratings are summarized in Table 13 for the wings-level turn configuration. The pilot ratings for the lateral translation modes are summarized in Table 4. Pilots were briefed on the configurations prior to their evaluations.

Control sensitivity was referred to by the potentiometer setting number in the experiment, e.g., a "pot setting" of 0.80 was called out as "800." The conversion of the CCV pot setting to engineering units is:

$$\text{CCV Control Sensitivity} = \text{Pot Setting} \times 0.01 \quad (\text{g/lb})$$

The outline used as a format for the pilot briefings is given as Fig. 82, and pilot comment card as Fig. 83.

TABLE 13. PILOT RATING SUMMARY, WINGS-LEVEL TURN CONFIGURATIONS

CONFIGURATION	CCV SENSITIVITY (g/lb) ^a	PILOT			
		MIKE PHILLIPS	ROGER HOH	KEVEN OLSON	BARRY NIXON
WLT1	0.0080	3	3	4	3, 3.5
	0.0075	4			
	0.0040				3.5
WLT2	0.0080	4	3	2.5	4
WLT3	0.0080	6	4.5	5	4
WLT4	0.0080	7	6.5	6.5	4.5-5
	0.0060				4-4.5, 3.5
WLT5	0.0080	6		2.5	4.5
	0.0045	4			4
	0.0060		3		
WLT6	0.0080	5			
WLT7	0.0080	5			
WLT10	0.0075	5			
	0.0050	4			
	0.0080		4		
WLT11	0.0075	8			
	0.0080		6.5	7	6.5
	0.0050				7
WLT12	0.0075	7			
	0.0050	6			
	0.0035	5			
	0.0080		5	4.5	
	0.0040		3.5	4	5
	0.0020		7		
	0.0060			4, 4.5	
WLT13	0.0080				6
WLT14	0.0080				4.5
WLT15	0.0060				6

^aThis refers to steady-state lateral acceleration to force at the "rudder" pedals.

Do not start evaluation until:

- 1) You feel reasonably proficient in the maneuver.
- 2) You have set the CCV sensitivity to its best value.

Air-to-air tracking:

- 1) Start straight and level.
- 2) Track aggressively with CCV control (pedals) during entire run (60 seconds)
- 3) Safety pilot will have ranging responsibilities.
- 4) Use centerstick as a secondary control if beneficial.
- 5) Evaluate immediately after run.
- 6) Make as many runs as you feel are required.
- 7) Record comments on tape recorder and transmit pilot rating to ground as well.
- 8) Fly slightly above target to avoid wake.

Formation Flying:

- 1) Fly slightly below and behind leader.
- 2) Be ready to hit reset if any uncommanded motions occur.
- 3) Pull away laterally.
- 4) The task is to sidestep back to a reasonably tight formation position.

Figure 82. Pilot Briefing Summary

- 1) Make Cooper-Harper rating evaluation.
- 2) Discuss specific configuration deficiencies that most influenced your rating.
- 3) Did you notice any secondary motions? If so, did they aid or detract from the assigned task?
- 4) Discuss specific deficiencies that did not have a primary influence on your ability to do the task but that you feel should be accounted for.
- 5) Did you use the centerstick to "help" the CCV mode? To what extent?

Figure 83. Pilot Comment Card

MIKE PHILLIPS, 20 November 1979

Background. USAF pilot with experience primarily in large transport type aircraft; primary subject pilot in LAMARS CCV simulation at WPAFB.

WLT1. On the first run this was rated a 3. The amount of pilot compensation was minimal to achieve the desired result. There was a slight tendency to overcontrol on occasion, but primarily the response was quick and easily adjusted to even in the most dynamic environment. The largest turn rates for the aircraft seemed to be maneuvered fairly easily to achieve the amount of side force and turn rate desired. No particular secondary motions were noted. A slight bobble was noted on occasion in the attempt to stabilize on target. Primarily these were pilot inputs simply to kill the motion, rather than a residual motion caused by getting off the controls. There were no other deficiencies noted, and the overall feeling was that roll control when used was not advantageous, not advantageous at all. The roll is too sensitive, and therefore the optimum use is simply to go rudder pedals; very little resultant motion was shown in the pitch mode so the pipper was maintained on target in pitch virtually to stabilize. The centerstick was not used, and should not be used as presently designed in this configuration.

WLT2. The Cooper-Harper rating was a 4. More pilot compensation was noted to be required in the loop. However, it was still possible to perform the task adequately for tracking — slightly more overshoots were noted, possibly due to the sensitivity being higher than desired, but still within reason. Noted when the tracking was not being used specifically that upon release of the controls the aircraft would yaw back to the previous heading chosen. In other words, if the control input were not held, the aircraft would return to whatever position it had before introduction. In an actual tracking task, however, the pilot is going directly from one input to the other and therefore this is not seen to be a particular problem. The only other factor noted was that at one point in the turn maximum differential was obtained, so that the

maximum vertical side force generating capability was reached and a certain amount of stalling was noted in that mode.

Overall, however, the amount of time on target was adequate and the design of the experiment was adequate. Secondary motions were not noted to be of any objection. The only time the secondary motions were noted was at frequencies stated prior to the actual tracking task. Again, the centerstick was not used and the feeling remains that it should not be used as presently designed.

WLT3. The tendency was to have more overshoot involved going in both directions trying to stabilize on target. A fair amount of time on target was noted; however, maximum pilot compensation was needed to keep it there. On occasion the maximum A_y was exceeded and roll had to be input, which was not desirable, but necessary in order to reacquire the target. Once the stall started to occur, the only thing left for the pilot to do was to introduce the roll axis. This can be done, but it is far less desirable than previous efforts.

WLT6. As designed we saw an apparent slowing of the response time — not quite as quick to move from one position to the next, however the overall time on target was adequate. More pilot compensation was needed than desired, hence the Cooper-Harper rating of 5. The primary problem was the gross acquisition task; fine tracking seemed to be reasonably stable although an occasional bobble was noted around the desired setting. Also noted was an occasional pitch bobble which seemed to be induced by the rapid change from one direction to another in the lateral mode. This is not noted throughout, but did appear in at least one particular turn. Centerstick was in general not used until one particular point where close in to the target the centerstick did seem to be needed to help out the CCV mode.

WLT4. On this run the Cooper-Harper rating was a 7. In the gross acquisition task the aircraft is reasonably stable. The reason for the lack of good Cooper-Harper rating is because we were unable to keep the aircraft on target. The rapid change from one direction to the other

resulted in a certain amount of roll being input and a tendency to bobble laterally, as well as in pitch around the target. Once the aircraft was established in a yaw, and tracking on the target, the aircraft was relatively stable and we would be able to acquire a tracking solution. However, once rollout was attempted or there was any attempt to stabilize in something other than a steady turn, the initial tendency was one of yaw, and bobble — hence the reason for the rating. The centerstick was used on rare occasions, but not primarily. The only reason for this was to damp out the oscillation seen in the A_y mode.

WLT5. Last run of the day. It appeared that there were certain delays involved in the response and as a result the Cooper-Harper rating was a 6 for the run. This particular configuration might be worth taking a look at again. The Cooper-Harper of 6 was primarily because of the slow response, the apparent delay between pilot actuation of the rudder pedals and the response of the aircraft which led to a certain amount of hesitation and an occasional overshoot for the actual tracking task. On-target time was approximately 50 percent, and of all the configurations tested it was one of the worst for time on target, primarily because of that initial delay. Centerstick was not used on this last mode at all, so it was not particularly a factor of exceeding the limits of the CCV configuration, although at one time a burble was approached and backed away from, simply because the maneuvering aircraft allowed that to occur. It might have been possible to exceed the limits if the target aircraft had continued moving away from us.

21 November 1979

WLT5. The first set of experiments is simply to adjust sensitivities. I will be reducing sensitivity of ailerons from 418 down, and on the rudder from 800 down, and I will discuss the various positions as we adjust.

Immediately noted is a tendency when the aileron input is removed from the axis for the aircraft to immediately attempt to roll wings level, whether this be desired or not. In other words, it will not hold

the attitude set with the stick prior to release. Also, if the stick is released it is quite a violent return to wings level. This would not be desirable if this is a final setting.

I've gradually moved down to a setting of 350 on the ailerons, and this is starting to feel comfortable. I'm going to make similar attempts at the rudder now. Final setting for ailerons again will be 350.

Steps were made with rudder sensitivity settings of 700, 750, and 800. Of the three, 750 seemed to be the best compromise of quickness of response, without quite so much tendency to overshoot or provide over-compensation from the pilot.

First run on WLT5 was considered unsatisfactory; I'm uncertain exactly what the cause was. It did appear that the tracking was working satisfactorily until a large input was made. The control surfaces just seemed to stall out, and then it was uncertain what was going to happen. The Cooper-Harper rating for WLT5 was a 4. Noticeable pilot compensation was needed — a fair amount of time was spent off target, primarily due to the initial response to get the aircraft moving.

A lack of quick response and an uncertainty of the response and occasional overshoot led to time-off-target in the initial acquisition. Once stabilized, the aircraft was quite steady, and a good targeting solution was reached. There was a certain amount of lateral bobble, however, noted throughout; and the reason for the rating of 4 was that particular bobble. No noticeable secondary motions were induced. A certain amount of pitch compensation was needed, but that could not be directly attributed to the use of wings-level turn mode. Very possibly it was simply due to the normal maneuvering of the aircraft.

Centerstick was only used at the very end of the run when a very large turn required full CCV inputs and approaching a moderate buffet in the CCV. That could be held; it was kind of like maximum usable — a fairly good indication that you do have maximum usable inputs in and that a certain amount of aid with the ailerons was helpful.

WLT7. On this configuration the Cooper-Harper rating would be 6. I find that trying to get the amount of response I desire out of the aircraft is difficult. However, I don't have the particular yaw bobble that I noted previously. It seems to be more a function of either slow response or inability to get what I desire, requiring much greater input to get the response. There is a certain amount of uncertainty and difficulty in stabilizing on the condition. There were times toward the end of the run where a wrong lead of an input brought the pipper quite a ways off target, and it was very difficult to reacquire at that time. This particular configuration should be looked at again to see what the problem is that arises with that particular situation.

Centerstick was basically not used during this particular mode until the time-off-target was noted and correction was needed simply to get the pipper back in the vicinity of the target. This also should show up on the film.

A second runthrough on WLT7 was necessary, and the feeling now is that this configuration would be more properly rated a 5. There is still a great deal of pilot compensation needed, but it seems to be less now than it was before. This may be a learning effect, but perhaps is simply a matter of getting the inputs in more properly. There is still a tendency for the inputs to be difficult to time and execute, so there are overshoots from the desired position — a requirement to come back on target. It's not an overshoot with a damping out on the thing; it's more a factor of heavy residual inputs.

At this time there was no centerstick used even at the very end with a very high angle offset and tracking. The CCV mode held although it was on the buffet or the edge maximum controllable; maximum use of the controls was seen.

WLT10. This was a very interesting run. The most noticeable effect was that a rudder input quickly resulted in the wings following the target airplane, so that it was a helpful effect. In other words, the aircraft was going to roll in to follow as well. The problem that resulted from that, however, is that the pipper movement was not necessarily

identical with the movement of the airplane, so there was considerable offset of the pipper from the target in the process of this rapid roll. A certain amount of difficulty was noted in what one might call a control harmony in the attempt to get the control inputs matched with the amount of roll to help out the rider. The quickening of the response was good in that the aircraft was soon matching the target aircraft. However, the time-on-target suffered because of the aforementioned problem.

The end result of this as far as the Cooper-Harper rating is concerned would be a 5, and this is because compensation is needed to get the pipper back on target, despite the aid that is seen from the roll actions of the aircraft. So, while there are some benefits to be achieved, the corresponding detriment in time-on-target with the pipper must be accounted for. Centerstick is definitely needed to coordinate, but it is difficult to match because of the previously mentioned control harmony problem.

WLT11. This most recent configuration received a Cooper-Harper of 7.5. The reason for this is that adequate performance was not possible for the actual task, mainly because of the discomfort caused by attempting to cross-control the aircraft. The use of the centerstick was absolutely necessary throughout, and in a reverse motion, so that the aircraft was being cross-controlled. The feeling to the pilot was very unsettling, and it would be completely unacceptable for the task. Controllability was enough in question to bring it out of the 7 category. However, at no time was control, in my mind, threatened sufficiently to go to a 9. I feel the 8 rating would be quite likely, and that 7.5 indicates that controllability is a slight problem but not a total one. Controllability was not always in question throughout the run.

WLT12. The reason for the rating of 7 on this one was similar to a previous one where the rudders commanded a great deal of roll. Again, the problem was an oversensitivity of the roll axis so that the pipper was spending a great deal of time off target. The abruptness of the

turn created by the rudder was such that time-on-target suffered dramatically. The pilot's head tended to be thrown back and forth in the cockpit whenever the amount of rudder was changed. The hopefulness of matching the wing position of the target aircraft was favorable; however, the detriment caused by the rapidness of the turn with rudder, and difficulty of smoothing that out, made the overall performance unacceptable. While centerstick should have been used during this, the rapidness of the roll made it virtually impossible to bring those inputs into the picture suitably, so centerstick was not used during this particular run.

WLT10. We'll be going back to wings-level turn. The gains have been set properly; the sensitivity has been set properly. The sensitivity has been reduced on the rudder to 500 for this run. The aircraft could be better handled with the rudder turn...with a certain amount of coordination using the centerstick for roll control. Amount of time-on-target was acceptable. However, the need to coordinate the bank angle inputs along with the rudder axis is less than desirable, and the improvement would be to remove the need for a roll adjustment on the centerstick.

WLT12. With the same lowered sensitivity in the rudder axis of 500 this particular run got a Cooper-harper rating of 6. It was possible to track throughout the maneuver with a combination of rolls. While acceptable performance was possible, a great deal of pilot compensation was needed because of the abruptness of the use of rudders. The rudders are simply not good for very gradual and accurate rolls. I think that the major problems I'm seeing here is that whenever I try to make a very small change in roll angle, in order to match with the target aircraft, I get initially a very abrupt movement created by the powerful rudders, a very sharp change in roll angle and a resultant movement of the pipper off target along with the pilot's head bouncing around in the cockpit. So, to get adequate performance requires an extensive amount of compensation to try to bleed in very small and accurate amounts of rudder and to then hold that amount, if possible, with centerstick. Centerstick

was used during the run, on occasion, but no particular correlation could be seen because of the difficulty of anticipating and matching inputs. I don't feel that the particular configuration is helpful, because of the difficulty of getting the proper amount of roll simply due to rudder for a tracking solution.

WLT1. This time the nominal case resulted in a Cooper-Harper rating of 4. The reason for that was that there seemed to be a lag in the system. It was difficult for me to get the desired amount of response out to track the aircraft. The wings-level turn mode itself was excellent in maintaining position. The centerstick was not required, seeing that the use of centerstick would help the yaw rate to increase. So the only reason for the 4 (instead of a higher rating) is simply because the command rate against movement begun was not sufficiently rapid to achieve a higher rating. Time-on-target, once established and matched with the target aircraft, was very good, but the perceived delay in the system resulted in a high Cooper-Harper.

WLT12. Our last run has a sensitivity rating of 350 in the rudder action. This configuration was finally acceptable, with a Cooper-Harper of 5. The reason for that rating is that it still does not offer the optimum configuration for a wings-level turn, in that the wings are not level.

In other words, I do not feel that this is an ideal use of the wings-level turn mode. All we are really doing is to allow the pilot to use the rudder to make roll inputs, and I don't feel this is the best use of the particular technique. With reduced sensitivity, the amount of being tossed around the cockpit and time-off-target were reduced to the point where the situation could be used for actual solution, although more control authority seemed to be required in order to get the results. Previous cases were marginal at best; in this case an adequate case could be gained in this particular mode. But the fact remains that any time an abrupt change is made there is time spent off target needlessly, and the pilot is thrown around the cockpit in a manner not desirable nor particularly necessary. The centerstick was being used,

in conjunction. As the sensitivity drops on the rudder, the amount of pilot input on the centerstick can be fed in more properly.

A final comment here is the amount of side loads we are getting on the aircraft and, with the roll being increased by rudder applications, the amount of jostling is high. If the g-forces were increased it would be completely unacceptable. With the present g-levels, acceptable is about as good as we can say for it. In addition, with the nominal modes, where the wings-level turn is being held more appropriately, I think we can say that the amount of g generated, even in the maximum deflection, is tolerable as presently set up. Because of the implicit return of increased time-on-target, the pilot will accept the uncomfortable side load feeling. Instantaneous sideload g is another factor that must be considered, and will result to a certain degree in time-off-target, but can be compensated for rapidly.

LT2. The first evaluation of LT2 is that the optimum use of this particular technique will be in the formation test. The response in air-to-air is usable, but does not appear to be as quick as it was for the wings-level turn mode. The preference seems to be in the formation mode for the use of this, although it could be used in air-to-air if the sensitivity perhaps were increased somewhat. A mild amount of centerstick was needed when sliding into the target vehicle in formation. This may have been psychological more than anything else, but there was just a slight amount being used at the time. Also, just to correct when we crossed over the wake of the preceding aircraft, a certain amount of centerstick was necessary. Overall, the Cooper-Harper rating of 5 for the formation was indicates a high degree of confidence and ability to make movements toward and away from the lead aircraft.

The air-to-air test was virtually impossible. There was no question of controllability, so we'll give it an 8, but the task itself could not be done. There's no way to keep the pipper on target.

LT1. An excellent configuration — 2.5 was the Cooper-Harper assigned to it. Very little centerstick was needed, very smooth, not very abrupt, but very tolerable. For air-to-air it was a bit slow, and probably not very desirable.

LT1Y. Similarities were found with LT2. In the formation mode the particular configuration is acceptable because the nuisance modes are not too severe. However, the one place where that will start showing up is in closing on the target vehicle, where the gains start rising as the proximity of the two aircraft decreases. So, when the pilot's gains go up, the uncertainty of the rollout or the release of controls where there is a definite yaw bobble would be objectionable. In general, however, for a loose formation the tendency is more toward the annoying side; hence the rating of 4.5. At loose formation the tendency is annoying; for close formation it would be objectionable and closer to the 5 rating. It becomes extremely noticeable when one gets out to the air-to-air task, where there is a virtually constant yaw bobble. As the pilot attempts to use very small inputs of rudder to hold the aircraft on target, the piper is moving from side to side in a very unsatisfactory manner, so this particular mode would not be usable for the air-to-air task.

Summary Comments. Perhaps one of the situations worthy of comment here is that, although the lateral translation mode seems reasonable for the formation tests, a question remains in my mind as to whether it is really necessary. The same movements we were able to achieve in formation with the mode could almost virtually as easily have been done in a fighter high-performance aircraft by just minor changes in either the rudder and/or the aileron.

Summarizing the air-to-air tracking task with the four lateral translation modes, the best of the four would be LT2. This had the least amount of delay and was acceptable, and I would consider a Cooper-Harper to be approximately a 4 for this mode. It was somewhat slower than desired, but that particularly response might be able to be quickened, so with that in mind I feel LT2 could be a Cooper-Harper of 4.

Next in priority, and this is with very minimal centerstick usage, is LT1, which is a very stable platform. However, the stability was marred by a fairly slow response time, and therefore a Cooper-Harper would be a 6. Extensive pilot compensation was required to bring the

aircraft around and try to hold it on target. In fact, let me modify that; no, I'll stick with the 6. What you may find is that some people will call that same configuration a 7, because of time-on-target. If driven out of some fairly tight consideration, the response may be too slow to ever get back on target, except by sheer chance. I don't like the 6.5 thing, but you may find people on either side of the boundary there, depending on how the setup occurs.

Two were unacceptable — LT2Y and LT1Y. Of the two, the worst was LT2Y, which would be 8 at least. The reason for that is there was just a constant bobbling in the yaw mode. It was virtually impossible to ever stabilize on target because of what had previously been described as a nuisance mode of bobbling on entry or exit from a yaw input. This now becomes a driving force when you are trying to make very small corrections in the rudder axis, so I think an 8 or 8.5 would be an appropriate rating. The tighter the loop is attempted to be closed, the higher the gains, and the worse that rating is going to become. As stated before, in the lower gain formation task a nuisance is seen in LT2Y. As the pilot attempts to increase his accuracy in any particular situation, using the rudder pedal inputs, the worse his performance is going to become with that particular configuration.

LT1Y exhibited many of the same problems. The difference here would only be that with LT1Y I seem to be able to keep the pipper in the vicinity of the target. There was slightly less wandering about, but still unacceptable. I would say that LT1Y was perhaps a 7, because controllability was more addressable. In none of these modes was the centerstick necessarily required. It appeared, as I mentioned earlier in formation flight, that on occasion a certain amount of roll input, aileron input, away from the target aircraft was being put in. I think that was primarily psychological, just to start slowing the rate of closure even though the feet were commanding one thing, the hands were sort of compensating and slowing the rate a bit. So I'm not certain this was a tendency to level wings or to do anything other than to provide a little backup psychological input, and I would minimize any emphasis on this area. The wings-level correction, the rudder pedal

inputs, were far and away the most important inputs, and the centerstick was nothing more than that required for ordinary formation. Again I would stress that since we are talking about formation primarily the gains are somewhat lower, as presently designed, than for the air-to-air task, so the deficiencies may not be quite so obvious. Along with that, the benefits are not particularly obvious, because these same results can be obtained with very small rudder inputs, or very small bank and heading change corrections in normal formation, so the applicability of this particular mode to formation flying is questionable in my mind.

Final Comments for Comparing the In-Flight Simulation with the LAMARS Simulation at Wright-Patterson. Each has particular advantages and disadvantages. The advantages seen now in the flight test: particularly the side g-loading that can be felt and maintained throughout the flight; and the presence of a real target with real inputs as opposed to a less real one. This latter is, as I think about it, perhaps more minor than the first. The problem with LAMARS is the washout of any g-inputs and the particular difficulty with this kind of testing is the uncertainty of what pilots will be able to sustain. I feel that in-flight testing therefore is invaluable. On the other hand, the place that in-flight fell down was in realism of high-speed simulation, and therefore the LAMARS backs up in-flight testing in the light aircraft by providing the possibility of 0.8 Mach, 20,000 ft type information, with a cockpit that give you an appropriate configuration for the CCV vehicle. One of the other minor failures of the Navion was the failure to have the sidestick controller; the differences there, while not likely to lead to changes in Cooper-Harper ratings, are still another difference between the two that must be accounted for.

I've searched through my mind to see if there were other differences worthy of comment, but my summary statement would be that the two need to be used in conjunction. The in-flight testing of the Navion fills in the holes and broadens the spectrum of approach to the problem. However, I don't see the Navion as established as being able to get sufficient data on all the modes being looked at in LAMARS, so LAMARS still has its place, and perhaps even a primary place for developing the

desirable gains that can then be flight tested and verified in the Navion.

One final thing that bear repetition is the question of sensitivities, and I find myself still wondering if the method of choice of sensitivities is adequate. The pilot gets in the aircraft, flies for perhaps five to ten minutes, adjusting the gains and feeling out the control system, and then chooses some set of gains that he feels are comfortable for him. The problem is that then we do not readjust after each changing configuration, and it is possible that for a given configuration a given sensitivity is needed. Therefore, it would seem, if time is available, that rather than simply asking the pilot to choose one that is comfortable for him, a variety of sensitivities (at least in the rudder mode) should be selected. In those cases where the aileron axis, the roll axis, is going to be excited or augmented, then a similar attempt should be made there to vary sensitivities in that mode as well. This is speaking solely of the Navion experience. I think a certain degree of difference in Cooper-Harper ratings was seen when we varied the sensitivity of the rudder axis, when we were inducing roll with rudder, roll due to yaw so to speak, and the change in sensitivity made a considerable difference in the Cooper-Harper ratings. As the sensitivity went down, the ratings improved. So, although I might have found a particular sensitivity to be desirable overall, changes may occur because of a change in overall aircraft configuration as we move from one mode to the next. I think it is worthy of consideration anyway that perhaps three widely differing sensitivities should be chosen and implemented as part of the test matrix. [Editor's note: The pilots were all briefed to spend as much time as they felt necessary to determine the optimum sensitivity for each configuration. Unfortunately, Mike Phillips had a scheduling problem and had to leave after only two days of testing. The pressure of this schedule probably resulted in our spending less time varying control sensitivity than was desirable. Based on these comments, more evaluation time was allocated to control sensitivity variations with the three subsequent evaluation pilots.]

ROGER HOH, 4 December 1979

Background. Primarily in general aviation aircraft (4000 hours); also has extensive experience as subject pilot in ground and flight research simulators. Used primarily to finalize test scenario, as well as to select final configurations for evaluation. However, his ratings were averaged in with those of the other three pilots.

WLT1. Pilot rating is 3, and I didn't notice any significant secondary coupling. I would rate my tracking performance as very good. The use of bank angle to assist in tracking the target is only required during some of the extreme target maneuvers, and coordination between the CCV mode and lateral stick for bank angle is quite easy to accomplish. That is, the two modes are harmonious. Having a CCV mode wherein it can be used to augment the basic airplane bank angle is very favorable. The pilot rating of 3 is primarily oriented toward the CCV mode, and the primary tracking was done with rudder pedals.

WLT2. I couldn't see any significance difference between this and WLT1. Pilot rating is a 3. Sensitivity on WLT1 and WLT2 was 800. Tracking using WLT2 was considered to be excellent, and there's little equalization required. For very large bank angles I used centerstick to keep up with the target, and that seemed to work very well.

WLT3. Pilot rating was 4.5; the sensitivity is 800. I did some tracking while the target was stationary doing step changes with the nose, and there's obviously some initial yaw in the wrong direction. In doing the actual tracking this resulted in some nose bobbling or gunsight bobbling about the target when attempting to track in a very tight fashion. The apparent neutral stability when attempting to tighten up is the basic reason behind the 4.5. This is indicative of the considerable pilot equalization required to try to hold the aimpoint on the target, and the necessity to kind of loosen up a little bit in order to get a good tracking score.

WLT4. Configuration WLT4 had a significant amount of adverse coupling. It was nearly impossible to do any usable tracking. My tracking

performance was poor. There's no tendency to lose control of the aircraft; it's just that you can't do the assigned task in an adequate manner. For that reason the pilot rating is 6. It's possible to hold the pippet on the target during the less violent maneuvering. However, during times when it was necessary to tighten up, it was found to be nearly impossible. Sensitivity on WLT4 was 800.

WLT5. Sensitivity on this configuration was varied until I found what I felt to be at least acceptable, if not optimum. The configuration had some proverse coupling; that is, it tends to yaw into the turn initially, and this can be a little touchy at the higher sensitivity levels. The sensitivity I found to be most acceptable was 600. With a sensitivity of 600 I'd give it a pilot rating of 3 to do the task. Once the sensitivity was optimized I found it was an excellent configuration, and tracking the target was no problem at all. In fact, the coupling seemed to augment my ability to do the tracking and keep the pippet on the target.

WLT10. Sensitivity 800 and the pilot rating is 4. This configuration had a significant amount of roll coupling. Its primary problem was that, when attempting to tighten up, the motions in the cockpit are quite violent. The aircraft appears to be rolling about the velocity vector, and there's a lot of lateral acceleration to pedal, resulting in very abrupt lateral motions of the head and shoulders to abrupt pedal inputs. When trying to tighten up with the CCV control you get a very abrupt rattling around in the cockpit. The ability to actually track was not degraded significantly by this, and for that reason the pilot rating is a 4. However, if I were going to rate the ride I'd give it a 5.5. One or two times while tracking the target when it made some reversals I found that the aimpoint wandered excessively. This was either due to my head motion or to a coupling between my head and feet which resulted in some excursions themselves. I can't really pin down precisely what it was, but the gunsight did wander significantly off the target one or two times during the run. As long as I was able to use smooth control I was able to hold the pippet on the target, so I'm sort

of torn a little bit on the rating. I would say for the cases where I really have to tighten down it might even degraded to 5.5, maybe even a 6. I believe that lowering the sensitivity would improve this significantly. I think one of the problems we might have had here was a sensitivity problem, and the rating of 4 indicates a willingness on my part to back off and do some internal smoothing; whereas the 6 rating perhaps is more consistent with an unwillingness to do that and to want it to be done for me. I should point out that it's not strictly a ride quality problem, and that the motions are reflected in actual tracking errors and inability to do the task. I should emphasize that sensitivity is probably the key issue here. We should have run some lower sensitivities for this case, and later perhaps we'll get to WLT12 and vary the sensitivity.

WLT11. Sensitivity 800 and pilot rating 6.5. This configuration had some unfavorable coupling in roll. My technique in flying this was to sort of coordinate with lateral stick. For example, in a left turn I fed in left pedal to utilize the CCV mode, and also left stick to counter the right roll coupling. However, the tendency to roll right and yaw left made the tracking very difficult. The tracking scores would be very poor for this configuration, and the pilot rating would be on the order of 6.5.

LT1. Tracking a target and translating sideways was not what I would consider an optimum way to track a target. However, given that that's the way we are doing it, the pilot rating would be about 5. I could achieve adequate performance, but it required considerable compensation, and the compensation is primarily that which arises from its being a fairly slow mode. That is, when he turns I have to translate sideways and it seems like I drift past him or have trouble catching up with him, and I'm just not able to hold the piper on the target continuously.

LT1Y. In this case there was some yaw added. It looked almost like a wings-level turn, actually, and holding the piper on the target was very simple. This was of course compounded by the fact that the target

motions are reduced for this mode, so the target's moving very mildly and I have a very powerful aiming device. The pilot rating is a 2.5 and my tracking was excellent.

WLT12. Sensitivity 800; pilot rating is a 5. A lot of proverse coupling. It tends to be somewhat twitchy and sensitivity, and the rating of 5 is primarily for the same reasons I did not like Configuration WLT10.

Reducing the sensitivity to 400 improved this configuration dramatically and made it quite easy to track the target. However, I felt we were reaching a limit of useful authority on the CCV mode and had to start helping it some using the centerstick on the larger target maneuvers. Pilot rating is a 3.5.

With a sensitivity of 200 this configuration definitely is lacking in control power and it's mostly a bank angle tracking task. It is very difficult to keep the pipper on the target. Basically, maneuvering with bank angle using lateral stick the CCV mode is primarily a nuisance. If we're rating the CCV mode, the pilot rating would be a 7. That rating could probably be increased to a 4 or a 3.5 or whatever if I just ignore the CCV mode and track with the bank angle only.

General Comments. I did not feel that the lateral acceleration levels were at all unacceptable. The only time lateral acceleration was noticeable was on the favorable roll coupling cases where we seemed to rattle around a bit. There was some very abrupt side motions, but because of the tremendous improvement in tracking capability they weren't really bothersome and were not a problem basically. For the lateral acceleration levels achieved today I don't see any problem.

I think an important comment is that coordinating the wings-level turn CCV mode, and perhaps even the lateral translation CCV mode, with a basic aircraft bank to turn mode has some very favorable aspects, and that a CCV mode probably should be evaluated in conjunction with the basic airplane bank. That allows you to do some very good tracking with precision, using the bank angle as a coarse mode to do the large high-g

turns and then the CCV mode to home in and nail the pipper on target. I think the two modes in conjunction should be evaluated together, and are complementary to each other.

KEVEN OLSON, 4 December 1979

Background. Air Force fighter pilot. Currently flying F-105 in reserves and flying for Eastern Airlines (5500 hours total time, 1300 in fighters).

WLT1. Rating a 4. Significant amount of pilot compensation required, especially during the more violent maneuvers. Very fine tracking was almost impossible without a lot of compensation.

WLT2. Rating a 2.5. I found the tracking, especially during the more violent maneuvers, much easier; was able to maintain a more steady pipper and anticipate the changes quicker. Generally, it was a steadier airplane to fly; not as much jumping around and the pipper remained on target much easier.

WLT3. Rating of 5. I found it particularly difficult to track during turn reversals as the pipper would be lagging behind — understeering, if you will. Using the WLT1 as a base with a 4, I'd have to give this a 5.

WLT4. Rating a 6.5. I was able to perform the task in that I had the pipper on the target about 10 percent of the time, but that's it. Most of the time I was undershooting or overshooting, especially in the turn reversals, and the higher-g maneuvers. If 10 percent pipper on target is adequate performance, then it was successful; but controllability was a real problem.

WLT5. Much better configuration; has the pipper on the target perhaps 80 or 90 percent of the time, even in the higher-g turns. Give that run a 2.5.

WLT12. Able to keep the pipper on the target 50 to 60 percent of the time. A lot of shaking around in the cockpit, being thrown around quite a bit. Not real easy to work, but acceptable. Turn reversals weren't as difficult to maintain, to stay with, as on some of the other configurations. I'm going to give that a rating of 4.5. Sensitivity was 80.

Gain of 400 — I'd have to think about this rating for a second. The airplane was more comfortable to fly, as far as transverse g's in the cockpit. However, I was surprised to find that I wasn't able to track better than I did. I estimate pipper was on the target about 60 percent of the time, but I felt I was behind the airplane quite a bit in my directions. Give that a rating of 4.

WLT11. Gain of 800. Pipper was on the target no more than about 5 percent of the time — marginal as to the success of the mission. Perhaps a snap shot or a lucky shot might have been successful, but I got behind the airplane very quickly in the run and lost it by 10 degrees off the nose hard to get it back, side forces were stalling out. Perhaps some practice in flying the configuration might make it minimally acceptable, but right now it's unacceptable. Controllability was not a question, but performance is marginal. I'm going to give it a 7.

LTL. Gain 800; I give that a rating of 4. I managed to keep the pipper on the target 60 to 70 percent of the time. However, once anything but minor bank angles were attempted it became very difficult. Small bank excursions; no problems keeping pipper on the airplane.

Formation flying task rated a 3. You do the job pretty well as long as straight and level flight is maintained — make small corrections, simulated refueling operation. Not any particular problem, but I imagine a problem would be encountered if I tried to refuel in a turn with any g's on the airplane.

LT1Y. 800 sensitivity on run 10. Give that a 2.5 no real problems, piper remained on the target 85-90 percent of the time. Slight transverse oscillation, so I'm trying to keep it exactly centered, but no real problem.

Run 12, for air-to-air refueling, give it a 3.5. Didn't like it as well as the other formation — a little too sensitive, a little too jerky. I felt that unless you paid a lot of attention to what you were doing you could inadvertently snap a refueling hose with a quick jerk, left to right a little too snappy.

BARRY NIXON, 3 December 1979

Background. Captain in Navy Reserves. Most operational experience in large multi-engine aircraft (antisubmarine warfare). Considerable experience as evaluation pilot and safety pilot on VRA. Was primary safety pilot on this program.

WL1. The baseline configuration, and I gave it a rating of 3.5. I really thought it was going to be more like a 2.5 to 3 through the beginning of the run, but then I lost the target airplane a couple of times and had to use bank angle to reinforce the side force modes. It caught me a little bit short, a little behind. What raised it to a 3.5 was the lack of control power to really hang in there totally on rudder control. What was nice was the almost absolute lack of any secondary motions as far as Question 3 [see Fig. C-2]. There were no distracting motions particularly, just a little bobble from the side force panels one time — otherwise no distractions, either primary or secondary. In answer to Question 5, I did have to use the roll stick on about three occasions, definite use of the roll stick to reinforce side force input to catch up with the target airplane. The rest of the time I think it was just jittering or feathering or trying to correct the wings to wings-level. Sensitivity was 400.

On run 2, with a sensitivity of 800. The Cooper-Harper rating improved to 3, based primarily on the increased sensitivity. It allowed me to hang with the target airplane with a little less effort. Even

though I had gotten the full control power in the previous run, it was a little bothersome having to use quite so much pedal and force. It came a little easier this time; I only lost the target airplane once, of any significance. That's not to say I'm on target 100 percent of the time, but I think I'm holding target 50 percent of the time, maybe a little bit better that run. I did notice a tendency to overshoot in the roll reversals of the lead airplane, just due to the increased sensitivity, perhaps a little less than 800 might be better. Again, no secondary motions are bothering me, except perhaps that little bit of primary overshoot in heading, in yaw. I don't notice any other specific deficiencies that are affecting the path. Again I used the roll stick one time to help out on the side forces when I began to lose the lead airplane and approach the burble, but the rest of the time it was just nominally keeping the wings level.

WLT2. With a sensitivity of 800, a rating of 4. A nominal task; it's no specific problem. I guess the closest thing to bothering me is a little greater sensitivity in yaw, like a little greater oscillation, a little upset, a little coupling. I don't see anything in roll, but as I try to track it onto the target I seem not to always overshoot but just oscillate around the target for a second and then damp out. As soon as he moves again I start dithering around the target a little bit. It did have a bit of influence then on target time; I don't know what, but less than 50 percent, perhaps 40 percent or 30 percent on target. But, still a rather satisfying accomplishment of the task, by my standards. Any auxiliary influences that were not primary — I don't see any yet. No adverse coupling roll or anything else is bothering me. I only had to use the centerstick once on a large excursion where the side forces started to stall. I used bank angle to help track the lead airplane, but that's no great interference if you know it's coming.

WLT3. Sensitivity 800, rating of 4. Not too different from the last run. It was a nice challenging task, it's taking some compensation by the pilot; but it's not very difficult — doing very little in the roll, tracking with the rudder, but a slight tendency to dither or over-

shoot or almost PIO. There's just a little tenseness there, maybe it's just the unusual nature of the task. Just minor problem there, really can't see the difference between this run and the previous run. Still not noticing any secondary motions, such as an annoying dutch roll or any kind of roll. No secondary influences that I can spot. Again, the stick is just being used as an auxiliary device for a very large excursion.

WLT5. Sensitivity 800, Cooper-Harper rating a 6. All of a sudden becoming bothered by a lateral motion, a yaw oscillation, that I could not quite get rid of; it seems to be something in the control combination. It took extensive compensation to get adequate performance, and I don't know how much time on target. I was passing through it quite often but not really holding very well — just a small percentage of the time really hugging the target. It seemed almost like a pilot-induced dithering, back and forth and yaw across the target, and that was the primary distraction. As for any secondary motions, I'm not aware of any side accelerations or roll that are upsetting me. It all seemed to be a yaw problem, getting the nose quieted down. Since I am working primarily with rudders I am not even aware of using the stick except maybe occasionally on a very high amplitude turn where I just know it's going to be needed to allow me to stay with the target without stalling the side force panels. For Question 4, deficiencies that aren't a primary influence, I guess I'm just not aware of anything else entering into the task, just the primary influence of yawing motion. Finally, again, using the roll stick as an auxiliary control for high amplitude augmentation in the turn, the test of the time just dithering with it, just trying to keep the wings level.

Second Flight

WLT1. Sensitivity 800, Cooper-Harper rating 3.5. Getting above this minor deficiency and the trouble there just seemed to be control power this time, in that trying to keep to the target airplane using the feet alone was a very nice mode of operation up until I exceeded the stall angle of attack on the side force, got a burble, and had to back

off and introduce some roll component to make up for it. Just the transient involved in catching up again was just a bit annoying. Got back on target very quickly and could get the rollout back, the wings level, and do a very nice job of tracking with the feet in between the big excursions. No secondary upsets; I did have to use the centerstick to catch up on the large excursions.

WLT5. Sensitivity of 800, rating of 4.5. I was getting what I call adequate performance, keeping the piper on the target airplane laterally. Again, a couple of large excursions. I started to stall the side force panels, had to add in some bank angle in order to compensate, but that wasn't so bad. That only happens on one or two occasions. There seemed to be a tendency to somehow overshoot the target. I'm not quite able to hold the piper on, a little overcontrolling somehow, little dithering back and forth. I got adequate performance but I was getting up to considerable compensation — a little relaxed part of the time, working pretty hard the rest of the time.

With a sensitivity of 450, gave it a rating of 4. What happened was that the lower sensitivity helped with the dithering problem or the sensitivity "overcontrol" problem I had on the previous run of getting on target due to dancing back and forth. But, it increased the problem of not quite keeping up with the target airplane in a sudden turn reversal. I just didn't have sufficient control power to catch up with them, hold them. As a result, I had to come in with aileron control, roll control, much more than I was ready for, and lost out in that sense. So what we had was a trade in that the compensation went down and I wasn't working nearly so hard on the controls when I had them in the sights, but my performance went down a little bit too in that I lost them for a period in the large reversals. Overall, a slight gain — not working quite so hard and doing almost the same job.

WLT11. Sensitivity at 800; Cooper of 6.5 and almost 7. In fact, I was debating whether I was actually getting the mission done, whether I was getting adequate performance. The performance, whether or not it was adequate, bothered me. I don't know that much about the guidelines

for air-to-air combat, but I might have been on target 25 percent of the time. The extreme difficulty in holding the target is due to what seems to be some sort of adverse feature of the airplane; that is, when you kick to go one way with the rudders I guess the nose goes the opposite way, maybe even the roll. It's very hard to catch up with the target airplane. Just about the time you're getting him acquired and start to hold him, off he goes a different direction and you lose it. But, there were periods when I almost could have said I was holding him long enough to get some firing time in. That's why I debated on the 6.5 to 7. For pure task performance 6.5, but 7 overall; you just wouldn't want to try to do the task that way if you could get it improved. As to what the side issues are, I think everything adds up to affect the primary problem. I just can't get the airplane quieted down and pointed right. I don't know whether it's an adverse yawing or adverse rolling, I couldn't quite pick that up, or whether it's light damping — but I couldn't keep the pipper on the target. Using the stick, yes, considerably; only way to do the problem is to use the roll stick. Seems like it rolls the wrong way when you first start kicking, and you have to use the stick to get the wings level, and even turn with the target airplane to keep up with him, so it's very busy on the stick.

Third Flight

WLT13. New configuration, sensitivity 800, Cooper rating 6. Improvement over WLT11 in that the cross-coupling wasn't quite so bad. I think it's an adverse coupling of some sort; it rolls. As soon as you try to use the rudder I think you roll adversely to the turn. It's a little upsetting, wasn't quite so bad. I improved the situation but it's still barely getting what I would call adequate performance. It takes just about all the compensation I've got to stay on the target maybe 25 percent of the time, so we'll give that an even 6.

WLT14. Sensitivity 800, Cooper rating 4.5. Still seems to be a little adverse coupling up back, but much diminished now, so that wasn't too unpleasant. I almost thought for a bit that it might just be moderate compensation, getting the performance I wanted. Very nearly so, I'd

say almost more desired than just plain adequate. On target quite a bit, but a little more than moderate compensation would do it — edging toward quite a bit of compensation — so there's a fine cut in there. Call it a 4.5. The primary problem seems to be, again, just the adverse coupling and the upset just as you start to track the change in the lead aircraft. As soon as you get on target again everything quiets down, looks all right. No secondary problems seem to come in — no oscillation, no roll, no pitching problem. Using a roll stick to a moderate extent, though, to counter the adverse coupling and to make sure I stay with the target on the maximum excursions. It burbled the side force panels once in a while.

WLT12. Sensitivity of 400, Cooper rating 5. In this case I had proverse roll coupling. That's a much more natural motion when I go to use the rudder to chase the target to have the plane bank that way, because it helps alleviate, right away, part of the exceeding the maximum capability and getting a burble; I'm already into a bank and, rather than fight it, I just go with it. But, even so, a few things bothered me. It seemed to have a tendency to oscillate and roll a little bit; that really wasn't affecting the task, so that might better be called a secondary effect. But, the primary interference was just a sensitivity low enough so I didn't have sufficient control power compared to what I have been applying. I just fell behind the lead airplane several times so I couldn't quite get the desired performance. I think it was adequate, as I was probably on target 50-60 percent of the time, or very close to it. A little more sensitivity, I thought, might have helped me hang right in there. The roll oscillation seemed to be present, whereas I hadn't noticed it once I got on target before. The time-on-target tended to roll back and forth just a little bit, as though I was feeding it. Although it wasn't distracting from the task too much, it was an annoyance; so that may be one of your side factors. And, of course, I'm using the stick to a moderate extent here, trying to damp that little bit of roll and to modulate the roll due to the favorable coupling here. I'm not really working too hard at it, but I notice I am using it to some extent.

With sensitivity raised to 600. I kind of thought that was going to help and asked for it; Cooper rating 4.5 — 4 to 4.5 — hedging because I thought it was better at first in that there was a little snappier response in heading control, keeping the pipper moving laterally to keep up with the target airplane, but increased problem with the roll coupling, which was reverse. It goes in the right direction, but it's just a little bit too snappy. I tend to fight it a little with the stick, and I think I'm getting into a little PIO in roll. So, my primary deficiency of heading control has been eased, but the secondary deficiency of roll upsets and bobbling back and forth in roll is getting a little disconcerting. I think a heading alone rating of 4 — you have to work at it but it's not bad, and you get pretty good performance; but it's a 4.5 based on the fact that roll coupling is getting a little upsetting. Just guessing that a sensitivity halfway in between might be the better part of two worlds. Finally, with respect to use of the roll stick. I used it, it seemed, dithering with it considerably the first half of the run. Then I arbitrarily tried to take my hand off it and not use it, just use the rudder. And indeed, the problem improved again, in that I have pretty steady tracking and no roll oscillations is disconcert me, but then I would like to have use of roll stick if I could have it. By that I mean that the fact that I wasn't fighting the roll upsets, just let them die out, made me feel a little bit better about this whole situation.

WLT4. Considering the couple of configurations I had right ahead of it, it was very nice — no roll coupling and on the primary problem of just sliding the heading back and forth laterally to keep up with the lead airplane I did a pretty nice job. It felt pretty comfortable; I was maybe a little lucky on target, I thought something like 70-80 percent of the time, and that felt really good. I noticed was that when I'd lose the target, they'd pull away from me a little right at the beginning of a turn, which makes me think I was lagging somehow in heading pointing, but I'd catch up pretty good. Then when he started to roll out, same thing; I'd seem to overshoot a little. Maybe an adverse yawing tendency. I'd start to go one way, the nose momentarily goes the

other, but I could get it right back in and do a good job of tracking. I was quite pleased with the performance and really was not working very hard on the rudders, so I gave it a 3.5. Roll stick — almost nothing going on, with no roll upset or coupling; doing very little in the rolling department.

WLT15. Sensitivity 600, Cooper rating 6. Things went bad in a hurry on the primary performance parameter. Just holding the pipper on the target was very difficult. There's an adverse yawing of the nose, I guess, with a left rudder, starting to translate left, but the nose points right, with the result that I end up lagging the lead airplane rather badly. When I do finally catch him and go to quiet down or settle out on that bearing or heading, it overshoots adverse the other way. The result was that I was crossing the target a lot of the time when we were steady and lagging behind him every time he started a new maneuver. I thought maybe my performance was fairly adequate — give it a 6 — but working like to dickens to try to hold the nose on them. It makes me think the previous run should have been more like a 4, because I was beginning to see the start of that type of problem. Any other effects are unnoticeable. No rolling problem, to worry me, and I'm using the stick very minimally, generally to try to help catch up when I first lose them at the beginning of a big maneuver; otherwise not paying much attention to it.

WLT4. Repeated with sensitivity at 800; Cooper rating went up a little — 4.5 to 5. I'm giving a range, hedging a little, because I'm a little bothered. Parts of the run seemed better than other parts and I'm not quite sure why, except the one thing that seems to come through is the increased sensitivity just magnified the adverse yaw effect. For a given amount of input that I'm used to with my feet the response was a little larger, allowing for sensitivity, but then so was the adverse yaw effect. It kind of gave me trouble as I tried to catch up with the target and center on it. I seemed to get all sorts of problems settling down on the target, and I did stall the side force panels once or twice with the increased sensitivity. So, it's just the primary task effect.

Auxiliary effects — using some roll just to help out, but I'm not sure how I'm using it, just a moderate usage of the roll stick.

HLT11. Sensitivity of 500, Cooper rating of 6.5 to 7. Now the problem seems to be in roll, which is an adverse roll. In fact, when you pull into the turn it rolls opposite, which is very disconcerting. You learn very quickly to be ready for it, to get ready to overpower with your roll control, and even more than make wings level, to roll into the turn to help out. This is because by then you've started, it seems, to lag the lead airplane. It's a little bit of an odd mix. The actual heading control is not so bad — you can swing with the lead airplane — but you get so disconcerted by the adverse roll that you get thrown off target trying to get your wings either level or into the turn. So, for the actual heading control and on-target, I would have thought it was going to be a better rating; but indeed the side effects of the adverse roll become predominant. So, it's first a 6.5 on strict heading control when there isn't a big reversal. As soon as a big reversal comes along I'm all fouled up and it takes me forever to catch up, and that's the 7. I'm really not getting adequate performance at this point. Using the stick — I would say yes, using it to a maximum extent to try to get the roll under control.

LT1. Sensitivity 800. I gave it a Cooper rating of 4 on the air combat maneuvering pass. They were mild turns to suit the nature of the configuration, and that might have eased the problem considerably. Even so, tracking and keeping the piper on seemed to come along rather nicely. The deficiencies may be a tendency to lag once I've caught up with the lead, got on target, and seemed to be able to hold it rather nicely for the moment; the initial departure into a new maneuver seemed to lag, had to catch up. The air combat task was rather like getting a good performance but working moderate compensation, with roll coming into the picture to help out on the excursions; in that respect I was moving the stick to a moderate extent.

Next run with the same configuration. Sensitivity 800. Only a formation task. Rating 2.5; was an easy pass — as I understand it,

straight formation for air-to-air refueling, which I haven't done. Maybe you'd have to be even more precise, but I can move from wingtip to wingtip very readily and hold positive where I want it. Great.

LTIY. Sensitivity of 800; air combat maneuvering task; Cooper rating of 5 — had a little trouble with it. Even though the target had gone a little far in front, a little farther than 600 feet, which I thought would have made the problem a little easier, it made it a little worse. I had considerable problem over the LTI configuration, but it was purely the nose dancing back and forth. I'm not sure what the problem was, but I just had a problem settling down on the heading. I'd get off to one side and I'd go to correct in a given direction and overshoot every time. No matter how I tried to change my gain and all, I just couldn't seem to get it quieted down — just couldn't do a good task. On the heading control, roll is an insignificant problem. I really don't remember using the roll stick an awful lot. Just completely annoyed by the heading task; I think I barely got adequate performance; held the target airplane for maybe 25 percent of the time but working pretty hard at it.

APPENDIX D

DISCUSSION OF COUPLING NUMERATORS, LIMITING FORMS, AND DECOUPLED MODES

COUPLING NUMERATORS

The appearance of coupling numerators in multiple-loop analyses is simply a result of applying Cramer's rule for determinants (see, for example, Ref. 42, page 32). When more than one control input is involved (more than one term on right-hand side of equation), coupling numerators result. This is illustrated in the following example wherein we derive the solution for the response of a DFC aircraft in the lateral translation mode (see Table 2). Finally, some limiting characteristics which can be ascertained from the coupling numerators are derived.

The lateral-directional equations of motion are taken directly from Ref. 27 (page 354) with the addition of a column for DFC inputs as follows. (note that we have assumed $r = s\psi$ and $p = s\phi$ for simplicity in this example.)

$$\begin{bmatrix} s-Y_V & -g/U_0 & s\psi \\ -L'_\beta & s(s-L'_p) & -sL'_r \\ -N'_\beta & -sN'_p & s(s-N'_r) \end{bmatrix} \begin{bmatrix} \beta \\ \phi \\ \psi \end{bmatrix} = \begin{bmatrix} Y^*_{\delta_{DFC}} \\ -L'_{\delta_{DFC}} \\ -N'_{\delta_{DFC}} \end{bmatrix} \delta_{DFC} + \begin{bmatrix} Y^*_{\delta_r} \\ -L'_{\delta_r} \\ -N'_{\delta_r} \end{bmatrix} \delta_r + \begin{bmatrix} Y^*_{\delta_A} \\ -L'_{\delta_A} \\ -N'_{\delta_A} \end{bmatrix} \delta_A \quad (54)$$

where $Y^*_\delta = Y_\delta/U_0$ and primes imply that the cross products of inertia are implicitly accounted for (see Ref. 30, page 257). The block diagram in Fig. 84 represents a generic implementation of the lateral translation mode. Pure gain feedbacks have been used to keep the notation simple. In practice, the feedbacks may have to be equalized to maintain adequate stability margins.

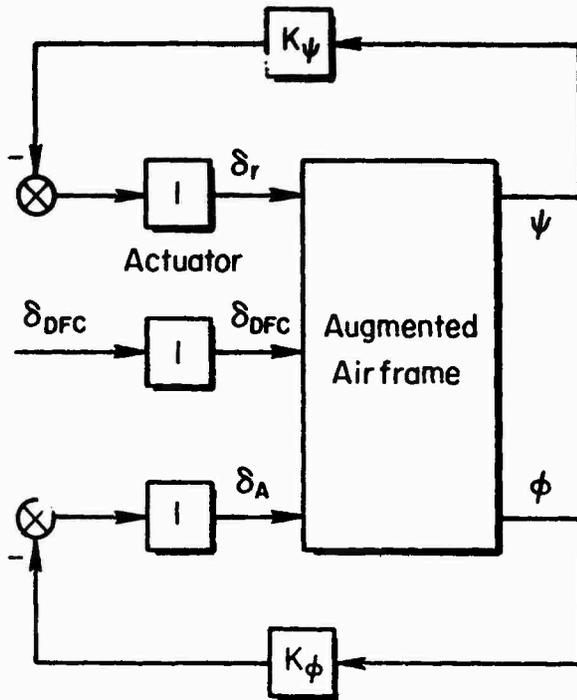


Figure 84. Generic Representation of Lateral Translation Mode

From Fig. 84,

$$\begin{aligned} \delta_r &= -K_\psi \psi \\ \delta_A &= -K_\phi \phi \end{aligned} \quad (55)$$

Substituting Eq. 55 in Eq. 54 results in the following equations of motion:

$$\begin{bmatrix} s - Y_v & -\frac{g}{U_0} + Y_{\delta_A}^* K_\phi & s + Y_{\delta_r}^* K_\psi \\ -L_\beta & s(s - L_p) - L_{\delta_A} K_\phi & -sL_r - L_{\delta_r} K_\psi \\ -N_\beta & -sN_p - N_{\delta_A} K_\phi & s(s - N_r) - N_{\delta_r} K_\psi \end{bmatrix} \begin{bmatrix} \beta \\ \phi \\ \psi \end{bmatrix} = \begin{bmatrix} Y_{\delta_{DFC}}^* \\ L_{\delta_{DFC}} \\ N_{\delta_{DFC}} \end{bmatrix} \delta_{DFC} \quad (56)$$

We are now in a position to solve for the β response* to a δ_{DFC} input. Using Cramer's rule, the characteristic equation (denominator) is simply the determinant of the left side of Eq. 56. The numerator is the determinant of the left side of Eq. 56 with the β column replaced by the DFC input column.

Solving for the numerator of β/δ_{DFC} :

$$N_{\delta_{DFC}}^{\beta''} = \begin{bmatrix} Y_{\delta_{DFC}}^* & -(g/U_0) + Y_{\delta_A}^* K_{\phi} & s + Y_{\delta_r}^* K_{\psi} \\ L_{\delta_{DFC}} & s(s - L_p) - L_{\delta_A} K_{\phi} & -sL_r - L_{\delta_r} K_{\psi} \\ N_{\delta_{DFC}} & -sN_p - N_{\delta_A} K_{\phi} & s(s - N_r) - N_{\delta_r} K_{\psi} \end{bmatrix} \quad (57)$$

where double prime indicates that the numerator is defined with two loops closed. This determinant can be expanded so that each element has only one term. Such expansion results in the coupling numerators. To show how this occurs, we first note the following theorem (Ref. 42, page 9, Theorem 9):

If the elements in one column of a determinant are expressed as binomials, the determinant can be written as the sum of two determinants according to the formula:

*The β and y responses are approximately equal since r and ϕ are approximately zero and

$$y = \int_0^t a_y dt = \int_0^t (\cancel{V_0 r} + \beta - \cancel{g\phi}) dt = \beta t$$

$$\begin{bmatrix} a_{11} & \cdots & (a_{1j}+a_{1j}) & \cdots & a_{1n} \\ a_{21} & \cdots & (a_{2j}+a_{2j}) & \cdots & a_{2n} \\ \cdot & \cdots & \cdot & \cdots & \cdot \\ a_{n1} & \cdots & (a_{nj}+a_{nj}) & \cdots & a_{nn} \end{bmatrix} = \begin{bmatrix} a_{11} & \cdots & a_{1j} & \cdots & a_{1n} \\ a_{21} & \cdots & a_{2j} & \cdots & a_{2n} \\ \cdot & \cdots & \cdot & \cdots & \cdot \\ a_{n1} & \cdots & a_{nj} & \cdots & a_{nn} \end{bmatrix} + \begin{bmatrix} a_{11} & \cdots & a_{1j} & \cdots & a_{1n} \\ a_{21} & \cdots & a_{2j} & \cdots & a_{2n} \\ a_{n1} & \cdots & a_{nj} & \cdots & a_{nn} \end{bmatrix} \quad (58)$$

Following this theorem, Eq. 57 becomes

$$\begin{aligned}
 N_{\delta_{DFC}}^{\beta''} &= \begin{bmatrix} Y_{\delta_{DFC}}^* & -(\xi/U_0) & s \\ L_{\delta_{DFC}} & s(s - L_p) & -sL_r \\ N_{\delta_{DFC}} & -sN_p & s(s - L_r) \end{bmatrix} \\
 &+ K_{\phi} \begin{bmatrix} Y_{\delta_{DFC}}^* & Y_{\delta_A}^* & s \\ L_{\delta_{DFC}} & -L_{\delta_A} & -sL_r \\ N_{\delta_{DFC}} & -N_{\delta_A} & s(s - N_r) \end{bmatrix} + K_{\psi} \begin{bmatrix} Y_{\delta_{DFC}}^* & -(g/U_0) & Y_{\delta_r}^* \\ L_{\delta_{DFC}} & s(s - L_p) & -L_{\delta_r} \\ N_{\delta_{DFC}} & -sN_p & -N_{\delta_r} \end{bmatrix} \\
 &+ K_{\phi}K_{\psi} \begin{bmatrix} Y_{\delta_{DFC}}^* & Y_{\delta_A}^* & Y_{\delta_r}^* \\ L_{\delta_{DFC}} & -L_{\delta_A} & -L_{\delta_r} \\ N_{\delta_{DFC}} & -N_{\delta_A} & -N_{\delta_r} \end{bmatrix}^* \quad (59)
 \end{aligned}$$

*The coefficients of determinants 2, 3, and 4 result from a theorem which states "If all the elements of a column are multiplied by the same quantity, C, the determinant is multiplied by C."

It is awkward and time consuming to write the above determinants which occur repeatedly in closed-loop analysis. Hence, a shorthand notation has been adopted which indicates the replacement of specific columns in the characteristic determinant by control vectors:

Numerator

$$N_{\delta_{DFC}}^{\beta} = \begin{bmatrix} Y_{\delta_{DFC}}^* & -(g/U_0) & s \\ L_{\delta_{DFC}} & s(s - L_p) & -sL_r \\ N_{\delta_{DFC}} & -sN_p & s(s - N_r) \end{bmatrix} \quad (60)$$

Coupling Numerators

$$N_{\delta_{DFC} \phi_a}^{\beta} = \begin{bmatrix} Y_{\delta_{DFC}}^* & Y_{\delta_A}^* & s \\ L_{\delta_{DFC}} & -L_{\delta_A} & -sL_r \\ N_{\delta_{DFC}} & -N_{\delta_a} & s(s - N_r) \end{bmatrix} \quad (61)$$

$$N_{\delta_{DFC} \psi_r}^{\beta} = \begin{bmatrix} Y_{\delta_{DFC}}^* & -(g/U_0) & Y_{\delta_r}^* \\ L_{\delta_{DFC}} & s(s - L_p) & -L_{\delta_r} \\ N_{\delta_{DFC}} & -sN_p & -N_{\delta_r} \end{bmatrix} \quad (62)$$

Coupling-Coupling Numerators

$$N_{\delta_{DFC} \phi_a \psi_r}^{\beta} = \begin{bmatrix} Y_{\delta_{DFC}}^* & Y_{\delta_A}^* & Y_{\delta_r}^* \\ L_{\delta_{DFC}} & -L_{\delta_A} & -L_{\delta_r} \\ N_{\delta_{DFC}} & -N_{\delta_A} & -N_{\delta_r} \end{bmatrix} \quad (63)$$

Using the shorthand notation of Eqs. 60 through 63, Eq. 59 is written:

$$N_{\delta_{DFC}}^{\beta''} = N_{\delta_{DFC}}^{\beta} + K_{\phi} N_{\delta_{DFC}}^{\beta} \delta_A^{\phi} + K_{\psi} N_{\delta_{DFC}}^{\beta} \delta_r^{\psi} + K_{\psi} K_{\phi} N_{\delta_{DFC}}^{\beta} \delta_A^{\phi} \delta_r^{\psi} \quad (64)$$

The characteristic equation or denominator of the β/δ_{DFC} transfer function is obtained by expanding Eq. 56 in like fashion with the following result:

$$\Delta'' = \Delta + K_{\phi} N_{\delta_A}^{\phi} + K_r N_{\delta_r}^{\psi} + K_{\phi} K_{\psi} N_{\delta_A \delta_r}^{\phi \psi} \quad (65)$$

Closed-loop analyses are routinely performed by manipulating the numerators and coupling numerators directly, i.e., the determinants are not written out. Rules for such manipulations have been established and are given in Ref. 27 on page 171.

If the feedbacks in this example involved equalization, the K 's would become transfer functions, e.g., substitute $G_{\delta_A}^{\phi} = K_{\phi}$ and $G_{\delta_r}^{\psi} = K_r$, etc.

LIMITING FORMS

It is physically enlightening to consider the limiting forms of a transfer function. That is, if we could close the $\phi \rightarrow \delta_A$ and $\psi \rightarrow \delta_r$ loops with extremely high gains, all the system poles would drive into the numerator zeros.* The numerator zeros therefore are representative of the "best we can do" for a given set of loop closures. For multiple loops with more than one control point the definition of the numerator includes coupling numerators such as in Eq. 64.

*In cases where stability considerations will not allow such high-gain closures, feedback equalization must be employed. However, such equalization usually results in dipoles which cancel, leaving the numerator roots as the dominant effect.

In the present example let us assume K_ϕ and K_ψ are very large. The limiting form of the β/δ_{DFC} transfer function can then be obtained as follows:

$$\left(\frac{\beta}{\delta_{DFC}}\right)_{\substack{\phi \rightarrow \delta_A \\ \psi \rightarrow \delta_r}} = \frac{N_{\delta_{DFC}}^{\beta}}{\Delta''} = \frac{\cancel{K_\psi} \cancel{K_\phi} N_{\delta_{DFC}}^{\beta} \phi \psi}{\cancel{K_\psi} \cancel{K_\phi} N_{\delta_r}^{\phi \psi}} \quad (65)$$

This is the expression shown in Table 2. The approximate factors of coupling numerators are usually very simple since at least two columns are made up of constants (control sensitivities). For example, the denominator of Eq. 66 is defined as:

$$N_{\delta_A \delta_r}^{\phi \psi} = \begin{bmatrix} s - Y_v & Y_{\delta_A}^* & Y_{\delta_r}^* \\ -L_{\beta} & -L_{\delta_A} & -L_{\delta_r} \\ -N_{\beta} & -N_{\delta_A} & -N_{\delta_r} \end{bmatrix} \quad (67)$$

Assuming ideal controls, e.g., only L_{δ_A} and N_{δ_r} are finite in Eq. 67:

$$N_{\delta_A \delta_r}^{\phi \psi} = L_{\delta_A} N_{\delta_r} (s - Y_v) \quad (68)$$

If we retain the assumption that the ailerons only produce rolling moments and the rudders yawing moments, Eq. 63 becomes:

$$N_{\delta_{DFC} \delta_A \delta_r}^{\beta \phi \psi} = N_{\delta_r} L_{\delta_A} Y_{\delta_{DFC}}^* \quad (69)$$

Note that this expression could also be obtained by assuming that δ_{DFC} produced only side acceleration (mounted at c.g.). Using Eqs. 68 and 69 in Eq. 66:

$$\left(\frac{\beta}{\delta_{DFC}} \right)_{\substack{\phi \rightarrow \delta_A \\ \psi \rightarrow \delta_R}} = \frac{Y_{\delta_{DFC}}^*}{(s - Y_v)} \quad (70)$$

This is the expression shown as the limiting form for the lateral translation mode in Table 2.

As a final item, it is interesting to note that we can investigate the effect of non-ideal locations for the direct force control surface. For example, if it is mounted well forward of the c.g. (as in the YF-16 CCV), $N_{\delta_{DFC}}$ would be large and Eq. 69 becomes:

$$N_{\delta_{DFC}}^{\beta} \phi_{\delta_A} \psi_{\delta_R} \doteq L_{\delta_A} (Y_{\delta_{DFC}}^* N_{\delta_R} - N_{\delta_{DFC}} Y_{\delta_R}^*) \quad (71)$$

wherein side force due to rudder ($Y_{\delta_R}^*$) is seen to be an important factor. If indeed $Y_{\delta_R}^*$ is large, this effect could be included in Eq. 67,** resulting in the limiting β/δ_{DFC} response for a forward mounted side force control as in the YF-16:

*Here we have implicitly assumed $Y_{\delta_A} = 0$.

**The other cross derivatives, $Y_{\delta_A}^*$, L_{δ_R} , and N_{δ_A} , are assumed negligible. Hence

$$N_{\delta_A}^{\phi \ r} \psi_{\delta_R} = L_{\delta_A} [(s - Y_v) N_{\delta_R} + N_{\beta} Y_{\delta_R}^*]$$

$$\left(\frac{\beta}{\delta_{DFC}} \right)_{\substack{\phi \rightarrow \delta_A \\ \psi \rightarrow \delta_r}} = \frac{Y_{\delta_{DFC}} - N_{\delta_{DFC}}(Y_{\delta_r}/N_{\delta_r})}{[s - Y_v + N_{\beta}(Y_{\delta_r}^*/N_{\delta_r})]} \quad (72)$$

The physical interpretations of the additional terms in Eq. 72 over Eq. 70 are:

- For statically stable configurations (N_{β} negative) the forward mounted side force generator will increase the basic bandwidth defined by Y_v [e.g., $N_{\beta}(Y_{\delta_r}^*/N_{\delta_r})$ adds to Y_v]. This reflects the fact that the rudder must deflect so as to produce forces opposite to the direct force control surface to cancel the moment due to forward mounting. This opposing force tends to damp the motion and hence augment Y_v .
- The opposing force also reduces the control effectiveness, i.e., subtracts from $Y_{\delta_{DFC}}^*$ in the numerator of Eq. 72.

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